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Forming and Forging

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Foreword

Forming and forging processes are among the oldest and most important of materials-related technologies. Volume 14 of the 9th Edition of Metals Handbook describes these processes comprehensively, with accuracy and clarity.

Today, industry must continuously evaluate the costs of competitive materials and the operations necessary for converting each material into finished products. Manufacturing economy with no sacrifice in quality is paramount. Therefore, "precision" forming methods, net and near-net shape processing, and modern statistical and computer-based process design and control techniques are more important than ever. This book serves as an invaluable introduction to this new technology, and also provides a strong foundation with regard to more standard, well-established metalworking operations.

This is the second of three volumes in the 9th Edition devoted to the technologies used to form metal parts. Volume 7, Powder Metallurgy, was published in 1984; Volume 15, Casting, will follow the present volume. The combination of these significant contributions to the metallurgical literature will provide Handbook readers with unprecedented coverage of metal forming methods.

A successful Handbook is the culmination of the time and efforts of hundreds of contributors. To those individuals listed in the next several pages, we extend our sincere thanks. The Society is especially indebted to Dr. S.L. Semiatin for his tireless efforts in organizing and editing this volume. Finally, we are grateful for the support and guidance provided by the ASM Handbook Committee and the skill of an experienced editorial staff. As a result of these combined efforts, the tradition of excellence associated with the Metals Handbook continues.

William G. Wood

President, ASM International

Edward L. Langer

Managing Director, ASM International

Preface

Metalworking is one of the oldest of materials-related technologies and accounts for a large percentage of fabricated metal products. The usefulness of the deformation processes that comprise metalworking technology is indicated by the wide variety of parts of simple and complex shape with carefully tailored mechanical and physical properties that are made routinely in industry. It is difficult to imagine what our lives would be like without such products.

The 8th Edition of Metals Handbook treated various aspects of metalworking in two separate volumes: forging was addressed in the volume Forging and Casting, and sheet forming in the one on Forming. In the present 9th Edition, the decision was made to bring all this information together in one Handbook.

During the editing process, all of the articles from the 8th Edition volumes were reviewed for technical content. Some required only minor revision, others were totally rewritten. A section on other bulk forming processes was added to provide a balance to the extensive collection of articles on forging. In this new section, topics such as conventional hot extrusion; hydrostatic extrusion; wire, rod, and tube drawing; and flat, bar, and shape rolling are discussed.

In addition, approximately 20 new articles have been added to describe advances in metalworking technology that have occurred since publication of the 8th Edition. These advances can be broadly grouped in the categories of new processes, new materials technologies, and new methods of process design and control. New processes include isothermal and hot-die forging, precision forging, and superplastic forming of sheet metals. New materials technologies center on the development and widespread use of thermal-mechanical processing, particularly for aerospace alloys, and concepts of metal workability and formability. In the area of process design and control, several articles were written to summarize the powerful mathematical and statistical methods that have been developed to take metalworking from an experienced-based art into the realm of scientific technology. These techniques have allowed forming engineers to design dies and preforms for single and multistage processes without actually constructing tooling or tying up expensive production equipment. With the development of user-friendly computer programs and low-cost computers, such techniques are finding increasing acceptance by manufacturers worldwide.

Thanks are due to the various individuals who organized, wrote, edited, and reviewed various sections and articles in this Handbook; their voluntary contributions of time and expertise are invaluable in a project such as this. We would also like to extend thanks to the ASM Handbook staff. The amount of careful and devoted work that the staff put into the Handbook cannot really be appreciated until one actually works with them on one of these volumes.

S.L. Semiatin

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Introduction to Forming and Forging Processes

S.L. Semiatin, Battelle Columbus Division

Introduction

METALWORKING consists of deformation processes in which a metal billet or blank is shaped by tools or dies. The design and control of such precesses depend on an understanding of the characteristics of the workpiece material, the conditions at the tool/workpiece interface, the mechanics of plastic deformation (metal flow), the equipment used, and the finished-product requirements. These factors influence the selection of tool geometry and material as well as processing conditions (for example, workpiece and die temperatures and lubrication). Because of the complexity of many metalworking operations, models of various types, such as analytic, physical, or numerical models, are often relied upon to design such processes.

This Volume presents the state of the art in metalworking processes. Various major sections of this Volume deal with descriptions of specific processes, selection of equipment and die materials, forming practice for specific alloys, and various aspects of process design and control. This article will provide a brief historical perspective, a classification of metalworking processes and equipment, and a summary of some of the more recent developments in the field.

Introduction to Forming and Forging Processes

S.L. Semiatin, Battelle Columbus Division

Historical Perspective

Metalworking is one of three major technologies used to fabricate metal products; the others are casting and powder metallurgy. However, metalworking is perhaps the oldest and most mature of the three. The earliest records of metalworking describe the simple hammering of gold and copper in various regions of the Middle East around 8000 B.C. The forming of these metals was crude because the art of refining by smelting was unknown and because the ability to work the material was limited by impurities that remained after the metal had been separated from the ore. With the advent of copper smelting around 4000 B.C., a useful method became available for purifying metals through chemical reactions in the liquid state. Later, in the Copper Age, it was found that the hammering of metal brought about desirable increases in strength (a phenomenon now known as strain hardening). The quest for strength spurred a search for alloys that were inherently strong and led to the utilization of alloys of copper and tin (the Bronze Age) and iron and carbon (the Iron Age). The Iron Age, which can be dated as beginning around 1200 B.C., followed the beginning of the Bronze Age by some 1300 years. The reason for the delay was the absence of methods for achieving the high temperatures needed to melt and to refine iron ore.

Most metalworking was done by hand until the 13th century. At this time, the tilt hammer was developed and used primarily for forging bars and plates. The machine used water power to raise a lever arm that had a hammering tool at one end; it was called a tilt hammer because the arm tilted as the hammering tool was rised. After raising the hammer, the blacksmith let it fall under the force of gravity, thus generating the forging blow. This relatively simple device remained in service for some centuries.

The development of rolling mills followed that of forging equipment. Leonardo da Vinci's notebook includes a sketch of a machine designed in 1480 for the rolling of lead for stained glass windows. In 1945, da Vinci is reported to have rolled flat sheets of precious metal on a hand-operated two-roll mill for coin-making purposes. In the following years, several designs for rolling mills were utilized in Germany, Italy, France, and England. However, the development of large mills capable of hot rolling ferrous materials took almost 200 years. This relatively slow progress was primarily due to the limited supply of iron. Early mills employed flat rolls for making sheet and plate, and until the middle of the 18th century, these mills were driven by water wheels.

During the Industrial Revolution at the end of the 18th century, processes were devised for making iron and steel in large quantities to satisfy the demand for metal products. A need arose for forging equipment with larger capacity. This need was answered with the invention of the high-speed steam hammer, in which the hammer is raised by steam power, and the hydraulic press, in which the force is supplied by hydraulic pressure. From such equipment came products ranging from firearms to locomotive parts. Similarly, the steam engine spurred developments in rolling, and in the 19th century, a variety of steel products were rolled in significant quantities.

The past 100 years have seen the development of new types of metalworking equipment and new materials with special properties and applications. The new types of equipment have included mechanical and screw presses and high-speed tandem rolling mills. The materials that have benefited from such developments in equipment range from the ubiquitous low-carbon steel used in automobiles and appliances to specialty aluminum-, titanium-, and nickel-base alloys. In the last 20 years, the formulation of sophisticated mathematical analyses of forming processes has led to higher-quality products and increased efficiency in the metalworking industry.

S.L. Semiatin, Battelle Columbus Division

Classification of Metalworking Processes

In metalworking, an initially simple part--a billet or a blanked sheet, for example--is plastically deformed between tools (or dies) to obtain the desired final configuration. Metal-forming processes are usually classified according to two broad categories:

- Bulk, or massive, forming operations
- Sheet forming operations*

In both types of process, the surfaces of the deforming metal and the tools are in contact, and friction between them may have a major influence on material flow. In bulk forming, the input material is in billet, rod, or slab form, and the surface-to-volume ratio in the formed part increases considerably under the action of largely compressive loading. In sheet forming, on the other hand, a piece of sheet metal is plastically deformed by tensile loads into a three-dimensional shape, often without significant changes in sheet thickness or surface characteristic.

Processes that fall under the category of bulk forming have the following distinguishing features (Ref 1):

- The deforming material, or workpiece, undergoes large plastic (permanent) deformation, resulting in an appreciable change in shape or cross section
- The portion of the workpiece undergoing plastic deformation is generally much larger than the portion undergoing elastic deformation; therefore, elastic recovery after deformation is negligible

Examples of generic bulk forming processes are extrusion, forging, rolling, and drawing. Specific bulk forming processes are listed in Table 1.

Table 1 Classification of bulk (massive) forming processes

Forging
Closed-die forging with flash
Closed-die forging without flash
Coining
Electro-upsetting
Forward extrusion forging
Backward extrusion forging
Hobbing
Isothermal forging
Nosing
Open-die forging
Rotary (orbital) forging
Precision forging
Metal powder forging
Radial forging
Upsetting
Rolling
Sheet rolling
Shape rolling

Tube rolling Ring rolling Rotary tube piercing Gear rolling Roll forging Cross rolling Surface rolling Shear forming Tube reducing
Extrusion Nonlubricated hot extrusion Lubricated direct hot extrusion Hydrostatic extrusion
Drawing Drawing Drawing with rolls Ironing Tube sinking

Source: Ref 1

The characteristics of sheet metal forming processes are as follows (Ref 1):

- The workpiece is a sheet or a part fabricated from a sheet
- The deformation usually causes significant changes in the shape, but not the cross-sectional area, of the sheet.
- In some cases, the magnitudes of the plastic and the elastic (recoverable) deformations are comparable; therefore, elastic recovery or springback may be significant.

Examples of processes that fall under the category of sheet metal forming are deep drawing, stretching, bending, and rubber-pad forming. Other processes are listed in Table 2.

Table 2 Classification of sheet metal forming processes

Bending and straight flanging Brake bending Roll bending
Surface contouring of sheet Contour stretch forming (stretch forming) Androforming Age forming Creep forming Die-quench forming Bulging Vacuum forming

Linear stretch forming (stretch forming) Linear roll forming (roll forming)
Deep recessing and flanging
Spinning (and roller flanging) Deep drawing Rubber-pad forming Marform process Rubber-diaphragm hydroforming (fluid cell forming or fluid forming)
Shallow recessing
Dimpling Drop hammer forming Electromagnetic forming Explosive forming Joggling

Source: Ref 1

Reference cited in this section

1. T. Altan, S.I. Oh, and H.L. Gegel, Metal Forming: Fundamentals and Applications, American Society for Metals, 1983

Note cited in this section

* Sheet forming is also referred to as forming. In the broadest and most accepted sense, however, the term forming is used to described bulk as well as sheet forming processes.

Introduction to Forming and Forging Processes

S.L. Semiatin, Battelle Columbus Division

Types of Metalworking Equipment

The various forming processes discussed above are associated with a large variety of forming machines or equipment, including the following (Ref 1):

- Rolling mills for plate, strip, and shapes
- Machines for profile rolling from strip
- Ring-rolling machines
- Thread-rolling and surface-rolling machines
- Magnetic and explosive forming machines
- Draw benches for tube and rod; wire- and rod-drawing machines
- Machines for pressing-type operations (presses)

Among those listed above, pressing-type machines are the most widely used and are applied to both bulk and sheet forming processes. These machines can be classified into three types: load-restricted machines (hydraulic presses), stroke-restricted machines (crank and eccentric, or mechanical, presses), and energy-restricted machines (hammers and screw presses). The significant characteristics of pressing-type machines comprise all machine design and performance data that are pertinent to the economical use of the machine. These characteristics include:

- Characteristics for load and energy: Available load, available energy, and efficiency factor (which equals the energy available for workpiece deformation/energy supplied to the machine)
- Time-related characteristics: Number of strokes per minute, contact time under pressure, and velocity under pressure.
- Characteristics for accuracy: For example, deflection of the ram and frame, particularly under off-center loading, and press stiffness

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Introduction to Forming and Forging Processes

S.L. Semiatin, Battelle Columbus Division

Recent Developments in Metalworking

Over the last 20 years, metalworking practice has seen advances with regard to the development of new processes and new materials, the understanding and control of material response during forming, and the application of sophisticated process design tools. Some of these technological advances will be summarized in the following sections in this article.

New Processes

A number of processes have recently been introduced or accepted. These include a variety of forging processes, such as radial, precision, rotary, metal powder, and isothermal forging, as well as sheet forming processes, such as superplastic forming. Laser cutting and abrasive waterjet cutting of sheet and plate materials are also finding increased use. Each of these processes is described in greater detail in subsequent articles in this Volume.

Radial forging is a technique that is most often used to manufacture axisymmetrical parts, such as gun barrels. Radial forging machines (Fig. 1) use the radial hot- or cold-forging principle with three, four, or six hammers to produce solid or hollow round, square, rectangular, or profiled sections. The machines used for forging large gun barrels are of a horizontal type and can size the bore of the gun barrel to the exact rifling that is machined on the mandrel. Products produced by radial forging often have improved mechanical and metallurgical properties as compared to those produced by other, more conventional techniques.

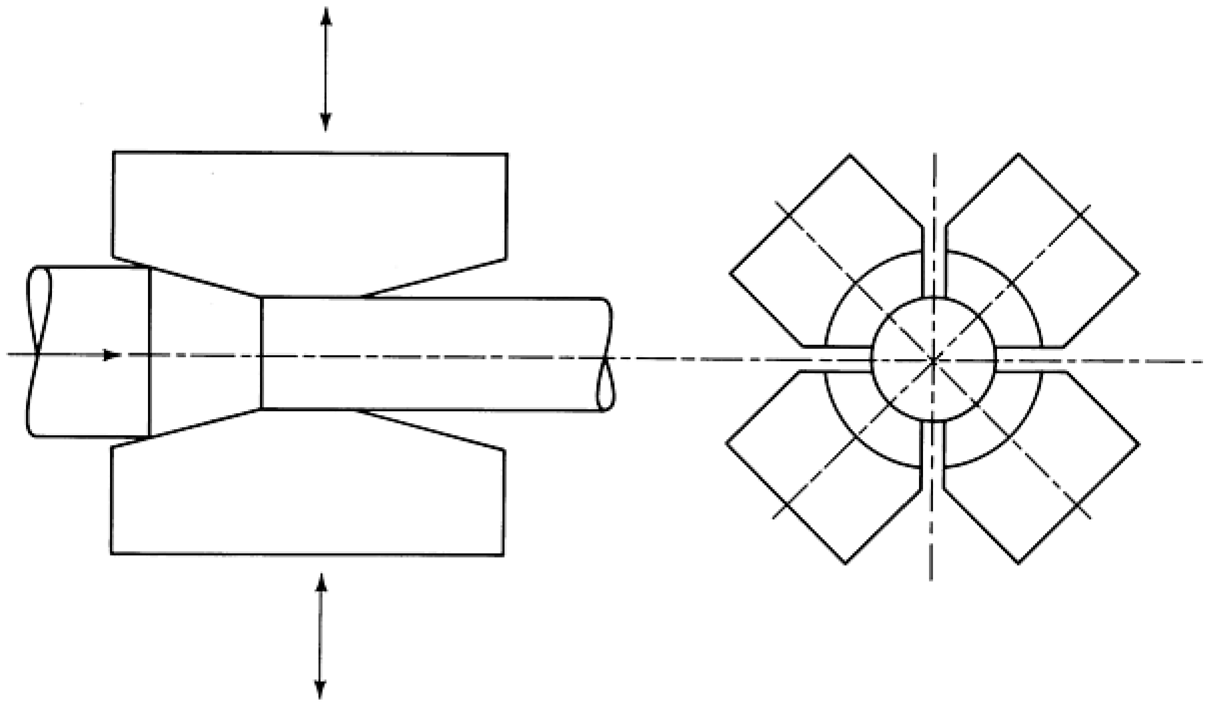

Fig. 1 Workpiece and tooling configurations for radial forging. Source: Ref 2.

Rotary, or orbital, forging is a two-die forging process that deforms only a small portion of the workpiece at a time in a continuous manner. As shown in Fig. 2, the axis of the upper die is tilted at a slight angle with respect to the axis of the lower die, causing the forging force to be applied to only a small area of the workpiece. As one die rotates relative to the other, the contact area between die and workpiece (called the footprint) continually progresses through the workpiece until the final shape is obtained. The tilt angle between the two dies has a major effect on the size of the footprint and therefore on the amount of forging force applied to the workpiece. Rotary forging requires as little as one-tenth the force needed for conventional forging processes. The smaller forging forces result in lower die and machine deflections and therefore in the ability to make intricate parts to a high degree of accuracy.

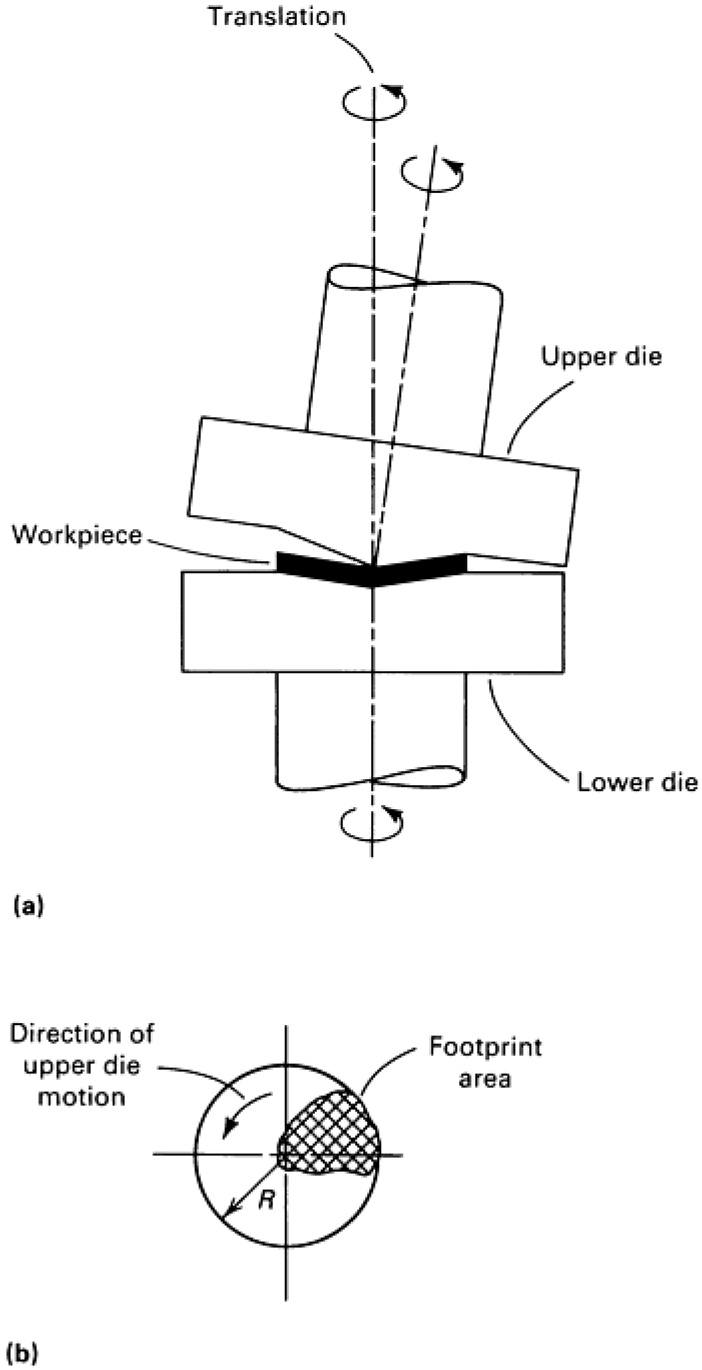

Fig. 2 Rotary (orbital) forging. Die arrangement (a) and top view (b) of the workpiece indicating the die-workpiece contact area (footprint). Source: Ref 3.

Precision forging, also known as draftless forging, is a relatively recent development that is distinguished from other types of forging principally by finished products with thinner and more detailed geometric features, virtual elimination of drafted surfaces and machining allowances, varying die parting line locations, and closer dimensional tolerances. These types of parts are most commonly manufactured from light metals, such as aluminum, and more recently from titanium for aerospace applications in which weight, strength, and intricate shaping are important considerations, along with price and delivery (see the articles "Forging of Aluminum Alloys" and "Forging of Titanium Alloys," respectively, in this Volume).

Precision forging achieves close tolerances and low drafts through the use of die inserts, improved accuracy in die sinking, and close control of process temperatures and pressures during forging. Modified die designs are also frequently used. One such design is known as through die (Fig. 3), and it derives its name from the fact that the outer periphery of the forging cavity is machined completely through the die. An upper and lower punch enter and forge the part entirely within this ring. The top punch is then retracted by the press stroke, and the completed forging is ejected by raising the lower punch attached to a knockout mechanism below.

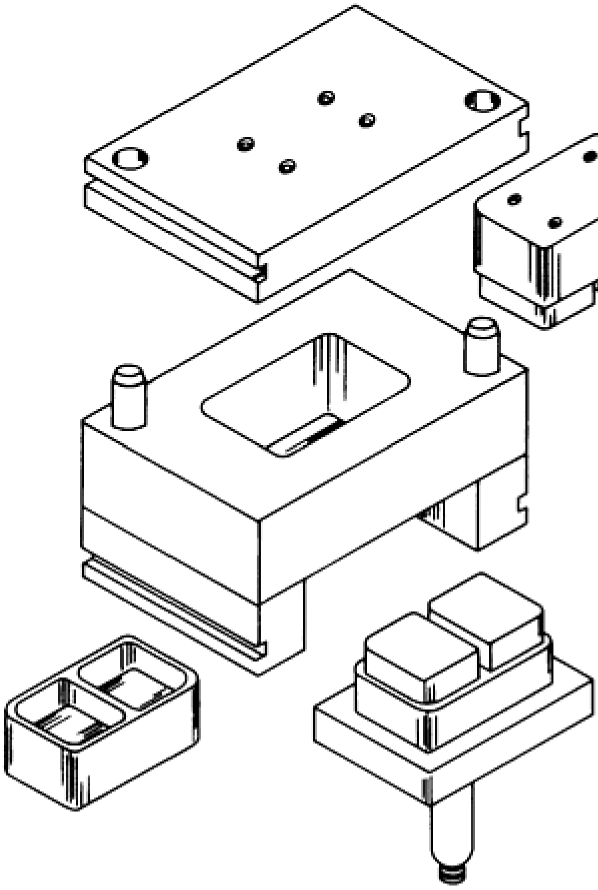

Fig. 3 Through-die design for precision forging. Source: Aluminum Precision Products, Inc.

Powder forging is a process in which sintered preforms are hot forged to 100% of theoretical density. Powder forging is primarily used for ferrous parts and difficult-to-work superalloys that require high service integrity, and it is most suitable for symmetrical shapes containing large holes and parts that would otherwise require a large amount of machining. In addition to the article "Powder Forging" in this Volume, detailed information and property data related to forged powder metallurgy products can be found in Powder Metal Technologies and Applications, Volume 7 of the ASM Handbook.

Isothermal and hot-die forging are hot-forging processes in which the dies are at the same (isothermal forging) or nearly the same (hot-die forging) temperature as the workpiece. The processes are primarily used for costly materials, such as titanium and nickel-base alloys, which possess fine, stable two-phase microstructures at hot-working temperatures. Such microstructures often give rise to a property known as superplasticity. Superplasticity is characterized by good die-filling capacity in bulk forming processes and high tensile elongations in sheet forming applications.

The total (or partial) elimination of die chilling in isothermal (or hot-die) forging, in addition to the superplastic properties of the workpiece material, allows forging to closer tolerance than is possible with conventional hot forging, in which the die temperature is typically only slightly above ambient. As a result, machining and material costs are reduced. Moreover, elimination of die chilling permits a reduction in the number of preforming and blocking dies necessary for forging a given part. In addition, because die chilling is not a problem, a slow ram speed machine, such as a hydraulic press, can be used. The lower strain rate imposed gives rise to a lower material flow stress and therefore a lower forging pressure. The net result is that larger parts can be forged in equipment of capacity smaller than that required for conventional forging. Figure 4 shows a number of Alloy 100 (UNS N13100) jet engine disks made using a version of isothermal forging known as "Gatorizing."

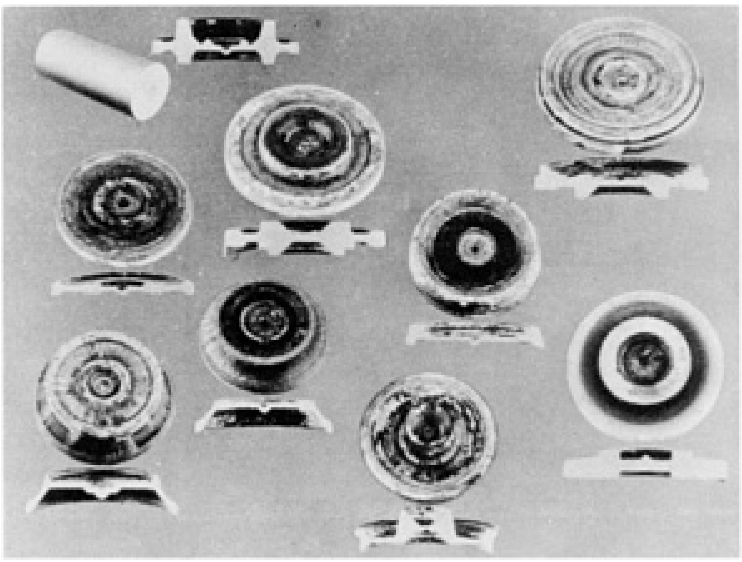

Fig. 4 Typical isothermally forged (Gatorized) jet engine disks made from Alloy 100. A starting billet preform is shown in the upper left-hand corner of the photograph. Source: Ref 4.

Superplastic forming is the sheet forming counterpart to isothermal forging. The isothermal, low strain rate conditions in superplastic forming result in low workpiece flow stress. Therefore, gas pressure, rather than a hard punch, is most often used to carry out a stretching-type operation; the only tooling requirement is a female die (Fig. 5). The very high tensile ductilities characteristic of superplastically formed sheet alloys such as Ti-6Al-4V, Zn-22Al, and aluminum alloy 7475 enable the forming of parts of very complex shape. Although cycle times for superplastic forming are relatively long (of the order of 10 min per part), economies of manufacture are realized primarily through reduced machining and assembly costs. The latter savings is a result of the fact that individual superplastically formed parts are usually used as replacements for assemblies of many separate component parts.

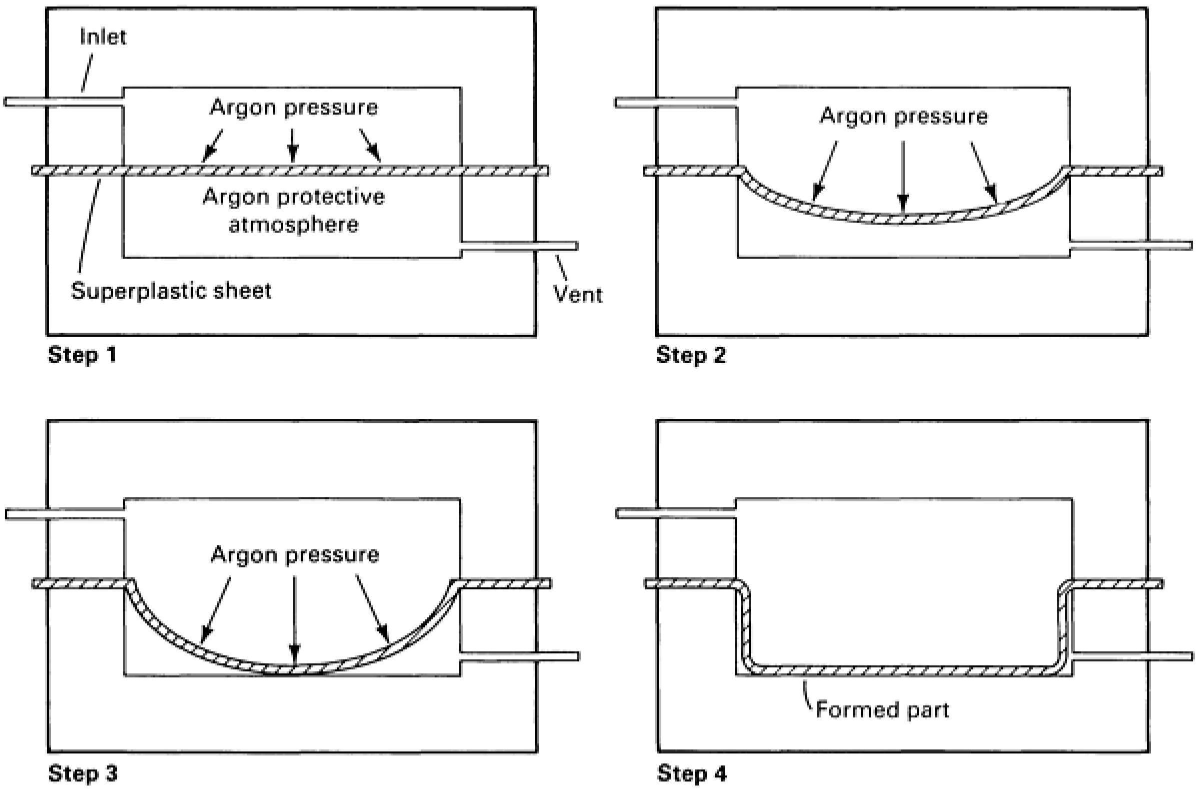

Fig. 5 Illustration of the blow-forming method of superplastic forming. Source: Ref 5.

Laser cutting is an increasingly popular method of cutting sheet materials accurately. Laser cutting typically makes use of a computer numerical control program that allows new cutting paths to be quickly generated. In addition to rapid cutting, laser cutting offers such advantages as precision (cutting accuracy of 0.13 mm, or 0.005 in., or less), the ability to cut most materials (including metals, ceramics, plastics, and glass), minimal heat-induced distortion, and very clean straight-sided cuts. The fact that cutting is done under computer control also provides ease of cutting complex shapes in sheet stock, high material utilization, excellent pattern reproducibility, and economical low-volume production. Laser cutting systems are generally used for cutting prototypes or small production runs from sheet stock. Hard tooling is usually more economical for high volumes. However, one high-volume application of lasers is the trimming of automobile parts. These parts, are being made of thinner materials, and trim dies capable of cutting to the required tolerances are so expensive that laser cutting is cost-competitive even for the large lot sizes involved.

Abrasive waterjet cutting is a process developed in the late 1960s which relies on the impingement of a high-velocity, high-pressure, abrasive-laden waterjet onto the workpiece for the purpose of cutting. Among the advantages of the technique are high cutting rates, high quality of the cut surface, almost total absence of heat generation within the workpiece (thus minimizing the development of a heat-affected zone), and a relatively narrow kerf. Applications of abrasive waterjet cutting can be found in the machining of hard metals (for example, superalloys, high-strength steels, and titanium alloys) and a number of nonmetals (for example, concrete, ceramics, composites, and plastics). The only major limitation of the process is the inability to mill, turn, or drill blind holes or perform other operations that involve cutting or drilling to a partial depth.

New Materials Developments

An increased understanding of material behavior during deformation has led to the improved design of metalworking processes. Two areas of particular significance in this regard are the emergence of thermal-mechanical processing techniques and the development of metal workability/formability relationships.

Thermal-mechanical processing refers to the design and control of the individual metalworking and heat treatment steps in a manufacturing process in order to enhance final properties. Originally used as a method of producing high-strength or high-toughness alloy steels, thermal-mechanical processing is now routinely used for other alloy systems, especially those based on nickel.

Most thermal-mechanical processing treatments for steels rely on deformation that is imposed before, during, or after austenite transformation. The various types of treatments are summarized in Table 3. This classification, based on the relative positions of deformation and transformation in the treatment cycle, has other justification in that the tensile stress-strain curves and the rate of increase in yield strength with increasing deformation (Fig. 6) have been found to be broadly similar for a variety of steels subjected to a given class of treatment and have been found to differ for each of the classes.

Table 3 Classification of thermal-mechanical processing treatments for high-strength steels

Type I

Deformation before austenite transformation
Normal hot-working processes
Deformation before transformation to martensite

Type II

Deformation during austenite transformation
Deformation during transformation to martensite
Deformation during transformation to ferrite-carbide aggregates

Type III

Deformation after austenite transformation
Deformation of martensite followed by tempering
Deformation of tempered martensite followed by aging
Deformation of isothermal transformation products

Source: Ref 6

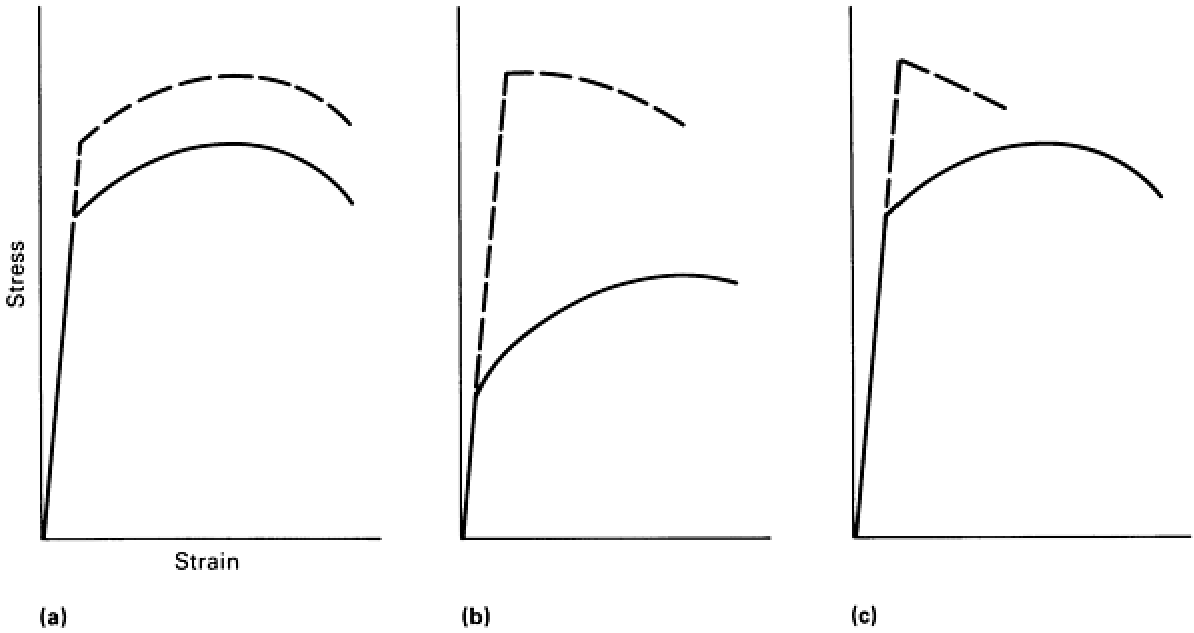

Fig. 6 Effects of different classes of thermal-mechanical treatments on the shape of the tensile stress-strain curve. (a) Type I. (b) Type II. (c) Type III. See Table 3 for description of the types of treatments. Source: Ref 6.

In the thermal-mechanical processing of nickel-base superalloys, metalworking temperature is carefully controlled (especially during finish forming) to make use of the structure control effects of second phases (see, for example, the articles "Forging of Heat-Resistant Alloys" and "Forging of Nickel-Base Alloys" in this Volume). Above the optimal working temperature range, the structure control phases go into solution and lose their effect in controlling grain size and structure. Below this range, extensive fine precipitates are formed, and the alloy becomes too stiff to process. Proper thermal-mechanical processing leads to excellent combinations of tensile, fatigue, and creep properties.

Workability and formability are the terms that are commonly used to refer to the ease with which metal can be shaped during bulk and sheet forming operations, respectively. In the broadest sense, workability and formability indices provide quantitative estimates of the strength properties of a metal (and therefore the required working loads) and its resistance to failure. However, the latter characteristics (that is, ductility or failure resistance) is usually of primary concern. The techniques used to estimate this property vary, depending on the class of forming operation.

In bulk forming, the most common types of failures are those known as free surface fracture (at cold-working temperatures) or triple-point cracking/cavitation (at hot-working temperatures). A vast array of mechanical tests and theoretical analyses have been developed for predicting failures of these and other types during forging, extrusion, rolling, and other bulk forming operations. These tests and analyses are summarized in Ref 7 and are discussed in the Section "Evaluation of Workability" in this Volume. Other common test techniques used to gage bulk workability include the uniaxial upset, flanged or tapered compression, notched-bar upset, and wedge tests.

One of the most successful and useful design tools to come from bulk workability research is the workability diagram for free surface fracture during the cold working of wrought and powder metals. An example of a workability diagram of this type is shown in Fig. 7. The graph indicates the locus of free surface normal strains (one tensile and one compressive) that cause fracture. These diagrams are determined by mechanical tests such as those mentioned above. For many metals, the fracture locus is a straight line of slope $-\frac{1}{2}$. Some metals have a bilinear failure locus. The workability diagrams are used during process design by plotting calculated or estimated surface strain paths that are to be imposed during forming on the fracture locus diagram (Fig. 7). If the final strains lie above the locus, part failure is likely, and changes are necessary in preform design, lubrication, and/or material. The fracture locus concept has been used to prevent free surface cracking in forging and to prevent edge cracking in rolling. With modifications, the fracture locus approach has also provided insight into such failure modes as center bursting in extrusion and forging and die-workpiece contact fractures in forging.

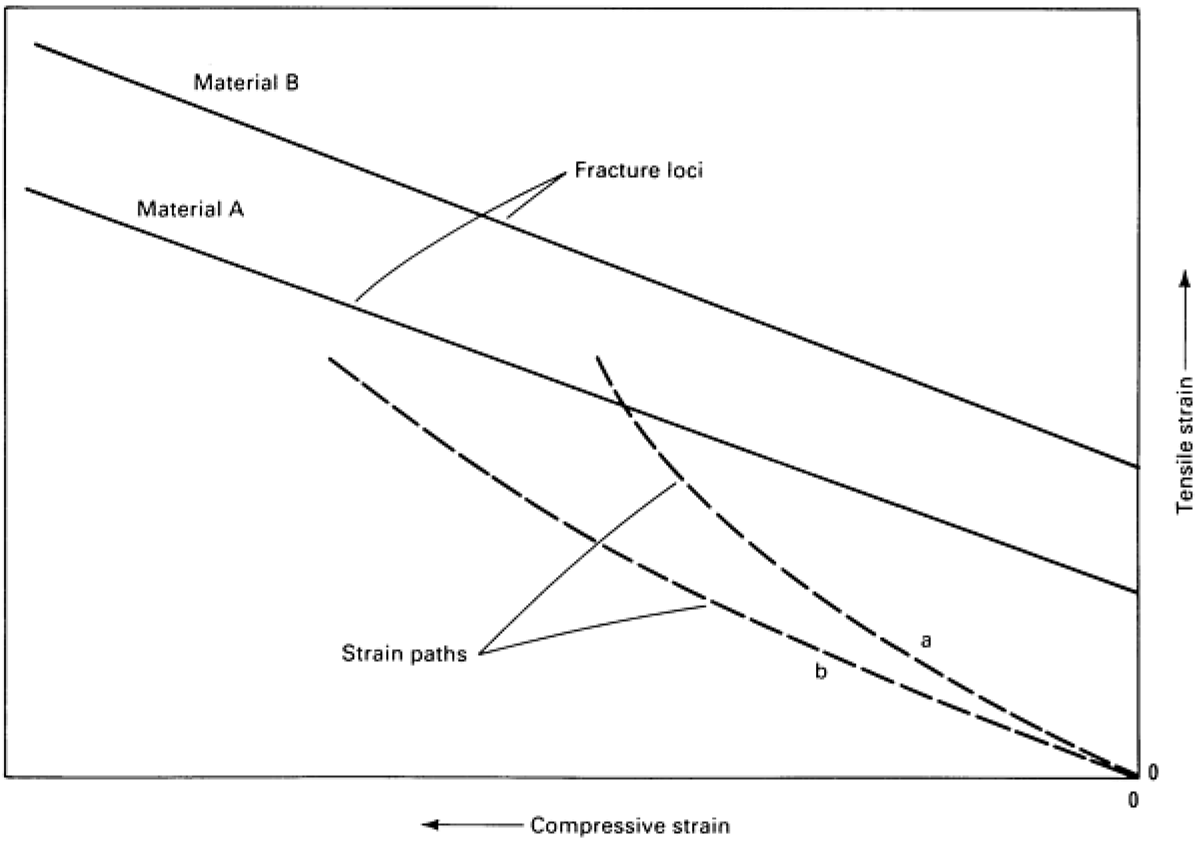

Fig. 7 Schematic workability diagrams for bulk forming processes. Strain path (a) would lead to failure for material A. Both strain paths (a and b) can be used for the successful forming of material B. Source: Ref 8.

A related concept is the forming limit diagram used to quantify sheet metal formability. An example is shown in Fig. 8. As for the bulk workability fracture locus, the sheet metal forming limit diagram is the locus of normal surface strains that give rise to failure. The magnitudes of the failure strains are usually controlled by one of two processes: localized through-thickness thinning or fracture prior to localized thinning. In either case, the forming limit diagram is most easily determined by stretching experiments using a hemispherical punch. Strain path and failure strains (in terms of the so-called major and minor strains) are varied by changes in lubrication and test blank width. Experimentally determined forming limit diagrams are then compared to the strains that are to be developed during forming to determine the possibility for failure. Additional information on forming limit diagrams can be found in the article "Formability Testing of Sheet Metals" in this Volume.

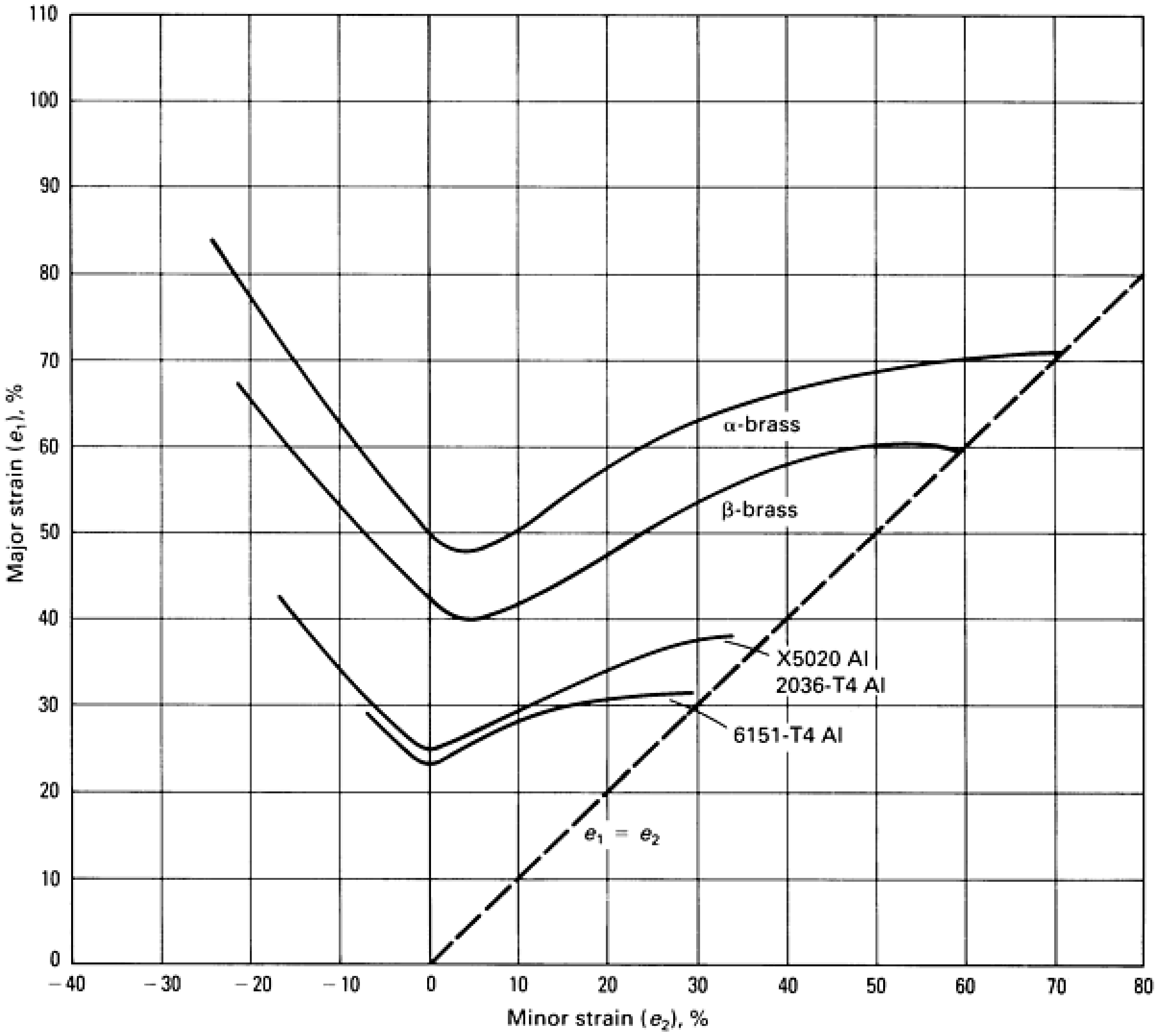

Fig. 8 Typical forming limit curves for α-brass (70Cu-30Zn), β-brass (61Cu-39Zn), X5020-T4 aluminum, 2036-T4 aluminum, and 6151-T4 aluminum. Source: Ref 9.

Process Simulation

The development of powerful computer-based simulation techniques, such as those based on the finite-element method, has provided a vital link between advances in tooling and equipment design, on the one hand, and an improved understanding of materials behavior on the other. Inputs to finite-element codes include the characteristics of the workpiece material (flow stress and thermal properties) and the tool/workpiece interface (friction and heat transfer properties), as well as workpiece and tooling geometries. Typical outputs include predictions of forming load; strain, strain rate, and temperature contour plots; and tooling deflections. This information can serve a number of design functions, such as selection of press capacity, determination of success or failure with regard to material workability or formability, and estimation of likely sources of tooling failure (abrasive wear, thermal fatigue, and so on).

Process simulation techniques also provide a method for preform and die design through the ability to determine metal flow patterns without constructing tooling or conducting expensive in-plant trials. In addition, the output from process simulations can be helpful in selecting variables that are useful in process control (for example, ram speed or load monitoring) or finished-product quality control. The advent of these computer-based technologies will help in eliminating the hidden costs of trial-and-error design and in increasing productivity in the metalworking industries. Detailed information on finite-element method process simulation for bulk and sheet forming operations can be found in the articles "Modeling Techniques Used in Forging Process Design" and "Process Modeling and Simulation for Sheet Forming" in this Volume.

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Future Trends

The metalworking industry is likely to see changes in the major areas of materials, processes, and process design. Some of these changes will include the following.

Materials. Developments in materials will greatly affect the metals that are formed. These will range from aluminum- and titanium-base alloys to alloy steels and superalloys. New classes of aluminum alloys that will be processed include aluminum-lithium alloys, SiC whisker-reinforced aluminum metal-matrix composites, and high-strength high-temperature powder metallurgy alloys. More use will be made of β-titanium alloys, which combine good strength and toughness, and there will be increased use of thermal-mechanical processing for titanium alloys and superalloys. In the ferrous area, microalloyed steels, which permit elimination of final heat treatment by controlled cooling afer hot working, are becoming increasingly popular for a variety of automotive applications.

Processes. In the forming process area, metalworking methods that give rise to net or near-net shape will be increasingly used to conserve materials and to reduce machining costs. These processes include precision forging, isothermal and hot-die forging, and superplastic forming of sheet materials. There will also be increased use of automatic tool change systems as lot sizes and delivery times decrease.

Process Design. With the development of user-friendly programs and the decreasing cost of computer hardware, there will be significant growth in computer-aided techniques in tooling design and process control. In particular, there will be more interaction between parts users and parts vendors during the design stage. Process simulation will streamline the design process, and this will decrease delivery times as well as the overall cost of fabricated components.

Introduction to Forming and Forging Processes

S.L. Semiatin, Battelle Columbus Division

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Hammers and Presses for Forging

Revised by Taylan Altan, The Ohio State University

Introduction

FORGING MACHINES can be classified according to their principle of operation. Hammers and high-energy-rate forging machines deform the workpiece by the kinetic energy of the hammer ram; they are therefore classed as energy-restricted machines. The ability of mechanical presses to deform the work material is determined by the length of the press stroke and the available force at various stroke positions. Mechanical presses are therefore classified as stroke-restricted machines. Hydraulic presses are termed force-restricted machines because their ability to deform the material depends on the maximum force rating of the press. Although they are similar in construction to mechanical and hydraulic presses, screw-type presses are classified as energy-restricted machines.

Hammers and Presses for Forging

Revised by Taylan Altan, The Ohio State University

Hammers

Historically, hammers have been the most widely used type of equipment for forging. They are the least expensive and most flexible type of forging equipment in the variety of forging operations they can perform. Hammers are capable of developing large forces and have short die contact times. The main components of a hammer are a ram, frame assembly, anvil, and anvil cap. The anvil is directly connected to the frame assembly, the upper die is attached to the ram, and lower die is attached to the anvil cap.

In operation, the workpiece is placed on the lower die. The ram moves downward, exerting a force on the anvil and causing the workpiece to deform. Forging hammers can be classified according to the method used to drive the ram downward. Various types of hammers are described in the following sections; Table 1 compares the capacities of some of these types.

Table 1 Capacities of various types of forging hammers

Type of hammer	Ram weight		Maximum blow energy		Impact speed		Number of blows per minute
	kg	lb	kJ	ft·lb	m/s	ft/s	
Board drop	45-3400	100-7500	47.5	35,000	3-4.5	10-15	45-60
Air or steam lift	225-7250	500-16,000	122	90,000	3.7-4.9	12-16	60
Electrohydraulic drop	450-9980	1000-22,000	108.5	80,000	3-4.5	10-15	50-75
Power drop	680-31,750	1500-70,000	1153	850,000	4.5-9	15-30	60-100

Gravity-Drop Hammers

Gravity-drop hammers consist of an anvil or base, supporting columns that contain the ram guides, and a device that returns the ram to its starting position. The energy that deforms the workpiece is derived from the downward drop of the ram; the height of the fall and the weight of the ram determine the force of the blow.

Board-drop hammers (Fig. 1) are widely used, especially for producing forgings weighing no more than a few kilograms. In the board-drop hammer, the ram is lifted by one or more boards keyed to it and passing between two friction rolls at the top of the hammer. The boards are rolled upward and are then mechanically released, permitting the ram to drop from the desired height. Power for lifting the ram is supplied by one or more motors. The hammers have a falling weight, or rated size, of 180 to 4500 kg (400 to 10,000 lb); standard sizes range from 450 to 2250 kg (1000 to 5000 lb) in increments of 225 and 450 kg (500 and 1000 lb). The height of fall of the ram varies with hammer size, ranging from about 900 mm (35 in.) for a 180 kg (400 lb) hammer to about 2 m (75 in.) for a 3400 kg (7500 lb) hammer. The height of fall, and therefore the striking force, of the hammer is approximately constant for a given setting and cannot be altered without stopping the machine and adjusting the length of stroke. Anvils on board-drop hammers are 20 to 25 times as heavy as the rams.

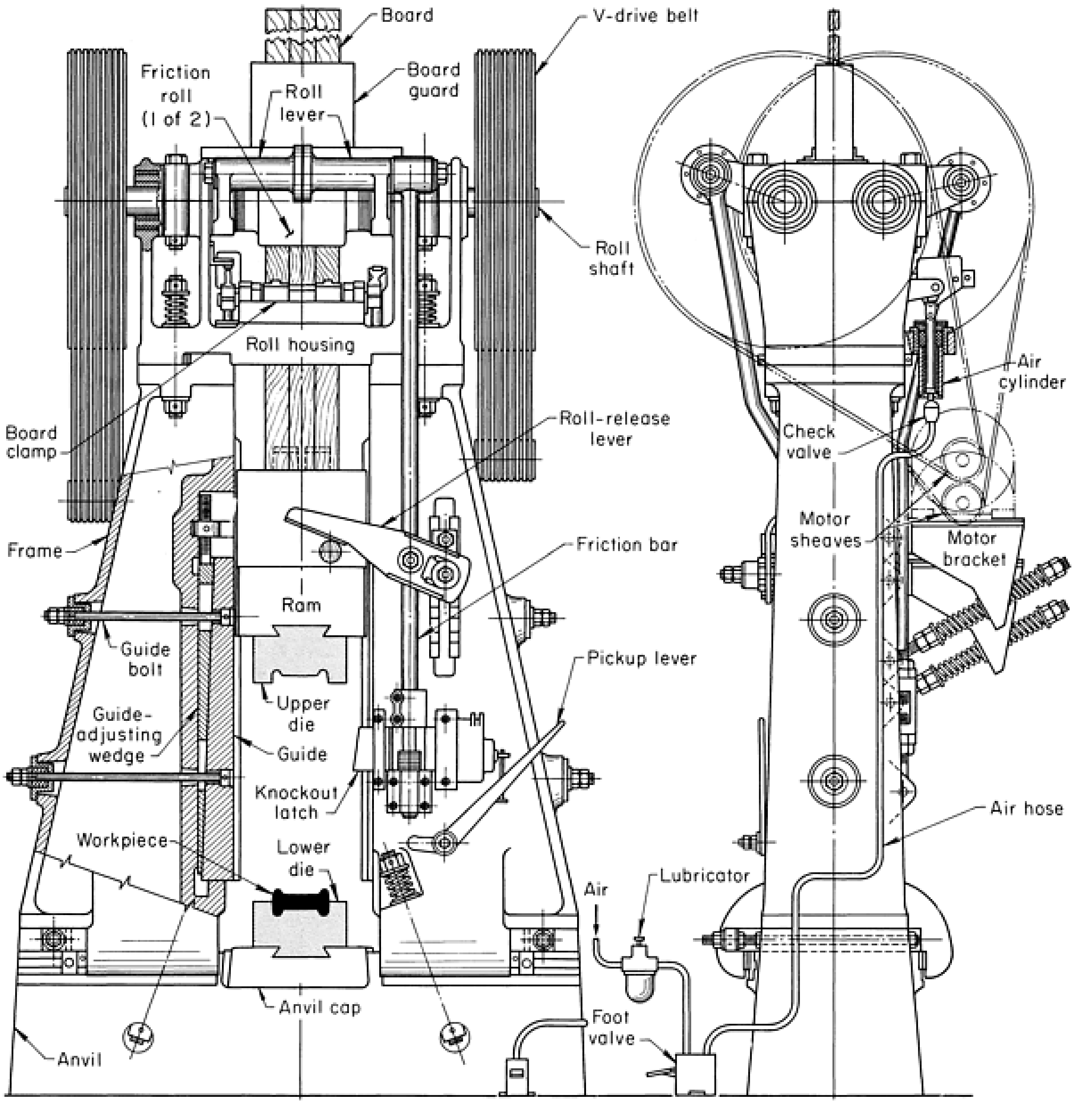

Fig. 1 Principal components of a board-drop hammer

The air-lift gravity-drop hammer is similar to the board-drop hammer in that the forging force is derived from the weight of the falling ram assembly and upper die. It differs in that the ram in the air-lift hammer is raised by air or steam power. Stroke-control dogs, preset on a rocker and actuated by the ram, control power to the ram cylinder. With the hammer shut down, the dogs can be reset on the rocker to adjust stroke length. A device is available that allows both a long stroke and a short stroke in a variable sequence.

The ram is held in the raised position by a piston-rod clamp, which is operated by its own cylinder using a separate compressed-air supply. When the clamp is oblique, the piston rod is clamped. When the operator's treadle is depressed, air enters the cylinder and raises the clamp horizontally, and the ram cycles. Cycling will continue until the treadle is released, causing the rod clamp to drop obliquely and grip the rod. The treadle should not be released on the downstroke of the ram, because this will produce excessive strain in the rod and clamp parts.

The range of sizes generally available in air-lift hammers is 225 to 4500 kg (500 to 10,000 lb). The weight of forging that can be produced in an air-lift hammer of a given size is about the same as that which can be produced in its board-drop hammer counterpart.

Electrohydraulic Gravity-Drop Hammers. In recent years, two significant innovations have been introduced in hammer design. The first is the electrohydraulic gravity-drop hammer. In this type of hammer, the ram is lifted with oil pressure against an air cushion. The compressed air slows the upstroke of the ram and contributes to its acceleration during the downstroke blow. Therefore, the electrohydraulic drop hammer also has a minor power hammer action.

The second innovation in hammer design is the use of electronic blow-energy control. Such control allows the user to program the drop height of the ram for each individual blow. As a result, the operator can set automatically the number of blows desired in forging in each die cavity and the intensity of each individual blow. The electronic blow control increases the efficiency of the hammer operations and decreases the noise and vibration associated with unnecessarily strong hammer blows.

Power-Drop Hammers

In a power-drop hammer, the ram is accelerated during the downstroke by air, steam, or hydraulic pressure. The components of a steam- or air-actuated power-drop hammer are shown in Fig. 2. This equipment is used almost exclusively for closed-die (impression-die) forging.

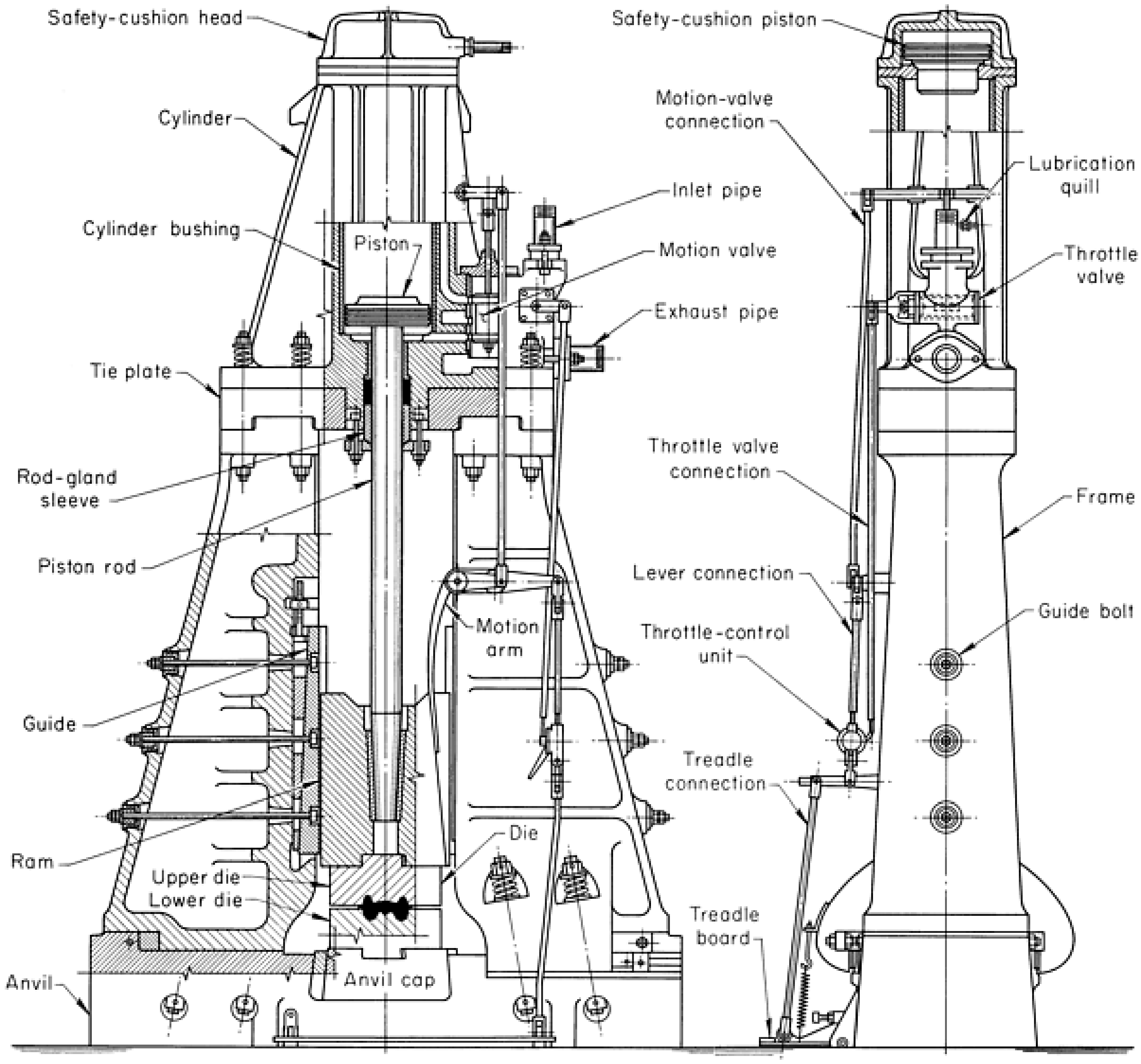

Fig. 2 Principal components of a power-drop hammer with foot control to regulate the force of the blow

The steam- or air-powered drop hammer is the most powerful machine in general use for the production of forgings by impact pressure. In a power-drop hammer, a heavy anvil block supports two frame members that accurately guide a vertically moving ram; the frame also supports a cylinder that, through a piston and piston rod, drives the ram. In its lower face, the ram carries an upper die, which contains one part of the impression that shapes the forging. The lower die, which contains the remainder of the impression, is keyed into an anvil cap that is firmly wedged in place on the anvil. The motion of the piston is controlled by a valve, which admits steam, air, or hydraulic oil to the upper or lower side of the piston. The valve, in turn, is usually controlled electronically. Most modern power-drop hammers are equipped with programmable electronic blow control that permits adjustment of the intensity of each individual blow.

Power-drop hammers are rated by the weight of the striking mass, not including the upper die. Hammer ratings range from 450 to 31,750 kg (1000 to 70,000 lb). The large mass of a power-drop hammer is not apparent, because a great deal of it is beneath the floor. A hammer rated at 22,700 kg (50,000 lb) will have a sectional steel anvil block weighing 453,600 kg (1,000,000 lb) or more. The ram, piston, and piston rod will have an aggregate weight of approximately 20,400 kg (45,000 lb). The striking velocity obtained by the downward pressure on the piston sometimes exceeds 7.6 m/s (25 ft/s).

Rating hammers by the weight of the striking mass is not correct, although it has been the common practice. The more realistic method of rating hammers is by the maximum energy, in joules or foot-pounds, that the ram can impart to the hot metal during a single blow at the maximum energy setting of the hammer controls. The useful energy supplied to the forged metal by the hammer ram depends on the hammer design (weight of the ram and the pressure on the top of the piston), the ratio of the anvil weight versus the ram weight, and the hammer foundation design.

Apart from the size of power-drop hammers and the force they make available for the production of large forgings (forgings commonly produced in power-drop hammers range in weight from 23 kg, or 50 lb, to several megagrams), another important advantage is that the striking intensity is entirely under the control of the operator or is preset by the electronic blow-control system. Consequently, effective use can be made of auxiliary impressions in the dies to preform the billet to a shape that will best fill the finishing impressions in the dies and result in proper grain flow, soundness, and metal economy, with minimum die wear. When adequate preliminary impressions cannot be incorporated into the same set of die blocks, two or more hammers are used to produce adequate shaping or blocking before the final die is used.

Although there are many advantages associated with the use of power-drop hammers, the greater striking forces they develop give rise to several disadvantages. As much as 15 to 25% (and, in hard finishing blows, up to 80%) of the kinetic energy of the ram is dissipated in the anvil block and foundation, and therefore does not contribute to deformation of the workpiece. This loss of energy is most critical when finishing blows are struck and the actual deformation per stroke is relatively slight. The transmitted energy imposes a high stress on the anvil block and may even break it. The transmitted energy also develops violent, and potentially damaging, shocks in the surrounding floor area. This necessitates the use of shock-absorbing materials, such as timber or iron felt, in anvil-block foundations and adds appreciably to the cost of the foundation.

Die Forger Hammers

Die forger hammers are similar in operation to power-drop hammers, but have shorter strokes and more rapid striking rates. The ram is held at the top of the stroke by a constant source of pressurized air, which is admitted to and exhausted from the cylinder to energize the blow. The die forger hammers from one manufacturer are capable of delivering 5.5 to 89.5 kJ (4000 to 66,000 ft · lb) of energy per blow. Blow energy and the forging program (that is, the number of die stations and the number and intensity of blows at each station) are preprogrammed by the operator.

Counterblow Hammers

The counterblow hammer, another variation of the power-drop hammer, is widely used in Europe. These hammers develop striking force by the movement of two rams, simultaneously approaching from opposite directions and meeting at a midway point. Some hammers are pneumatically or hydraulically actuated; others incorporate a mechanical-hydraulic or a mechanical-pneumatic system.

A vertical counterblow hammer with a steam-hydraulic actuating system is illustrated in Fig. 3 (air-hydraulic systems are also available). In this hammer, steam is admitted to the upper cylinder and drives the upper ram downward. At the same time, pistons connected to the upper ram act through a hydraulic linkage in forcing the lower ram upward. Retraction speed is increased by steam (or air) pressure acting upward on the piston. Through proper design relative to weights (including tooling and workpiece) and hydraulics (slower lower-assembly velocities), the kinetic energy of the upper and lower assemblies can be balanced at impact.

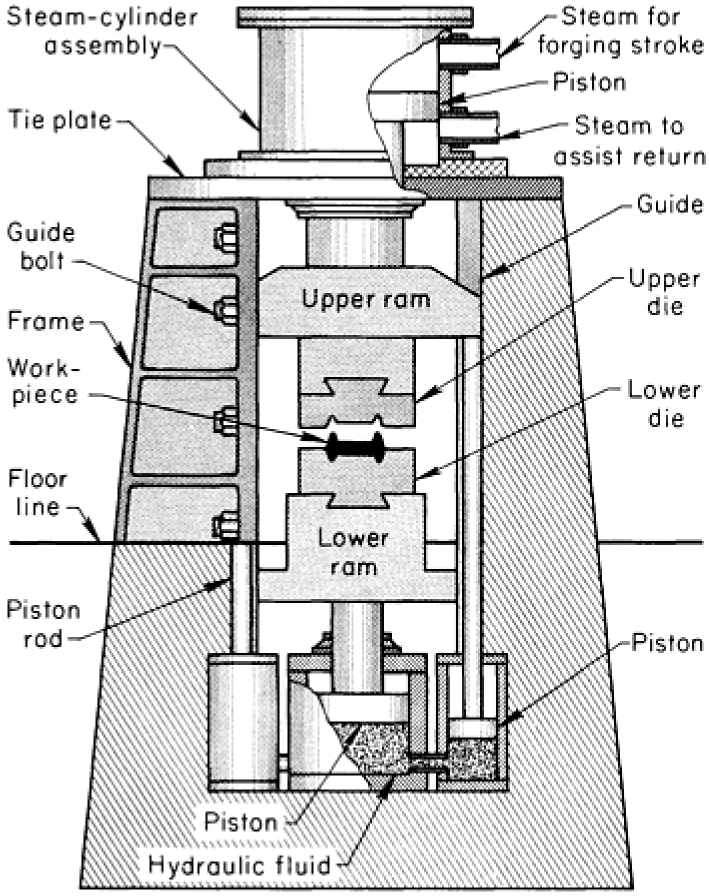

Fig. 3 Principal components of a vertical counter-blow hammer with a steam-hydraulic actuating system

The rams of a counterblow hammer are capable of striking repeated blows; they develop combined velocities of 5 to 6 m/s (6 to 20 ft/s). Compared to single-action hammers, the vibration of impact is reduced, and approximately the full energy of each blow is delivered to the workpiece, without loss to an anvil. As a result, the wear of moving hammer parts is minimized, contributing to longer operating life. At the time of impact, forces are canceled out, and no energy is lost to foundations. In fact, counterblow hammers do not require the large inertia blocks and foundations needed for conventional power-drop hammers.

Horizontal counterblow hammers have two opposing, die-carrying rams that are moved horizontally by compressed air. Heated stock is positioned automatically at each die impression by a preset pattern of accurately timed movements of a stock handling device. A 90° rotation of stock can be programmed between blows.

Open-Die Forging Hammers

Open-die forging hammers are made with either a single frame (often termed C-frame or single-arch hammers) or a double frame (often called double-arch hammers) (Fig. 4). Open-die forging hammers are used to make a large percentage of open-die forgings. The rated sizes of double-frame open-die forging hammers range from about 2720 to 10,900 kg (6000 to 24,000 lb), although larger hammers have been built.

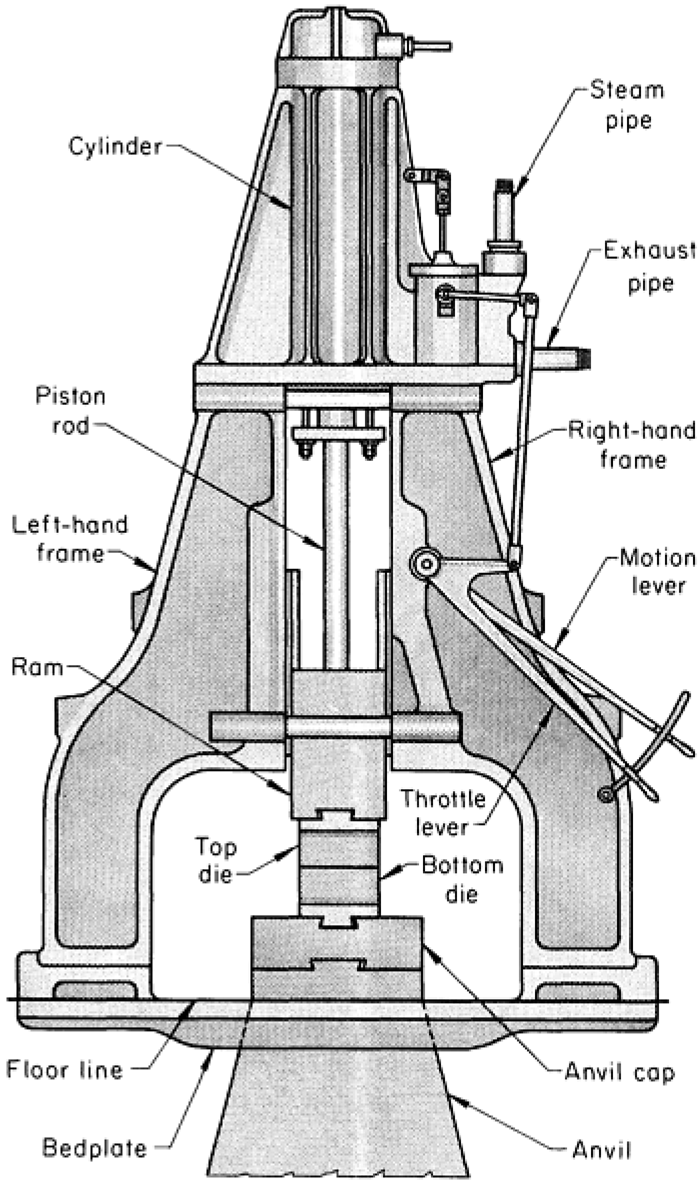

Fig. 4 Double-frame power hammer used for open-die forging

A typical open-die forging hammer is operated by steam or compressed air--usually at pressures of 690 to 825 kPa (100 to 120 psi) for steam and 620 to 690 kPa (90 to 100 psi) for air. These pressures are similar to those used for power-drop hammers.

There are two basic differences between power-drop hammers used for closed-die forging and those used for open-die forging. First, a modern power-drop hammer has blow-energy control to assist the operator in setting the intensity of each blow. In hammers for closed-die forging, the hammer stroke is limited by the upper die surface contacting the surface of the lower die face. In open-die forging, the upper and lower dies do not make contact; stroke-position control is provided through control of the air or steam valve that actuates the hammer piston.

The second difference between closed- and open-die forging hammers is that the anvil of an open-die hammer is separate and independent of the hammer frame that contains the striking ram and the top die. Separation of the anvil from the frame allows the anvil to give way under a heavy blow or series of blows, without disturbing the frame. The anvil may rest on oak timbers, which absorb the hammering shock.

High-Energy-Rate Forging (HERF) Machines

High-energy-rate forging machines are essentially high-speed hammers. They can be grouped into three basic designs: ram and inner frame, two-ram, and controlled energy flow. Each differs from the others in engineering and operating features, but all are essentially very-high-velocity single-blow hammers that require less moving weight than conventional hammers to achieve the same impact energy per blow. All of the designs employ counterblow principles to minimize foundation requirements and energy losses, and they all use inert high-pressure gas controlled by a quick-release mechanism for rapid acceleration of the ram. In none of the designs is the machine frame required to resist the forging forces.

Ram and inner frame machines are produced in several sizes, ranging in capacity from 17 to 745 kJ (12,500 to 550,000 ft · lb) of impact energy. The machine illustrated in Fig. 5(a) has a frame consisting of two units: an inner, or working, frame connected to a firing chamber and an outer, or guiding, frame within which the inner frame is free to move vertically. As the trigger-gas seal is opened, high-pressure gas from the firing chamber acts on the top face of the piston and forces the ram and upper die downward. Reaction to the downward acceleration of the ram raises the inner frame and lower die.

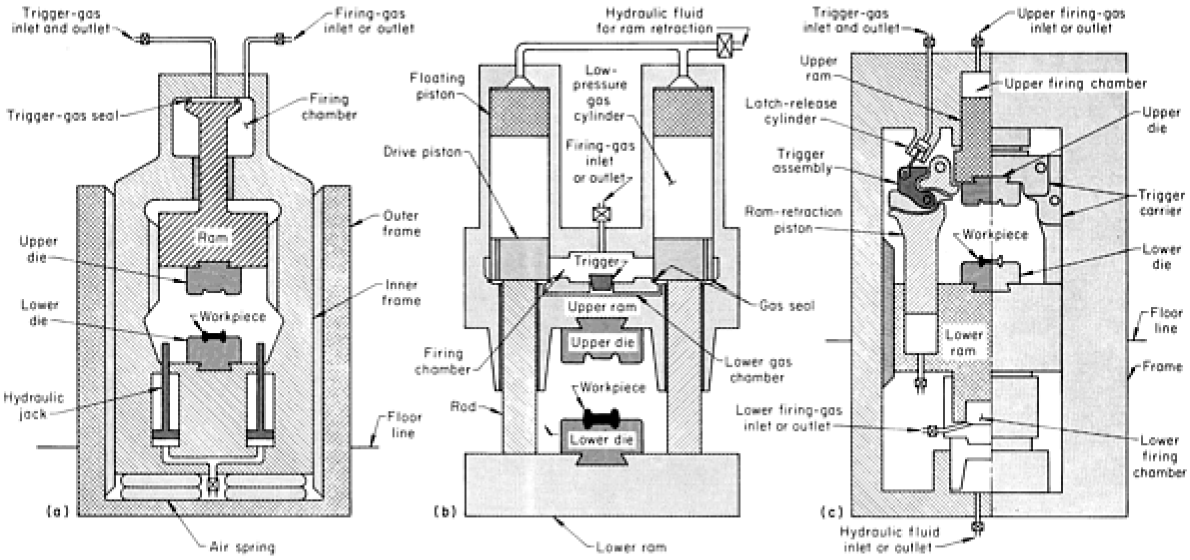

Fig. 5 The three basic machine concepts of high-energy-rate forging. (a) Ram and inner frame machine. (b) Two-ram machine. (c) Controlled-energy-flow machine. Triggering and expansion of the gas in the firing chamber cause the upper and lower rams to move toward each other at high speed. An outer frame provides guiding surfaces for the rams.

The machine is made ready for the next blow by means of hydraulic jacks that elevate the ram until the trigger-gas seal between the upper surface of the firing chamber and the ram piston is reestablished. Venting of the seal gas, as well as gas pressure on the lower lip of the piston, then holds the ram in the elevated position.

Two-ram machines are available in several sizes; the largest has a rating of 407 kJ (300,000 ft · lb) of impact energy. In a two-ram machine (Fig. 5b), the counterblow is achieved by means of an upper ram and a lower ram. An outer frame (not shown in Fig. 5) provides vertical guidance for the two rams. Vertical movement of the trigger permits high-pressure gas to enter the lower chamber and the space beneath the drive piston. This forces and drive piston, rod, lower ram, and lower die upward. The reaction to this force drives the floating piston, cylinder, upper ram, and upper die downward. The rods provide relative guidance between the moving upper and lower assemblies.

After the blow, hydraulic fluid enters the cylinder, returning the upper and lower rams to their starting positions. The gas is recompressed by the floating pistons, and the gas seals at the lower edges of the drive pistons are reestablished. When the trigger is closed, the hydraulic pressure is released, the high-pressure gas in the lower chamber expands through the drive-piston ports and forces the floating pistons up, and the machine is ready for the next blow.

Controlled energy flow forging machines have been made in two sizes, with ratings of 99 and 542 kJ (73,000 and 400,000 ft · lb) of maximum impact energy. These machines (Fig. 5c) are counterblow machines from the standpoint of having separately adjustable gas cylinders and separate rams for the upper and lower dies; however, self-reacting principles are not employed. The lower ram has a hydraulically actuated vertical-adjustment cylinder so that different stroke lengths may be preset.

The trigger, although pneumatically, operated, is a massive mechanical latch that returns and holds the rams through mechanical support of the upper ram and hydraulic connection with the lower ram. With this arrangement, simultaneous release of the two rams is ensured.

Applicability. High-energy-rate forging machines are basically limited to fully symmetrical or concentric forgings such as wheels and gears or coining applications in which little metal movement but high die forces are required. Information on the HERF process, as well as examples of parts forged using high-energy-rate forging, are available in the article "High-Energy-Rate Forging" in this Volume.

Revised by Taylan Altan, The Ohio State University

Mechanical Presses

All mechanical presses employ flywheel energy, which is transferred to the workpiece by a network of gears, cracks, eccentrics, or levers. Driven by an electric motor and controlled by means of an air clutch, mechanical presses have a full-eccentric type of drive shaft that imparts a constant-length stroke to a vertically operating ram (Fig. 6). Various mechanisms are used to translate the rotary motion of the eccentric shaft into linear motion to move the ram (see the section "Drive Mechanisms" in this article). The ram carries the top, or moving, die, while the bottom, or stationary, die is clamped to the die seat of the main frame. The ram stroke is shorter than that of a forging hammer or a hydraulic press. Ram speed is greatest at the center of the stroke, but force is greatest at the bottom of the stroke. The capacities of these forging presses are rated on the maximum force they can apply and range from about 2.7 to 142 MN (300 to 16,000 tonf).

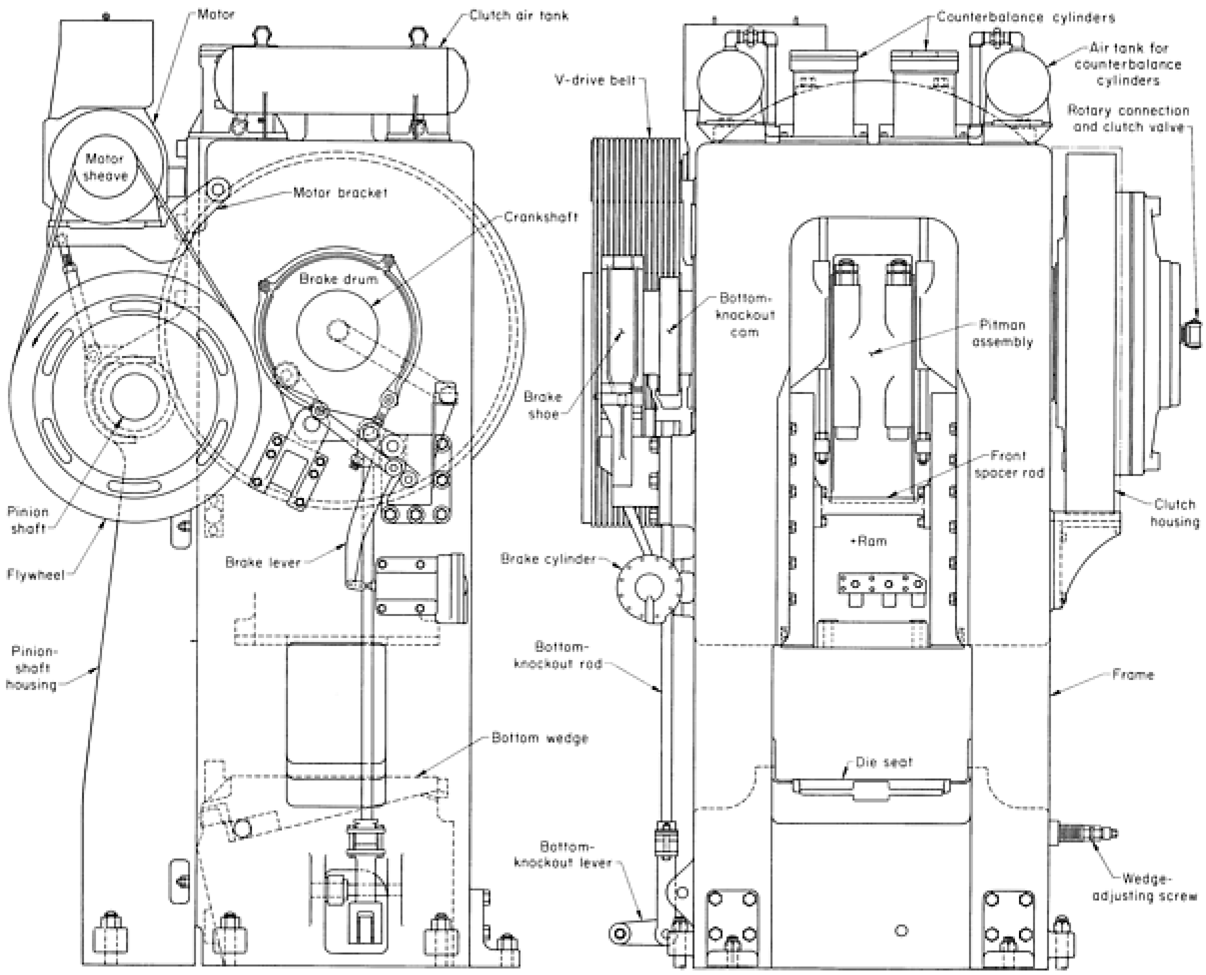

Fig. 6 Principal components of a mechanical forging press

Mechanical forging presses have principal components that are similar to those of eccentric-shaft, straight-side, single-action presses used for forming sheet metal (see the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume). In detail, however, mechanical forging presses are considerably different from mechanical presses that are used for forming sheet. The principal differences are:

- Forging presses, particularly their side frames, are built stronger than presses for forming sheet metal.
- Forging presses deliver their maximum force within 3.2mm ($\frac{1}{8}$ in.) of the end of the stroke, because maximum pressures is required to form the flash
- The slide velocity in a forging press is faster than in a sheet metal deep-drawing press, because in forging it is desirable to strike the metal and retrieve the ram quickly to minimize the time the dies are in contact with the hot metal

Unlike the blow of a forging hammer, a press blow is more of a squeeze than an impact and is delivered by uniform stroke length. The character of the blow in a forging press resembles that of an upsetting machine, thus combining some features of hammers and upsetters. Mechanical forging presses use drive mechanisms similar to those of upsetters, although an upsetter is generally a horizontal machine.

Advantages and Limitations

Compared to hammer forging, mechanical press forging results in accurate close-tolerance parts. Mechanical presses permit automatic feed and transfer mechanisms to feed, pick up, and move the part from one die to the next, and they have higher production rates than forging hammers (stroke rates vary from 30 to 100 strokes per minute). Because the dies used with mechanical presses are subject to squeezing forces instead of impact forces, harder die materials can be used in order to extend die life. Dies can also be less massive in mechanical press forging.

One limitation of mechanical presses is their high initial cost--approximately three times as much as forging hammers that can do the same amount of work. Because the force of the stroke cannot be varied, mechanical presses are also not capable of performing as many preliminary operations as hammers. Generally, mechanical presses forge the preform and final shape in one, two, or three blows; hammers are capable of delivering up to ten or more blows at varying intensities.

Drive Mechanisms

In most mechanical presses, the rotary motion of the eccentric shaft is translated into linear motion in one of three ways: through a pitman arm, through a pitman arm and wedge, or through a Scotch-yoke mechanism.

In a pitman arm press drive (Fig. 7), the torque derived from the rotating flywheel is transmitted from the eccentric shaft to the ram through a pitman arm (connecting rod). Presses using single- or twin-pitman design are available. Twin-pitman design limits the tilting or eccentric action resulting from off-center loading on wide presses. The shut height of the press can be adjusted mechanically or hydraulically through wedges. Mechanical presses with this type of drive are capable of forging parts that are located in an off-center position.

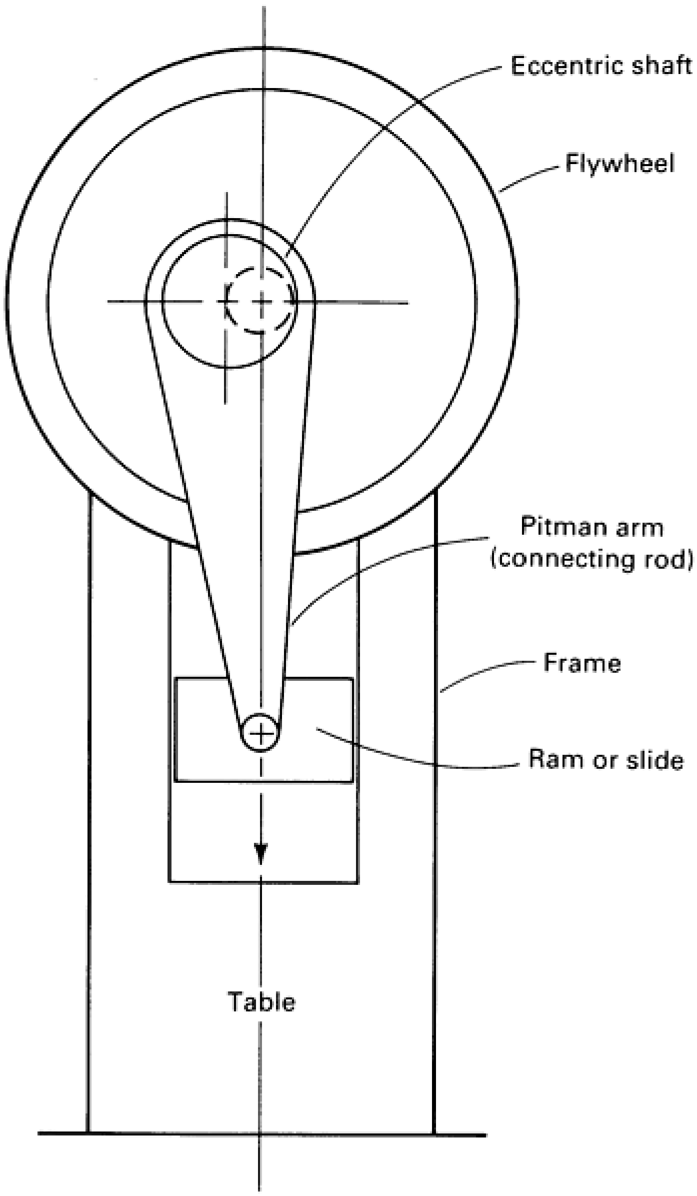

Fig. 7 Principle of operation of a mechanical press driven by a pitman arm (connecting rod)

A wedge drive (Fig. 8) consists of a massive wedge sloped upward at an angle of 30° toward the pitman, an adjustable pitman arm, and an eccentric driveshaft. The torque form the rotating flywheel is transmitted into horizontal motion through the pitman arm and the wedge. As the wedge is forced between the frame and the ram, the ram is pushed downward; this provides the force required to forge the part. The amount of wedge penetration between the ram and frame determines the shut height of the ram. The shut height can be adjusted by rotating the eccentric bushing on the eccentric shaft by means of a worm gear. A ratchet mechanism prevents the adjustment from changing during press operation.

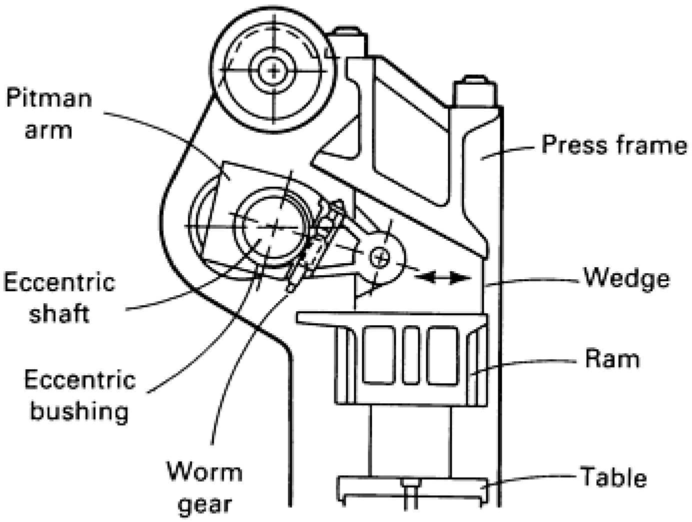

Fig. 8 Principle of operation of a wedge-driven press. See text for details of operation.

Wedge drives transmit the forging force more uniformly over the entire die surface than pitman arm drives. Wedge drives also reduce ram tilting due to off-center loading. Increases in forging accuracies during on-center and off-center loading conditions and the ability to adjust the shut height are the main advantages of wedge-driven mechanical presses. A disadvantage is the relatively long contact time between the die and the forged part.

The Scotch-yoke drive (Fig. 9) contains an eccentric block that wraps around the eccentric shaft and is contained within the ram. As the shaft rotates, the eccentric block moves in both horizontal and vertical directions, while the ram is actuated by the eccentric block only in a vertical direction. The shut height of the ram can be adjusted mechanically or hydropneumatically through wedges.

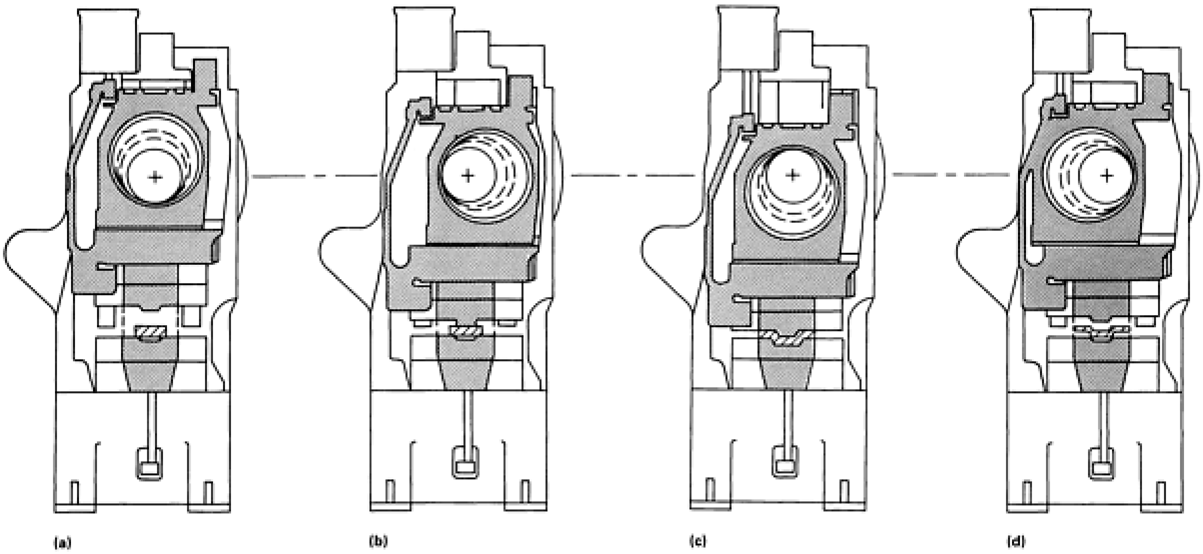

Fig. 9 Principle of operation of mechanical press with a Scotch yoke drive. (a) The ram is at the top of the stroke; the Scotch yoke is centered. (b) Scotch yoke is in the extreme forward position midway through the downward stroke. (c) At bottom dead center, the Scotch yoke is in the center of the ram. (d) Midway through the upward stroke, the Scotch yoke is in the extreme rear position.

This press design provides more rigid guidance for the ram, which results in more accurate forgings. Forging of parts off-center is also possible with this type of drive. Because the drive system is more compact than the pitman arm drive, the press has a shorter overall height.

Capacity

Mechanical presses are considered stroke-restricted machines because the forging capability of the press is determined by the length of the stroke and the available force at the various stroke positions. Because the maximum force attainable by a mechanical press is at the bottom of the work stroke, the forging force of the press is usually determined by measuring the force at a distance of 3.2 or 6.4 mm ($\frac{1}{8}$ or $\frac{1}{4}$ in.) before bottom dead center. Table 2 compares the capacities of mechanical presses with those of hydraulic and screw presses. More information on determining the capacities of mechanical presses and other types of forging equipment is available in the article "Selection of Forging Equipment" in this Volume.

Table 2 Capacities of forging presses

Type of press	Force		Pressing speed	
	MN	tonf	m/s	ft/s
Mechanical	2.2-142.3	250-16,000	0.06-1.5	0.2-5
Hydraulic	2.2-623	250-70,000	0.03-0.8	0.1-2.5

Hydraulic Presses

Hydraulic presses are used for both open- and closed-die forging. The ram of a hydraulic press is driven by hydraulic cylinders and pistons, which are part of a high-pressure hydraulic or hydropneumatic system. After a rapid approach speed, the ram (with upper die attached) moves at a slow speed while exerting a squeezing force on the work metal. Pressing speeds can be accurately controlled to permit control of metal-flow velocities; this is particularly advantageous in producing close-tolerance forgings. The principal components of a hydraulic press are shown in Fig. 10.

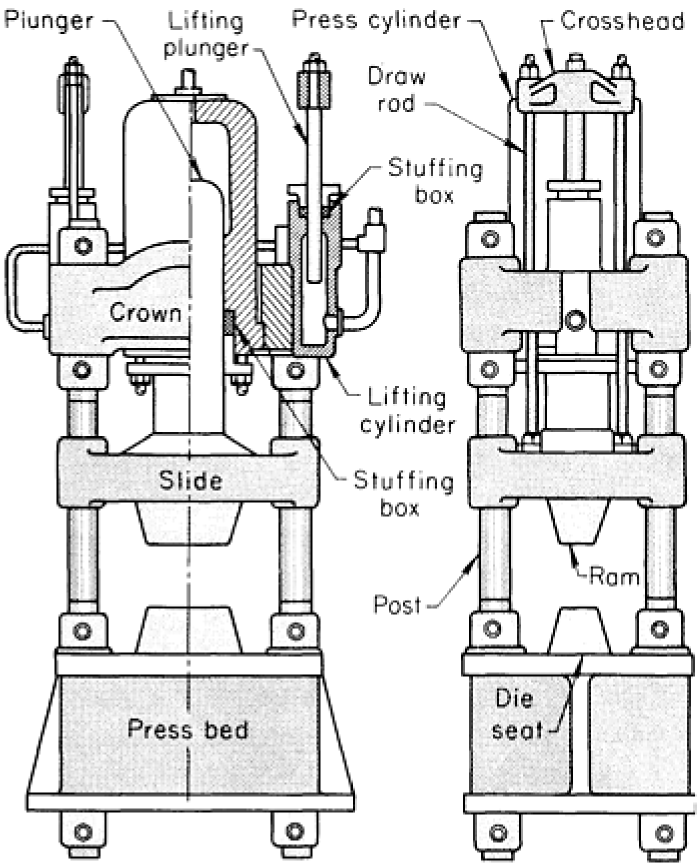

Fig. 10 Principal components of a four-post hydraulic press for closed-die forging

Some presses are equipped with a hydraulic control circuit designed specifically for precision forging (see the article "Precision Forging" in this Volume). With this circuit, it is possible to obtain a rapid advance stroke, followed by preselected first and second pressing speeds. If necessary, the maximum force of the press can be used at the end of the second pressing stroke with no limits on dwell time. The same circuit also provides for a slow pullout speed and can actuate ejectors and strippers at selected intervals during the return stroke.

Advantages and Limitations

The principal advantages of hydraulic presses include:

- Pressure can be changed as desired at any point in the stroke by adjusting the pressure control valve
- Deformation rate can be controlled or varied during the stroke if required. This is especially important

when forging metals that are susceptible to rupture at high deformation rates

- Split dies can be used to make parts with such features as offset flanges, projections, and backdraft, which would be difficult or impossible to incorporate into hammer forgings
- When excessive heat transfer from the hot workpiece to the dies is not a problem or can be eliminated, the gentle squeezing action of a hydraulic press results in lower maintenance costs and increased die life because of less shock as compared to other types of forging equipment
- Maximum press force can be limited to protect tooling

Some of the disadvantages of hydraulic presses are:

- The initial cost of a hydraulic press is higher than that of an equivalent mechanical press
- The action of a hydraulic press is slower than that of a mechanical press
- The slower action of a hydraulic press increases contact time between the dies and the workpiece. When forging materials at high temperatures (such as nickel-base alloys and titanium alloys), this results in shortened die life because of heat transfer from the hot work metal to the dies

Press Drives

The operation of a hydraulic press is simple and based on the motion of a hydraulic piston guided in a cylinder. Two types of drive systems are used on hydraulic presses: direct drive and accumulator drive. These are shown in Fig. 11.

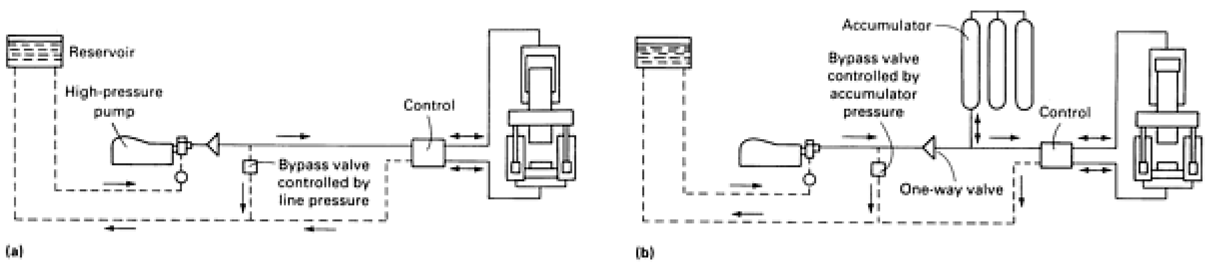

Fig. 11 Schematic of drive systems for hydraulic presses. (a) Direct drive. (b) Accumulator drive. See text for details.

Direct drive presses for closed-die forging usually have hydraulic oil as the working medium. At the start of the downstroke, the return cylinders are vented allowing the ram/slide assembly to fall by gravity. The reservoir used to fill the cylinder as the ram is withdrawn can be pressurized to improve hydraulic flow characteristics, but this is not mandatory. When the ram contacts the workpiece, the pilot operated check valve between the ram cylinder and the reservoir closes, and the pump builds up pressure in the ram cylinder. Modern control systems are capable of very smooth transitions from the advance mode to the forging mode.

In modern direct drive systems used for open die work (see Fig. 11a), a residual pressure is maintained in the return cylinders during the downstroke by means of a pressure control valve. The ram/slide assembly is pumped down against the return system backpressure, and dwell inherent in free fall is eliminated. When the press stroke is completed, that is, when the upper ram reaches a predetermined position or when the pressure reaches a certain value, the oil pressure is released and diverted to lift the ram. With this drive system, the maximum press load is available at any point during the working stroke.

Accumulator-drive presses (Fig. 11b) usually have a water-oil emulsion as a working medium and use nitrogen or air-loaded accumulators to keep the medium under pressure. Accumulator drives are used on presses with 25 MN (2800 tonf) capacity or greater. The sequence of operations is essentially similar to that for the direct-drive press except that the pressure is built up by means of the pressurized water-oil emulsion in the accumulators. Consequently, the ram speed under load is not directly dependent on pump characteristics and can vary, depending on the pressure in the accumulator, the compressibility of the pressure medium, and the resistance of the workpiece to deformation.

Accumulator-drive presses can operate at faster speeds than direct-drive presses. The faster press speed permits rapid working of materials, reduces the contact time between the tool and workpiece, and maximizes the amount of work performed between reheats. Pressure build-up is related to workpiece resistance. Modern pumps can fully load in 100 ms--not much different than the opening time for large valves.

Capacity and Speed

Hydraulic presses are rated by the maximum amount of forging force available. Open-die presses are built with capacities ranging from 1.8 to 125 MN (200 to 14,000 tonf), and closed-die presses range in size from 4.5 to 640 MN (500 to 72,000 tonf). Ram speeds during normal forging conditions vary from 635 to 7620 mm/min (25 to 300 in./min). Press speeds have been slowed to a fraction of an inch per minute to forge materials that are extremely sensitive to deformation rate.

Hammers and Presses for Forging

Revised by Taylan Altan, The Ohio State University

Screw Presses

Screw presses are energy-restricted machines, and they use energy stored in a flywheel to provide the force for forging. The rotating energy of inertia of the flywheel is converted to linear motion by a threaded screw attached to the flywheel on one end and to the ram on the other end.

Screw presses are widely used in Europe for job-shop hardware forging, forging of brass and aluminum parts, precision forging of turbine and compressor blades, hand tools, and gearlike parts. Recently, screw presses have also been introduced in North America for a wide range of applications, notably, for forging steam turbine and jet engine compressor blades and diesel engine crankshafts.

The screw press uses a friction, gear, electric, or hydraulic drive to accelerate the flywheel and the screw assembly, and it converts the angular kinetic energy into the linear energy of the slide or ram. Figure 12 shows two basic designs of screw presses.

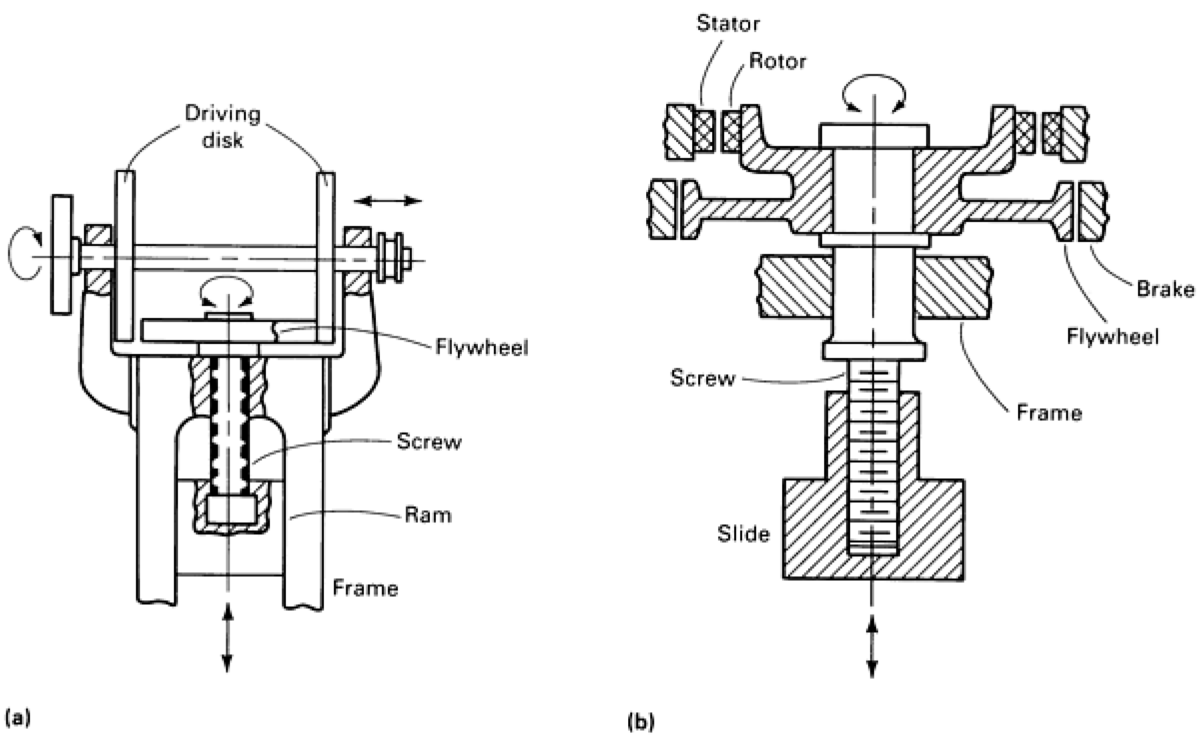

Fig. 12 Two common types of screw press drives. (a) Friction drive. (b) Direct electric drive

Advantages and Limitations

Screw presses are used for open- and closed-die forging. They usually have more energy available per stroke than mechanical presses with similar tonnage ratings, permitting them to accomplish more work per stroke. When the energy has been dissipated, the ram comes to a halt, even though the dies may not have closed. Stopping the ram permits multiple blows to be made to the workpiece in the same die impression. Die height adjustment is not critical, and the press cannot jam. Die stresses and the effects of temperature and height of the workpiece are minimized; this results in good die life. Impact speed is much greater than with mechanical presses. Most screw presses, however, permit full-force operation only near the center of the bed and ram bolsters.

Drive Systems

In the friction drive press (Fig. 12a), two large energy-storing driving disks are mounted on a horizontal shaft and rotated continuously by an electric motor. For a downstroke, one of the driving disks is pressed against the flywheel by a servomotor. The flywheel, which is connected to the screw either positively or by a friction-slip clutch, is accelerated by this driving disk through friction. The flywheel energy and the ram speed continue to increase until the ram hits the workpiece. Thus, the load necessary for forming is built up and transmitted through the slide, the screw, and the bed to the press frame. The flywheel, the screw, and the slide stop when the entire energy in the flywheel is used in deforming the workpiece and elastically deflecting the press. At this moment, the servomotor activates the horizontal shaft and presses the upstroke-driving disk wheel against the flywheel. Thus, the flywheel and the screw are accelerated in the reverse direction, and the slide is lifted to its top position.

In the direct-electric-drive press (Fig. 12b), a reversible electric motor is built directly on the screw and on the frame, above the flywheel. The screw is threaded into the ram or slide and does not move vertically. To reverse the direction of flywheel rotation, the electric motor is reversed after each downstroke and upstroke.

Other Drive Systems. In addition to direct friction and electric drives, several other types of mechanical, electric, and hydraulic drives are commonly used in screw presses. A relatively new screw press drive is shown in Fig. 13. A flywheel (1) supported on the press frame is driven by one or more electric motors and rotates at a constant speed. When the stroke is initiated, a hydraulically-operated clutch (2) engages the rotating flywheel against the stationary screw (3). This feature is similar to that used to initiate the stroke of an eccentric mechanical forging press. Upon engagement of the clutch, the screw is accelerated rapidly and reaches the speed of the fly-wheel. As a result, the ram (4), which acts as a large nut, moves downward. The downstroke charges a hydropneumatic lift cylinder system. The downstroke is terminated by controlling the ram position through the use of a position switch or by controlling the maximum load on the ram by disengaging the clutch and the flywheel from the screw when the preset forming load is reached. The ram is then lifted by the lift-up cylinders (5), releasing the elastic energy stored in the press frame, the screw, and the lift-up cylinders. At the end of the upstroke, the ram is stopped and held in position by a hydraulic brake.

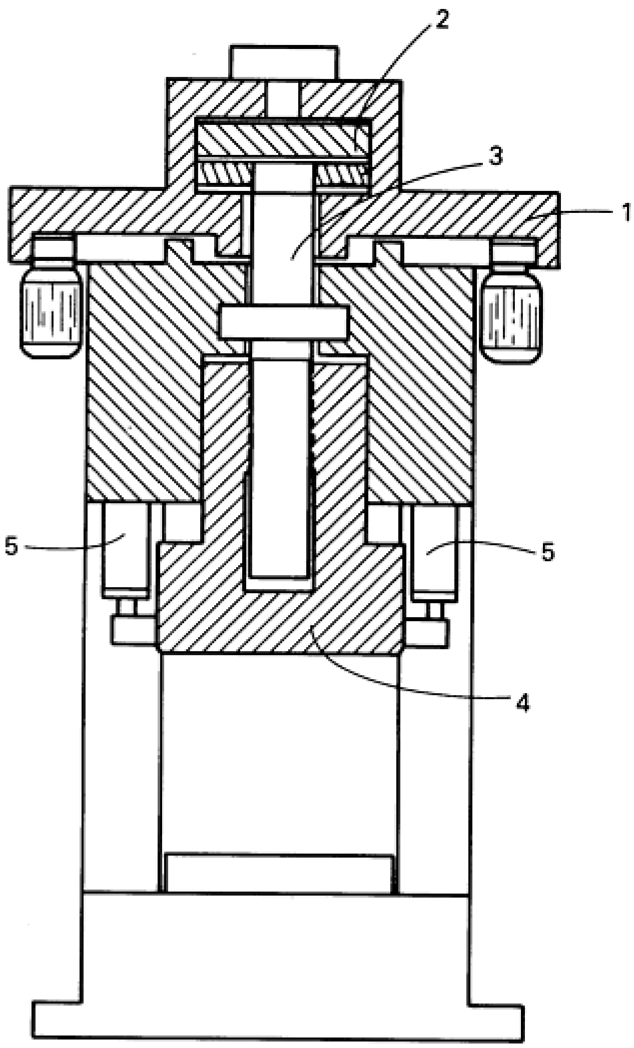

Fig. 13 Screw press drive combining the characteristics of mechanical and screw presses. 1, flywheel; 2, air-operated clutch; 3, screw; 4, ram; 5, lift-up cylinders

This press provides several distinct advantages:

- A high and nearly constant ram speed throughout the stroke
- Full press load at any position of the stroke
- High deformation energy
- Overload protection
- Short contact time between the workpiece and the tools

Limitations of this type of drive system include:

- Only two levels of energy are available, high and low
- Maintenance is increased on the clutch and hydraulic cylinders
- Force is controlled through slippage of the clutch, which can lead to unpredictable application of power
- The large amount of energy available can create material flow problems

Capacities and Speed

Screw presses are generally rated by the diameter of the screw. This diameter, however, is comparable to a listing to nominal forces that can be produced by the press. The nominal force is the force that the press is capable of delivering to deform the workpiece while maintaining maximum energy. The coining, or working, force is approximately double the nominal force when forging occurs near the bottom of the stroke.

Friction screw presses have screw diameters ranging from 100 to 635 mm (4 to 25 in.). These sizes translate to nominal forces of 1.4 to 35.6 MN (160 to 4000 tonf). Direct-electric-drive screw presses have been built with 600 mm (24 in.) diam screws, or 37.3 MN (4190 tonf) of nominal force capacity.

Hydraulically driven screw presses with hard-on-hand blow capacities up to 310 MN (35,000 tonf) have been built.

Press speed, in terms of the number of strokes per minute, depends largely on the energy required by the specific forming process and on the capacity of the drive mechanism to accelerate the screw and the flywheel. In general, however, the production rate of a screw press is lower than that of a mechanical press, especially in automated high-volume operations. Small screw presses operate at speeds of up to 40 to 50 strokes per minute, while larger presses operate at about 12 to 16 strokes per minute.

Revised by Taylan Altan, The Ohio State University

Multiple-Ram Presses

Hollow, flashless forgings that are suitable for use in the manufacture of valve bodies, hydraulic cylinders, seamless tubes, and a variety of pressure vessels can be produced in a hydraulic press with multiple rams. The rams converge on the workpiece in vertical and horizontal planes, alternately or in combination, and fill the die by displacement of metal outward from a central cavity developed by one or more of the punches. Figure 14 illustrates the multiple-ram principle, with central displacement of metal proceeding from the vertical and horizontal planes.

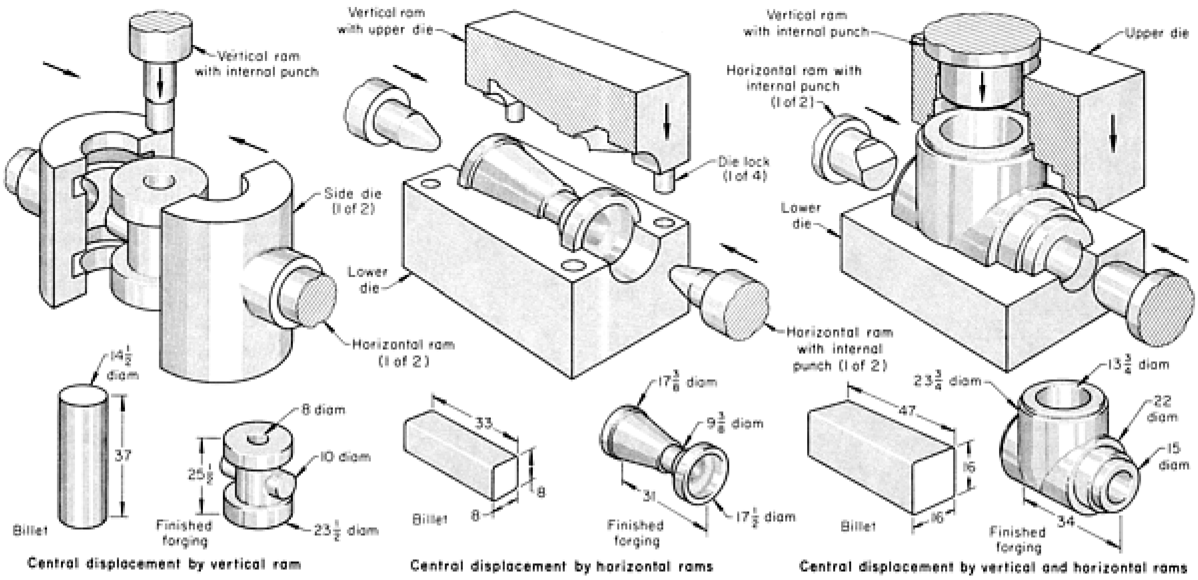

Fig. 14 Examples of multiple-ram forgings. Displacement of metal can take place from vertical, horizontal, and combined vertical and horizontal planes. Dimensions given in inches

Piercing holes in a forging at an angle to the normal direction of forging force can result in considerable material savings, as well as savings in the machining time required to generate such holes.

In addition to having the forging versatility provided by multiple rams, these presses can be used for forward or reverse extrusion. Elimination of flash at the parting line is a major factor in decreasing stress-corrosion cracking in forging alloys susceptible to this type of failure, and the multidirectional hot working that is characteristic of processing in these presses decreases the adverse directional effects on mechanical properties.

Safety

A primary consideration in forging is the safety of the operator. Therefore, each operator must be properly trained before being allowed to operate any forging equipment. Protective equipment must be distributed and used by the operator to protect against injuries to the head, eyes, ears, feet, and body. This equipment is described in ANSI standard B24.1.

The forging machines should be equipped with the necessary controls to prevent accidental operation. This can be achieved through dual pushbutton controls and/or point-of-operation devices. Guards should be installed on all exterior moving parts to prevent accidental insertion of the hands or other extremities. Guards should also be installed to protect against flying scale or falling objects during the forging operation.

All forging equipment must be properly maintained according to manufacturer's recommendations. During machine repair or die changing, the power to the machine should be locked out to prevent accidental operation; the ram should be blocked with blocks, wedges, or tubing capable of supporting the load. The strength and dimensions of the blocking material are given in ANSI B24.1. More information on safety is available in the publications cited in the Selected References at the end of this article.

Hammers and Presses for Forging

Revised by Taylan Altan, The Ohio State University

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Introduction

FORGING EQUIPMENT influences the forging process because it affects deformation rate, forging temperature, and rate of production. The forging engineer must have sound knowledge of the different forging machines in order to:

- Use existing machinery more efficiently
- Define the existing plant capacity accurately
- Communicate better with, and at times request improved performance from, the machine builder.
- Develop, if necessary, in-house proprietary machines and processes not available in the machine tool market
- Utilize them in the most cost-effective manner

This article will detail the significant factors in the selection of forging equipment for a particular process. The article "Hammers and Presses for Forging" in this Volume contains information on the principles of operation and the capacities of various types of forging machines.

Selection of Forging Equipment

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Process Requirements and Forging Machines

Figure 1 illustrates the interaction between the principal machine and process variable for hot forging conducted in presses. As shown at the left in Fig. 1, flow stress σ, interface friction conditions, and part geometry (dimensions and shape) determine the load L_p at each position of the stroke and the energy E_p required by the forming process. The flow stress $\bar{s}$ increases with increasing deformation rate $\dot{e}$ and with decreasing work metal temperature, θ. The magnitudes of these variations depend on the specific work material (see the Sections on forging of specific metals and alloys in this Volume). The frictional conditions deteriorate with increasing die chilling.

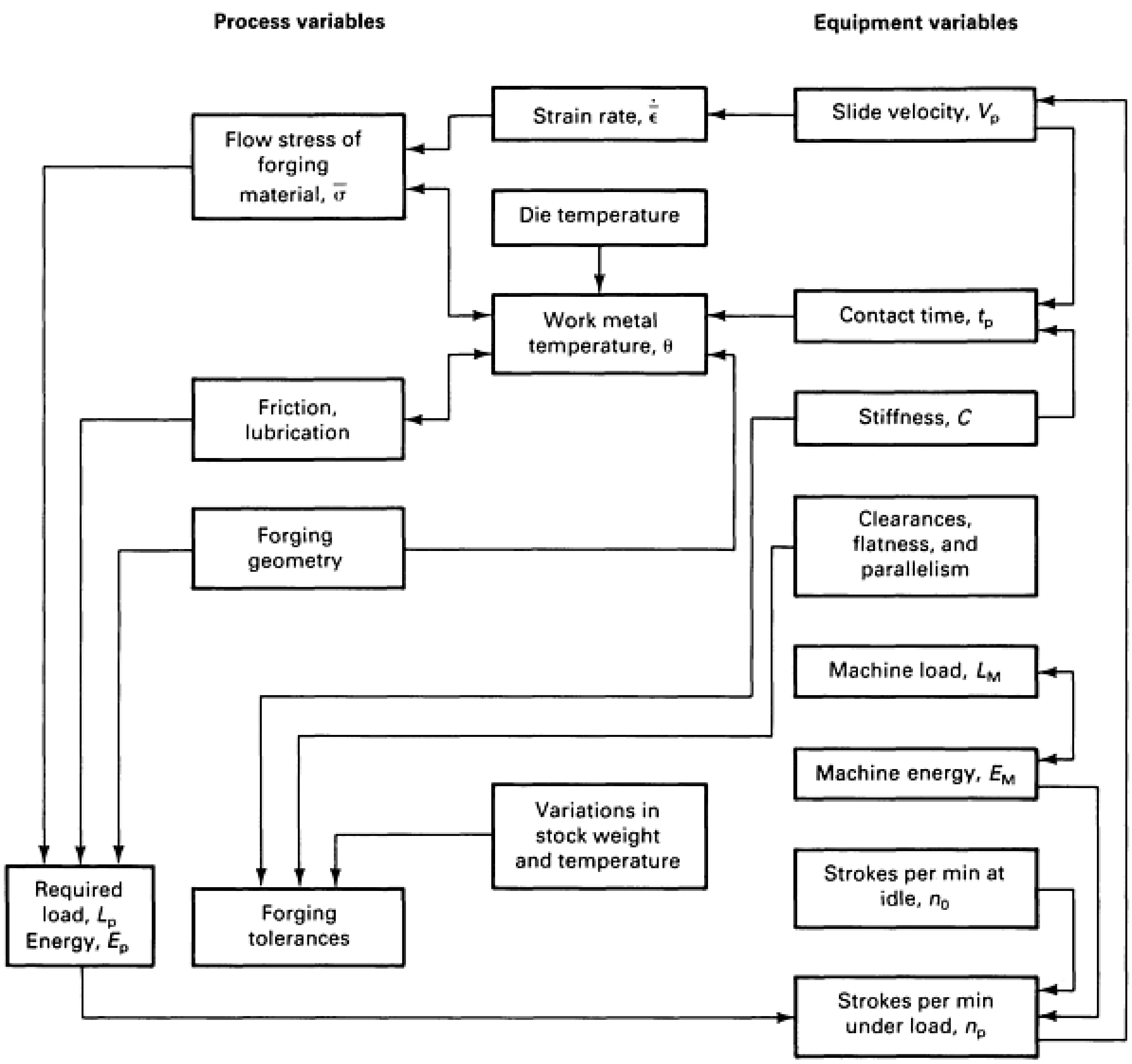

Fig. 1 Relationships between process and machine variables in hot-forging processes conducted in presses

As indicated by the lines connected to the "Work metal temperature" block in Fig. 1, for a given initial stock temperature, the temperature variations in the part are largely influenced by the surface area of contact between the dies and the part, the part thickness or volume, the die temperature, the amount of heat generated by deformation and friction, and the contact time under pressure t_p.

The velocity of the slide under pressure V_p determines mainly t_p and the deformation rate $\dot{\bar{\varepsilon}}$. The number of strokes per minute under no-load conditions n_0, the machine energy E_M, and the deformation energy E_p required by the process influence the slide velocity under load V_p and the number of strokes under load n_p; n_p determines the maximum number of parts formed per minute (the production rate) if the feed and unloading of the machine can be carried out at that speed. The relationships illustrated in Fig. 1 apply directly to hot forging in hydraulic, mechanical, and screw presses.

For a given material, a specific forging operation, such as closed-die forging with flash, forward or backward extrusion, upset forging, or bending, requires a certain variation of the load over the slide displacement (or stroke). This is illustrated qualitatively in Fig. 2, which shows load versus displacement curves characteristic of various forming operations. For a given part geometry, the absolute load values will vary with the flow stress of the material and with frictional conditions. In forming, the equipment must supply the maximum load as well as the energy required by the process.

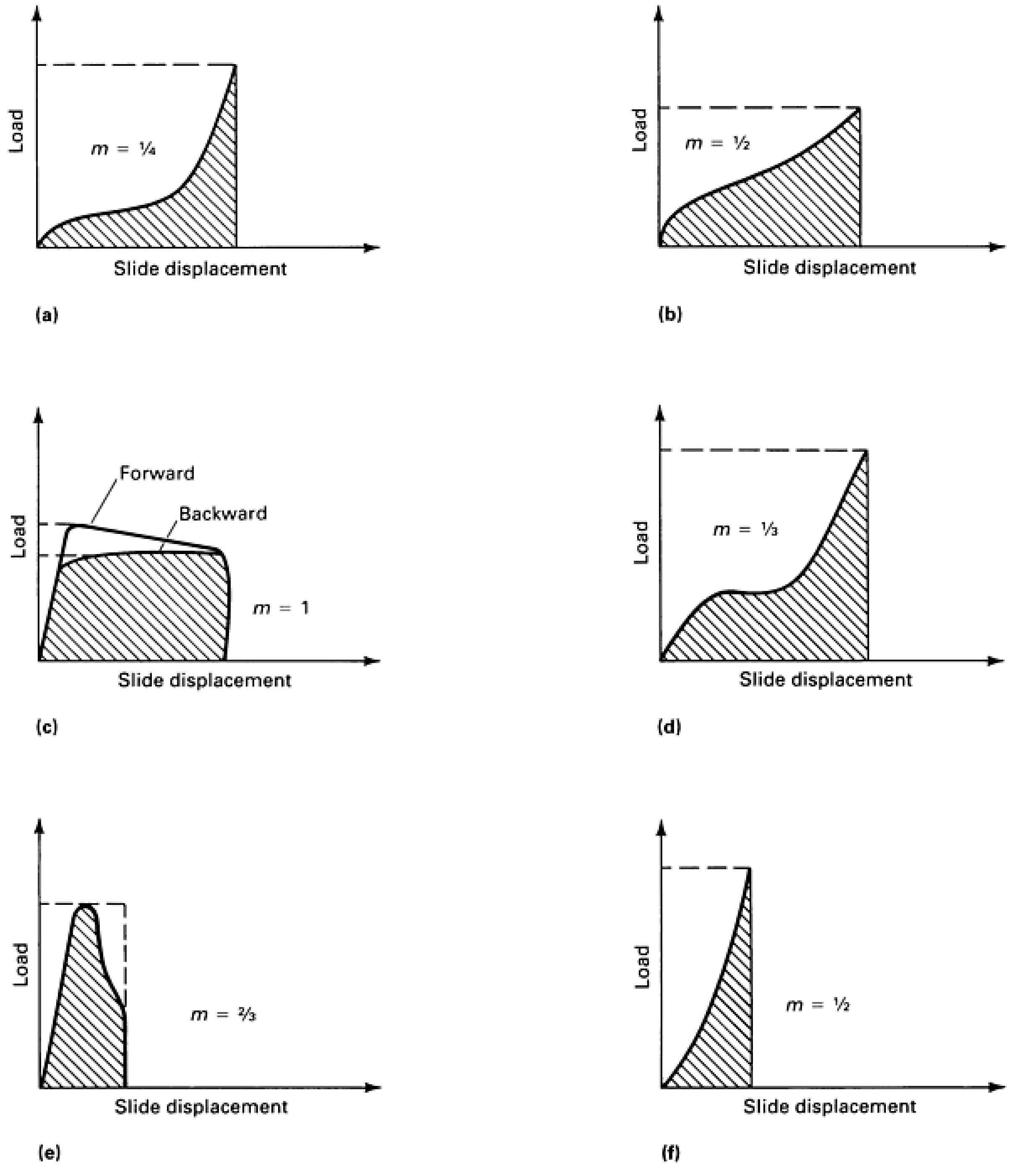

Fig. 2 Load versus displacement curves for various forming operations. Energy developed in the process = load × displacement × *m*, where *m* is a factor characteristic of the specific forming operation. (a) Closed-die forging with flash. (b) Upset forging without flash. (c) Forward and backward extrusion. (d) Bending. (e) Blanking. (f) Coining. Source: Ref 1, 2

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Classification and Characterization of Forging Machines

Forging machines can be classified into three types:

- Force-restricted machines (hydraulic presses)
- Stroke-restricted machines (mechanical presses)
- Energy-restricted machines (hammers and screw presses)

The significant characteristics of these machines constitute all machine design and performance data that are pertinent to the economical use of the machine, including characteristics of load and energy, time-related characteristics, and characteristics of accuracy. More information on these machines is available in the article "Hammers and Presses for Forging" in this Volume.

Hydraulic Presses

The operation of hydraulic presses is relatively simple and is based on the motion of a hydraulic piston guided in a cylinder. Hydraulic presses are essentially force-restricted machines; that is, their capability for carrying out a forming operation is limited mainly by the maximum available force.

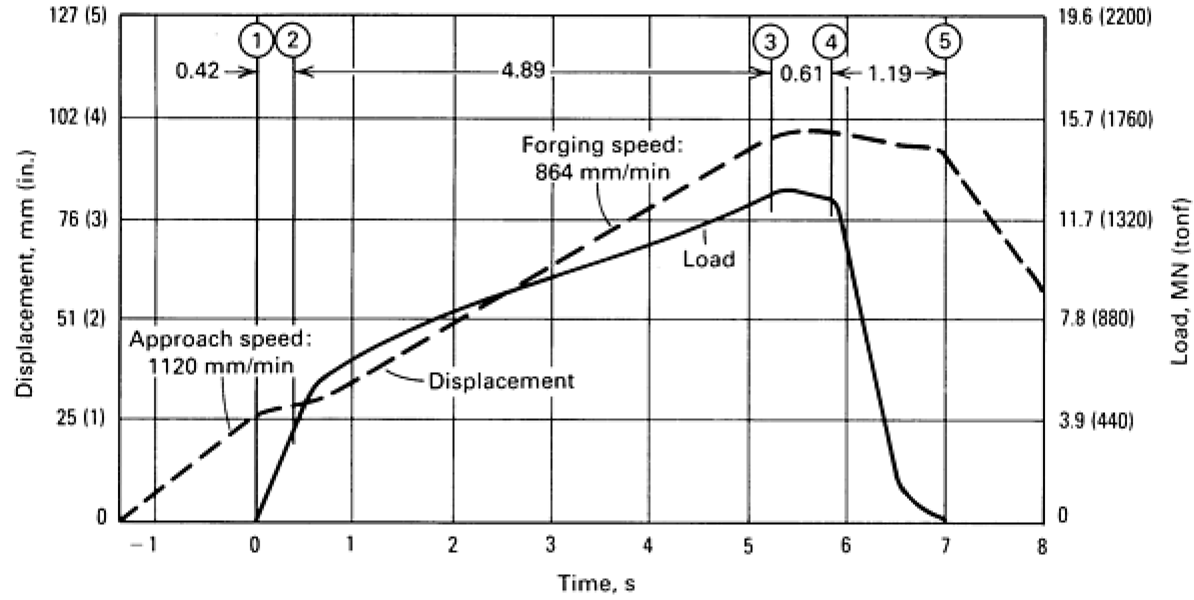
The operational characteristics of a hydraulic press are essentially determined by the type and design of its hydraulic drive system. The two types of hydraulic drive systems--direct drive and accumulator drive (see Fig. 11 in the article "Hammers and Presses for Forging" in this Volume)--provide different time-dependent characteristic data.

In both direct and accumulator drives, a slowdown in penetration rate occurs as the pressure builds and the working medium is compressed. This slowdown is larger in direct oil-driven presses, mainly because oil is more compressible than a water emulsion.

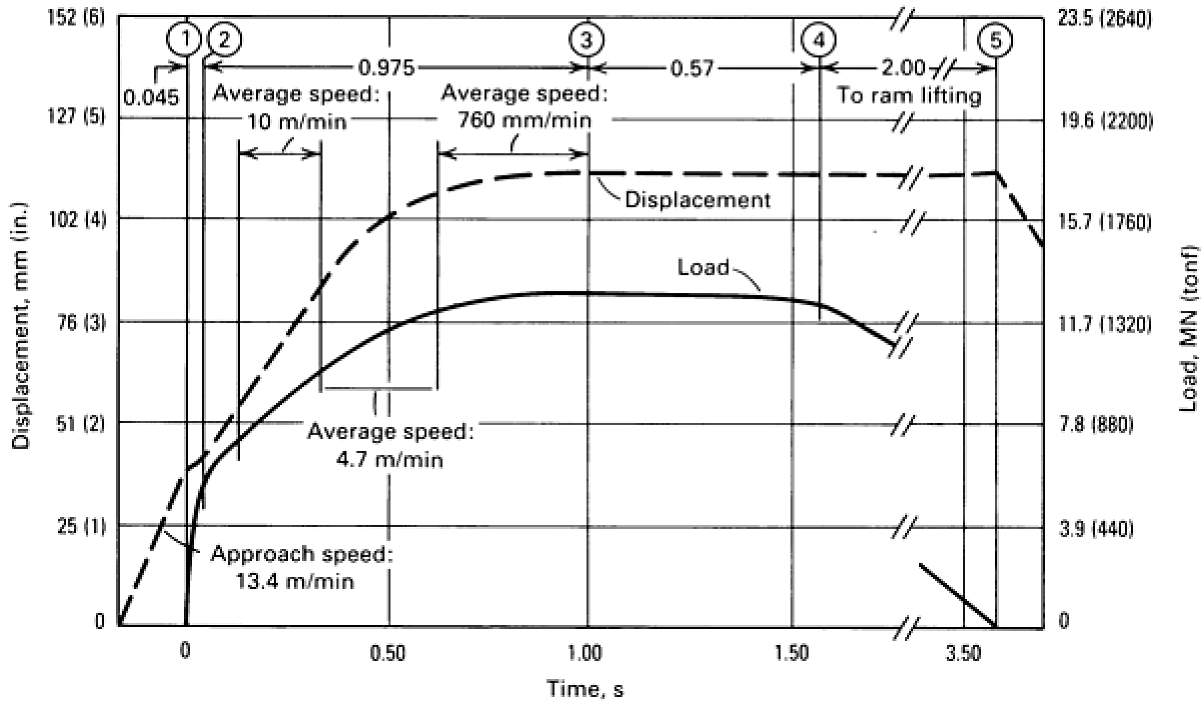
Approach and initial deformation speeds are higher in accumulator-drive presses. This improves hot-forging conditions by reducing die contact times, but wear in the hydraulic elements of the system also increases. Wear is a function of fluid cleanliness; no dirt equals no wear. Sealing problems are somewhat less severe in direct drives, and control and accuracy in manual operation are generally about the same for both types of drives.

From a practical point of view, in a new installation, the choice between direct and accumulator drive is based on the capital cost and the economics of operation. The accumulator drive is usually more economical if one accumulator system can be used by several presses or if very large press capacities (89 to 445 MN, or 10,000 to 50,000 tonf) are considered. In direct-drive hydraulic presses, the maximum press load is established by the pressure capability of the pumping system and is available throughout the entire press stroke. Therefore, hydraulic presses are ideally suited to extrusion-type operations requiring very large amounts of energy. With adequate dimensioning of the pressure system, an accumulator-drive press exhibits only a slight reduction in available press load as the forming operation proceeds.

In comparison with direct drive, the accumulator drive usually offers higher approach and penetration speeds and a shorter dwell time before forging. However, the dwell at the end of processing and prior to unloading is longer in accumulator drives. This is shown in Fig. 3, in which the load and displacement variations are given for a forming process using a 22 MN (2500 tonf) hydraulic press equipped with either direct-(Fig. 3a) or accumulator-drive (Fig. 3b) systems.



(a)



(b)

Fig. 3 Load versus time and displacement versus time curves obtained on 22 MN (2500 tonf) hydraulic presses with (a) direct-drive and (b) accumulator-drive systems. 1, start of deformation; 2, initial dwell; 3, end of deformation; 4, dwell before pressure release; 5, ram lift. Source: Ref 3

Mechanical Presses

The drive system used in most mechanical presses is based on a slider-crank mechanism that translates rotary motion into reciprocating linear motion. The eccentric shaft is connected, through a clutch and brake system, directly to the flywheel (see Fig. 7 in the article "Hammers and Presses for Forging" in this Volume). In designs for larger capacities, the flywheel is located on the pinion shaft, which drives the eccentric shaft.

Kinematics of the Slider-Crank Mechanism. The slider-crank mechanism is illustrated in Fig. 4(a). The following valid relationships can be derived from the geometry illustrated.

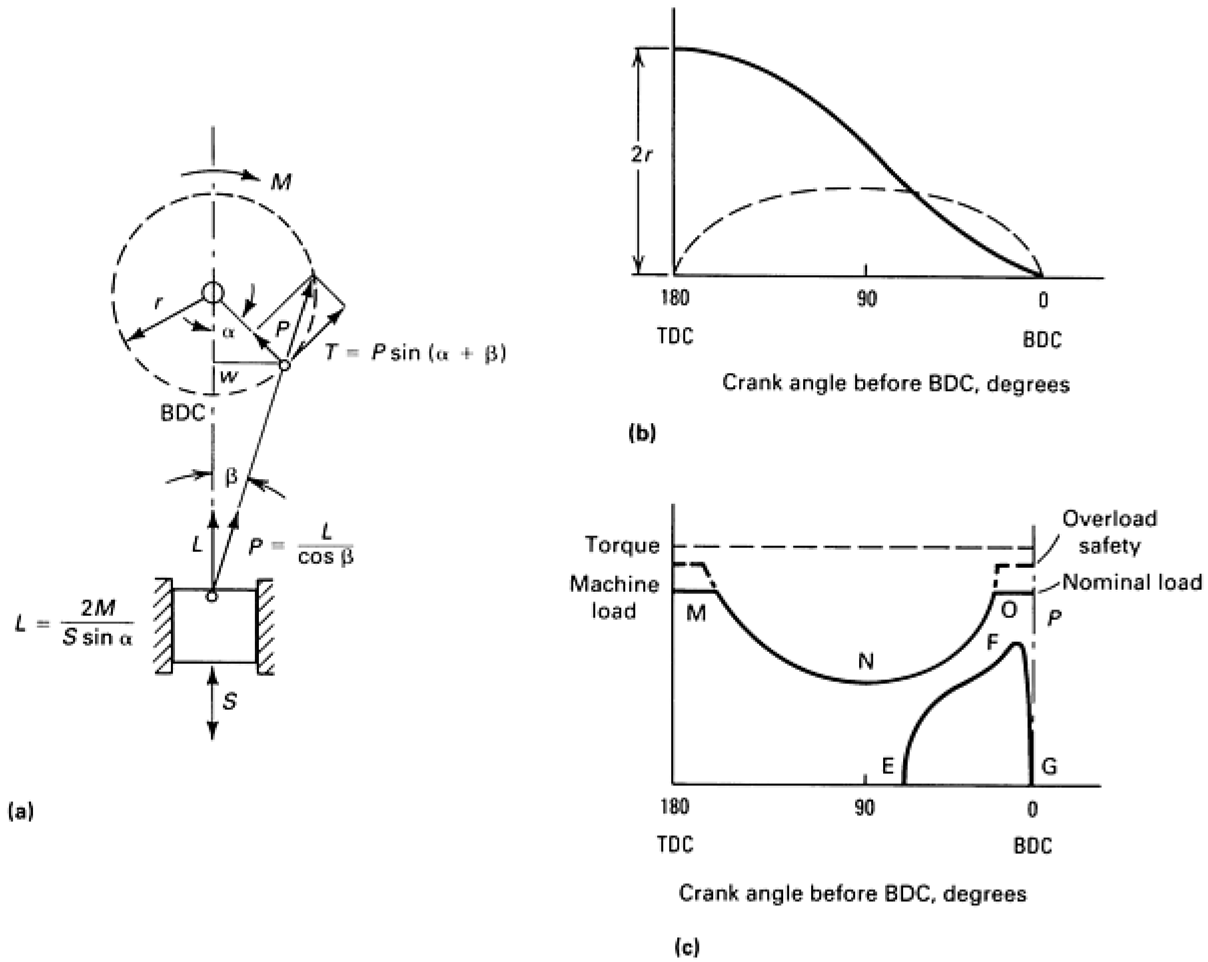


Fig. 4 Load, displacement, velocity, and torque in a simple slider-crank mechanism. (a) Schematic of slider-crank mechanism. (b) Displacement (solid curve) and velocity (dashed curve). (c) Clutch torque *M* and machine load L_M. Source: Ref 3

The distance *w* of the slide from the lowest possible ram position (bottom dead center, BDC; the highest possible position is top dead center, TDC) can be expressed in terms of *r*, *l*, *S*, and α, where (from Fig. 4) *r* is the radius of the crank or one-half of the total stroke *S*, *l* is the length of the pitman arm, and α is the crank angle before bottom dead center.

Because the ratio of *r*/*l* is usually small, a close approximation is:

$$w = \frac{S}{2}(1 - \cos \alpha) \qquad \textbf{(Eq 1)}$$

Equation 1 gives the location of the slide at a crank angle α before bottom dead center. This curve is plotted in Fig. 4(b) along with the slide velocity *V*, which is given by the close approximation:

$$V = \frac{S \pi n}{60} \sin \alpha \qquad \textbf{(Eq 2)}$$

where *n* is the number of strokes per minute.

The slide velocity *V* with respect to slide location *w* before bottom dead center is given by:

$$V = 0.105\, wn \sqrt{\frac{S}{w} - 1} \quad \textbf{(Eq 3)}$$

Therefore, Eq 1 and 2 give the slide position and the slide velocity at an angle α above bottom dead center. Equation 3 gives the slide velocity for a given position *w* above bottom dead center if the number of strokes per minute *n* and the press stroke *S* are known.

Load and Energy Characteristics. An exact relationship exists between the torque *M* of the crankshaft and the available load *L* at the slide (Fig. 4a and c). The torque *M* is constant, and for all practical purposes, angle β is small enough to be ignored (Fig. 4a). A very close approximation then is given by:

$$L = \frac{2M}{S \sin \alpha} \quad \textbf{(Eq 4)}$$

Equation 4 gives the variation of the available slide load *L* with respect to the crank angle α above bottom dead center (Fig. 4c). From Eq 4, it is apparent that as the slide approaches bottom dead center--that is, as angle α approaches zero--the available load *L* may become infinitely large without exceeding the constant clutch torque *M* or without causing the friction clutch to slip.

The following conclusions can be drawn from the observations that have been made thus far. Crank and the eccentric presses are displacement-restricted machines. The slide velocity *V* and the available slide load *L* vary accordingly with the position of the slide before bottom dead center. Most manufacturers in the United States and the United Kingdom rate their presses by specifying the nominal load at 12.7 mm ($\frac{1}{2}$ in.) before bottom dead center. For different applications, the nominal load can be specified at different positions before bottom dead center, according to the standards established by the American Joint Industry Conference. If the load required by the forming process is smaller than the load available at the press--that is, if curve EFG in Fig. 4(c) remains below curve NOP--then the process can be carried out, provided the flywheel can supply the necessary energy per stroke.

For small angles α above bottom dead center, within the OP portion of curve NOP in Fig. 4(c), the slide load *L* can become larger than the nominal press load if no overload safety (hydraulic or mechanical) is available on the press. In this case, the press stalls, the flywheel stops, and the entire flywheel energy is transformed into deflection energy by straining the press frame, the pitman arm, and the drive mechanism. The press can be freed in most cases only by burning out the tooling.

If the applied load curve EFG exceeds the press load curve NOP (Fig. 4c) before point O is reached, the friction clutch slides and the press slide stops, but the flywheel continues to turn. In this case, the press can be freed by increasing the clutch pressure and by reversing the flywheel rotation if the slide has stopped before bottom dead center.

The energy needed for the forming process during each stroke is supplied by the flywheel, which slows to a permissible percentage, usually 10 to 20% of its idle speed. The total energy stored in a flywheel is:

$$E_{FT} = \frac{I\omega^2}{2} = \frac{I}{2}\left(\frac{\pi N}{30}\right)^2 \quad \textbf{(Eq 5)}$$

where *I* is the moment of inertia of the flywheel, ω is the angular velocity in radians per second, and *N* is the rotation speed of the flywheel.

The total energy, *E*, used during one stroke is:

$$E_s = \frac{I}{2}(\omega_0^2 - \omega_1^2) = \frac{I}{2}\left(\frac{\pi}{30}\right)^2 (N_0^2 - N_1^2) \qquad \textbf{(Eq 6)}$$

where ω_0 is the initial angular velocity, ω_1 is the angular velocity after the work is done, N_0 is the initial flywheel speed, and N_1 is the flywheel speed after the work is done.

The total energy E_s also includes the friction and elastic deflection losses. The electric motor must bring the flywheel from its slowed speed N_1 to its idle speed N_0 before the next stroke for forging starts. The time available between two strokes depends on the mode of operation, namely, continuous or intermittent. In a continuously operating mechanical press, less time is available to bring the flywheel to its idle speed; consequently, a larger horsepower motor is necessary.

Frequently, the allowable slowdown of the flywheel is given as a percentage of the nominal speed. For example, if a 13% slowdown is permissible, then:

$$\frac{N_0 - N_1}{N_0} = \frac{13}{100} \text{ or } N_1 = 0.87\, N_0 \qquad \textbf{(Eq 7)}$$

The percentage energy supplied by the flywheel is obtained by using Eq 5 and 6 to give:

$$\frac{E_s}{E_{FT}} = \frac{N_0^2 - N_1^2}{N_0^2} = 1 - (0.87)^2 = 0.25 \qquad \textbf{(Eq 8)}$$

Equations 7 and 8 illustrate that for a 13% slowdown of the flywheel, 25% of the flywheel energy will be used during one stroke.

Time-Dependent Characteristics. The number of strokes per minute n has been discussed previously as an energy consideration. For a given idle flywheel speed, the contact time under pressure t_p and the velocity under pressure V_p depend primarily on the dimensions of the slide-crank mechanism and on the total stiffness C of the press. The effect of press stiffness on contact time under pressure t_p is shown in Fig. 5. As the load builds, the press deflects elastically. A stiffer press (larger C) requires less time t_{p1} for pressure to build and less time t_{p2} for pressure release (Fig. 5a). Consequently, the total contact time under pressure ($t_p = t_{p1} + t_{p2}$) is less for a stiffer press.

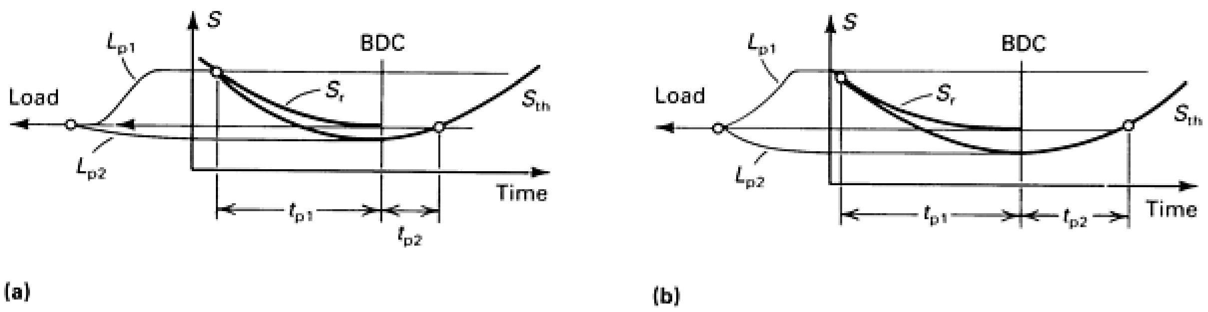


Fig. 5 Effect of press stiffness C on contact time under pressure t_p. (a) Stiffer press (larger C). (b) Less stiff press (smaller C). S_r and S_{th} are the real and theoretical displacement-time curves, respectively; L_{p1}, and L_{p2} are load change during pressure buildup and pressure release, respectively. Source: Ref 4

Characteristics for Accuracy. The working accuracy of a forging press is substantially characterized by two features: the tilting angle of the ram under off-center loading and the total deflection under load (stiffness) of the press. The tilting of the ram produces skewed surfaces and an offset on the forging; the stiffness influences the thickness tolerance.

Under off-center loading conditions, two- or four-point presses perform better than single-point presses, because the tilting of the ram and the reaction forces into gibways are minimized. The wedge-type press, developed in the 1960s, has been claimed to reduce tilting under off-center stiffness. The design principle of the wedge-type press is shown in Fig. 8 in the article "Hammers and Presses for Forging" in this Volume. In this press, the load acting on the ram is supported by the wedge, which is driven by a two-point crank mechanism.

Assuming the total deflection under load for a one-point eccentric press to be 100%, the distribution of the total deflections was obtained after measurement under nominal load on equal-capacity two-point and wedge-type presses (Table 1). It is interesting to note that a large percentage of the total deflection is in the drive mechanism, that is, slide, pitman arm, drive shaft, and bearings.

Table 1 Distribution of total deflection in three types of mechanical presses

Type of press	Distribution of total deflection, %			
	Slide and pitman arm	**Frame**	**Drive shaft and bearings**	Total deflection
One-point eccentric	30	33	37	**100**
Two-point eccentric	21	31	33	**85**
Wedge-type	**21**[(a)]	**29**	**10**	**60**

Source: Ref 5

(a) Includes wedge.

Figure 6 shows table-load diagrams for the same presses discussed above. Table-load diagrams show, in percentage of the nominal load, the amount and location of off-center load that causes the tilting of the ram. The wedge-type press has advantages, particularly in front-to-back off-center loading. In this respect, it performs like a four-point press.

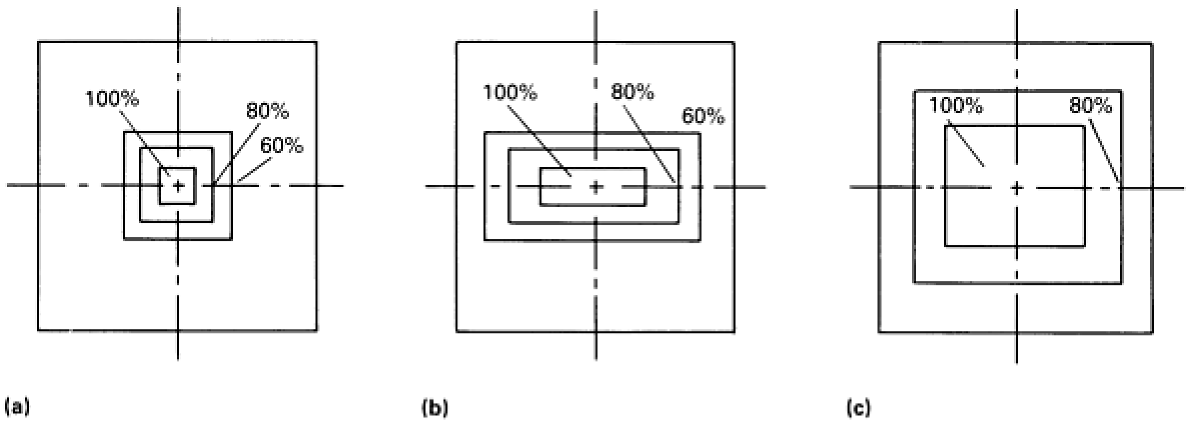


Fig. 6 Amount and location of off-center load that causes tilting of the ram in eccentric one-point presses (a), eccentric two-point presses (b), and wedge-type presses (c). Source: Ref 5

Another type of press designed to minimize deflection under eccentric loading uses a scotch-yoke drive system. The operating principle of this type of press is shown in Fig. 9 in the article "Hammers and Presses for Forging" in this Volume.

Crank Presses With Modified Drives. The velocity versus stroke and load versus stroke characteristics of crank presses can be modified by using different press drives. A well-known variation of the crank press is the knuckle-joint design (Fig. 7), which is capable of generating high forces with a relatively small crank drive. In the knuckle-joint drive, the ram velocity slows much more rapidly toward bottom dead center than the regular crank drive. This machine is successfully used primarily for cold-forming and coining applications.

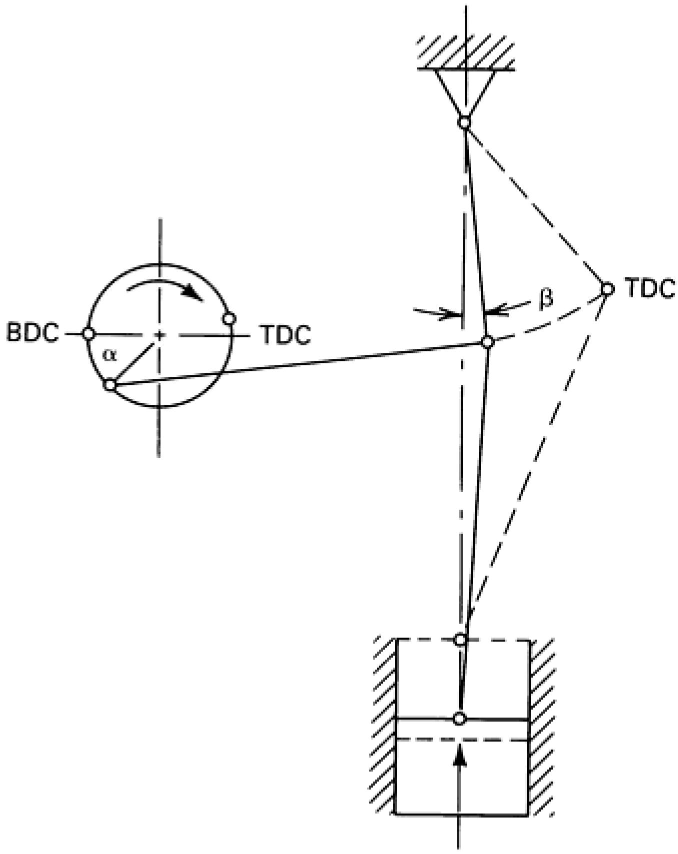


Fig. 7 Schematic of a knuckle-joint mechanical press. Source: Ref 6

Another relatively new mechanical press drive uses a four-bar linkage mechanism (Fig. 8). In this mechanism, the load-stroke and velocity-stroke behavior of the slide can be established at the design stage by adjusting the length of one of the four links or by varying the connection point of the slider link with the drag link. Therefore, with this press, it is possible to maintain maximum load, as specified by press capacity, over a relatively long deformation stroke. Using a conventional slider-crank-type press, this capability can be achieved only by using a much larger capacity press.

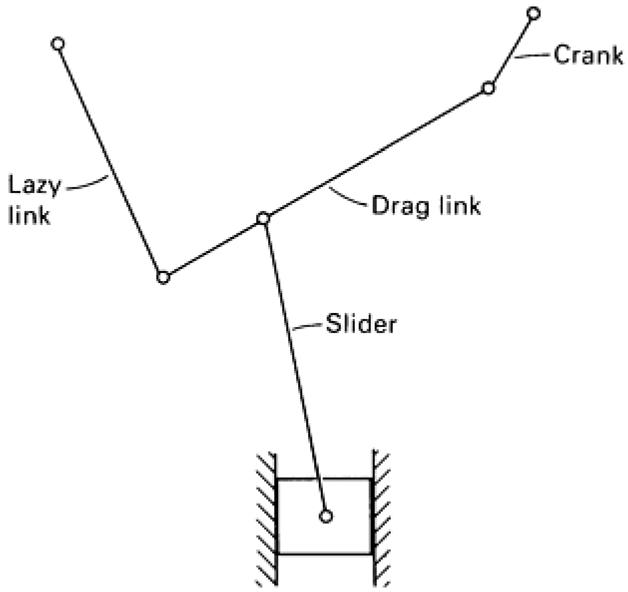


Fig. 8 Four-bar linkage mechanism for mechanical press drives. Source: Ref 7

Figure 9 compares the load-stroke curves for a four-bar linkage press and a conventional slider-crank press. It is apparent that a slider-crank press equipped with a 384 kJ (1700 ton · in.) torque drive can generate a force of about 13.3 MN (1500 tonf) at 0.8 mm ($\frac{1}{32}$ in.) above bottom dead center. The four-bar press equipped with a 135 kJ (600 ton · in.) drive generates a force of about 6.7 MN (750 tonf) at the same location. However, in both machines, a 1.8 MN (200 tonf) force is available at 152 mm (6 in.) above bottom dead center. Therefore, a 6.7 MN (750 tonf) four-bar press could perform the same forming operation, requiring 1.8 MN (200 tonf) over 152 mm (6 in.), as a 13.3 MN (1500 tonf) eccentric press. The four-bar press, which was originally developed for sheet metal forming and cold extrusion, is well suited to extrusion-type forming operations, in which a nearly constant load is required over a long stroke.

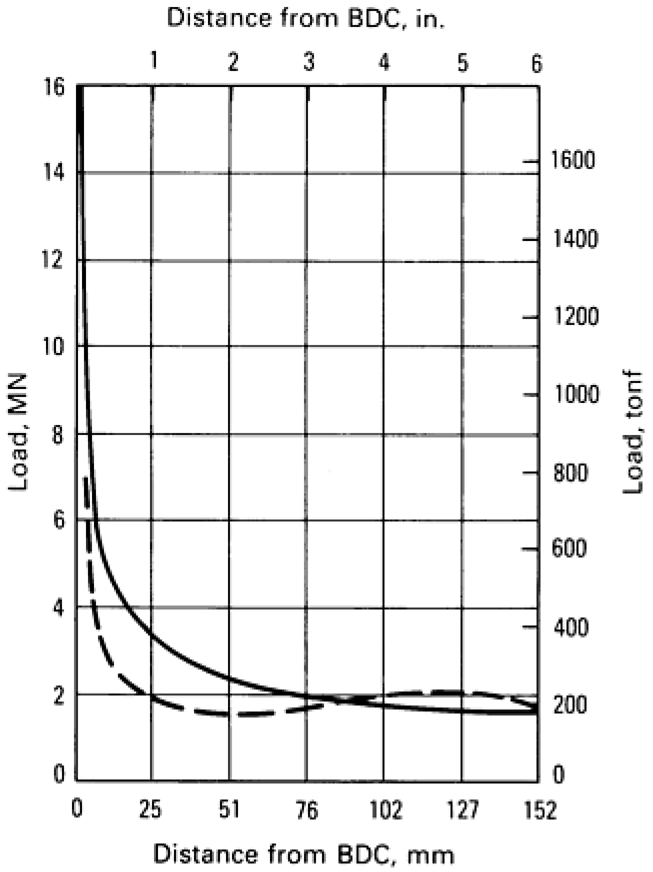


Fig. 9 Load-stroke curves for a 6.7 MN (750 tonf) four-bar linkage press (dashed curve) and a 13.3 MN (1500 tonf) slider-crank press with a 384 kJ (1700 ton · in.) drive (solid curve). Source: Ref 7

Screw Presses

The screw press uses a friction, gear, electric, or hydraulic drive to accelerate the flywheel and the screw assembly, and it converts the angular kinetic energy into the linear energy of the slide or ram. Figure 12 in the article "Hammers and Presses for Forging" in this Volume shows two basic designs of screw presses.

Load and Energy. In screw presses, the forging load is transmitted through the slide, screw, and bed to the press frame. The available load at a given stroke position is supplied by the stored energy in the flywheel. At the end of the downstroke after the forging blow, the flywheel comes to a standstill and begins its reversed rotation. During the standstill, the flywheel no longer contains any energy. Therefore, the total flywheel energy E_{FT} has been transformed into:

- Energy available for deformation E_p to carry out the forging process
- Friction energy E_f to overcome frictional resistance in the screw and in the gibs
- Deflection energy E_d to elastically deflect various parts of the press

At the end of a downstroke, the deflection energy E_d is stored in the machine and can be released only during the upward stroke.

Load versus displacement diagrams for a forging operation are illustrated in Fig. 10. The flywheel in Fig. 10(a) is accelerated to such a velocity that at the end of downstroke the deformation is carried out, and no unnecessary energy is left in the flywheel. This is done by using an energy-metering device that controls flywheel velocity. The flywheel shown in Fig. 10(b) has excess energy at the end of the downstroke. The excess energy from the flywheel stored in the press frame at the end of the stroke is used to begin the acceleration of the slide back to the starting position immediately at the end of the stroke. The screw is not self-locking and is easily moved.

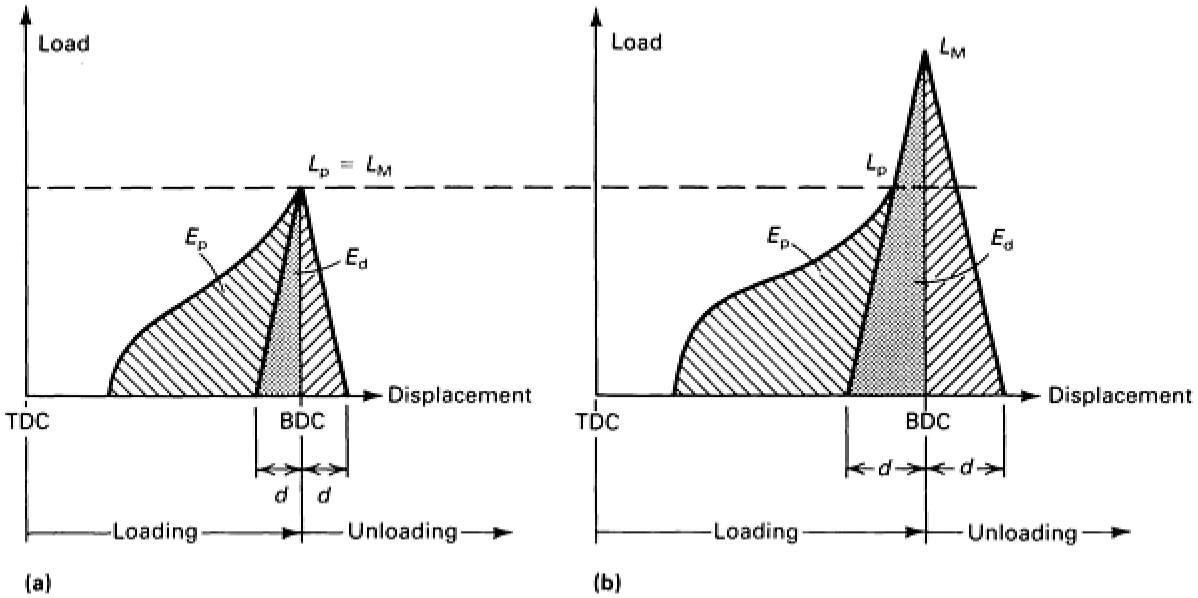


Fig. 10 Load versus displacement curves for die forging using a screw press. (a) Press with energy or load metering. (b) Press without energy or load metering. E_p, energy required for deformation; L_p, load required for deformation; L_M, maximum machine load; E_d, elastic deflection energy; d, elastic deflection of the press. Source: Ref 8

It is apparent from the above discussion that in screw presses the load and energy are inversely proportional. For given friction losses, elastic deflection properties, and available flywheel energy, the load available at the end of the stroke depends mainly on the deformation energy required by the process. Therefore, for a constant flywheel energy, low deformation energy E_p results in high end load L_M, and high E_p results in low L_M. These relationships are shown in Fig. 11.

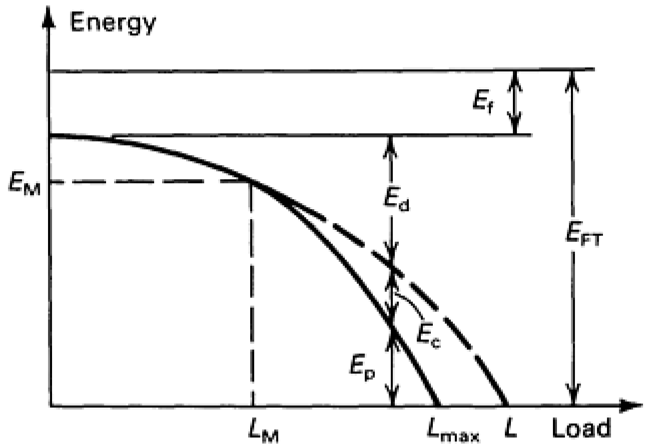


Fig. 11 Energy versus load diagram for a screw press both without a friction clutch at the flywheel (broken line) and with a slipping friction clutch at the flywheel (solid line). E_M, nominal machine energy available for forging; L_M, nominal machine load; E_p, energy required for deformation; E_c, energy lost in slipping clutch; E_d, deflection energy; E_f, friction energy; E_{FT}, total flywheel energy. Source: Ref 9

The screw press can generally sustain maximum loads L_{max} up to 160 to 200% of its nominal load L_M. Therefore, the nominal load of a screw press is set rather arbitrarily. The significant information about the press load is obtained from its energy versus load diagram (Fig. 11). Many screw presses have a friction clutch between the flywheel and the screw. At a preset load, this clutch starts to slip and uses part of the flywheel energy as friction heat energy E_c at the clutch. Consequently, the maximum load at the end of downstroke is reduced to L from L_{max}.

The energy versus load curve has a parabolic shape so that energy decreases with increasing load. This is because the deflection energy E_d, is given by a second-order equation:

$$E_d = \frac{L^2}{2C} \qquad \textbf{(Eq 9)}$$

where L is load and C is the total stiffness of the press.

A screw press can be designed so that it can sustain die-to-die blows without any workpiece for maximum energy of the flywheel. In this case, a friction clutch between the flywheel and the screw is not required. It is important to note that a screw press can be designed and used for forging operations in which large deformation energies are required or for coining operations in which small energies but high loads are required. Another interesting feature of screw presses is that they cannot be loaded beyond the calculated overload limit of the press.

Time-Dependent Characteristics. For a screw press, the number of strokes per minute n is a dependent characteristic. Because modern screw presses are equipped with energy-metering devices, the number of strokes per minute depends on the energy required by the process. In general, however, the production rate of screw presses is comparable with that of mechanical presses.

The velocity under pressure V_p is generally higher than in mechanical presses, but lower than in hammers. This is because the slide velocity of a mechanical press slows toward bottom dead center and the velocity of the slide in a screw press increases until deformation starts and the load builds. This fact is more pronounced in forging thin parts such as airfoils or in coining and sizing operations.

The contact time under pressure t_p is related directly to the ram velocity and to the stiffness of the press. In this respect, the screw press ranks between the hammer and the mechanical press. Contact times for screw presses are 20 to 30 times longer than for hammers. A similar comparison with mechanical presses cannot be made without specifying the thickness of the forged part. In forging turbine blades, which require small displacement but large loads, contact times for screw presses have been estimated to be 10 to 25% of those for mechanical presses.

Variations in Screw Press Drives. In addition to direct friction and electric drives, several other types of mechanical, electric, and hydraulic drives are commonly used in screw presses. A relatively new screw press drive is shown in Fig. 13 in the article "Hammers and Presses for Forging" in this Volume; the principle of operation of this press is also detailed in that article.

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Selection of Forging Equipment

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Hammers

The hammer is the least expensive and most versatile type of equipment for generating load and energy to carry out a forming process. Hammers are primarily used for the hot forging, coining, and, to a limited extent, sheet metal forming of parts manufactured in small quantities--for example, in the aircraft industry. The hammer is an energy-restricted machine. During a working stroke, the deformation proceeds until the total kinetic energy is dissipated by plastic deformation of the material and by elastic deformation of the ram and anvil when the die faces contact each other. Therefore, the capacities of these machines should be rated in terms of energy. The practice of specifying a hammer by its ram weight, although fairly common, is not useful for the user. Ram weight can be regarded only as model or specification number.

There are basically two types of anvil hammers: gravity-drop and power-drop. In a simple gravity-drop hammer, the upper ram is positively connected to a board (board-drop hammer), a belt (belt-drop hammer), a chain (chain-drop hammer), or a piston (oil-, air-, or steam-lift drop hammer) (see the article "Hammers and Presses for Forging" in this Volume). The ram is lifted to a certain height and then dropped on the stock placed on the anvil. During the downstroke, the ram is accelerated by gravity and builds up the blow energy. The upstroke takes place immediately after the blow; the force necessary to ensure quick lift-up of the ram can be three to five times the ram weight.

The operation principle of a power-drop hammer is similar to that of an air-drop hammer. In the downstroke, in addition to gravity, the ram is accelerated by steam, cold air, or hot air pressure.

Electrohydraulic gravity-drop hammers, introduced in the United States in recent years, are more commonly used in Europe. In this hammer, the ram is lifted with oil pressure against an air cushion. The compressed air slows the upstroke of the ram and contributes to its acceleration during the downstroke. Therefore, the electrohydraulic hammer also has a minor power hammer action.

Counterblow hammers are widely used in Europe; their use in the United States is limited to a relatively small number of companies. The principal components of a counterblow hammer are illustrated in Fig. 3 in the article "Hammers and Presses for Forging" in this Volume. In this machine, the upper ram is accelerated downward by steam, but it can also be accelerated by cold or hot air. At the same time, the lower ram is accelerated by a steel band (for smaller capacities) or by a hydraulic coupling system (for larger capacities). The lower ram, including the die assembly, is approximately 10% heavier than the upper ram. Therefore, after the blow, the lower ram accelerates downward and pulls the upper ram back up to its starting position. The combined speed of the rams is about 7.6 m/s (25 ft/s); both rams move with exactly one-half the total closure speed. Due to the counterblow effect, relatively little energy is lost through vibration in the foundation and environment. Therefore, for comparable capacities, a counterblow hammer requires a smaller foundation than an anvil hammer.

Characteristics of Hammers. In a gravity-drop hammer, the total blow energy E_T is equal to the kinetic energy of the ram and is generated solely through free-fall velocity, or:

$$E_T = \frac{1}{2} m_1 V_1^2 = \frac{1}{2} \frac{G_1}{g} V_1^2 = G_1 H \qquad \textbf{(Eq 10)}$$

where m_1 is the mass of the dropping ram, V_1 is the velocity of the ram at the start of deformation, G_1 is the weight of the ram, g is the acceleration of gravity, and H is the height of the ram drop.

In a power-drop hammer, the total blow energy is generated by the free fall of the ram and by the pressure acting on the ram cylinder, or:

$$E_T = \frac{1}{2} m_1 V_1^2 + pAH = (G_1 + pA)H \quad \textbf{(Eq 11)}$$

where, in addition to the symbols given above, p is the air, steam, or oil pressure acting on the ram cylinder in the downstroke and A is the surface area of the ram cylinder.

In counterblow hammers, when both rams have approximately the same weight, the total energy per blow is given by:

$$E_T = 2\left(\frac{m_1 V_1^2}{2}\right) = \frac{m_1 V_t^2}{4} = \frac{G_1 V_t^2}{4g} \quad \textbf{(Eq 12)}$$

where m_1 is the mass of one ram; V_1 is the velocity of one ram; V_t is the actual velocity of the blow of the two rams, which is equal to $2V_1$; and G_1 is the weight of one ram.

During a working stroke, the total nominal energy E_T of a hammer is not entirely transformed into useful energy available for deformation, E_A. A small amount of energy is lost in the form of noise and vibration to the environment. Therefore, the blow efficiency $\eta(\eta = E_A/E_T)$ of hammers varies from 0.8 to 0.9 for soft blows (small load and large displacement) and from 0.2 to 0.5 for hard blows (high load and small displacement).

The transformation of kinetic energy into deformation energy during a working blow can develop considerable force. An example is a deformation blow in which the load P increases from $P/3$ at the start to P at the end of the stroke h. The available energy E_A is the area under the curve shown in Fig. 12. Therefore:

$$E_A = \frac{P/3 + P}{2} h = \frac{4Ph}{6} \quad \textbf{(Eq 13)}$$

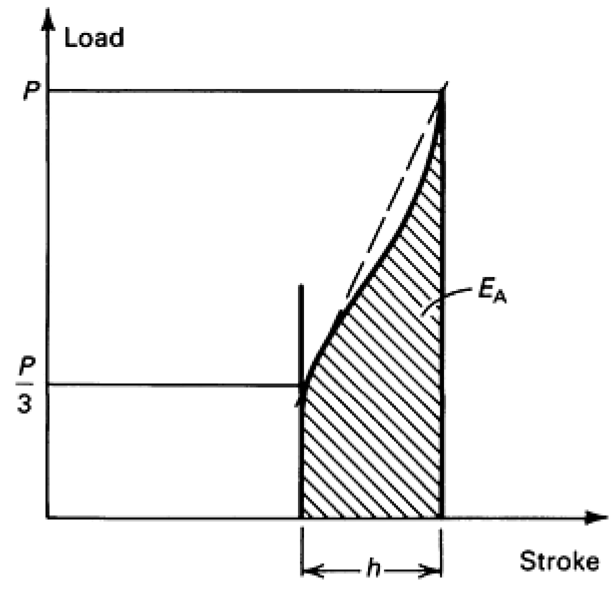

Fig. 12 Example of a load-stroke curve in a hammer blow. Energy available for forging: $E_A = \eta E_T$ (see text for explanation). Source: Ref 10.

For a hammer with a total nominal energy E_T of 47.5 kJ (35,000 ft · lb) and a blow efficiency $\eta$of 0.4, the available energy is $E_A = \eta E_T$ = 19 kJ (14,000 ft · lb). With this value, for a working stroke h of 5 mm (0.2 in.) Eq 13 gives:

$$P = \frac{6E_A}{4h} = 1\ 260\ 000\ \text{lbf} = 630\ \text{tonf} \quad \textbf{(Eq 14)}$$

If the same energy were dissipated over a stroke *h* of 2.5 mm (0.1 in.), the load would reach approximately double the calculated value. The simple hypothetical calculations given above illustrate the capabilities of relatively inexpensive hammers in exerting high forming loads.

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Dies and Die Materials for Hot Forging

Introduction

DIE MATERIALS used for hot forging include hot-work tool steels (AISI H series), some alloy steels such as the AISI 4300 or 4100 series, and a small number of proprietary, lower-alloy materials. The AISI hot-work tool steels can be loosely grouped according to composition (see Table 1). Die materials for hot forging should have good hardenability as well as resistance to wear, plastic deformation, thermal fatigue and heat checking, and mechanical fatigue (see the section "Factors in the Selection of Die Materials" in this article). Die design is also important in ensuring adequate die life; poor design can result in premature wear or breakage.

Table 1 Compositions of tool and die materials for hot forging

Designation	Nominal composition, %								
	C	**Mn**	**Si**	**Co**	**Cr**	**Mo**	**Ni**	**V**	W
Chromium-base AISI hot-work tool steels									
H10	0.40	0.40	1.00	. . .	3.30	2.50	. . .	0.50	**. . .**
H11	0.35	0.30	1.00	. . .	5.00	1.50	. . .	0.40	**. . .**
H12	0.35	0.40	1.00	. . .	5.00	1.50	. . .	0.50	**1.50**
H13	0.38	0.30	1.00	. . .	5.25	1.50	. . .	1.00	**. . .**
H14	0.40	0.35	1.00	. . .	5.00	. . .	. . .	. . .	**5.00**
H19	0.40	0.30	0.30	4.25	4.25	0.40	. . .	2.10	**4.10**
Tungsten-base AISI hot-work tool steels									
H21	0.30	0.30	0.30	. . .	3.50	. . .	. . .	0.45	**9.25**
H22	0.35	0.30	0.30	. . .	2.00	. . .	. . .	0.40	**11.00**
H23	0.30	0.30	0.30	. . .	12.00	. . .	. . .	1.00	**12.00**
H24	0.45	0.30	0.30	. . .	3.0	. . .	. . .	0.50	**15.00**
H25	0.25	0.30	0.30	. . .	4.0	. . .	. . .	0.50	**15.00**
H26	0.50	0.30	0.30	. . .	4.0	. . .	. . .	1.00	**18.00**
Low-alloy proprietary steels									
ASM 6G	0.55	0.80	0.25	. . .	1.00	0.45	. . .	0.10	**. . .**
ASM 6F2	0.55	0.75	0.25	. . .	1.00	0.30	1.00	0.10	**. . .**

This article will address dies and die materials used for hot forging in vertical presses, hammers, and horizontal forging machines (upsetters). Dies used in other forging processes, such as rotary forging and isothermal forging, are discussed in the articles in the Section "Forging Processes" in this Volume.

Open Dies

Most open-die forgings are produced in a pair of flat dies--one attached to the hammer or to the press ram, and the other to the anvil. Swage (semicircular) dies and V-dies are also commonly used. These different types of die sets are shown in Fig. 1. In some applications, forging is done with a combination of a flat die and a swage die.

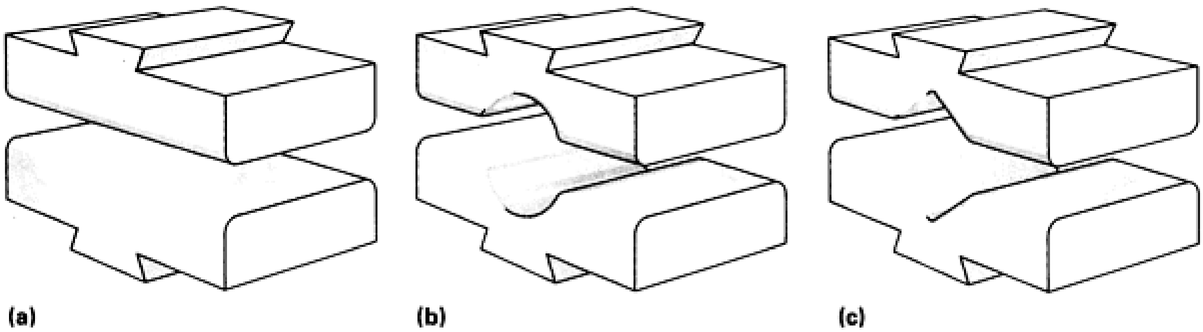

Fig. 1 Three types of die sets used for open-die forging

Flat Dies. The surfaces of flat dies (Fig. 1a) should be parallel to avoid tapering of the workpiece. Flat dies may range from 305 to 510 mm (12 to 20 in.) in width, although most are from 405 to 455 mm (16 to 18 in.) in width. The edges of flat dies are rounded to prevent pinching or tearing of the workpiece and the formation of laps during forging.

Flat dies are used to form bars, flat forgings, and round shapes. Wide dies are used when transverse flow (sideways movement) is desired or when the workpiece is drawn out using repeated blows. Narrower dies are used for cutting off or for necking down larger cross sections.

Swage dies are basically flat dies with a semicircular shape cut into their centers (Fig. 1b). The radius of the semicircle corresponds to the smallest-diameter shaft that can be produced. Swage dies offer the following advantages over flat dies in the forging of round bars:

- Minimal side bulging
- Longitudinal movement of all metal
- Greater deformation in the center of the bar
- Faster operation

Disadvantages of swage dies include the inability to:

- Forge bars of more than one size, in most cases
- Mark or cut off parts (in contrast to flat-die use)

V-dies (Fig. 1c) can be used to produce round parts, but they are usually used to forge hollow cylinders from a hollow billet. A mandrel is used with the V-dies to form the inside of the cylinder. The optimum angle for the V is usually between 90 and 120°.

Impression Dies

Dies for closed-die (impression-die) forging on presses are often designed to forge the part in one blow, and some sort of ejection mechanism (for example, knockout pins) is often incorporated into the die. Dies may contain impressions for several parts.

Hammer forgings are usually made using several blows in successive die impressions. A typical die used for hammer forging is shown in Fig. 2. Such dies usually contain several different types of impressions, each serving a specific function. These are discussed below.

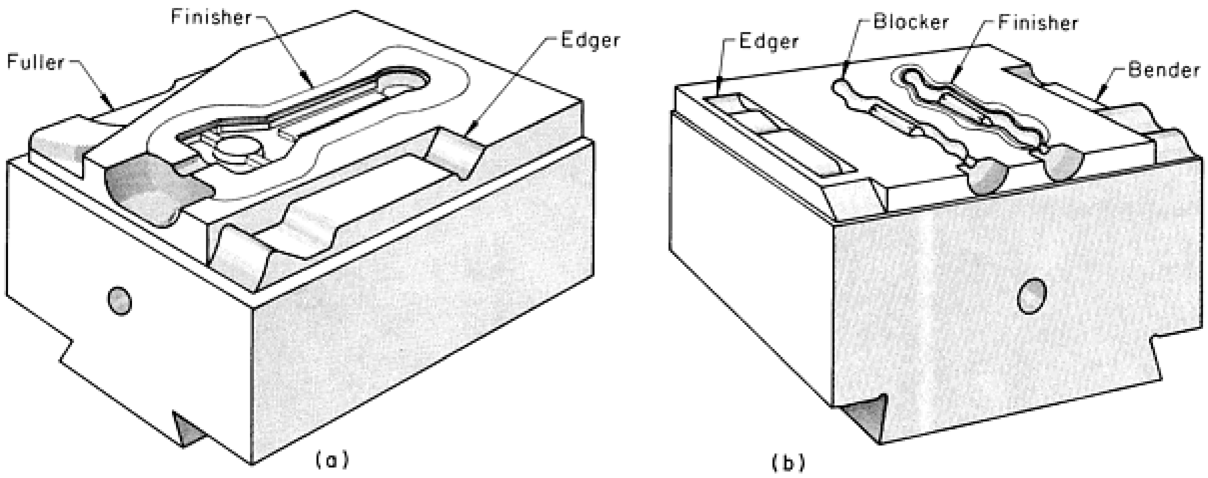

Fig. 2 Typical multiple-impression dies for closed-die forging

Fullers. A fuller is a die impression used to reduce the cross section and to lengthen a portion of the forging stock. In longitudinal cross section, the fuller is usually elliptical or oval to obtain optimum metal flow without producing laps, folds, or cold shuts. Fullers are used in combination with edgers or rollers, or as the only impression before use of the blocker or finisher.

Because fullering usually is the first step in the forging sequence, and generally uses the least amount of forging energy, the fuller is almost always placed on the extreme edge of the die, as shown in Fig. 2(a).

Edgers are used to redistribute and proportion stock for heavy sections that will be further shaped in blocker or finisher impressions. Thus, the action of the edger is opposite to that of the fuller. A connecting rod is an example of a forging in which stock is first reduced in a fuller to prepare the slender central part of the rod and then worked in an edger to proportion the ends of the boss and crank shapes (Fig. 2a).

The edger impression may be open at the side of the die block, as in Fig. 2(a), or confined, as in Fig. 2(b). An edger is sometimes used in combination with a bender in a single die impression to reduce the number of forging blows necessary to produce a forging.

Rollers are used to round the stock (for example, from a square billet to a round, barlike shape) and often to cause some redistribution of mass in preparation for the next impression. The stock usually is rotated, and two or more blows are needed to roll the stock.

The operation of a roller impression is similar to that of an edger, but the metal is partially confined on all sides, with shapes in the top and bottom dies resembling a pair of shallow bowls. Because of the cost of sinking the die impressions, rollering is more expensive than edging, provided both operations can be done in the same number of blows.

Flatteners are used to widen the work metal, so that it more nearly covers the next impression or, with a 90° rotation, to reduce the width to within the dimensions of the next impression. The flattener station can be either a flat area on the face of the die or an impression in the die to give the exact size required.

Benders. A portion of the die can be used to bend the stock, generally along its longitudinal axis, in two or more planes. There are two basic designs of bender impressions: free-flow and trapped-stock.

In bending with a free-flow bender (Fig. 2b), either one end or both ends of the forging are free to move into the bender. A single bend is usually made. This type of bending may cause folds or small wrinkles on the inside of the bend.

The trapped-stock bender usually is employed for making multiple bends. With this technique, the stock is gripped at both ends as the blow is struck, and the stock in between is bent. Because the metal is held at both ends, it is usually stretched during bending. There is a slight reduction in cross-sectional area in the bend, and the work metal is less likely to wrinkle or fold than in a free-flow bender.

Stock that is to be bent may require preforming by fullering, edging, or rollering. Bulges of extra material may be provided at the bends to prevent the formation of kinks or folds in free-flow bending. This is particularly necessary when sharp bends are made. The bent preform usually is rotated 90° as it is placed in the next impression.

Splitters. In making fork-type forgings, frequently part of the work metal is split so that it conforms more closely to the subsequent blocker impression. In a splitting operation, the stock is forced outward from its longitudinal axis by the action of the splitter. Generous radii should be used to prevent the formation of cold shuts, laps, and folds.

Blockers. The blocker impression immediately precedes the finisher impression and serves to prepare the shape of the metal before it is forged to final shape in the finisher. Usually, the blocker imparts the general final shape to the forging, omitting those details that restrict metal flow in finishing, and including those details that will permit smooth metal flow and complete filling in the finisher impression.

Finishers. The finisher impression gives the final overall shape to the workpiece. It is in this impression that any excess work metal is forced out into the flash. Despite its name, the finisher impression is not necessarily the last step in the production of a forging. A bending or hot coining operation is sometimes used to give the final shape or dimensions to a forged part after it has passed through the finisher impression and the trimming die.

A blocker may be a streamlined model of the finisher, used to provide a smooth transition from partially finished to finished forging. Streamlining helps the metal flow around radii, reducing the possibility of cold shuts or other defects.

Sometimes, the blocker impression is made by duplicating the finisher impression in the die block and then rounding it off as required for smooth flow of metal. When this practice is used, the volume of metal in the blockered preform is greater than will be needed in the finisher impression. Also, the blocker impression is larger at the parting line than is the finisher impression. The excess metal causes the finisher impression to wear at the flash land--where the excess metal must be extruded as flash--and around the top of the impression. With wear, the finisher will produce forgings that cannot be properly trimmed or that are out of tolerance. The impression must be reworked more frequently, or the die must be scrapped prematurely.

It is better practice to make the blocker impression slightly narrower and deeper than the finisher impression, with a volume that is equal to, or only slightly greater than, that of the finisher. The use of a blocker impression having this narrower design minimizes die wear at the parting line in the finisher impression. Moreover, it eliminates the occurrence of the type of lap that is likely to be produced in a finished forging made from a blockered preform of the rounded, finisher-duplicate sort described above, namely, the lap made when the finisher shaves excess metal from the sides of the blockered preform. An added benefit of the narrower design is that it allows for some wear of the blocker impression.

Forging of parts that include deep holes or bosses can cause trouble in the finisher. For producing such parts, the blocker sometimes serves as a gathering operation: A volume of metal that is sunk to one side of a forging in the blocker impression can be forced through to the other side in the finisher impression, filling a high boss.

Use of a blocker impression, in addition to promoting smooth metal flow in the finisher impression, reduces wear.

Die Materials

Hot-work die steels are commonly used for hot-forging dies subjected to temperatures ranging from 315 to 650 °C (600 to 1200 °F). These materials contain chromium, tungsten, and in some cases, vanadium or molybdenum or both. These alloying elements induce deep hardening characteristics and resistance to abrasion and softening. These steels usually are hardened by quenching in air or molten salt baths. The chromium-base steels contain about 5% Cr (Table 1). High molybdenum content gives these materials resistance to softening; vanadium increases resistance to abrasion and softening. Tungsten improves toughness and hot hardness; tungsten-containing steels, however, are not resistant to thermal shock and cannot be cooled intermittently with water. The tungsten-base hot-work die steels contain 9 to 18% W, 2 to 12% Cr, and sometimes small amounts of vanadium. The high tungsten content provides resistance to softening at high temperatures while maintaining adequate toughness, but it also makes water cooling of these steels impossible.

Low-alloy proprietary steels are also used frequently as die materials for hot forging. Steels with ASM designations 6G, 6F2, and 6F3 have good toughness and shock resistance, with good resistance to abrasion and heat checking. These steels are tempered at lower temperatures (usually 450 to 500 °C, or 840 to 930 °F); therefore, they are more suited for applications that do not result in high die surface temperatures, for example, die holders for hot forging or hammer die blocks.

The origin of the "ASM" designations for these steels dates back to the 1948 edition of *Metals Handbook.* ASM International does not issue standards of any kind. However, because these steels were never given designations by AISI, SAE, or the Unified Numbering System (UNS), they are still often referred to by their ASM designations. In the 1948 *Handbook*, tool steels were grouped into six broad categories. The steels under discussion here were grouped under category VI (6), "Miscellaneous Tool Steels." The letters of the designation referred to the principal alloying elements. Thus, 6G is a chromium-molybdenum steel, while the 6F steels are nickel-chromium-molybdenum compositions. The difference between 6F2 and 6F3 is in the amounts of these principal alloying elements (see Table 1).

Factors in the Selection of Die Materials

Properties of materials that determine their selection as die materials for hot forging are:

- Ability to harden uniformly
- Wear resistance (ability to resist the abrasive action of hot metal during forging)
- Resistance to plastic deformation (ability to withstand pressure and resist deformation under load)
- Toughness
- Resistance to thermal fatigue and heat checking
- Resistance to mechanical fatigue

Ability to Harden Uniformly. The higher the hardenability of a material, the greater the depth to which it can be hardened. Hardenability depends on the composition of the tool steel. In general, the higher the alloy content of a steel, the higher its hardenability, as measured by the hardenability factor D_1 (in inches). The D_1 of a steel is the diameter of an infinitely long cylinder which would just transform to a specific microstructure (50% martensite) at the center if heat transfer during cooling were ideal, that is, if the surface attained the temperature of the quenching medium instantly. A larger hardenability factor D_1 means that the steel will harden to a greater depth on quenching, not that it will have a higher hardness. For example, the approximate nominal hardenability factors D_1 (inches) for a few die steels are as follows: ASM 6G, 0.6; ASM 6F2, 0.6; ASM 6F3, 1.4; AISI H10, 5; AISI H12, 3.5.

Wear Resistance. Wear is a gradual change in the dimensions or shape of a component caused by corrosion, dissolution, or abrasion and removal or transportation of the wear products. Abrasion resulting from friction is the most important of these mechanisms in terms of die wear. The higher the strength and hardness of the steel near the surface of the die, the greater its resistance to abrasion. Thus, in hot forming, the die steel should have a high hot hardness and should retain this hardness over extended periods of exposure to elevated temperatures.

Figure 4 shows hot hardnesses of five AISI hot-work die steels at various temperatures. All of these steels were heat treated to about the same initial hardness. Hardness measurements were made after holding the specimens at testing temperature for 30 min. Except for H12, all the die steels considered have about the same hot hardness at temperatures below about 315 °C (600 °F). The differences in hot hardness show up only at temperatures above 480 °C (900 °F).

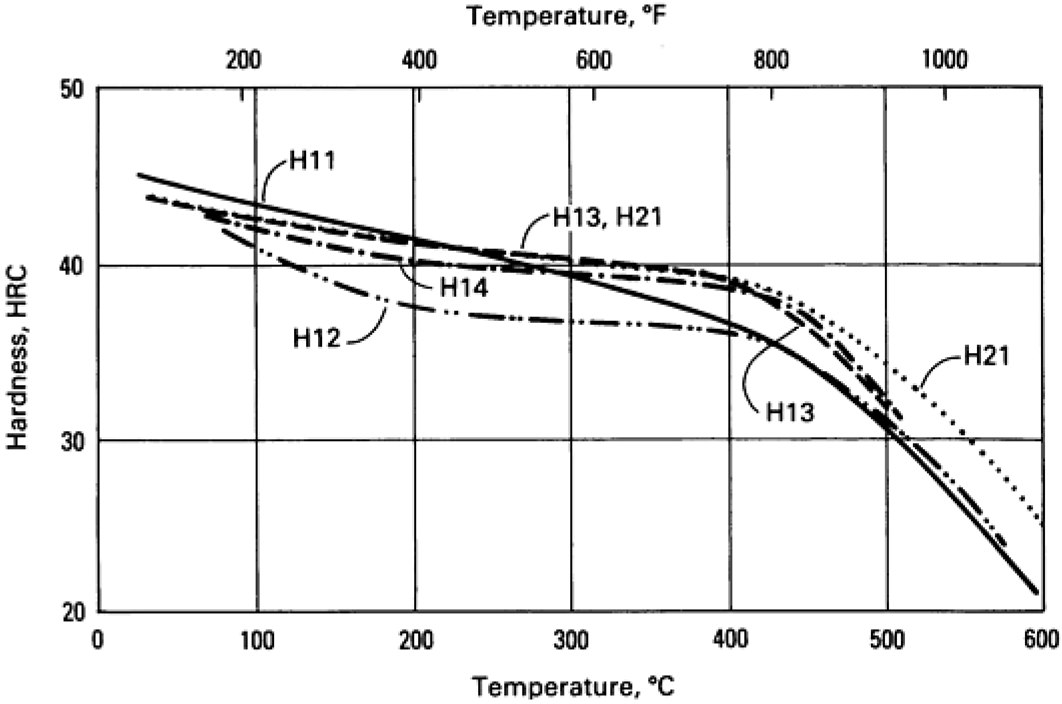

Fig. 4 Hot hardnesses of AISI hot-work tool steels. Measurements were made after holding at the test temperature for 30 min. Source: Ref 1

Figure 5 shows the resistance of some hot-work die steels to softening at elevated temperatures after 10 h of exposure. All of these steels have about the same initial hardness after heat treatment. For the die steels shown, there is not much variation in resistance to softening at temperatures below 540 °C (1000 °F). However, for longer periods of exposure at higher temperatures, high-alloy hot-work steels, such as H19, H21, and H10 modified, retain hardness better than do medium-alloy steels, such as H11.

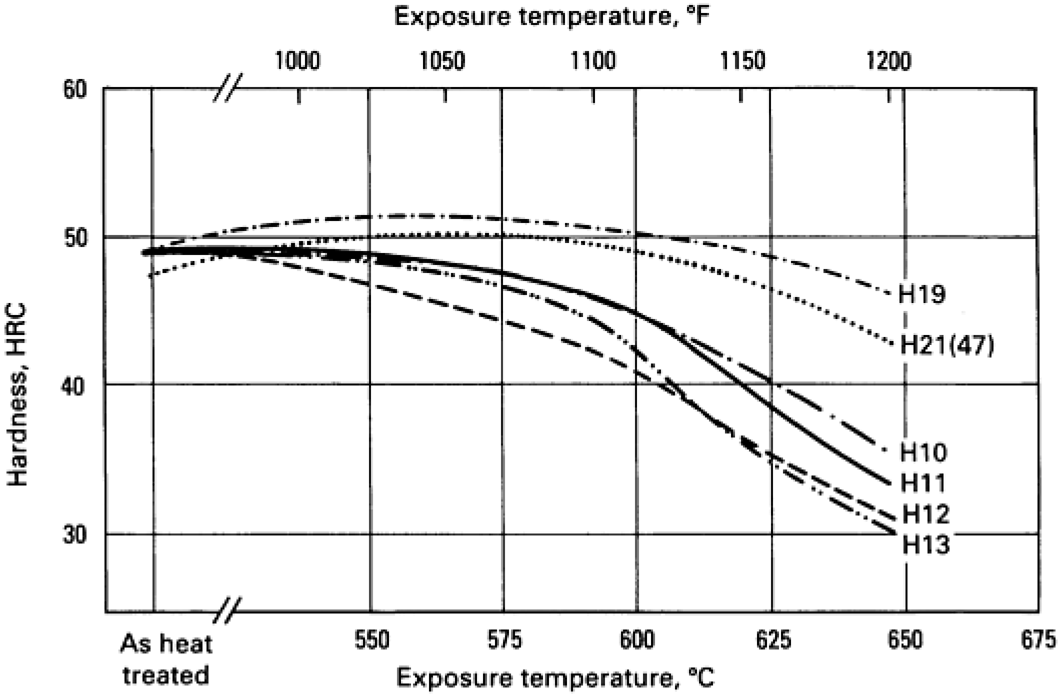

Fig. 5 Resistance of AISI hot-work tool steels to softening during 10 h elevated-temperature exposure as measured by room-temperature hardness. Unless otherwise specified by values in parentheses, initial hardness of all specimens was 49 HRC. Source: Ref 2

Resistance to Plastic Deformation. As shown in Fig. 6, the yield strengths of steels decrease at higher temperatures. However, yield strength also depends on prior heat treatment, composition, and hardness. The higher the initial hardness, the greater the yield strength at various temperatures. In normal practice, the level to which a die steel is hardened is determined by toughness requirements: the higher the hardness, the lower the toughness of a steel. Thus, in metal-forming applications, the die block is hardened to a level at which it should have enough toughness to avoid cracking. Figure 6 shows that, for the same initial hardness, 5% Cr-Mo steels (H11, and so forth) have better hot strengths than 6F2 and 6F3 at temperatures above 370 °C (700 °F).

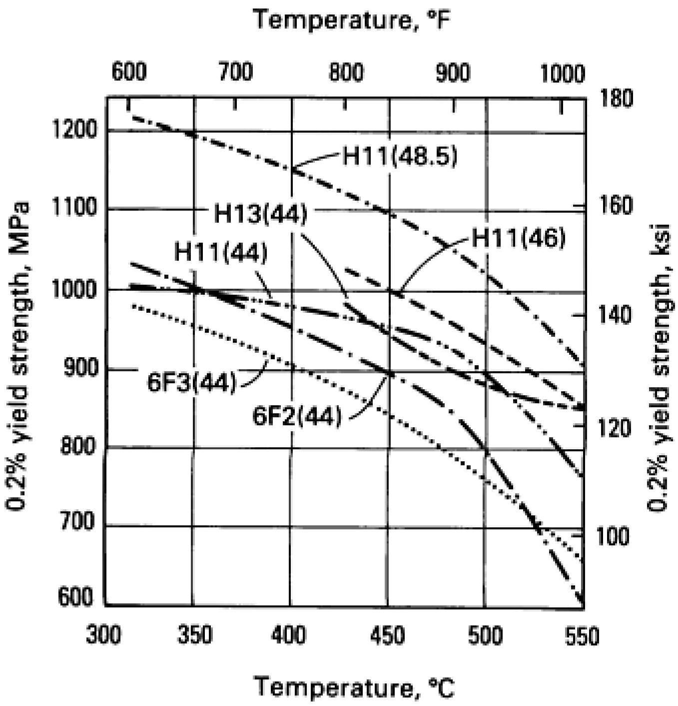

Fig. 6 Resistance of die steels to plastic deformation at elevated temperatures. Values in parentheses indicate room-temperature Rockwell C hardness. Source: Ref 2, 3

Toughness can be defined as the ability to absorb energy without breaking. The energy absorbed before fracture is a combination of strength and ductility. The higher the strength and ductility, the higher the toughness. Ductility, as measured by reduction in area or percent elongation in a tensile test, can therefore be used as a partial index of toughness at low strain rates.

Figure 7 shows the ductility of various hot-work steels at elevated temperatures, as measured by percent reduction in area of a specimen before fracture in a standard tensile test. As the curves show, high-alloy hot-work steels, such as H19 and H21, have less ductility than medium-alloy hot-work steels, such as H11. This explains the lower toughness of H19 and H21 in comparison to that of H11.

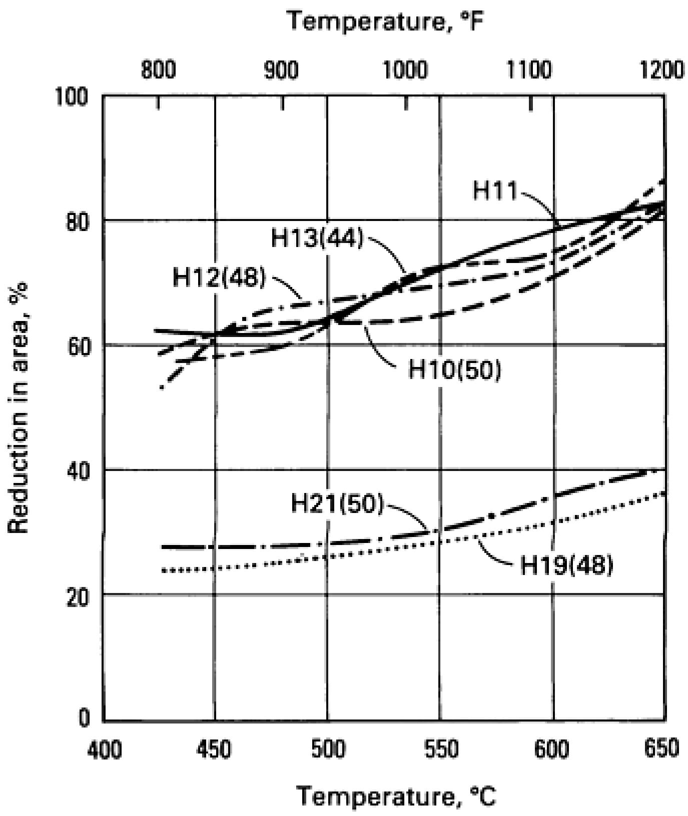

Fig. 7 Elevated-temperature ductilities of various hot-work die steels. Values in parentheses indicate room-temperature Rockwell C hardness.

Fracture toughness and resistance to shock loading are often measured by the notched-bar Charpy test. This test measures the amount of energy absorbed in introducing and propagating fracture, or the toughness of a material at high rates of deformation (impact loading). Figure 8 shows the results of Charpy V-notch tests on various die steels. The data show that toughness decreases as the alloy content of the steel increases. Medium-alloy steels, such as H11, H12, and H13, have better resistance to brittle fracture in comparison with H14, H19, and H21, which have higher alloy contents. Increasing the hardness of a steel lowers its impact strength. On the other hand, wear resistance and hot strength decrease with decreasing hardness. Thus, a compromise is made in actual practice, and the dies are tempered to near-maximum hardness levels at which they have sufficient toughness to withstand loading.

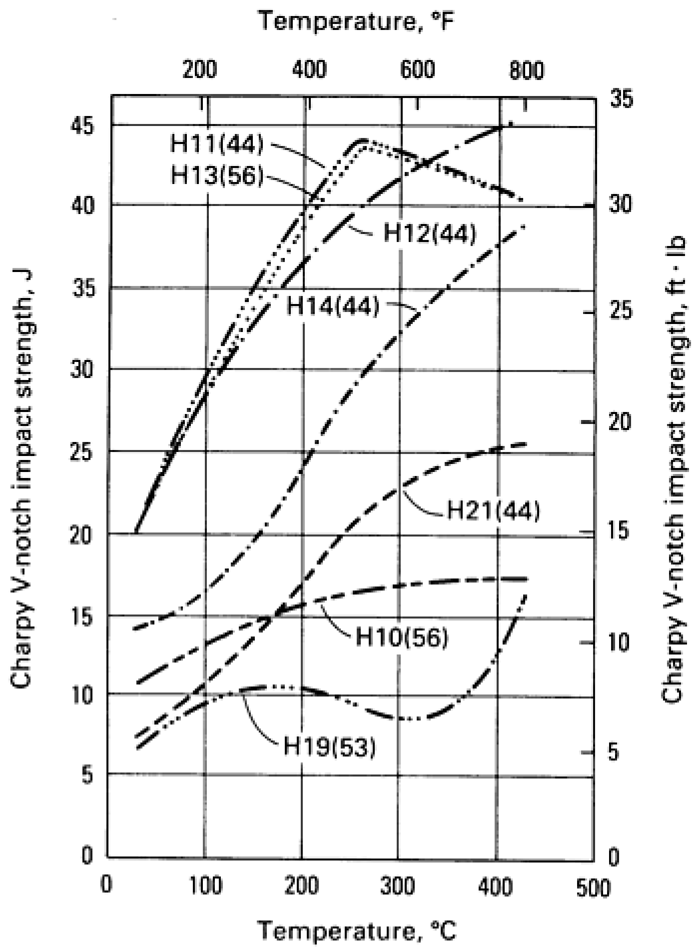

Fig. 8 Effect of hardness, composition, and testing temperature on Charpy V-notch impact strength of hot-work die steels. Values in parentheses indicate Rockwell C hardness at room temperature. Source: Ref 4

The data shown in Fig. 8 also illustrate the importance of preheating the dies before hot forming. Steels such as H10 and H21 require preheating and attain reasonable toughness only at high temperatures. For general-purpose steels, such as 6F2 and 6G, preheating to a minimum temperature of 150 °C (300 °F) is recommended; for high-alloy steels, such as H14 and H19, a higher preheating temperature is desirable to improve toughness.

Resistance to Heat Checking. Nonuniform expansion, caused by thermal gradients from the surface to the center of a die, is the chief factor contributing to heat checking. Therefore, a material with high thermal conductivity will make dies less prone to heat checking by conducting heat rapidly away from the die surface, reducing surface-to-center temperature gradients, and lessening expansion/contraction stresses. The magnitudes of thermal stresses caused by nonuniform expansion or temperature gradients also depend on the coefficient of thermal expansion of the steel; the higher the coefficient of thermal expansion, the greater the stresses.

From tests in which the temperature of the specimen fluctuated between 650 °C (1200 °F) and the water-quench bath temperature, it was determined that H10 was slightly more resistant to heat checking or cracking after 1740 cycles than were H11, H12, and H13. After 3488 cycles, H10 exhibited significantly more resistance to cracking than did H11, H12, and H13.

Fatigue Resistance. Mechanical fatigue of forging dies is affected by the magnitude of the applied loads, the average die temperature, and the condition of the die surface. Fatigue cracks usually initiate at points at which the stresses are highest, such as at cavities with sharp radii of curvature whose effects on the fatigue process are similar to notches (Fig. 9). Other regions where cracks may initiate include holes, keyways, and deep stamp markings used to identify die sets.

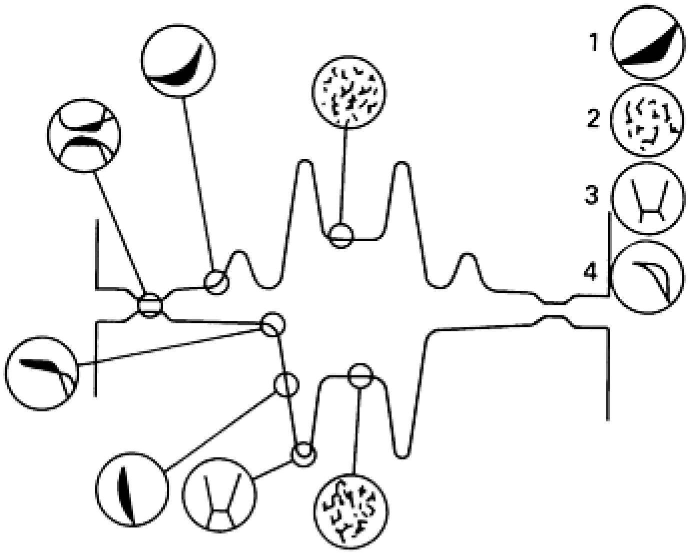

Fig. 9 Common failure mechanisms for forging dies. 1, Abrasive wear; 2, thermal fatigue; 3, mechanical fatigue; 4, plastic deformation. Source: Ref 5

Redesigning to lower the stresses is probably the best way to minimize fatigue crack initiation and growth. Redesigning may include changes in the die impression itself or modification of the flash configuration to lower the overall stresses. Surface treatments may also be beneficial in reducing fatigue-related problems. Nitriding, mechanical polishing, and shot peening are effective because they induce surface residual (compressive) stresses or eliminate notch effects, both of which delay fatigue crack initiation. On the other hand, surface treatments such as nickel, chromium, and zinc plating, which may be beneficial with respect to abrasive wear, have been found to be deleterious to fatigue properties.

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Die Inserts

Die inserts are used for economy in the production of some forgings. In general, they prolong the life of the die block into which they fit. The use of inserts can decrease production costs when several inserts can be made for the cost of making one solid die. The time required for changeover or replacement of inserts is brief, because a second set of inserts can be made while the first set is being used. Finally, more forgings can be made accurately in a die with inserts than in a solid die, because steel of higher alloy content and greater hardness can be used in inserts than would be safe or economical to use in solid dies. However, some commercial forge shops in which most of the forging units are gravity drop hammers make only limited use of die inserts.

Inserts can contain the impression of only the portion of a forging that is subject to greatest wear, or they can contain the impression of a whole forging. An example of the first type of insert is a plug type used for forging deep cavities.

Examples of the second type include master-block inserts that permit the forging of a variety of shallow parts in a single die block, and inserts for replacement of impressions that wear the most rapidly in multiple-impression dies.

A plug-type insert (Fig. 10) is usually a projection in the center of the die, such as would be required for making a hub or cup forging. In some impressions, the plug may not be in the center, and more than one plug can be used in a single impression.

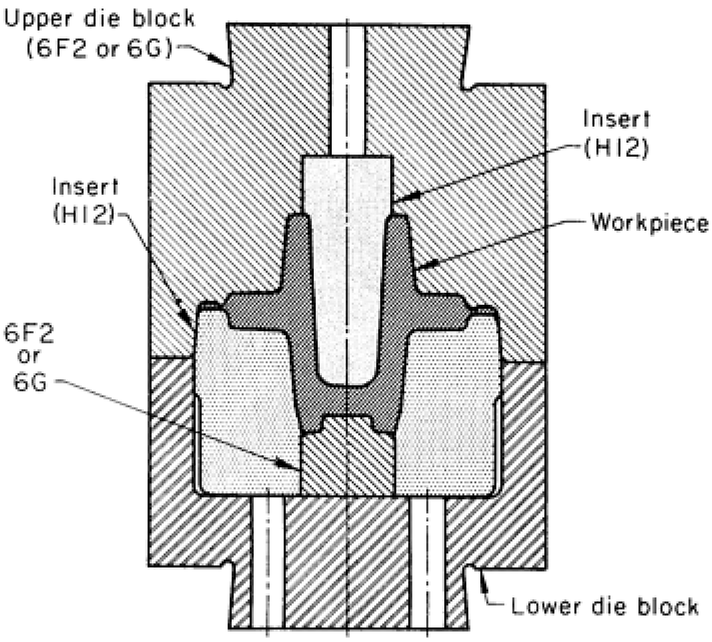

Fig. 10 Use of a plug-type insert in combination with a nearly complete insert in the lower die block for making a forging of extreme severity

Although plugs are used in either shallow or deep impressions, the need is usually greater in deep impressions. For impressions of moderate depth, an insert is advantageous if medium or large quantities of forgings are required. For deep, narrow impressions like that shown in Fig. 10, a plug-type insert is always recommended. Sometimes it is advantageous to use a plug in combination with a complete or nearly complete insert, as in Fig. 10, where a long H12 steel plug is used in the upper die and an almost complete female insert is used in the lower die.

Plug inserts can be made either from prehardened die steel at a higher hardness than the main die part or, for still longer life, from one of the hot-work tool steels. If wear is extremely high, the plug can be hard faced. Plugs are held in place by press fitting, by shrink fitting (by packing in dry ice before insertion), or by the use of plug keys.

Full inserts are generally used for making relatively shallow forgings. They offer one or more of the following advantages: the insert can be of high hardness with less danger of breakage, because it has the softer block as a backing; a higher-alloy steel can be used for the insert portion without a large increase in cost; changes in forging design are less costly when inserts are used; the same die block can be used for slightly different forgings by changing inserts; and inserts can be readily replaced if breakage occurs. Full inserts are used in many commercial forge shops, where a set of standard master blocks is kept available for use.

Another type of insert is for use in multiple-impression dies in which the impressions wear at different rates. Fuller, edger, or bender impressions are seldom used for close-tolerance work and may wear slowly compared with other impressions. Inserts are used only for the impressions that wear most rapidly.

This type of insert is not necessarily limited to shallow impressions. If the insert contains a single impression, the impression can be of any practical depth. However, if it contains several impressions, the impression depth is limited to about 64 mm ($2\frac{1}{2}$ in.) or less. Width of the insert must be considered: Sufficient wall thickness must be allowed between the edge of the impression and the edge of the insert, so that the die-block walls are not weakened too greatly.

Inserts for Hot Upset Forging. Inserts are widely used in upset forging. Solid dies are used in less severe stock gathering in short runs. A particular exception occurs with gripper dies in which the initial impressions are sunk in solid die blocks and used until worn out. The blocks are then resunk and used thereafter with inserts. Another exception occurs when the size of the available block and the number of required passes do not allow enough space between impressions for the sinking of inserts.

Heading tools for punching, trimming, and bending are often made with inserts. Most individual inserts can be replaced readily, and breakage of one heading tool in a multiple operation will not require replacement of the complete heading tool set. In operations in which wear is a major factor and replacement is frequent, as in deep punching, the use of inserts results in considerable savings in both die material and labor. Figure 11 shows heading tool and gripper die inserts used in horizontal forging machines.

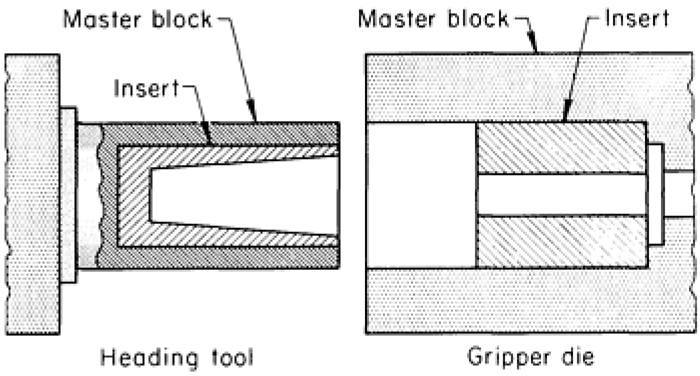

Fig. 11 Heading tool and gripper die inserts used in horizontal forging machines

Dies and Die Materials for Hot Forging

Parting Line

The parting line is the line along the forging where the dies meet. It may be in a single plane or it may be curved or irregular with respect to the forging plane, depending on the design of the forging. The shape and location of the parting line determine die cost, draft requirements, grain flow, and trimming procedures. A few of the considerations that determine the most effective location and shape of the parting line are described below.

In most forgings, the parting line is at the largest cross section of the part, because it is easier to spread metal by forging action than to force it into deep die impressions. If the largest cross section coincides with a flat side of a forging, there may be a particular advantage in locating the parting line along the edges of the flat section, thus placing the entire impression in one die half. Die costs can be reduced, because one die is simply a flat surface. Also, mismatch between upper and lower dies cannot occur, and forging flash can be trimmed readily.

When a die set having one flat die cannot be used, the position of the parting line should allow location of the preform in the finisher impression of the forging die and of the finished forging in the trimming die.

Because part of the metal flow is toward the parting line during forging, the location of the parting line affects the grain flow characteristics of a forged piece (Fig. 12). For good metal flow patterns in, for example, a forging having a vertical wall adjacent to a bottom web section, a parting line on the outer side of the wall should be placed either adjacent to the web section and near the bottom of the wall, or at the top of the wall. Placing the parting line at any point above the center of the bottom web but below the top of the wall may disrupt the grain flow and cause defects in the forging.

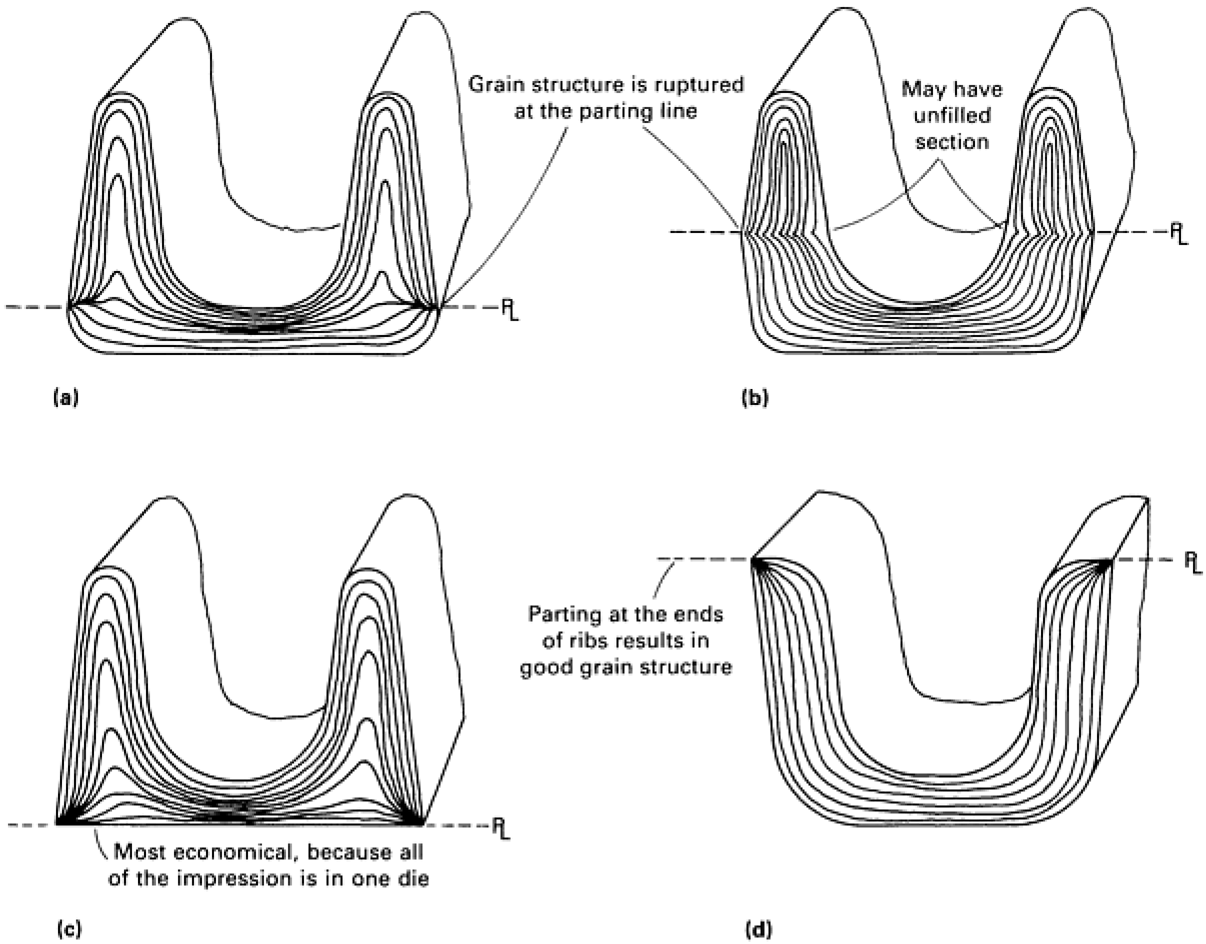

Fig. 12 Effect on metal flow patterns of various parting line locations on a channel section. (a) and (b) Undesirable; these parting lines result in metal flow patterns that cause forging defects. (c) and (d) Recommended; metal flow patterns are smooth at stressed sections with these parting lines. Source: Ref 6

Because the dies move only in a straight line, and because the forging must be removed from the die without damage either to the impression or to the forging, there can be no undercuts in the die impressions. Frequently, the forging can be inclined, with respect to the forging plane, to overcome the effect of an undercut.

Reference cited in this section

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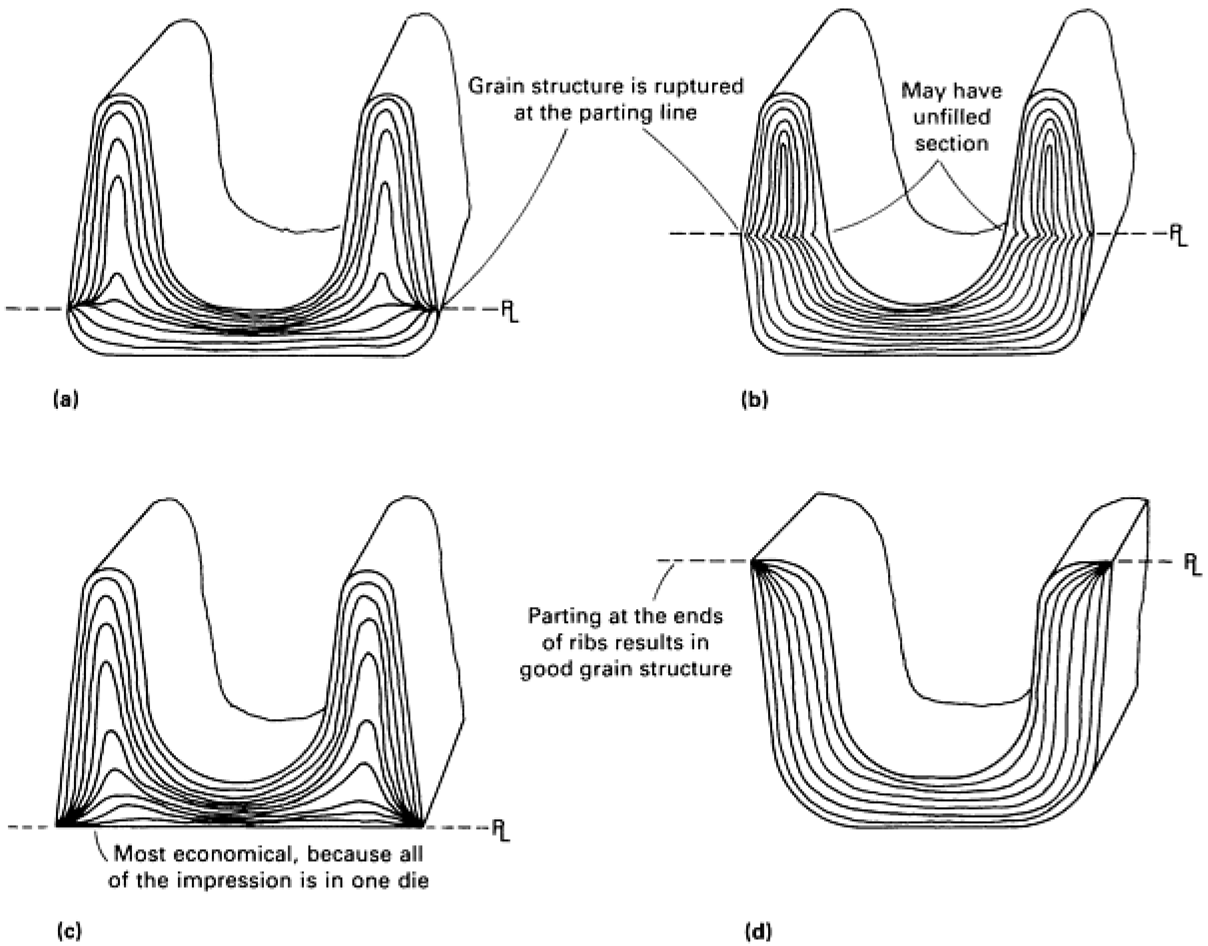

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Locks and Counterlocks

Many forgings require a parting line that is not flat and, correspondingly, die parting surfaces that are neither planar nor perpendicular to the direction in which the forging force is applied. Dies that have a change in the plane of their mating surfaces, and that therefore mesh ("lock") in a vertical direction when closed, are called locked dies.

In forging with locked dies, side or end thrust is frequently a problem. A strong lateral thrust during forging may cause mismatch of the dies or breakage of the forging equipment. There are several ways to eliminate or control side thrust. Individual forgings can be inclined, rotated, or otherwise placed in the dies so that the lateral forces are balanced (see Fig. 13c). Flash can be used to cushion the shock and help absorb the lateral forces. When the production quantity is large enough and the size of the forging is small enough to permit forging in multiple-part dies, the impressions can be arranged so that the side thrusts cancel one another out.

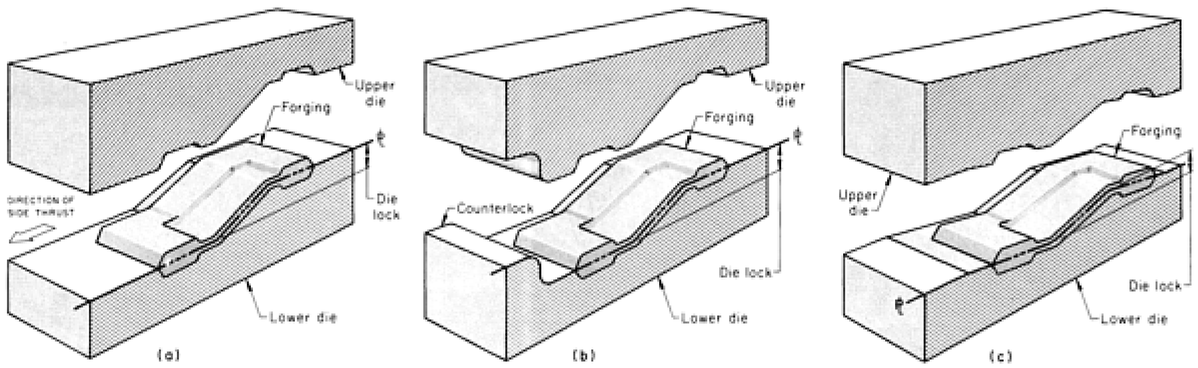

Fig. 13 Locked and counterlocked dies. (a) Locked dies with no means to counteract side thrust. (b) Counterlocked dies. (c) Dies requiring no counterlock because the forging has been rotated to minimize side thrust

Generally, with optimum placement of the impression in the die, and with the clearance between the guides on the hammer or press absorbing some side thrust, alignment between the upper and lower die impression can be maintained. Sometimes, however, the methods suggested above are insufficient or unsuitable for maintaining the required alignment, and it is necessary to counteract side thrust by machining mating projections and recesses (counterlocks) into the parting surfaces of the dies.

Counterlocks can be relatively simple. A pin lock that consists of a round or square peglike section with a mating section may be all that is required to control mismatch. Two such sections, or even sections at each corner of the die, may be necessary. A simple raised section with a mating countersunk section running the width and the length of the die can control side and end match. Counterlocks of these types should not be used in long production runs.

Counterlocks in high-production dies should be carefully designed and constructed. The height of the counterlock usually is equal to, or slightly greater than, the depth of the locking portion of the die. The thickness of the counterlock should be at least 1.5 times the height, so that it will have adequate strength to resist side thrust. Adequate lubrication of the sliding surfaces is difficult to maintain, because of the temperature of the die and the heat radiated from the workpiece. Therefore, the surfaces of the counterlock wear rapidly and need frequent reworking. Because of the cost of constructing and maintaining counterlocks, they should be used only if a forging cannot be produced more economically without them.

To forge the connecting link shown in Fig. 13 requires a locked die because of the part shape. With the die design shown in Fig. 13(a), side thrust is particularly large because of the angle at which the die faces meet the inclined portion of the work metal. Because no means is provided to counteract side thrust, it is impossible to avoid mismatch of the upper and lower dies. The position of the forging in the die in Fig. 13(b) is the same as in Fig. 13(a), but a counterlock is machined into the die to counteract side thrust. With this arrangement, the possibility of mismatch is eliminated, but the cost of making and maintaining the dies is high. Figure 13(c) shows a position of the forging in the die that is preferable for production. The workpiece has been rotated so that the side thrusts produced when forging the ends and the web cancel each other out. No counterlock is required, and accurate forgings can be produced.

Mismatch

Mismatch between the top and bottom dies is sometimes the cause of serious forging problems. Such mismatch can often be related to the design of the forging dies. An unacceptable amount of mismatch may persist despite optimum die design. When this happens, it may be possible to compensate for mismatch in forgings by the use of dies with built-in mismatch. For example, nonsymmetrical parts like connecting rods can often be forged in pairs (Fig. 14a), minimizing off-center force. Furthermore, ram deflection is minimized by locating the blocker and finisher impressions as close to the center of the die as possible. Some deflection still occurs, but it can be corrected by building a compensating mismatch into the die impressions. Because the blocker impression does most of the work in the forging of connecting rods, the mismatch is built into this impression, in a direction opposite that of ram deflection, as shown in Fig. 14(b). The amount of built-in mismatch varies with the offset from center, the size and shape of the forging stock, and the equipment used. In the forging of automotive connecting rods from 35 mm ($1\frac{3}{8}$ in.) diam stock in a 13.3 kN (3000 lbf) hammer, a 0.76 mm (0.030 in.) mismatch in the dies (Fig. 14b) was optimum.

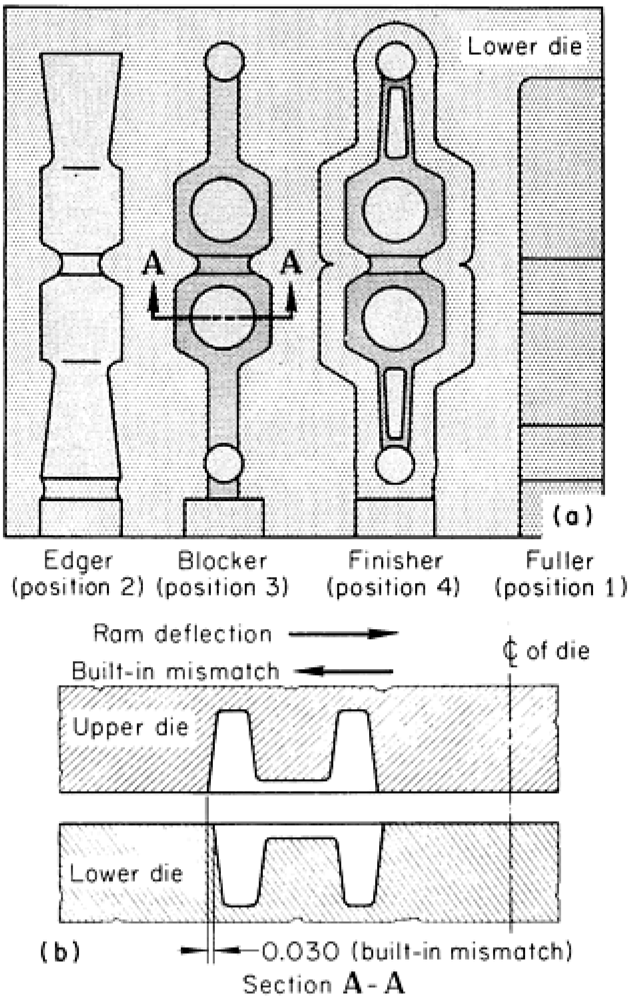

Fig. 14 Built-in die mismatch to compensate for ram deflection. (a) Arrangement of die impression for forging pairs of connecting rods. (b) Upper and lower dies with mismatch built into the blocker impression

Die locks and counterlocks are sometimes used to ensure proper alignment of the upper and lower dies. These locks consist of male and female components (projections and recesses) that are located on the parting surfaces of the dies to provide close-fitting junctions when the dies are closed. Because they are expensive to produce and require frequent maintenance or replacement, die locks are generally used only when the contours of the forging prevent the use of alternative methods for limiting or eliminating mismatch.

Draft

Draft, or taper, is added to straight sidewalls of a forging to permit easier removal from the die impression. Forgings having round or oval cross sections or slanted sidewalls form their own draft. Forgings having straight sidewalls, such as square or rectangular sections, can be forged by parting them across the diagonal and tilting the impression in the die so that the parting line is parallel to the forging plane. Another method is to place the parting line at an angle to the forging plane and machine a straight-wall cavity and a counterlock in each die. If ejectors or die kickouts are used, draft angles can be minimized.

The draft used in die impressions normally varies from 3 to 7° for external walls of the forging. Surfaces that surround holes or recesses have draft angles ranging from 5 to 10°. More draft is used on walls surrounding recesses to prevent the forging from sticking in the die as a result of natural shrinkage of the metal as it cools.

Dies and Die Materials for Hot Forging

Flash

The excess material in an impression die surrounds the forged part at the parting plane and is referred to as flash. Flash consists of two parts: the flash at the land and that in the gutter. The flash land is the portion of the flash adjacent to the part, and the gutter is outside the land. Flash is normally cut off in the trimming die.

The flash land impression in the die is designed so that as the dies close and metal is forced between the dies, the pressure in the part cavity is sufficient to fill the cavity without breaking the die. The pressure is controlled through land geometry, which determines the flash thickness and width. The flash land is generally constructed as two parallel surfaces that have the proper thickness-to-width ratio when the dies are closed.

The land thickness is determined by the forging equipment used, the material being forged, the weight of the forging, and the complexity of the forged part. The ratio of flash land width to flash land thickness varies from 2:1 to 5:1. Lower ratios are used in presses, and higher ratios are used in hammers.

Flash Gutter. The gutter is thicker than the flash land and provides a cavity in the die halves for the excess material. The gutter should be large enough so that it does not fill up with excess material or become pressurized. The four gutter designs commonly used are parallel, conventional, tapered open, and tapered closed (Fig. 15). Choice of gutter design is generally determined by the type of forging equipment used, the properties of the material being forged, the forging temperature, and the overall pressures exerted in the die cavity.

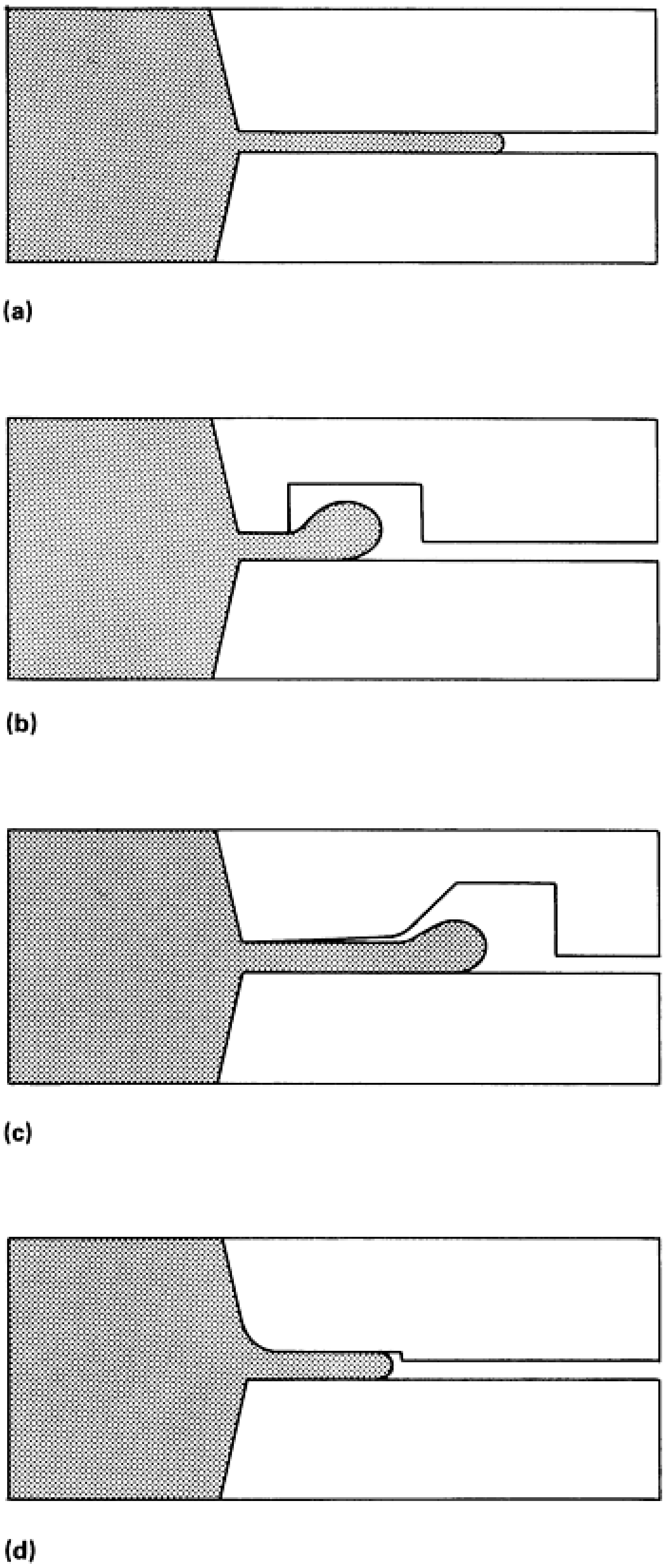

Fig. 15 Four designs commonly used for flash gutters. (a) Parallel. (b) Conventional. (c) Tapered open. (d) Tapered closed

Preform Design

One of the most important aspects of the closed-die forging process is the design of preforms (or blockers) to achieve adequate metal distribution. With proper preform design, defect-free metal flow and complete die fill can be achieved in the final forging operation and metal losses into flash can be minimized. The determination of the preform configuration is an especially difficult task and art in itself requiring skills achieved only with years of experience. In attempting to develop quantitative and objective engineering guidelines for preform design, one must have a thorough understanding of metal flow. Metal flow during forging can be considered to take place in two basic modes: extrusion (parallel to the direction of die motion) and upsetting (perpendicular to the direction of die motion). In most forgings, the geometry of the part is such that both modes of flow occur simultaneously. In the study of metal flow for designing the preform, it is very useful to consider various cross sections of a forging at which the flow is approximately in one plane. Figure 16 illustrates the planes of metal flow for some simple parts. The surface connecting the centers of the planes of flow is the neutral surface of the forging. The neutral surface can be thought of as the surface on which all movement of metal is parallel to the direction of die motion. Thus, metal flows away from the neutral surface, in a direction perpendicular to die motion.

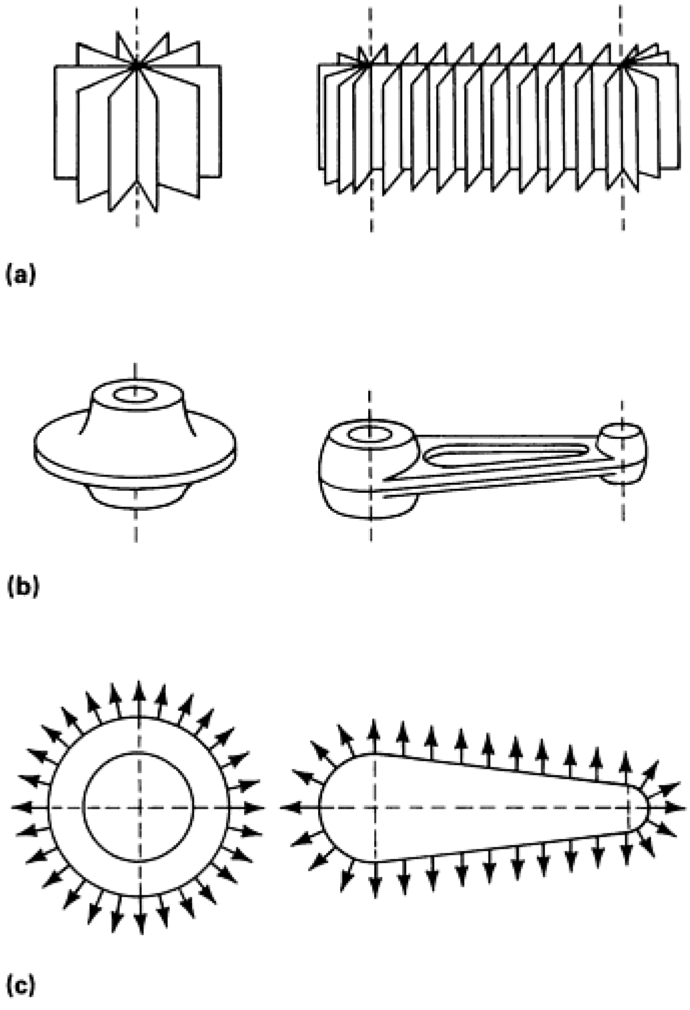

Fig. 16 Planes and directions of metal flow in the forging of two simple shapes. (a) Planes of flow. (b) Finished forging shape. (c) Directions of flow. Source: Ref 7

It is common practice in designing a preform to consider planes of metal flow, that is, selected cross sections of the forging, and to design the preform configuration for each cross section based on metal flow. The basic design guidelines are given below.

First, the area of each cross section along the length of the preform must be equal to the area of the finished cross section augmented by the area necessary for flash. Thus, the initial stock distribution is obtained by determining the areas of cross sections along the main axis of the forging. Second, all the concave radii (including fillet radii) of the preform should be larger than the radii of the forged part. Finally, whenever practical, the dimensions of the preform should be larger than those of the finished part in the forging direction so that metal flow is mostly of the upsetting type rather than of the extrusion type. During the finishing operation the material then will be squeezed laterally toward the die cavity without additional shear at the die/material interface. Such conditions minimize friction and forging load and reduce wear along the die surfaces. The application of the three principles for forging steel parts is illustrated for some solid cross sections in Fig. 17.

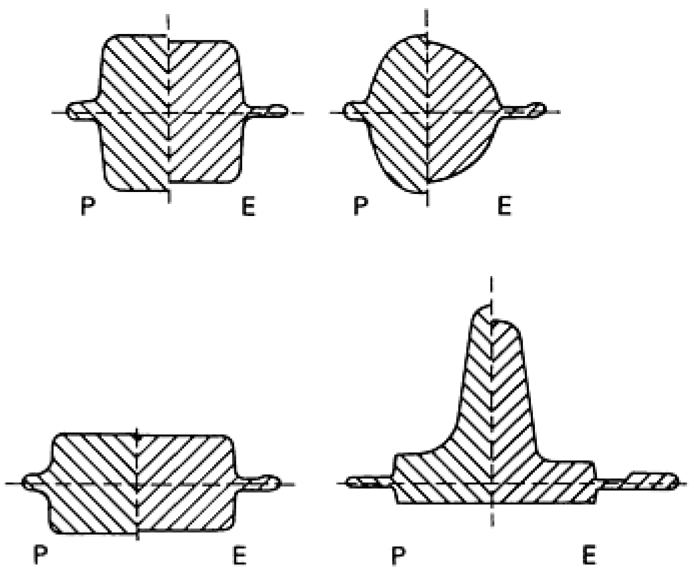

Fig. 17 Examples of suggested preform cross section designs for various steel forging end shapes. P, preform; E, end form. Source: Ref 8

Experimental and Modeling Methods for Preform Design. In order to ensure filling of a die cavity, without any forging defects, a preform of geometry determined by experimentation may be used. In this case, an initial preform geometry is selected based on an "educated guess," the part is forged, and if adequate cavity filling is not obtained, the preform shape is modified by machining or open-die forging until an adequate finishing operation is designed. Once the preform geometry is determined, the preforming dies can be modified accordingly. This trial-and-error procedure may be time consuming and expensive and therefore practical only for rather simple finish shapes.

A more systematic and well-proved method for developing the preform shape is by use of physical modeling, using a soft material such as lead, plasticine, or wax as model forging material, and hard plastic or mild steel dies as tooling. Thus, with relatively low cost tooling and with some experimentation, preform shapes can be determined.

More information on the use of physical modeling is available in the article "Modeling Techniques Used in Forging Process Design" in this Volume.

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Location of Impressions

The preform and finisher impressions should be positioned across the die block such that the forging force is as close to the center of the striking force (ram) as possible. This minimizes tipping of the ram, reduces wear on the ram guides, and helps maintain the thickness dimensions of the forging. When the forging is transferred manually to each impression, the impression for the operation requiring the greatest forging force is placed at the center of the die block, and the remaining impressions are distributed as nearly equally as possible on each side of the die block.

Symmetrical forgings usually have their centerline along the front-to-back centerline of the die block. For asymmetrical forgings, the center of gravity can be used as a reference for positioning the preform and finisher impressions in the die block.

The center of gravity of a forging does not necessarily correspond to the center of the forging force, because of the influence of thin sections on the forging force. Because the increase in force is not always directly proportional to the decrease in thickness, both the flash and the location of the thin sections must be considered when locating the impressions in a die block. Evenly distributed flash has little effect on an out-of-balance condition; very thin sections have a marked effect.

When the forgings are automatically transferred from station to station, the impressions must be in operational sequence across the die block. The machine construction usually counteracts the effects of off-center loading.

Multiple-Part Dies

Forging of more than one part in a single die is desirable under certain conditions, including:

- Costs for forging without multiple-part dies are prohibitively high because machine time is long and the proportion of metal lost to flash, sprues, and tonghold is high
- Production requirements are large
- Parting face of the die is uneven, and a balance of forces is needed to avoid incorporating a counterlock in the die
- The forging is so small that it cannot be produced economically in the equipment available

There are conditions, however, under which it is not practical to consider making more than one forging in a single die. These include:

- The parts are too large to be made in multiples in the available equipment
- The parts are too large to be handled more than one at a time
- Production requirements are not sufficient to make full use of the life of a multiple-part die

The above conditions generally cannot be considered singly, because there are many applications for which labor and machine costs, along with savings in metal, may or may not offset the cost of multiple-part dies.

Forgings that are best suited to production in multiple-part dies are those that can be arranged in pairs or other multiples in such a way that the forging forces are balanced. A forging in which the distribution of stock is uneven from one end to another, such as a connecting rod, is an example. When forged singly in a hammer, parts of this type require several blows in fuller and roller impressions, but when forged in multiples, they can be nested, grain flow permitting, to eliminate some of the blows required and to improve the production rate. A second example is a forging that, produced singly, must be made in dies having a single plane of lock (locked dies in which the nonhorizontal parting surface is planar). When such parts are forged in multiples in alternating positions, the forces imparted by the opposing planes of lock can be balanced.

Forgings of uniform section can be made either singly or in multiples. For making such forgings, multiple-part dies are used mainly to reduce per-piece forging costs or to increase the rate of production.

An advantage of multiple-part dies is that by more fully using the machine capacity and operator time they allow a reduction in forging piece costs, even though a larger-capacity forging hammer or press may be required or the machine cycle time may be longer.

The flash allowance for a part made in a multiple-part die is generally less than for a part made in a single-part die.

Dies for Precision Forging

The aircraft industry requires aluminum alloy and titanium alloy airframe forgings that undergo a minimum of machining. The forging industry has responded by developing precision, or no-draft, dies that produce forgings that require little or no machining before assembly.

Dies are being designed and fabricated not only with zero draft, but also with an undercut and closer tolerances. These dies consist of several pieces of steel that lock together to form a single unit. The simplest precision die has only a top and bottom die with a knockout pin to help remove the forging during the forging operation. As the complexity of a forging increases, the design of the die requires more pieces to form the part. The die may consist of two or more pieces to form the outside of the forging (wraps), and a bottom and top punch to form the inside configuration. All of these pieces must fit together--the wraps and the bottom punch, which fits into the wraps to make a bottom die, and top punch, which then fits into the bottom assembly to make a complete set of forging dies (Fig. 18). For the forging operation, the dies are contained in a holder or ring die designed to accept several different precision dies. During the forging operation, the bottom assembly has to separate so that the forging can be removed.

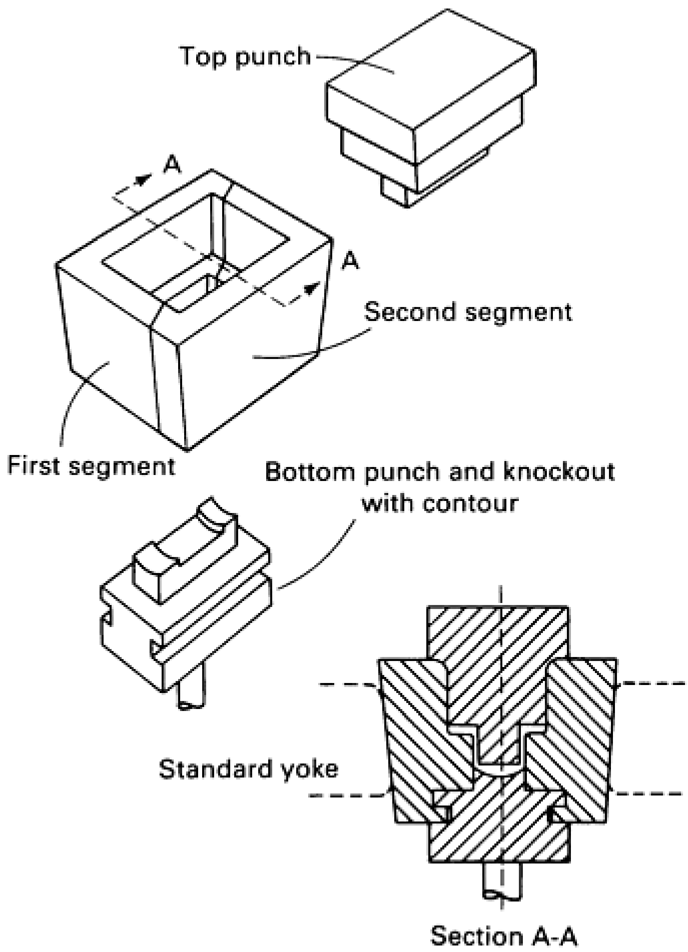

Fig. 18 Typical wrap dies for precision forging

More information on precision forging is available in the articles "Precision Forging," "Forging of Aluminum Alloys," and "Forging of Titanium Alloys" in this Volume.

Fabrication of Impression Dies

Die sinking is a machine trade whereby a craftsman known as a die sinker performs certain steps to produce a forging die. In addition to personal skills, the die sinker needs the appropriate machines and hand tools. As the forging industry has increasingly demanded more complex forgings, the machine tool industry has developed more sophisticated machine tools to facilitate the production of these complex dies. The die sinker still uses the same basic steps that have been used for years, but with new machine tools and refined techniques that permit fabrication of dies that can furnish extremely complex and close-tolerance forgings. The die-making process includes selection of materials for the die; die preparation, taking into consideration the forging machine that will produce that particular forging; design preparation; machining the dies; benching the dies; and taking a cast of the dies.

Quality forging dies are achieved through a blending of the skill and knowledge of both the forging engineer and the die sinker. When the forging design has been completed and approved, the die sinker, after consulting with the designer on any special details of the job, begins the process of sinking the desired impression in the die blocks of alloy steel. Rough die blocks, carefully forged and heat treated, usually are obtained from firms that specialize in their manufacture. Blocks may be purchased in a variety of shapes, sizes, and tempers, depending on the type and size of forging intended and, accordingly, the type and size of equipment to be used. They may range from a few hundred pounds to several tons in weight.

Generally, the die shop begins its work by following this sequence of operations: top, bottom, one side, and one end need to be finish surfaced either on a planer, a milling machine, and/or a surface grinder. All surfaces must be flat, parallel, and 90° to each other. Because of the size and weight of the die block, handling holes are drilled in the ends or sides so that the dies can be handled more easily. The rough blocks are then moved to a planer or planer mill where they are paired as upper and lower die blocks of a die set. Die faces are often ground to a fine finish to obtain a smooth surface for layout work.

After the material has been selected and prepared, the die sinker is given a print of the customer's forging and a die design. He is now ready to sink the die. In order to make the layout lines on the die steel more visible, a solution of copper sulfate or die blue is applied to the face of each die. The outline of the forging is scribed on the face of the dies to the exact dimensions dictated by the drawing. Mold lines are identified first, and the draft lines are added (3°, 5°, 7°, and so forth). Dimensions for the draft are determined by the depths of the impressions. To ensure that impressions in each die match, the layout is located on the dies in relation to the side and end match edges. Special shrink scales are used that are based on the shrink factor of the material to be forged. The design dictates the number of impressions--roller, fuller, edger, cutoff, and gate--in each set of dies.

Layout lines are scribed on each die using a square and a blade protractor, dividers, and a hardened scriber. If it is possible to stand the dies on end or on their sides on a surface plate, a height gage can be used to scribe lines that are parallel to the match edges. This method is very accurate; some tools have digital readouts and a programmable shrink factor. The finishing impression is usually positioned such that its weight center will be aligned as nearly as possible with the center of the hammer or press ram, as measured from all sides. This helps ensure perfect balance in the forging equipment, permits full utilization of maximum ram impact as the forging is in the finishing impression, and eliminates wear-causing side thrusts and pressures during forging. After the layout is finished and checked, the dies are ready for machining of the impression.

The machine tools for die sinking have changed dramatically over the years. The simple vertical milling machine has developed into a very sophisticated machine tool, with hydraulic movement of ram, table, and spindle, having the ability to trace from a template or tracing mold. The impression (cavity) is sunk to within a few thousandths of an inch of its finished part size.

The cutting tools used are fabricated from high-speed tool steel and have two, three, or four flutes (straight or spiral). They may also have angles to produce drafts of 3°, 5°, 7°, and so forth. For heavy flat cutting, a carbide insert cutter is used. As the die sinking begins, the deepest section is cut first with the largest cutter, working progressively to the shallowest section, until all vertical walls are machined. The webs and radii are machined last. The X and Y dimensions are machined according to the scribed lines on the face, with control of the Z dimensions or depth by means of a depth gage or profile template. If the design calls for more than one impression, only the first impression is made until it has been benched and a cast has been submitted for approval. Regardless of when the rest of the operations are completed, the same procedure is used. Flashing and guttering of the dies can be done at either time.

The complexity of some forgings may dictate that a die be fabricated using a wooden pattern of the forging. The pattern is then used to construct a plaster mold that is used to trace the impression into the die. This method requires minimal layout. The dimensions of the impression are determined by the mold.

Finishing of impressions is primarily done by hand with the aid of power hand grinders. All tool marks and sharp corners must be removed, and all vertical and horizontal radii made according to specifications. The surfaces are then polished. Most of the surfaces have been machined within a few thousandths of the finish dimensions; subsequent benching is not done to remove an appreciable amount of stock, but only to polish the surfaces to ensure that they are true in every dimension and free of tool marks, blemishes, and sharp corners. These hand operations help ensure filling of the impression with the least resistance to metal flow during forging. Likewise they minimize abrasive wear on the impressions.

When the bench work on the finishing impression is completed, a parting agent is applied to the surface of the impression to prepare for proofing of the impression. The pair of dies is clamped together in exact alignment, using the matched edges as guides, and the cavity formed by the finishing impression is filled with molten lead, plaster, or special nonshrinking compounds to obtain a die proof. The die proof is then checked for dimensional accuracy. When all dimensions are correct, the die proof is submitted to the customer for approval, if requested.

Other die impressions may then be sunk (to perform edging, fullering, and bending operations), depending on the complexity of the forging. These impressions for preliminary forging operations may also be sunk in a separate set of dies. The arrangement and sequence of preliminary operations differ widely according to variations in practice throughout the forging industry.

Ordinarily, the final machining operations on the faces of a set of dies are performed on the flash gutter. After guttering of dies, dowel pockets are usually milled into one side of the shank of each die block. The dowel pocket accommodates the dowel key, which is inserted by the hammer or press operator to maintain die alignment in the equipment from front to back.

Another close inspection of the dies is generally scheduled as a final precaution. All dimensions of blocking, as well as finishing impressions, are again carefully compared with the blueprint dimensions and specifications.

Extreme care is required in bringing the dies into exact alignment as they are placed in the forging equipment so that forgings will be on match and there will be a minimum of strain on the equipment and wear on the dies. Dies correctly and properly handled are normally capable of producing thousands of uniform forgings of identical shape and size.

An alternative method for sinking dies uses electrodischarge machining (EDM) in place of a vertical mill. This method is used when minimal draft angles and very narrow ribs are required, and it has the ability to produce dies accurately. Also, if several of the same cavities are to be sunk in one die, use of EDM ensures reproducibility.

The machine tool for this method of die fabrication has a hydraulic-powered ram and table. The table is a large tank that is open at the top. All metal removal is done with the die block submerged in a dielectric solution, which is used as a flushing agent to keep the burning area clean. The solution also acts as the carrier for electric current between the electrode and the die block. The solution is constantly circulated through a separate filter system to keep it clean and free of contaminants from the burning operation. A clean solution is necessary for an efficient burn. The electrode never makes contact with the die block as the electric current passes through the dielectric solution to the die block and erodes the die steel to create the impression.

Resinking

Solid dies must be resunk after they have worn out of tolerance. The number of resinkings that can be made in a set of dies is a function of block thickness less maximum depth of impression. For a block of a given thickness, the number of resinkings depends mainly on the depth of the impression. Shallow impressions such as those used for making open-end wrenches or adjustable wrench handles may be resunk as many as six times before the blocks are too thin for further use. With deeper impressions, the number of possible resinkings decreases to one or, in extreme cases, none. In general, the thickness of the block remaining beneath (or above) the impression should be at least three times the depth of the impression. That is, if the impression is 51 mm (2 in.) deep, the total thickness of the block should be at least 203 mm (8 in.). These figures are only approximate, and the thickness required will depend somewhat on the severity of the impression (radii and draft angles) as well as on the depth. For extremely shallow forgings such as thin open-end wrenches, the block thickness should be more than three times the depth of the impression; otherwise, the block might not have enough thickness to provide adequate backing.

For long production runs, some shops resink the dies by small amounts (for example, 1.6 mm, or $\frac{1}{16}$ in.) at shorter intervals instead of waiting until the impression is worn completely out of tolerance and needs a deeper resink.

Cast Dies

Most forging dies are fabricated by machining the impressions in wrought steel (die sinking; see the section "Fabrication of Impression Dies" in this article). For some applications, however, cast dies have proved to be economical alternatives.

Advantages. The principal advantage of cast dies is the savings in diemaking costs that can be effected by minimizing the amount of machining necessary for die fabrication. Usually, only a polishing operation is necessary to finish cast dies. Another advantage of cast dies is improved microstructure over wrought dies, with smaller, more evenly dispersed carbides and less grain-boundary segregation of carbides. Nonuniform carbide distribution in some wrought tool steels can lead to early wear (in areas lean in carbides) and premature heat checking (in areas rich in carbides). A further advantage provided by cast dies is more equiaxed grain structure than wrought products formed by rolling or forging. Grain direction in wrought alloys improves properties in some directions (parallel to the grain) but results in reduced properties transverse to the grain direction. Castings have no grain directionality and therefore display more uniform properties.

Disadvantages. There are also some disadvantages in using cast dies. Sections around the die cavity must be of a fairly uniform thickness to avoid excessive residual stresses in the casting of the die. Also, because of the lower strength of cast dies, the sections around the die cavity must be relatively thick; the dies can therefore become rather massive. Finally, inspection can be difficult; radiographic inspection is virtually the only method available to test for soundness.

Where Cast Dies Are Used. Large cast dies are used when it is not convenient to make the die as a forging either because of its mass or because of a lack of capacity to produce a forging of the required size. Cast dies can be used as inserts when intricate detail is required in the die cavity. Cast dies also are sometimes used for isothermal forging because the alloys used for these dies (for example, nickel-base alloys and TZM molybdenum alloy) are difficult to machine.

Heat Treating

Nominal compositions of chromium- and tungsten-base AISI hot-work tool steels are given in Table 1. The group of steels denoted low-alloy proprietary steels in Table 1 is included here in the discussion of hot-work tool steels because they are also used extensively for hot-work applications. Table 2 summarizes the heat-treating practices commonly employed for this composite group of tool steels.

Table 2 Recommended heat-treating practice for hot-work tool steels listed in Table 1

Steel[a]	**Annealing**					Hardening						
	Temperature[b],		**Cooling rate**[c],		**Annealed hardness, HB**	**Temperature**				**Holding time, min**	**Quenching medium**	Quenched hardness, HRC
						Preheat		**Austenitize**				
	°C	**°F**	**°C/h**	**°F/h**		**°C**	**°F**	**°C**	**°F**			
Chromium-base AISI hot-work tool steels												
H10	845-900	1550-1650	22	40	192-229	815	1500	1010-1040	1850-1900	15-40[d]	A	**56-59**
H11	845-900	1550-1650	22	40	192-229	815	1500	995-1025	1825-1875	15-40[d]	A	**53-55**
H12	845-900	1550-1650	22	40	192-229	815	1500	995-1025	1825-1875	15-40[d]	A	**52-55**
H13	845-	1550-	22	40	192-229	815	1500	995-	1825-	15-40[d]	A	**49-53**

	900	1650						1040	1900			
H14	870-900	1600-1650	22	40	207-235	815	1500	1010-1065	1850-1950	15-40[d]	A	**55-56**
H19	870-900	1600-1650	22	40	207-241	815	1500	1095-1205	2000-2200	2-5	A, O	**52-55**
Tungsten-base AISI hot-work tool steels												
H21	870-900	1600-1650	22	40	207-235	815	1500	1095-1205	2000-2200	2-5	A, O	**43-52**
H22	870-900	1600-1650	22	40	207-235	815	1500	1095-1205	2000-2200	2-5	A, O	**48-57**
H23	870-900	1600-1650	22	40	212-255	815	1500	1205-1260	2200-2300	2-5	O	**33-35[e]**
H24	870-900	1600-1650	22	40	217-241	815	1500	1095-1230	2000-2250	2-5	A, O	**44-55**
H25	870-900	1600-1650	22	40	207-235	815	1500	1150-1260	2100-2300	2-5	A, O	**46-53**
H26	870-900	1600-1650	22	40	217-241	870	1600	1175-1260	2150-2300	2-5	A, O, S	**63-64**
Low-alloy proprietary steels												
6G	790-815	1450-1500	22[f]	40[f]	197-229	Not required		845-855	1550-1575	. . .	O[g]	**63 min[h]**
6F2	780-795	1440-1460	22[i]	40[f]	223-235	Not required		845-870	1550-1600	. . .	O[g]	**63 min[h]**
6F3	**760-775**	**1400-1425**	**22[j]**	**40[f]**	**235-248**	**Not required**		**900-925**	**1650-1700**	**. . .**	**A[k]**	**63 min[h]**

Note: A, air; O, oil; S, salt.

(a) Holding time, after uniform through heating, varies from about 15 min, for small sections, to about 1 h, for large sections. Work is cooled from temperature in still air.

(b) Lower limit of range should be used for small sections, upper limit should be used for large sections. Holding time varies from about 1 h for light sections and small furnace charges to about 4 h for heavy sections and large charges; for pack annealing, hold for 1 h per inch of pack cross section.

(c) Maximum rate, to 425 °C (800 °F) unless footnoted to indicate otherwise.

(d) For open-furnace heat treatment. For pack hardening, hold for $\frac{1}{2}$ h per inch of pack cross section.

(e) Temper to precipitation harden.

(f) To 370 °C (700 °F).

(g) To 205 to 175 °C (400 to 350 °F), then air cool.

(h) Temper immediately.

(i) For isothermal annealing, furnace cool to 650 °C (1200 °F), hold for 4 h, furnace cool to 425 °C (800 °F), then air cool.

(j) For isothermal annealing, furnace cool to 670 °C (1240 °F), hold for 4 h, furnace cool to 425 °C (800 °F), then air cool.

(k) Cool with forced-air blast to 205 to 175 °C (400 to 350 °F), then cool in still air.

Normalizing. Because these steels as a group are either partially or completely airhardening, normalizing is not recommended.

Annealing. Recommended annealing temperatures, cooling practice, and expected hardness values are given in Table 2. Heating for annealing should be slow and uniform to prevent cracking, especially when annealing hardened tools. Heat losses from the furnace usually determine the rate of cooling; large furnace loads will cool at a slower rate than light loads. For most of these steels, furnace cooling to 425 °C (800 °F), at 22 °C max (40 °F max) per hour, and then air cooling, will suffice.

For types 6F2 and 6F3, an isothermal anneal (Table 2) may be employed to advantage for small tools that can be handled in salt or lead baths or for small loads in batch-type furnaces; however, isothermal annealing has no advantage over conventional annealing for large die blocks or large furnace loads of these steels.

In controlled-atmosphere furnaces, the work should be supported so that it does not touch the bottom of the furnace. This will ensure uniform heating and permit free circulation of the atmosphere around the work. Workpieces should be supported in such a way that they will not sag or distort under their own weight.

Stress Relieving. It is sometimes advantageous to stress relieve tools made of hot-work steel after rough machining but before final machining, by heating them to 650 to 730 °C (1200 to 1350 °F). This treatment minimizes distortion during hardening, particularly for dies or tools that have major changes in configuration or deep cavities. However, closer dimensional control can be obtained by hardening and tempering after rough machining and before final machining, provided that the final hardness obtained by this method is within the machinable range.

Preheating before austenitizing is nearly always recommended for all hot-work steels, with the exception of 6G, 6F2, and 6F3. These steels may or may not require preheating, depending on size and configuration of the workpieces. Recommended preheating temperatures for all the other types are given in Table 2.

Die blocks or other tools for open-furnace treatment should be placed in a furnace that is not over 260 °C (500 °F). Work that is packed in containers may be safely placed in furnaces at 370 to 540 °C (700 to 1000 °F). Once the workpieces (or containers) have attained furnace temperature, they are heated slowly and uniformly, at 85 to 110 °C (150 to 200 °F) per hour, to the preheating temperature (Table 2) and held for 1 h per inch of thickness (or per inch of container thickness, if packed). Thermocouples should be placed adjacent to the pieces in containers. Controlled atmospheres or other protective means must be used above 650 °C (1200 °F) to minimize scaling and decarburization.

Austenitizing temperatures recommended for the hardening of hot-work tool steels are given in Table 2. Rapid heating from the preheating temperature to the austenitizing temperature is preferred for types H19 through H26.

Except for steels H10 through H14 (see Table 2), time at the austenitizing temperature should only be sufficient to heat the work completely through; prolonged soaking is not recommended.

The equipment and method employed for austenitizing are frequently determined by the size of the workpiece. For tools weighing less than about 227 kg (500 lb), any of the methods would be suitable. However, larger tools or dies would be difficult to handle in either a salt bath or a pack.

Tools or dies made of hot-work steel must be protected against carburization and decarburization when being heated for austenitizing. Carburized surfaces are highly susceptible to heat checking. Decarburization causes decreased strength, which may result in fatigue failures. However, the principal detrimental effect of decarburization is to mislead the heat treater as to the actual hardness of the die. To obtain specified hardness of the decarburized surface, the die is tempered at too low a temperature. The die then goes into operation at excessive internal hardness and breaks at the first application of load.

An endothermic atmosphere produced by a gas generator is probably the most widely used protective medium. The dew point is normally held from 2 to 7 °C (35 to 45 °F) in the furnace, depending on the carbon content of the steel and the operating temperature. A dew point of 3 to 4 °C (38 to 40 °F) is ideal for most steels of type H11 or H13 when austenitized at 1010 °C (1850 °F).

Quenching. Hot-work steels range from high to extremely high in hardenability. Most of them will achieve full hardness by cooling in still air; however, even with those types having the highest hardenability, sections of die blocks may be so large that insufficient hardening results. In such instances, an air blast or an oil quench is required to achieve full hardness. Hot-work steels are never water quenched. Recommended quenching media are listed in Table 2.

If blast cooling is used, dry air should be blasted uniformly on the surface to be hardened. Dies or other tools should not be placed on concrete floors or in locations where water vapor may strike them during air quenching.

Some of the hot-work steels will scale considerably during cooling to room temperature in air. An interrupted quench reduces this scaling by eliminating the long period of contact with air at elevated temperature, but it also increases distortion. The best procedure is to quench from the austenitizing temperature in a salt bath held at 595 to 650 °C (1100 to 1200 °F), holding the workpiece in the quench until it reaches the temperature of the bath, and then with-drawing it and allowing it to cool in air. An alternative, but less precise, procedure is to quench in oil at room temperature or slightly above and judge by color (faint red) when the workpiece has reached 595 to 650 °C (1100 to 1200 °F); the piece is then quickly withdrawn and permitted to cool to room temperature in air. While cooling, the piece should be placed in a suitable rack, or be supported by wires, in such a manner as to allow air to come in contact with all surfaces.

Steel H23 requires a different type of interrupted quench, because ferrite precipitates rapidly in this steel at 595 °C (1100 °F), and M_S is below room temperature. This steel should be quenched in molten salt at 165 to 190 °C (325 to 375 °F) and the air cooled to room temperature. This steel will not harden in quenching but will do so by secondary hardening during the tempering cycle.

Parts quenched in oil should be completely immersed in the oil bath, held until they have reached bath temperature, and then transferred immediately to the tempering furnace. Oil bath temperatures may range from 55 to 150 °C (130 to 300 °F), but should always be below the flash point of the oil. Oil baths should be circulated and kept free of water.

Tempering. Hot-work tool steels should be tempered immediately after quenching, even though sensitivity to cracking in this stage varies considerably among the various types. These steels are usually tempered in air furnaces of the forced-convection type. Salt baths are used successfully for smaller parts, but for large, complex parts, salt bath tempering may induce too severe a thermal shock and cause cracking. The effect of tempering temperature on the hardness of chromium-base AISI hot-work tool steels is shown in Fig. 19; the effect of tempering temperature on the hardness of tungsten-base AISI hot-work tool steels is shown in Fig. 20.

Fig. 19 Effect of tempering temperature on hardness of chromium-base AISI hot-work tool steels. See also Fig. 20.

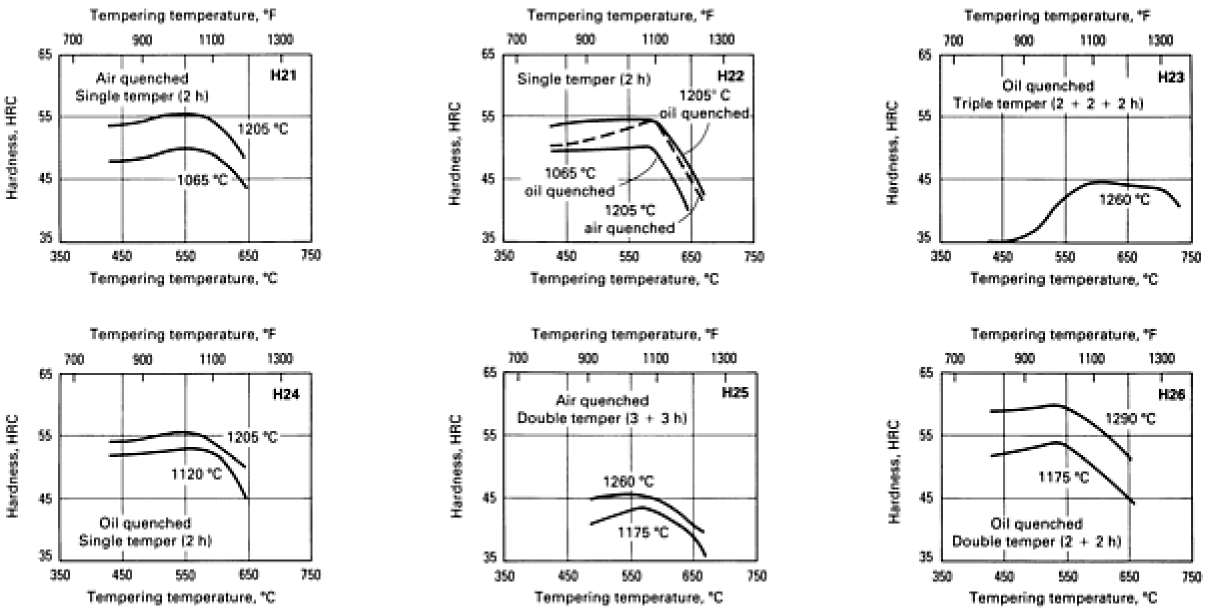

Fig. 20 Effect of tempering temperature on hardness of tungsten-base AISI hot-work tool steels. See also Fig. 19.

Multiple tempering ensures that any retained austenite that transforms to martensite during the first tempering cycle is tempered before a tool is placed in service. Multiple tempering also minimizes cracks due to stress originating from the hardening operation.

Multiple tempering has proved to be particularly advantageous for large or sharp-cornered die blocks that are not permitted to reach room temperature before the first tempering operation.

Trimming and Punching Dies

Trimming is the removal of flash that is produced on the part during the forging operation. Trimming may also be used to remove some of the draft material, thereby producing straight sidewalls on the part. It is usually performed by a top die and bottom die that are shaped to the contour of the part. The top die acts as a punch to push the part through the lower die containing the cutting edge. If the top die does not follow the contour of the part, the part may be deformed during the trimming operation.

An operation similar to trimming is punching, in which excess material on an internal surface is removed. To ensure accurate cuts, punching and trimming operations are often performed simultaneously.

Selection of materials for trimming and punching dies is based on the type of material to be trimmed and whether the part is to be trimmed while hot or cold. Punches are normally made from proprietary tool steels when carbon and stainless steels are to be trimmed, and from 1020 steel that has been hard faced when nonferrous alloys are to be trimmed. The trimming die, or bottom die, can be made from D2 tool steel or from cold-rolled steel that has a high-strength alloy hard facing applied to the cutting edge (see Table 3).

Table 3 Typical materials for trimming and punching dies

Material to be trimmed	**Cold trimming**				Hot trimming[a]	
	Normal trim		**Close trim**			
	Punch	**Blade**	**Punch**	**Blade**	**Punch**	Blade
Carbon and alloy steels	6F2 or 6G at 341 to 375 HB	D2 at 54 to 56 HRC	Generally hot trim		6F2 or 6G at 341 to 375 HB	**Hard facing alloy 4A on 1035 steel[b]; or D2 at 58 to 60 HRC**
Stainless steels and heat-resisting alloys	Generally hot trim		Generally hot trim		6F2 to 6G at 388 to 429 HB	**D2 at 58 to 60 HRC**
Aluminum, magnesium, and	**6150 at 461 to 477 HB**	**Hard facing alloy 4A on 1020 steel[b]; or O1**	**D2 at 58 to 60**	**D2 at 58 to 60**	**1020 soft**	**Hard facing alloy 4A on 1020 steel[b]**

(a) Both normal and close trimming.

(b) Hard facing alloy 4A has nominal composition of Co-1C-30Cr-4.5W-3Ni-1.5Fe.

Dies and Die Materials for Hot Forging

Causes of Die Failure

The three basic causes of premature die failure are overloading of the die, abrasive action, and overheating.

Overloading. Although fewer die failures can be ascribed to overloading than to abrasion or overheating, an overloaded die wears rapidly and may break. Overloading can be avoided by careful selection of die steel and hardness, use of blocks and inserts of adequate size, proper application of working pressures, proper die design to ensure correct metal flow, and proper seating of the dies in the hammer or press. Overloading from inadequate hammer or press capacity should not be compensated for by overheating the work metal.

Abrasive action is inherent in the flow and spreading of hot metal in the impression of a forging die. Abrasion is particularly severe if the design of the forging is complex or in other respects difficult to forge, if the metal being forged has a high hot strength, or if there is scale on the work metal.

Although abrasion cannot be eliminated, its effects can be minimized by good die design (including provision for a smooth progression in the shape of the forging from one die impression to the next, with work in the finisher at the minimum that is practical), careful selection of die composition and hardness, and a forging technique that includes proper heating, any necessary descaling, and correct die lubrication.

Overheating. As a die becomes hotter, its resistance to wear decreases. Overheating causes most of the premature die wear that occurs in forging.

Overheating is likely to occur in areas of the die impression that project into the cavity. In addition, overheating may result from continuous production. If an internal die-cooling system that is adequate to prevent overheating cannot be provided economically, dies, or portions of dies, that are susceptible to overheating should be constructed of steels with high heat resistance.

Cold dies may break in a brittle manner; for this reason, preheating to 260 to 315 °C (500 to 600 °F) is recommended. Preheating may be accomplished by installing heating devices to maintain temperature during idle periods. Inadequate preheating of dies has often resulted in die failure.

Die Life

Die life depends on several factors, including die material and hardness, work metal composition, forging temperature, condition of the work metal at forging surfaces, type of equipment used, workpiece design, and a variety of other factors. Changing one factor almost always changes the influence of another, and the effects are not constant throughout the life of the die.

Die material and hardness have a great influence on die life. A die made of well-chosen material at the proper hardness can withstand the severe strains imposed by both high pressure and heavy shock loads, and can resist abrasive wear, cracking, and heat checking.

Work Metal. Each material being forged has a different resistance to plastic deformation and, therefore, a different abrasive action against the die surfaces. The resistance of hot steel to plastic deformation increases as the carbon or alloy content increases. Other factors being constant, the higher the carbon or alloy content of the steel being forged, the shorter the life expectancy of the forging die.

Of all the work metal factors influencing die life, the temperature of the metal being forged is one of the most difficult to analyze. The surface temperature of the metal as it leaves the furnace can be determined, but unless the proper heating technique has been used, ensuring that the temperature is the same throughout the cross section, the measured temperature will not be an accurate indication of metal temperature. In addition, the time used for performing all the operations involved in forging works against maintenance of the optimum forging temperature. The metal loses heat during transfer from the heating source to the forging machine. Cooling of the metal during forging is accompanied by an increase in its resistance to plastic deformation and, correspondingly, in its abrasiveness.

The life of the finisher impression can be increased by reheating the preform before finish forging. Even though the metal may be hot enough to forge satisfactorily without reheating, forging of cooled metal in the finisher impression may cause premature flash cooling and premature wear of the flash land.

When the temperature of the flash is reduced several hundred degrees and forging is continued, the cushioning effect that otherwise would be provided by freely flowing flash is either greatly reduced or lost completely. If the dies do not crack, they suffer a peening effect on the flash land, which may cause a bulge in the die impression.

Scale is a hard, abrasive substance formed by the combining of iron and atmospheric oxygen on the surface of heated steel, particularly at the high temperatures of hot forging. The amount of scale formed varies with the grade of steel, type of furnace, and the atmosphere, or air-to-fuel ratio, in which the metal is heated. Lifting the forging and blowing the scale away after every blow or every two blows in the hammer or press helps reduce die wear due to scale. Hydraulic descaling, scraping, or using a preforming impression in which the scale is broken reduces die wear.

Workpiece Design. The shape and design of the workpiece often have a greater influence on die life than any other factor. For instance, records in one plant showed that in hammer forging of simple, round parts (near minimum severity), using dies made of 6G tool steel at 341 to 375 HB, the life of five dies ranged from 6000 to 10,000 forgings. In contrast, with all conditions essentially the same except that the workpiece had a series of narrow fibs about 25 mm (1 in.) deep (near maximum severity), the life of five dies ranged from 1000 to 2000 forgings.

In thin sections of a forging, the metal cools relatively rapidly. Upon cooling, it becomes resistant to flow and causes greater wear on the die. Thin sections, therefore, should be forged in the shortest time possible.

Pads or surfaces on the forging designated as tooling points, or those used for locating purposes during machining, should be as far from the parting line as practicable to increase die life. Draft angles in the die cavity and, correspondingly, draft on the part increase as more forgings are made in the die. This is because wear on the die wall is greatest at the parting line, and least on the sidewall at the bottom of the cavity. Maximum wear near the parting line is caused by metal being forced to flow into the cavity and then along the flash land.

Deep, narrow depressions in a forging must be formed by high, thin sections in the die. The life of thin die sections usually is less than that of other die sections, because the thin sections may become upset after repeated use.

Workpiece tolerance also has an influence on die life. Its effect on die life can be demonstrated by assuming a constant amount of die wear for a given number of forgings, assigning different tolerances to a single hypothetical forging dimension, and then comparing the number of forgings that can be made before the tolerances are exceeded. For instance, if a dimension on a forging increased 0.025 mm (0.001 in.) during the production of 1000 forgings and the dimension had a total tolerance of 0.76 mm (0.030 in.), die life would be no greater than 30,000 forgings, assuming a uniform rate of die wear. If the tolerance on the dimension were reduced to 0.5 mm (0.020 in.), all other factors being the same, die life would be reduced to no more than 20,000 forgings.

In assuming a constant rate of die wear, this calculation does not give an accurate reflection of the relation between number of forgings made and amount of die wear. In particular, experience has shown that die wear is not constant during the forging of carbon and alloy steels. The first few hundred forgings cause more wear on the die than an intermediate group of a larger number of forgings. Near the end of the die life, a small number of forgings cause a large amount of die wear. The actual effect of a change in dimensional tolerance on die life therefore depends on the slope of the curve that shows the relationship of die wear to the number of forgings made.

Rapidity and Intensity of Blow. The best die life is obtained when the forging energy is applied rapidly, uniformly, and without excessive pressure. A single high-energy blow does not necessarily result in maximum die life: A blow that is too hard causes the metal to flow too fast and high pressures to develop on the die surfaces. Therefore, if all the energy needed to make a forging is applied in one blow, the dies may split. If the blows are softened, die wear due to pressure may decrease; on the other hand, the increase in number of blows will add to forging time, and the additional time the hot metal is in contact with the lower die can decrease die life. The amount of heat transferred to the dies also can be reduced by stroking the hammer or press as rapidly as practicable.

Computer Applications

Computer-aided design and manufacturing (CAD/CAM) techniques are being increasingly applied in forging technology. Use of the three-dimensional description of a machined part, which may have been computer designed, makes it possible to generate the geometry of the associated forging. For this purpose, it is best to use a CAD/CAM system with software for handling geometry, drafting, dimensioning, and numerical control (NC) machining. Thus, the forging sections can be obtained from a common database.

Using well-proved analyses based on the slab method or other techniques, the forging load and stresses can be obtained and flash dimensions can be selected for each section, permitting metal flow to be regarded as approximately two-dimensional (plane strain or axisymmetric). In some relatively simple section geometries, a computer simulation can be used to evaluate initial estimates on blocker or preform sections. Once the blocker and finisher sections are obtained to the designer's satisfaction, this geometric database can be used to write NC part programs and thereby obtain NC tapes or disks for cutting the forging die (or the die used for EDM of the forging die).

This CAD/CAM procedure is still developing. In the near future, this technology can be expected to evolve in two main directions: handling the geometry of complex forgings, for example, three-dimensional description, automatic drafting and sectioning, and NC machining; and use of design analysis, for example, calculation of stresses in the forging and stress concentrations in the dies, prediction of elastic deflections in the dies, metal flow analysis, and blocker/preform design.

More information on computer applications for forging design, die design, and process modeling is available in the Section "Computer-Aided Process Design for Bulk Forming" in this Volume.

Safety

Flying flash may be a result of faults in die design, including inadequate gutters, incorrect flash land, or incorrect flash clearance. It is a hazard in forging and requires the use of protective equipment. Flash guards on the die and protective clothing are needed to minimize the danger to the operator; movable shields placed in back of the hammer will protect the passerby. Although such devices help to provide protection should flying flash occur, the problem can best be met by careful die construction and, if necessary, by correction in the die.

A hazard in the production of dies for closed-die forging involves the practice of making lead casts (proofs) of die impressions to check die dimensions. Personnel handling the lead must take precautions against lead absorption. Aprons, face shields, goggles, and gloves should be worn. Workers should be trained in personal hygiene precautions specific to the use of lead. Dies should be dry when the molten lead is poured into them, to prevent the formation of steam and the accompanying expulsion of hot metal. Overheating of the lead pot can be avoided by close temperature control. An exhaust system should be installed over the lead pot, and skimmings kept in a container.

References containing information on die safety are included in the list of Selected References on safety at the end of the article "Hammers and Presses for Forging" in this Volume.

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Open-Die Forging

Revised by the ASM Committee on Open-Die Forging*; Chairman: Ashok K. Khare, National Forge Company

Introduction

OPEN-DIE FORGING, also referred to as hand, smith, hammer, and flat-die forging, can be distinguished from most other types of deformation processes in that it provides discontinuous material flow as opposed to continuous flow. Forgings are made by this process when:

- The forging is too large to be produced in closed dies
- The required mechanical properties of the worked metal that can be developed by open-die forging cannot be obtained by other deformation processes
- The quantity required is too small to justify the cost of closed dies
- The delivery date is too close to permit the fabrication of dies for closed-die forging

All forgeable metals can be forged in open dies.

Note

* R.L. Bodnar and E. Erman, Bethlehem Steel Corporation; N.M. Medei and R.R. Cappellini, Beth Forge--Bethlehem Steel Corporation; C.A. Johnson and D.J. Stuart, National Forge Company

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Size and Weight

The size of a forging that can be produced in open dies is limited only by the capacity of the equipment available for heating, handling, and forging. Items such as marine propeller shafts, which may be several meters in diameter and as long as 23 m (75 ft), are forged by open-die methods. Similarly, forgings no more than a few inches in maximum dimension are also produced in open dies. An open-die forging may weigh as little as a few kilograms or as much as 540 Mg (600 tons).

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Shapes

Highly skilled hammer and press operators, with the use of various auxiliary tools, can produce relatively complex shapes in open dies. However, the forging of complex shapes is time consuming and expensive, and such forgings are produced only under unusual circumstances. Generally, most open-die forgings can be grouped into four categories: cylindrical (shaft-type forgings symmetrical about the longitudinal axis), upset or pancake forgings, hollow (including mandrel and shell-type forgings), and contour-type forgings. Some examples of the various shapes generated are:

- Rounds, squares, rectangles, hexagons, and octagons forged from ingots, concast material, or billet stock (Example 1), in order to develop mechanical properties that are superior to those of rolled bars or to provide these shapes in compositions for which the shapes are not readily available as as-rolled products. These shapes are usually forged in lengths of 3 to 5 m (10 to 16 ft) and then sawed to obtain desired multiple lengths
- Hub forgings that have a small diameter adjacent to a large diameter (Example 2). Hub forgings are machined into gears, pulleys, and similar components of machinery
- Spindle, pinion gear, and rotor forgings (Examples 3 and 4). These forgings are for shaftlike parts and have their major or functional diameters either in the center or at one end, with one or more smaller diameters extending from one or both sides of the major diameter in shaftlike extensions
- Simple pancake forgings, made by upsetting a length of stock. Finished parts made from these forgings include gears, wheels, and milling cutter and tubesheet blanks
- Forged and pierced blanks, for subsequent conversion to rolled or saddle-forged rings (see Examples 5 and 6). When saddle forging is used to produce symmetrical forgings, the forging process includes expanding in the tangential direction by working on a loose-fitting mandrel bar
- Mandrel forgings to produce symmetrical, long, hollow forgings. The forging process includes expanding in the longitudinal (axial) direction by working on a tight-fitting mandrel (Example 7)
- Various basic shapes that are developed between open dies with the aid of loose tooling. Depending on the design of the tooling, these forgings may be of the open-die type, or they may be closed-die blocker-type forgings. Such forgings are discussed in the article "Dies and Die Materials for Hot Forging" in this Volume
- Contour forgings, such as turbine wheels and pressure vessel components with extruded nozzles and bottleneck-shaped forgings (see the section "Contour Forging" in this article)

Hammers and Presses

Because the length of the hammer ram stroke and the magnitude of the force must be controllable over a wide range throughout the forging cycle, gravity-drop hammers and most mechanical presses are not suitable for open-die forging. Power forging hammers (air or steam driven) and hydraulic presses are most commonly used for the production of open-die forgings that weigh up to 4.5 Mg (5 tons). Larger forgings are usually made in hydraulic presses. Further information on hammers and presses is available in the article "Hammers and Presses for Forging" in this Volume.

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Dies

Most open-die forgings are produced in a pair of flat dies--one attached to the hammer or to the press ram, and the other to the anvil. Swage dies (curved), V-dies, V-die and flat-die combinations, FM (free from Mannesmann Effect) dies and FML (free from Mannesmann Effect with low load) dies are also used. The Mannesmann Effect refers to a tensile stress state as a result of compressive stresses in a perpendicular orientation. These die sets are shown in Fig. 1. In some applications, forging is done with a combination of a flat die and a swage die. The dies are attached to platens and rams by either of the methods shown in Fig. 1(a) and (b). Figure 1 also shows several types of dies that are held on the anvil manually by means of handles similar to those on the cutting and fullering bars shown in Fig. 4. Information on die materials, die parallelism, and die life for open-die forging is presented in the article "Dies and Die Materials for Hot Forging" in this Volume.

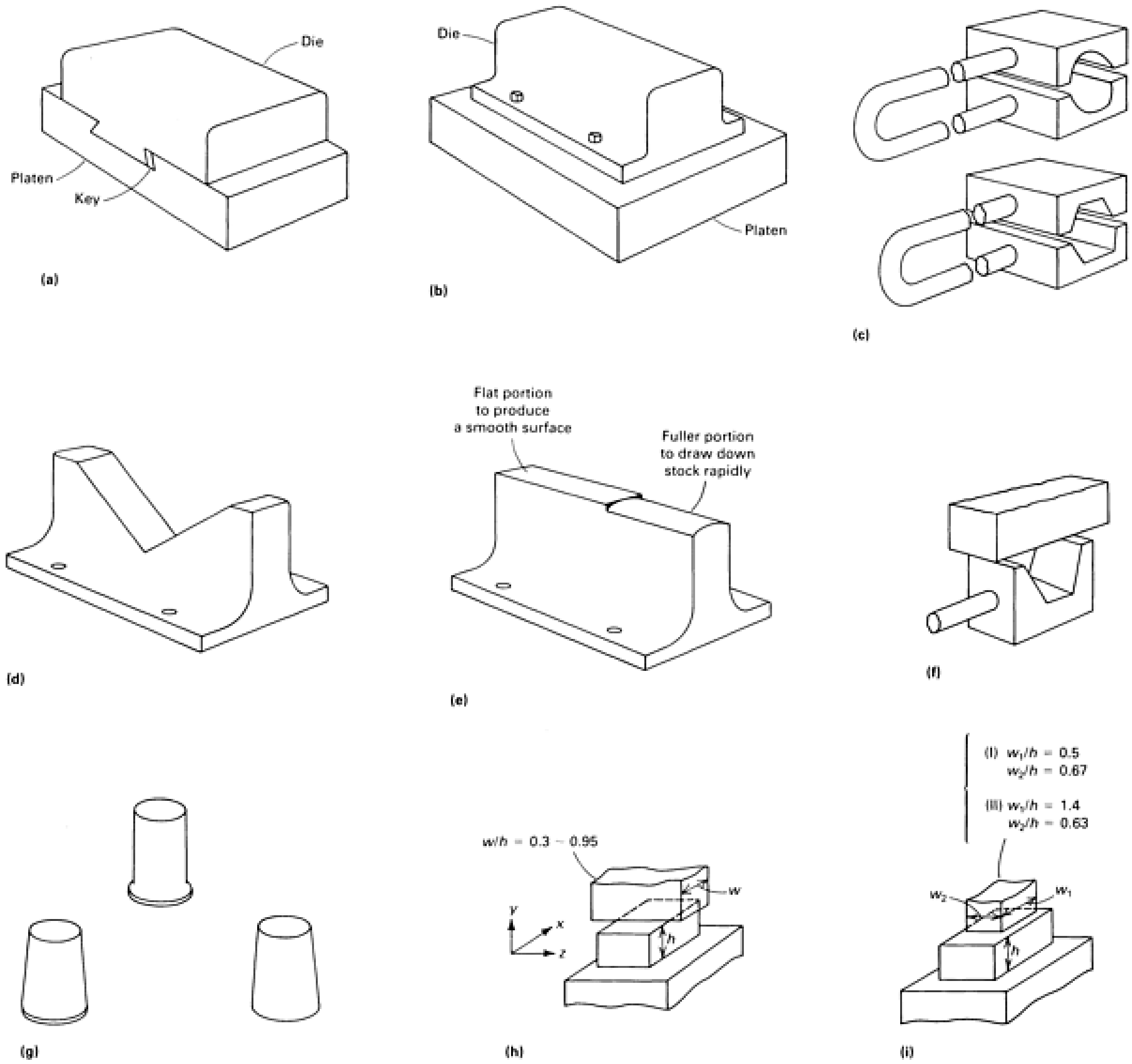

Fig. 1 Typical dies and punches used in open-die forging. (a) Die mounted with dovetail and key. (b) Flange-mounted die. (c) Swages for producing smooth round and hexagonal bars. (d) V-die. (e) Combination die (bar die). (f) Single loose die with flat top for producing hexagonal bars. (g) Three styles of hole-punching tools. (h) FM process. (i) FML process

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Auxiliary Tools

Mandrels, saddle supports, sizing blocks (spacers), ring tools, bolsters, fullers, punches, drifts (expansion tools), and a wide variety of special tools (for producing shapes) are used as auxiliary tools in forging production. Because most auxiliary tools are exposed to heat, they are usually made from the same steels as the dies.

Saddle Supports. An open-die forging can be made with an upper die that is flat, while the lower die utilizes another type of tool. Two or more hammers or presses and die setups are often needed to complete a shape (or operations are done at different times in the same hammer or press by changing the tooling). For example, large rings are made by upsetting the stock between two flat dies, punching out the center, and then saddle forging (Examples 5 and 6). As shown in Fig. 2, the lower die is replaced by a saddle arrangement that supports a mandrel inserted through the hollow workpiece.

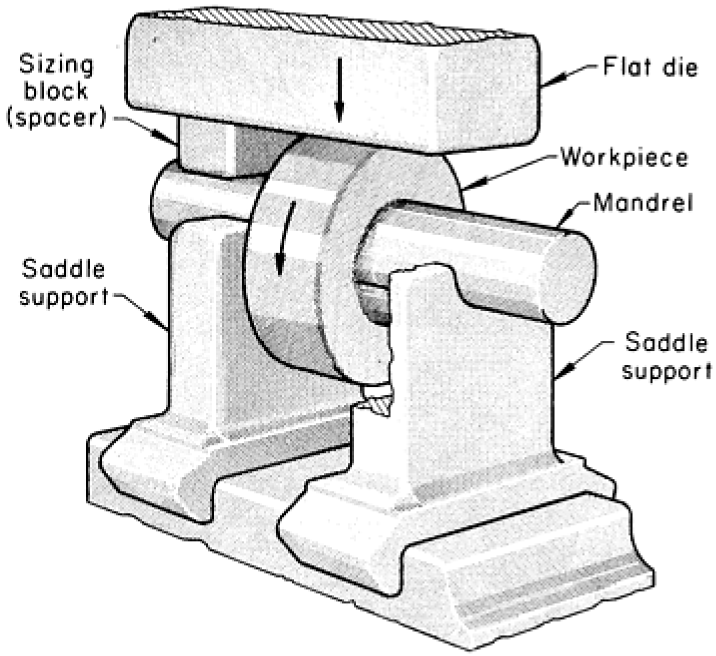

Fig. 2 Setup for saddle forging a ring

Sizing Blocks. A sizing block can be used between the mandrel and the ram to prevent the cross section of the workpiece from being forged too thin. Most state-of-the-art presses have automatic sizing or thickness controls.

Bolsters. The open-die forging of hubs requires a bolster (Example 2). Hub forgings are forged to the shape shown in Fig. 13, Operation 2. A bolster is then placed on the lower die, the smaller diameter of the workpiece is inserted into the bolster, and the larger diameter is upset. Depending on the size and shape of the workpiece, it may be necessary to remove the lower die and to use the anvil to support the bolster.

Ring Tools. A tonghold can be retained on a forging so that the forging can be more easily handled after upsetting, as shown in Fig. 3. A ring tool with a center opening is placed on the workpiece. During the upsetting, the hot work metal at the ring tool opening is protected from being upset, and it is back extruded to a tonghold with a length equal to the thickness of the ring tool. Alternatively, the tonghold can be forged on one end of the workpiece prior to upsetting; a hole in the lower die protects the tonghold during the upsetting operation.

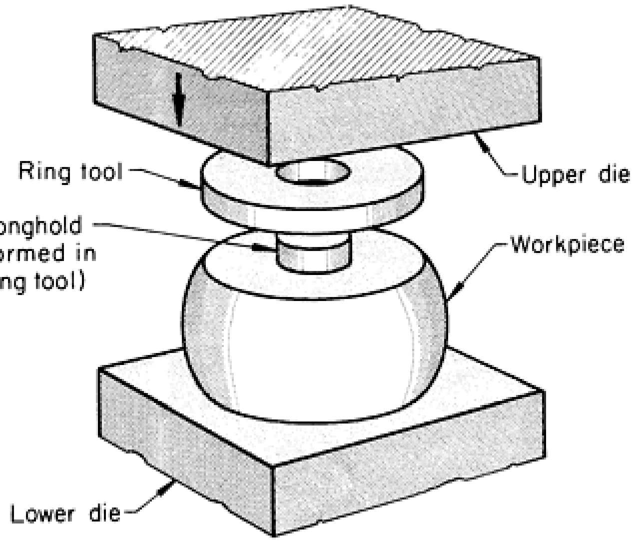

Fig. 3 Setup showing use of a ring tool for forming and retaining a tonghold in the workpiece during upsetting

Fullers are required for starting stepped-down diameters on workpieces such as spindle forgings. They are often used in pairs (see Example 3). Figure 4 illustrates some of the commonly used cutting and fullering bars.

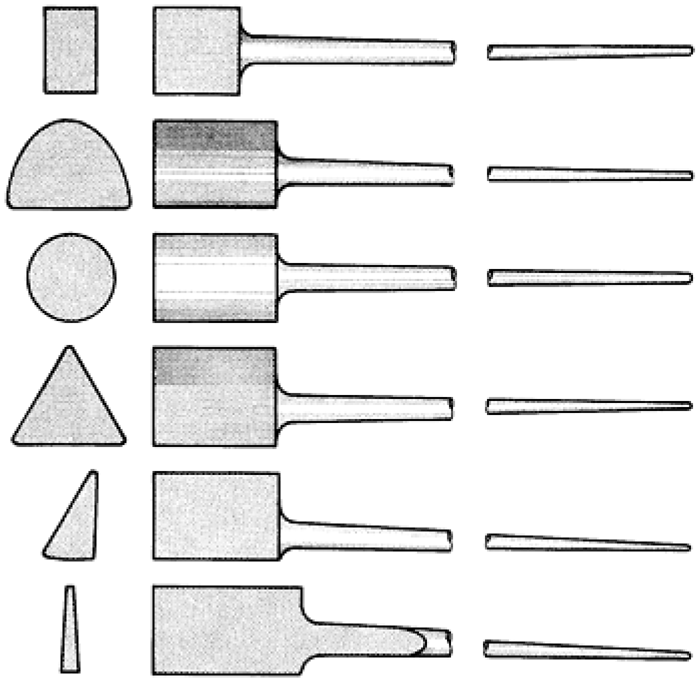

Fig. 4 Cutting and fullering bars

Mandrels are used to produce long, symmetrical, hollow forgings. The workpiece is elongated in the longitudinal (axial) direction while positioned on the mandrel and is worked between the top flat die and bottom V-die combination (Example 7). The mandrel has a slight taper on the outside diameter in order to facilitate removal of the finished hollow forging. In addition, a 25 to 50 mm (1 to 2 in.) hole in the center helps to provide water cooling of the mandrel inside diameter in order to avoid the hot forge welding of the workpiece onto the mandrel. The length and outside diameter of the mandrel bar is governed by the inside diameter and the length of the hollow forging.

Punches. To make holes, punches are placed on the hot workpiece and are driven through, or partly through, by a ram. A hole can also be made by punching from both sides (Example 5). Relatively deep holes can be produced by punching from both sides until only a thin center section remains.

Hot trepanning is done to produce a hole through the center of a large cross section, large-mass workpiece. A circular cutter having an outside diameter of the same size as the desired hole and measuring about 25 mm (1 in.) in wall thickness and about 203 mm (8 in.) in height is initially positioned and pushed into the hot workpiece by the top die while the workpiece is sitting on a lower die with a hole in it. The hot-trepanning operation is continued by pushing the followers through the workpiece.

These followers have the same inside diameter as the cutter, but a slightly smaller outside diameter (~13 mm, or $\frac{1}{2}$ in. smaller). The followers are locked into position prior to being pushed into the hot workpiece. The length of the followers varies and is based on the length of hot trepanning desired. This hot-trepanning length could be made up by using one or more multiple followers.

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Handling Equipment

The handling of workpieces is often more difficult in open-die forging than in closed-die forging. Usually, the workpieces are heavier, and they must be repositioned many times during the forging cycle.

In practice, small forgings weighing up to about 45 kg (100 lb) are handled with tongs by the forging crew, or a small floor manipulator can be used. Larger forgings weighing up to about 910 kg (2000 lb) are usually handled by floor manipulators and, less frequently, by special tongs or porter bars. Forgings weighing more than 910 kg (2000 lb) are handled by large mobile manipulators, by manipulators on tracks, or by porter bars in conjunction with overhead cranes. Ingots that are forged into bars or billets are usually handled by a balancing porter bar and an overhead crane.

Electric overhead traveling cranes with special lifting devices are used to transport billets and semifinished forgings to and from the heating furnaces and to and from the forging machines. At the forging machine, several different types of equipment are available for moving the workpiece. One is an electric crane that carries a turning gear suspended from the main hoist. The turning gear consists of a frame carrying a drum that can be rotated by an electric motor through gearing. An endless chain, called a sling, constructed of flat links and pins, passes over the drum and moves with it. This device is also called a rotator.

Porter Bars. Another handling device is the porter bar. It has a hollow end that is shaped to fit the sinkhead of the billet being forged or some portion of the workpiece. The load, represented by the workpiece and porter bar, is balanced on the sling at the center of gravity of the combined load. The sling is occasionally moved to preserve the balance as the dimensions of the forging change. Figure 5 shows a porter bar and a sling used for handling a large forging.

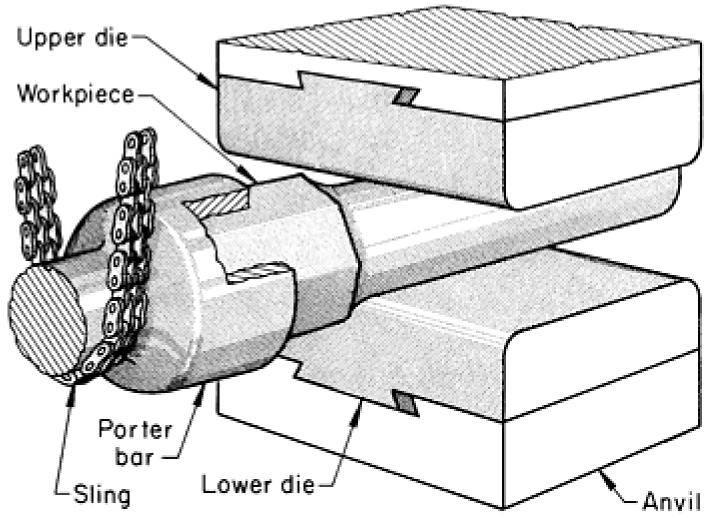

Fig. 5 Handling a forging by means of a porter bar and a sling.

Manipulators. Faster and more accurate handling of hot workpieces is accomplished by manipulators. These machines are equipped with powerful tongs at the end of a horizontal arm that can be moved from side to side, raised or lowered, tilted, and rotated about its longitudinal axis. Large manipulators travel on tracks (track-bound) between the furnace and the forging hammer or press, and they can handle workpieces weighing up to 68 Mg (75 tons). Small manipulators move on rubber-tired wheels. State-of-the-art manipulators include both manned and unmanned operations. Unmanned operations are frequently controlled by the press operator and incorporate programmable positioning and manipulating sequences.

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Production and Practice

Stock for smaller open-die forgings is usually prepared by cold sawing to a length that is computed to contain the required weight and volume of material. Allowance is made for dimensional variations in the cross section of the billet stock. Stock is sometimes sheared to length, but the upper limit that can be sheared is about 152 mm (6 in.) square or round. Large open-die forgings are commonly forged from ingots. Large ingots are sometimes used to produce two or more forgings in which the individual forgings are parted by cutting (cold or hot), burning, or machining. When ingots are used, an additional weight allowance is usually provided for the removal of end defects, such as shrinkage, porosity, and pipe.

Blocking and Upsetting. The first step in the forging process usually consists of elongating the ingot along its longitudinal axis. This process has been referred to as blocking, cogging, solid forging, elongation forging, or drawing out. However, some forging ingots--particularly small electroslag remelted and vacuum arc remelted ingots, which are usually free from solidification porosity--are direct upset forged. Upsetting is a hot-working process done with the ingot axis in a vertical position under the press. This operation decreases the axial length of the ingot and increases its cross section. As discussed later in this article, both blocking and upsetting are sometimes used to produce certain forging shapes.

Heating practice for the forging stock is the same in open-die and closed-die forging (see the article "Closed-Die Forging in Hammers and Presses" in this Volume). Large ingots, blooms, or billets of alloy steels such as AISI 4340 should be heated carefully in order to minimize decarburization and to avoid cracking due to rapid heating. Preheating can be used to minimize cracking.

Die temperature is usually less critical in open-die than in closed-die forging. Flat dies are usually not preheated (forgings composed of aluminum and nonferrous alloys are the exception). Swage or V-dies, if they have become completely cold (as from a weekend shutdown), are sometimes warmed, particularly for hammer operations. Die heating or warming can be accomplished by closing the dies on slabs of heated steel (warmers). Any cooling of the open dies is incidental and results from the compressed air or high-pressure water spray used in descaling the forging in process or from the ambient temperature of the forge shop.

Lubrication is usually not required for open-die forging except in those loose tooling applications in which metal flow is problematic. Lubrication is sometimes used for the upsetting operation in order to eliminate the dead zone (undeformed material) directly under the dies. This is especially critical for materials that cannot be refined through phase transformation, such as austenitic stainless steels, aluminum alloys, and nickel-base alloys. Lubrication is also used in mandrel forging and in contour forming to improve metal flow (such as for nozzle extrusion and certain pressure vessel components that are contour formed).

Descaling of the workpiece is done by busting and blowoff, as in some closed-die operations (see the article "Closed-Die Forging in Hammer and Presses" in this Volume). Best practice includes the use of compressed air to blow away the scale as it breaks off. High-pressure water is also sometimes used to loosen scale, especially at hard-to-reach locations, such as the inside diameter of a mandrel forging. Failure to remove the scale causes it to be forged in, resulting in pits and pockets on the forged surfaces. The total amount of scale formed in open-die forging is usually greater than in closed-die forging because the hot metal is exposed to the atmosphere for a longer time; that is, open-die forgings usually require more forging strokes and sometimes require reheating. Metal loss through scaling usually ranges from 3 to 5%. For certain types of forgings, such as back extrusions, the descaling time is critical in terms of forgeability because the temperature of the forging can drop dramatically during prolonged descaling, resulting in a loss in forgeability.

Hammer/Press Practice. Unlike closed-die forging, in which the metal in the entire forging is worked at the same time, open-die forging involves the working of only a portion of the forging. Therefore, a given hammer or press can produce open-die forgings of greater weight and size than a hammer or press of equivalent rating in closed-die work, but at a lower production rate.

Hammer and press practice vary considerably from one open-die shop to another. For example, in one shop, a hammer may make three times as many blows per hour as a similar hammer in another shop, yet each shop may be using the equipment efficiently in terms of the nature of the work, the capacity of the furnaces and other equipment, and the size of the crew. In addition, different shops may make the same shape in different steps. For instance, in Example 5, a square billet was pancaked, shingled to an octagonal shape, and then rounded. Another shop might make this disk by breaking the corners of the square billet to obtain an octagonal shape, which would then be pancaked to a disk.

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Ingot Structure and Its Elimination

Ingots are extensively used as forging stock in the open-die forging of large components, such as the turbine rotor described in Example 4. Whenever ingots are used, it is desirable (and often mandatory) to adopt a forging procedure that will remove the cast structure (ingotism) in the finished forging. Figure 6 shows a schematic cross section of a large ferrous forging ingot. Because of the large diameter of heavy forging ingots (up to 4.1 m, or 160 in.), the solidification process is extremely slow, often taking as long as 2 to 3 days. Unfortunately, the slow cooling rate causes considerable macrosegregation, especially in the ingot center toward the top of the ingot. Consequently, the center of the ingot must be mechanically worked during the forging operation to redistribute the segregated elements and to heal internal porosity (Ref 2).

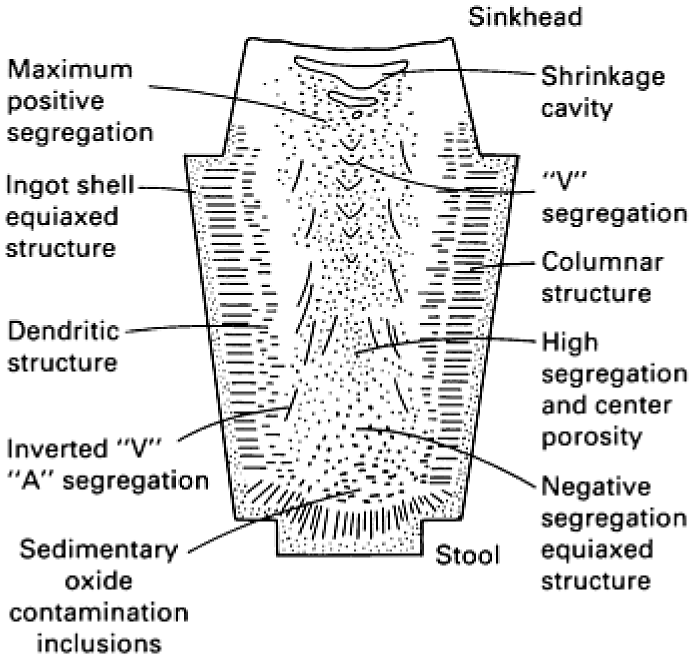

Fig. 6 Schematic illustrating macrosegregation in a large steel ingot. Source: Ref 1.

The segregated regions are usually associated with a coarse dendritic structure; therefore, breaking up these regions by using hot deformation leads to refined microstructures. Compression of the dendritic arms reduces the local diffusion distance, which can enhance homogenization during subsequent heat treatment. Repeated hot deformation also causes grain refinement through static and/or dynamic recrystallization of the austenite. Finer austenitic grain sizes promote finer microstructures during subsequent transformation to ferrite, pearlite, and bainite or martensite or both. Finer microstructures lead to more uniform mechanical properties and, in general, improved tensile properties coupled with greater toughness. However, nonuniform hot deformation can lead to undesirable duplex microstructures, that is, mixed fine and coarse grain size/transformation products. Segregated regions containing higher alloy concentrations can also lead to nonuniform recrystallization and grain growth.

Various approaches are available for minimizing the undesirable effects of segregation. In some forgings, the centerline is actually removed from the finished product in the form of a core bar by machine trepanning. This is permissible for some symmetrical rotating machinery; however, many forgings are not symmetrical, and the center region cannot be removed. In these cases, the thermal and thermomechanical treatments must be optimized in order to redistribute the solute elements. Long homogenization treatments at temperatures approaching 1290 °C (2350 °F) are frequently conducted to allow some diffusion of alloying elements. However, redistribution (homogenization) of the substitutional solid-solution elements, such as manganese, silicon, nickel, chromium, molybdenum, and vanadium, would require several weeks at temperature, which is far too long to be economically feasible. The other alternative is to put as much hot work as possible into the segregated regions.

Hot deformation in the center of the ingot is enhanced when there is a temperature gradient from the surface to the center of the ingot (Ref 3, 4, 5). Under certain circumstances in production, ingots are deliberately air cooled from the soaking temperature before forging. The cooler surface regions, having a higher flow stress, translate the forces of the draft (percentage of reduction) to the center of the ingot, thus increasing centerline consolidation.

Transformation of the initial cast structure into a fully wrought structure requires extensive hot working in the form of successive reduction of cross section, enlargement of cross section by upsetting, and an additional reduction of cross section. Therefore, in Example 4, the principal section of the rotor forging was enlarged by upsetting in Operation 3, Position 1, and was then reduced by almost 30% in Operation 3, Position 2. This seemingly circuitous procedure helps to break up the cast structure and to eliminate ingotism throughout the section.

The development of substantial deformation at the center of the ingot, bloom, or billet to break up the cast structure and to heal any porosity depends on the press capacity and on the relationship between die width and stock height (*w*/*h*). If the press capacity is small and if die width is narrow, the penetration, or depth of deformation, will be small. The width of the draw-out dies should be at least 60% of the stock height in order to ensure adequate centerline deformation (Ref 6). The die width and depth of penetration (percentage of the reduction, or draft size) have a significant influence on the size of the press used for open-die forging. Although billets cut from wrought bars are normally free of ingotism, they can be given additional hot working (more than the minimum required to develop contour) in order to refine the structure and to impose a more desirable flow pattern than that inherent in the original billet or in the wrought product.

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Open-Die Forging

Revised by the ASM Committee on Open-Die Forging*; Chairman: Ashok K. Khare, National Forge Company

Forgeability

Metals and alloys vary in forgeability from highly forgeable to relatively brittle. Relative forgeability is indicated below for metals and alloys used in open-die forging:

Most forgeable
Aluminum alloys
Magnesium alloys
Copper alloys
Carbon and low-alloy steels
Martensitic stainless steels
Maraging steels

Austenitic stainless steels
Nickel alloys
Semiaustenitic PH stainless steels
Titanium alloys
Iron-base superalloys
Cobalt-base superalloys
Niobium alloys
Tantalum alloys
Molybdenum alloys
Nickel-base superalloys
Tungsten alloys
Beryllium alloys
Least forgeable

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Deformation Modeling

The ability to predict material flow, energy requirements, and forming loads is very helpful in facilitating design or operations in open-die forging. The maximum force developed in forging will determine the size of the hammer or press required and will set the limits for the elastic distortion permissible for the forging equipment to be used. The energy requirement will determine whether a given forging can be made on an available hammer or press. The design of a forging practice for an open-die forging involves the selection of certain parameters to be used, such as die dimensions and shapes, amount of reduction, ingot shape, temperature gradient, ram velocity, and pass sequence. The development of forging practices through full-scale production trials is expensive and time consuming. In addition, only minimal internal strain data can be collected. Therefore, both mathematical and physical modeling are applied to provide design criteria and to gain a better understanding of open-die forging operations.

Mathematical Modeling. The forging process can be understood with the aid of a series of theoretical approaches in the field of metalworking. Elementary plasticity theory (Ref 7, 8) is used to provide a series of relationships that can yield an estimation of the force and energy requirements for such forging operations as upsetting and blocking. If the correct coefficient of friction can be selected, such relationships permit an accurate estimation of the force and energy requirements (Ref 9).

Slip-line theory is used to obtain deformation information relating to localized stress states. This permits precise statements to be made concerning stress states in the center of the forged ingots (Ref 10). The disadvantage of this theoretical method lies in its assumption that the metal used in hot forging behaves as an ideal rigid-plastic material, which is usually not the case. Therefore, this technique is incapable of describing such an effect as the influence of bite displacement on stress state. On the other hand, the upper bound method seeks to compensate for the lack of information on the actual material flow by assuming a velocity field and by optimizing the performance without stress consideration (Ref 11, 12). The disadvantage of this method is that the assumed velocity field becomes extremely complex if all of the kinematic parameters are to be satisfied.

Because precise knowledge of the stress and deformation history of a workpiece is necessary to determine its real formability during forging, the computational procedure of the finite-element method appears to have the best prospects for simulating forging processes. The use of the finite-element method as a numerical analysis tool has dominated this field and remains the most popular method for deformation modeling. In two dimensions, a variety of problems can be explained and simulated, such as the progress of centerline penetration or comparisons between two forging processes (Ref 13), the design of upsetting and ring compression tests (Ref 14, 15, 16, 17), and the influence of selected forging parameters on the final quality of the forge products (Ref 18, 19).

In general, the theoretical methods used to predict forces and other performance variables are based on certain assumptions (ideal conditions) that deviate to some degree from the actual forging process. In addition, their reliability and effectiveness are strictly dependent on how smoothly a forging process proceeds. However, as soon as the workpiece is of any complexity (that is, any deviation from the ideal), this method fails. Therefore, calculated values are usually considerably higher or (depending on the conditions and forging process) lower than the measured values. One reason for this discrepancy is related to the temperature gradients developed during forging. In addition, strain rates vary during various parts of the forging stroke, and it is difficult to choose a true representative strain rate and corresponding yield stress at the estimated average temperature. For all of these reasons, calculation of the force and energy requirements on a theoretical basis is still in its infancy.

Both private and government-sponsored research efforts are making progress toward the goal of providing modeling techniques that are useful to the open-die forging industry. In addition, heuristic or artificial-intelligence expert systems are being developed to apply new open-die technology processes and designs. More detailed information can be found in the Section "Computer-Aided Process Design for Bulk Forming" in this Volume.

Physical Modeling. Because of the above disadvantages associated with the use of theoretical modeling methods, physical modeling is often employed. Physical modeling can often provide deformation information that would otherwise be inaccessible or too expensive to obtain by other techniques; this makes physical modeling a powerful tool for the study of forging practices. As its name implies, physical modeling involves changing some physical aspect of the process being studied, such as the size or the material being deformed. In doing so, however, some properties of the original material or the process or both are sacrificed in order to bring the relevant properties more clearly into focus. Nonetheless, if the modeling material employed is homogeneous, isotropic, and obeys the laws of similitude and if the boundary conditions, especially friction and tool geometry, are met in the physical modeling experiment, then excellent qualitative and sometimes quantitative results can be achieved (Ref 20).

Among the various metallic (steel, aluminum, and lead) and nonmetallic (wax and plasticine) modeling materials, plasticine, a particular type of modeling clay, is probably the most widely used for studying open-die press processes (Ref 21, 22, 23, 24, 25, 26, 27, 28, 29). There are several advantages to using plasticine as a modeling material. First, plasticine is readily available, inexpensive, and nontoxic. Second, plasticine deforms under low forces at room temperature, thus considerably simplifying the experimentation and allowing the use of low-cost tooling and equipment. Third, two-color models are feasible for studying internal material flow. Fourth, plasticine exhibits dynamic deformation properties that are similar to those of steel at high temperature. Lastly, plasticine is able to provide quantitative information with respect to the deformation distribution by means of specially designed layered specimens.

Physical modeling with plasticine and lead is extensively used to develop processes for new products and to improve existing manufacturing techniques for better economical processes in various types of open-die forgings. In blocking, such parameters as die width, die configuration, die overlapping, die staggering ingot shape, temperature gradient, and draft design can be optimized to maximize the internal deformation for better structural homogeneity and soundness of material in the core of the ingot (Ref 26, 27). Figures 7 and 8 show the effects of temperature gradient and draft design, respectively, on the centerline deformation distribution for square cross-sectional ingots subjected to multiple-stroke blocking (Ref 27).

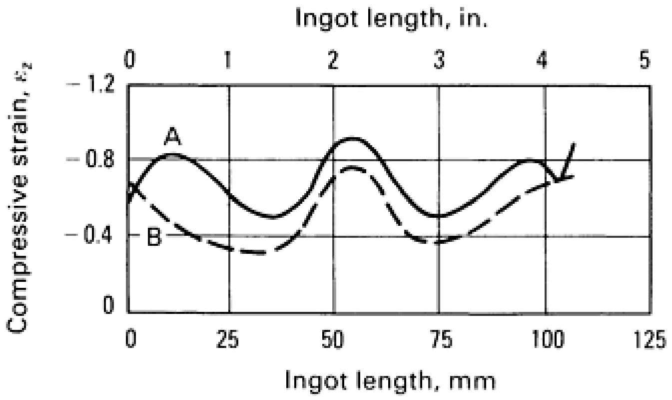

Fig. 7 Effect of temperature gradient using scaled 2.79 × 2.79 × 3.86 m (110 × 110 × 152 in.) ingots, 1.52 × 1.83 m (60 × 72 in.) flat conventional dies, and a 24% reduction. A, with temperature gradient; B, without temperature gradient

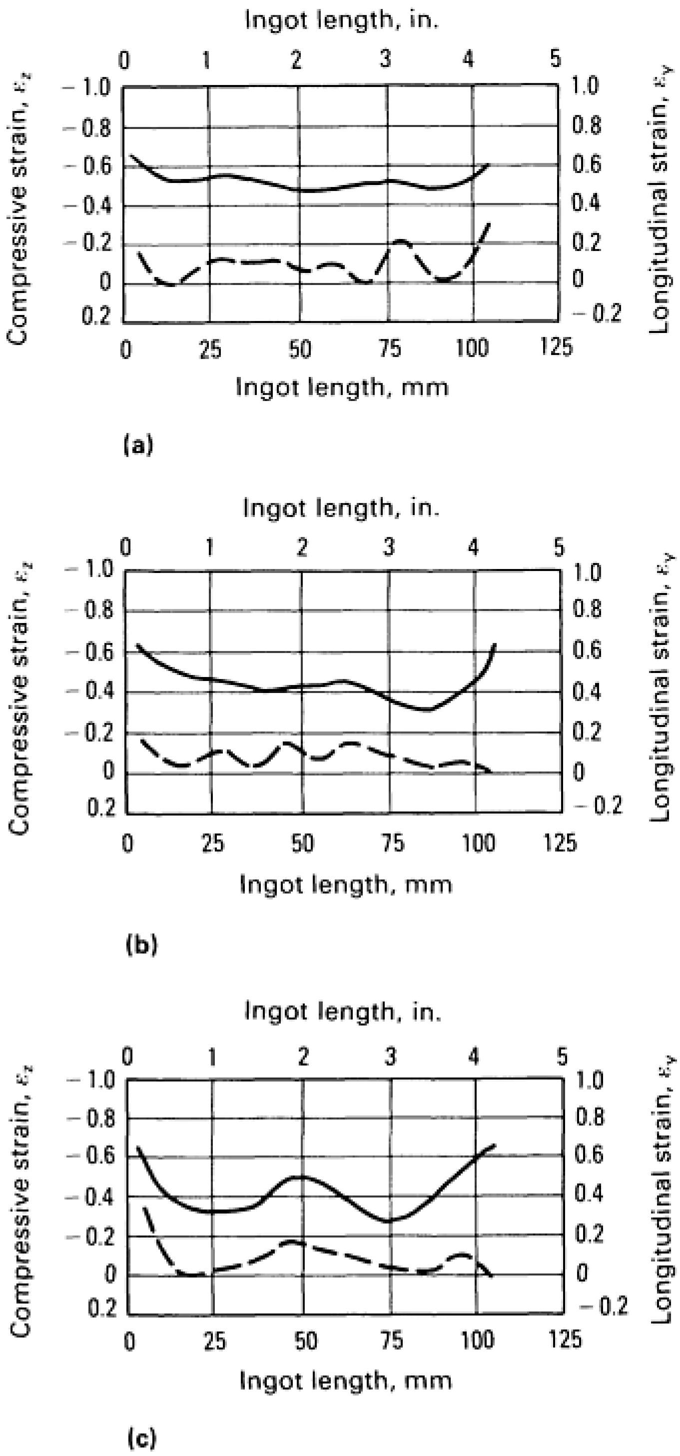

Fig. 8 Effect of draft design on the compressive strain distribution. Solid line indicates compressive strain; broken line, longitudinal strain. (a) 5% reduction increments. (b) 8% reduction increments. (c) 10% reduction increments

In upsetting, the influence of selected parameters such as aspect ratio, crosshead speed, ingot chuck, spreading, indenting, and dished dies versus upsetting dies on the internal deformation distribution can be effectively studied through physical modeling (Ref 28). Figure 9 shows the influence of various aspect ratios on the compressive strain distribution from the top to the bottom of the upset-forged ingot (Ref 28). The influence of these blocking and upsetting parameters on void closure can be determined by providing artificial holes inside plasticine or lead ingots (Ref 29, 30).

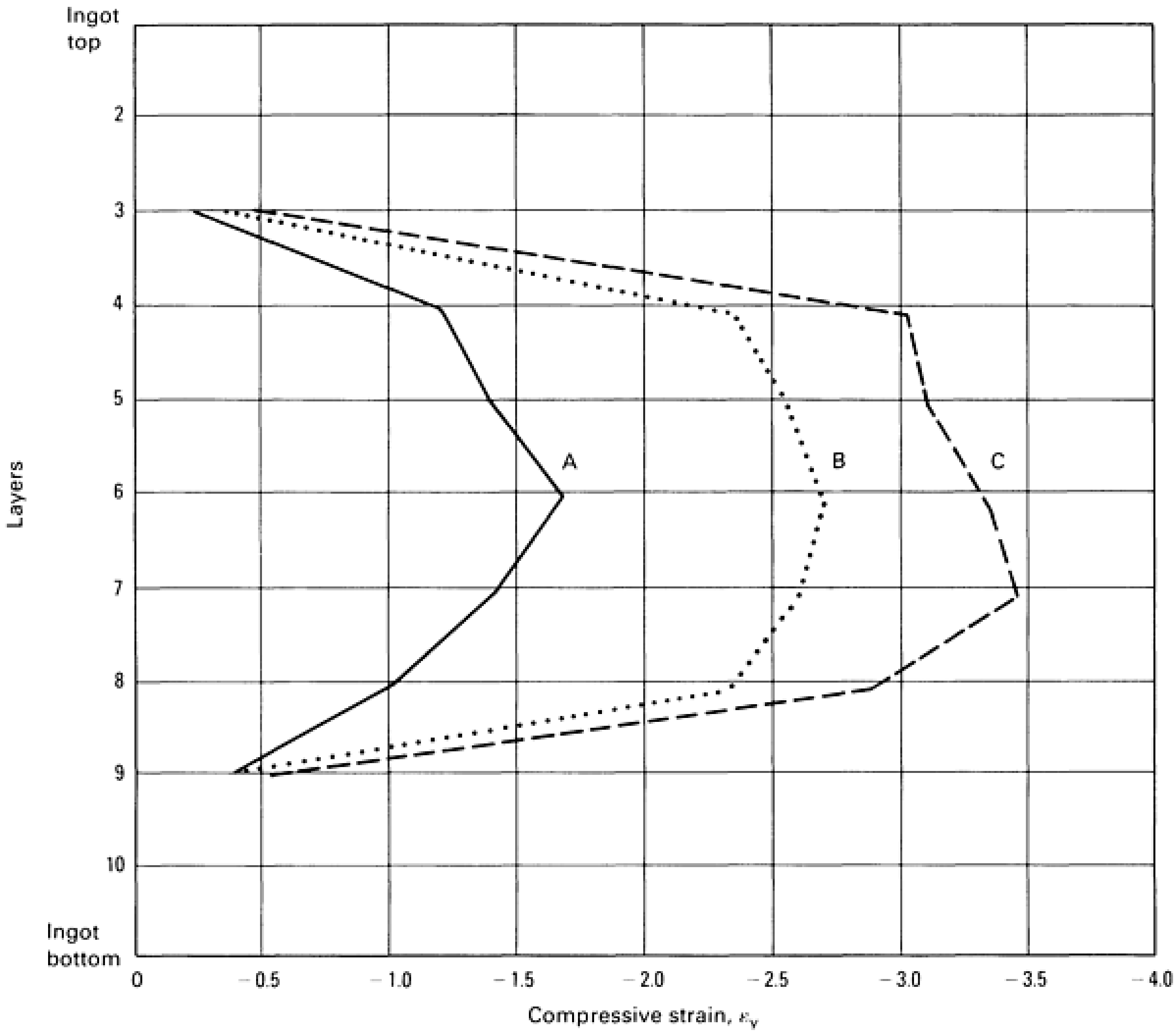

Fig. 9 Effect of aspect ratio (H/D) on compressive strain distribution in plasticine ingots. A, 1.0 ratio; B, 1.5 ratio; C, 2.0 ratio

The application of physical modeling to forged products has led to improvements in yield and quality and cost savings. Additional information is available in the Section "Computer-Aided Process Design for Bulk Forming" in this Volume.

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Open-Die Forging

Revised by the ASM Committee on Open-Die Forging*; Chairman: Ashok K. Khare, National Forge Company

Examples of Production Practice

Because of differences in equipment and operator skill, procedures for open-die forging vary considerably from plant to plant. Figure 10 shows typical steps in the drawing and forging of stock and in the fabrication of common shapes from billets of square, rectangular, and round cross sections. The procedures described in the following examples are typical of those used for the production of some common open-die forgings.

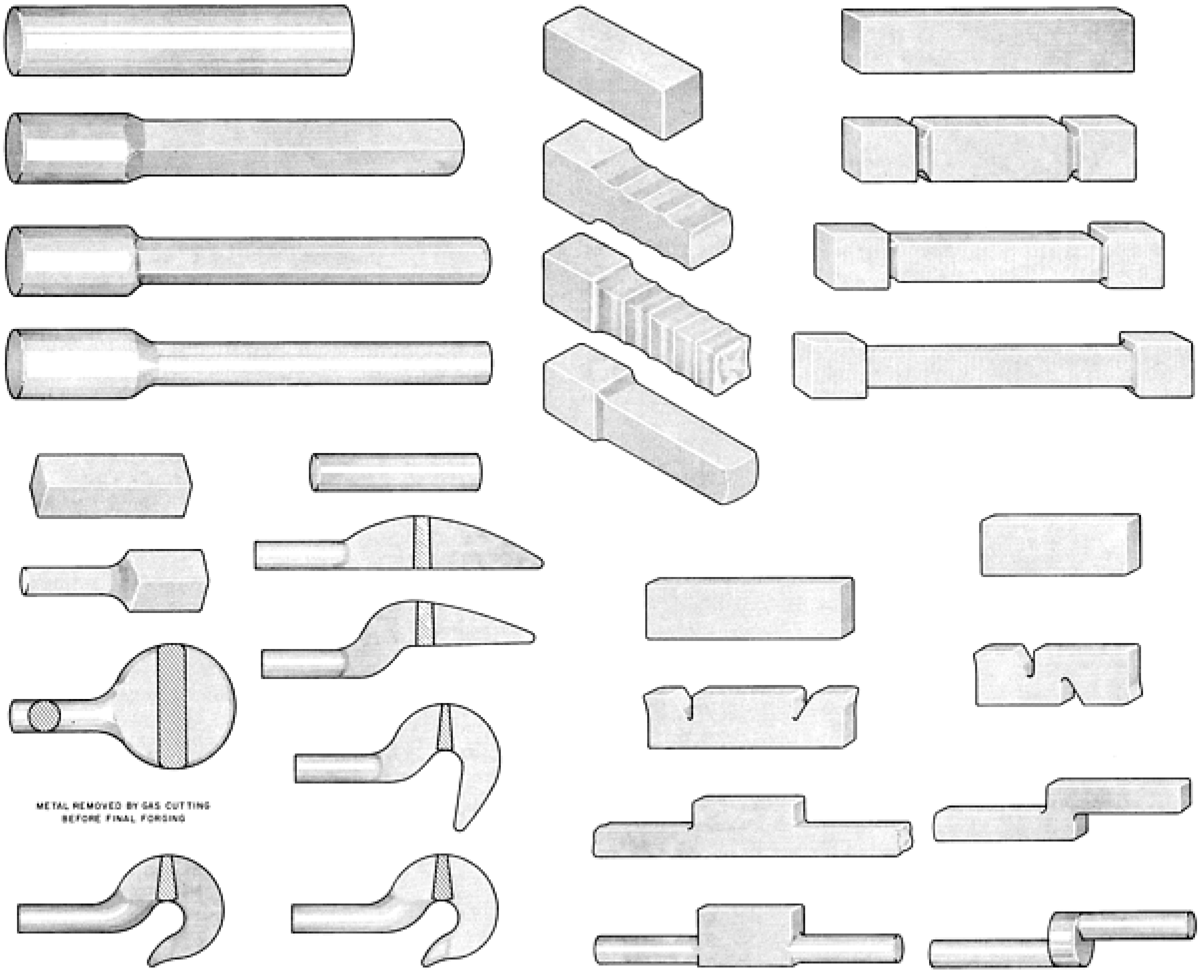
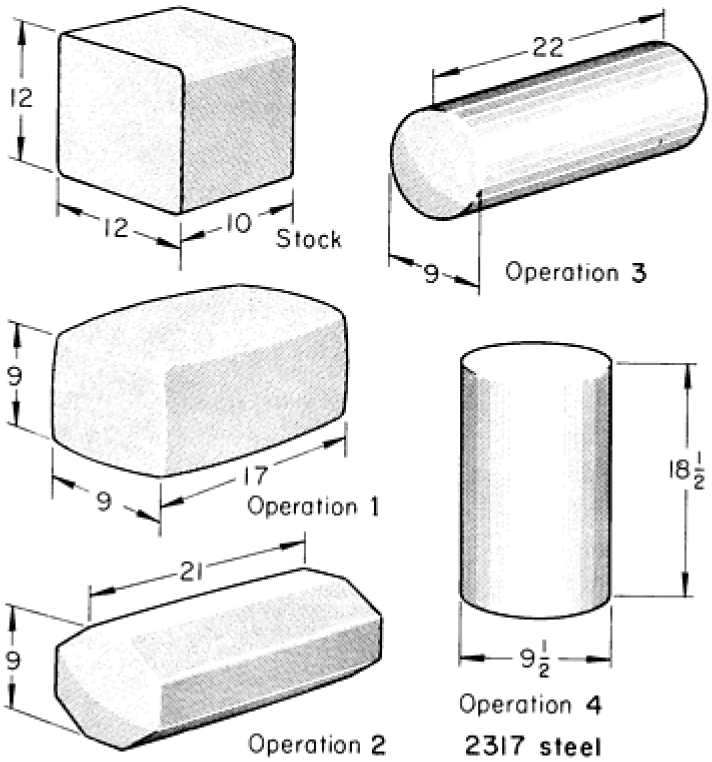


Fig. 10 Typical steps in drawing out forging stock and in producing common shapes in open dies

Example 1: Forging a 170-kg (375-lb) Solid Cylinder in Flat Dies.

A cylinder, 241 mm ($9\frac{1}{2}$ in.) in diameter by 470 mm ($18\frac{1}{2}$ in.) in length, was forged in flat dies from 305 × 305 × 254 mm (12 × 12 × 10 in.) stock in four operations without reheating the billet (Fig. 11). The following sequence of operations was used.



Stock preparation	**Cold sawing**
Stock size	**305 × 305 × 254 mm (12 × 12 × 10 in.)**
Stock weight	**179 kg (395 lb)**
Finished weight	**170 kg (375 lb)**
Heating furnace	**Gas-fired, automatic temperature control**
Heating temperature	**1230 °C (2250 °F)[(a)]**
Forging machine	**18 kN (4000 lb) steam hammer**

(a) Forging was completed in one heat.

Fig. 11 Sequence of operations in the forging of a cylindrical workpiece from square stock. Dimensions in figure given in inches

Operation 1. The 305 mm (12 in.) square section was hammered to a 229 mm (9 in.) square section, which increased the length to 432 mm (17 in.).

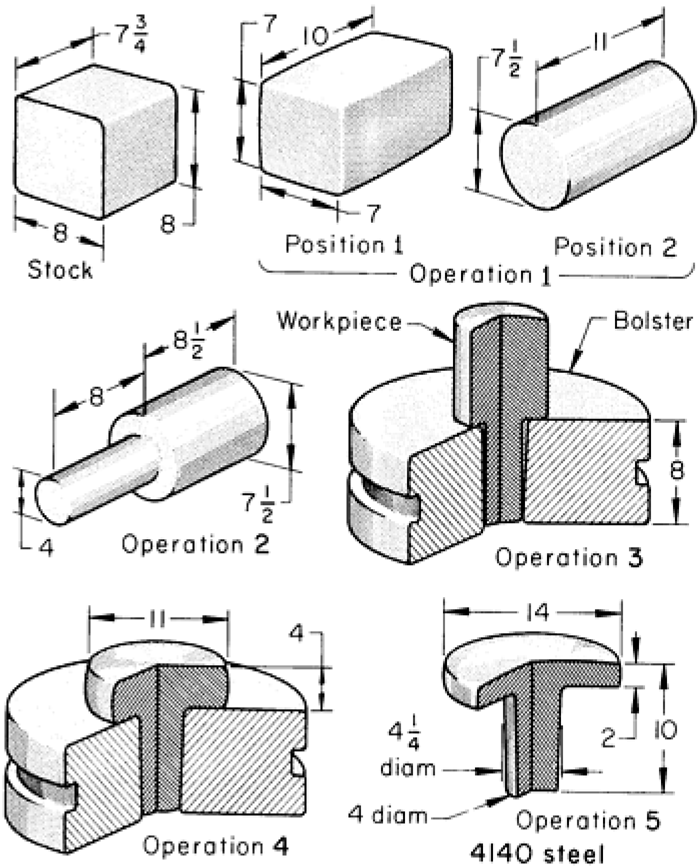
Operation 2. The corners of the square were hammered to produce an octagonal shape approximately 229 mm (9 in.) across flats and 533 mm (21 in.) long.

Operation 3. The octagon was rounded by successive hammer blows as the workpiece was rotated. The cylindrical forging was then approximately 559 mm (22 in.) long.

Operation 4. The forging was upended and hammered lightly on both ends to flatten the bulge on the ends. This decreased the length to 470 mm ($18\frac{1}{2}$ in.) and increased the diameter to 241 mm ($9\frac{1}{2}$ in.). Additional processing details are given in the table in Fig. 11.

Example 2: Forging a Combined Gear Blank and Hub in Flat Dies Using a Bolster.

The combined gear blank and hub forging shown in Fig. 12 was forged from 203 × 203 × 175 mm (8 × 8 × $7\frac{3}{4}$ in.) stock in five operations, as follows.



Stock preparation	**Cold sawing**
Stock size	**203 × 203 × 197 mm (8 × 8 × $7\frac{3}{4}$ in.)**

Stock weight	**64 kg (140 lb)**
Forging weight (after rough machining)	**54 kg (120 lb)**
Heating furnace	**Gas-fired, automatic temperature control**
Heating temperature	**1230 °C (2250 °F)[(a)]**
Forging machine	**18 kN (4000 lb) steam hammer**
Crew size	**Four men**

(a) Forging was completed in one heat.

Fig. 12 Typical procedure for the forging of a gear blank and hub in open dies, featuring the use of a bolster. Dimensions in figure given in inches.

Operation 1. The stock was forged to 178 × 178 × 254 mm (7 × 7 × 10 in.). This oblong was then forged into a bellied-end cylinder about 191 mm ($7\frac{1}{2}$ in.) in diameter and 279 mm (11 in.) in length, by being rotated and struck with successive hammer blows.

Operation 2. A stem approximately 102 mm (4 in.) in diameter and 203 mm (8 in.) in length was drawn from 64 mm ($2\frac{1}{2}$ in.) of the 279 mm (11 in.) length.

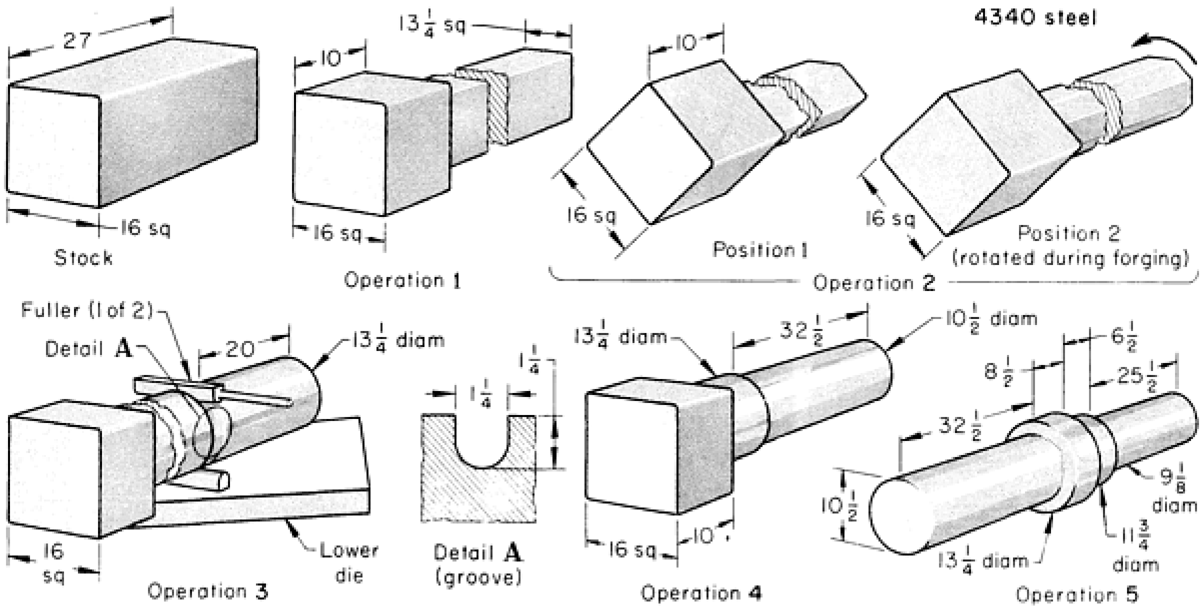
Operation 3. The workpiece was placed vertically in a bolster, as shown in Fig. 12, Operation 3.

Operation 4. The head was flattened (upset) until it was approximately 102 mm (4 in.) thick. The forging was then removed from the bolster and rounded up in flat dies.

Operation 5. The workpiece was placed in the bolster again and forged to the dimensions shown in Fig. 12, Operation 5. The forging was fully annealed and rough machined. Additional processing details are given in the table with Fig. 12.

Example 3: Forging a Four-Diameter Spindle in Flat Dies.

The four-diameter spindle forging shown in Fig. 13 was forged from 686 × 406 × 406 mm (27 × 16 × 16 in.) stock with one reheat in the following sequence of operations.



Stock preparation	**Cold sawing**
Stock size	**686 × 406 × 406 mm (27 × 16 × 16 in.)**
Stock weight	**878 kg (1935 lb)**
Forging weight (after rough machining)	**796 kg (1755 lb)**
Heating furnace	**Gas-fired, automatic temperature control**
Heating temperature	**1230 °C (2250 °F)[(a)]**
Forging machine	**22 kN (5000 lb) steam hammer**
Crew size	**Five men**

(a) Forging was reheated for operation 5.

Fig. 13 Sequence of operations in the forging of a four-diameter spindle in open dies, featuring the use of fullers. Dimensions in figures given in inches.

Operation 1. All but 254 mm (10 in.) of the hot stock was forged to a 337 mm ($13\frac{1}{4}$ in.) square section, using a sizing block on the lower die to gage size.

Operation 2. The workpiece was turned 45°, and the 337 mm ($13\frac{1}{4}$ in.) square section was flattened as shown in Position 1, Operation 2 (Fig. 13). The workpiece was rotated as the reduced portion was forged to an octagonal shape, as shown in Position 2, Operation 2. The octagon was then hammered into a round approximately 337 mm ($13\frac{1}{4}$ in.) in diameter (final shape in Position 2 not shown).

Operation 3. The workpiece was placed diagonally across the lower die; 508 mm (20 in.) from the end, a 267 mm ($10\frac{1}{2}$ in.) diam section was started by top and bottom fullers. The workpiece was rotated as the fullers were pressed into the hot steel, and a deep groove was formed around the workpiece (Fig. 13, Operation 3).

Operation 4. The 337 mm ($13\frac{1}{4}$ in.) sizing block was replaced by 267 mm ($10\frac{1}{2}$ in.) sizing block. The 508 mm (20 in.) long section was hammered first to a square, then to an octagon, and finally to a round (similar to procedures for Operations 1 and 2), with the length of this section increasing to 826 mm ($32\frac{1}{2}$ in.). The workpiece was then reheated.

Operation 5. The reheated workpiece was grasped on the 267 mm ($10\frac{1}{2}$ in.) diameter by 254 mm (10 in.) tongs. The 406 mm (16 in.) square section (unforged stock) was converted to a 337 mm ($13\frac{1}{4}$ in.) diam round section. At a distance of 216 mm ($8\frac{1}{2}$ in.) along the 337 mm ($13\frac{1}{4}$ in.) diameter, a back shoulder was started, using fullers as in Operation 3. After the groove was formed, the 337 mm ($13\frac{1}{4}$ in.) sizing block was replaced with a 298 mm ($11\frac{3}{4}$ in.) sizing block, and the 298 mm ($11\frac{3}{4}$ in.) diam by 165 mm ($6\frac{1}{2}$ in.) long section was forged in the same manner as described in Operations 1 and 2. The final section 232 mm, or $9\frac{1}{8}$ in., in diameter by 648 mm, or $25\frac{1}{2}$ in., in length, as shown in Fig. 13, Operation 5, was formed by similar procedures.

After forging, the workpiece was immediately placed in the furnace for full annealing. Additional processing details are given in the table with Fig. 13.

Example 4: Five-Operation Forging of a Large Seven-Diameter Turbine Rotor.

A seven-diameter turbine rotor (bottom right, Fig. 14) was forged from a 1.78 m (70 in.) diam, 2.79 m (110 in.) long, 64,900 kg (143,000 lb) corrugated ingot of low-alloy (Ni-Cr-Mo-V) steel. The steel was melted in basic electric furnaces and was vacuum stream degassed at the ingot mold to prevent flaking from entrapped hydrogen. The forging operations (Fig. 14) were as follows.

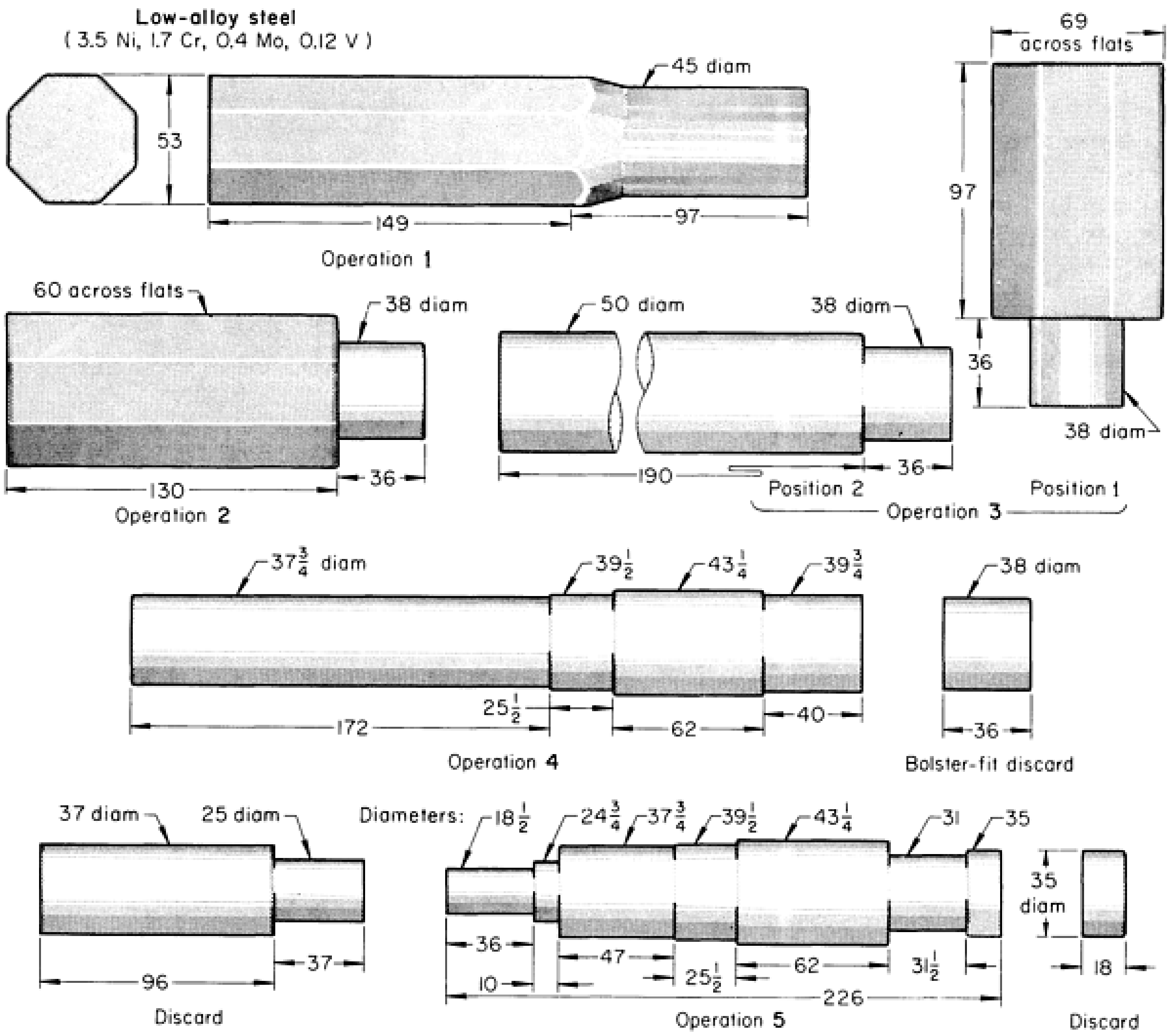


Fig. 14 Sequence of operations in the forging of a large turbine rotor in open dies. Dimensions given in inches.

Operation 1. The ingot was edged between flat dies to develop a bottle shape 6.25 m (246 in.) long, along with an octagonal section 1.35 m (53 in.) across flats and a round section 1.15 m (45 in.) in diameter.

Operation 2. The bottle-shaped workpiece was further developed by forging the 1.15 m (45 in.) diameter and the adjacent shoulder in V-dies, thus eliminating the shoulder and reducing the 1.15 m (45 in.) section to a 965 mm (38 in.) bolster fit. The bolster section was then cropped to remove part of the sinkhead, reducing the length of this section to 914 mm (36 in.). In addition, the octagonal section was upset to a width of 1.52 m (60 in.) across flats and a length of 3.30 m (130 in.).

Operation 3. In Position 1 of this operation (Fig. 14), the heavy section of the piece was upset, expanding the 1.52 m (60 in.) section to 1.75 m (69 in.), with the bolster in a position at the stem end, which rested on the lower die. The upset reduced the length of the heavy octagonal section from 3.30 to 2.46 m (130 to 97 in.). In Position 2 of this operation, the bloom was returned to the horizontal position, and the octagonal section was rounded between a flat top die and a bottom V-die, reducing its diameter to 1.27 m (50 in.) and extending its length to 4.83 m (190 in.).

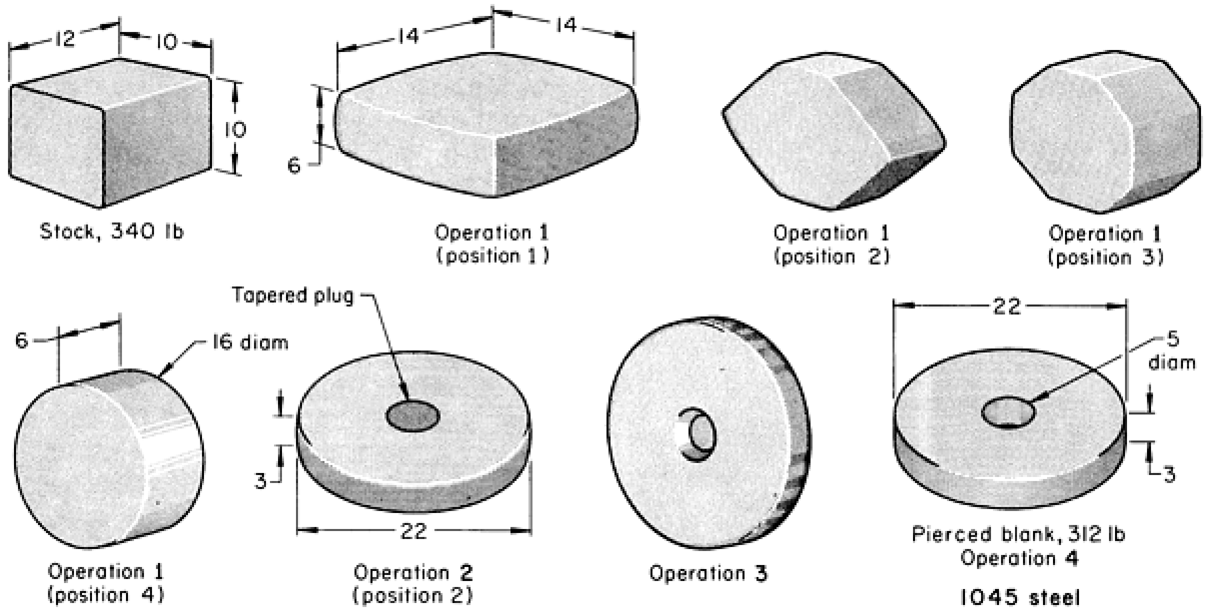
Operation 4. The main body of the forging was developed between a flat top die and a bottom V-die. The ends of the forging were set down to 959 mm and 1.01 m ($37\frac{3}{4}$ and $39\frac{3}{4}$ in.) diameters, respectively, and two additional diameters were forged between these sections. The bolster section (965 mm, or 38 in., in diameter by 914 mm, or 36 in., in length) was cut away at the conclusion of this operation.

Operation 5. Finish forging developed two additional stepped sections, ranging from 470 to 889 mm ($18\frac{1}{2}$ to 35 in.) in diameter, at each end of the forging. Following this operation, discard sections were cut from both ends of the forging. A large discard section was removed from the end of the forging (corresponding to the bottom of the ingot) that had not been cropped during the previous operations.

The finished forging was heat treated to develop optimal mechanical properties. Extensive mechanical tests were performed on specimens taken from the discard sections.

Example 5: Forging and Piercing a Blank for Forming a Ring.

The forged and pierced blank shown in Fig. 15 was forged from 305 × 254 × 254 mm (12 × 10 × 10 in.) stock. The sequence of operations was as follows.



Stock preparation	**Cold sawing**
Stock size	**305 × 254 × 254 mm (12 × 10 × 10 in.)**
Stock weight	**154 kg (340 lb)**
Shipping weight	**142 kg (312 lb)**
Heating furnace	**Gas-fired, automatic temperature control**
Heating temperature	**1230 °C (2250 °F)[(a)]**
Forging equipment	**18 kN (4000 lb) steam hammer**

Size of ring saddle forged from pierced blank	**1020 mm (40 in.) OD × 762 mm (30 in.) ID × 50 mm (2 in.)**

(a) Blank was completed in one heat.

Fig. 15 Sequence of operations in the forging and piercing of a circular blank. Dimensions in figure given in inches.

Operation 1. Heated stock was placed vertically on a flat die. The 305 mm (12 in.) height was reduced to 152 mm (6 in.) and the 254 mm (10 in.) square cross section was increased to 356 mm (14 in.) square. The workpiece was repositioned and hammered, first to a hexagonal, next to an octagonal, and then to a round section 406 mm (16 in.) in diameter by 152 mm (6 in.) in length.

Operation 2. The workpiece was flattened to a 75 mm (3 in.) thick, 559 mm (22 in.) round, and a tapered plug was centered and hammered in.

Operation 3. The hot workpiece was rotated and hammered on its circumference to flatten the edge, which bulged from previous hammering, and to loosen the plug.

Operation 4. The workpiece was positioned as shown in Fig. 15, Operation 4, and the 127 mm (5 in.) diam hole was completed by piercing from the opposite side. The pierced blank was saddle forged to a ring on a mandrel, following the technique shown in Fig. 2 (see also Example 6).

Forging of Rings. Rings are often rolled from forged and pierced blanks (see the article "Ring Rolling" in this Volume); however, when rolling is precluded (because of small quantities, short delivery time, or other reasons), saddle forging (Fig. 2) is often used. Typical procedures for producing rings by this method are described in the following example.

Example 6: Saddle Forging a 1.02 m (40 in.) OD Ring From a 559 mm (22 in.) OD Blank.

A 1.02 in (40 in.) OD ring was saddle forged in a 6670 N (1500 lbf) steam hammer from a 559 mm (22 in.) OD blank produced as described in Example 5 and shown in Fig. 15. Flattening operations were done at suitable intervals to reduce the ring to a 50 mm (2 in.) thickness. Saddle forging was done as follows (Fig. 16).

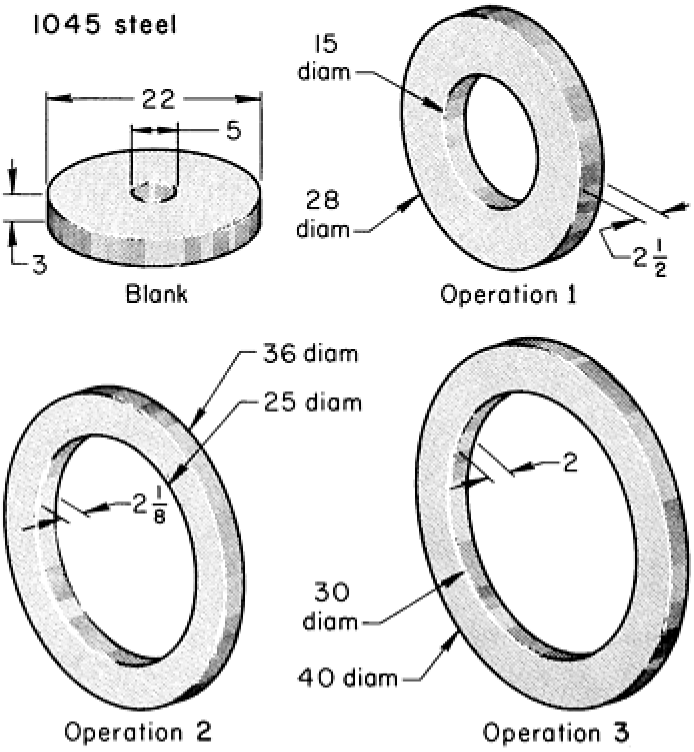


Fig. 16 Shapes produced in the three-operation saddle forging of a ring from a forged and pierced blank. Dimensions given in inches.

Operation 1. The blank was heated to 1230 °C (2250 °F) and forged to the dimensions shown in Fig. 16, Operation 1, by alternate saddle forging and flattening.

Operation 2. The 711 mm (28 in.) OD ring was reheated to 1230 °C (2250 °F) and forged by the same technique used in Operation 1 to produce a 914 mm (36 in.) diam ring.

Operation 3. The 914 mm (36 in.) OD ring was reheated to 1230 °C (2250 °F) and saddle forged and flattened as needed to obtain a 50 mm (2 in.) thickness, a 1.02 m (40 in.) outside diameter, and a 762 mm (30 in.) inside diameter.

Example 7: Mandrel Forging a Long Hollow Piece on a 40.9 MN (4600 tonf) Hydraulic Press.

Mandrel-forging technique is utilized to produce a long, hollow, cylindrically symmetrical piece. The outside diameter of the production piece was 1.32 m (52.0 in.). The average inside diameter was 914 mm (36.0 in.). The total overall length was 7.0 m (23.0 ft) with a 1.59 m (62.75 in.) diam by 482 mm (19.0 in.) long flange included on one end of the piece. The flange drops to a 1.45 m (57.0 in.) diameter, which tapers to the 1.32 m (52.0 in.) body diameter over a 229 mm (9.0 in.) length.

Operation 1. The 2.11 m (83 in.) diam, 78,900 kg (174,000 lb) ingot of AISI 4130 grade steel was used as the starting stock. It as heated to the forging temperature and straight forged (saddened) to 1.57 m (62.0 in.) diam size.

Operation 2. Top and bottom ingot discards were taken by flame cutting to yield a slug of 1.57 m (62.0 in.) in diameter and 3.20 m (126.0 in.) in length.

Operation 3. The slug was upset forged by positioning it vertically under the press. The 3.20 m (126.0 in.) dimension was reduced to 3.15 m (80.0 in.).

Operation 4. The upset slug was hot trepanned using 559 mm (22.0 in.) cutters to remove the core.

Operation 5. The slug was saddle forged to increase the inside diameter to 991 mm (39.0 in.).

Operation 6. The piece was mandrel forged on a tapered mandrel (0.8 to 1 m, or 33 to 39 in., in diameter) using the top flat die and bottom V-die. Mandrel forging caused the metal to move in the longitudinal (axial) direction, thus producing the desired part.

Open-Die Forging

Revised by the ASM Committee on Open-Die Forging*; Chairman: Ashok K. Khare, National Forge Company

Examples of Production Practice

Because of differences in equipment and operator skill, procedures for open-die forging vary considerably from plant to plant. Figure 10 shows typical steps in the drawing and forging of stock and in the fabrication of common shapes from billets of square, rectangular, and round cross sections. The procedures described in the following examples are typical of those used for the production of some common open-die forgings.

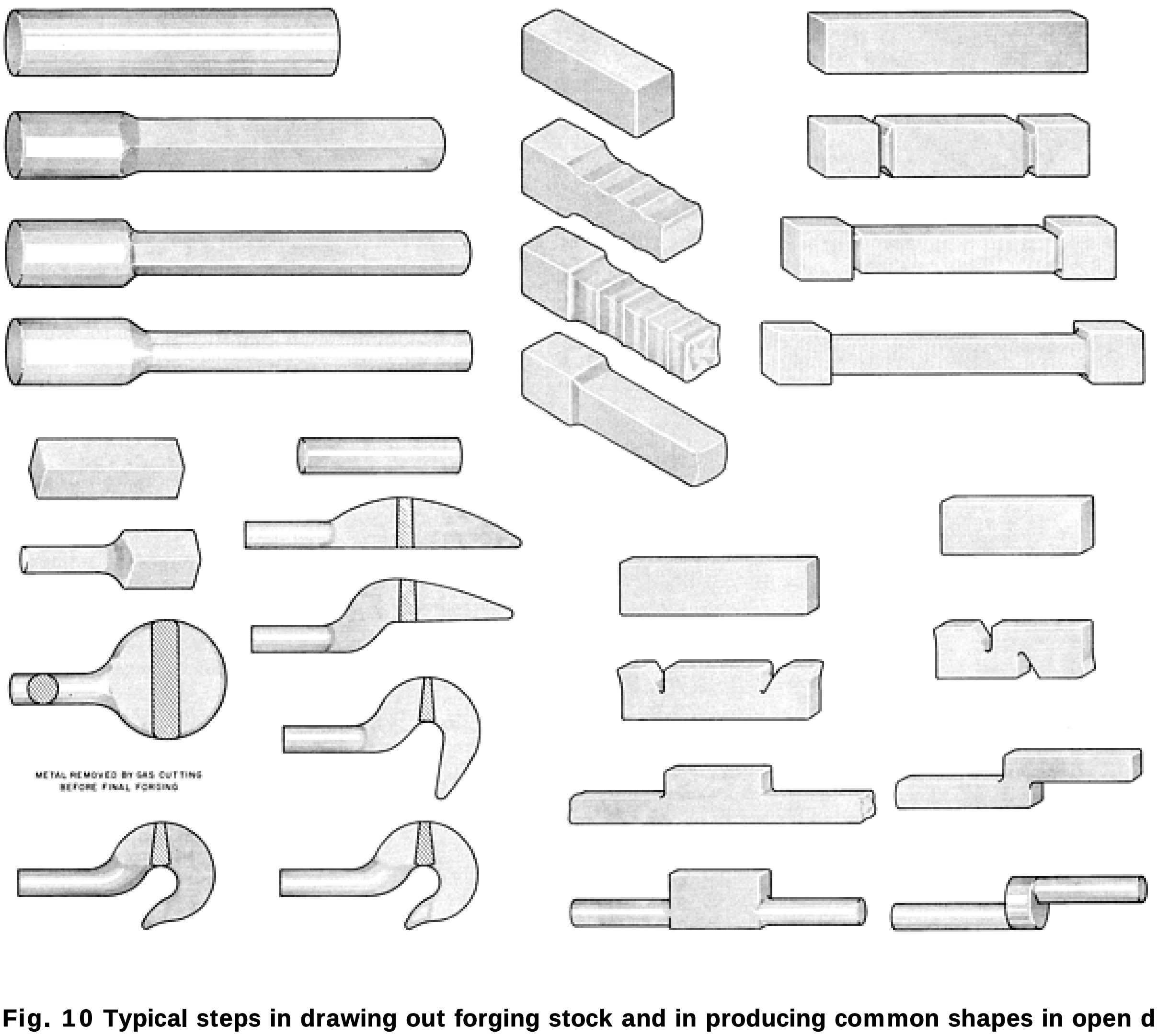
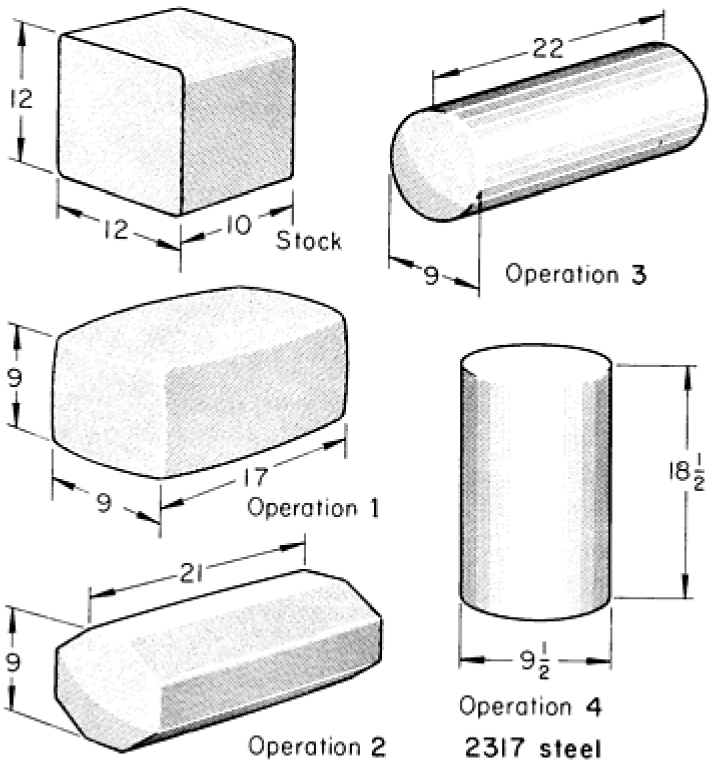


Fig. 10 Typical steps in drawing out forging stock and in producing common shapes in open dies

Example 1: Forging a 170-kg (375-lb) Solid Cylinder in Flat Dies.

A cylinder, 241 mm ($9\frac{1}{2}$ in.) in diameter by 470 mm ($18\frac{1}{2}$ in.) in length, was forged in flat dies from 305 × 305 × 254 mm (12 × 12 × 10 in.) stock in four operations without reheating the billet (Fig. 11). The following sequence of operations was used.



Stock preparation	**Cold sawing**
Stock size	**305 × 305 × 254 mm (12 × 12 × 10 in.)**
Stock weight	**179 kg (395 lb)**
Finished weight	**170 kg (375 lb)**
Heating furnace	**Gas-fired, automatic temperature control**
Heating temperature	**1230 °C (2250 °F)[(a)]**
Forging machine	**18 kN (4000 lb) steam hammer**

(a) Forging was completed in one heat.

Fig. 11 Sequence of operations in the forging of a cylindrical workpiece from square stock. Dimensions in figure

given in inches

Operation 1. The 305 mm (12 in.) square section was hammered to a 229 mm (9 in.) square section, which increased the length to 432 mm (17 in.).

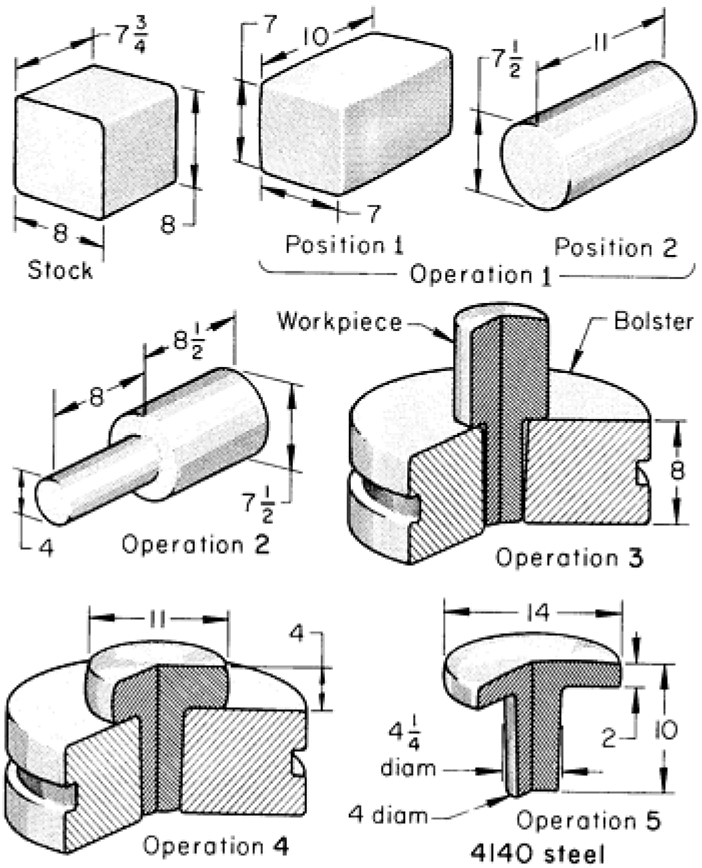
Operation 2. The corners of the square were hammered to produce an octagonal shape approximately 229 mm (9 in.) across flats and 533 mm (21 in.) long.

Operation 3. The octagon was rounded by successive hammer blows as the workpiece was rotated. The cylindrical forging was then approximately 559 mm (22 in.) long.

Operation 4. The forging was upended and hammered lightly on both ends to flatten the bulge on the ends. This decreased the length to 470 mm ($18\frac{1}{2}$ in.) and increased the diameter to 241 mm ($9\frac{1}{2}$ in.). Additional processing details are given in the table in Fig. 11.

Example 2: Forging a Combined Gear Blank and Hub in Flat Dies Using a Bolster.

The combined gear blank and hub forging shown in Fig. 12 was forged from 203 × 203 × 175 mm (8 × 8 × $7\frac{3}{4}$ in.) stock in five operations, as follows.



Stock preparation	**Cold sawing**

Stock size	**203 × 203 × 197 mm (8 × 8 × 7$\frac{3}{4}$ in.)**
Stock weight	**64 kg (140 lb)**
Forging weight (after rough machining)	**54 kg (120 lb)**
Heating furnace	**Gas-fired, automatic temperature control**
Heating temperature	**1230 °C (2250 °F)[a]**
Forging machine	**18 kN (4000 lb) steam hammer**
Crew size	**Four men**

(a) Forging was completed in one heat.

Fig. 12 Typical procedure for the forging of a gear blank and hub in open dies, featuring the use of a bolster. Dimensions in figure given in inches.

Operation 1. The stock was forged to 178 × 178 × 254 mm (7 × 7 × 10 in.). This oblong was then forged into a bellied-end cylinder about 191 mm (7$\frac{1}{2}$ in.) in diameter and 279 mm (11 in.) in length, by being rotated and struck with successive hammer blows.

Operation 2. A stem approximately 102 mm (4 in.) in diameter and 203 mm (8 in.) in length was drawn from 64 mm (2$\frac{1}{2}$ in.) of the 279 mm (11 in.) length.

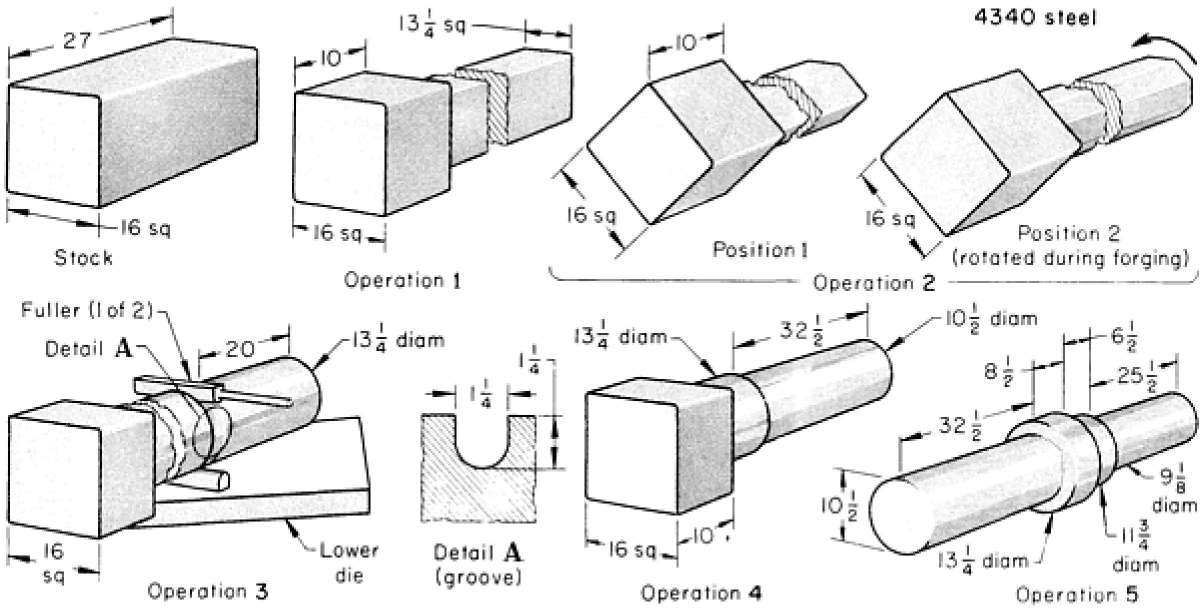
Operation 3. The workpiece was placed vertically in a bolster, as shown in Fig. 12, Operation 3.

Operation 4. The head was flattened (upset) until it was approximately 102 mm (4 in.) thick. The forging was then removed from the bolster and rounded up in flat dies.

Operation 5. The workpiece was placed in the bolster again and forged to the dimensions shown in Fig. 12, Operation 5. The forging was fully annealed and rough machined. Additional processing details are given in the table with Fig. 12.

Example 3: Forging a Four-Diameter Spindle in Flat Dies.

The four-diameter spindle forging shown in Fig. 13 was forged from 686 × 406 × 406 mm (27 × 16 × 16 in.) stock with one reheat in the following sequence of operations.



Stock preparation	**Cold sawing**
Stock size	**686 × 406 × 406 mm (27 × 16 × 16 in.)**
Stock weight	**878 kg (1935 lb)**
Forging weight (after rough machining)	**796 kg (1755 lb)**
Heating furnace	**Gas-fired, automatic temperature control**
Heating temperature	**1230 °C (2250 °F)[(a)]**
Forging machine	**22 kN (5000 lb) steam hammer**
Crew size	**Five men**

(a) Forging was reheated for operation 5.

Fig. 13 Sequence of operations in the forging of a four-diameter spindle in open dies, featuring the use of fullers. Dimensions in figures given in inches.

Operation 1. All but 254 mm (10 in.) of the hot stock was forged to a 337 mm ($13\frac{1}{4}$ in.) square section, using a sizing block on the lower die to gage size.

Operation 2. The workpiece was turned 45°, and the 337 mm ($13\frac{1}{4}$ in.) square section was flattened as shown in Position 1, Operation 2 (Fig. 13). The workpiece was rotated as the reduced portion was forged to an octagonal shape, as shown in Position 2, Operation 2. The octagon was then hammered into a round approximately 337 mm ($13\frac{1}{4}$ in.) in diameter (final shape in Position 2 not shown).

Operation 3. The workpiece was placed diagonally across the lower die; 508 mm (20 in.) from the end, a 267 mm ($10\frac{1}{2}$ in.) diam section was started by top and bottom fullers. The workpiece was rotated as the fullers were pressed into the hot steel, and a deep groove was formed around the workpiece (Fig. 13, Operation 3).

Operation 4. The 337 mm ($13\frac{1}{4}$ in.) sizing block was replaced by 267 mm ($10\frac{1}{2}$ in.) sizing block. The 508 mm (20 in.) long section was hammered first to a square, then to an octagon, and finally to a round (similar to procedures for Operations 1 and 2), with the length of this section increasing to 826 mm ($32\frac{1}{2}$ in.). The workpiece was then reheated.

Operation 5. The reheated workpiece was grasped on the 267 mm ($10\frac{1}{2}$ in.) diameter by 254 mm (10 in.) tongs. The 406 mm (16 in.) square section (unforged stock) was converted to a 337 mm ($13\frac{1}{4}$ in.) diam round section. At a distance of 216 mm ($8\frac{1}{2}$ in.) along the 337 mm ($13\frac{1}{4}$ in.) diameter, a back shoulder was started, using fullers as in Operation 3. After the groove was formed, the 337 mm ($13\frac{1}{4}$ in.) sizing block was replaced with a 298 mm ($11\frac{3}{4}$ in.) sizing block, and the 298 mm ($11\frac{3}{4}$ in.) diam by 165 mm ($6\frac{1}{2}$ in.) long section was forged in the same manner as described in Operations 1 and 2. The final section 232 mm, or $9\frac{1}{8}$ in., in diameter by 648 mm, or $25\frac{1}{2}$ in., in length, as shown in Fig. 13, Operation 5, was formed by similar procedures.

After forging, the workpiece was immediately placed in the furnace for full annealing. Additional processing details are given in the table with Fig. 13.

Example 4: Five-Operation Forging of a Large Seven-Diameter Turbine Rotor.

A seven-diameter turbine rotor (bottom right, Fig. 14) was forged from a 1.78 m (70 in.) diam, 2.79 m (110 in.) long, 64,900 kg (143,000 lb) corrugated ingot of low-alloy (Ni-Cr-Mo-V) steel. The steel was melted in basic electric furnaces and was vacuum stream degassed at the ingot mold to prevent flaking from entrapped hydrogen. The forging operations (Fig. 14) were as follows.

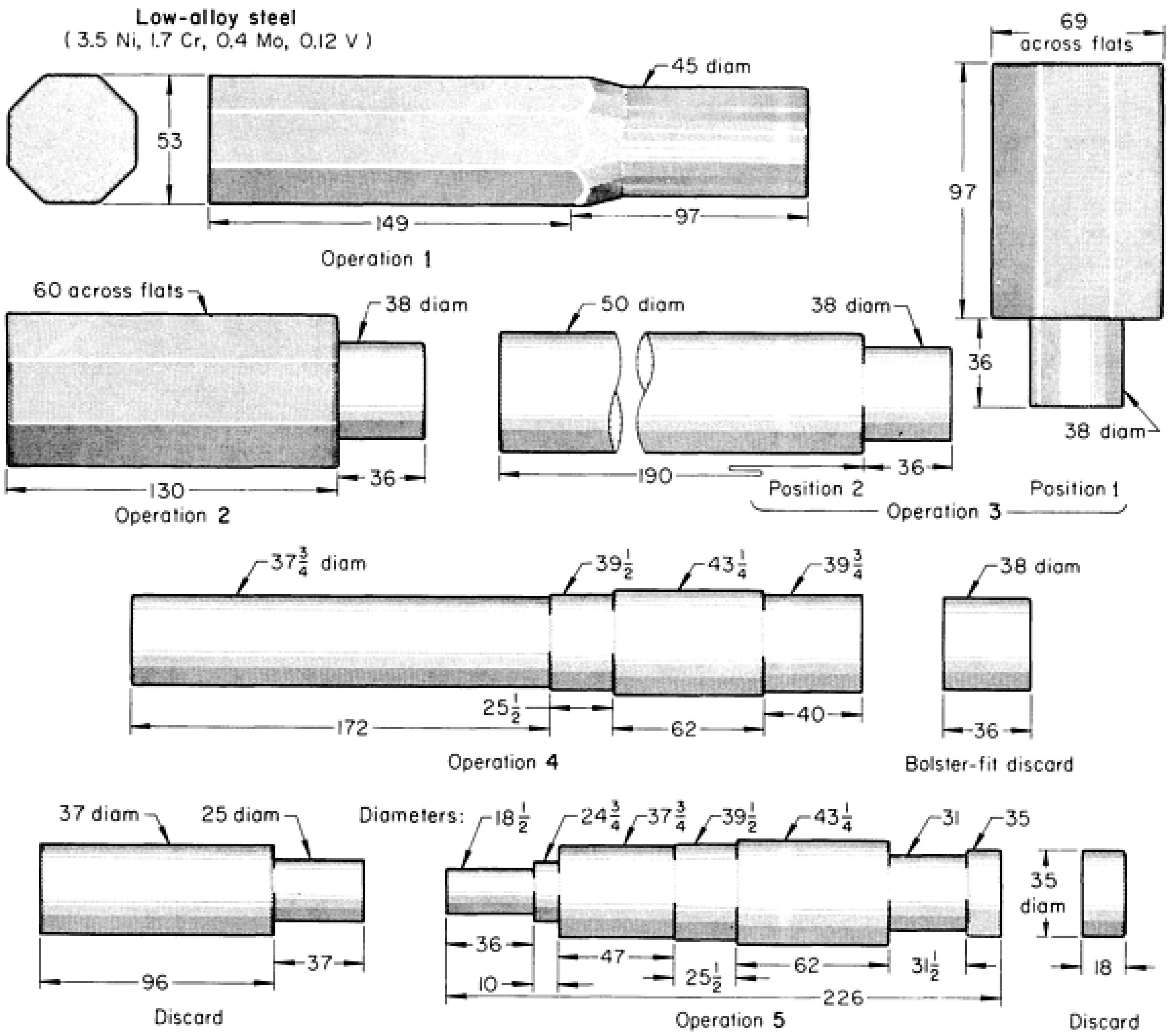


Fig. 14 Sequence of operations in the forging of a large turbine rotor in open dies. Dimensions given in inches.

Operation 1. The ingot was edged between flat dies to develop a bottle shape 6.25 m (246 in.) long, along with an octagonal section 1.35 m (53 in.) across flats and a round section 1.15 m (45 in.) in diameter.

Operation 2. The bottle-shaped workpiece was further developed by forging the 1.15 m (45 in.) diameter and the adjacent shoulder in V-dies, thus eliminating the shoulder and reducing the 1.15 m (45 in.) section to a 965 mm (38 in.) bolster fit. The bolster section was then cropped to remove part of the sinkhead, reducing the length of this section to 914 mm (36 in.). In addition, the octagonal section was upset to a width of 1.52 m (60 in.) across flats and a length of 3.30 m (130 in.).

Operation 3. In Position 1 of this operation (Fig. 14), the heavy section of the piece was upset, expanding the 1.52 m (60 in.) section to 1.75 m (69 in.), with the bolster in a position at the stem end, which rested on the lower die. The upset reduced the length of the heavy octagonal section from 3.30 to 2.46 m (130 to 97 in.). In Position 2 of this operation, the bloom was returned to the horizontal position, and the octagonal section was rounded between a flat top die and a bottom V-die, reducing its diameter to 1.27 m (50 in.) and extending its length to 4.83 m (190 in.).

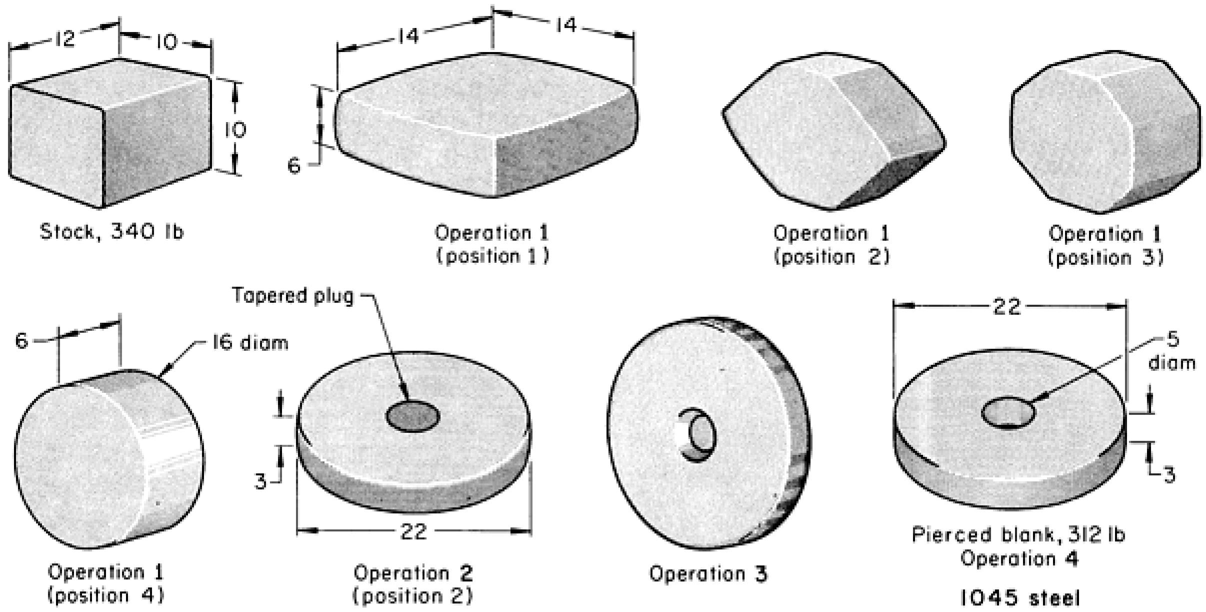
Operation 4. The main body of the forging was developed between a flat top die and a bottom V-die. The ends of the forging were set down to 959 mm and 1.01 m ($37\frac{3}{4}$ and $39\frac{3}{4}$ in.) diameters, respectively, and two additional diameters were forged between these sections. The bolster section (965 mm, or 38 in., in diameter by 914 mm, or 36 in., in length) was cut away at the conclusion of this operation.

Operation 5. Finish forging developed two additional stepped sections, ranging from 470 to 889 mm ($18\frac{1}{2}$ to 35 in.) in diameter, at each end of the forging. Following this operation, discard sections were cut from both ends of the forging. A large discard section was removed from the end of the forging (corresponding to the bottom of the ingot) that had not been cropped during the previous operations.

The finished forging was heat treated to develop optimal mechanical properties. Extensive mechanical tests were performed on specimens taken from the discard sections.

Example 5: Forging and Piercing a Blank for Forming a Ring.

The forged and pierced blank shown in Fig. 15 was forged from 305 × 254 × 254 mm (12 × 10 × 10 in.) stock. The sequence of operations was as follows.



Stock preparation	**Cold sawing**
Stock size	**305 × 254 × 254 mm (12 × 10 × 10 in.)**
Stock weight	**154 kg (340 lb)**
Shipping weight	**142 kg (312 lb)**
Heating furnace	**Gas-fired, automatic temperature control**
Heating temperature	**1230 °C (2250 °F)[(a)]**
Forging equipment	**18 kN (4000 lb) steam hammer**

Size of ring saddle forged from pierced blank	**1020 mm (40 in.) OD × 762 mm (30 in.) ID × 50 mm (2 in.)**

(a) Blank was completed in one heat.

Fig. 15 Sequence of operations in the forging and piercing of a circular blank. Dimensions in figure given in inches.

Operation 1. Heated stock was placed vertically on a flat die. The 305 mm (12 in.) height was reduced to 152 mm (6 in.) and the 254 mm (10 in.) square cross section was increased to 356 mm (14 in.) square. The workpiece was repositioned and hammered, first to a hexagonal, next to an octagonal, and then to a round section 406 mm (16 in.) in diameter by 152 mm (6 in.) in length.

Operation 2. The workpiece was flattened to a 75 mm (3 in.) thick, 559 mm (22 in.) round, and a tapered plug was centered and hammered in.

Operation 3. The hot workpiece was rotated and hammered on its circumference to flatten the edge, which bulged from previous hammering, and to loosen the plug.

Operation 4. The workpiece was positioned as shown in Fig. 15, Operation 4, and the 127 mm (5 in.) diam hole was completed by piercing from the opposite side. The pierced blank was saddle forged to a ring on a mandrel, following the technique shown in Fig. 2 (see also Example 6).

Forging of Rings. Rings are often rolled from forged and pierced blanks (see the article "Ring Rolling" in this Volume); however, when rolling is precluded (because of small quantities, short delivery time, or other reasons), saddle forging (Fig. 2) is often used. Typical procedures for producing rings by this method are described in the following example.

Example 6: Saddle Forging a 1.02 m (40 in.) OD Ring From a 559 mm (22 in.) OD Blank.

A 1.02 in (40 in.) OD ring was saddle forged in a 6670 N (1500 lbf) steam hammer from a 559 mm (22 in.) OD blank produced as described in Example 5 and shown in Fig. 15. Flattening operations were done at suitable intervals to reduce the ring to a 50 mm (2 in.) thickness. Saddle forging was done as follows (Fig. 16).

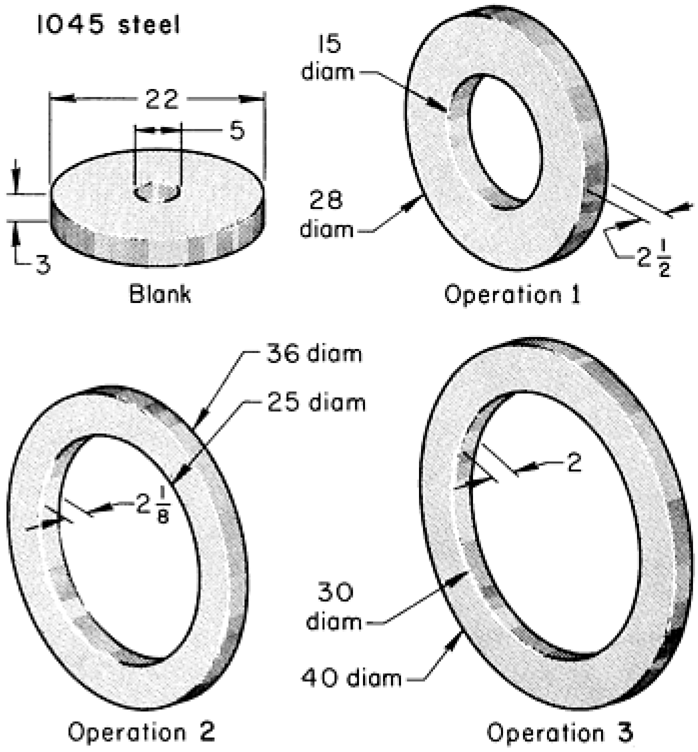


Fig. 16 Shapes produced in the three-operation saddle forging of a ring from a forged and pierced blank. Dimensions given in inches.

Operation 1. The blank was heated to 1230 °C (2250 °F) and forged to the dimensions shown in Fig. 16, Operation 1, by alternate saddle forging and flattening.

Operation 2. The 711 mm (28 in.) OD ring was reheated to 1230 °C (2250 °F) and forged by the same technique used in Operation 1 to produce a 914 mm (36 in.) diam ring.

Operation 3. The 914 mm (36 in.) OD ring was reheated to 1230 °C (2250 °F) and saddle forged and flattened as needed to obtain a 50 mm (2 in.) thickness, a 1.02 m (40 in.) outside diameter, and a 762 mm (30 in.) inside diameter.

Example 7: Mandrel Forging a Long Hollow Piece on a 40.9 MN (4600 tonf) Hydraulic Press.

Mandrel-forging technique is utilized to produce a long, hollow, cylindrically symmetrical piece. The outside diameter of the production piece was 1.32 m (52.0 in.). The average inside diameter was 914 mm (36.0 in.). The total overall length was 7.0 m (23.0 ft) with a 1.59 m (62.75 in.) diam by 482 mm (19.0 in.) long flange included on one end of the piece. The flange drops to a 1.45 m (57.0 in.) diameter, which tapers to the 1.32 m (52.0 in.) body diameter over a 229 mm (9.0 in.) length.

Operation 1. The 2.11 m (83 in.) diam, 78,900 kg (174,000 lb) ingot of AISI 4130 grade steel was used as the starting stock. It as heated to the forging temperature and straight forged (saddened) to 1.57 m (62.0 in.) diam size.

Operation 2. Top and bottom ingot discards were taken by flame cutting to yield a slug of 1.57 m (62.0 in.) in diameter and 3.20 m (126.0 in.) in length.

Operation 3. The slug was upset forged by positioning it vertically under the press. The 3.20 m (126.0 in.) dimension was reduced to 3.15 m (80.0 in.).

Operation 4. The upset slug was hot trepanned using 559 mm (22.0 in.) cutters to remove the core.

Operation 5. The slug was saddle forged to increase the inside diameter to 991 mm (39.0 in.).

Operation 6. The piece was mandrel forged on a tapered mandrel (0.8 to 1 m, or 33 to 39 in., in diameter) using the top flat die and bottom V-die. Mandrel forging caused the metal to move in the longitudinal (axial) direction, thus producing the desired part.

Open-Die Forging

Revised by the ASM Committee on Open-Die Forging*; Chairman: Ashok K. Khare, National Forge Company

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Contour Forging

Open-die contour or form forging requiring the use of dedicated dies has been successfully accomplished for carbon, alloy, and stainless steels as well as for superalloys. Contour forging can be advantageous under such circumstances as the following:

- Enhancement of grain flow at specific locations, when demanded by product application
- Reduction of the quantity of starting material; this is especially critical when using expensive materials such as stainless steels and superalloys
- Reduction of machining costs; this is critical when machinability or excessive material removal are factors

Open-die contour forging may be a requirement, as in the case of grain flow, or it may be an option, as in the case of material and machining cost savings. The material and machining cost savings typically outweighs the forging tooling costs.

Die material is largely dependent on the forging hours required for the product run. Generally, when dealing with a small production run having total forging hours of 30 or fewer, in which tooling cost has a significant impact on product cost, H-13 would be an acceptable die material. However, larger forging runs would require the use of superalloy material.

Set Down. It may not be possible to calculate precisely the amount of material required for the contour forging of complex shapes. It is then recommended to run trials on low-cost material. The factors affecting the consideration would be the condition of the forge press, operator skill, forge preheat, and the extent of the net shape design affecting metal flow.

Turbine Wheel Forging. Turbine wheels, which are commonly 2.54 m (100 in.) in diameter, are forged by first upsetting a block of steel and then contour forging to provide the thick hub and thin rim sections (Fig. 17). This is done using a shaped (contoured) bottom die, which supports the entire workpiece, and a shaped partial top (contoured swing) die. Successive strokes are taken with the top die as it is indexed around the vertical centerline of the press. The partial top die minimizes the force required to deform the metal, yet produces the desired forge envelope.

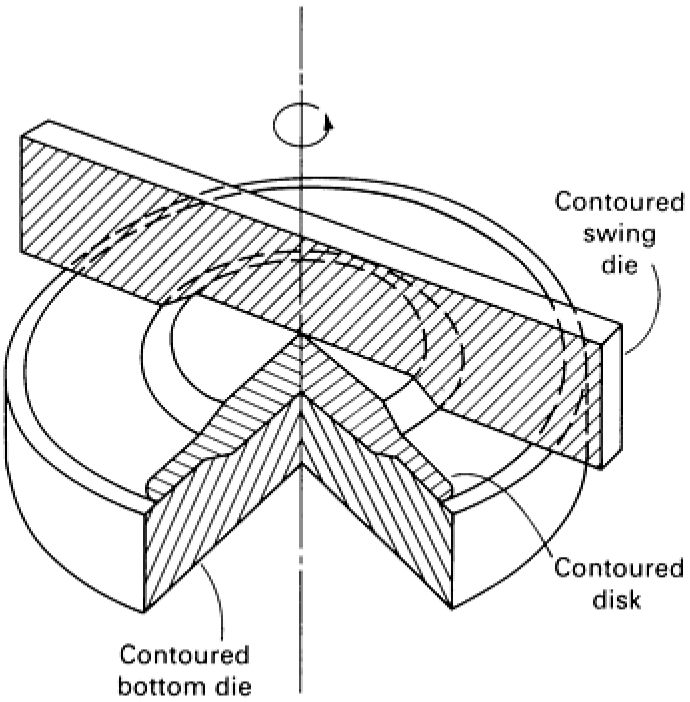


Fig. 17 Illustrations showing turbine wheel formed by using contour forging method.

Nozzle extrusion is a more complex contour-forging method (Fig. 18). Nozzle extrusions are commonly used for thick-wall vessels in cases in which the cost of extruding the nozzle shape offsets the cost and quality risk factors involved in producing the shell and the nozzle as a weldment. The tooling consists of a shaped bottom die and a punch. The punch is forced through a machined pilot hole in the workpiece. The material conforms to the shape of the bottom die and is extended forward to form the nozzle. Two possible methods of producing a shell section with a nozzle are shown in Fig. 19. Design engineers prefer the nozzle extrusion technique over the welded nozzle because of the superior grain flow characteristics, toughness, and favorable costs associated with the extrusion process.

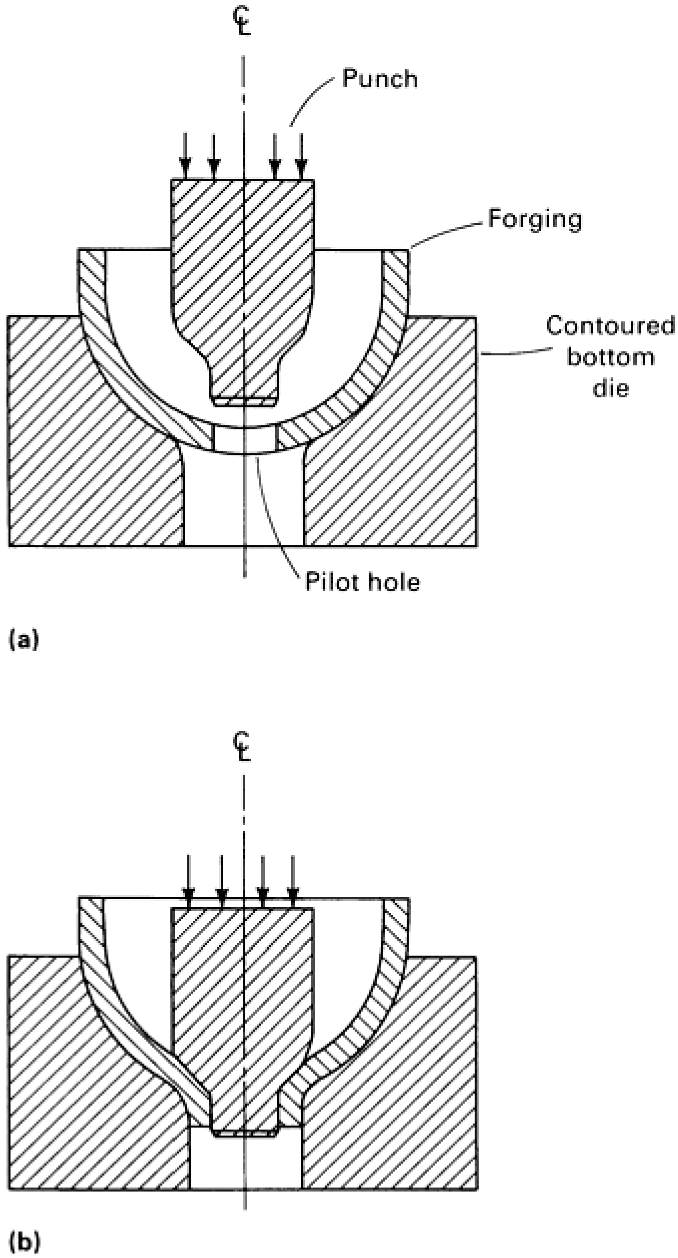
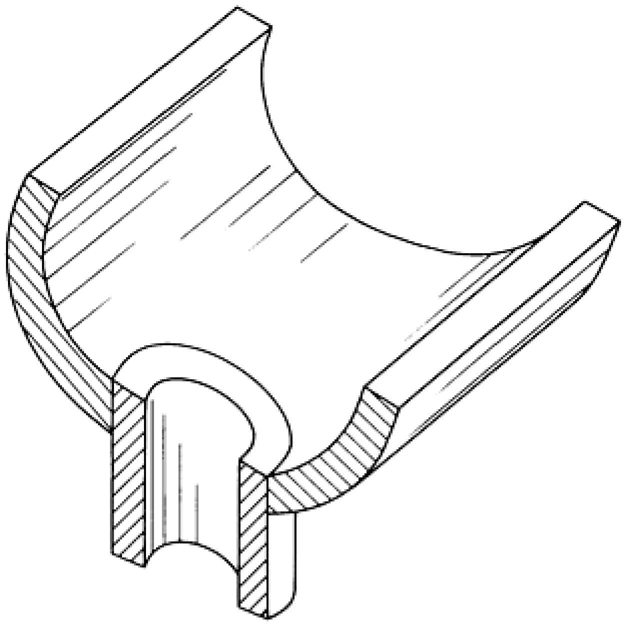
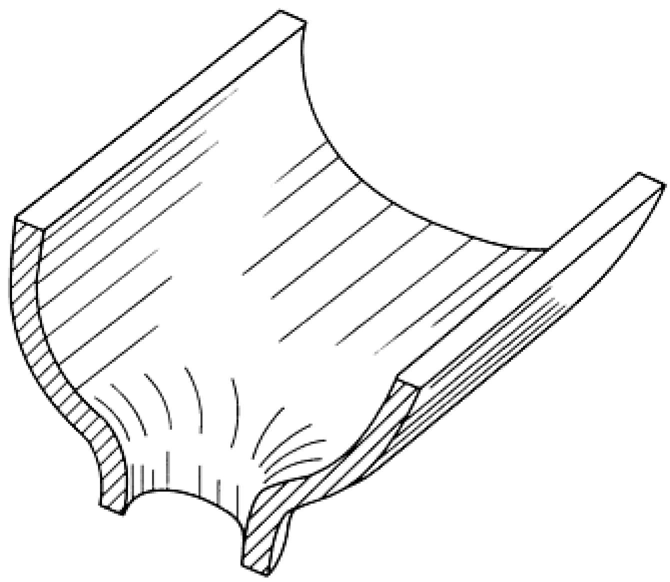


Fig. 18 Illustration of nozzle extrusion, a complex contour forging method. (a) Punch position before extrusion. (b) Punch position after extrusion.



(a)



(b)

Fig. 19 Metal shells featuring nozzles formed by two different methods. (a) Welded nozzle. (b) Extruded nozzle.

Pressure vessel head forgings can be produced from either forged or rolled plate by either of two methods. In the first method, full male and female dies are used to develop a dome shape (Fig. 20a). In the second method, a partial male die and a full female die are used to produce a dome shape (Fig. 20b). The second method, although requiring more forging strokes than the first method (the top die is swung in incremental positions for each stroke), reduces the press load per stroke. Therefore, larger dome shapes can be made by this technique. In addition, if required, smaller presses can be used to make the dome shapes (press capacity will determine the appropriate swing die width that can be used).

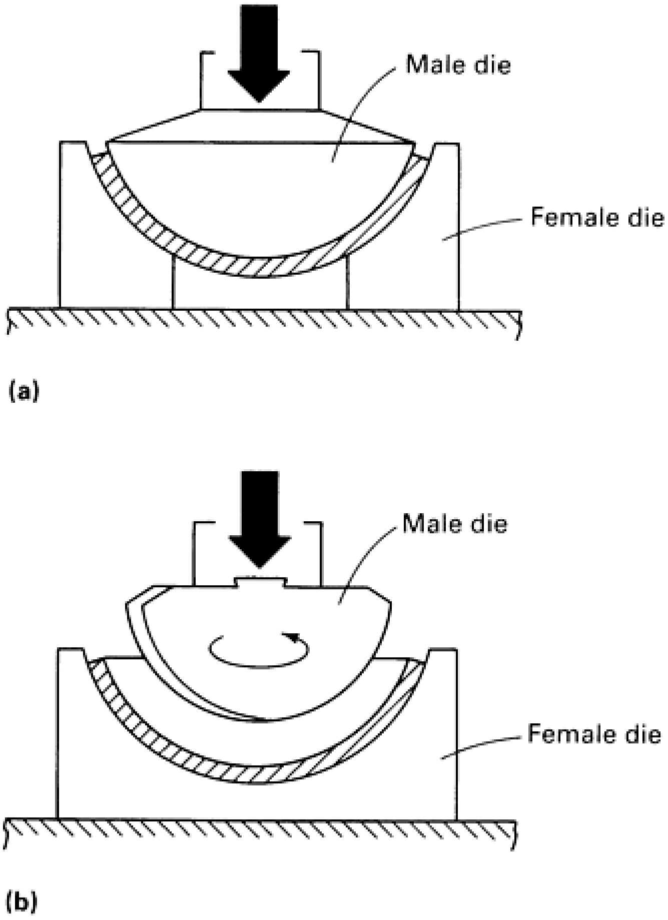


Fig. 20 Contour forming of a pressure vessel head using a (a) full male die and a (b) partial male die.

Bottleneck-shaped forgings are made as doubles from a straight forged bar (Fig. 21). For example, 292 mm (11.5 in.) radius contour dies are set down 165 mm (6.5 in.) to achieve the small diameter of 254 mm (10.0 in.) from the large diameter of 584 mm (23.0 in.). In order to generate axial movement during the forging process, the flat die width must be a minimum of 50 mm (2.0 in.) less than the set down dimension. In addition, the die radius adjacent to the flat and the contour should be a minimum of 38 mm (1.5 in.) to enhance axial metal flow and to minimize material lapping.

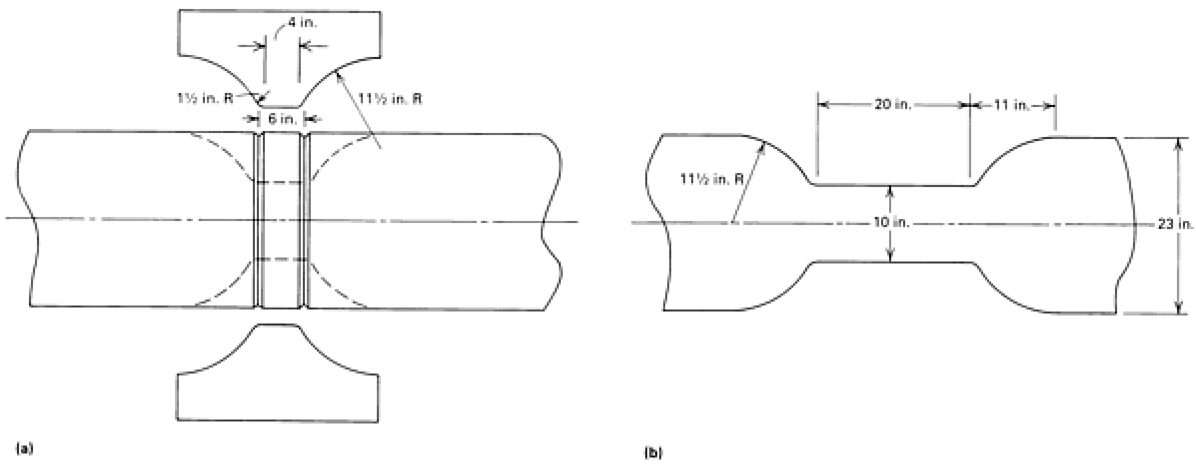


Fig. 21 Contour forging of a straight forged bar to form a double bottleneck-shaped workpiece. (a) Original 320 kg (700 lb) bar. (b) Contour-forged, 205 kg (450 lb) finished workpiece.

Forging quality is best achieved using a 17.8 MN (2000 tonf) hydraulic press by positioning the die to the set-down mark as shown in Fig. 21 and manually or mechanically rotating the workpiece in 10° to 15° increments using not greater than 25 mm (1 in.) drafts. The process is continued by working from side to side, keeping the die tight to the contour, while exercising caution to avoid lapping on the contour.

Open-Die Forging

Revised by the ASM Committee on Open-Die Forging[*]; Chairman: Ashok K. Khare, National Forge Company

Allowances and Tolerances

To make certain that forgings can be machined to correct final measurements, it is necessary at the forging stage to establish allowances, tolerances, and specifications for flatness and concentricity.

Allowance. In open-die forging, the allowance defines the amount by which a dimension is increased in order to determine its size at an earlier stage of manufacture. An allowance is added to a finish-machined size. Similarly, an additional allowance is added to a rough-machined dimension to determine the forged size. These allowances provide enough stock to permit machining to final dimensions.

The stock provided for machining increases the weight of the forging at earlier stages of manufacture. The weight of the additional metal and the machining operations necessary to remove it increase the cost of the finished part. Consequently, the allowances specified for each step of manufacture should be kept as small as practical while still maintaining enough metal so that all dimensions of the finished part can be readily achieved with normal production techniques.

Table 1 shows allowances added to rough-machined dimensions of straight round, square, rectangular, or octagonal bars of uniform cross section. The allowance increases as diameter (or section width) and length increase. Table 1 also explains how allowances are determined for open-die forgings with more than one cross-sectional dimension.

Table 1 Allowances and tolerances for as-forged shafts and bars

Allowance is added to rough-machined dimension to obtain forged dimension. Tolerances are the variations permitted on forged dimensions.

Rough-machined diameter or width, mm (in.)	Allowance for overall rough-machined length, mm (in.), of:			
	Over 152-762 (6-30)	**Over 762-1520 (30-60)**	**Over 1520-2290 (60-90)**	Over 2290-3050 (90-120)
Over 25-75 (1-3)	7.7 ($\frac{5}{16}$) + 3.2, -0 (+ $\frac{1}{8}$, -0)	9.5 ($\frac{3}{8}$) + 3.2, -1.6 (+$\frac{1}{8}$, -$\frac{1}{16}$)	11.1 ($\frac{7}{16}$) + 3.2, -1.6 (+$\frac{1}{8}$, -$\frac{1}{16}$)	**12.7 ± 3.2 ($\frac{1}{2}$ ± $\frac{1}{8}$)**
Over 75-152 (3-6)	9.5 ($\frac{3}{8}$) + 3.2, -1.6 (+$\frac{1}{8}$, - $\frac{1}{16}$)	11.1 ($\frac{7}{16}$) +3.2, -1.6 (+$\frac{1}{8}$, - $\frac{1}{16}$)	12.7 ± 3.2 ($\frac{1}{2}$ ± $\frac{1}{8}$)	**14.3 ($\frac{9}{16}$)** **+4.8, -1.6 (+$\frac{3}{16}$, -$\frac{1}{16}$)**
Over 152-229 (6-9)	11.1 ($\frac{7}{16}$) +3.2, -1.6 (+$\frac{1}{8}$, - $\frac{1}{16}$)	12.7 ± 3.2 ($\frac{1}{2}$ ± $\frac{1}{8}$)	14.3 ($\frac{9}{16}$) +4.8, -1.6 (+$\frac{3}{16}$, -$\frac{1}{16}$)	**15.9 ($\frac{5}{8}$)** **+4.8, -3.2 (+$\frac{3}{16}$, -$\frac{1}{8}$)**
Over 229-305 (9-12)	12.7 ± 3.2 ($\frac{1}{2}$ ± $\frac{1}{8}$)	14.3 ($\frac{9}{16}$)	15.9 ($\frac{5}{8}$)	**19.1 ± 4.8 ($\frac{3}{4}$ ± $\frac{3}{16}$)**

		+4.8, -1.6 (+$\frac{3}{16}$, -$\frac{1}{16}$)	+4.8, -3.2 (+$\frac{3}{16}$, -$\frac{1}{8}$)	
Over 305-457 (12-18)	19.1 ± 4.8 ($\frac{3}{4}$ ± $\frac{3}{16}$)	19.1 ± 4.8 ($\frac{3}{4}$ ± $\frac{3}{16}$)	25.4 ± 6.4 (1 ± $\frac{1}{4}$)	**25.4 ± 6.4 (1 ± $\frac{1}{4}$)**
Over 457-610 (18-24)	31.8 ± 7.9 (1$\frac{1}{4}$ ± $\frac{5}{16}$	31.8 ± 7.9 (1$\frac{1}{4}$ ± $\frac{5}{16}$	31.8 ± 7.9 (1$\frac{1}{4}$ ± $\frac{5}{16}$	**31.8 ± 7.9 (1$\frac{1}{4}$ ± $\frac{5}{16}$**
Over 610-762 (24-30)	38.1 ± 9.5 (1$\frac{1}{2}$ ± $\frac{3}{8}$)	38.1 ± 9.5 (1$\frac{1}{2}$ ± $\frac{3}{8}$)	38.1 ± 9.5 (1$\frac{1}{2}$ ± $\frac{3}{8}$)	**38.1 ± 9.5 (1$\frac{1}{2}$ ± $\frac{3}{8}$)**
Over 762-914 (30-36)	44.5 ± 11.1 (1$\frac{3}{4}$ ± $\frac{7}{16}$)	44.5 ± 11.1 (1$\frac{3}{4}$ ± $\frac{7}{16}$)	44.5 ± 11.1 (1$\frac{3}{4}$ ± $\frac{7}{16}$)	**44.5 ± 11.1 (1$\frac{3}{4}$ ± $\frac{7}{16}$)**
Over 914-1070 (36-42)	50.8 ± 12.7 (2 ± $\frac{1}{2}$)	50.8 ± 12.7 (2 ± $\frac{1}{2}$)	50.8 ± 12.7 (2 ± $\frac{1}{2}$)	**50.8 ± 12.7 (2 ± $\frac{1}{2}$)**
Over 1070-1220 (42-48)	57.2 ± 14.3 (2$\frac{1}{4}$ ± $\frac{9}{16}$)	57.2 ± 14.3 (2$\frac{1}{4}$ ± $\frac{9}{16}$)	57.2 ± 14.3 (2$\frac{1}{4}$ ± $\frac{9}{16}$)	**57.2 ± 14.3 (2$\frac{1}{4}$ ± $\frac{9}{16}$)**
Over 1220-1370 (48-54)	63.5 ± 15.9 (2$\frac{1}{2}$ ± $\frac{5}{8}$)	63.5 ± 15.9 (2$\frac{1}{2}$ ± $\frac{5}{8}$)	63.5 ± 15.9 (2$\frac{1}{2}$ ± $\frac{5}{8}$)	**63.5 ± 15.9 (2$\frac{1}{2}$ ± $\frac{5}{8}$)**
Over 1370-1520 (54-60)	**69.8 ± 17.5 (2$\frac{3}{4}$ ± $\frac{11}{16}$)**	**69.8 ± 17.5 (2$\frac{3}{4}$ ± $\frac{11}{16}$)**	**69.8 ± 17.5 (2$\frac{3}{4}$ ± $\frac{11}{16}$)**	69.8 ± 17.5 (2$\frac{3}{4}$ ± $\frac{11}{16}$)

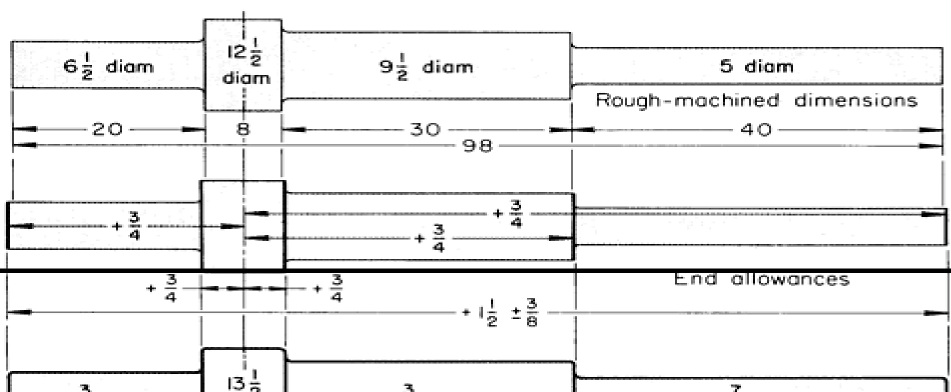
Allowance for overall rough-machined length, mm (in.), of:					
Over 3050-4060 (120-160)	**Over 4060-5080 (160-200)**	**Over 5080-7620 (200-300)**	**Over 7620-10160 (300-400)**	**Over 10160-12700 (400-500)**	Over 12700-15240 (500-600)
14.3 ($\frac{9}{16}$) +4.8, -1.6 (+$\frac{3}{16}$, -$\frac{1}{16}$)	15.9 ($\frac{5}{8}$) +4.8, -3.2 (+$\frac{3}{16}$, -$\frac{1}{8}$)	25.4 ± 6.4 (1 ± $\frac{1}{4}$)	31.8 ± 7.9 (1$\frac{1}{4}$ ± $\frac{5}{16}$	. . .	**. . .**
15.9 ($\frac{5}{8}$) +4.8, -3.2 (+$\frac{3}{16}$, -$\frac{1}{8}$)	19.1 ± 4.8 ($\frac{3}{4}$ ± $\frac{3}{16}$)	25.4 ± 6.4 (1 ± $\frac{1}{4}$)	31.8 ± 7.9 (1$\frac{1}{4}$ ± $\frac{5}{16}$)	. . .	**. . .**
19.1 ± 4.8 ($\frac{3}{4}$ ± $\frac{3}{16}$)	22.2 ($\frac{7}{8}$) +6.4, -4.8 (+ $\frac{1}{4}$, -$\frac{3}{16}$)	31.8 ± 7.9 (1$\frac{1}{4}$ ± $\frac{5}{16}$)	38.1 ± 9.5 (1$\frac{1}{2}$ ± $\frac{3}{8}$)	44.5 ± 11.1 (1$\frac{3}{4}$ ± $\frac{7}{16}$)	**50.8 ± 12.7 (2 ± $\frac{1}{2}$)**

22.2 ($\frac{7}{8}$) +6.4, -4.8 (+$\frac{1}{4}$, -$\frac{3}{16}$)	25.4 ± 6.4 (1 ± $\frac{1}{4}$)	31.8 ± 7.9 ($1\frac{1}{4}$ ± $\frac{5}{16}$)	38.1 ± 9.5 ($1\frac{1}{2}$ ± $\frac{3}{8}$)	44.5 ± 11.1 ($1\frac{3}{4}$ ± $\frac{7}{16}$)	**50.8 ± 12.7 (2 ± $\frac{1}{2}$)**
31.8 ± 7.9 ($1\frac{1}{4}$ ± $\frac{5}{16}$)	31.8 ± 7.9 ($1\frac{1}{4}$ ± $\frac{5}{16}$)	38.1 ± 9.5 ($1\frac{1}{2}$ ± $\frac{3}{8}$)	44.5 ± 11.1 ($1\frac{3}{4}$ ± $\frac{7}{16}$)	50.8 ± 12.7 (2 ± $\frac{1}{2}$)	**57.2 ± 14.3 ($2\frac{1}{4}$ ± $\frac{9}{16}$)**
38.1 ± 9.5 ($1\frac{1}{2}$ ± $\frac{3}{8}$)	38.1 ± 9.5 ($1\frac{1}{2}$ ± $\frac{3}{8}$)	44.5 ± 11.1 ($1\frac{3}{4}$ ± $\frac{7}{16}$)	50.8 ± 12.7 (2 ± $\frac{1}{2}$)	57.2 ± 14.3 ($2\frac{1}{4}$ ± $\frac{9}{16}$)	**63.5 ± 15.9 ($2\frac{1}{2}$ ± $\frac{5}{8}$)**
44.5 ± 11.1 ($1\frac{3}{4}$ ± $\frac{7}{16}$)	44.5 ± 11.1 ($1\frac{3}{4}$ ± $\frac{7}{16}$)	50.8 ± 12.7 (2 ± $\frac{1}{2}$)	57.2 ± 14.3 ($2\frac{1}{4}$ ± $\frac{9}{16}$)	63.5 ± 15.9 ($2\frac{1}{2}$ ± $\frac{5}{8}$)	**69.8 ± 17.5 ($2\frac{3}{4}$ ± $\frac{11}{16}$)**
50.8 ± 12.7 (2 ± $\frac{1}{2}$)	50.8 ± 12.7 (2 ± $\frac{1}{2}$)	57.2 ± 14.3 ($2\frac{1}{4}$ ± $\frac{9}{16}$)	63.5 ± 15.9 ($2\frac{1}{2}$ ± $\frac{5}{8}$)	69.8 ± 17.5 ($2\frac{3}{4}$ ± $\frac{11}{16}$)	**76.2 ± 19.1 (3 ± $\frac{3}{4}$)**
57.2 ± 14.3 ($2\frac{1}{4}$ ± $\frac{9}{16}$)	57.2 ± 14.3 ($2\frac{1}{4}$ ± $\frac{9}{16}$)	63.5 ± 15.9 ($2\frac{1}{2}$ ± $\frac{5}{8}$)	69.8 ± 17.5 ($2\frac{3}{4}$ ± $\frac{11}{16}$)	76.2 ± 19.1 (3 ± $\frac{3}{4}$)	**82.6 ± 20.6 ($3\frac{1}{4}$ ± $\frac{13}{16}$)**
63.5 ± 15.9 ($2\frac{1}{2}$ ± $\frac{5}{8}$)	63.5 ± 15.9 ($2\frac{1}{2}$ ± $\frac{5}{8}$)	69.8 ± 17.5 ($2\frac{3}{4}$ ± $\frac{11}{16}$)	76.2 ± 19.1 (3 ± $\frac{3}{4}$)	82.6 ± 20.6 ($3\frac{1}{4}$ ± $\frac{13}{16}$)	**88.9 ± 22.2 ($3\frac{1}{2}$ ± $\frac{7}{8}$)**
69.8 ± 17.5 ($2\frac{3}{4}$ ± $\frac{11}{16}$)	69.8 ± 17.5 ($2\frac{3}{4}$ ± $\frac{11}{16}$)	76.2 ± 19.1 (3 ± $\frac{3}{4}$)	82.6 ± 20.6 ($3\frac{1}{4}$ ± $\frac{13}{16}$)	88.9 ± 22.2 ($3\frac{1}{2}$ ± $\frac{7}{8}$)	**95.3 ± 23.8 ($3\frac{3}{4}$ ± $\frac{15}{16}$)**
76.2 ± 19.1 (3 ± $\frac{3}{4}$)	**76.2 ± 19.1 (3 ± $\frac{3}{4}$)**	**82.6 ± 20.6 ($3\frac{1}{4}$ ± $\frac{13}{16}$)**	**88.9 ± 22.2 ($3\frac{1}{2}$ ± $\frac{7}{8}$)**	**95.3 ± 23.8 ($3\frac{3}{4}$ ± $\frac{15}{16}$)**	**101.6 ± 25.4 (4 ± 1)**

Allowances and tolerances for as-forged shoulder shafts

A shaft forging that has more than one cross-sectional dimension is illustrated at right. To compute allowances and tolerances for a forging of this type, use the following method:

For the largest diameter, take the allowance given in the table above, using the overall length of the forging.



For each smaller diameter, take allowance given in table above, using overall length of forging, and average this with allowance for largest diameter. Use next-larger allowance wherever calculated average is not found. Allowance on each end of the overall length is the value indicated in the first column for the largest diameter or the value indicated on the top line for the overall length, whichever is greater. Allowance on each end of intermediate lengths is same as allowance on each end of overall length. Tolerance is as indicated in the table above for the allowance that is applied.	
Applying the rules given above to the forging illustrated at right:	

Allowances and tolerances for diameters

Machined dimension, mm (in.)	**Allowance, mm (in.)**	**Forging dimension, mm (in.)**	Tolerance on forging, mm (in.)[a]
318 (12 $\frac{1}{2}$)	25.4 (1)	343 (13 $\frac{1}{2}$)	**±6.4 (± $\frac{1}{4}$)**
241 (9 $\frac{1}{2}$)	22.2 [(19.1 + 25.4) ÷ 2] ($\frac{7}{8}$ [($\frac{3}{4}$ + 1) ÷ 2])	264 (10 $\frac{3}{8}$)	**+6.4, -4.8 (+ $\frac{1}{4}$, - $\frac{3}{16}$)**
165 (6 $\frac{1}{2}$)	22.2 [(15.9 + 25.4) ÷ 2] ($\frac{7}{8}$ [($\frac{5}{8}$ + 1) ÷ 2])[b]	187 (7 $\frac{3}{8}$)	**+6.4, -4.8 (+ $\frac{1}{4}$, - $\frac{3}{16}$)**
127 (5)	**22.2 [(14.3 + 25.4) ÷ 2] ($\frac{7}{8}$ [($\frac{9}{16}$ + 1) ÷ 2])[b]**	**149 (5 $\frac{7}{8}$)**	**+6.4, -4.8 (+ $\frac{1}{4}$, - $\frac{3}{16}$)**

Allowances and tolerances for ends

Table allowance for 2490 mm (98 in.) length	**12.7 mm ($\frac{1}{2}$ in.)**
Table allowance for 318 mm (12 $\frac{1}{2}$ in.) diameter	**19.1 mm ($\frac{3}{4}$ in.)**
End allowance applicable (point 3 above)	**19.1 mm ($\frac{3}{4}$ in.) per end**
Tolerance on 19.1 mm ($\frac{3}{4}$ in.) end allowance	**4.8 mm ($\frac{3}{16}$ in.) per end; 9.5 mm ($\frac{3}{8}$ in.) on total length**

(a) From the table, for allowances of 25.4 and 22.2 mm (1 and $\frac{7}{8}$ in.).

(b) Because product is not in the table, the next-larger allowance is used (as noted in item 2 in the list of instructions at left above). Dimensions in figure given in inches

Under precisely controlled conditions and with state-of-the-art thickness-controlled presses manned by highly skilled operators, it may be possible to forge somewhat closer to rough-machined dimensions; however, such a decrease in allowances must be carefully controlled to avoid machining problems. For example, usual practice may consist of increasing the allowance for critical applications in which all decarburization must be removed during rough machining. Under these conditions, 6.4 mm ($\frac{1}{4}$ in.) on a diameter or cross section (3.2 mm, or $\frac{1}{8}$ in., per side) is usually added to the allowance given in Table 1.

Tolerance describes the permissible variation in a specific dimension. Tolerances on allowances are given in Table 1. Tolerance is approximately one-fourth (plus or minus) the allowance.

Flatness and concentricity for a forging are usually negotiated between the forge shop and the customer. However, some users of open-die forgings have established specifications. For example, one user specifies that for pancake forgings up to 610 mm (24 in.) in diameter eccentricity or out-of-roundness shall not exceed 6.4 mm ($\frac{1}{4}$ in.) and flatness shall be within 4.8 mm ($\frac{3}{16}$ in.). For pancake forgings somewhat larger than 610 mm (24 in.) in diameter, eccentricity or out-of-roundness shall be no more than 9.5 mm ($\frac{3}{8}$ in.), and flatness shall be within 6.4 mm ($\frac{1}{4}$ in.).

Safety

In open-die forging, as in other types of forging operations, safe practices must be observed when handling materials and operating equipment. More information on safety in a forging facility is available in the article "Hammers and Presses for Forging" in this Volume.

Open-Die Forging

Revised by the ASM Committee on Open-Die Forging*; Chairman: Ashok K. Khare, National Forge Company

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Closed-Die Forging in Hammers and Presses

Introduction

CLOSED-DIE FORGING, or impression-die forging, is the shaping of hot metal completely within the walls or cavities of two dies that come together to enclose the workpiece on all sides. The impression for the forging can be entirely in either die or can be divided between the top and bottom dies.

The forging stock, generally round or square bar, is cut to length to provide the volume of metal needed to fill the die cavities, in addition to an allowance for flash and sometimes for a projection for holding the forging. The flash allowance is, in effect, a relief valve for the extreme pressure produced in closed dies. Flash also acts as a brake to slow the outward flow of metal in order to permit complete filling of the desired configuration.

Capabilities of the Process

With the use of closed dies, complex shapes and heavy reductions can be made in hot metal within closer dimensional tolerances than are usually feasible with open dies. Open dies are primarily used for the forging of simple shapes or for making forgings that are too large to be contained in closed dies. Closed-die forgings are usually designed to require minimal subsequent machining.

Closed-die forging is adaptable to low-volume or high-volume production. In addition to producing final, or nearly final, metal shapes, closed-die forging allows control of grain flow direction, and it often improves mechanical properties in the longitudinal direction of the workpiece.

Size. The forgings produced in closed dies can range from a few ounces to several tons. The maximum size that can be produced is limited only by the available handling and forging equipment. Forgings weighing as much as 25,400 kg (56,000 lb) have been successfully forged in closed dies, although more than 70% of the closed-die forgings produced weigh 0.9 kg (2 lb) or less.

Shape. Complex nonsymmetrical shapes that require a minimum number of operations for completion can be produced by closed-die forging. In addition, the process can be used in combination with other processes to produce parts having greater complexity or closer tolerances than are possible by forging alone. Cold coining and the assembly of two or more closed-die forgings by welding are examples of other processes that can extend the useful range of closed-die forging.

Forging Materials

In closed-die forging, a material must satisfy two basic requirements. First, the material strength (or flow stress) must be low so that die pressures are kept within the capabilities of practical die materials and constructions, and, second, the forgeability of the material must allow the required amount of deformation without failure. By convention, closed-die forging refers to hot working. Table 1 lists various alloy groups and their respective forging temperature ranges in order of increasing forging difficulty. The forging material influences the design of the forging itself as well as the details of the entire forging process. For example, Fig. 1 shows that, owing to difficulties in forging, nickel alloys allow for less shape definition than aluminum alloys. For a given metal, both the flow stress and the forgeability are influenced by the metallurgical characteristics of the billet material and by the temperatures, strains, strain rates, and stresses that occur in the deforming material.

Table 1 Classification of alloys in order of increasing forging difficulty

Alloy group	Approximate forging temperature range	
	°C	°F
Least difficult		
Aluminum alloys	400-550	**750-1020**
Magnesium alloys	250-350	**480-660**
Copper alloys	600-900	**1110-1650**

Carbon and low-alloy steels	850-1150	**1560-2100**
Martensitic stainless steels	1100-1250	**2010-2280**
Maraging steels	1100-1250	**2010-2280**
Austenitic stainless steels	1100-1250	**2010-2280**
Nickel alloys	1000-1150	**1830-2100**
Semiaustenitic PH stainless steels	1100-1250	**2010-2280**
Titanium alloys	700-950	**1290-1740**
Iron-base superalloys	1050-1180	**1920-2160**
Cobalt-base superalloys	1180-1250	**2160-2280**
Niobium alloys	950-1150	**1740-2100**
Tantalum alloys	1050-1350	**1920-2460**
Molybdenum alloys	1150-1350	**2100-2460**
Nickel-base superalloys	1050-1200	**1920-2190**
Tungsten alloys	1200-1300	**2190-2370**
Most difficult		

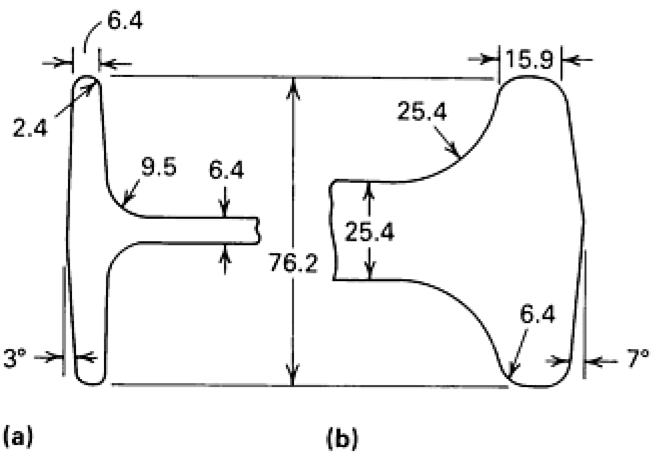

Fig. 1 Comparison of typical design limits for rib-web structural forgings of aluminum alloys (a) and nickel-base alloys (b). Dimensions given in millimeters.

In most practical hot-forging operations, the temperature of the workpiece material is higher than that of the dies. Metal flow and die filling are largely determined by the resistance and the ability of the forging material to flow, that is, flow stress and forgeability; by the friction and cooling effects at the die/material interface; and by the complexity of the forging shape. Of the two basic material characteristics, flow stress represents the resistance of a metal to plastic deformation, and forgeability represents the ability of a metal to deform without failure, regardless of the magnitude of load and stresses required for deformation.

The concept of forgeability has been used vaguely to denote a combination of resistance to deformation and the ability to deform without fracture. A diagram illustrating this type of information is presented in Fig. 2. Because the resistance of a metal to plastic deformation is essentially determined by the flow stress of the material at given temperature and strain rate conditions, it is more appropriate to define forgeability as the capability of the material to deform without failure, regardless of pressure and load requirements.

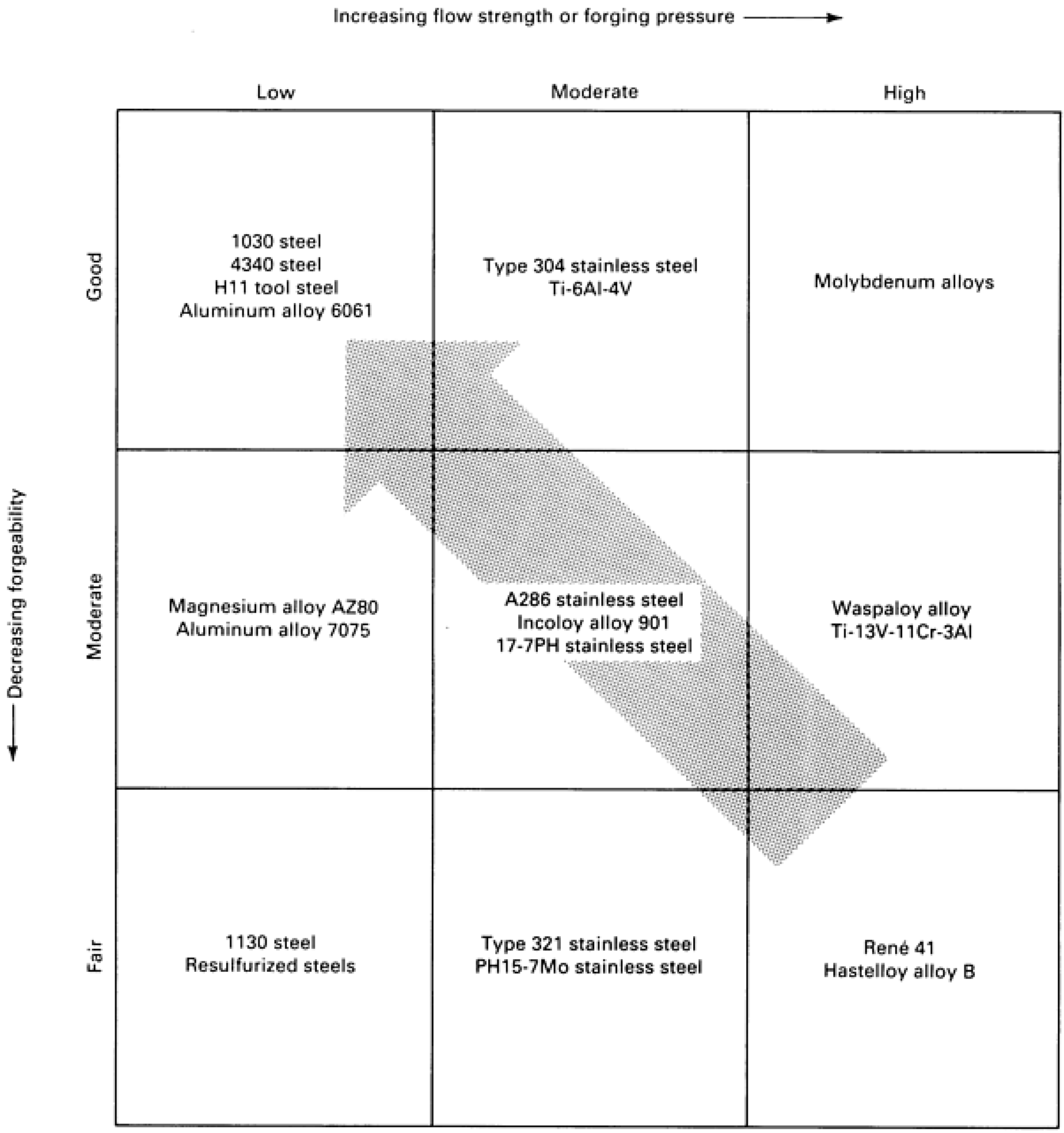

Fig. 2 Influence of forgeability and flow strength in die filling. Arrow indicates increasing ease of die filling.

In general, the forgeability of metals increases with temperature. However, as temperature increases, grain growth occurs, and in some alloy systems, forgeability decreases with increasing grain size. In other alloys, forgeability is greatly influenced by the characteristics of second-phase compounds. The state of stress in a given deformation process significantly influences forgeability. In upset forging at large reductions, for example, cracking may occur at the outside fibers of the billet, where excessive barreling occurs and tensile stresses develop. In certain extrusion-type forging operations, axial tensile stresses may be present in the deformation zone and may cause centerburst cracking. As a general and practical rule, it is important to provide compressive support to those portions of a less forgeable material that are normally exposed to the tensile and shear stresses.

The forgeability of metals at various deformation rates and temperatures can be evaluated by using such tests as torsion, tension, and compression tests. In all of these tests, the amount of deformation prior to failure of the specimen is an indication of forgeability at the temperature and deformation rate used during that particular test.

Closed-Die Forging in Hammers and Presses

Friction and Lubrication in Forging

In forging, friction greatly influences metal flow, pressure distribution, and load and energy requirements. In addition to lubrication effects, the effects of die chilling or heat transfer from the hot material to colder dies must be considered. For example, for a given lubricant, friction data obtained from hydraulic press forging cannot be used for mechanical press or hammer forging even if die and billet temperatures are comparable.

In forging, the ideal lubricant is expected to:

- Reduce sliding friction between the dies and the forging in order to reduce pressure requirements, to fill the die cavity, and to control metal flow
- Act as a parting agent and prevent local welding and subsequent damage to the die and workpiece surfaces
- Possess insulating properties so as to reduce heat losses from the workpiece and to minimize temperature fluctuations on the die surface
- Cover the die surface uniformly so that local lubricant breakdown and uneven metal flow are prevented
- Be nonabrasive and noncorrosive so as to prevent erosion of the die surface
- Be free of residues that would accumulate in deep impressions
- Develop a balanced gas pressure to assist quick release of the forging from the die cavity; this characteristic is particularly important in hammer forging, in which ejectors are not used
- Be free of polluting or poisonous components and not produce smoke upon application to the dies.

No single lubricant can fulfill all of the requirements listed above; therefore, a compromise must be made for each specific application.

Various types of lubricants are used, and they can be applied by swabbing or spraying. The simplest is a high flash point oil swabbed onto the dies. Colloidal graphite suspensions in either oil or water are frequently used. Synthetic lubricants can be employed for light forging operations. The water-base and synthetic lubricants are extensively used primarily because of cleanliness.

Closed-Die Forging in Hammers and Presses

Classification of Closed-Die Forgings

Closed-die forgings are generally classified as blocker-type, conventional, and close-tolerance.

Blocker-type forgings are produced in relatively inexpensive dies, but their weight and dimensions are somewhat greater than those of corresponding conventional closed-die forgings. A blocker-type forging approximates the general shape of the final part, with relatively generous finish allowance and radii. Such forgings are sometimes specified when only a small number of forgings are required and the cost of machining parts to final shape is not excessive.

Conventional closed-die forgings are the most common type and are produced to comply with commercial tolerances. These forgings are characterized by design complexity and tolerances that fall within the broad range of general forging practice. They are made closer to the shape and dimensions of the final part than are blocker-type forgings; therefore, they are lighter and have more detail.

Close-tolerance forgings are usually held to smaller dimensional tolerances than conventional forgings. Little or no machining is required after forging, because close-tolerance forgings are made with less draft, less material, and thinner walls, webs, and ribs. These forgings cost more and require higher forging pressures per unit of plan area than conventional forgings. However, the higher forging cost is sometimes justified by a reduction in machining cost.

Shape Complexity in Forging

Metal flow in forging is greatly influenced by part or die geometry. Several operations (preforming or blocking) are often needed to achieve gradual flow of the metal from an initially simple shape (cylinder or round-cornered square billet) into the more complex shape of the final forging. In general, spherical and blocklike shapes are the easiest to forge in impression or closed dies. Parts with long, thin sections or projections (webs and ribs) are more difficult to forge because they have more surface area per unit volume. Such variations in shape maximize the effects of friction and temperature changes and therefore influence the final pressure required to fill the die cavities. There is a direct relationship between the surface-to-volume ratio of a forging and the difficulty in producing that forging.

The ease of forging more complex shapes depends on the relative proportions of vertical and horizontal projections on the part. Figure 3 shows a schematic of the effects of shape on forging difficulties. The parts illustrated in Fig. 3(c) and 3(d) would require not only higher forging loads but also at least one more forging operation than the parts illustrated in Fig. 3(a) and 3(b) in order to ensure die filling.

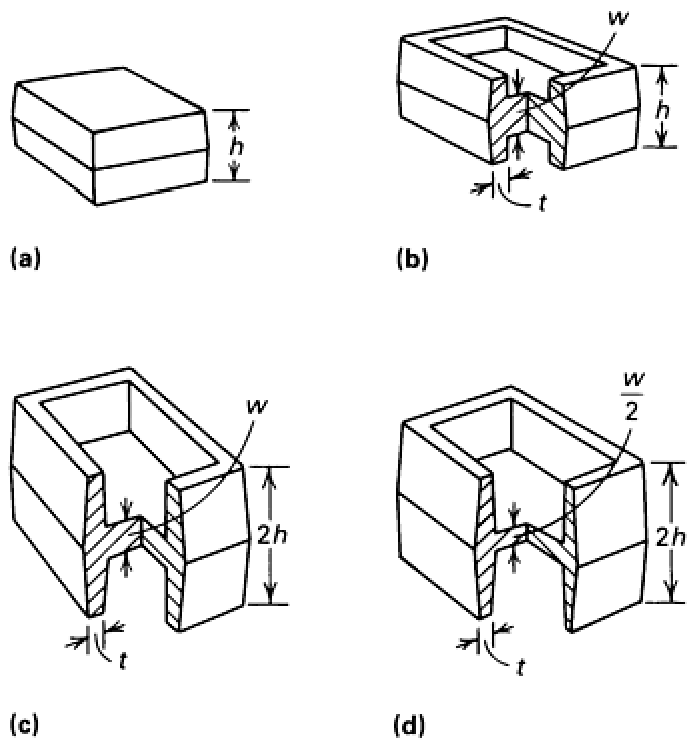

Fig. 3 Forging difficulty as a function of part geometry. Difficulty in forging increases from (a) to (d). (a)

Rectangular shape. (b) Rib-web part. (c) Part with higher rib. (d) Part with higher rib and thinner web.

As shown in Fig. 4, most forgings can be classified into three main groups. The first group consists of the so-called compact shapes, whose three major dimensions (length, l; width, w; and height, h) are approximately equal. The number of parts that fall into this group is rather small. The second group consists of disk shapes for which two of the three dimensions (l and w) are approximately equal and are greater than the height h. All round forgings belong in this group, which includes approximately 30% of all commonly used forgings. The third group consists of long shapes that have one major dimension significantly greater than the other two ($l > w \cdot h$). These three basic groups are further divided into subgroups depending on the presence and type of elements subsidiary to the basic shape.

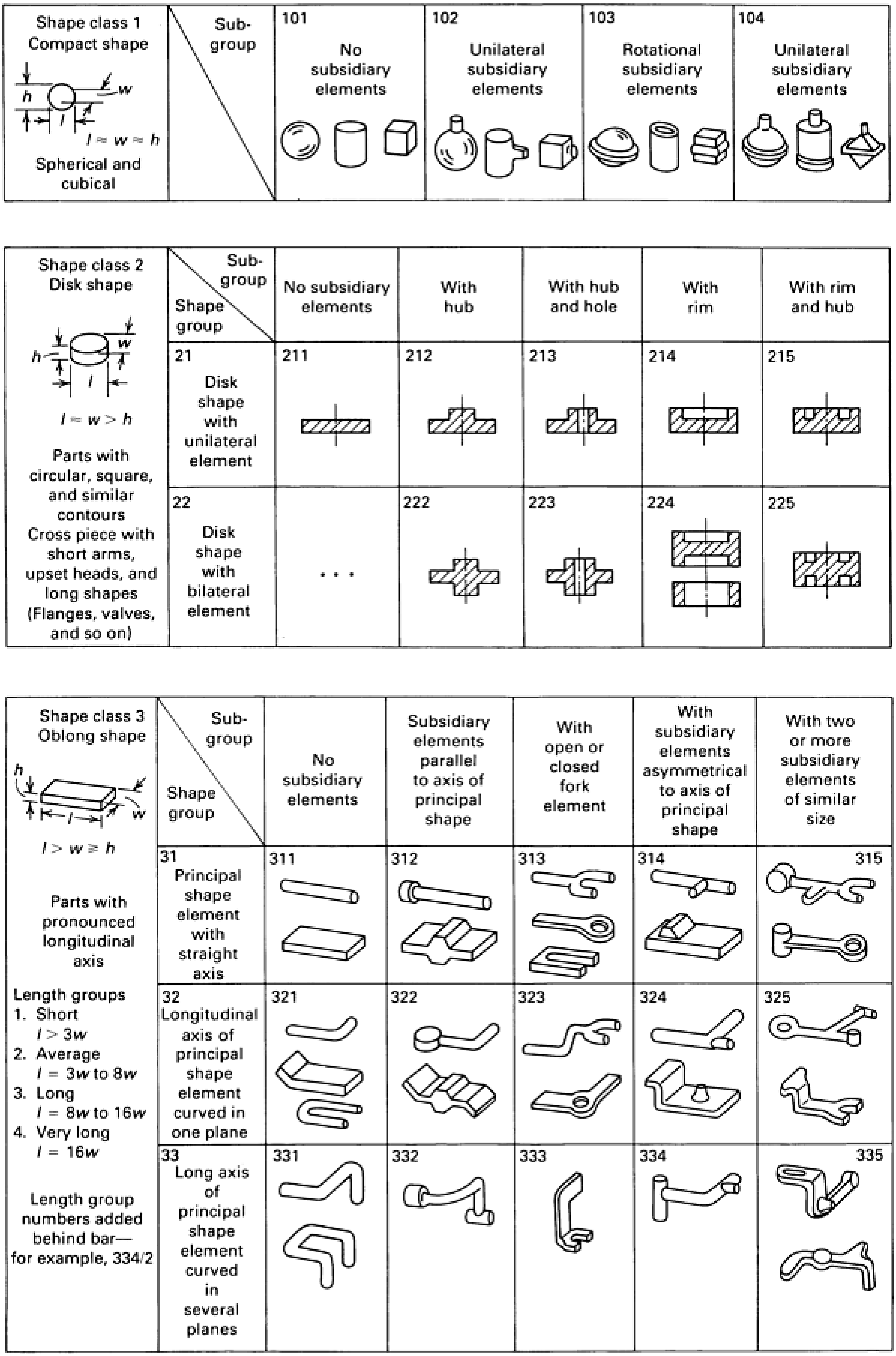

Fig. 4 Classification of forging shapes. See text for details.

This shape classification can be useful for practical purposes, such as estimating costs and predicting preforming steps. However, this method is not entirely quantitative and requires some subjective evaluation based on past experience.

Design of Blocker (Preform) Dies

One of the most important aspects of closed-die forging is proper design of preforming operations and of blocker dies to achieve adequate metal distribution. Therefore, in the finish-forging operation, defect-free metal flow and complete die filling can be achieved, and metal losses into the flash can be minimized. In preforming, round or round-cornered square stock with constant cross section is deformed such that a desirable volume distribution is achieved prior to the final closed-die forging operation. In blocking, the preform is die forged in a blocker cavity before finish forging.

The primary objective of preforming is to distribute the metal in the preform in order to:

- Ensure defect-free metal flow and adequate die filling
- Minimize the amount of material lost into flash
- Minimize die wear in the finish-forging cavity by reducing metal movement in this direction
- Achieve desired grain flow and control mechanical properties

Common practice in preform design is to consider planes of metal flow--that is, selected cross sections of the forging--as shown in Fig. 5. Several preforming operations may be required before a part can be successfully finish forged. In determining the various forging steps, it is first necessary to obtain the volume of the forging, based on the areas of successive cross sections throughout the forging. A volume distribution can be obtained by using the following procedure:

- Lay out a dimensioned drawing of the finish configuration, complete with flash
- Construct a baseline for area determination parallel to the centerline of the part
- Determine maximum and minimum cross-sectional areas perpendicular to the centerline of the part
- Plot these areas at proportional distances from the baseline
- Connect these points with a smooth curve. In cases in which it is not clear how the curve would best show the changing cross-sectional areas, plot additional points to assist in determining a smooth representative curve
- Above this curve, add the approximate area of the flash at each cross section, giving consideration to those sections where the flash should be widest. The flash will generally be of a constant thickness, but will be widest at the narrower sections and smallest at the wider sections
- Convert the maximum and minimum area values to round or rectangular shapes having the same cross-sectional areas

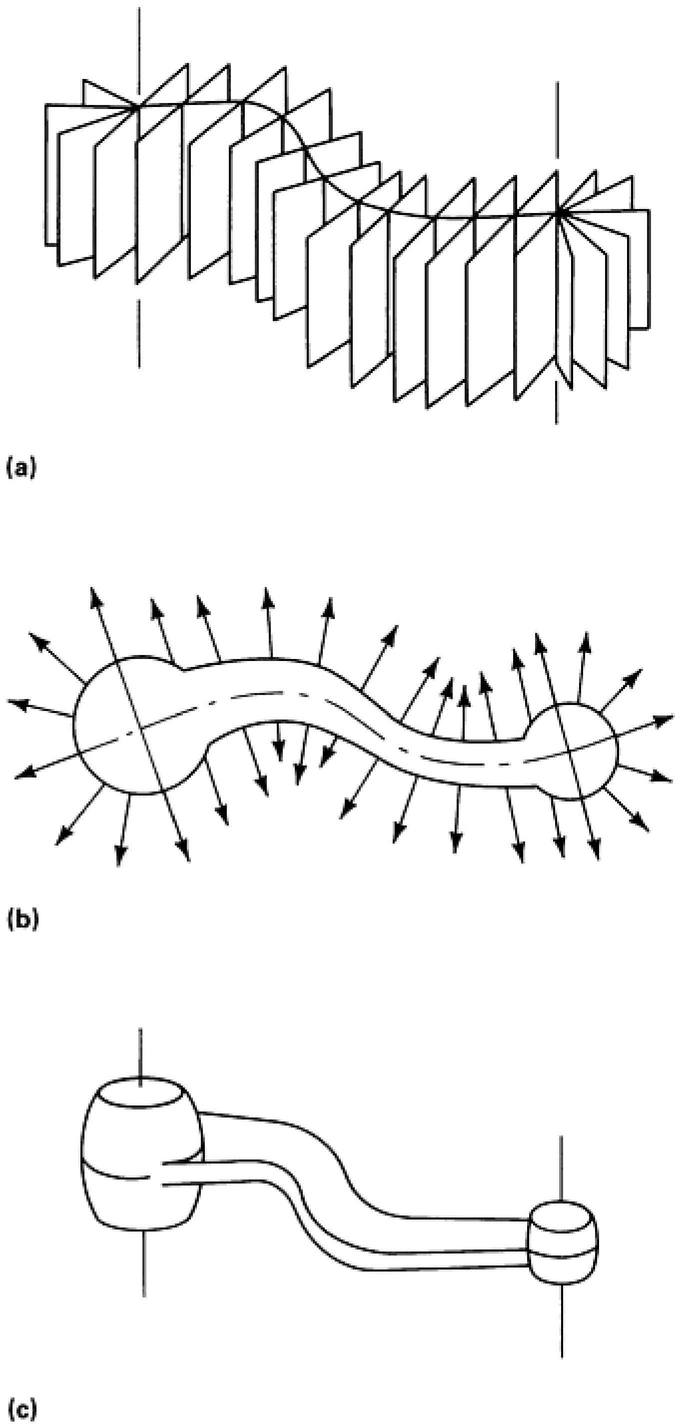

Fig. 5 Planes (a) and directions (b) of metal flow during the forging of a relatively complex shape. The finished forging is shown in (c).

In designing the cross sections of a blocker (preform) die impression, three basic rules must be followed:

- The area of each cross section along the length of the preform must be equal to the area of the finish cross section augmented by the area necessary for flash. Therefore, the initial stock distribution is obtained by determining the areas of cross sections along the main axis of the forging
- All the concave radii (including fillet radii) of the preform should be larger than the radii of the forged part
- When practical, the dimensions of the preform should be greater than those of the finished part in the forging direction so that metal flow is mostly of the upsetting type rather than the extrusion type. During

the finishing operation, the material will then be squeezed laterally toward the die cavity without additional shear at the die/material interface. Such conditions minimize friction and forging load and reduce wear along the die surfaces

Application of these three principles to steel forgings is illustrated in Fig. 6 for some solid cross sections. The qualitative principles of preform design are well known, but quantitative information is rarely available.

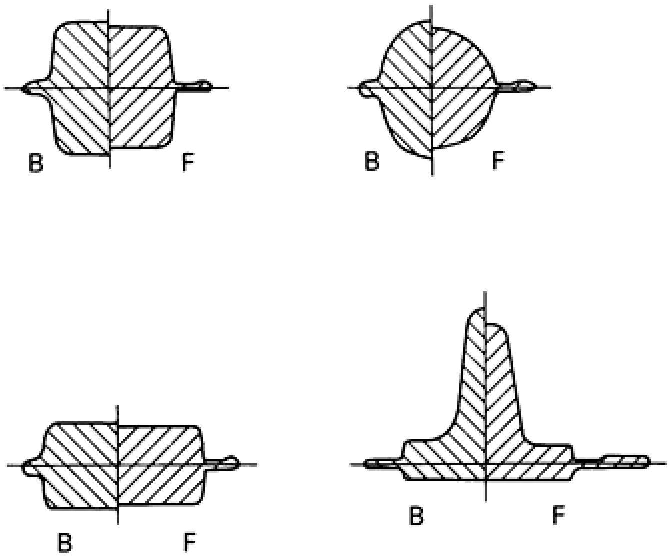

Fig. 6 Suggested blocker cross sections for steel forgings. B, blocker; F, finished forging.

For the forging of complex parts, empirical guidelines may not be sufficient, and trial-and-error procedures may be time consuming and costly. A more systematic and well-proven method for developing preform shapes is physical modeling, using a soft material such as lead, plasticine, or wax as a model forging material and hard plastic or low-carbon steel dies as tooling. Therefore, with relatively low-cost tooling and with some experimentation, preform shapes can be determined. Detailed information on physical modeling and the use of computer-aided design and manufacturing (CAD/CAM) for forging design is available in the Section "Computer-Aided Process Design for Bulk Forming" in this Volume. The use of CAD/CAM in die design is also discussed in the section "CAD/CAM of Forging Dies" in this article.

Flash Design

The influences of flash thickness and flash land width on forging pressure are reasonably well understood from a qualitative viewpoint (Fig. 7). Essentially, forging pressure increases with decreasing flash thickness and with increasing flash land width because of combinations of increasing restriction, increasing frictional forces, and decreasing metal temperatures at the flash gap.

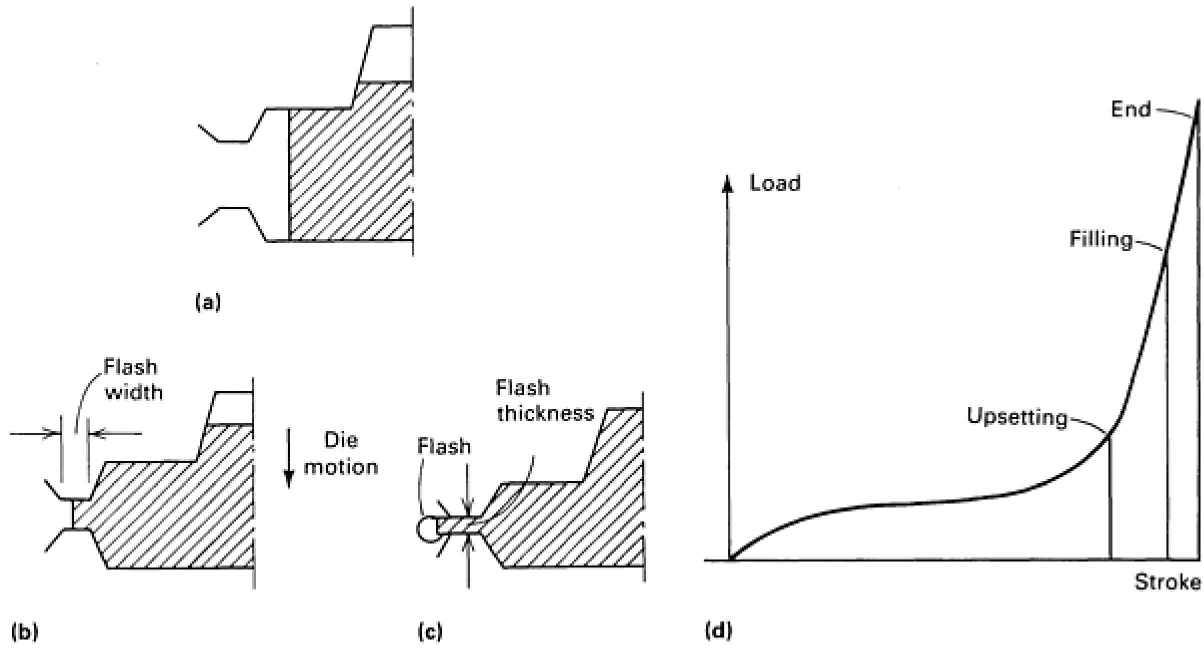

Fig. 7 Metal flow (a to c) and load-stroke curve (d) in closed-die forging. (a) Upsetting. (b) Filling. (c) End.

A typical load-versus-stroke curve for a closed-die forging is shown in Fig. 8. Loads are relatively low until the more difficult details are partly filled and the metal reaches the flash opening (Fig. 7). This stage corresponds to point P_1 in Fig. 8. For successful forging, two conditions must be fulfilled when this point is reached. First, a sufficient volume of metal must be trapped within the confines of the die to fill the remaining cavities, and second, extrusion of metal through the narrowing gap of the flash opening must be more difficult than filling the more intricate detail in the die.

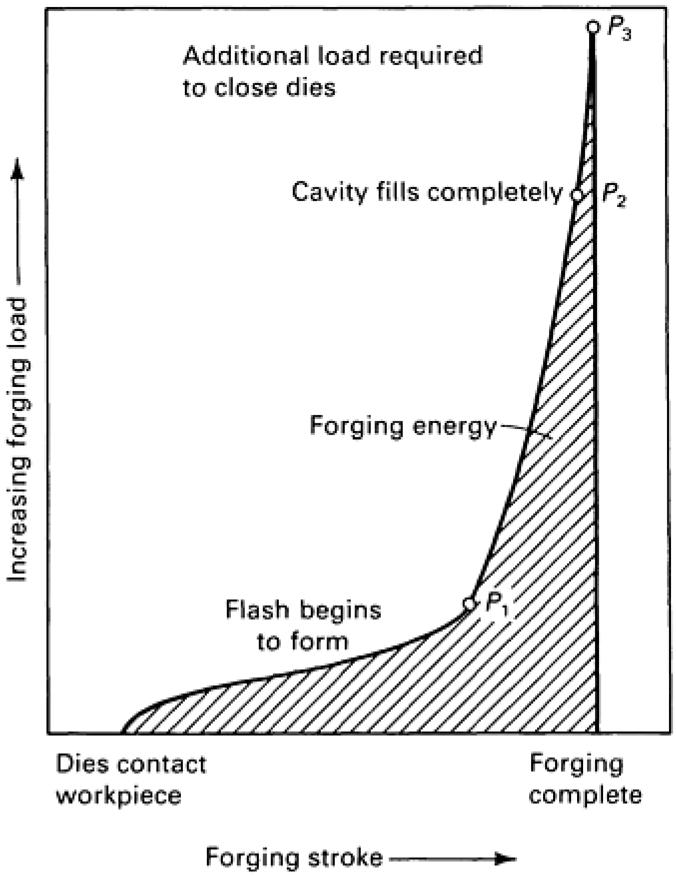

Fig. 8 Typical load-stroke curve for a closed-die forging showing three distinct stages.

As the dies continue to close, the load increases sharply to a point P_2, the stage at which the die cavity is filled completely. Ideally, at this point, the cavity pressure provided by the flash geometry should be just sufficient to fill the entire cavity, and the forging should be completed. However, P_3 represents the final load reached in normal practice for ensuring that the cavity is completely filled and that the forging has the proper dimensions. During the stroke from P_2 to P_3, all metal flow occurs near or in the flash gap, which in turn becomes more restrictive as the dies close. In this respect, the detail most difficult to fill determines the minimum load for producing a fully filled forging. Therefore, the dimensions of the flash determine the final load required for closing the dies. Formation of the flash, however, is greatly influenced by the amount of excess material available in the cavity, because this amount determines the instantaneous height of the extruded flash and therefore the die stresses.

A cavity can be filled with various flash geometries if there is always sufficient material in the die. Therefore, is it possible to fill the same cavity by using a less restrictive (thicker) flash and to do this at a lower total forging load if the necessary excess material is available (in this case, the advantages of lower forging load and lower cavity stress are offset by increased scrap loss) or if the workpiece is properly preformed (in which case low stresses and material losses are obtained by additional preforming).

The shape classification (Fig. 4) has been used in the systematic evaluation of flash dimensions in steel forgings. The results for shape group 224 are presented in Fig. 9 as an example. In general, the flash thickness is shown to increase with forging weight, while the ratio of flash land width to flash thickness decreases to a limiting value.

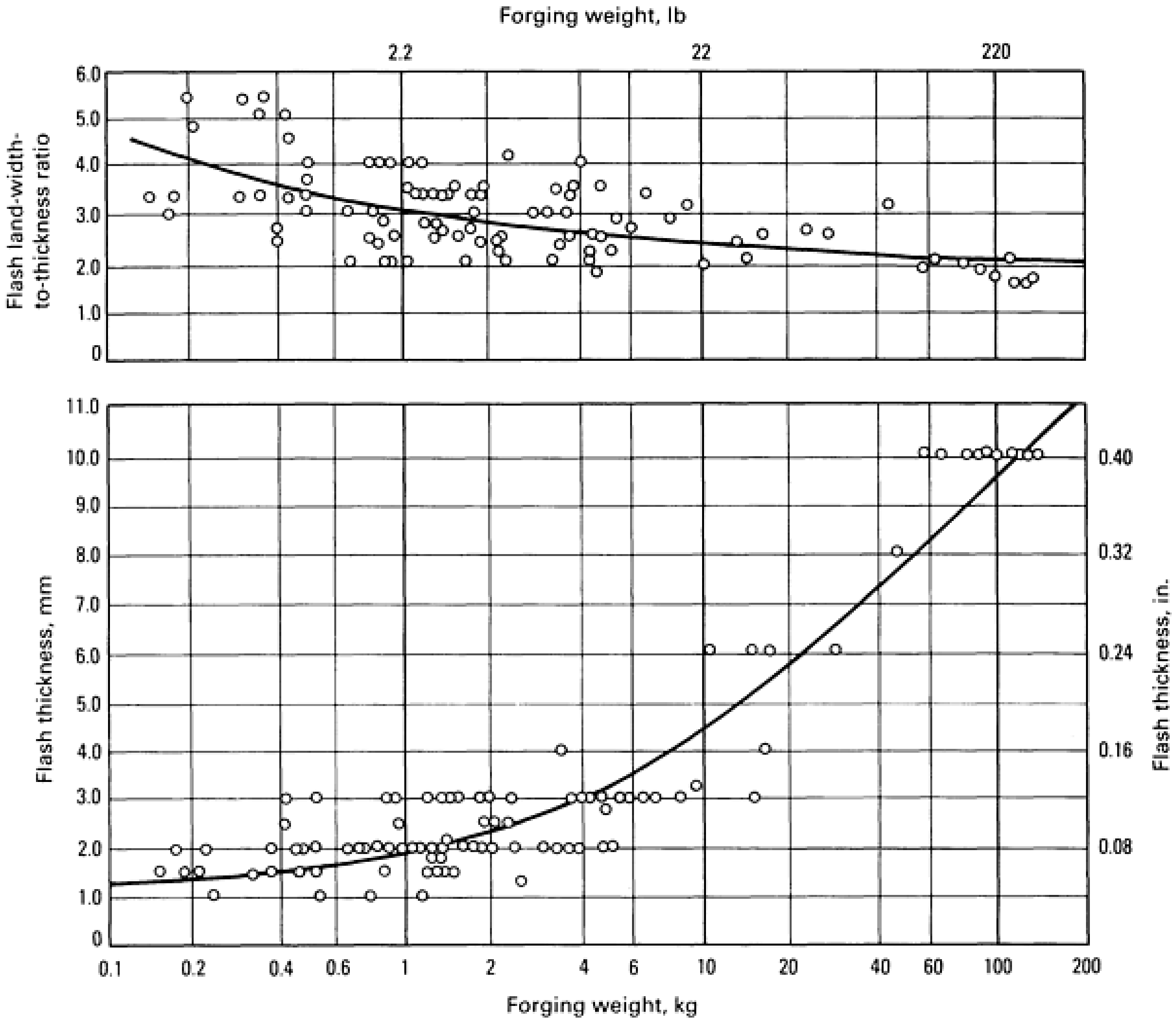

Fig. 9 Variations in flash land-width-to-thickness ratio (top) and in flash thickness (bottom) with forging weight for carbon and alloy steel forgings in shape group 224 (see Fig. 4).

Prediction of Forging Pressure

It is often necessary to predict forging pressure so that a suitable press can be selected and so that die stresses can be prevented from exceeding allowable limits. In estimating the forging load empirically, the surface area of the forging, including the flash zone, is multiplied by an average forging pressure known from experience. The forging pressures encountered in practice vary from 56 to 98 kg/mm^2 (80 to 140 ksi), depending on the material and the geometrical configuration of the part. Figure 10 shows forging pressures for parts made of various carbon (up to 0.6% C) and low-alloy steels. In these trials, flash land-width-to-thickness ratios from 2 to 4 were used. The variable that most influences forging pressure is the average height of the forging. The lower curve in Fig. 10 relates to relatively simple parts, and the upper curve to more difficult-to-forge parts.

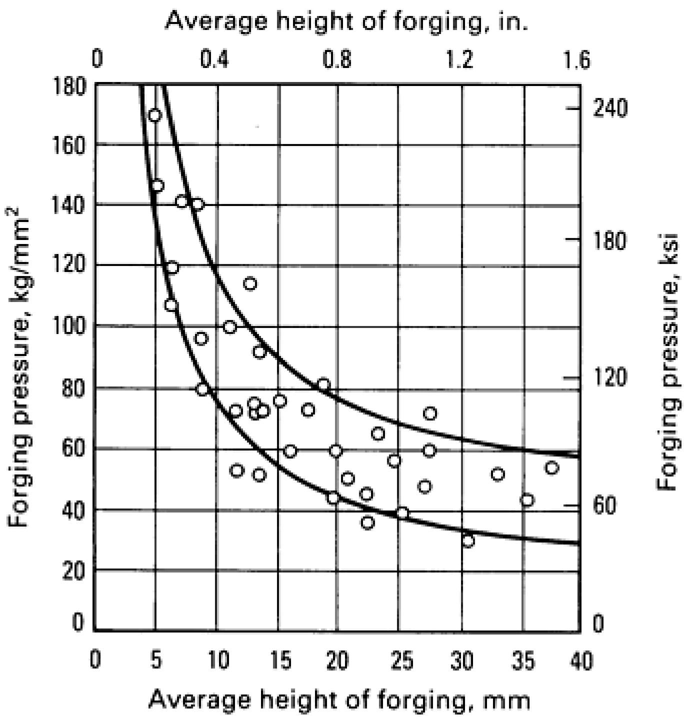

Fig. 10 Forging pressure versus average height of forging for carbon and low-alloy steel forgings. Lower curve is for relatively simple parts; upper curve relates to more difficult-to-forge part geometries. Data are for flash land-to-thickness ratios from 2 to 4.

Most empirical methods, summarized in terms of simple formulas or nomograms, are not sufficiently general for predicting forging loads for a variety of parts and materials. Lacking a suitable empirical formula, one may use analytical or computer-aided techniques for calculating forging loads and stresses.

CAD/CAM of Forging Dies

During the last decade, computers have been used to an increasing extent for forging applications. The initial developments focused on the numerically controlled (NC) machining of forging dies. In the mid-1970s, computer-aided drafting and NC machining were also introduced for structural forgings and for forging steam turbine blades. During the early 1980s several companies began to use stand-alone CAD/CAM systems--normally used for mechanical designs, drafting, and NC machining--for the design and manufacture of forging dies.

Stand-alone CAD/CAM systems are commercially available and have the necessary software for computer-aided drafting and NC machining. A typical CAD/CAM system consists of a microcomputer or minicomputer, a graphics display terminal, a keyboard, a digitizer with menu for data entry, an automatic drafting machine, and hardware for information storage and NC tape punching or floppy disk preparation. Such systems also allow, at various levels of automation, three-dimensional representation of the forging and the possibility of zooming and rotating the forging-geometry display on the graphics terminal screen for the purpose of visual inspection. These systems also allow sectioning of a given forging, that is, the description, drawing, and display of desired forging cross sections for the purpose of die stress and metal flow analyses. Therefore, the results can be displayed for easy interaction between the designer and the computer system, modifications to die design can be easily made, and alternatives can be explored.

The ultimate advantage to computer-aided design in forging is achieved when reasonably accurate and inexpensive computer software is available for simulating metal flow throughout a forging operation (Fig. 11). In this case, forging experiments can be conducted on a computer by simulating the finish forging that would result from an assumed or selected blocker design, and the results can be displayed on a graphics terminal. If the simulation indicates that the selected blocker design would not fill the finisher die or that too much material would be wasted, another blocker design can be selected and the computer simulation, or trial, can be repeated. Such computer-aided simulations reduce the required number of expensive die tryouts. More information on CAD/CAM in forging design is available in the Section "Computer-Aided Process Design for Bulk Forming" in this Volume.

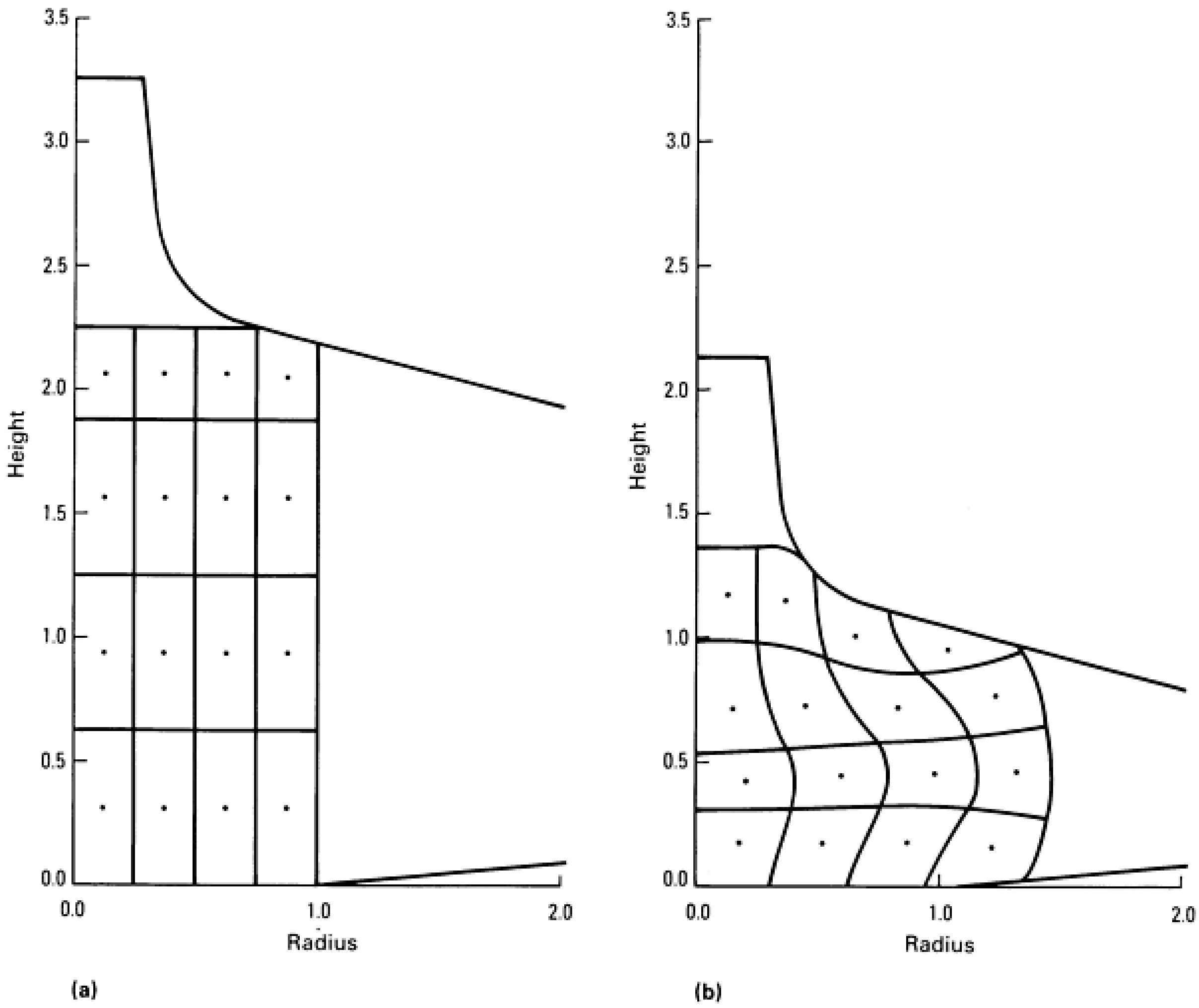

Fig. 11 Computer simulation of deformation in the forging of an axisymmetric spike. (a) Undeformed grid. (b) Deformation at a die stroke of one-half the initial billet height.

Equipment for Closed-Die Forging

Hammers and Presses. The various types of hammers and presses used to provide the force for closed-die forging are described in the article "Hammers and Presses for Forging" in this Volume. Capacities and ratings of each major type of press or hammer are discussed in the article "Selection of Forging Equipment" in this Volume.

Dies for closed-die forging are discussed in detail in the article "Dies and Die Materials for Hot Forging" in this Volume.

Cutting of bar stock can be accomplished by cold or hot shearing, sawing, abrasive cutting, and thermal or electric arc cutting. These operations, as well as equipment used for cutting, are described in the Section "Shearing, Slitting, and Cutting" in this Volume.

Heating Equipment. There are wide variations in the forging temperature ranges for various materials (Table 1). These differences, along with differences in stock and the availability of various fuels, have resulted in a wide variety of heating equipment. Various types of electric and fuel-fired furnaces are used, as well as resistance and induction heating. Regardless of the heating method used, temperature and atmospheric conditions within the heating unit must be controlled to ensure that the forgings subsequently produced will develop the optimal microstructure and properties.

Forging Temperatures for Steels

Maximum safe forging temperatures for carbon and alloy steels are given in Table 2, which indicates that forging temperature decreases as carbon content increases. The higher the forging temperature, the greater the plasticity of the steel, which results in easier forging and less die wear; however, the danger of overheating and excessive grain coarsening is increased. If a steel that has been heated to its maximum safe temperature is forged rapidly and with large reduction, the energy transferred to the steel during forging can substantially increase its temperature, thus causing overheating.

Table 2 Maximum safe forging temperatures for carbon and alloy stools of various carbon contents

Carbon content, %	Maximum safe forging temperature			
	Carbon steels		Alloy steels	
	°C	**°F**	**°C**	°F
0.10	1290	2350	1260	**2300**
0.20	1275	2325	1245	**2275**
0.30	1260	2300	1230	**2250**
0.40	1245	2275	1230	**2250**
0.50	1230	2250	1230	**2250**
0.60	1205	2200	1205	**2200**

0.70	1190	2175	1175	**2150**
0.90	1150	2100	. . .	**. . .**
1.10	**1110**	**2025**	**. . .**	**. . .**

The effect of carbon content on forging temperature is the same for most tool steels as for carbon and alloy steels. However, the complex alloy compositions of some tool steels have different effects on forging temperature. Forging temperatures for tool steels are listed in Table 3.

Table 3 Recommended forging temperature ranges for tool steels

Steels	Forging temperatures					
	Preheat slowly to:		**Begin forging at[(a)]:**		Do not forge below:	
	°C	**°F**	**°C**	**°F**	**°C**	°F
Water-hardening tool steels						
W1-W5	790	1450	980-1095[(b)]	1800-2000[(b)]	815	**1500**
Shock-resisting tool steels						
S1, S2, S4, S5	815	1500	1040-1150	1900-2100	870	**1600**
Oil-hardening cold-work tool steels						
O1	815	1500	980-1065	1800-1950	845	**1550**
O2	815	1500	980-1040	1800-1900	845	**1550**
O7	815	1500	980-1095	1800-2000	870	**1600**
Medium-alloy air-hardening cold-work tool steels						
A2, A4, A5, A6	870	1600	1010-1095	1850-2000	900	**1650**
High-carbon high-chromium cold-work tool steels						
D1-D6	900	1650	980-1095	1800-2000	900	**1650**

Chromium hot-work tool steels						
H11, H12, H13	900	1650	1065-1175	1950-2150	900	**1650**
H14, H16	900	1650	1065-1175	1950-2150	925	**1700**
H15	845	1550	1040-1150	1900-2100	900	**1650**
Tungsten hot-work tool steels						
H20, H21, H22	870	1600	1095-1205	2000-2200	900	**1650**
H24, H25	900	1650	1095-1205	2000-2200	925	**1700**
H26	900	1650	1095-1205	2000-2200	955	**1750**
Molybdenum high-speed tool steels						
M1, M10	815	1500	1040-1150	1900-2100	925	**1700**
M2	815	1500	1065-1175	1950-2150	925	**1700**
M4	815	1500	1095-1175	2000-2150	925	**1700**
M30, M34, M35, M36	815	1500	1065-1175	1950-2150	955	**1750**
Tungsten high-speed tool steels						
T1	870	1600	1065-1205	1950-2200	955	**1750**
T2, T4, T8	870	1600	1095-1205	1950-2200	955	**1750**
T3	870	1600	1095-1230	2000-2250	955	**1750**
T5, T6	870	1600	1095-1205	2000-2200	980	**1800**
Low-alloy special-purpose tool steels						
L1, L2, L6	815	1500	1040-1150	1900-2100	845	**1550**
L3	815	1500	980-1095	1800-2000	845	**1550**

Carbon-tungsten special-purpose tool steels						
F2, F3	815	1500	980-1095	1800-2000	900	**1650**
Low-carbon mold steels						
P1	. . .	. . .	1205-1290	2200-2350	1040	**1900**
P3	. . .	. . .	1040-1205	1900-2200	845	**1550**
P4	870	1600	1095-1230	2000-2250	900	**1650**
P20	**815**	**1500**	**1065-1230**	**1950-2250**	**815**	**1500**

(a) The temperature at which to begin forging is given as a range; the higher side of the range should be used for large sections and heavy or rapid reductions, and the lower side for smaller sections and lighter reductions. As the alloy content of the steel increases, the time of soaking at forging temperature increases proportionately. Similarly, as the alloy content increases, it becomes more necessary to cool slowly from the forging temperature. With very high alloy steels, such as high-speed steels and air-hardening steels, this slow cooling is imperative in order to prevent cracking and to leave the steel in a semisoft condition. Either furnace cooling of the steel or burying it in an insulating medium (such as lime, mica, or diatomaceous earth) is satisfactory.

(b) Forging temperatures for water-hardening tool steels vary with carbon content. The following temperatures are recommended: for 0.60-1.25% C, the range given; for 1.25 to 1.40% C, the low side of the range given.

Heating Time. For any steel, the heating time must be sufficient to bring the center of the forging stock to the forging temperature. A longer heating time than necessary results in excessive decarburization, scale, and grain growth. For stock measuring up to 75 mm (3 in.) in diameter, the heating time per inch of section thickness should be no more than 5 min for low-carbon and medium-carbon steels or no more than 6 min for low-alloy steel. For stock 75 to 230 mm (3 to 9 in.) in diameter, the heating time should be no more than 15 min per inch of thickness. For high-carbon steels (0.50% C and higher) and for highly alloyed steels, slower heating rates are required, and preheating at temperatures from 650 to 760 °C (1200 to 1400 °F) is sometimes necessary to prevent cracking.

Finishing temperature should always be well above the transformation temperature of the steel being forged in order to prevent cracking of the steel and excessive wear of the dies, but should be low enough to prevent excessive grain growth. For most carbon and alloy steels, 980 to 1095 °C (1800 to 2000 °F) is a suitable range for finish forging. More information on forging parameters for ferrous alloys is available in the articles "Forging of Carbon and Alloy Steels" and "Forging of Stainless Steel" in this Volume.

Forging Temperatures for Steels

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0.30	1260	2300	1230	2250
0.40	1245	2275	1230	2250
0.50	1230	2250	1230	2250
0.60	1205	2200	1205	2200
0.70	1190	2175	1175	2150
0.90	1150	2100	. . .	. . .
1.10	1110	2025	. . .	. . .

The effect of carbon content on forging temperature is the same for most tool steels as for carbon and alloy steels. However, the complex alloy compositions of some tool steels have different effects on forging temperature. Forging temperatures for tool steels are listed in Table 3.

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Oil-hardening cold-work tool steels						
O1	815	1500	980-1065	1800-1950	845	**1550**
O2	815	1500	980-1040	1800-1900	845	**1550**
O7	815	1500	980-1095	1800-2000	870	**1600**
Medium-alloy air-hardening cold-work tool steels						
A2, A4, A5, A6	870	1600	1010-1095	1850-2000	900	**1650**
High-carbon high-chromium cold-work tool steels						
D1-D6	900	1650	980-1095	1800-2000	900	**1650**
Chromium hot-work tool steels						
H11, H12, H13	900	1650	1065-1175	1950-2150	900	**1650**
H14, H16	900	1650	1065-1175	1950-2150	925	**1700**
H15	845	1550	1040-1150	1900-2100	900	**1650**
Tungsten hot-work tool steels						
H20, H21, H22	870	1600	1095-1205	2000-2200	900	**1650**
H24, H25	900	1650	1095-1205	2000-2200	925	**1700**
H26	900	1650	1095-1205	2000-2200	955	**1750**
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Tungsten high-speed tool steels						
T1	870	1600	1065-1205	1950-2200	955	**1750**
T2, T4, T8	870	1600	1095-1205	1950-2200	955	**1750**
T3	870	1600	1095-1230	2000-2250	955	**1750**
T5, T6	870	1600	1095-1205	2000-2200	980	**1800**
Low-alloy special-purpose tool steels						
L1, L2, L6	815	1500	1040-1150	1900-2100	845	**1550**
L3	815	1500	980-1095	1800-2000	845	**1550**
Carbon-tungsten special-purpose tool steels						
F2, F3	815	1500	980-1095	1800-2000	900	**1650**
Low-carbon mold steels						
P1	. . .	. . .	1205-1290	2200-2350	1040	**1900**
P3	. . .	. . .	1040-1205	1900-2200	845	**1550**
P4	870	1600	1095-1230	2000-2250	900	**1650**
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(a) The temperature at which to begin forging is given as a range; the higher side of the range should be used for large sections and heavy or rapid reductions, and the lower side for smaller sections and lighter reductions. As the alloy content of the steel increases, the time of soaking at forging temperature increases proportionately. Similarly, as the alloy content increases, it becomes more necessary to cool slowly from the forging temperature. With very high alloy steels, such as high-speed steels and air-hardening steels, this slow cooling is imperative in order to prevent cracking and to leave the steel in a semisoft condition. Either furnace cooling of the steel or burying it in an insulating medium (such as lime, mica, or diatomaceous earth) is satisfactory.

(b) Forging temperatures for water-hardening tool steels vary with carbon content. The following temperatures are recommended: for 0.60-1.25% C, the range given; for 1.25 to 1.40% C, the low side of the range given.

Heating Time. For any steel, the heating time must be sufficient to bring the center of the forging stock to the forging temperature. A longer heating time than necessary results in excessive decarburization, scale, and grain growth. For stock measuring up to 75 mm (3 in.) in diameter, the heating time per inch of section thickness should be no more than 5 min for low-carbon and medium-carbon steels or no more than 6 min for low-alloy steel. For stock 75 to 230 mm (3 to 9 in.) in diameter, the heating time should be no more than 15 min per inch of thickness. For high-carbon steels (0.50% C and higher) and for highly alloyed steels, slower heating rates are required, and preheating at temperatures from 650 to 760 °C (1200 to 1400 °F) is sometimes necessary to prevent cracking.

Finishing temperature should always be well above the transformation temperature of the steel being forged in order to prevent cracking of the steel and excessive wear of the dies, but should be low enough to prevent excessive grain growth. For most carbon and alloy steels, 980 to 1095 °C (1800 to 2000 °F) is a suitable range for finish forging. More information on forging parameters for ferrous alloys is available in the articles "Forging of Carbon and Alloy Steels" and "Forging of Stainless Steel" in this Volume.

Control of Die Temperature

Dies should be heated to at least 120 °C (250 °F), and preferably to 205 to 315 °C (400 to 600 °F), before forging begins. Dies are sometimes heated in ovens before being placed in the hammer or press. Temperature-indicating crayons can be used to measure surface temperature. Failure to warm the dies is likely to result in die breakage.

Operating Temperature. Normal hammer-forging and press-forging practices do not include special methods for cooling the dies; their mass and the lubricant usually provide cooling and keep them within a safe operating range (typically 315 °C, or 600 °F, maximum). However, the maximum operating temperature depends greatly on the die-steel composition. Higher temperatures may be permitted for the higher-alloy die steels, such as H11. In no event should any portion of the die be operated at a temperature higher than that at which it was tempered. Most dies are tempered at 540 to 595 °C (1000 to 1100 °F), and sometimes higher; therefore, the danger of exceeding the temperature is not great. However, the hardness at working temperature varies a great deal for different steels.

Trimming

The trimming method used for closed-die forgings depends mainly on the quantity of forgings to be trimmed, the size of the forgings, and the equipment available. A specific trimming procedure can sometimes eliminate a machining operation.

For small quantities or for large forgings, sawing or other machining operations are frequently used to remove the flash. For large quantities, the cost of trimming dies can usually be justified. Most closed-die forgings are die trimmed.

With respect to die trimming, forging materials can be divided into two groups: those that can be trimmed cold and those that should be trimmed hot. Almost all materials can be cold trimmed, but some must have special treatment after forging and prior to cold trimming. Generally, a forging can be cold trimmed satisfactorily if the work metal to be trimmed has a tensile strength of not more than 690 MPa (100 ksi) or a hardness of not more than 207 HB.

Cold trimming usually refers to the trimming of metal flash at a temperature below 150 °C (300 °F). This method is extensively used, especially for small forgings. An advantage of cold trimming is that it can be done at any time; it need not be a part of the forging sequence, and no reheating of the forgings is needed.

Hot trimming is done at temperatures as low as 150 °C (300 °F) for nonferrous alloys and as high as 980 °C (1800 °F) or above for steels and other ferrous alloys.

Cooling Practice

Cooling in still air or in factory tote boxes is common practice and is usually satisfactory for carbon steel or low-alloy steel forgings when cross sections are no greater than approximately 64 mm ($2\frac{1}{2}$ in.). Flaking may occur on larger forgings when they are air cooled. Flakes (also called shatter cracks or snowflakes) are short, discontinuous internal fissures attributed to stresses produced by localized transformation and decreased solubility of hydrogen during cooling. In a fractured surface, flakes appear as bright silvery areas; on an etched surface, they appear as short cracks. Flaking indicates the need for cooling to at least 175 °C (350 °F) in a furnace or cooling by burying the piece in sand or slag. An alternative method of treating large forgings made of alloy steels such as 4340 consists of cooling in air to about 540 °C (1000 °F), followed by isothermal annealing at 650 °C (1200 °F). Forgings of alloy tool steel should always be cooled slowly, as is recommended above for larger forgings of carbon and alloy steels.

Closed-Die Forging in Hammers and Presses

Typical Forging Sequence

The forging of automotive connecting rods is a good example of the various steps taken to produce a closed-die forging. As shown in Fig. 12, the sequence begins with round bar stock. The bar stock is heated to the proper temperature, then delivered to the hammer. Preliminary hot working proportions the metal for forming of the connecting rod and improves grain structure.

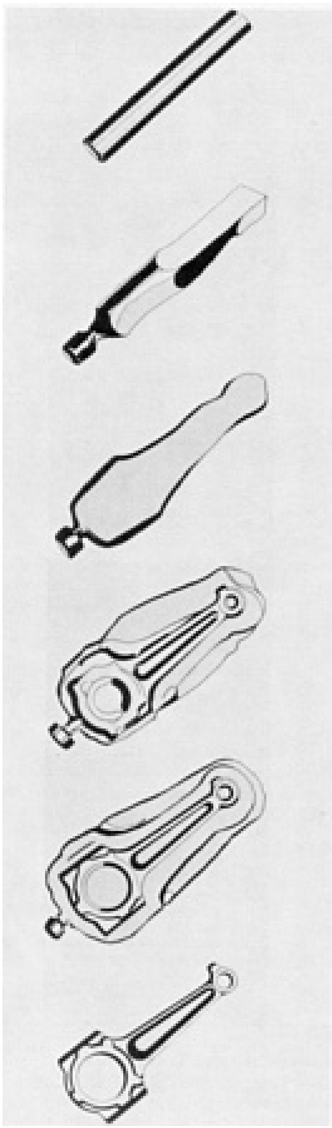

Fig. 12 Steps involved in the closed-die forging of automotive connecting rods. See text for details.

Blocking then forms the connecting rod into its first definite shape. This may necessitate several blows from the hammer. Flash is produced in the blocking operation and appears as flat, unformed metal around the edges of the connecting rod. The final shape of the connecting rod is obtained by the impact of several additional blows from the hammer to ensure that the dies are completely filled by the hot metal. The completed part may be trimmed either hot or cold to remove flash.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Introduction

HOT UPSET FORGING (also called hot heading, hot upsetting, or machine forging) is essentially a process for enlarging and reshaping some of the cross-sectional area of a bar, tube, or other product form of uniform (usually round) section. In its simplest form, hot upset forging is accomplished by holding the heated forging stock between grooved dies and applying pressure to the end of the stock, in the direction of its axis, by the use of a heading tool, which spreads (upsets) the end by metal displacement.

Hot Upset Forging

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Applicability

Although hot upsetting was originally restricted to the single-blow heading of parts such as bolts, current machines and tooling permit the use of multiple-pass dies that can produce complex shapes accurately and economically. The process is widely used for producing finished forgings ranging in complexity from simple bolts or flanged shafts to wrench sockets that require simultaneous upsetting and piercing. Forgings that require center (not at bar end) or offset upsets can also be completed.

In many cases, hot upsetting is used as a means of preparing stock for forging on a hammer or in a press. Hot upsetting is also occasionally used as a finishing operation following hammer or press forging, such as in making crankshafts.

Because the transverse action of the moving die and the longitudinal action of the heading tool are available for forging in both directions, either separately or simultaneously, hot upset forging is not limited to simple gripping and heading operations. The die motion can be used for swaging, bending, shearing, slitting, and trimming. In addition to upsetting, the heading tools are used for punching, internal displacement, extrusion, trimming, and bending.

In the upset forging process, the working stock is frequently confined in the die cavities during forging. The upsetting action creates pressure, similar to hydrostatic pressure, that causes the stock to fill the die impressions completely. Thus, a wide variety of shapes can be forged and removed from the dies by this process.

Work Material and Size. Although most forgings produced by hot upsetting are made of carbon or alloy steel, the process can be used for shaping any other forgeable metal. The size or weight of a workpiece that can be hot upset is limited only by the capabilities of available equipment; forgings ranging in weight from less than an ounce to several hundred pounds can be produced by this method.

Hot Upset Forging

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Forging Machines

The essential components of a typical machine for hot upset forging are illustrated in Fig. 1. These machines are mechanically operated from a main shaft with an eccentric drive that operates a main slide, or header slide, horizontally. Cams drive a die slide, or grip slide, which moves horizontally at right angles to the header slide, usually through a toggle mechanism. The action of the header slide is similar to that of the ram in a mechanical press. Power is supplied to a machine flywheel by an electric motor. A flywheel clutch provides for stop-motion operation, placing movement of the slides under operator control.

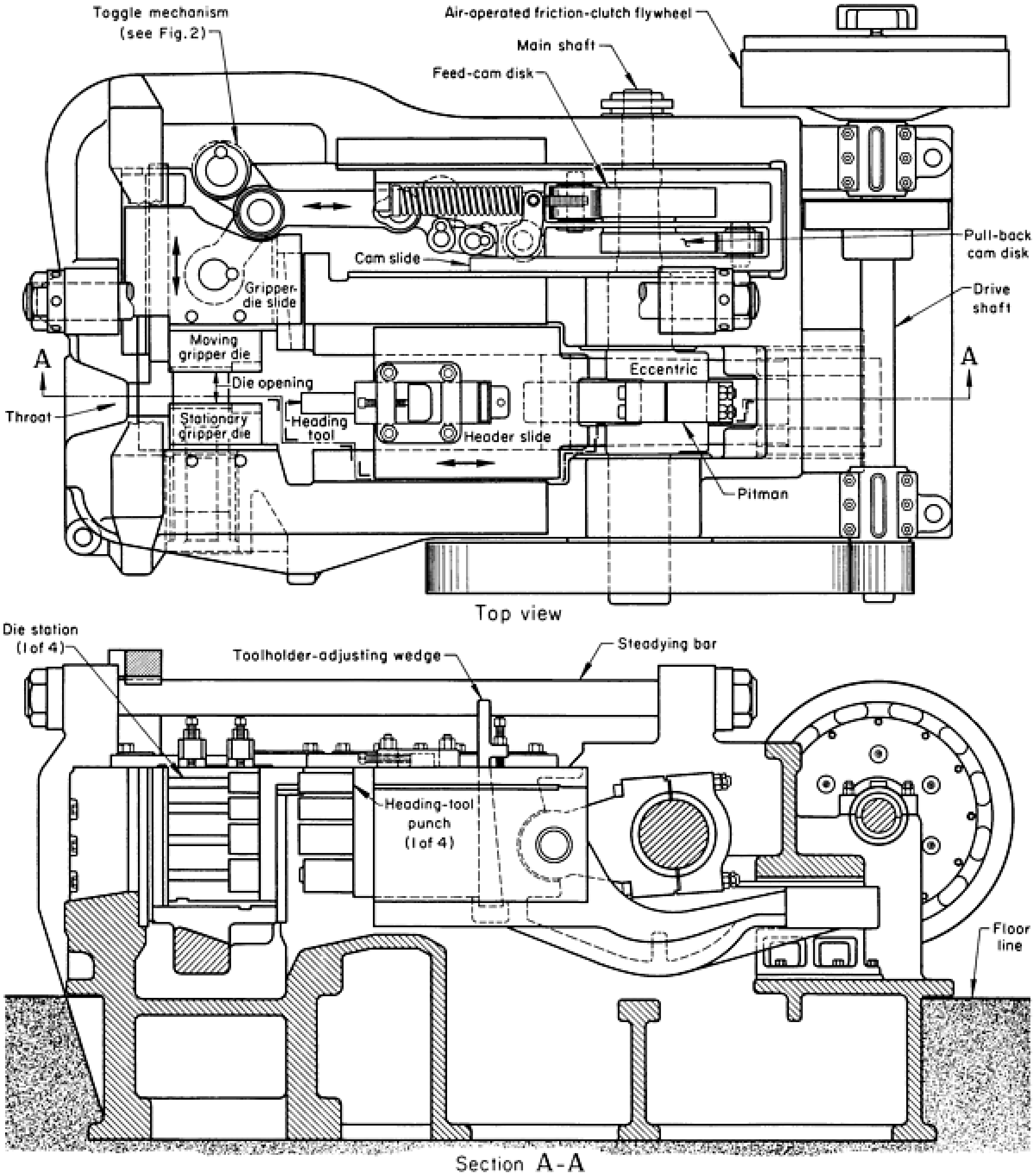

Fig. 1 Principal components of a typical machine for hot upset forging with a vertical four-station die. See text for description of operation.

Forging takes place in three die elements. There are two gripper dies (one stationary and one moved by the die slide), which have matching faces with horizontal grooves to grip the forging stock and hold it by friction, and there is a heading tool, or header, which is carried by the header slide in the plane of the work faces of the gripper dies and aligns with the grooves in these dies (Fig. 2). The travel of the moving die is designated as the die opening, and its timed relationship to the movement of the header slide is such that the dies close during the early part of the header-slide stroke. The part of the forward header-slide stroke that takes place after the dies are closed is known as the stock gather, and the amount that the returning header slide travels before the moving die starts to open is called the hold-on, or the hold.

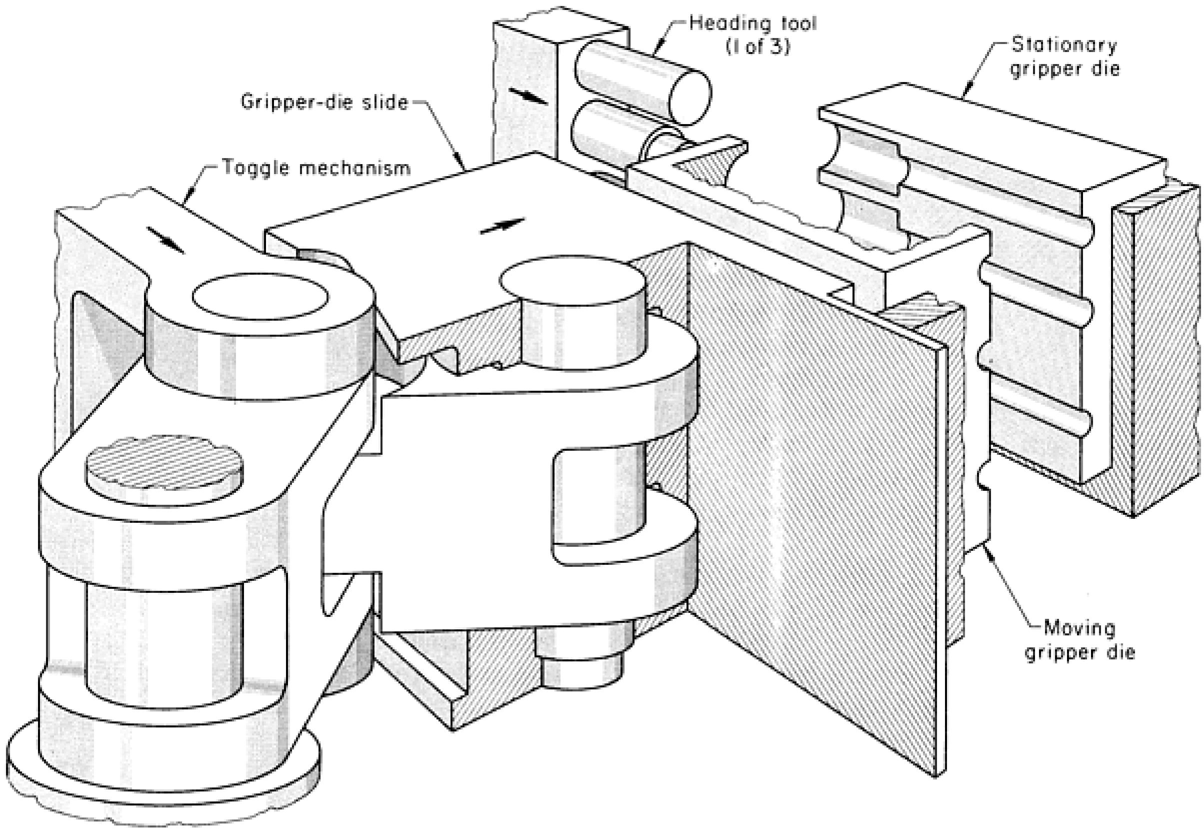

Fig. 2 Basic actions of the gripper dies and heading tools of an upsetter

The die opening determines the maximum diameter of upset that can be transferred between the dies and withdrawn through the throat, without pushing the workpiece forward and lifting it out over the top. The diameter of the stock, rather than the stock gather, determines the amount of stock that can be upset; the stock gather, however, has an important bearing on the depth to which internal displacement can be carried. The height of the die determines the number of progressive operations that can be accommodated in one set of dies.

Operation. The basic actions of the gripper dies and the header tools of an upsetter can be demonstrated by the three-station setup shown in Fig. 2. The stock is positioned in the first (topmost) station of the stationary die of the machine.

During the upset forging cycle, the movable die slides against the stationary die to grip the stock. The header tool, fastened in the header slide, advances toward and against the forging stock to spread it into the die cavity. When the header punch retracts to its back position, the movable dies slide to open position to release the forging. This permits the operator to place the partly forged piece into the next station, where the cycle of the movable die and header tool is repeated. Many forgings can be produced to final shape in a single pass of the machine. Others may require multiple passes for completion.

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Selection of Machine Size

The rated sizes for upsetters are listed in Table 1, which also provides data on typical rated tonnage capacities, working strokes per minute, and motor ratings. Pressure capacities required for the upset forging of carbon and low-alloy steels are about 345 MPa (25 tons per square inch, or tsi) for simple shapes, but more complex shapes may require pressures of about 510 MPa (37 tsi). Tonnage calculations must include the area of flash produced. The effects of alloy composition on the capacity requirements for upsetters are approximately the same as those for other types of forging equipment. These effects are discussed in the article "Hammers and Presses for Forging" in this Volume. The choice of machine size is also affected by one or more of the following factors: gripper-die stroke, die space, throat clearance, header-slide stroke, header-slide gather, header-slide hold-on, available energy, and cost.

Table 1 Size and operating data for upset forging machines

Rated size, in.[(a)]	**Nominal rated capacity, tonf**[(b)]	**Average strokes per minute**	Average motor rating, hp
1	200	90	**7.5**
$1\frac{1}{4}$	225	75	**10**
$1\frac{1}{2}$	300	65	**10-15**
2	400	60	**15-20**
$2\frac{1}{2}$	500	55	**20-25**
3	600	45	**30**
4	800	35	**40-60**
5	1000	30	**60-75**
6	1200	27	**75**
7	1500	25	**125**
9	1800	23	**150**

10	**2250**	**20**	**200**

(a) 1 in. = 25.4 mm.

(b) 1 tonf = 8.896 kN

Gripper-die stroke is one of the simplest indicators of the maximum diameter of upset (assuming that the stock is a readily forgeable carbon or alloy steel) that can be safely produced on a given size of machining. This stroke must permit a forging having a maximum-diameter upset to drop freely between the dies into the discharge chute below the dies. In using this criterion, allowance must be made for the fact that, unless brake adjustment is perfect, there will be some override (failure of the brake to stop the movement in the extreme open condition), which will reduce the effective clearance between the dies. Therefore, the maximum diameter of upset on forgings that are to drop between the dies should be 12.5 to 25 mm ($\frac{1}{2}$ to 1 in.) less than the gripperdie stroke, depending on machine size. This is a general rule that is applicable to simple upsets in readily forgeable steels and adjustments must be made to accommodate varying conditions. For example, the maximum diameter of upset on forging from more difficult materials, such as stainless steel or heat-resistant alloys, must be reduced in proportion to the reduced forgeability of the material. Similarly, on extremely thin flanges or on upsets having difficult-to-fill contours, maximum diameters must be reduced in proportion to the increase in force required to finish the upset; otherwise, the part will not be completely filled.

Under some circumstances, with special consideration to die design to avoid overloading the machine, it is possible to produce forgings with larger-diameter upsets than the above rule would indicate. When this is done, forgings must be moved forward ahead of the dies if they are to be dropped into the chute, or if long bars are being upset, they are moved forward to clear the dies and then raised and brought back over the top of the dies and out the rear of the machine, where they are unloaded by the operator. The following three techniques can be employed to extend the maximum diameter of upset that can be produced in a machine of a given size.

The first technique involves the use of a blocking pass that finishes the center portion of the upset, followed by a final pass that finishes the outer portion. By this procedure, the effective area of the metal being worked is lessened in each pass. To be effective, however, the face of the finished upset should be slightly concave, so that the finishing punch does not contact the center area finished by the blocking pass.

Second, flange diameters that are in excess of the normal machine capacity can be forged if no attempt is made to confine the outside diameter of the flange. This requires some additional stock removal by machining or trimming, but is an effective means of producing a larger-than-normal upset on an available machine without damage to the machine.

Lastly, the maximum diameter of upset that can be produced in a given size of machine can sometimes be increased by slightly modifying the shape of the upset to facilitate metal flow. Upset shapes that restrict metal flow should be avoided in favor of those that encourage the metal to flow in the desired direction. Small corner or fillet radii and thin flanges should be avoided when the size of a forging makes it borderline for machine capacity.

Die Space. For some applications, a larger machine must be selected because more die space is needed. Die blocks must be high enough to accommodate all passes, and the dies should be long enough to contain all impressions and to allow for gripping or for tong or porter-bar backup. Dies are normally thick enough for any forging that can be produced in the machine in which they fit.

Throat clearance through the machine may become a limiting factor, particularly in upsetting long bars or tubes that extend through the machine throat during operation. The extension of the stationary die beyond the throat is one-half of the maximum diameter of stock that can be cleared.

Header-slide stroke is normally adequate for any forging that can be produced on a given size of machine. However, in some applications, unusually long punches will be retracted insufficiently when the machine is open, thus inhibiting installation and removal of the dies without interference. Under these circumstances, a larger machine may be required.

Header-Slide (Stock) Gather. The forward movement of the header slide and the closing movement of the gripper dies begin simultaneously. That portion of the forward stroke of the header slide remaining after the gripper dies are fully closed is known as the stock gather, and it is the maximum portion of the stroke that can be used for forging. Die layout, particularly in applications involving long upsets or deep piercing operations, should be checked to determine the position of all punches in relation to the work at the start of the stock gather in each pass. Occasionally, this will dictate the selection of a larger machine than would otherwise be required.

Header-slide hold-on, the short distance the header slide travels back on the return stroke before the gripper die starts to open, is important in such operations as deep piercing, in which the tools must be stripped from the work. In these operations, the punch designs should be checked to determine that they will strip free from the work before the gripper die starts to open.

Available Energy. When using the general rule that upsets should be 12.5 to 25 mm ($\frac{1}{2}$ to 1 in.) less in diameter than the gripper-die stroke, it usually follows that the energy input of the machine is sufficient. However, it is sometimes helpful--particularly in applications involving thin flanges, difficult-to-fill shapes, difficult-to-forge materials, or other special upsetting problems--to consider machine capacity in terms of equivalent static pressure, measured in tonnage. This is especially practical when facilities are available to determine experimentally, using hydraulic press equipment, the unit force (MPa or tsi) required to upset a specific workpiece.

If the tonnage rating of machine is not known, it can be obtained from the manufacturer. This tonnage rating will be the load that can be imposed close to the end of the forward stroke without damaging the machine or without causing slip of the friction relief overload protection. As with any crank-operated machine, the available force decreases as the distance from the end of the stroke increases. In a typical upsetter, the available force at the start of the gather will be approximately 80% of the safe rating at the end of the stroke. This is a factor that must be considered in selecting the proper size of machine for upsetting long lengths of stock in one pass.

Cost is often a primary factor in the selection of machine size. If an undersize machine is used, the cost of machine maintenance and tool replacement will be excessive. For production runs, an oversize machine is usually not economical, because burden rate increases with equipment size, and the higher rate increases cost per piece excessively. However, there are exceptions in which the increase in burden cost accompanying the use of a machine larger than required is outweighed by increased productivity.

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Tools

The four basic types of upsetter heading tools and dies, shown schematically in Fig. 3, differ in operating principle as follows:

- Tooling does not support exposed working stock (Fig. 3a). Stock is held by the gripper dies, and the heading tool advances to upset the exposed stock
- Stock is supported in the gripper-die impression (Fig. 3b). Great lengths of stock can be upset with this method by using repeated blows. The diameter of the preceding upset becomes the diameter of the working stock for the next pass
- Stock is supported in a recess in the heading tool, which is shaped like the frustum of a cone (Fig. 3c). Stock is gathered in the recessed heading tool. This method is widely used when large amounts of stock must be gathered, as in the forging of transmission shafts
- Stock is supported in the frustum-shaped recess of the heading tool and in the recesses of the gripper dies (Fig. 3d). This method is widely used to achieve a better balance of metal displacement, especially in the development of intricate, difficult-to-forge shapes

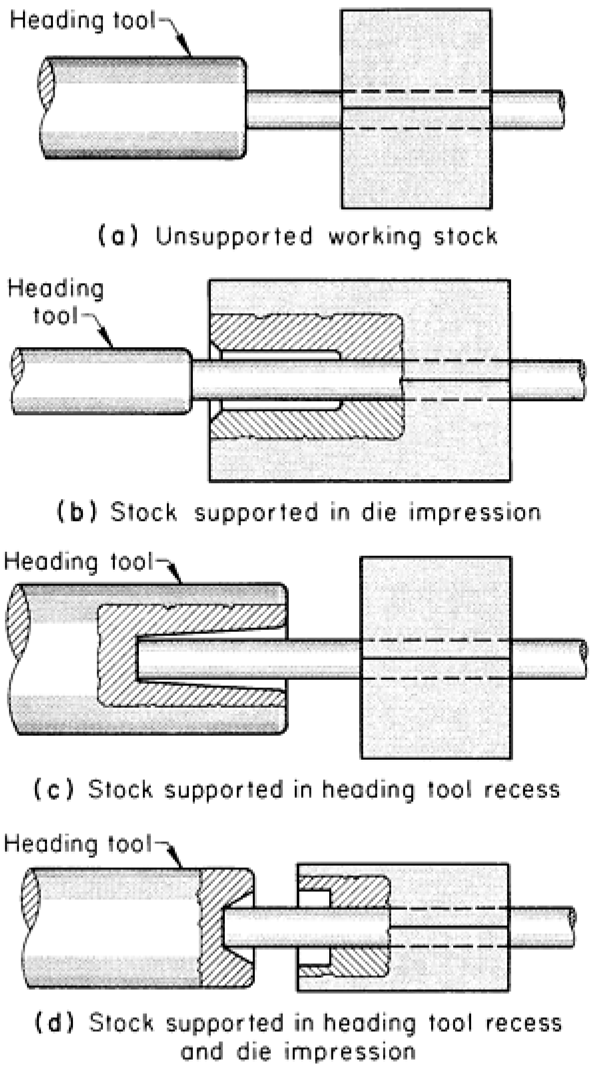

Fig. 3 Basic type of upsetter heading tools and dies showing the extent to which stock is supported

Although some forgings are produced by a single stroke of the ram, most shapes require more than one pass. The upsetter dies may incorporate several different impressions, or stations. The stock is moved from one impression to the next in sequence to give the forging a final shape. Each move constitutes a pass. Three or more passes are commonly used to complete the upset, and if flash removal (trimming) is a part of the forging operation, another pass is added.

Piercing and shearing passes can also be incorporated into the dies. In single-blow solid-die machines, the gripper dies are replaced by a shear arm and a shear blade. A long, heated bar of forging stock is placed in a slot and pushed against a stop. As the foot pedal is depressed, a motion similar to that of a conventional upsetter occurs except that, instead of the die closing, a section of the bar is sheared off. While the shear slide is moving, a cam actuates a transfer arm, which moves until it contacts the stock. The stock, now positioned between the shear blade and the transfer arm, is moved into the proper position between the punch and the die. As the punch advances and contacts the stock, the shear blade and the transfer arm move apart. The punch continues its advance, and the forging is produced in a single blow. Ejector pins push the forging from the die, and the forging drops onto an underground conveyor. The operator pushes another heated bar of forging stock against the stop, and the cycle is repeated.

Tool Materials. Hot-work tool steels are commonly used for hot upsetting dies. Alloy steels such as 4150 and 4340 are also used, especially for gripper dies.

For short runs, it is common practice to use solid dies made of alloy steels such as 4340, 6G, or 6F3. For runs of about 1000 pieces, higher-alloy hot-work tool steels such as H11, H13, 6H1, or 6H2 are commonly used for dies or for die inserts. Detailed information on the factors that govern the selection of tool materials for hot upsetting, recommendations for specific applications, and tool life is provided in the article "Dies and Die Materials for Hot Forging" in this Volume.

Using inserts in master blocks may be less costly than making the entire heading tool or the gripper dies from an expensive steel. However, the two more important advantages of using punch and die inserts are that they can be replaced when worn out and that, in many applications, two or more different parts can be forged with a master block by changing inserts. Additional information is provided in the section "Inserts versus Solid Dies" in the article "Dies and Die Materials for Hot Forging" in this Volume.

Hot Upset Forging

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Preparation of Forging Stock

Cold and hot shearing are the most commonly used methods of preparing blanks for hot upset forging. Sawing, cutting with abrasive wheels, and flame cutting are also used, but less frequently. The use of machined or previously forged blanks for hot upsetting is usually confined to applications involving special requirements.

Cold shearing blanks from mill-length hot-rolled bar stock is the most common method of preparing stock for hot upsetting. Cold shearing is the most rapid method of producing blanks, and it involves no waste of metal. One shear can accommodate a wide range of sizes, and equipment is adaptable to mass production when used in conjunction with tables and transfer mechanisms. Magnetic feed tools and proper bar hold-down devices are usually required for efficient operation.

With the types of shearing equipment available, it is not uncommon to cold shear medium-carbon alloy steels in diameters to 125 mm (5 in). If section thickness and hardness of material permit, it is usually economical to shear as many bars in one cut as possible, using multiple-groove shear blades. It is common practice to use multiple shearing on low-carbon steel up to 50 mm (2 in.) in diameter.

For medium-diameter bar stock, it is common practice to forge from the bar progressively, cutting off each forging on the last upsetter pass. This method produces a short length of bar scrap, which can be held to a minimum by careful selection of bar length in relation to blank length. This method is widely used for producing small, simple forgings that can be upset in one blow. A secondary cold trimming operation may be necessary to remove flash.

For small-diameter blanks, it is often advantageous to use coiled cold-drawn wire. This wire is straightened and cut off, and the blanks are stacked by means of high-speed machines. The use of blanks made from wire is especially beneficial when shank diameter on the upset forging must be held to closer tolerances than can be obtained with hot-rolled bars. A more detailed discussion of the equipment and techniques used in the cold shearing of bars is provided in the article "Shearing of Bars and Bar Sections" in this Volume.

Hot shearing is recommended for cutting bars more than 125 mm (5 in.) in diameter, and it can be used for smaller-diameter bars in semiautomatic operations. For diameters up to about 28.6 mm ($1\frac{1}{8}$ in.) and when the upset can be made in one blow, the preliminary preparation of individual blanks can be avoided. Mill-length bars are heated and fed into a semiautomatic header. The blank is cut off at the same time the upset is made. A stock gage between the gripper dies and the header die locates the stock before it is held by the gripper dies. The gage, mounted on a slide that is actuated by the header slide, retracts as the header tool advances. A typical tooling arrangement is shown in Fig. 4.

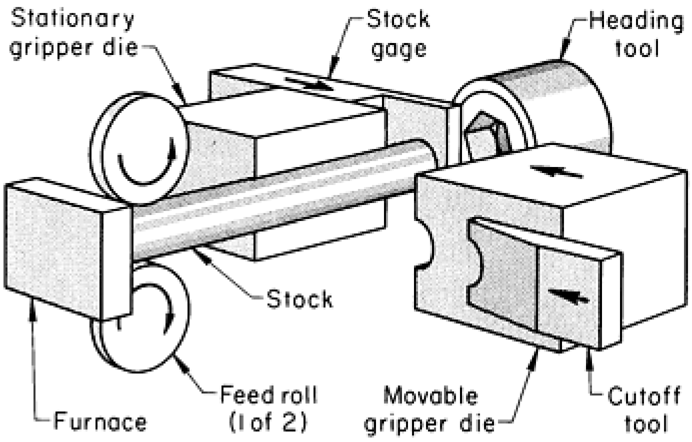

Fig. 4 Setup for simultaneous upsetting and cutoff of continuously fed, heated mill lengths of stock in a semiautomatic header.

Cold sawing is used in conjunction with or as an alternative to shearing. The saw is power fed and may have an automatic clamping device to hold the stock. It has a pump and supply tank to feed coolant to the cutting edge of the blade. Stock gages are used to set cutting lengths.

Sawing is useful for those sizes or materials that cannot be readily sheared. It produces a uniform edge and can be used for sampling and where distortion is a problem. Sawing is a comparatively slow operation and wastes a significant amount of metal. Maintenance costs are also higher in sawing than in shearing. In sawing, however, set-ups can be made quickly; therefore, sawing is often preferred for preparing small quantities of blanks.

Abrasive cutoff wheels are sometimes used for preparing blanks from high-alloy or extremely hard metals. This method must be used with extreme care if the material being cut is susceptible to grinding cracks. Except for this warning, the advantages and disadvantages of abrasive cutting are essentially the same as those of cold sawing.

Gas cutting is generally used only for the preparation of large-diameter blanks. In this operation, the cost of the fuel gases and the resulting melted metal on the ends of the cut stock must be considered.

Special Methods. Some forgings require an unusual distribution of metal, which necessitates some preliminary gathering of material before the final upset forging operation. This can be accomplished in several ways, such as using rolled sections, machining the blank, or preshaping the blank on a hammer or press.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Metal-Saving Techniques

In high-production upsetting, even the most minute saving of metal on a single forging can result in substantial overall savings. Metal can be saved by observing the following practices, when applicable:

- The least wasteful method of stock preparation should be used
- The part and the procedure should be designed to avoid or minimize flash
- Stock should be calculated in order to obtain the most economical length for the specific forging, thus minimizing loss from cropped ends
- Procedures that eliminate or minimize machining, such as combined upsetting and piercing, should be used

- Backstop tongs should be used to avoid loss in cropped ends
- Welded-on or embedded tongholds should be used to obtain additional forgings from a bar

Use of Backstop Tongs. In the production of forgings from precut lengths of stock, when the dies are longer than the forging, the stock is cut to a length that allows one end to protrude from the dies (Fig. 5) so that it can be held by the operator during the forging operation. After the opposite end has been upset, the extra stock for holding is cut off to bring the forging within specified length. The waste of metal involved in this practice can be eliminated by the use of backstop tongs as shown in Fig. 5(b), which also eliminates the additional operation of cutting to length after forging.

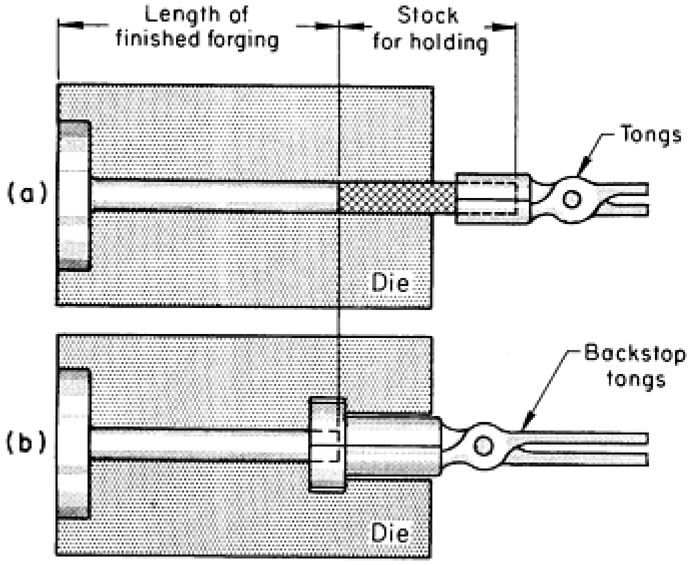

Fig. 5 One method of eliminating the need for overlength stock for holding during forging. (a) Dies exceed length of finished forging. (b) Backstop tongs reduce amount of stock required for holding and eliminate separate operation for trimming of excess stock

Use of Tongholds. In the production of forgings from bar stock that is continuously upset and cut off within the machine, a portion of the stock used in handling and gripping becomes too short to yield additional forgings. One method of obtaining several more forgings from the crop ends is to attach a tonghold to the end of the bar. This can be done by embedding a pin into the heated end of the bar or by welding a stud to the bar, as in one application in which 54 and 75 mm ($2\frac{1}{8}$ and 3 in.) diam bars were forged in 102, 127, and 152 mm (4, 5, and 6 in.) upsetters. The crop ends were about 305 mm (12 in.) long, and loss was appreciable. By welding studs 16 mm ($\frac{5}{8}$ in.) in diameter and 70 mm ($2\frac{3}{4}$ in.) long onto bar ends (Fig. 6) additional forgings were produced, and crop-end loss was reduced by approximately 50%.

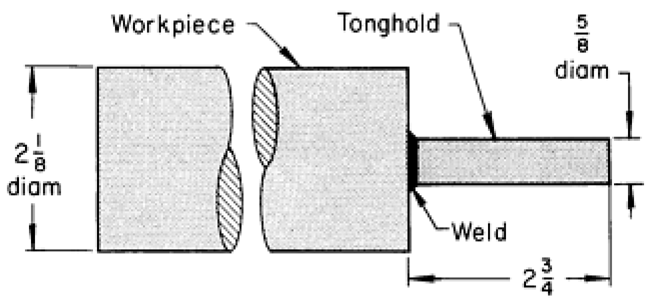

Fig. 6 Welded-on tonghold that substantially reduced crop-end loss. Dimensions given in inches

Hot Upset Forging

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Heating

The variations in upsetting temperature for different materials, the differences in stock, and the availability of various fuels have produced a substantial variety of equipment and procedures that can be used to heat stock for upsetting. Heating for upsetting can be accomplished in electric or fuel-fired furnaces, by electrical induction or resistance processes, or by special gas burner techniques. Whatever the method of heating, care should be taken to prevent excessive scaling, decarburization, burning, overheating, or rupturing of the forging stock. Heating of specific metals and alloys for forging is discussed in the Sections "Forging of Carbon, Alloy, and Stainless Steels and Heat-Resistant Alloys" and "Forging of Nonferrous Metals" in this Volume.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Descaling

Preventing the formation of scale during heating or removing the scale between heating and upsetting will result in longer die life, smoother surfaces on the forging, and improved dimensional control. The presence of scale on forgings also makes hot inspection unreliable and increases cleaning cost. When controlled heating methods for minimizing scale formation are not available, scale can be removed from the heated metal before forging, either by mechanical methods or by the use of high-pressure jets of water.

Mechanical Methods. One effective method of descaling is to brush the heated bar with rotating wire brushes. In another method, knifelike tools are shaped to the periphery of the heated bar, and the bar is scraped across the knife-edge to dislodge and remove scale. For example, for descaling a round bar, a curved knife section having the shape of a half circle is used. The heated round bar is placed in the half-circle knife section and drawn through the knife to remove the scale from half of the surface of the bar. The bar is then rotated 180°, and the operation is repeated to remove scale from the remaining surface of the bar length. Although economical, this method is less effective than wire brushing.

High-Pressure Water Jets. The use of high-pressure water jets is the most effective method of descaling. Four or more high-pressure nozzles are used; they are positioned equidistantly from one another to impinge simultaneously on all sides of the workpiece. These nozzles are usually placed inside a cabinet that is shielded at the opening into which the hot bar is inserted. Water is supplied to the nozzles at 8 to 12 MPa (1200 to 1800 psi). Nozzle openings vary with stock diameter, but an opening of 0.75 × 1.3 mm (0.030 × 0.05 in.) is common for stock diameters from 38 to 75 mm ($1\frac{1}{2}$ to 3 in.). A 35° angle of the water stream relative to the workpiece provides optimal efficiency. The water spurts are only a fraction of a second in duration in order to prevent excessive lowering of the workpiece temperature.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Die Cooling and Lubrication

Normal practice is to keep dies below 205 °C (400 °F) during operation. In some low-production operations, no coolant is required for keeping dies below this temperature. In most applications, however, a water spray (sometimes containing a small amount of salt) is used as a coolant.

Die lubrication slows production and is not widely used in the upsetting of steel. Because of the die action in upsetting, parts are less likely to stick than in hammer or press forging. In deep punching and piercing, however, sticking may be encountered, necessitating the use of a lubricant. An oil-graphite spray is an effective lubricant and may also provide adequate cooling. A recirculated suspension of alumina in water is used in some high-production operations.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Simple Upsetting

In simple upsetting, the severity limitation is directly related to the length of unsupported stock beyond the gripper dies.

In the single-blow upsetting of low-carbon, medium-carbon, or alloy steels, the maximum unsupported length is about $2\frac{1}{2}$ times the diameter. Beyond this length, the unsupported stock may buckle or bend, forcing metal to one side and preventing the formation of a concentric forging. Exceeding this limitation also causes grain flow to be erratic and nonuniform around the axis of the forging and encourages splitting of the upset on its outside edges.

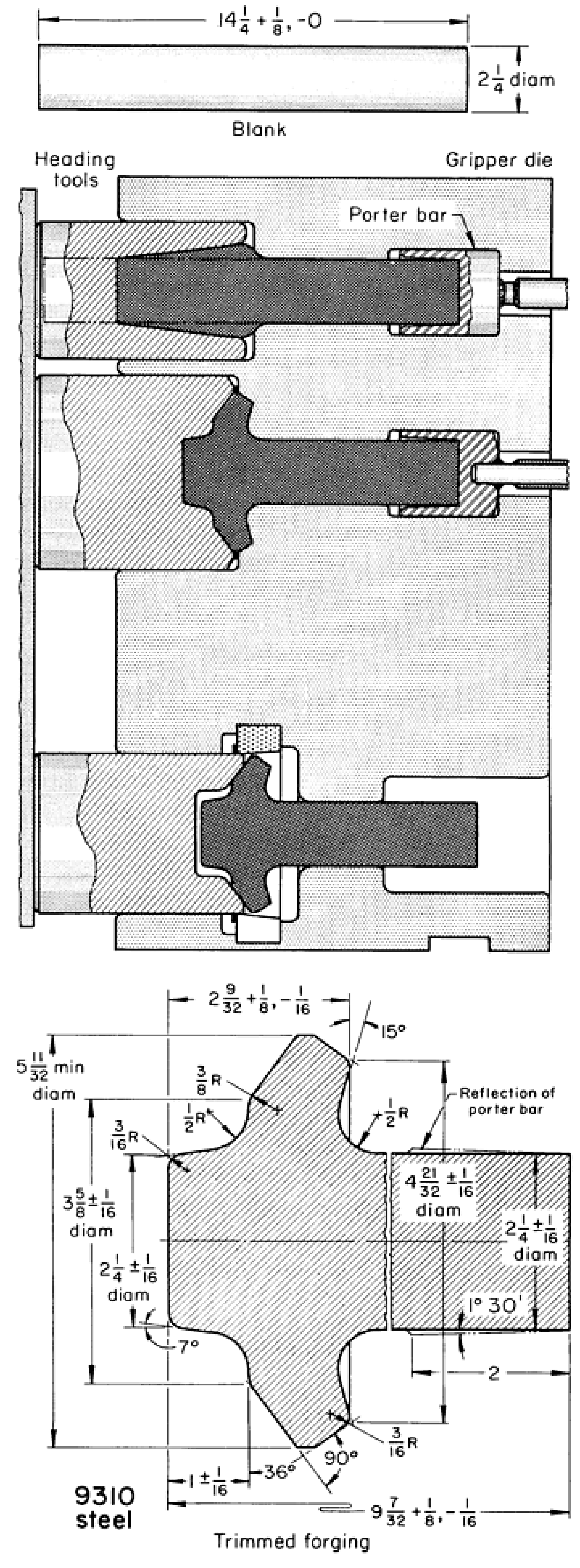
Location of Upset Cavities. Upset cavities may be located entirely within the heading tool, entirely within the gripper dies, or divided between the heading tool and gripper dies. The location depends largely on the severity of the upset and the preferred location of flash--either for convenience in trimming or for satisfying dimensional requirements in the trimmed area.

Simple forgings, requiring an upset of minimum or near-minimum severity, are often upset with the entire cavity within the heading tool. Conversely, forgings requiring an upset of greater severity are often forged with the entire cavity within the gripper dies.

Preventing Laps and Cold Shuts. Laps and cold shuts are forging defects that arise from the partial separation of some hot metal from the main body of the forging. The defects are formed when the partly separated metal, in the course of the forging cycle, is folded back against, and forged into, the main body of the forging. An oxide film, formed on the underside of the fold, creates a barrier that prevents satisfactory welding of the fold with the parent metal, thus accounting for the defect.

In hot upsetting, the displacement of too much metal in a single pass is a common cause of laps and cold shuts. When the size or shape of the upset is such that these defects occur, one or more stock-gathering passes must be added to the forging cycle in advance of the finishing pass.

The volume of upset on a forging similar to that shown in Fig. 7 could be increased slightly without the need for additional finishing passes, but additional stock-gathering passes would be required. Alternatively, with no increase in upset volume but with a more severe upset shape, an additional pass would be required to ensure complete filling of the upset impression.



14 1/4 + 1/8, -0
2 1/4 diam
Blank
Heading tools
Gripper die
Porter bar
2 9/32 + 1/8, - 1/16
15°
5 11/32 min diam
3/8 R
1/2 R
3/16 R
1/2 R
Reflection of porter bar
4 21/32 ± 1/16 diam
3 5/8 ± 1/16 diam
2 1/4 ± 1/16 diam
2 1/4 ± 1/16 diam
1° 30'
7°
2
3/16 R
90°
36°
1 ± 1/16
9 7/32 + 1/8, - 1/16
9310 steel
Trimmed forging

Fig. 7 Tooling setup for upsetting and trimming a pinion gear blank. Two passes were necessary to prevent cold shuts. Dimensions given in inches

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Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Upsetting and Piercing

In addition to providing upset shapes with a central recess or bore, upsetting and piercing are frequently combined to promote die filling, to lessen material use, and to eliminate one or more machining operations. The maximum depth that can be pierced is limited only by the equipment available. In the following example, upsetting and piercing were combined for the production of gear blanks.

Example 1: Combined Upsetting and Piercing of 8622 Steel Gear Blank.

The gear blank shown in Fig. 8 was produced more satisfactorily by upsetting and piercing than if a conventional hammer or press had been used. Less material was used, and external flash was eliminated. It was also possible to hold dimensional tolerances of +1.6, -0 mm (+$\frac{1}{16}$, -0 in.).

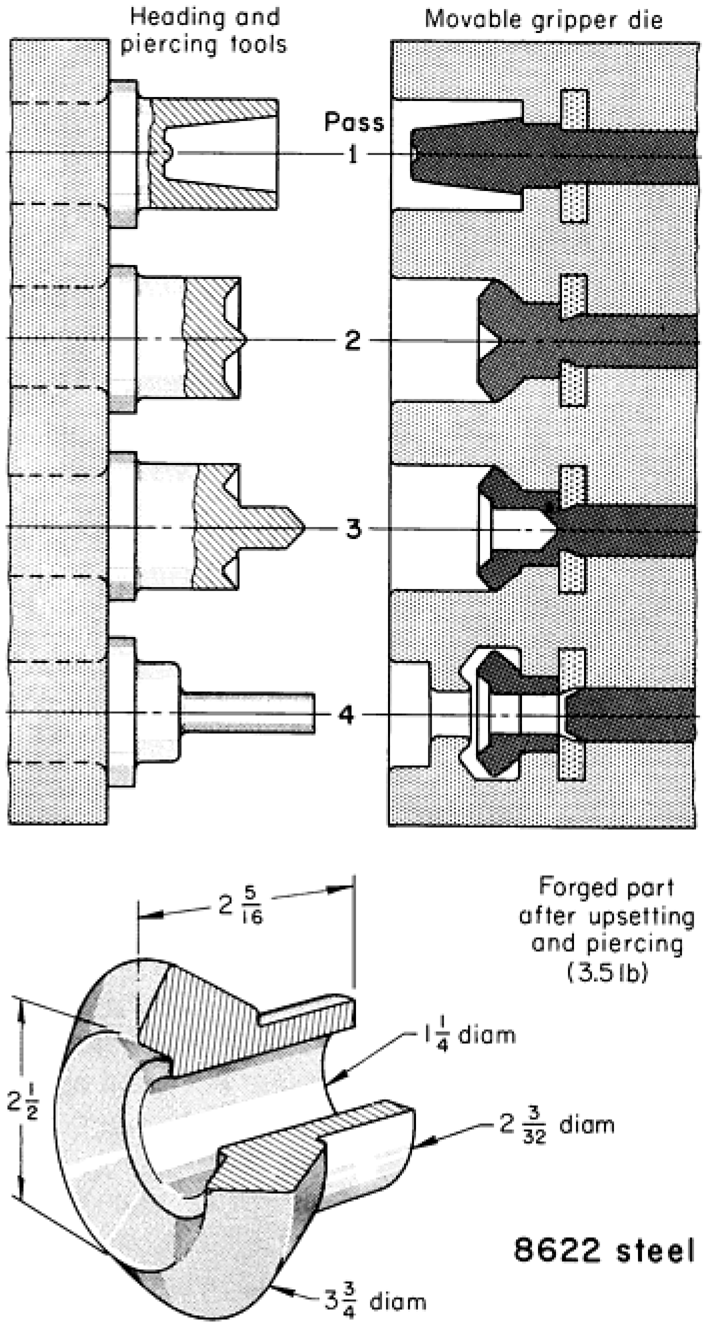

Fig. 8 Gear blank produced by four-pass hot upsetting and piercing in the tooling arrangement shown, with almost no metal loss and no trimming required. Dimensions given in inches

Forging stock consisted of 41 mm ($1\frac{5}{8}$ in.) diam 8622 steel bars, cold sheared to 1.5 m (60 in.) lengths, each of which produced ten gear blanks. The steel was heated to 1260 °C (2300 °F) in an oil-fired batch furnace, then upset and pierced in four passes (Fig. 8) in a 102 mm (4 in.) machine. Production rate was 90 forgings per hour.

The solid dies were made of H11 tool steel and were heat treated to 37 HRC. Approximately 8000 pieces were produced before the dies required resinking.

Ringlike shapes can sometimes be more economically produced from a bar by combined upsetting and piercing than from machining of tubing, as in the following example.

Example 2: Use of Upsetting and Piercing to Produce Bearing Races Without Flash.

The bearing race shown in Fig. 9 was upset, pierced, and cut off in two passes without flash. A 127 mm (5 in.) upsetter was used to forge the part from 3 m (10 ft) lengths of 64 mm ($2\frac{1}{2}$ in.) diam bar stock of 4720 steel in the tooling setup shown in Fig. 9. Long bars were used to minimize loss of material from cropping; however, although 68 forgings were obtained from each 3 in (10 ft) bar, only enough bar for forging three parts was heated at a time. This method was more economical than machining the bearing races from tubing.

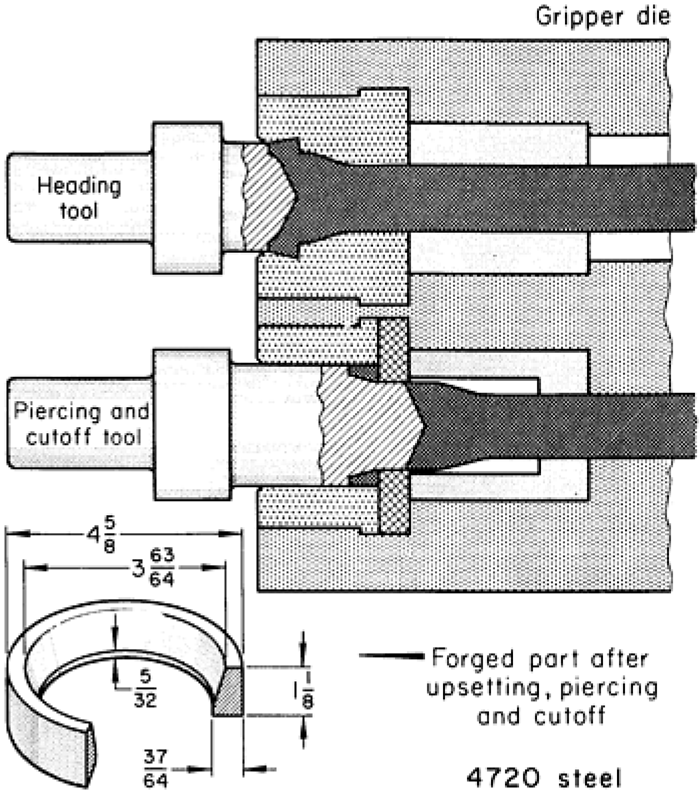

Fig. 9 Tooling setup for producing bearing races from 3-m (10-ft) lengths of 64-mm ($2\frac{1}{2}$-in.) diam bar by upsetting, piercing, and cutoff in two passes. Dimensions given in inches.

Heating (to 1205 °C, or 2200 °F, in an oil-fired batch furnace) and upsetting were done by a two-man crew at a production rate of 150 pieces per hour. Because there were no provisions for atmosphere control in the furnace, a descaler was used to minimize carryover of scale into the upsetter. Die inserts (made solid from H11 tool steel and heat treated to 37 HRC) produced about 8000 pieces before requiring resinking to maintain the tolerances of +1.6, -0 mm (+$\frac{1}{16}$, -0 in.) specified for the forging.

Double upsetting and piercing can often be used to produce complicated shapes, such as the cluster gear discussed in the following example.

Example 3: Two Upsetting and Piercing Passes in the Production of Cluster Gears.

Two separate operations, each involving two upsetting and piercing passes and one trimming pass, were used for producing 152 mm (6 in.) OD cluster gear blanks from 373 mm ($14\frac{11}{16}$ in.) lengths of 75 mm (3 in.) diam 4320 steel. These operations were performed in a 127 mm (5 in.) upsetter; the tooling setup used is illustrated in Fig. 10. The initial forging blank, which weighed 13.4 kg (29.5 lb) was cold sawed to length and heated to 1230 °C (2250 °F) in a box furnace. After upsetting one end, blanks were reheated to the same temperature before upsetting the other end.

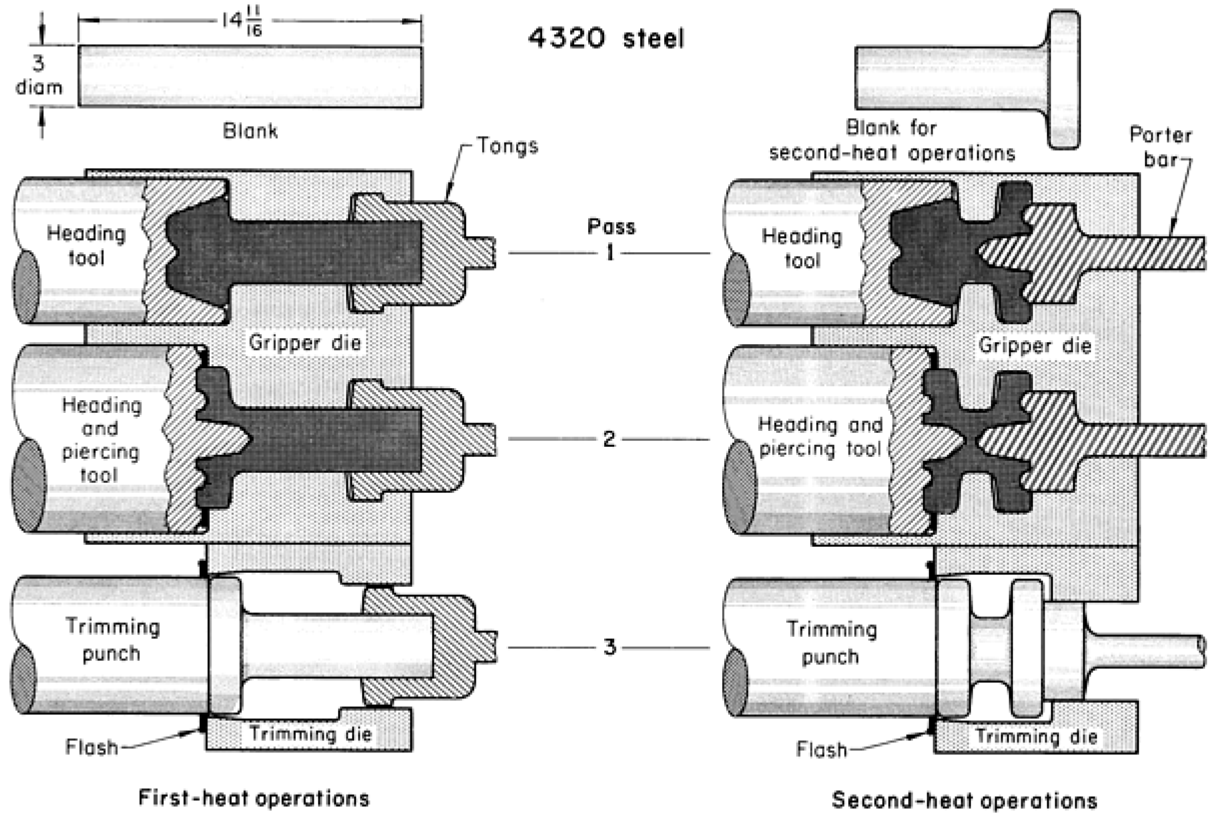

Fig. 10 Tooling setup for producing a cluster gear blank in two separate operations involving upsetting and piercing, then trimming. Dimensions given in inches

The die inserts used were made of 6F2 alloy steel at a hardness of 341 to 375 HB. Dies for forging each end produced an average of 5000 pieces (and occasionally as many as 6000) before requiring resinking to maintain specified tolerances of +3.2, -0 mm (+1.8, -0 in.) on the outside diameter and of +0, -3.2 mm (+0, -$\frac{1}{8}$ in.) on the inside diameter. Each end of the gear blank was produced at the rate of 70 pieces per hour.

Recesses for Flash. Depending on the shape of the upset, a recess may be required in the gripper die to take care of the flash that forms as a collar on the workpiece. The shape of the workpiece often provides natural clearance. In other applications, as in the following example, a recess must be provided.

Example 4: Shape of Upset That Necessitated a Recess for Flash in the Gripper Dies.

Five passes were required to upset, pierce, and trim the wrench socket shown in Fig. 11 Because of the required shape of the upset, a recess was necessary in the gripper dies to allow space for the flash, as shown in Fig. 11.

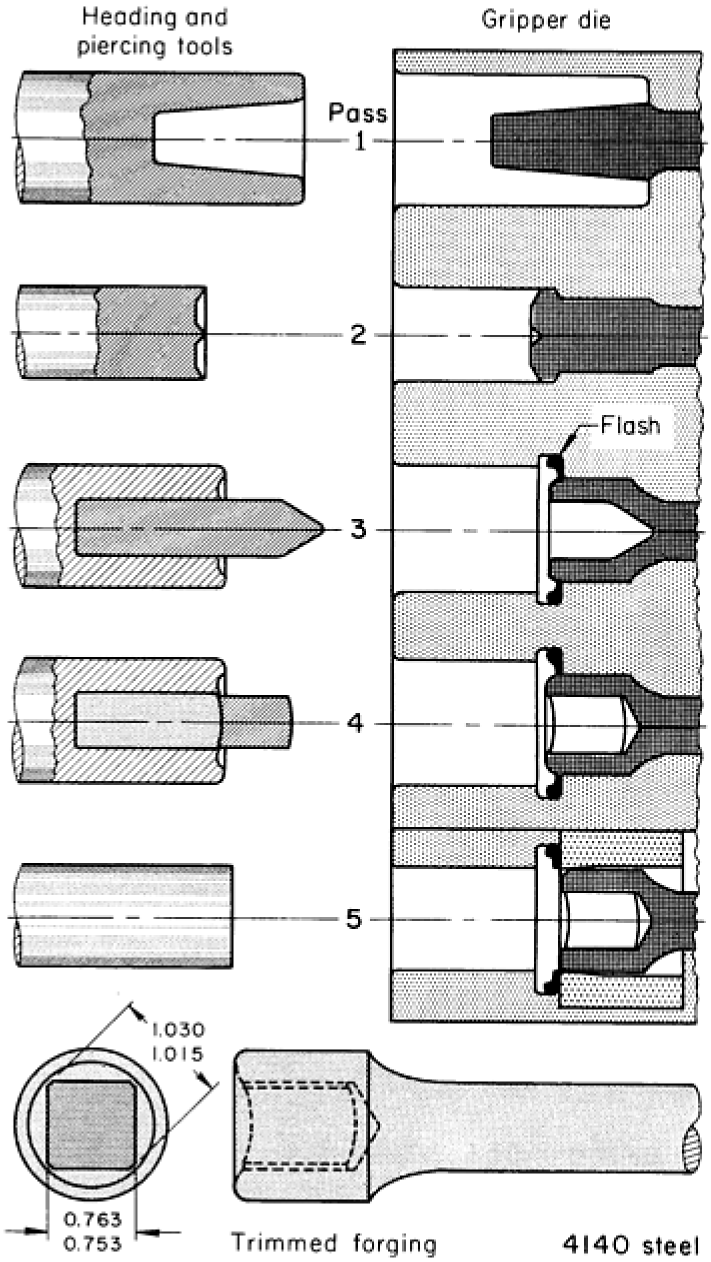

Fig. 11 Tooling arrangement in which a recess for flash was incorporated into the gripper die for five-pass upsetting, piercing, and trimming of a wrench socket. Dimensions given in inches

The forgings were produced from 0.63 kg (1.38 lb) blanks of 19 mm ($\frac{3}{4}$ in.) diam 4140 steel sheared to lengths of 280 mm (11.04 in.). Blanks were induction heated to 1150 °C (2100 °F) and forged in a 50 mm (2 in.) upsetter using solid dies. Gripper dies and trimming guides were made of H12 tool steel, punches of H21, and trimming cutters of T1. Because of the square pierce and the close dimensional requirements (Fig. 11), die life between reworkings was short (500 to 600 pieces).

Irregular Shapes. Different methods of forging can be combined advantageously to produce irregular shapes, such as that of the hand-tool component discussed in the following example. Because the direction of the blind hole prevented the use of drop forging, the main body was hammer forged, and the blind hole was pierced in an upsetter. The closing of the gripper dies was used to advantage in hot sizing the flat portion of the forging.

Example 5: Upsetting and Piercing an Irregularly Shaped Hammer-Forged Blank.

The component (used on hand tools such as spades and root-cutters to serve as a junction between tool and handle) shown in Fig. 12 was originally produced as a casting. For production as a forging, this part was first blanked by hammer forging from 4142 steel. The hammer-forged blank was then heated to 1205 °C (2200 °F) and upset and pierced in a 102 mm (4 in.) upsetter using the tooling setup shown in Fig. 12. The gripper dies were also used to hot size the flat portion of the forging during upsetting. Dies for the upsetter were made solid from 6F2 alloy steel at 341 to 373 HB and produced an average of 12,000 pieces (at a rate of 175 per hour) before requiring resinking to maintain dimensional requirements.

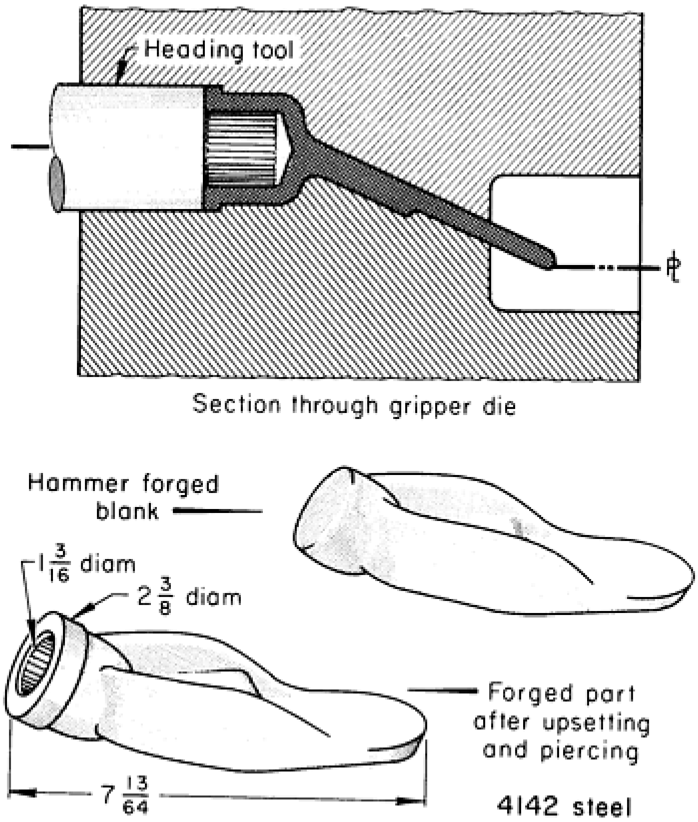

Fig. 12 Irregularly shaped hand-tool component that was upset and pierced from a hammer-forged blank in the tooling setup shown. Dimensions given in inches.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Offset Upsetting

In most of the forgings produced in upsetters, the upset portions are symmetrical and concentric with the axis of the initial forging stock. However, upsetters are not limited to the production of this type of forging. With proper die design and techniques, parts having eccentric, or offset, upsets can be produced. Such upsets are usually, but not necessarily, symmetrical to the plane through the axis of the stock in the direction of the offset. Dies for offset upsetting must be designed so that the metal for the upset is directed eccentrically but is sufficiently restricted in movement to prevent folding or buckling that will cause cold shuts in the finished forging.

In some applications, particularly when the eccentric upset is directly at the end of the forging, the stock is bent in the first operation so that the axis of the bent-over portion is perpendicular to the direction of travel of the header slide. In such applications, the forging techniques used in the subsequent passes (blocking, finishing, and trimming) are basically the same as those used in producing symmetrical upsets. Forgings of this type can be produced with or without flash. When they are forged with flash, the flash can be removed in a final trimming operation.

When the eccentric upset is some distance removed from the end of the forging, it is impossible to position the stock in an initial bending operation. In such parts, the metal must be forced to upset eccentrically into cavities in the punches, dies, or both by the axial movement of the punches. The degree of eccentricity of such upsets is more limited, because of the problem of preventing the stock from initially buckling in the direction of the upset and thus producing cold shuts on the opposite side.

Double-End Upsetting

For many forgings, the use of double-end upsetting--that is, two separate upsetting operations performed on opposite ends of the stock--is required for producing the desired shape. In double-end upsetting, the passes for the operation at each end are based on the same design considerations as in producing an upset on only one end of a straight bar. Double-end upsetting, however, often presents handling and heating problems not encountered in single-end upsetting.

One of the first decisions that must be made in planning the processing for double-end upset forgings is which end is to be forged in the first heat. If there is a difference in the upset diameters, it is almost always preferable to forge the smaller diameter first. This usually simplifies handling in the second heat. It also permits closer spacing in the furnace for the reheating, which results in more efficient use of furnace capacity.

The cut blank for the first-heat operations is handled by tongs or porter bars, as in single-end upsetting. Handling in second-heat operations is done by similar means, except that the design of the handling tools is influenced by the shape of the first upset.

If the finished part produced from the forging will have a drilled or bored hole central with the axis of the forging, it is often desirable, as a first-heat operation, to pierce a hole of suitable diameter and depth to facilitate handling in the second operation with a porter bar made to fit the pierced hole. When pierced holes are not permitted, some other means must be used to handle the forging during the second upsetting operation.

When a double-upset forging requires a pierced through hole, part of the hole is pierced in each upset end, and the connecting metal is removed by trimming, either in an additional pass in the upsetter or in a separate operation. Forgings to be produced by double-end upsetting must be provided with enough draft to facilitate insertion and removal from the second operation without pinching or sticking. To prevent distortion of the first-heat upset during the second-heat operations, the workpiece should be reheated such that the upset portion is kept as cool as possible. The difference in diameters, together with proper placement in the furnace, usually provides a satisfactory temperature differential. A greater differential may be provided by the use of a water-cooled furnace front designed to shield the first-heat upset from furnace heat during reheating.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Upsetting With Sliding Dies

The hot upset forging process is not limited to forging heads or upsets at the ends of bars; it can also gather material for the upset at any point along the length of a bar. This special type of upsetting, which can be performed on round or rectangular bars, requires special tooling in the form of sliding dies. These sliding dies are inserted into the gripper-die frames.

A typical sliding-die arrangement is shown in Fig. 13. With this method, one of the sliding dies moves in the same direction as the moving gripper die to hold the workpiece firmly against a second sliding die and a stationary gripper die.

The ram stroke then pushes both sliding dies inward against the end of the stock to form the upset. The sliding action is facilitated by backing the sliding dies with brass liners. The sliding dies can be retracted by springs or by loading a new workpiece into the upsetter.

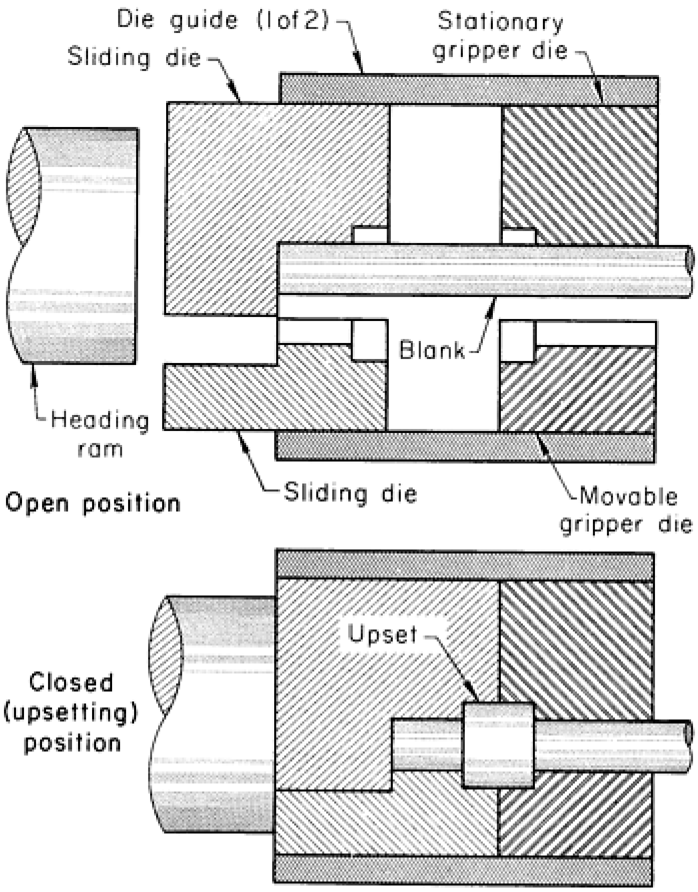

Fig. 13 Typical arrangement of sliding dies used for forging an upset at some point along the length of a bar

Recessed Heading Tools. The use of sliding dies requires a greater-than-normal amount of die maintenance and often presents operating problems. Forging scale becomes entrapped between the sliding members, causing scoring, excessive wear, and sticking. Springs that return the dies to the open position often become weakened because of the softening effect of heat, or they become loaded with scale, which interferes with their action.

Because of these undesirable features, the use of recessed heading tools (or hollow punches), as described in the following example, is a common alternative to sliding dies. When this method is used, however, a slight draft, or taper, must be added to the portion of the stock contained in the heading-tool cavity to facilitate removal after upsetting.

Example 6: Use of Two-Piece Recessed Heading Tools for Center Upset.

The forging shown in Fig. 14 was center upset in two passes in a 152 mm (6 in.) machine using recessed heading tools. As the tooling arrangement in Fig. 14 indicates, two-piece recessed heading tools were used to facilitate machining of the deep cavities.

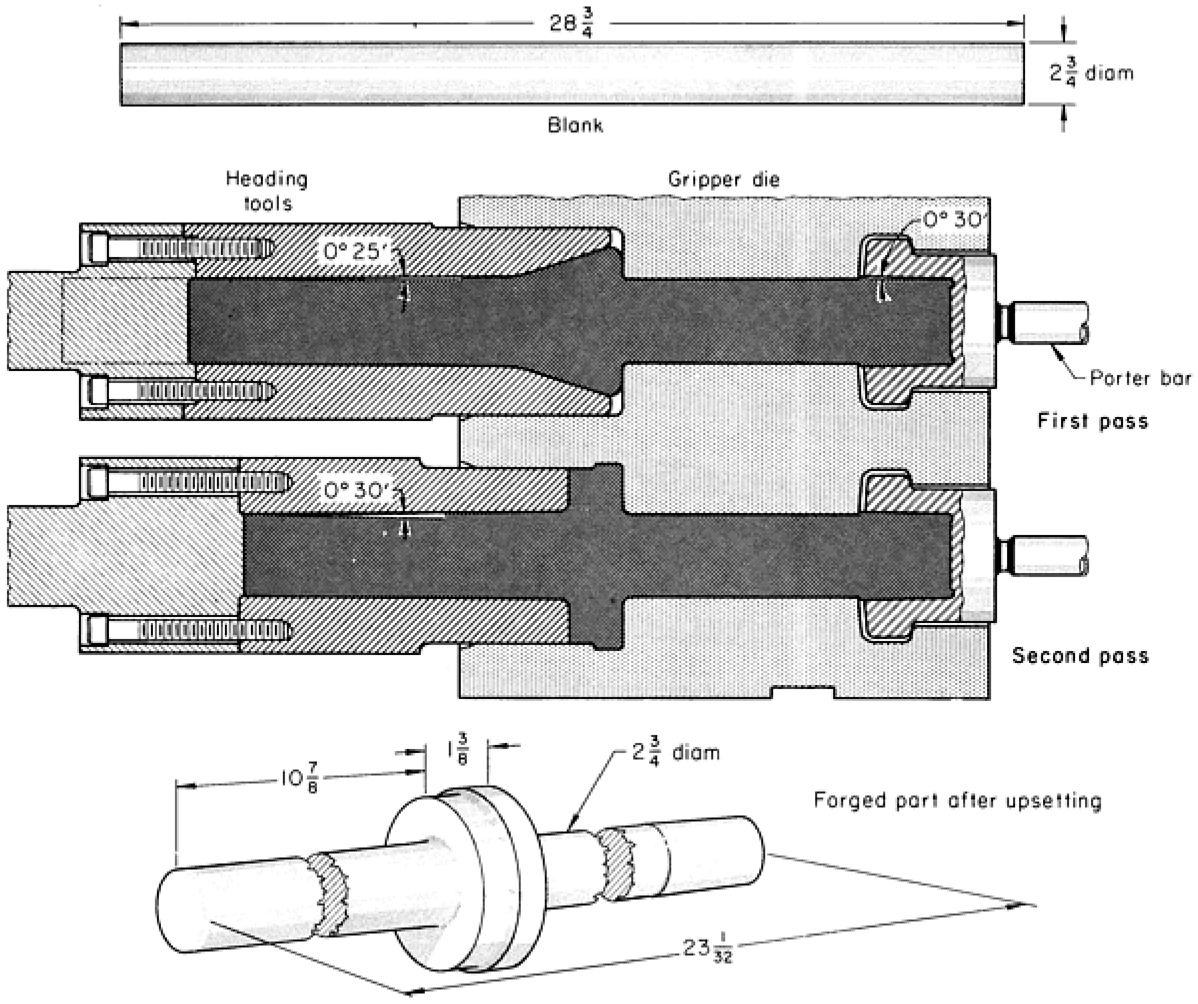

Fig. 14 Tooling setup for two-pass center upsetting using two-piece recessed heading tools. Dimensions given in inches

The bore in the first-pass heading tool had a 0° 25' taper, and the bore in the second-pass tool had a 0° 30' taper to assist in removal of the forging. A backstop porter bar was used in addition to the gripper dies to locate the upset portion.

The first pass gathered the stock into a conical shape; the second pass finish-upset the flange. Both header tools were piloted in the gripper die to ensure alignment.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Upsetting Pipe and Tubing

In many applications, it is desirable and practical to use seamless pipe or mechanical tubing as the stock for upset forgings, particularly for long forgings requiring a through hole. The use of tubular stock for such forgings reduces weight and eliminates the need for gun drilling.

Many forge shops are reluctant to use pipe or tubing as raw material for upset forgings because these product forms present forging problems not encountered when upsetting bar stock. However, most of these problems can be eliminated or minimized by fully understanding the dimensional tolerances applicable to pipe or tubing and making compensating allowances for those tolerances in both the forging design and the die design; by employing heating techniques that will provide close control of temperature and of length heated; and by observing the following rules, which relate wall thickness to the extent to which tubing can be upset in a single blow without injurious folds or buckling:

- To prevent buckling in single-blow flanging, the length of working stock to be upset without support should not exceed $2\frac{1}{2}$ times the wall thickness of the stock
- In single-blow external upsetting (increasing the outside diameter of the tubing while confining the inside diameter), the wall thickness of working stock can be increased to a maximum of $1\frac{1}{2}$ times its original thickness. When greater wall thickness is required, successive outside upsets can be made, using the minimum wall thickness of the preceding upset as the limiting thickness
- In single-blow internal upsetting (decreasing the inside diameter of the tubing while confining the outside diameter), the wall thickness of working stock can be increased to a maximum of twice its original thickness. When greater wall thickness is required, successive inside upsets can be made, using the minimum wall thickness of the preceding upset as the limiting thickness
- In single-blow external and internal upsetting (simultaneously increasing the outside diameter and decreasing the inside diameter), the wall thickness of working stock can be increased to a maximum of $1\frac{1}{2}$ times its original thickness

Tolerances. Pipe or tubing used for upset forgings is normally purchased to specified outside diameter and wall thickness dimensions. Both of these dimensions are subject to mill tolerances. For example, pipe having an outside diameter up to 38 mm ($1\frac{1}{2}$ in.) can vary +0.4, -0.8 mm ($+\frac{1}{64}$, $-\frac{1}{32}$ in.); pipe 50 mm (2 in.) and more can vary -1% from standard. Wall thickness can vary -12.5% from standard. No direct tolerances apply to the inside diameter or to concentricity between outside and inside diameters; these dimensions are controlled only as required to meet the tolerances on outside diameter and wall thickness. Consequently, there is almost always some eccentricity, within the allowable wall thickness variations, between the outside and inside diameters of pierced tubing or pipe. This condition must be recognized, and the necessary allowances made in the design of the forgings as well as the forging tools.

It is also important to understand that the eccentricity does not necessarily run in a straight line throughout the tube. Instead, the locus of the center of the inside diameter may spiral around the centerline, as established from the outside diameter, in a long-pitched helix. That is, if a line were scribed along the outside wall of the tube connecting all points where the wall is thinnest (or thickest), this line may spiral around the outside wall.

When the above condition is not understood, it is commonly assumed that the outside diameter can be made to run true by chucking on the inside diameter for the initial machining. Except on short lengths, however, this is not correct, and in some cases, the runout may even be increased by chucking on the inside diameter. Therefore, it is almost always preferable to design the tubular forging with the understanding that the chucking for the initial machining operations is to be done with reference to the outside diameter. This is important, because a tubular forging with adequate machining-stock allowance when chucked on the outside diameter will not necessarily clean up when chucked on the inside diameter.

Assuming that the initial machining of the forging is to be done from the outside diameter, the outside diameter of the tube, when minimum, should be sufficient to provide the minimum amount of machining. The wall thickness should be such that when it is minimum and the outside diameter is maximum, the minimum desired machining stock will be allowed on the inside diameter. Additional allowances must be made on both the outside diameter and the wall thickness to compensate for any camber that is expected to be present in the forging after processing. The forging limitations in some parts will dictate the selection of tubing with a large outside diameter, a greater wall thickness, or both. However, the above advice should be followed to determine the minimum outside diameter and wall thickness that will ensure that the forging will clean up when machined, regardless of how it is chucked.

Heating pipe and tubing for upsetting requires more critical control than is necessary for bar stock or other solid product forms. For almost all tubular forgings, it is important that the blank be heated so that there is a sharp break between the heated and unheated portions and that this break be at precisely the desired distance from the end of the blank.

Control of the length heated can best be accomplished by induction heating. However, when this method is not available, satisfactory results can be obtained by using water-cooled fronts, or jackets, that are fitted in the slot of ordinary oil-fired or gas-fired slot-type forging furnaces. These fronts are designed with a desired number of holes of proper size, through which the tubular blanks are inserted for heating. Inlet and exhaust water lines to the fronts are located such that the front is completely filled with water at all times. A continuous flow of water, sufficient to prevent boiling, is maintained. The blanks to be heated are gaged from the back, in some convenient manner, to ensure correct length of insertion into the furnace. The use of water-cooled fronts, together with careful control of furnace temperature and time in the furnace, will ensure uniformity of blank temperature and length heated.

When working with thin-wall tubing, it is sometimes difficult to maintain a proper forging temperature in the blank throughout several operations, because of the chilling influence of the dies. This can be partly offset by preheating the dies, but in some applications, it is necessary to reheat the blank one or more times.

Examples of Procedures. A variety of upsetting operations can be performed on pipe or tubing. The wall can be upset externally or internally or both. Tubes can be flared, flanged, pierced, expanded, or reduced (bottled). In many cases, achieving the desired upset shape requires a combination of several of these operations. This is demonstrated in the following examples, which describe the tooling and techniques employed in various production applications involving upsetting of tubing.

Example 7: Internal and External Double-End Upsetting in Three Passes.

A 102 mm (4 in.) upsetter was used for the double-end upsetting of 690 mm ($27\frac{3}{16}$ in.) long, 95 mm ($3\frac{3}{4}$ in.) OD tubes of 4340 steel having a wall thickness of 19 mm (0.750 in.). As shown in Fig. 15, an external collar was upset on one end of the tube in two passes, using the top and center stations in the die, and the opposite end was upset internally in one pass in the bottom station.

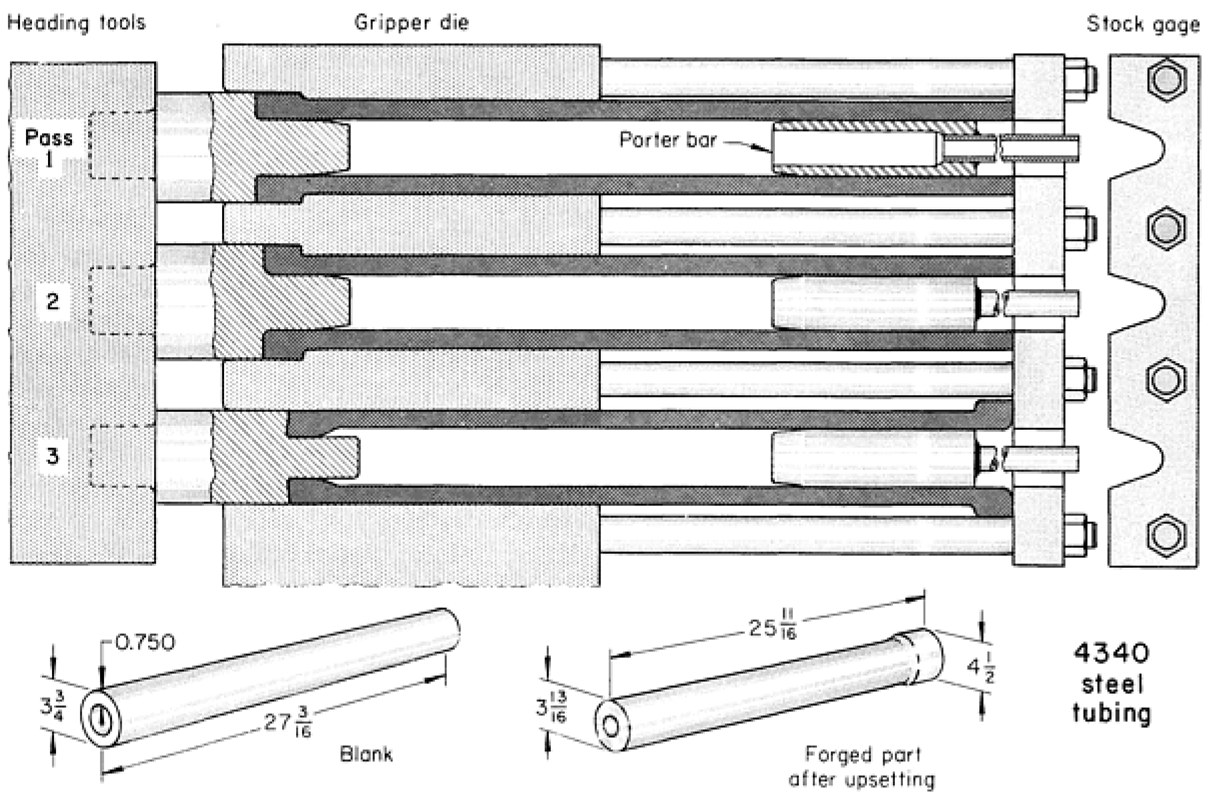

Fig. 15 Tooling setup for external (first and second passes) and internal (third pass) upsetting of opposite ends of a steel tube. Dimensions given in inches

For the external upset, the wall thickness was increased in both the first pass and the second pass by a total of about 50% over the original thickness. Only the amount of stock required for the upset was heated, and a sharp break was maintained between the heated and unheated portions of the stock. This prevented internal upsetting of the stock behind the upset portion.

Grip rings (not shown in Fig.15) designed to bite into the unheated tube were used in all passes in order to prevent slippage through the gripper dies. These rings were supplemented by a backstop secured to the stationary die with studs. The backstop also served as a stock gage and ensured close control of the length between upsets.

Blanks were prepared by sawing and were heated at 1205 °C (2200 °F) in a gas-fired slot-type furnace with a water-cooled front. Dies were made from H10 tool steel. Production rate was 32 pieces per heat. Die life was about 6000 pieces before reconditioning was required.

In this case, two passes were required for producing the external upset at one end of the forging, because the 50% increase in wall thickness was too great to be made in a single pass without risking forging defects. In upsets of this type, the metal barrels outward in one or more convolutions, depending on the length being forged, as the heading tool begins to work. If this outward barreling is contained quickly enough, the metal flows back in a smooth upset that is free from defects; if not, cold shuts may develop. Considering wall thickness variations and other factors, the practical maximum safe external upset in one pass is a 40% increase in wall thickness.

For internal upsets such as the one produced at the opposite end of the forging in Example 7, the only means of controlling the transition contour between the inside diameter of the upset and the inside diameter of the stock is by control of the length heated. This is less precise than control by tools, and tolerances must be established accordingly; however, if good control of the length heated is maintained, transitional contours can be consistently reproduced.

An unusual feature of the procedure described in the next example is the use of a combination flaring and upsetting operation in the first pass. When forging design permits the use of this type of operation, greater lengths of stock can be gathered in a single pass than in a straight external upsetting operation of the type described in Example 7.

Example 8: Upsetting and Flaring One End in Two Passes.

A 175 mm ($6\frac{7}{8}$ in.) flange was upset on the end of a 4340 steel tube, 114 mm ($4\frac{1}{2}$ in.) outside diameter and 22.2 mm (0.875 in.) wall thickness, in two passes in a 152 mm (6 in.) machine, using the tooling setup shown in Fig. 16. The heading tool for the first pass was unique in that it first flared and then upset the end of the stock in a continuous movement. The initial flaring produced a shape that hugged the heading tool as the tool traveled inward. When the stock became seated in the deepest section of the heading tool, it remained there, and the continuing forward movement of the tool upset the stock and filled the cavity. Forward movement was controlled so that no flash was formed. Because of the inherent variation in tubing wall thickness, however, the degree of filling varied around the periphery of the upsetting tool.

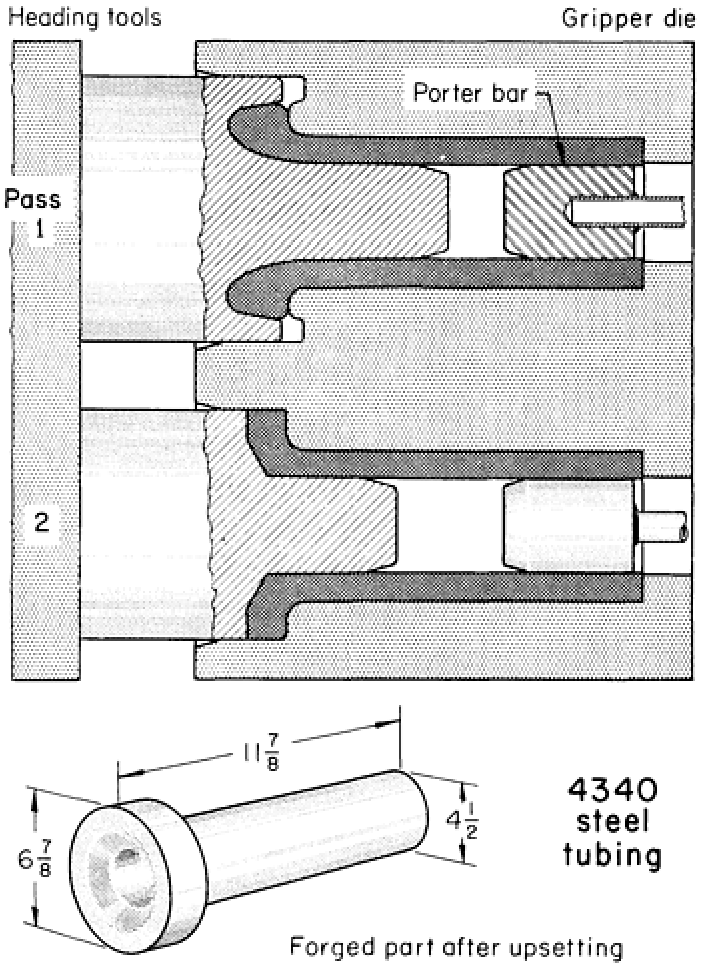

Fig. 16 Tooling setup for producing a flange on one end of a steel tube in two passes in a 152-mm (6-in.) upsetter. The first pass, a combination flaring-upsetting action, permitted gathering of a greater amount of stock than would have been possible by upsetting alone. Dimensions given in inches

The 360 mm ($14\frac{3}{16}$ in.) long blanks were prepared by sawing. Heating was done in a gas-fired, slot-type, water-cooled-front furnace at 1205 °C (2200 °F). Dies were made from H10 tool steel. Production rate was 55 pieces per hour, and die life was about 6000 pieces before reconditioning.

Upsetting Away From the Tube End. For some forgings, an upset must be produced at a distance from the end of the tube. A successful upset of this kind is described in the following example.

Example 9: Forming a Flange a Short Distance From the End in Three Passes.

The flange on the 4340 steel tube shown in Fig. 17 was produced in three passes in a 102 mm (4 in.) upsetter. Blanks were 718 mm ($28\frac{1}{4}$ in.) lengths of 64 mm ($2\frac{1}{2}$ in.) OD seamless mechanical tubing with a wall thickness of 18.2 mm (0.718 in.). The problem of upsetting the flange a short distance back from the end of the tube was solved by the use of the tooling setup illustrated in Fig. 17. In the first pass, the stock was upset into a cavity in the die, increasing the wall thickness by about 33%. In the second and third passes, the wall thickness through the upset was increased 39 and 23%, respectively, using heading tools that were designed to support the unforged section ahead of the flange.

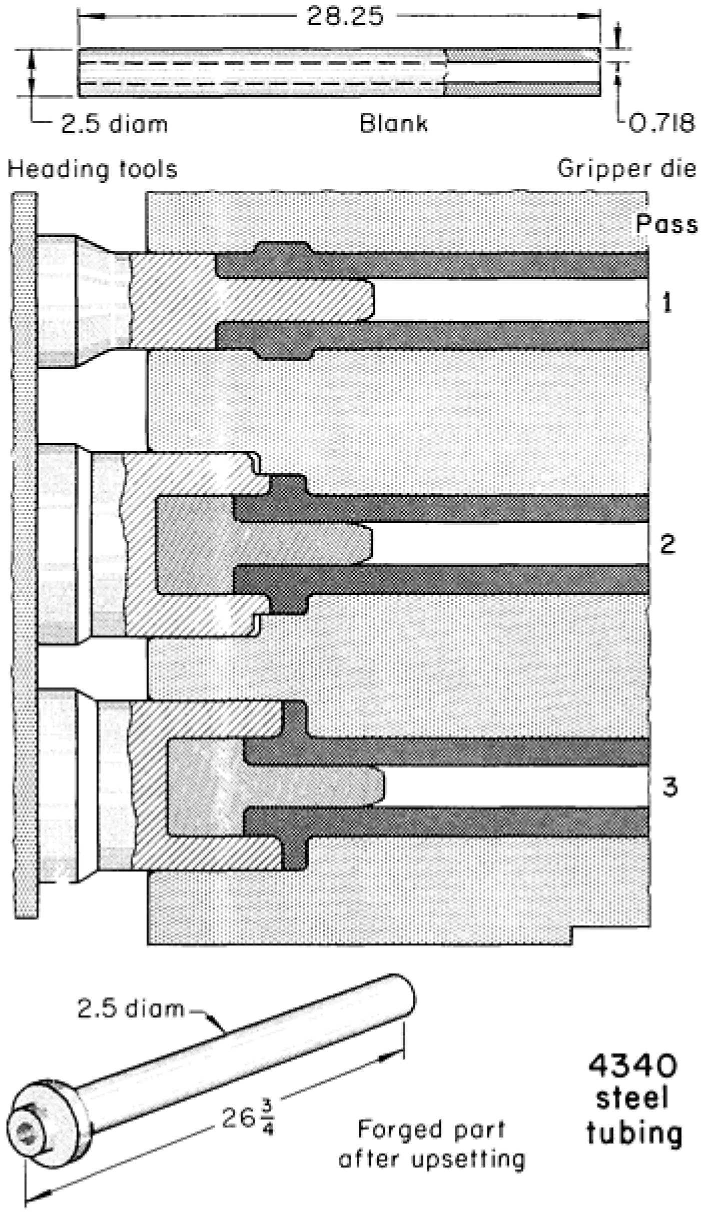

Fig. 17 Tooling setup for upsetting a flange a short distance in from the end of a tube. Dimensions given in inches

Blanks were prepared by sawing and were heated at 1205 °C (2200 °F) in a gas-fired, slot-type, water-cooled-front furnace. Dies were made from H10 tool steel. The production rate was 55 pieces per hour, and about 6000 pieces were produced before dies required reconditioning.

The die design and technique described in this example could be used for producing a flange still farther from the end of a tube. However, if the flange were considerably removed from the end, it would be necessary that only a band of the tube of proper length and location for the upset be heated, leaving both ends cool.

Large Workpieces. The upsetting of unusually large tubes may present tooling problems and may require the use of more heating operations or an increased number of passes or both, as indicated in the following example.

Example 10: Double-End Upsetting (Flanging and Bottling) of Large-Diameter Tubing in Three Heats and Six Passes.

The tooling used for producing a particularly difficult tubular forging by double-end upsetting in six passes and three heats is shown in Fig. 18. A 229 mm (9 in.) upsetter was used. The forging blanks were 1.14 m ($44\frac{7}{8}$ in.) lengths of 238 mm ($9\frac{3}{8}$ in.) OD 8620 steel seamless mechanical tubing with 19 mm (0.750 in.) wall thickness.

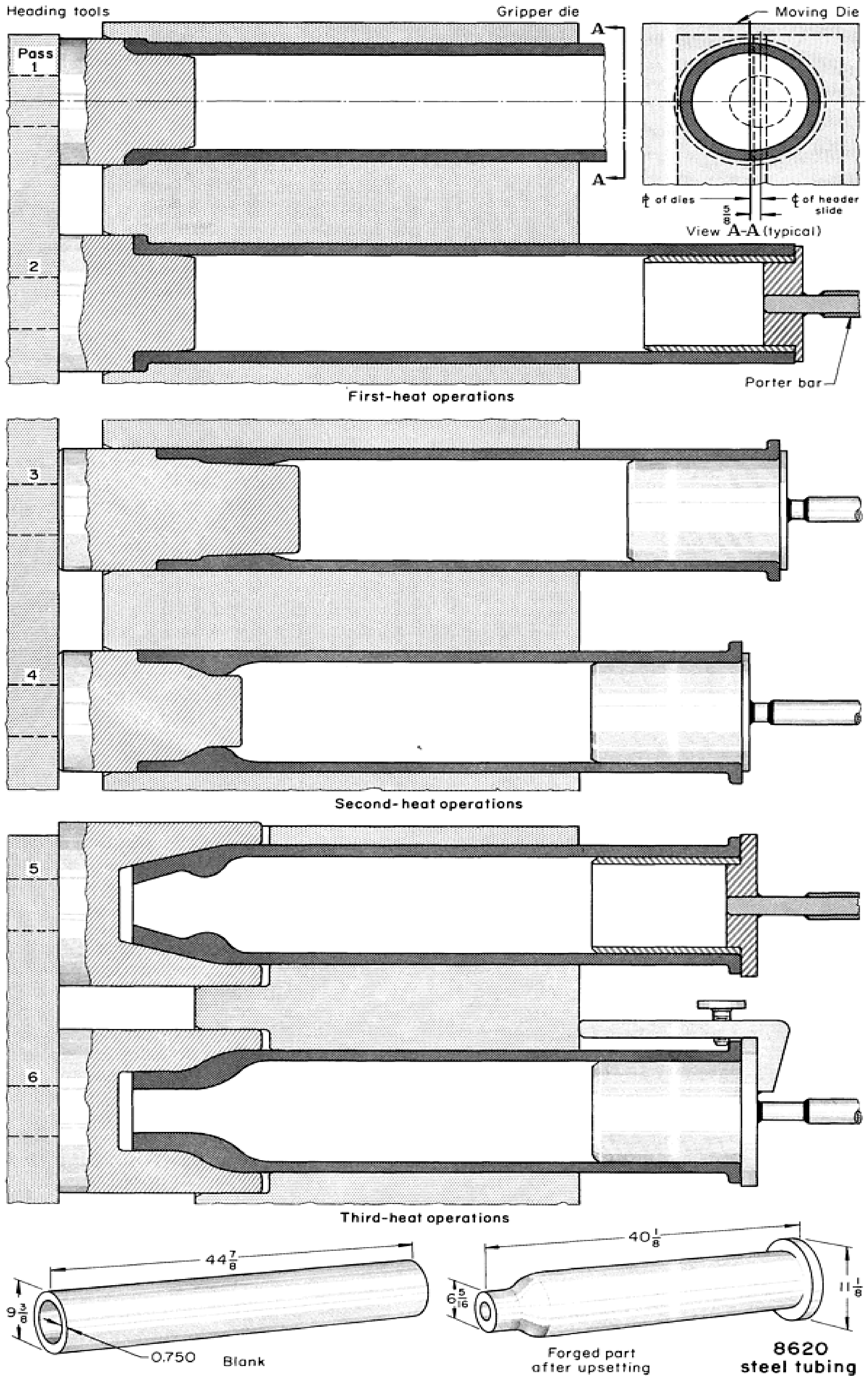
Heading tools
Gripper die
Moving Die
Pass 1
A
A
¢ of dies
5/8
¢ of header slide
View A-A (typical)
2
Porter bar
First-heat operations
3
4
Second-heat operations
5
6
Third-heat operations
44 7/8
9 3/8
0.750
Blank
40 1/8
6 5/16
11 1/8
Forged part after upsetting
8620 steel tubing

Fig. 18 Tooling setup for double-end upsetting of a large-diameter steel tube in six passes and three heats. Dimensions given in inches.

The unusually large outside diameter of the stock posed a problem because, following normal design procedures, there would have been interference between the tube and the stationary-die side of the machine. To prevent this interference, the die parting line was moved 16 mm ($\frac{5}{8}$ in.) from the vertical centerline of the header slide, toward the moving-die side of the machine. Heading tools were eccentrically shanked and keyed to the main toolholder to maintain alignment with the dies.

As shown in Fig. 18, in the first heat, one end of the tube was flanged in two operations. In the second heat, the opposite end was internally upset in two operations. In the third heat, the internally upset end was bottled, or reduced, in two operations. Controlled heating was an important factor in the production of acceptable forgings, and it was particularly critical for the second-heat and third-heat operations because production of the inside contour of the bottled section depended entirely on the maintenance of uniform blank temperature and length heated.

Blanks were prepared by sawing and were heated at 1205 °C (2200 °F) in a gas-fired, slot-type, water-cooled-front furnace. Dies were made from H10 tool steel. The production rate was 16 pieces per hour, and about 6000 pieces were produced before dies required reconditioning.

Hot Upset Forging

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Assignment of Tolerances

Any forging, regardless of its simplicity, may become a severe production problem if the forging tolerances assigned to it are unduly restrictive. Therefore, the tolerances specified for any new forging should be critically reviewed to determine whether or not they will result in the lowest cost for the finished part. This will not be accomplished by assigning tolerances that are so loose that all control of forging quality is lost. On the other hand, it is also possible to be too restrictive in an effort to avoid some subsequent cost, so that the end cost is actually increased because of the excessive die replacement and the high percentage of rejected forgings that result from the attempt to maintain close tolerances.

The establishment of optimal tolerances is based largely on consideration of all operations required to make the finished part. For example, if holding an abnormally tight tolerance in upsetting eliminates a subsequent machining operation, it is likely to prove economical to hold the close tolerance. However, if the machining operation cannot be completely eliminated, it will probably be less costly to use loose tolerances, thus lowering forging cost, and to make the corrections in machining.

Tolerances for upset forging are not completely standardized and are usually negotiated between the forger and the user. The most common tolerance for upset diameters is +1.6, -0 mm (+$\frac{1}{16}$, -0 in.). For thin sections of flanges and for upsets relatively large in ratio to the stock sizes used, the tolerance is +2.4, -0 mm (+$\frac{3}{32}$, -0 in.). An increase over these values is often necessary because of variations in the size of the hot-rolled bars, extreme die wear, or complexity of the part. Tolerances that are tighter than those mentioned above are arbitrarily identified as close tolerances. Individual tolerance specifications cited in the examples in this article vary widely, from 0.2 mm (0.008 in.) total tolerance to ±3.2 mm (±$\frac{1}{8}$ in.). For an upset forged part that requires several operations or passes, the dimensioning of lengths is determined on the basis of the design of each individual pass or operation.

Effect of Tolerances on Cost

As tolerances are tightened, cost generally is increased, mainly because of the decreased number of parts that can be obtained before dies require reworking to maintain the tolerances. Cost is increased through die resinking as well as increased setup time and machine downtime. Die life between reworkings may vary several hundred percent, depending on workpiece shape. However, for any given shape, tool life decreases rapidly as tolerances are tightened.

If close-tolerance upsetting is required, costs can be minimized by observing the following practices during die design and die maintenance:

- Tool materials and methods of heat treatment should be selected with care. Some experimentation may be required to determine the tool materials that are best suited to a specific job. A detailed discussion of the selection of tool materials for hot upset forging is provided in the article "Dies and Die Materials for Hot Forging" in this Volume
- Welding should be used for the repair of areas in die inserts where wear is most severe
- Sidewise mismatch should be reduced by restricting clearance between the heading tool and heading-tool guides in the gripper dies to 0.4 mm ($\frac{1}{64}$ in.) or less
- All practical steps should be taken to minimize the introduction of scale into the tooling, either by preventing the formation of scale (by heating under atmosphere protection, or rapidly as by induction) or by removing it. Effective methods are discussed in the section "Descaling" in this article
- Endwise mismatch should be reduced by the use of die locks to secure the gripper dies in the closed position

Probably the most efffective die lock is the bar lock, which consists of a key inserted in the face of the moving die and a mating keyway in the face of the stationary die. Wide master dies or die blocks are required for this type of lock. A typical bar lock for a 152 mm (6 in.) upsetter would be about 75 mm (3 in.) wide, protruding out of the moving die about 50 mm (2 in.) and locking into the stationary die.

Other types of die locks can be substituted; they are less expensive but are also less effective than bar locks. For example, a lock consisting of two or four round dowels pressed in at the faces of the dies can be used, or the top and bottom of the dies can be milled to accommodate a rectangular, tapered lock (about 25 × 75 × 152 mm, or 1 × 3 × 6 in.) that is bolted in position.

Die locks must be reworked after each resinking of the inserts. This can be done by hardfacing the locking surfaces.

Hot Upsetting Versus Alternative Processes

Hammer and press forging, hot extrusion, cold heading, and cold extrusion may, under specific conditions, be alternative processes for hot upsetting. In many cases, two or more of the above processes are combined with each other or with hot upsetting to achieve optimal results. The choice of method depends largely on the size and shape of the upset, the work metal composition, and the available forging equipment.

Hot Upsetting Versus Hammer or Press Forging. In comparing hot upsetting and hammer or press forging, the most important advantage of hot upsetting is that forging can be done in two directions 90° apart, a capability that is built into an upsetter and is common for any tooling. Accomplishing this in a conventional vertical hammer or press requires complex tooling for each part.

Other advantages of hot upsetting over hammer or press forging include:

- Less material is required, because flash is minimized or eliminated by the two-direction forging principle, in which just the right amount of metal is trapped in the dies
- Less draft is required, because upset forging dies open in both directions
- Production efficiency is higher for upsetting when piercing, because final piercing and cutoff can generally be accomplished in one pass from long bars
- Grain flow can be more easily controlled
- Large parts, such as automotive axle shafts, cannot fit into the die space (shut height) of a hammer or forging press

The primary disadvantage of hot upsetting is that it is limited to the production of reasonably symmetrical forgings, while hammers or presses can produce a greater variety of shapes. There are applications in which hammer or press forging can be advantageously combined with hot upsetting, as in Example 5.

Hot upsetting and hot extrusion are closely related. In many applications, some extrusion takes place during upsetting, or some upsetting during extrusion. When an upset is required that is much larger in diameter than the starting blank (six times, for example), hot upsetting or hot extrusion can be used, but this extreme severity may present difficulty with either process used alone. However, hot upsetting of a preform made by hot extrusion is often the best procedure for producing a part that requires a severe upset.

Hot Upsetting Versus Cold Heading. Size is the major factor in determining whether hot upsetting or cold heading will be used for a specific application. When cold heading can meet all requirements, it is less expensive than hot upsetting, because heating the blanks and cleaning the headed parts are eliminated.

Cold heading is generally restricted to blanks no more than 38 to 50 mm ($1\frac{1}{2}$ to 2 in.) in diameter, and most cold heading is done on starting diameters less than 32 mm ($1\frac{1}{4}$ in.). Up to about 19 mm ($\frac{3}{4}$ in.) of stock diameter almost any upsetting that can be done hot can also be done cold on ductile metals. This applies to center as well as end upsetting. Exceptions can be work metals that are harder than annealed steels, or extremely severe shapes.

Hot Upsetting Versus Cold Extrusion. Hot upsetting and cold extrusion are often used in sequence to produce a specific shape; hot upsetting is used to produce a preform. Automotive axle shafts are notable examples of parts produced by hot upsetting followed by cold extrusion. Hot upsetting and piercing is sometimes interchangeable with cold extrusion.

Large presses are required for cold extrusion. Thus, the availability of equipment often determines a choice between hot upsetting and piercing, and cold extrusion. More detailed information on cold extrusion is available in the article "Cold Extrusion" in this Volume.

Revised by Wilfred L. Mehling, Ajax Manufacturing Company

Safety

A primary consideration in hot upsetting is the safety of the operator. Adequate training must be provided before operators are allowed to work with hot upsetting equipment, and protective clothing and equipment must be used. Ear protection may or may not be necessary, depending on the noise level in the shop. The need for aprons, spats, leggings, and sleeves depends on the hazards to which the operator is exposed.

With the exception of the feed area, the entire upsetter should be heavily guarded. Provision should be made such that the access doors to the upsetter must be closed before it can be operated. A guard over the operating pedal and a pedal lock will minimize accidental tripping of the upsetter. All loose articles should be removed from the top of the upsetter to prevent them from falling from or into the machine.

At no time should the operator put his hands or arms between the dies of the upsetter. Lubricating swabs or scale removers should have handles that are long enough to permit the operator to reach the full length of the dies without putting his hands between the dies. Before an operator makes an adjustment to any of the tools or dies, the power should be locked off, the flywheel should be completely stopped, and the air, water, and oil lines should be shut off. All power switches and valves should be identified and should be located where they can be easily reached by the operator. In handling heavy tools, lifting equipment is needed; the operator should use care to avoid injury when changing tools.

When gripper dies are used, it is important that the dies hold the forging in place. Although the use of backstops is recommended where practical, they should not be employed to offset insufficient gripping. Gages should locate the part with minimum hazard to the operator. For heavy forgings, properly maintained balancing equipment will reduce operator fatigue.

A preventive maintenance program is needed to keep upsetters in safe operating condition. In addition to making a daily check of tools, belts, pulleys, lines, gages, and valves, the operator should report any change in the performance of the upsetter when it is first observed. Handling equipment should be checked before it is used and should be thoroughly inspected on a regular basis. Daily lubrication is needed on machines that are not equipped with automatic lubrication. Air clutches and brakes should be checked daily, and moving parts should be checked and adjusted weekly.

An important consideration with regard to safety in hot upset forging is the selection of proper die material and die hardness. This is discussed in the article "Dies and Die Materials for Hot Forging" in this Volume.

Roll Forging

Introduction

ROLL FORGING (also known as hot forge rolling) is a process for reducing the cross-sectional area of heated bars or billets by passing them between two driven rolls that rotate in opposite directions and have one or more matching grooves in each roll. The principle involved in reducing the cross-sectional area of the work metal in roll forging is essentially the same as that employed in rolling mills to reduce billets to bars.

Applications

Any metal that can be forged by other methods can be roll forged. Heating times and temperatures are the same as those used in the forging of metals in open or closed dies. See the articles "Closed-Die Forging in Hammers and Presses" and "Hot Upset Forging," as well as the articles on the forging of specific metals, in this Volume.

Roll forging serves two general areas of application:

- As the sole operation, or as the main operation, in producing a shape
- As a preliminary operation to save material and number of hits in subsequent forging in closed dies

Applications in the first category above generally involve the shaping of long, thin, usually tapered parts. Typical examples are airplane propeller-blade half sections, tapered axle shafts, tapered leaf springs, table-knife blades, barge nails, hand shovels and spades, various agricultural tools (such as pitchforks), and tradesman's tools (such as chisels and trowels). Roll forging is sometimes followed by the upsetting of one end of the workpiece to form a flange, as in the forging of axle shafts.

Applications in the second category above include preliminary shaping of stock prior to forging in closed dies in either a press or hammer, thus eliminating a fullering or blocking operation. Crankshafts, connecting rods, and other automotive parts are typical products that are first roll forged from billets to preform stock, and then finish forged in a press.

Machines

Machines for roll forging (often called forge rolls, reducer rolls, back rolls, or gap rolls) are of two general types (Fig. 1 and 2). In both types, the driving motor is mounted at the top of the main housing. The motor drives a large flywheel by means of V-belts. In turn, the flywheel drives the roll shafts, to which the roll dies are attached, through a system of gears.

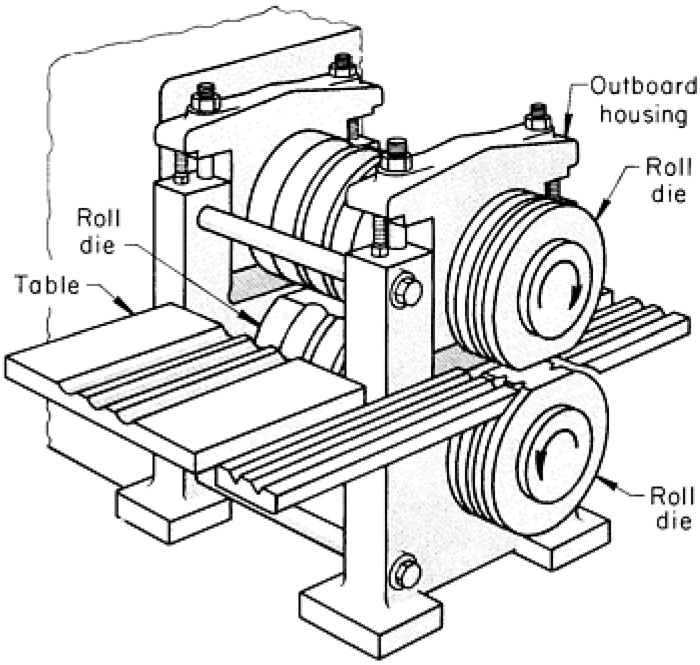

Fig. 1 Roll-forging machine with outboard housing.

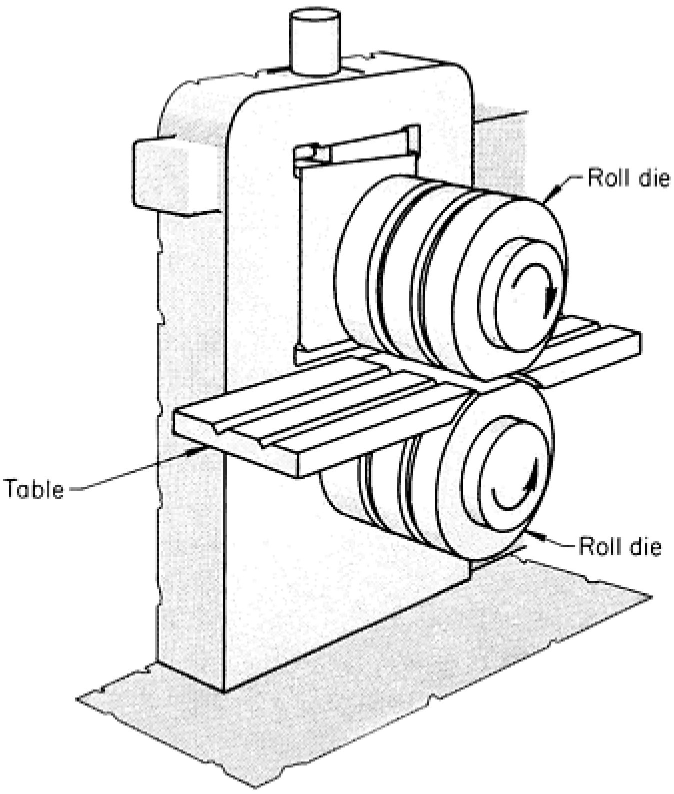

Fig. 2 Overhang-type roll-forging machine.

The machine shown in Fig. 1 has an outboard housing, which supports the roll shafts at both ends. On this machine, the shafts extend through the housing, thus permitting an additional pair of roll dies to be mounted on the shafts. On some machines of this type, the roll shafts extend only into the outboard housing; this permits the use of only one set of roll dies. Various sizes of this type of machine, ranging from 3.7 to 220 kW (5 to 300 hp), will accommodate roll dies 318 to 1120 mm ($12\frac{1}{2}$ to 44 in.) in diameter and 356 to 1520 mm (14 to 60 in.) wide.

The machine illustrated in Fig. 2 is generally known as the overhang type because it has no outboard housing to support the roll shafts. Otherwise, the significant components of this machine are similar to those of the machine illustrated in Fig. 1. Depending on size, these machines are equipped with 15 to 75 kW (20 to 100 hp) motors and will accommodate roll dies 305 to 559 mm (12 to 22 in.) in diameter and 178 to 457 mm (7 to 18 in.) wide.

Selection. The outboard-housing type of machine (Fig. 1) is ordinarily used when roll forging is the sole or the main operation for producing a shape and when close tolerances are required on the workpiece. The reason for the preference is that this class of work generally requires wide roll dies with many grooves (sometimes as many as 12 or more, but usually fewer than 8). If roll dies are extremely wide in relation to their diameter, lack of rigidity is a problem.

The overhang-type machine (Fig. 2) is most often used for the roll forging of stock in preparation for closed-die forging or up-setting. For this type of work, relatively narrow roll dies with two to four grooves are generally used. Therefore, lack of rigidity caused by excessive overhang is not a problem, and better accessibility is gained by the absence of the outboard housing. In addition, the fully cylindrical roll dies used in this type of machine offer more periphery for roll forging.

Selection of machine size depends mainly on the following considerations:

- Power must be adequate to reduce the forging stock
- Rigidity must be sufficient to maintain dimensional accuracy. Adequate rigidity is especially important when rolling to thin, wide wedge shapes
- Roll shafts must be long enough (overhang or distance between housings) to accommodate roll dies that

are wide enough to contain the entire series of grooves required to accomplish the cross-sectional reduction. The width of the roll dies can sometimes be reduced by using the first-reduction grooves for two or more passes or by inching the workpiece forward in the tapered grooves
- Distance between centers of roll shafts must be sufficient to accommodate roll dies large enough in diameter to roll the full length of the reduced section of the workpiece, so that the taper will not have to be overlapped in adjacent grooves of the roll dies

Forging rolls are available in numerous sizes and have the capacity to roll blanks up to 127 mm (5 in.) thick and 1020 mm (40 in.) tong.

Operation. Roll dies designed for forging the required shape are bolted to the roll shafts, which rotate in opposite directions during operation. Roll dies (or their effective forging portion) usually occupy about one-half the total circumference; therefore, at least some forging action takes place during half of the revolution.

Machines can be operated continuously or stopped between passes, as required. In the roll forging of long tapered workpieces, the more common practice is to operate the machine intermittently, using the following technique (Fig. 3):

- The operator lays the heated stock on the table of the machine, grasps the stock with tongs, and starts the machine (commonly controlled by a foot treadle)
- During the portion of the revolution when the roll dies are in the open position, the operator places the stock between them against a stock gage and in line with the first roll groove, retaining his tong hold on the workpiece. The tables are usually grooved to assist in aligning the stock
- As the roll dies rotate to the closed position, forging begins. The workpiece is forced back toward the operator, who moves it to the position of the next roll-die groove and again pushes it against the stop during the open position of the roll dies. This is repeated until the workpiece has been forged through the entire series of grooves

In a few mass-production applications, the roll-forging procedure described above has been automated, but manual operation is by far the most common practice.

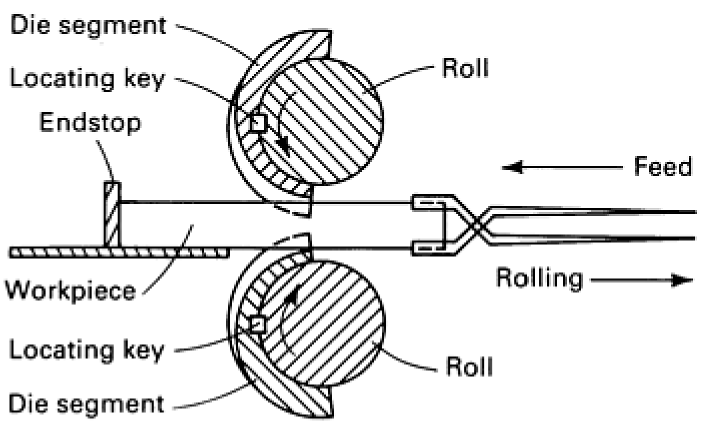

Fig. 3 Schematic of roll-forging operation using multiple passes.

When side squeezing between roll passes is desirable for such operations as the pointing of springs or the tapering of chisel blades, the machine can be designed to incorporate a horizontal front press close to the rolls. For shearing, trimming, straightening, and bending, a vertical side press can be built into the main housing. Both of these auxiliary presses are of the simple eccentric type, driven from a roll shaft.

When roll forging is used to preform stock prior to completing in dies, the machine is usually stopped after each roll pass, partly because fewer passes are used (often only one or two) and partly because continuous operation may be undesirable for the companion forging operations. Some automation is usually applied to this type of roll-forging application; therefore, little or no manual handling is required.

Roll Forging

Roll Dies

Roll dies are of three types: flat back, semicylindrical, and fully cylindrical (Fig. 4).

Fig. 4 Three types of dies used in roll forging.

Flat-back dies are primarily used for short-length reductions. They are bolted to the roll shafts and can be easily changed. Typical contours for a set of flat-back segmental dies are shown in Fig. 5.

Fig. 5 Contours in a typical set of flat-back segmental dies used to forge the workpiece illustrated.

Semicylindrical dies are well suited to the forging of medium-length workpieces. Most are true half-cylinders (180°), although some (particularly in large sizes) may encompass up to 220° of a circle to provide sufficient periphery for the specific application. When each die section is no more than 180°, the dies can be made by first machining the flat surfaces of the half-rounds for assembly, clamping the half-rounds together, and then boring and finishing.

Example 1: Forging of an Axle Shaft in Ten Passes Through Eight-Groove Semi-Cylindrical Roll Dies.

An axle shaft was roll forged from a 1037 steel blank in ten passes through eight-groove semicylindrical roll dies, as shown in Fig. 6. After each successive pass, the workpiece was rotated 90°. The shaft was forged in a 30 kW (40 hp) machine with an outboard housing; the eight-groove dies were 635 mm (24 in.) wide. The roll shafts were rotated at 40 rpm. In continuous operation, one operator rolled approximately 180 shafts per hour.

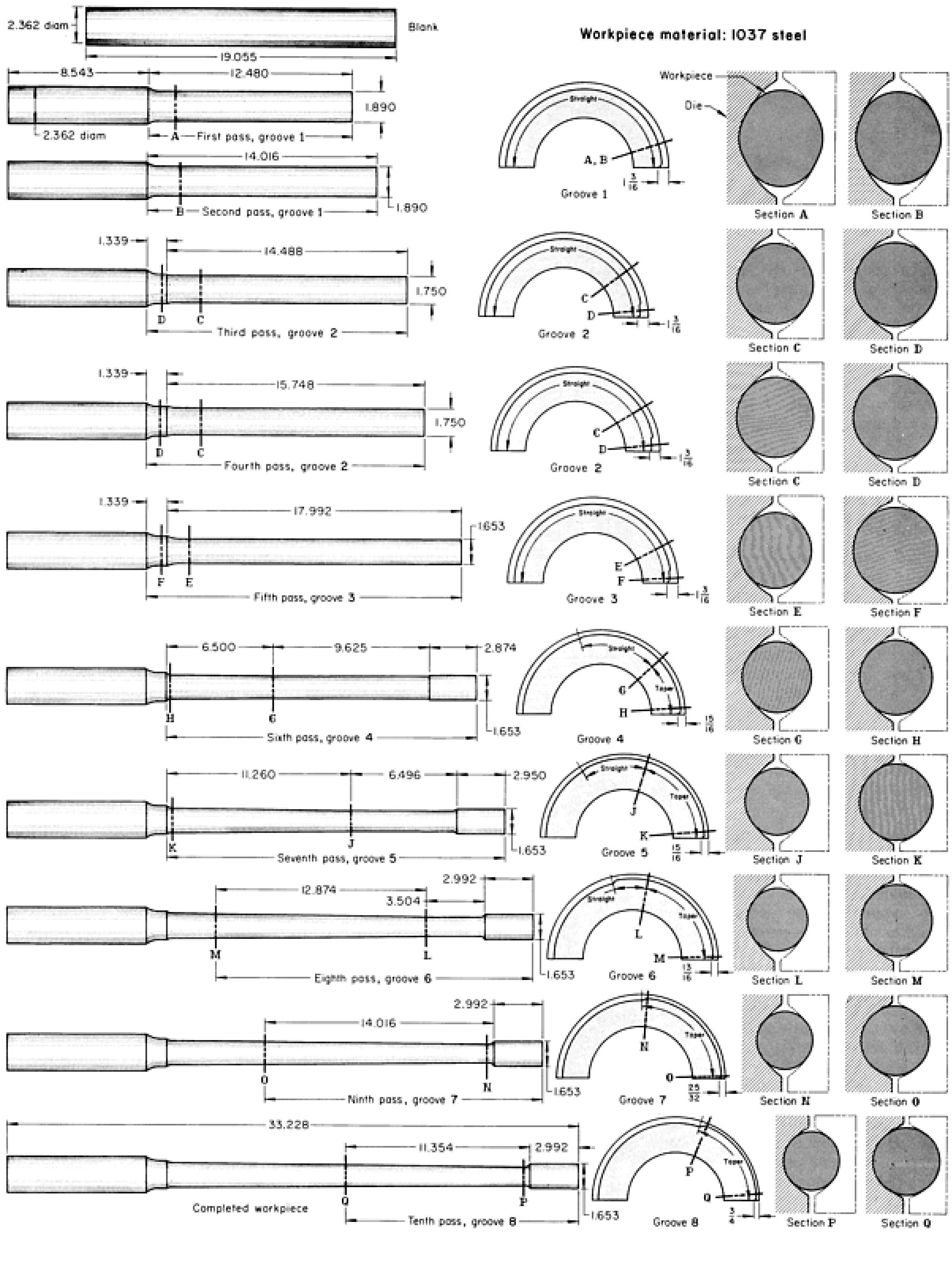

Fig. 6 Forging of an axle shaft in ten passes through eight-groove semicylindrical roll dies. Dimensions given in inches.

After it was roll forged, the shaft was straightened by hot coining and was sheared without being reheated. The large end was then reheated and was flanged in an upsetter.

Fully cylindrical dies are used for the forging of long members, sometimes in an overhang-type machine. They are made most economically by being built up with rings, with a cutaway portion just large enough to feed in the forging stock. Fully cylindrical dies are sometimes more efficient than semicylindrical or flat-back dies because of the larger periphery available for the forging action. However, one disadvantage of fully cylindrical dies is that the opening is too small to permit continuous operation; consequently, these dies require control of motion by a clutch and a brake.

Material. Steels used for roll dies do not differ greatly from those used for dies in hammer, press, and upset forging (see the articles "Closed-Die Forging in Hammers and Presses" and "Hot Upset Forging" in this Volume). However, because roll dies are subjected to less impact than dies in other types of forging, they can be made of die steels that are somewhat higher in carbon content--which is helpful in prolonging die life. The following composition is typical for roll dies:

Element	Composition, %
C	**0.70-0.80**
Mn	**0.60-0.80**
Si	**0.30-0.40**
Cr	**0.90-0.95**
Mo	**0.30-0.35**

Dies can be made from wrought material or from castings.

Hardness of roll dies is likely to vary considerably, depending largely on whether or not changes in die design are anticipated. When the die design is not subject to change, a hardness range of 50 to 55 HRC is common. Although this range is higher than can be tolerated in most hammer or press forging, it is permissible in roll forging because the dies are subjected to less impact, which helps to prolong die life.

When dies are subject to design changes, common practice is to keep hardness below the maximum that is practical to machine. Under these conditions, 45 HRC is the approximate maximum, and a range of 35 to 40 HRC is more common.

Die life depends mainly on die hardness, severity (depth of the grooves or other configurations in the dies), whether or not flash is permitted, and work metal composition. Die hardness has a major influence on die life. Dies hardened to 50 to 55 HRC have often had a total life of 190,000 to 200,000 pieces in the no-flash roll forging of low-carbon steel to simple shapes (severity no greater than that of the workpiece described in Example 1). In similar applications, however, dies of the same materials at 35 to 40 HRC have had a total life of only 30,000 pieces.

As severity increases, die life will decrease, to a degree generally parallel to that experienced with similar changes of severity in hammer and press forging (see the article "Dies and Die Materials for Hot Forging" in this Volume). If any flash is formed and not allowed for in die design, the dies will be overstressed and their life shortened. Although little significant difference in die life can be attributed to variations in composition among the carbon and alloy steels that are most commonly roll forged, die life does decrease as the hot strength of the work metal increases, as with other types of forging dies.

High-Energy-Rate Forging

Revised by Natraj C. Iyer, Westinghouse R&D Center

Introduction

HIGH-ENERGY-RATE FORGING (HERF), sometimes called high-velocity forging, is a closed-die hot- or cold-forging process in which the stored energy of high-pressure gas is used to accelerate a ram to unusually high velocities in order to effect deformation of the workpiece. Ideally, the final configuration of the forging is developed in one blow or, at most, a few blows. In high-energy-rate forging, the velocity of the ram, rather than its mass, generates the major forging force. Table 1 lists the die-closing speeds of forming machines, and it is apparent that the maximum impact velocity of HERF machines is about three to four times that of conventional drop hammers. Typically, the ram velocity at impact in the HERF machine is in the range of 5 to 22 m/s (16 to 72 ft/s); ram velocities range from 4.5 to 9.1 m/s (15 to 30 ft/s) for a power-drop hammer and from 3.6 to 5.5 m/s (12 to 18 ft/s) for a gravity-drop hammer.

Table 1 Forming machines and their die closing speeds

Press type	Impact speed	
	m/s	ft/s
Hydraulic press	0.27-0.456	**0.89-1.50**
Crank press	0.03-1.52	**0.10-4.99**
Toggle press	0.03-1.52	**0.10-4.99**
Friction screw press	0.30-1.21	**0.98-3.97**
Drop hammer	3.65-5.50	**12.0-18.0**
Power hammer	4.50-9.10	**14.8-29.9**

High-energy-rate machines can be used to hot forge parts of the same general shapes as those produced with conventional hammers and presses. However, the work metal must be capable of undergoing extremely rapid deformation rates as it fills the die cavity without rupturing it. In high-energy-rate forging, the high ram velocities permit the forging of parts with thin webs, high rib height-to-width ratios, and small draft angles to profiles sufficiently accurate that machining allowance can sometimes be as little as 0.500 mm (0.0197 in.). Even parts made of difficult-to-forge metals can be formed close to finished dimensions in a few blows and often without reheating.

When evaluating high-energy-rate forging in relation to conventional forging, both the machine advantages and process advantages, as a result of the high velocities, must be considered. The machine advantages are beyond dispute. For a given forming capacity, high-speed machines are much smaller than conventional forging machines, and they require much less installation/foundation and therefore a lower capital investment. These advantages arise because the principle utilized in these machines involves the conversion of the kinetic energy of a ram/platen into forming work. Kinetic energy is proportional to the square of the impact velocity; therefore, a threefold increase in impact speed produces a ninefold increase in forming energy. High-energy-rate forging machines are typically one-ninth the bulk and weight of equivalent slow-speed machines. Although the finished forging is generally made in one high-speed blow, some machines can be fired two or three times before the work metal has cooled below the forging temperature.

The process advantages are not as obvious as the machine advantages and depend on the particular application under consideration. In general, high-energy-rate forging offers the following advantages over conventional forging methods:

- Complex parts can be forged in one blow from a billet or a preform
- Many metals that have low forgeability or are difficult to forge by other methods can be successfully forged
- Dimensional accuracy, surface detail, and, often, surface finish are improved
- Draft allowances, both internal and external, can be reduced or, in some applications, eliminated
- Forgings are made to size or with a minimum of machining allowance. Reduced machining lowers the induced mechanical stress and minimizes the cutting of end grain, which improves the stress-corrosion resistance of some metals, notably aluminum
- Deep, thin sections can be forged because the rapidity of the blow provides little time for heat transfer to the die walls
- Substantial improvement in billet quality can be achieved when cropping/shearing at high speeds
- Severe deformation is possible, with the net result of greater grain refinement in some metals
- Less skill is required for the operating personnel

The process, however, does have the following limitations:

- Sharp corners and small radii cannot be forged without causing undue wear
- The process is generally limited to symmetrical parts, although some asymmetrical parts can be forged from preformed billets
- The production rate is about the same as in hammer or hydraulic press forging, but is slower than in mechanical press forging
- Part size is limited to about 11 to 12 kg (24 to 26 lb) for carbon steel forgings, and to lesser weights for forgings made of stainless steel or heat-resistant alloys
- Dies must be carefully designed and fabricated in order to withstand the high impact; compressive prestressing of the die inserts by a shrink ring is a common practice

Economics of High-Energy-Rate Forging. As mentioned earlier, HERF devices are only one-ninth the size and weight of conventional hammers and about two-fifths that of crank presses, and this accounts for the reduced capital investment associated with high-energy-rate forging. Furthermore, the installation costs are also lower because of the less expensive foundation requirements. Among the HERF machines, the combustion-pneumatic machines have a lower capital cost than the pneumatic-hydraulic devices.

Despite these advantages, HERF machines have not been found to be competitive in comparison with conventional forming machines for most of the components produced by the industry. This is partly attributed to the long cycle time of such devices, when loaded manually, and the high die and tooling costs. The production cycle time is typically 100% longer than the specified nominal cycle time (based on the ram speed) because of operator movement. With manually fed conventional presses, the operator does not need to change position during the forming operation; with a HERF hammer, the intensity of the blow would cause him to withdraw. Although robotic feeding of the workpiece has been tried with some success, most general-purpose feeding devices are slow. The introduction of innovative feeding mechanisms may drastically change these conditions.

Because of these limitations, the HERF process is better suited to special, rather than general, forging operations. The cost benefits associated with its application would have to be evaluated on a case-by-case basis for each part.

Revised by Natraj C. Iyer, Westinghouse R&D Center

Machines

There are three basic types of HERF machines:

- Ram-and-inner-frame machines
- Two-ram machines
- Controlled-energy-flow (counterblow) machines

These are illustrated and discussed in the article "Hammers and Presses for Forging" in this Volume. In all three types, the energy is derived from high-pressure gas (usually nitrogen) that is stored within the machine and released to accelerate the platens. The machines are designed to minimize shock transmission to the floor. Therefore, a special foundation is not needed, and the machine can be placed directly on the factory floor. Machine capacity ratings range from 17,000 to 544,000 J (12,500 to 400,000 ft · lb). Figure 1 shows a typical schematic of a HERF machine.

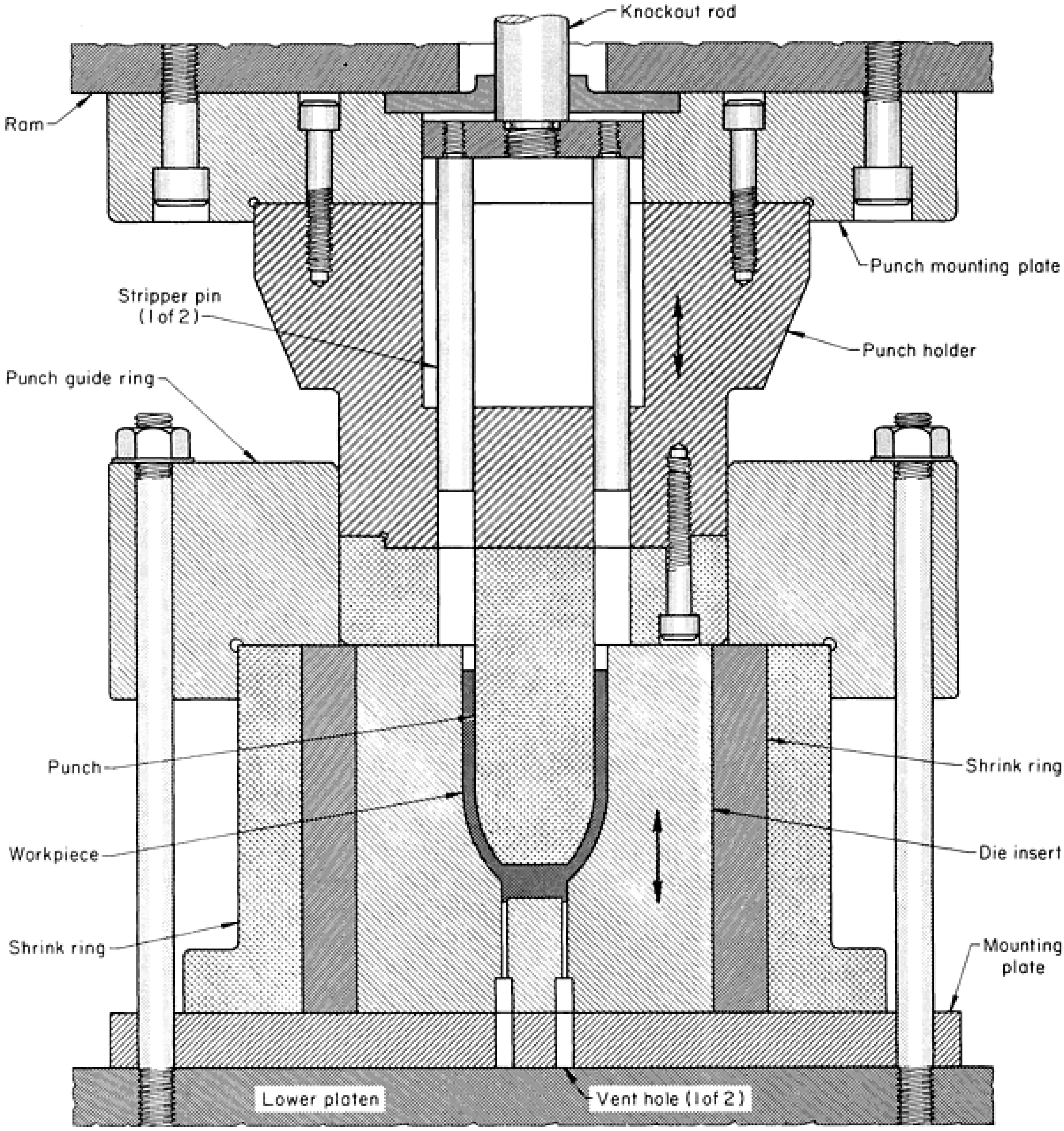

Fig. 1 Schematic of a HERF machine and details of the die used in making the mine nose shown as part D in Fig. 3.

Production Rate. The cycle time for a HERF machine is 12 to 20 s per piece, or a production rate of 180 to 300 parts per hour. Therefore, HERF machines can make parts to close profiles at production rates often comparable to those of drop-hammer and hydraulic presses. Adaptation of automatic transfer equipment to high-energy-rate forging would further increase the production rates to make it competitive with manually operated mechanical presses. The machines are readily adaptable to automatic loading and unloading equipment. However, high-production runs or multiple runs of similar parts are needed to justify the cost of automatic handling equipment.

Production Quantity. Table 2 compares HERF machines with hammer and press equipment on the basis of the production quantities for which each is typically used for forging a variety of parts. As this comparison shows, HERF machines are used for small and medium production quantities.

Table 2 Application of four basic types of machines for hot forging typical parts

L, large quantities: >10,000 parts; M, medium quantities: 500 to 10,000 parts; S, small quantities: <500 parts; N, not suitable for forging this type of part

Type of part	Hydraulic press	Mechanical press	Hammer	High-energy machine
Bulkhead	S	L	S	M
Cone	M	LM	LM	MS
Crankshaft	N	L	MS	N
Cup	LM	L	MS	MS
Disk	MS	LM	MS	M
Flange (weld)	M	L	MS	MS
Gear blank	L	L	S	M
Hemisphere	M	L	MS	MS
Shaft gear	L	L	S	M
Structural (rib-and-web)	MS	N	MS	N
Tube (long)	LMS	N	N	N
Tube (short)	LMS	LM	N	MS
Turbine blade	N	LM	MS	N
Wheel hub	N	L	S	M
Wheel spindle	M	L	S	N

Source: H. J. Henning, ASTME paper MF68-548

High-Energy-Rate Forging

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Dies

Closed dies and flash-and-gutter impression dies are used in high-energy-rate forging. The closed dies restrict the flow of metal and force it to fill the cavity completely; they are recommended because they require less material in the billet and permit closer dimensional control of the forging. Flash-and-gutter impression dies are generally used for thicker parts with more liberal dimensional tolerances.

As in conventional hammer forging, the force of the ram should be completely expended in deforming the workpiece. Impact between the upper and lower tools is potentially destructive. It can be effectively avoided by controlling those processing conditions that significantly influence metal plasticity (such as temperature, volume, and die temperature and lubrication) and in some cases by selective placement of a protective ring around the upper die.

Die Design. The design of tooling for high-velocity pneumatic-mechanical forging is quite different from the methods used for designing dies for use in mechanical presses. This is especially true when the part to be forged is shaftlike or cuplike, requiring relatively deep dies. In these cases, the die assemblies should comply more closely with the design rules used by impact extruders. These involve interference-fit assemblies, undercut punches, rugged ejectors, and vertical flash movement. An important element in these design concepts is the lack of any contact between the tool on the ram and the tool on the bolster during the forging stroke. Unlike hammer-die forging, the stroke of a pneumatic-mechanical press is controlled entirely by stalling the ram movement when plastically deforming the workpiece. No contact is permitted between the die faces. Prestressed die inserts, carefully guided punches, and provisions for vertical flash are typical. Flash movement may be horizontal in the forging of flat parts.

Die inserts are often fitted into the heavy retainer rings to counteract the large horizontal forging forces. These rings put a compressive stress, sometimes as great as 690 MPa (100 ksi), on the metal in the die insert and therefore reduce die breakage.

Alignment of the upper and lower dies is maintained partly by the accuracy of the machine guides and partly by piloting the upper die in the lower die. Clearance between the punch and the die piloting surfaces is 0.025 to 0.102 mm (0.001 to 0.004 in.). In some dies, another clearance is provided below the piloting surfaces to allow for vertical flash movement. For example, in Fig. 2, the upper dies, which were located in a simple holder for ease of replacement, were guided by an extension of the lower die cavity. An auxiliary ring was used in the die shown in Fig. 1. Although smaller corner and fillet radii can be forged by the HERF method as compared to conventional forging methods, it should be anticipated that such radii will restrict metal flow and increase die wear.

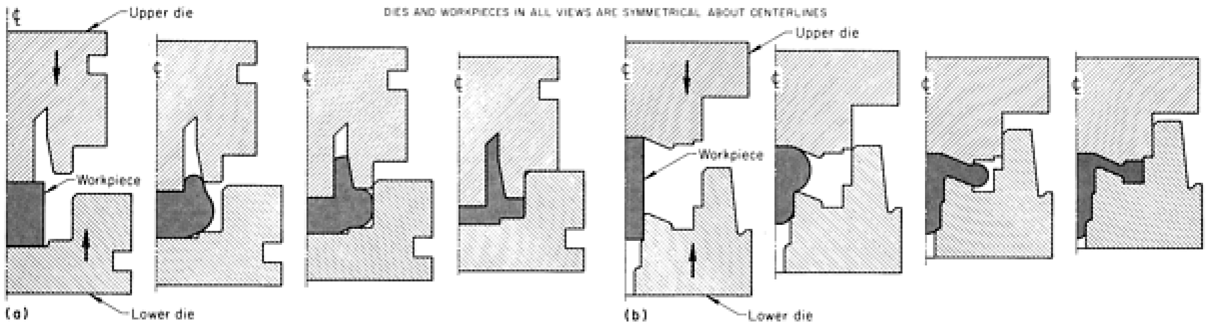


Fig. 2 Metal flow in high-energy-rate forging. (a) Upsetting and backward extruding a welding neck flange. (b) Upsetting and forward extruding a gear shaft.

As in conventional practice, the metal shrinkage allowance depends on the metal being forged. Draft is sometimes provided in the die cavity and on the upper die (or punch) for easy removal of the part. By using a positive punch stripper, it may be possible to eliminate the draft in the die cavity, allowing 1 to 3° draft on the punch. If depressions are formed in the bottom external surface of the part, knockouts are needed because of metal shrinkage around these areas.

Provision should be made in the lower die cavity for locating the hot billet. A shallow recess in the die is usually adequate. Die wear at locating points is greater than in other parts of the cavity, and an easy means of replacement should be designed into the die. The lower die cavity generally has a shorter die life than the punch or upper die, because of the longer time it is in contact with the billet.

Die fabrication practice for high-energy-rate forging is similar to that used for conventional forging dies. Dies have been made by casting and by electrical discharge machining the cavity in a rough-machined block. The use of electrical discharge machining in die fabrication is discussed in the article "Dies and Die Materials for Hot Forging" in this Volume.

Close-tolerance hubbing (die typing) is done in HERF machines. The hub, made of H11 or H13 steel and hardened to 48 to 52 HRC, is secured to a retainer fastened to the upper platen of the machine. The die blank, with a rough-machined cavity and heated to forging temperature, is placed into a retainer mounted on the lower platen. The hub is then driven at a high velocity into the blank to form the die cavity.

Die Materials. Prehardened tool steels such as 6F2 and 6F3 are used when forging stresses are low and production quantities are small. For larger quantities and severe forging requirements, tool steels such as H11 or H13 are used for the critical die components.

In high-production dies, 6F2 and 6F3 steels and alloy steels such as 4140 and 4340 are used for backup blocks, shrink rings, and retainers. A modified H13 tool steel containing 1.5% Ni is recommended for die inserts and punches for long production runs, close-tolerance forging, and hot-work applications in which exceptional toughness and resistance to abrasion and heat cracking are important.

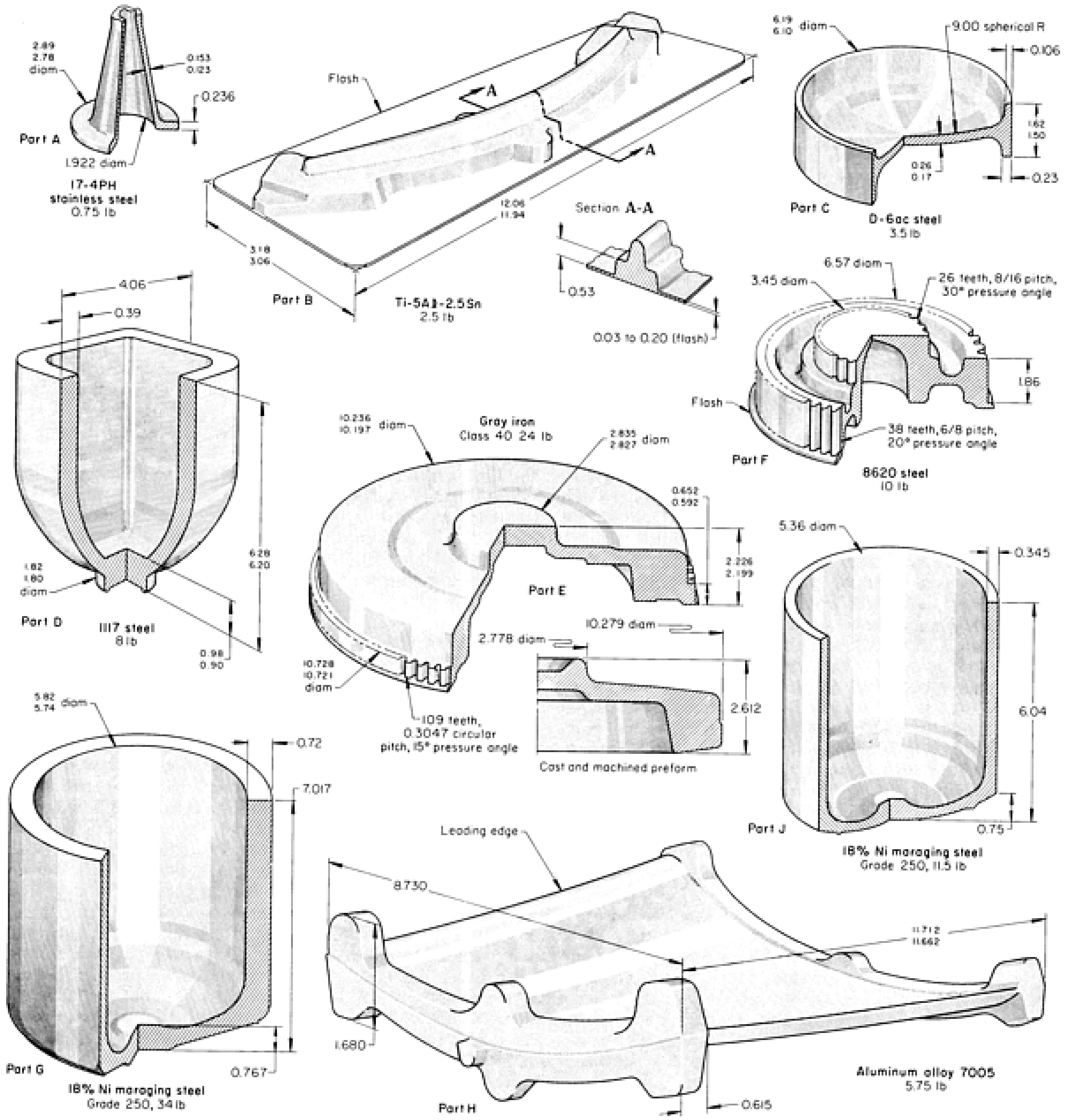
Hubbed die inserts are commonly made of H11 tool steel, but 4140 steel is sometimes used when production requirements are small or the operation is not severe. Almost any tool steel that will withstand hot working can be used for hubbed die blocks or inserts, although dies made of prehardened tool steel would soften during the hot-hubbing operation.

Die Life. The usual modes of failure of hot-forging dies, at both conventional and high forming speeds, are wear, heat checking, and fatigue. Early users of pneumatic-hydraulic HERF machines found die life to be considerably shorter than that encountered with conventional machines. This was subsequently attributed in part to the fact that the HERF machines tended to be used for the production of more complex and difficult components, which in many cases would not even have been attempted with conventional machinery.

Production tests carried out by the Drop Forging Research Association indicated that the service lives of dies used in a pneumatic-hydraulic HERF machine compare favorably with those of identical dies used in a conventional drop-stamping press. The forging of three different components using both methods established that neither method had the edge over the other in terms of die life. In general, no significant difference in the rate of change of salient component dimensions was noted in changing from the slow-speed process to the high-speed process. On both types of machines, the die life was limited by surface cracks. An exception to this has been observed in cases in which a corner adjacent to a flash gap of one die showed up to two to three times as much wear on the HERF machine as compared to the conventional machine.

Example 1: Die Specifications Needed to Produce Mine Nose by HERF.

The die shown in Fig. 1 was used to forge the mine nose shown in part D in Fig. 3. The die insert was made of H13 tool steel. The calculated prestress on this ring was 690 MPa (100 ksi). Small holes in the bottom of the die cavity vented the air during forging. The punch was made of H13 tool steel and hardened to 45 to 47 HRC. The punch holder and punch mounting plate were made of 4340 steel and hardened to 38 to 40 HRC. The punch guide ring on the die provided precise alignment of the punch as it entered the die. This die allowed production of concentric parts with uniform wall thickness. There was no draft on the side-walls of the die cavity or the punch. Because metal shrinkage caused the part to adhere to the punch, a stationary stripper ejected the forging from the punch on the upstroke of the ram.



Part	Name of part	Work metal	Blank size, mm (in.)		Weight		Forging temperature		Forging energy	
			Diameter	Length	kg	lb	°C	°F	J	ft · lbf[(a)]
A	Forward support	17-4 pH stainless steel	70 (2$\frac{3}{4}$) OD 41 (1$\frac{5}{8}$) ID	15 (0.6)	0.34	0.75	1095	2000	39,300	**29,000[(b)]**
B	Vane platform	Titanium alloy Ti-5Al-2.5Sn	35 (1$\frac{3}{8}$)	267 (10.5)	1.1	2.5	1095	2000	190,000	**140,000**

C	Bulkhead	D-6ac steel	102 (4)	25 (1.0)	1.6	3.5	1175	2150	217,000	**160,000**
D	Mine nose	1117 steel	99.6 (3.92)	61 (2.4)	4	8	1205	2200	214,000	**158,000**
E	Automotive flywheel	Class 40 gray iron	Cast and machined preform		11	24	955	1750	271,000	**200,000**
F	Cluster gear	8620 steel	76 (3)	124 (4.9)	4.5	10	1230	2250	353,000	**260,000**
G	Motor bulkhead	18% Ni maraging steel, grade 250	165 (6.5)	86 (3.4)	15	34	1230	2250	495,000	**365,000**[(c)]
H	Fan-stator outer vane	Aluminum alloy 7005	Dogbone-shape preform[(d)]		2.61	5.75	425	800	245,000	**181,000**
J	**Forward motor case**	**18% Ni maraging steel, grade 250**	**114 (4.5)**	**64 (2.5)**	**5.22**	**11.5**	**1230**	**2250**	**502,000**	**370,000**

(a) Parts were made in a single forging blow unless otherwise specified.

(b) Two separate blows with intermediate heating.

(c) Two successive blows at 495,000 J (365,000 ft · lbf) each.

(d) Extruded and machined

Fig. 3 Nine parts produced by high-energy-rate forging under conditions listed in table. See text for detailed discussion. Dimensions in figure given in inches.

High-Energy-Rate Forging

Revised by Natraj C. Iyer, Westinghouse R&D Center

Lubrication

Lubricants act as parting agents between the workpiece and the die. They also provide an insulating film on the die surface that reduces heat transfer between the hot billet and the die, thus lessening thermal cracking and increasing die life. Serviceable lubricants include high-temperature greases containing copper, molybdenum disulfide, or graphite and aqueous suspensions containing graphite. The spraying of water-base coolants containing soluble oil onto the surfaces of dies and punches prevents softening and extends die life.

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Preparation of Blanks

Blanks for symmetrical shapes are cut from bar stock that has been rough turned. The blank diameter is such that a hot blank will drop unimpeded into the die cavity and provide some degree of location. When a particular grain flow is specified, billets are pancaked or are progressively preformed by the multiple blows or by special preforming operations. The billet used in high-energy-rate forging is usually smaller than that used in conventional hammer forging methods for three reasons: Metal is not needed for a tonghold, the finished forging has little or no flash, and the machining allowance is small.

The volume of the blank is closely controlled so that the shape can be forged to relatively close tolerances. Close volume control helps reduce the cost of material; this is of particular importance in forging the more expensive metals, such as titanium alloys and maraging steel.

The tolerance on blank diameter and length varies with shop practice. One high-energy-rate forging shop purchases rough-turned bars manufactured to a diameter tolerance of +0.76 mm, -0.00 mm (+0.030 in., -0.000 in.), then saws blanks to length with the same tolerance. For some parts, tolerance is also specified on blank weight. For example, the forward motor case shown as part J in Fig. 3 was forged from a billet 114.3 + 0.8 mm, -0.0 mm (4.50 + 0.03 in., -0.00 in.) in diameter by 63.0 + 0.8 mm, -0.0 mm (2.48 + 0.03 in., -0.00 in.) in length with a specified weight of 5.22 kg ± 0.0285 kg (11 lb 8 oz ± 1 oz).

As-forged symmetrical parts often have little or no flash. However, when flash allowance is provided, it generally is less than 3% of the blank weight. Some of the flash is derived from the normal variation in blank weight. In sharp contrast, asymmetrical parts can develop considerable flash. For example, the fan stator vane shown as part H in Fig. 3 had a flash amounting to 10 to 15% of the blank weight.

High-Energy-Rate Forging

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HERF Processing

Like conventional forging, high-energy-rate forging can be accomplished over a broad range of temperatures, depending on the specific part, the material, and the design requirements.

Hot Forging. High forging speeds in hot forming result in reduced cooling of the material as it flows over the cooler die surfaces and in improved lubrication. The high strain rate increases the forming load, but this is partially offset by adiabatic heating of the workpiece, thus making it easier for the material to fill out the die cavity. However, in some materials, such as high nickel-base alloys, it can lead to incipient melting, which can in turn cause serious rupturing when large reductions are being forged.

Under the most favorable conditions, hot high-energy-rate forging allows the production of components that cannot be forged with conventional machines. Complex parts and components with thin sections have been produced; webs and ribs 3.18 mm (0.125 in.) thick and central ribs 4.76 mm (0.1875 in.) thick are technically feasible. However with pneumatic-hydraulic HERF machines, such results can often be obtained only at the expense of die life in view of their long dwell times.

The suitability of the hot-forming process depends on the component shape and the material formed. Although cylindrical, axisymmetric shapes are preferred, more complex shapes such as light alloy compressor blades have been successfully manufactured. Complex forging usually requires some preforming to ensure proper metal flow and to avoid such defects as laps, cracks, and unfavorable grain flow. Table 3 presents the suitability of a broad range of alloys for high-energy-rate forging. Most of the current applications are related to the first three alloys; namely, aluminum alloys, carbon and alloy steels, and stainless steels.

Table 3 Forgeability of various work metals by high-energy rate forging

Work metal	Forgeability
Aluminum alloys	**Excellent for alloys such as 2014, 2024, and others hardened basically with copper. For 7075 and 7079, forging temperatures and reductions must be adjusted downward to prevent rupturing. No improvements in shape detail obtained**
Carbon and low-alloy steels	**Excellent; greater shape detail possible than in hammers or hydraulic presses**
Stainless steels	**Excellent; greater shape detail possible than in hammers or hydraulic presses**
Iron-base heat-resistant alloys	**Good if forging temperatures are adjusted downward to prevent overheating. Greater shape detail possible than by conventional methods**
Nickel-base heat-resistant alloys	**Good if reduction per blow is limited to less than about 60% (depending on alloy).**
Magnesium alloys	**Poor for such alloys as AZ31B and AZ80A. Alloys HM21A and HM31A are as forgeable as in presses if temperatures are adjusted downward.**
Titanium alloys	**Good; care must be exercised to avoid β grain coarsening that can result from significant temperature increases.**

Cold Forging. High speeds in cold forging improve lubrication. Improved lubrication lowers the frictional forces, and this in turn results in improved metal flow and surface finish of the components. The process is practically adiabatic; therefore, with soft materials, such as aluminum and copper, some softening occurs. Forging forces and pressures are generally higher than those obtained at conventional speeds, although energy requirements may well be lower.

The research experience of high-speed cold forging is extensive, but production experience is very limited. There are two reasons for this. First, cold-formed components are generally small, and the available pneumatic-hydraulic HERF machines are too large in capacity and too slow to compete with the more automated high-production mechanical presses. Second, cold forming is carried out with presses or heading machines that have fixed stroke. With forward-extrusion hammers, adequate component length tolerance can be obtained only by the addition of clash faces, which generate unacceptably high levels of impact noise.

Cold forging, however, has been successfully used for the fabrication of blanks. Blanks for bevel gears, for example, have been cold forged from extruded preforms.

Warm Forging. In comparison to hot high-energy-rate forging, the principal advantage of warm forging is the absence of scale and therefore better surface quality, high precision, and improved tool life. In relation to cold high-energy-rate forging, the process leads to significant lowering of tool loads, permitting a wider range of both component sizes and available materials that can be formed without appreciable deterioration in component material properties.

For steels, the warm-forging temperatures range from 450 to 950 °C (840 to 1740 °F). Temperatures are most often between 600 and 815 °C (1100 and 1500 °F). The dies used resemble typical HERF dies.

Experience with high-speed warm forging is very limited. An example of a large warm-formed part is a transmission shaft, formed at 700 °C (1290 °F) with a single blow from a preform forged at conventional forging temperature on a horizontal upsetter.

Metals Forged. Current production by high-energy-rate forging involves a wide range of materials, such as aluminum, high-strength steel, stainless steel, alloy steels, and titanium. Low-carbon steel parts are being produced when the complexity of the part is such that the use of high-velocity impact equipment is required. Limited production work is being done with nickel-base heat-resistant alloys. The metals best suited to the process are those that withstand very high deformation rates without rupturing.

High-energy-rate forging is particularly suited to alloys that require high forging temperatures and pressures, especially when thin webs or unusual design features are required. Low-carbon steels, refractory metals, and nickel alloys that have broad forging temperature ranges can be forged. Metals with low ductility under rapid deformation rates, such as magnesium and beryllium alloys, cannot be forged by high-velocity methods.

Heating that is caused by rapid deformation, if excessive, may result in incipient melting and serious rupturing when forging to large reductions. The metals affected in this manner include high-carbon steels, high-strength aluminum alloys, and nickel-base heat-resistant alloys. Increased temperatures during forging can cause β embrittlement in some titanium alloys. Table 3 shows the forgeability of various work metals by HERF methods.

The range of metals and shapes forged by the HERF process is illustrated by the parts shown in Fig. 3. The aluminum alloy 7005 vane, shown as part H in Fig. 3, was forged in one die from an extruded and machined preform. The part was forged at a temperature of 425 °C (800 °F) to a closure tolerance of ±0.13 mm (±0.005 in.) and a contour tolerance within ±0.25 mm (±0.010 in.). The vane platform shown in part B in Fig. 3 was made of titanium alloy bar stock measuring 35 mm ($1\frac{3}{8}$ in.) in diameter by 267 mm ($10\frac{1}{2}$ in.) in length. Forging temperature was 1095 °C (2000 °F), and the forging energy was 190,000 J (140,000 ft · lbf). The bar stock was preformed into an arc having a radius about the same as that of the vane.

An example of a low-carbon steel part produced by high-energy-rate forging is an automobile front wheel hub that was forged from a 3.2 kg (7 lb) billet of 1020 steel. The part had a 102 mm (4 in.) diam flange and a 48 mm (1.9 in.) long hub. The outside surfaces were used as-forged; but the inside surfaces were machined, and bolt holes were drilled in the flange.

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Metal Flow

In high-energy-rate forging, the metal can flow through small openings to fill large cavities and accurately produce intricate details. An experimental part having a thin web was forged using both a high-energy-rate machine and a drop hammer. The high-energy-rate machine forged a web thickness of 0.96 mm (0.038 in.) in one blow. By hammer forging, the minimum thickness obtained was 1.24 mm (0.049 in.) using three blows with a 910 kg (2000 lb) hammer and 1.52 mm (0.060 in.) when four blows with a 450 kg (1000 lb) hammer were used. The high-energy-rate machine used the lowest amount of energy available.

Metal spreads more in a high-speed blow. In one test on forging from a cylindrical billet, a measurement was made of the largest flange diameter that could be obtained before cracks appeared on its periphery. Larger diameter flanges could be produced in a HERF machine than by conventional forging. However, greater surface roughness was observed with the HERF method than with conventional forging.

Metal Flow During HERF Processing. When the clutch hub illustrated in Fig. 4 was forged from a 1038 steel billet 51 mm (2 in.) in diameter by 83 mm ($3\frac{1}{4}$ in.) in length, one high-velocity blow was sufficient to cause the metal to flow through the thin center web and into the thicker web and flange without cracking. Conventional hammer forging of the part would have used a blocking operation involving eight or nine blows and would have had a metal loss of about 0.34 kg ($\frac{3}{4}$ lb) from scale and flash.

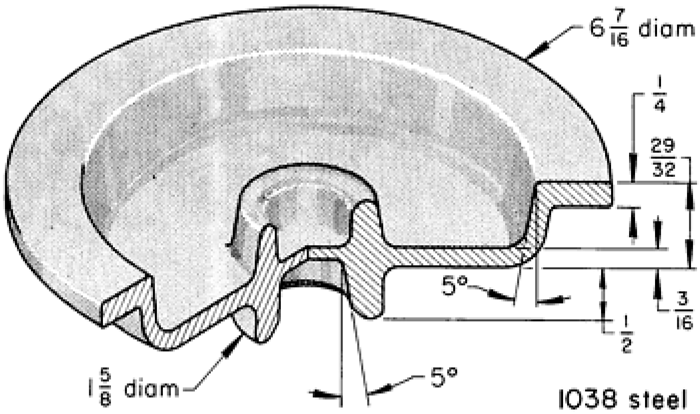

Fig. 4 Clutch hub with thin web and thick flange that was made in one blow by high-energy-rate forging. Dimensions given in inches.

The major diameter of a cluster gear (part F, Fig. 3) was 2.2 times the diameter of the blank. This gear was finish forged from the blank in one high-velocity blow consisting of 353,000 J (260,000 ft · lbf) of energy. The work metal was 8620 steel, and it was heated to a forging temperature of 1230 °C (2250 °F). In the hammer or press forging of this gear, the blank would have been flattened with one or more forging blows before being placed into the finisher impression. In addition, teeth are generally not formed on gear blanks during conventional forging.

The flow of metal by upsetting and backward extrusion during the high-energy-rate forging of a welding flange is shown in Fig. 2(a). Progressive metal flow during high-energy-rate upsetting and forward extrusion of a gear shaft is shown in Fig. 2(b). In the forging of both of these parts, the upper die was guided in the lower die, and die closure was controlled by the amount of metal in the workpiece and by a ring surrounding the upper die cavity that contacted the lower die. In each of these dies, the billet was located by a special recess in the lower part of the die cavity.

Dimensional Accuracy. Tolerances as small as ±0.05 mm (0.002 in.) have been held on parts produced by high-energy-rate forging. However, holding such close tolerances results in excessively greater die wear than when parts have more generous tolerances. This is true regardless of the type of forging process or equipment used. If the metal saved is expensive or if the machining that is eliminated or reduced involves costly three-dimensional profiling, forging to close dimensional tolerances may be justified. Forging with liberal tolerances may be more prudent if the metal is inexpensive and if machining operations are relatively simple.

Surface Finish. Surface finish on a forging, using any of the above processes, is only as good as the finish on the die or die cavity. Typically, any marks on the die will be reproduced on the surface of the forging. In general, the surface finish specified depends on the work metal, the amount of subsequent machining, and the end use of the forged product.

High-Energy-Rate Forging

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Applications

Nine typical parts that have been produced by high-energy-rate forging are shown in Fig. 3; the forging conditions are listed in the accompanying table. The weights of these parts range from 0.34 to 15 kg ($\frac{3}{4}$ to 34 lb). The 0.34 kg ($\frac{3}{4}$ lb) part A was forged in two separate blows with an intermediate reheat. It had a tolerance of ±0.38 mm (±0.015 in.) on the wall thickness and +2.8 mm, -0.0 mm (+0.11 in., -0.00 in.) on the flange diameter. The 15 kg (34 lb) part G was forged from the billet by two successive machine blows without being removed from the die.

The cup-shaped parts G and J in Fig. 3 were forged and backward extruded from billets of 18% Ni maraging steel, grade 250. Part G had a total tolerance on the inside diameter of 2.0 mm (0.08 in.); the draft angle on the punch was about $\frac{1}{2}$° per side. Permissible draft on the outside diameter was about 0.008 mm per mm (0.008 in. per in.). Part G had a wall thickness of 18.3 mm (0.72 in.) for a depth of about 171 mm (6.75 in.) and required two successive blows to finish forge. Part J had a total tolerance on the outside diameter of 3.8 mm (0.15 in.); the inside and the outside diameters were to be concentric within 1.02 mm (0.040 in.). The punch for part J had a draft angle of 1 to 3° per side. Part J had a wall thickness of 8.75 mm (0.345 in.) for a depth of about 135 mm (5.3 in.) and was forged in one machine blow. Both these parts had a 25 μm (1000 μin.) surface finish.

The double-flange bulkhead shown as part C in Fig. 3 was made of a D-6ac alloy steel billet that was heated to 1175 °C (2150 °F). Because of the spherical web, the draft allowance on the cavity for the outside diameter was 4° maximum per side. Total tolerance on the inside diameter was 2.3 mm (0.09 in.) on the convex side of the web and 1.02 mm (0.040 in.) on the opposite side. Tolerances on all three parts were unilateral, but were applied in order to allow wear on the punch and die cavity dimensions.

Another bulkhead with a flange extending from both sides of the web (an I-beam in cross section) was forged from D-6ac steel in one blow using 163,000 J (120,000 ft · lbf) of energy. The billet was 63.5 mm (2.50 in.) in diameter by 64.0 mm (2.52 in.) in length and was forged at 1150 °C (2100 °F). The web thickness was 0.267 mm ± 0.127 mm (0.0105 ± 0.005 in.). Maximum production rate was 120 pieces per hour for more than 20,000 bulkheads.

Forging a bulkhead by the high-energy-rate method can result in savings of material. For example, a bulkhead weighing 14 kg (30 lb) was forged from 18% Ni maraging steel, grade 250. When conventionally forged, the bulkhead weighed 41 kg (90 lb). The 14 kg (30 lb) part was made in a counterblow HERF machine by successive blows in the same die cavity without reheating the workpiece. The energy level was about 237,000 J (175,000 ft · lbf) per blow. The counterblow (controlled-energy-flow) machine is better suited for repetitive blows than the two other machines.

The 103 mm (4.06 in.) square mine nose shown as part D in Fig. 3 was made from a 1117 steel billet 99.6 mm (3.92 in.) in diameter by 61 mm (2.4 in.) in length that weighed about 4 kg (8 lb). The billet was heated to 1205 °C (2200 °F) and then forged with 214,000 J (158,000 ft · lbf) of energy. The draft angle on both the internal and external walls was 0°. The tolerance on the outside dimensions was +0.00 mm, -1.02 mm (+0.000 in., -0.040 in.); on the inside dimensions, +1.52 mm, -0.00 mm (+0.060 in., -0.000 in.). The zero draft angle and the close tolerances contributed to a substantial savings in machining compared to that required for a conventionally forged part.

The 12-point socket wrench shown in Fig. 5 is a precision forging that did not need machining of the internal spline section. This wrench was forged in one blow with 88,100 J (65,000 ft · lbf) of energy. The 4150 steel billet was 70 mm ($2\frac{3}{4}$ in.) in diameter by 83 mm ($3\frac{1}{4}$ in.) in length. The forging temperature was 1165 °C (2130 °F).

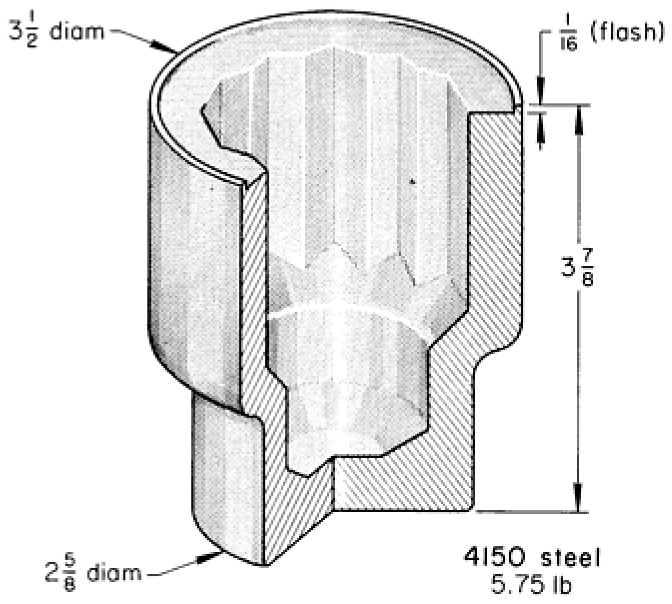

Fig. 5 Socket forged to final shape in one blow so that no machining was required for producing the 12-point spline. Dimensions given in inches.

Forging of Gears. It is possible to produce gears with a contoured grain flow that follows the configuration of the teeth using high-energy-rate forging. In the case of spur gears, this is achieved by pancaking to cause lateral flow of the metal in a die containing the desired tooth configuration at its periphery. Contoured grain increases the load-bearing capacity without increasing the tooth size. In addition, the process minimizes the machining required to produce the finished gear. Although spur gears are the easiest to forge, helical and spiral-bevel gears can also be forged if their configurations permit ejection of the gear from the die cavity. Gears have been forged from low-alloy steel, brass, aluminum alloys, stainless steel, titanium, and some of the heat-resistant alloys.

Gears with a diametral pitch of 5 to 20 are commonly forged with little or no machining allowance. The die life decreases significantly when forging finer-pitch gears.

The forging of 5-diametral-pitch gears with an involute tolerance of 0.013 mm (0.0005 in.) and total composite error of 0.08 mm (0.003 in.) has been reported. These gears were forged with a tooth-to-tooth spacing deviation of about 0.025 mm (0.001 in.) and a total accumulated deviation of 0.089 mm (0.0035 in.). Over-the-pins dimensions were held to ±0.05 mm (0.002 in.) on these gears, and the total composite error was about 0.20 mm (0.008 in.).

Holding gear dimensions to extremely close tolerances may eliminate finish machining, but the savings may be exceeded by higher die making/maintenance costs. Consequently, most forged gears have an allowance for machining.

A surface finish of 0.5 to 1.5 μm (20 to 60 μin.) on gear teeth is practical. However, even with a 0.5 μm (20 μin.) finish, local imperfections can increase the average to 1.5 μm (60 μin.) or greater. Therefore, it would be difficult to maintain a good surface finish on gear teeth without grinding.

Typical Gear Forgings. The 4.5 kg (10 lb) gear shown as part F in Fig. 3 was forged from 8620 steel billet 75 mm (3 in.) in diameter by 124 mm (4.9 in.) in length. An energy level of 353,000 J (260,000 ft · lbf) was needed to forge the gear in one blow at 1230 °C (2250 °F). The web on the gear was forged to final thickness; the teeth were forged with 0.51 mm (0.020 in.) of stock for finish machining.

The die inserts originally used to forge this gear were made of H11 or H13 tool steel. This steel typically softened after producing 20 gears because of its temperature rising above the 565 °C (1050 °F) tempering temperature of H13 steel. The use of Alloy 718 (UNS N07718) was found to improve the die insert life.

The automotive flywheel shown as part E in Fig. 3, 272.49 mm (10.728 in.) in diameter over the teeth and weighing 11 kg (24 lb), was forged from a machined blank cast from class 40 gray iron (generally considered unforgeable). The machined preform, a section of which is shown in Fig. 3, was heated to 955 °C (1750 °F) and forged at an energy level of 271,000 J (200,000 ft · lbf). This part had the smallest tolerance specification. The diameter over the teeth and the thickness of the body had a tolerance of +0.00 mm, -0.18 mm (+0.000 in., -0.007 in.). The largest tolerance on the part was ±1.02 mm (±0.040 in.) on the diameter of a recess. Tolerances on the other recesses were ±0.18 mm (±0.007 in.) and +0.48 mm, -0.00 mm (+0.019 in., -0.000 in.). This gear was forged to the finished dimensions.

Various gears with teeth as an integral part have been forged. These have ranged in outside diameter from 64 to 267 mm (2.5 to 10.5 in.) and in weight from 0.54 to 11 kg (1.2 to 24 lb). Most have been made with 0.13 to 0.51 mm (0.005 to 0.020 in.) of stock on the flank of each tooth for finish hobbing and grinding. Gears forged with integral teeth normally have longer fatigue and wear life than those made from a conventionally forged blank on which the teeth are hobbed, shaped, or milled.

Developmental Work. High-energy-rate forging is continually being extended to new applications. The rock drill bit shown in Fig. 6 is an example. Originally, the part was forged in a drop hammer as a conical forging, and the teeth were machined into the forging; hammer forging the part with integral teeth was considered impossible. The feasibility of forging the same part into near-net shape form has since been established using a HERF machine. The gray iron flywheel shown as part E in Fig. 3 was an experimental part used to determine the feasibility of forging teeth on a cast iron preform. Satisfactory parts have been made using the operating conditions listed.

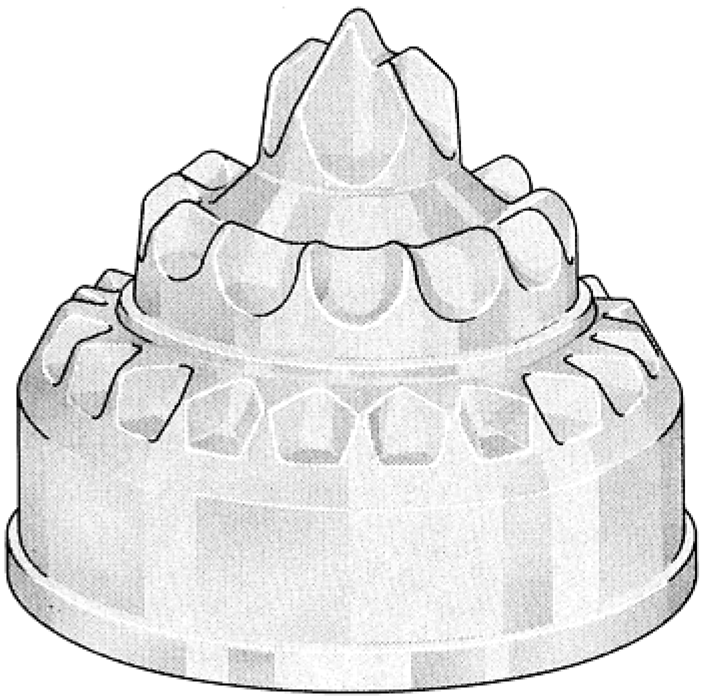

Fig. 6 Rock drill bit that was forged with three rows of teeth as shown.

High-Energy-Rate Forging

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Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Introduction

RING ROLLING is a versatile metal-forming process for manufacturing seamless annular forgings that are accurately dimensioned and have circumferential grain flow. Ring rolling usually requires less input material than alternative forging methods, and it is applicable to production in any quantity.

In ring rolling, a heated doughnut-shaped blank, preformed on a press or forging hammer, is placed over a mandrel of slightly smaller diameter than the hole in the blank. The roll gap between the mandrel (undriven) and a larger-diameter driven main roll is progressively reduced. Friction between the main roll and the ring causes the ring to rotate, and the ring in turn rotates the bearing-mounted mandrel. As the radial cross section of the ring decreases, circumferential extrusion occurs in the direction of ring rotation, and the ring diameter grows. The work rolls may be plain, producing uniformly rectangular ring cross sections, or may have grooves or flanges to produce contoured ring cross sections. Ring height is controlled either by main roll shape or by the use of axial rolls set diametrically across the ring from the mandrel and main roll pass.

Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Product and Application

Annular components can be ring rolled from any forgeable material. The configuration can range from very flat washer-shaped rings to long sleeve-type rings (Fig. 1). Typical materials include carbon and low-alloy steels, copper, brass, aluminum and titanium alloys, and high-strength nickel- and cobalt-base alloys, which are very difficult to form. Applications for seamless rolled rings include antifriction bearing races, gear rims, slewing rings, railroad wheel bearings, commutator rings, rotating and nonrotating rings for jet engines and other aerospace applications, nuclear reactor components, bevel ring gears, and flanges of all kinds (including weld-neck flanges), sheaves, wheels, valve bodies, food-processing dies, and chain master links.

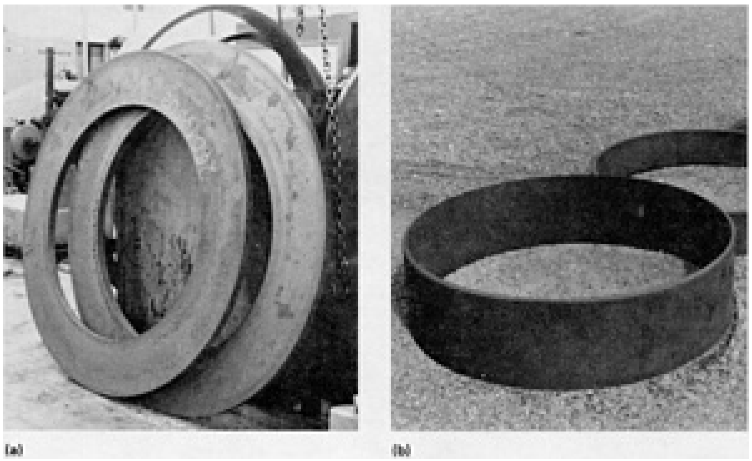


Fig. 1 Two possible rolled ring shapes. (a) Washerlike ring. (b) Sleeve-type ring. Rings with contoured cross sections can also be produced (see Fig. 2).

Sizes. About 90% of all rolled rings have outside diameters in the range from 240 to 980 mm (9.5 to 38.6 in.), heights (lengths) ranging from 70 to 210 mm (2.75 to 8.25 in.), and wall thicknesses between 16 and 48 mm (0.63 and 1.9 in.). A significant number of rings, however, are rolled outside the above parameters, and it is not unusual to find outside diameters ranging from 75 mm to 8 m (3 in. to 26.25 ft), heights from 15 mm to 2 m (0.6 in. to 6.5 ft), and weights from 0.4 to 82,000 kg (0.9 to 181,000 lb).

Shapes (Contours). Figure 2 shows a range of typical contoured/shaped cross sections that can be produced by ring rolling. In some cases, it is more economical and more practical to roll contoured rings as 2 in 1 multiples that are then slit. The two identical components are usually mirrored so as to place the thinnest wall section at the midheight of the rolled ring for ease of parting. Because the ring is then symmetrical about the line of midheight, such a ring can often be rolled from a simple blank, and it behaves more predictably during rolling than an asymmetrical ring would if rolled singly.

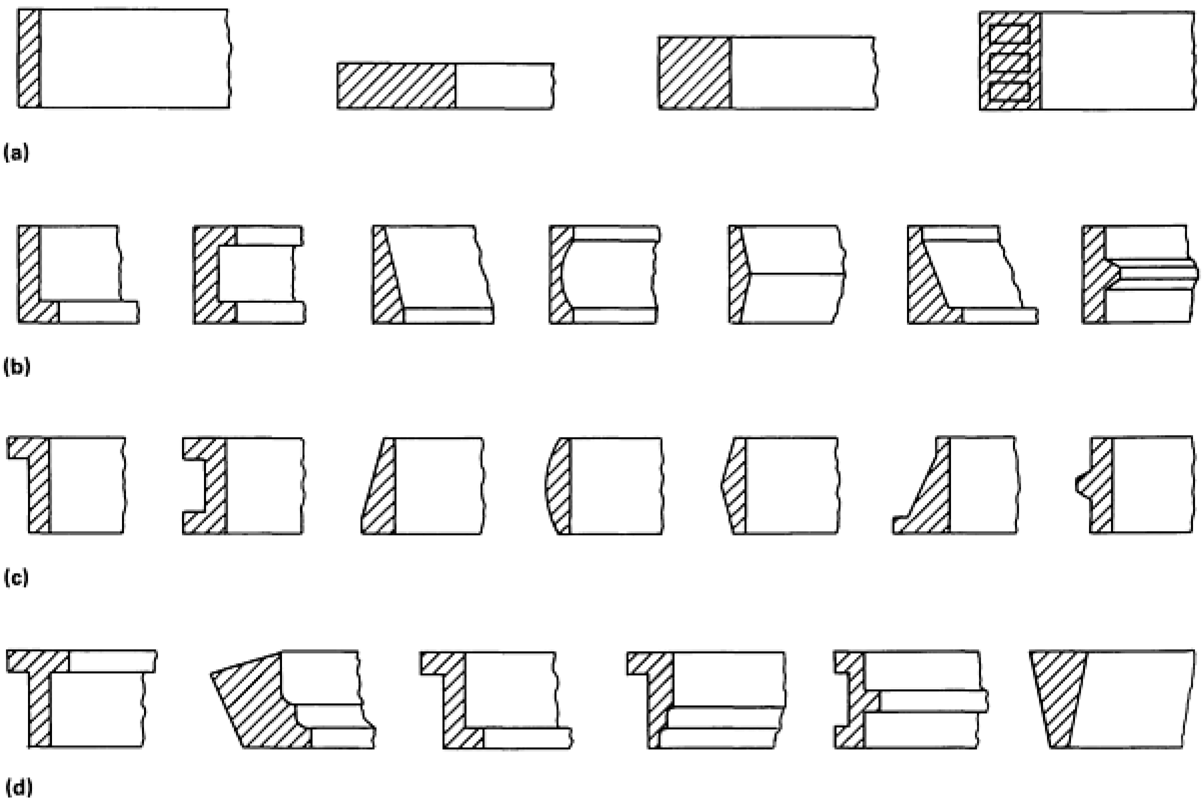


Fig. 2 Typical rolled ring cross sections. (a) Rectangular. (b) Rings with inside contours. (c) Rings with outside contours. (d) Rings with both inside and outside contours

Figure 3 shows the stages in producing a ring contoured on the inside diameter on a vertical mill, using a two-stage rolling technique. With improved ring mill design and construction and the introduction of computer control, the old rules concerning extremes of practically rollable ring-wall-to-ring-height ratios (1 to 4 for sleeves and 4 to 1 for flat, washer-like rings) are no longer applicable.

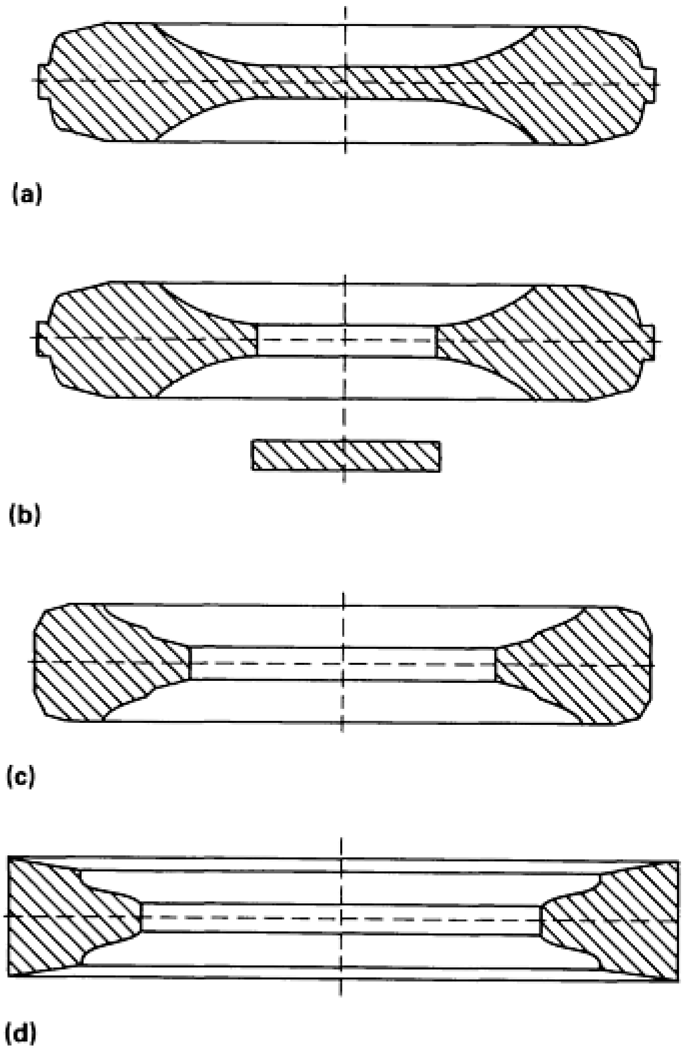


Fig. 3 Stages in the production of a contoured ring on a vertical mill. (a) Ring blank. (b) Pierced blank. (c) After first rolling operation. (d) After final rolling

Washers with a wall-to-height ratio of 16 to 1 and sleeves with wall-to-height ratios of 1 to 16 have been rolled on the same radial-axial ring mill. Using specially prepared tubular-shaped blanks, sleeves with wall-to-height ratios greater than 1 to 28 have been rolled in various materials.

Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Machines

Historical Background. In the mid to late 19th century, the rapid expansion of rail-road systems created an increasing demand for railroad wheel-tires. Originally, these items were forged, laboriously, using hammers. As early as 1852, however, a tire rolling machine was built in England. The resulting increased productivity, improved product performance, and ability to put more shape into the tires before machining ensured the ring rolling technique a firm foothold in the forging industry.

Early machines were radial-pass units only (Fig. 4); that is, they used a single roll-pair and controlled height by containing the ring in a shaped tool. These machines were of two basic types, based on the plane of the ring during rolling:

- Horizontal, in which the ring rotates about its vertical axis
- Vertical, in which the ring rotates about its horizontal axis

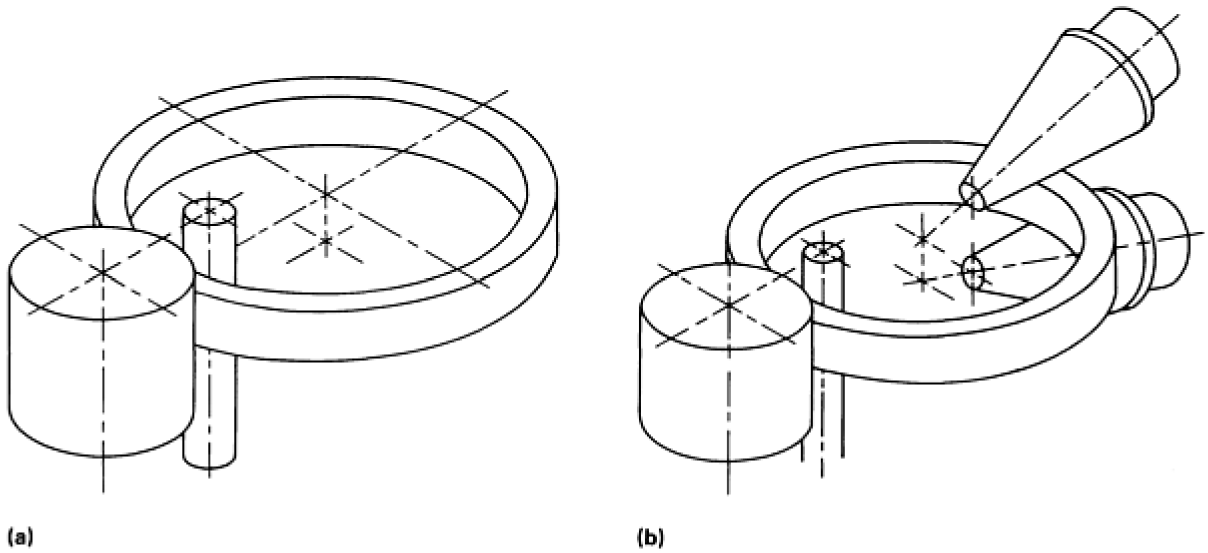


Fig. 4 Schematics showing single-pass (radial) rolling (a) and two-pass (radial-axial) rolling (b)

The vertical machine is limited in its diameter range by practical considerations of floor to working height. The upper diameter limit of the horizontal machine is constrained only by the available floor space. With the horizontal machine some means of supporting the bottom face of the ring must be provided. Either the main roll (older machines) or mandrel of the vertical machine serves as the means of ring support.

As the use of the technique encompassed a greater variety of rings and ring end uses, the fundamental shortcoming of single-pass rolling (end face defects) forced consideration of two-pass rolling (Fig. 4b). By the early 1900s, water-hydraulic horizontal machines with directly operated valves were being constructed with this second pass diametrically opposite the original radial pass for the purpose of (limited) axial height reduction. These machines were termed radial-axial mills. The first oil-hydraulic servo-valve-controlled radial-axial mills appeared in the early 1960s.

From 1930 to 1980, rapidly increasing use of antifriction bearings gave rise to a demand for a particular type of seamless rolled ring. Inner and outer bearing races are manufactured in a wide variety of sizes, those using rolled rings predominantly ranging from 75 to 1000 mm (3 to 40 in.) in outside diameter, 40 to 250 mm (1.6 to 10 in.) in height, and up to 140 kg (310 lb) in weight. High-output multiple-mandrel table mills were specifically designed to meet the lighter end of this need.

Variations on these four basic mill types continue to emerge, with ever-improving machine designs and control systems. Rapid advances in electronics have enabled the application of microprocessor and computer technology to ring rolling equipment.

A variety of special-purpose machines have also been built at various times in the past 70 years. Both vertical and horizontal railroad wheel rolling mills have existed since the turn of the century, although today they are few in number because the wrought wheel has largely given way to the pressure cast wheel.

Of particular note is the recently available closed-die axial rolling (rotary or orbital forging) machine. In this machine, a punched blank or prerolled ring produced on conventional ring rolling equipment is worked between inclining-rotating dies. Annular forgings of very accurate dimensions, and in a range of complex cross sections, can be produced, using only a fraction of the force required by competing forging processes.

Past attempts to employ the principle of rotary (orbital) forging have met with some success. Modern machine design and construction methods have made it possible to build more durable machines. More information on these machines and the rotary forging process is available in the section "Closed-Die Axial Rolling" in this article and in the article "Rotary Forging" in this Volume.

Vertical rolling machines (Fig. 5) offer more rolling force and drive power for a given capital outlay than their horizontal single-pass or two-pass counterparts. This is due to the simplicity of their rugged construction and the minimal requirements in terms of machine-accommodating foundations. Vertical rolling mills have for years been particularly favored by U.S. West Coast producers of jet-engine rings--so much so that they are often termed California mills.

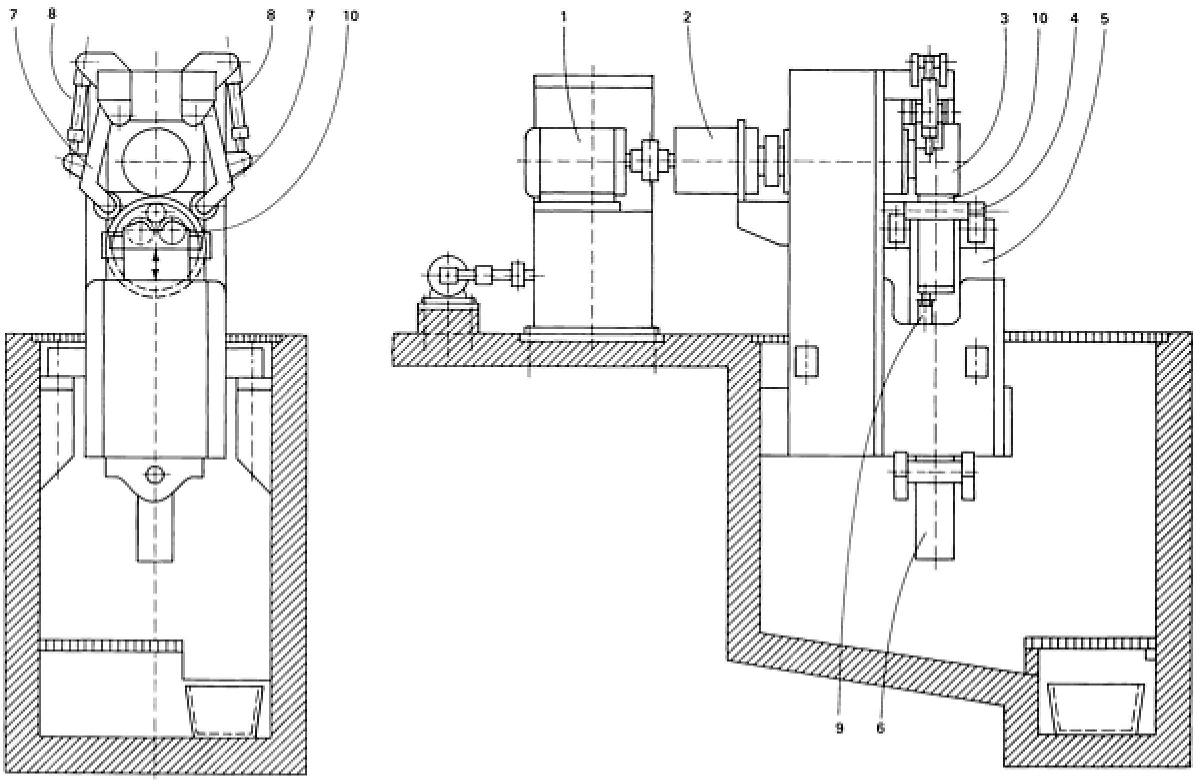


Fig. 5 Schematic of a vertical ring rolling mill. 1, motor; 2, transmission gear; 3, main roll; 4, mandrel; 5, mandrel carriage; 6, hydraulic cylinder to raise and lower mandrel carriage; 7, centering rollers; 8, hydraulic cylinder to control centering rollers; 9, tracer roll; 10, ring

Conventional hammer and press shops find such mills a logical and complementary extension of their facilities. These mills can reduce ring production time to one-tenth that required in hammer forging, can produce smooth concentric diameters, and can leave less allowance for machining.

Blanks are preformed on hammers and presses, and this same equipment is used to flatten the workpiece between operations when open-pass rolling produces the characteristic fish tail defects in ring end faces. Profiled (contoured) main rolls and mandrels can be used to produce correspondingly contoured cross sections. Table 1 lists the capacities of three vertical ring rolling machines.

Table 1 Capacities of three vertical ring rolling machines

Model No.	Maximum rolling force		Approximate rolling speed		Size range of finished rings			
					Outside diameter		Axial height	
	kN	**tonf**	**m/s**	**ft/s**	**mm**	**in.**	**mm**	in.
125	1250	140	1	3.3	250-1000	10-40	35-350	**1.4-14**
160	1600	180	1	3.3	275-1500	11-60	50-425	**2-17**
200	**2000**	**225**	**1**	**3.3**	**300-2000**	**12-80**	**50-500**	**2-20**

Radial-Axial Horizontal Rolling Machines. Although many single-pass horizontal machines are currently in use, very few have been installed in recent years. The predominant modern machine is the two-pass radial-axial machine.

Figure 6 shows a schematic of the operating principles of a radial-axial ring rolling mill. The ring blank is placed over an undriven mandrel (which can be retractable for ease of loading and unloading) and rests on table plates that form part of the radial carriage. A separate roller support carriage is used for larger rings. A backing arm with a mandrel upper bearing is lowered to support the mandrel. This backing arm is connected to the radial carriage so that they move as a unit, hydraulically activated, toward the fixed-axis main roll.

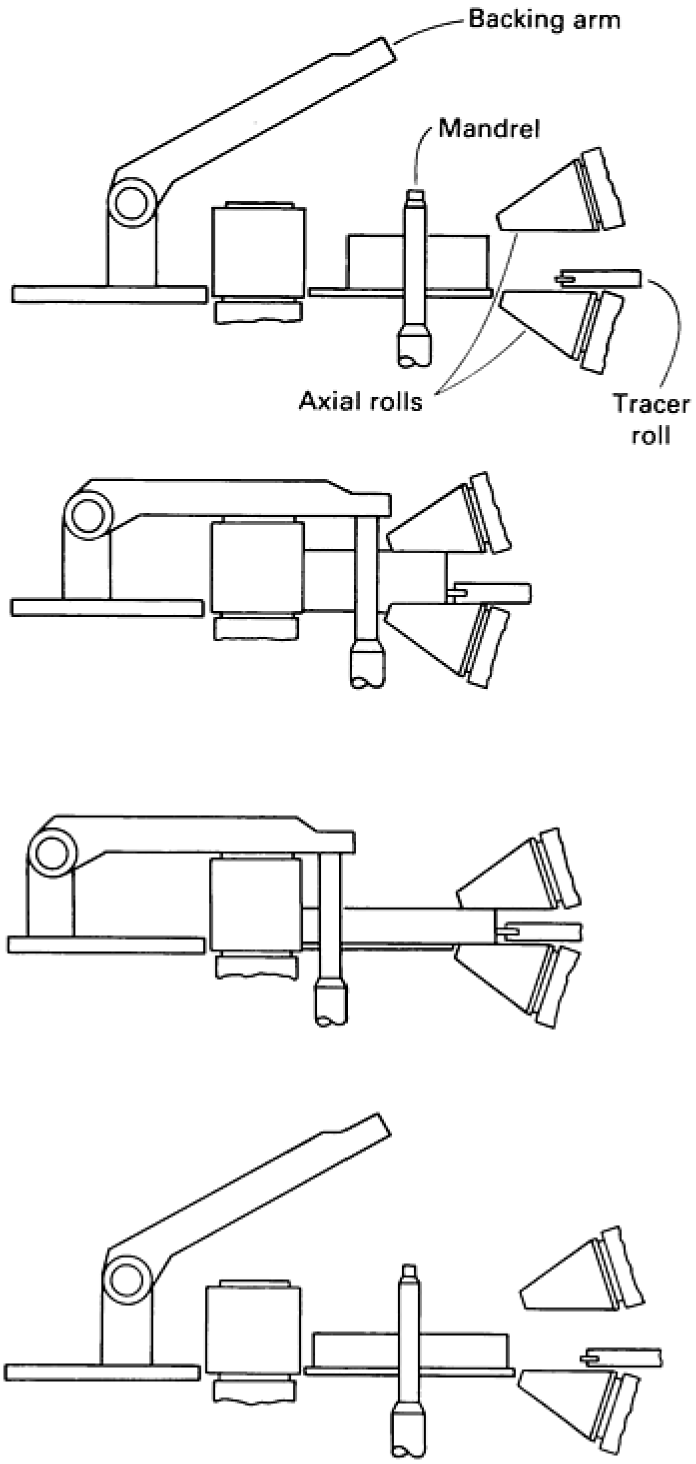


Fig. 6 Schematic showing operation of a radial-axial ring rolling mill.

The main roll rotates at a constant, preselected speed. The ring begins to rotate as the mandrel squeezes the ring wall. This in turn causes the mandrel to rotate.

A separate housing, which holds a pair of conical (axial) rolls, advances until these rolls cover the end faces of the ring blank. The lower conical roll is held in a fixed position such that the roll upper (horizontal) surface is typically 3 to 5 mm (0.12 to 0.2 in.) above the level of the table plates. Both conical rolls are driven, and the upper roll is moved hydraulically. The upper roll slides toward the lower roll to cause axial height reduction of the ring. The axial rolls withdraw as the ring diameter increases, maintaining minimum slip rolling conditions between the conical rolls and the ring end faces.

A tracer wheel mounted on slides between the axial rolls contacts the ring outer diameter. The ring diameter is monitored through measurement of the relative displacement of the tracer wheel and the axial roll carriage.

A pair of hydraulic centering arms (Fig. 7), connected through gear segments, contact the ring outside diameter and ensure that the ring stays round and in the correct position in relation to the longitudinal axis of the mill. Load cells in these centering arms detect differences in force against each centering roll. Through the mill control system, the load cells cause rapid, fine adjustment of axial roll speed to remove any force imbalance and therefore to maintain the correct positioning of the ring during rolling. Either manually (through a potentiometer) or automatically, the centering force is reduced as rolling progresses and the stiffness of the ring decreases.

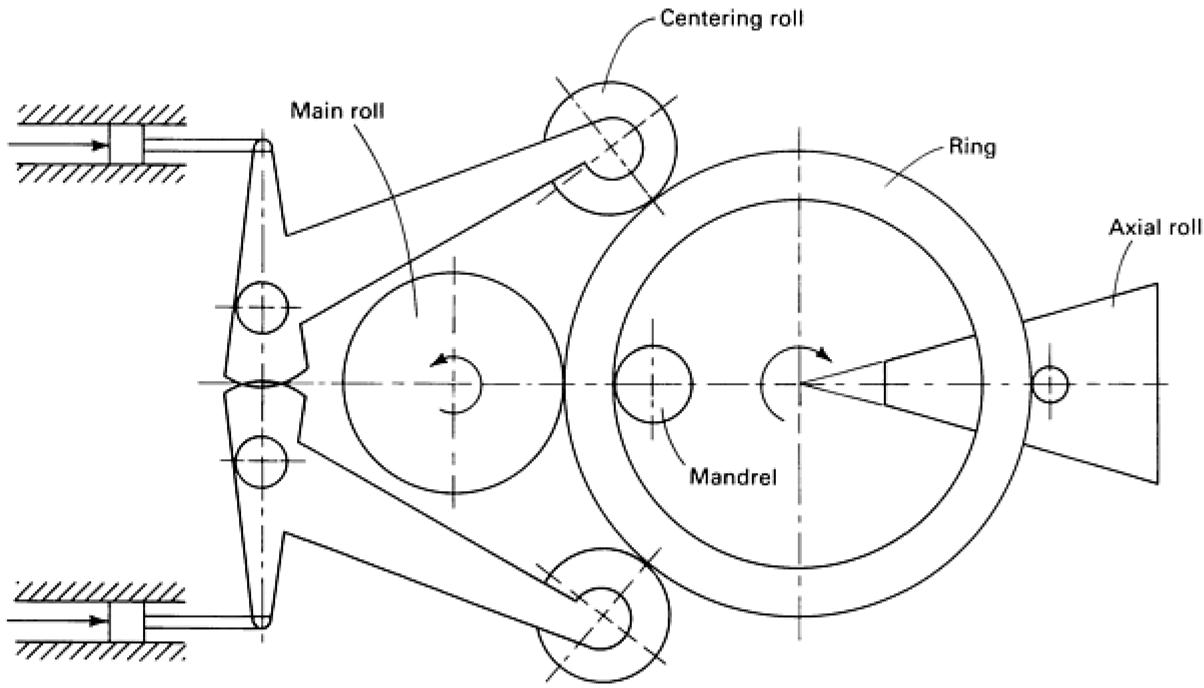


Fig. 7 Schematic of radial-axial mill showing centering rolls. Mounted in hydraulically controlled arms, the centering rolls ensure that the ring does not grow out of round and remains in the correct position relative to the longitudinal axis of the mill.

The relationship between radial (wall thickness) and axial (height) reduction is preselected to ensure the absence of ring surface defects, and it is maintained by computer control. Similarly, the pattern of diameter growth is predetermined and computer controlled. The mill operator need only set blank and finished ring dimensions at the control desk and initiate the rolling cycle. Rolling is automatically stopped when finished outside diameter, inside diameter, or mean diameter (chosen by the operator) is reached.

The reason for the predominance of these radial-axial mills is the wide range of capabilities provided by an individual mill. A typical midrange machine, with 980 kN (110 tonf) horizontal and 780 kN (88 tonf) vertical force capability and total motor power of 500 kW, can roll rings weighing less than 45 kg (100 lb) to more than 2000 kg (4400 lb), ranging in size from 300 mm (12 in.) inside diameter to 3000 mm (10 ft) outside diameter, and from 50 to 500 mm (2 to 20 in.) in height.

The extraordinary demands made on equipment when rolling superalloys, which have considerably higher resistance to deformation than carbon and alloy steels, have forced developments and improvements in radial-axial mill design and construction. These improvements have been beneficial when selectively applied to mills aimed at the less demanding materials.

High forces are required throughout the rolling of superalloys with little opportunity for final sizing (calibration) of the ring at reduced forces. Nevertheless, the expensive materials involved place a premium on near-net shape capability.

The guiding systems on such mills must encounter very low friction and must be free from play and wear. Therefore, hydrostatic bearings are used. Machine control systems are designed to compensate automatically for deflection of machine members on the axial frame. These load-bearing members are designed for maximum rigidity and minimum deflection.

Reversing conventional design, the mandrel is mounted in a fixed lower housing, and the main roll is mounted in a moving carriage. The mandrel can therefore be firmly clamped at both ends. This allows the use of smaller-diameter mandrels, which can better penetrate the ring wall. The maximum radial and axial forces on mills installed in the 1980s for rolling aircraft materials are equal and are high in relation to the overall dimensions of the mill and to their ring-size capabilities. For example, a mill for rolling such materials to 1250 mm (49 in.) diam and 200 mm (8 in.) height would have 980 kN (110 tonf) available both radially and axially.

Older mills, and those intended for rolling less demanding materials, have usually had axial force capabilities lower than their maximum radial force. Machines are usually designated according to the radial and axial rolling forces available; for example, 25/20 indicates a radial rolling force of 245 kN (27.5 tonf) and an axial rolling force of 196 kN (22 tonf). Table 2 lists the characteristics of numerous radial-axial ring mills. The trend of these mills has been toward ever-higher axial-force availability, taking advantage of the improved design prompted by development of the aircraft ring rolling "specials." This is particularly important when rolling washer-type rings (those with high wall-thickness-to-height ratios) free from end face defects.

Table 2 Characteristics of radial-axial ring rolling machines

Machine type	**Rolling force**				**Rolling speed**		Size range of finished rings			
	Radial		**Axial**				**Outside diameter**		Minimum height	
	kN	**tonf**	**kN**	**tonf**	**m/s**	**ft/s**	**mm**	**in.**	**mm**	in.
25-20	250	28	200	23	0.4-1.6	1.3-5.2	170-800	6.7-31.5	30-220	**1.2-8.7**
32-25	320	36	250	28	0.4-1.6	1.3-5.2	180-1000	7.1-40	30-220	**1.2-8.7**
40-32	400	45	320	36	0.4-1.6	1.3-5.2	200-1200	7.9-47	40-220	**1.6-8.7**
50-40	500	56	400	45	0.4-1.6	1.3-5.2	200-1400	7.9-55	40-350	**1.6-13.8**
63-50	630	71	500	56	0.4-1.6	1.3-5.2	230-1600	9-63	40 min	**1.6 min**
80-63	800	90	630	71	0.4-1.6	1.3-5.2	260-2000	10.2-80	40-510	**1.6-20**
100-80	1000	112	800	80	0.4-1.6	1.3-5.2	290-2500	11.4-98.5	40-560	**1.6-22**
125-100	1250	140	1000	112	0.4-1.6	1.3-5.2	320-3000	12.5-120	50-620	**2-24.4**
160-125	1600	180	1250	140	0.4-1.6	1.3-5.2	350-3500	14-140	50-670	**2-26.4**
200-160	2000	225	1600	180	0.4-1.6	1.3-5.2	380-4000	15-160	50-720	**2-28.3**

250-200	2500	281	2000	225	0.4-1.6	1.3-5.2	410-5000	16-200	60-770	**2.4-30.3**
315-250	3150	354	2500	281	0.4-1.6	1.3-5.2	440-6000	17.3-240	60-820	**2.4-32.3**
500-315	**5000**	**562**	**3150**	**354**	**0.4-1.6**	**1.3-5.2**	**Up to 7000**	**Up to 275**	**100-1160**	**4-45.7**

Source: Wagner Dortmund and J. Banning AG

Multiple-Mandrel Mills. Four-mandrel mechanical table mills have been extensively used in the production of antifriction bearing races. The undriven mandrels are supported only at their lower ends, where they are mounted in a rotating table (Fig. 8). The driven main roll is set inside the annular table, with its center offset from that of the table.

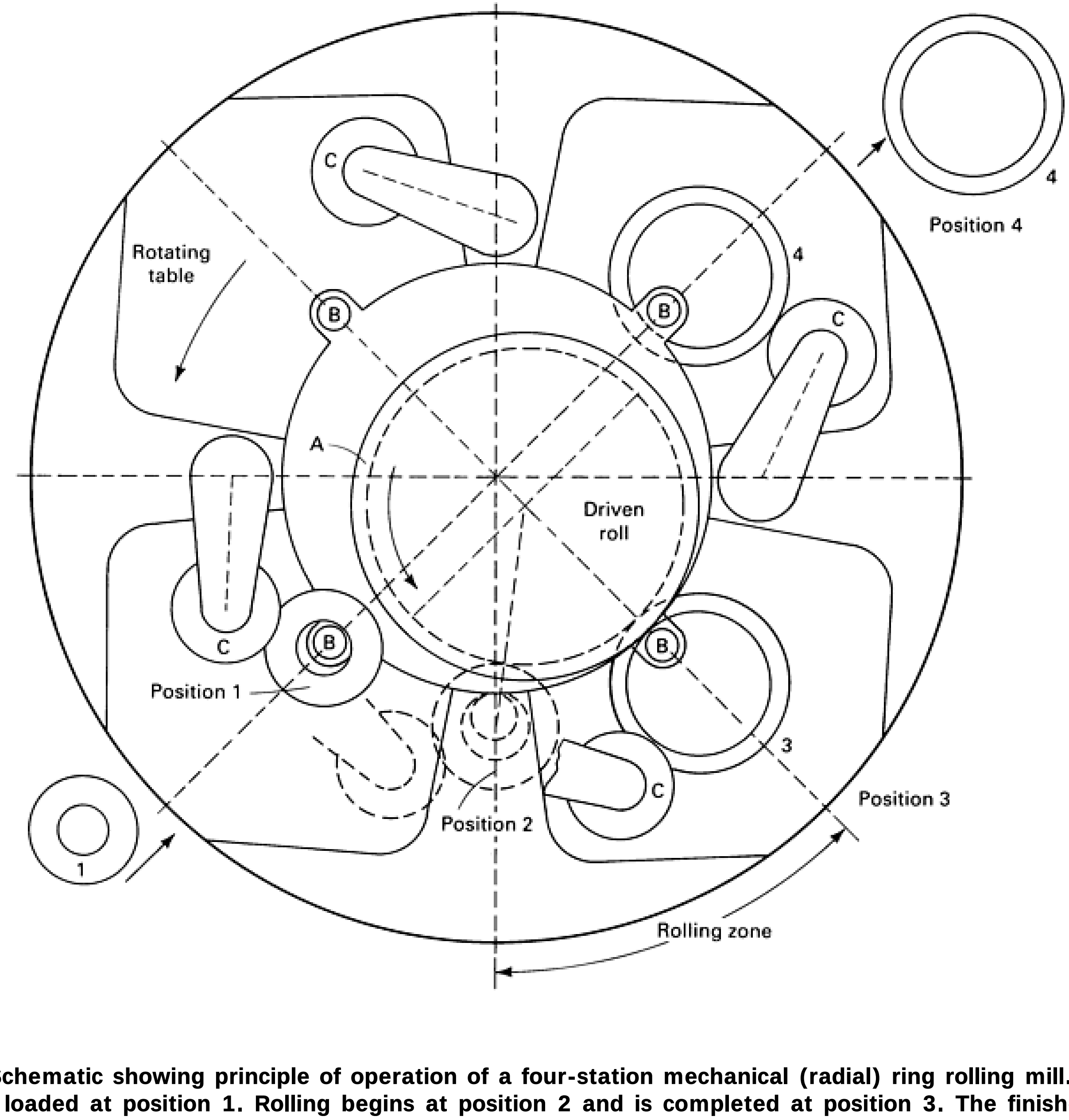


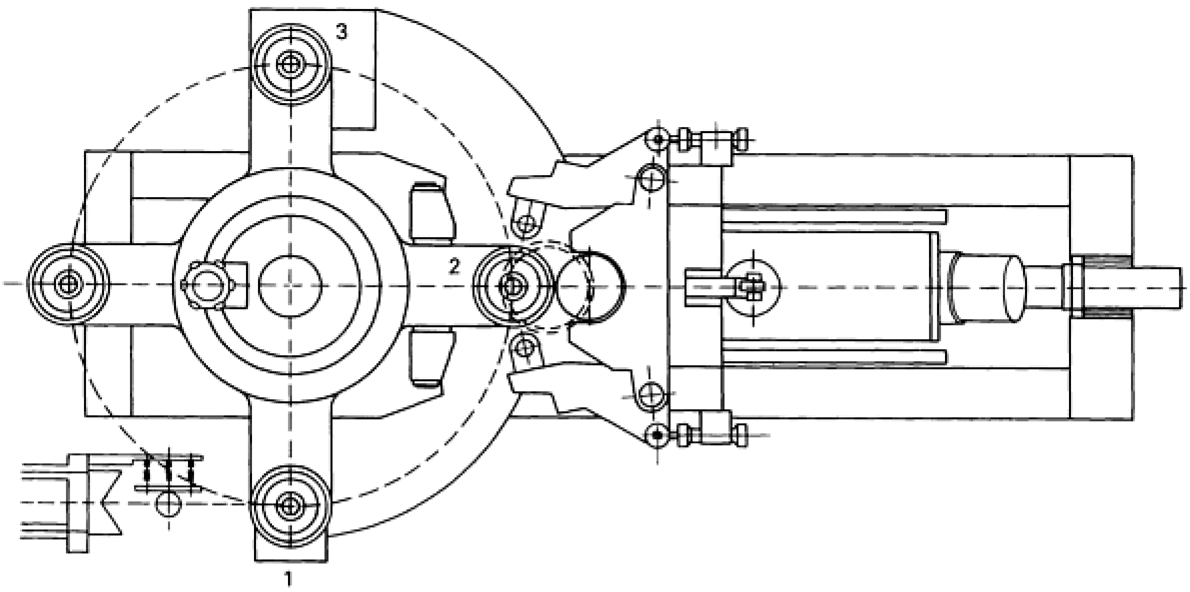
Fig. 8 Schematic showing principle of operation of a four-station mechanical (radial) ring rolling mill. The ring blank is loaded at position 1. Rolling begins at position 2 and is completed at position 3. The finished ring is unloaded at position 4. A, driven roll; B, mandrels; C, guide rolls.

The blank is loaded at position 1, where the eccentricity of the table and main roll centers provides a suitable clearance between the mandrel and the main roll. The table is then rotated by electrical drive, and the gap between the mandrel and the main roll decreases until the ring blank is contacted (position 2). As the table continues to rotate (at much slower angular velocity than the main roll), the gap between the mandrel and the main roll decreases to a minimum (position 3), causing the rapidly rotating ring to be reduced in wall thickness and to increase in diameter. The table rotates to position 4, and the ring is unloaded. The height of the ring is controlled by a closed pass.

Table mills require a pressed blank slightly lower in height than the finished ring. Because the ring dimensions depend entirely on start and finish wall thickness, the weight tolerances on starting material are critical. Although a single cam-operated support roll is used, final ring roundness and diameter variation mean that postrolling sizing presses must be employed.

Extensive tooling is required for such mills, with each ring needing its own special combination. Setup times are long. Although they are very efficient in terms of productivity when in operation, these mills require large production runs in order to be economical. Production rates to 1200 pieces per hour can be achieved with these machines on rings weighing 1.75 to 2.75 kg (3.9 to 6 lb), although outputs of 500 to 800 pieces per hour are more usual.

Automatic Radial-Axial Multiple-Mandrel Ring Mills. The modern computer-controlled, highly flexible, four-mandrel radial-axial ring mill was conceived in Germany in 1976. Again, the principle of using mandrels to transport the ring blank into the rolling position (Fig. 9) and from the rolling position to the unloading position is used in these units. However, the mandrel is housed at its upper end in a quick-change cassette, which is in turn located in a rotating carousel.



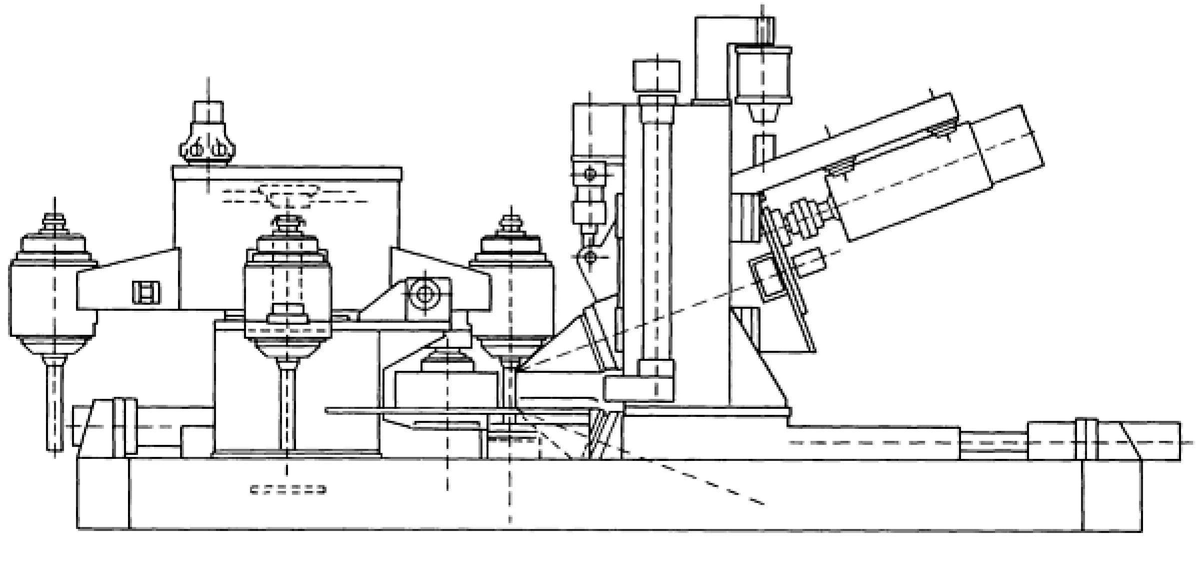


Fig. 9 Schematic of a multiple-mandrel radial-axial ring rolling mill.

A blank is presented at rolling table height to the mandrel at the loading station (position 1, Fig. 9), which then lowers sufficiently to enter the blank, and the carousel rotates, sweeping the blank through 90°, to the rolling position (position 2, Fig. 9). The mandrel is lowered into a bottom bearing housing, and the driven main roll advances hydraulically to provide both the squeezing force and the rotational drive.

Driven conical axial rolls are mounted in a horizontally movable carriage, with the upper roll movable vertically in slides to provide axial rolling force and drive. These rolls advance to cover the blank end faces, and axial rolling begins.

The overall method of operation is as described in the section "Radial-Axial Horizontal Rolling Machines" in this article, with the exception of the moving main roll carriage and centering arms mounted on the axial carriage. This latter feature is dictated by the need for a clear transport path around the main roll.

At the completion of rolling, the mandrel withdraws to clear its bottom bearing housing, the carousel rotates 90°, and the completed ring is swept to the unloading position (position 3, Fig. 9). At this point, the mandrel moves to its uppermost position, allowing the ring to be moved away either on a powered conveyor or by another handling device. Simultaneously, another blank is brought to the rolling position, and the process is repeated.

During rolling, computer controls maintain a preselected relationship between radial and axial cross-sectional reduction. This ensures a defect-free ring and controls the diameter growth rate so that, after a reduced rate of feed (roll gap closure) for the last few ring revolutions, each ring is round and accurately dimensioned.

Tool changing and setup for each ring type usually take 30 min. This assumes that the blanking press is of the preassembled, interchangeable bolster design. Rings that weigh up to 80 kg (175 lb) and are 800 mm (31 in.) in outside diameter are typically rolled on such mills, and production rates of up to 300 pieces per hour are achieved on smaller rings.

Closed-die axial rolling combines the elements of ring rolling with the elements of closed-die forging. Closed-die axial rolling relies on less than full contact area between the tool and the workpiece (Fig. 10) and therefore can produce circular forgings using 90 to 95% less force than would be required in closed-die forging.

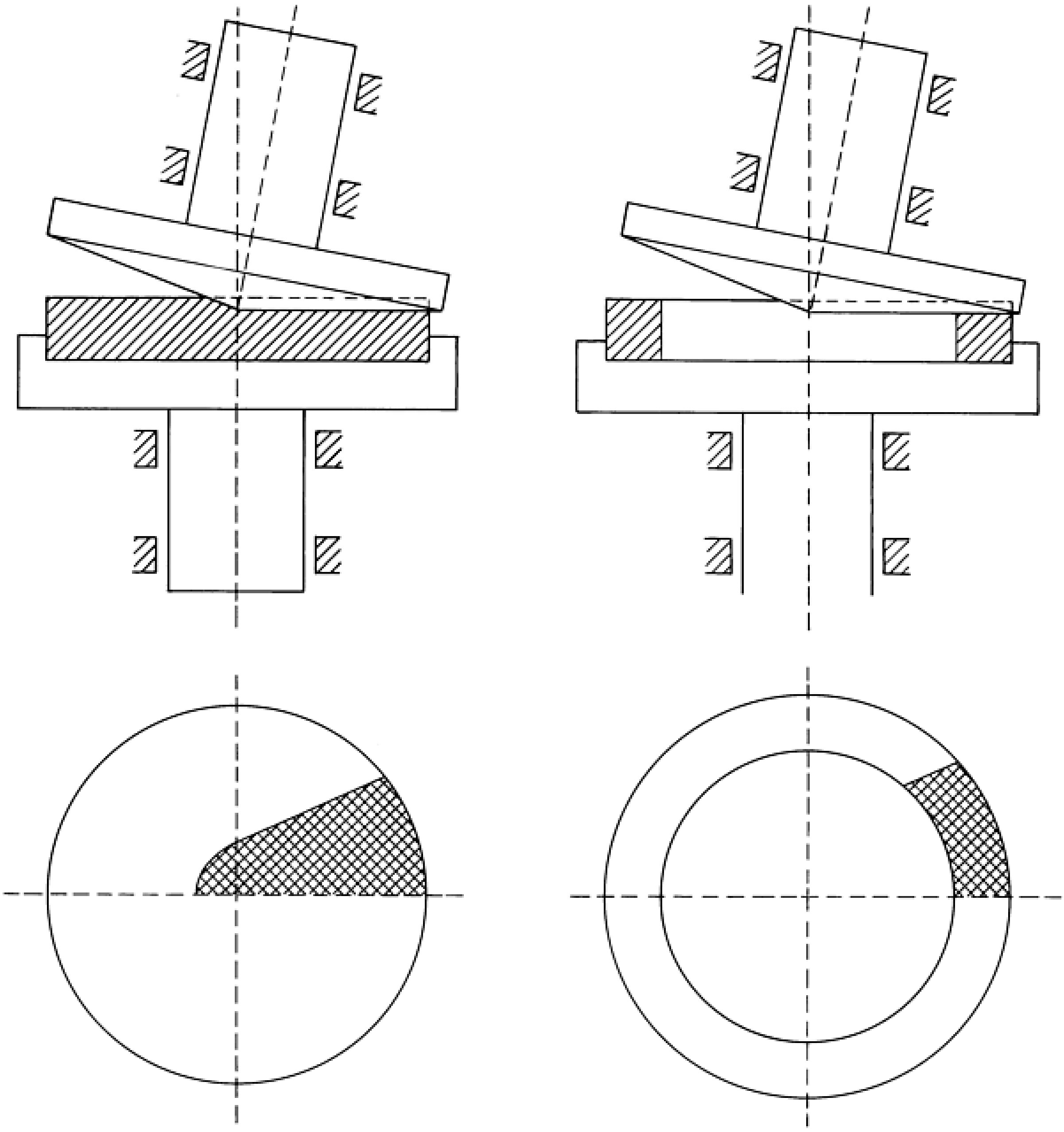


Fig. 10 Operating principle of the axial closed-die rolling process.

In closed-die axial rolling, the lower tool rotates about its vertical axis, typically at 30 to 250 rpm, and contains the workpiece. The upper tool, its axis inclined at some 7° to that of the lower tool, applies the rolling force. Feed rates in the range of 20 to 300 mm/min (0.75 to 12 in./min) are employed. The direction of tool movement is vertically downward. The conical shape of the upper tool, which would generate a parabolic contact area in the absence of tool rotation, is rotated by the bottom tool, through the workpiece. A semiparabolic contact area results.

The starting material may be in the form of a solid block or a prerolled ring. Products can be solid disk-type forgings or annular ring-type components and can be complexly contoured cross sections. Production rates to 120 pieces per hour are possible. More information on this process is available in the article "Rotary Forging" in this Volume.

Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Product and Process Technology

To the casual observer, ring rolling is a deceptively simple process. In truth, it is exceedingly complex and as yet not fully understood or fully predictable.

For many years, largely by experience or trial and error, manufacturers of ring rolling equipment and those using the equipment have developed manufacturing techniques that allow production of consistently dimensioned, and often complexly shaped, rings in a wide variety of forgeable materials.

Even today, there are many ring rolling mills in operation that rely heavily on operator skill and dexterity to produce a satisfactory product. However, the ever-increasing understanding of the fundamental behavior of materials during rolling has led to the incorporation of this knowledge as well as the latest prevailing process control technology into successive generations of rolling equipment.

By the early 1980s, the first computer-controlled radial-axial ring rolling machines became operational. These machines were capable of rolling defect-free rings with extremely high height-to-wall ratios at speeds considerably higher than those possible with manual control.

Early investigative work concentrated on the displacement of individual zones of material due to ring rolling (Ref 1). Deformation was found to occur across the entire cross section of the ring if the slip fields (force cones, Fig. 11) overlapped; slip fields are created by the roll indentation of the metal being worked. Considerable displacement of material was found at the inside diameter, with less displacement occurring at the outside diameter, both in the direction of rolling, in relation to the relatively undisturbed material at the ring mean diameter (Fig. 12). The grain flow was confirmed as circumferential (Ref 2).

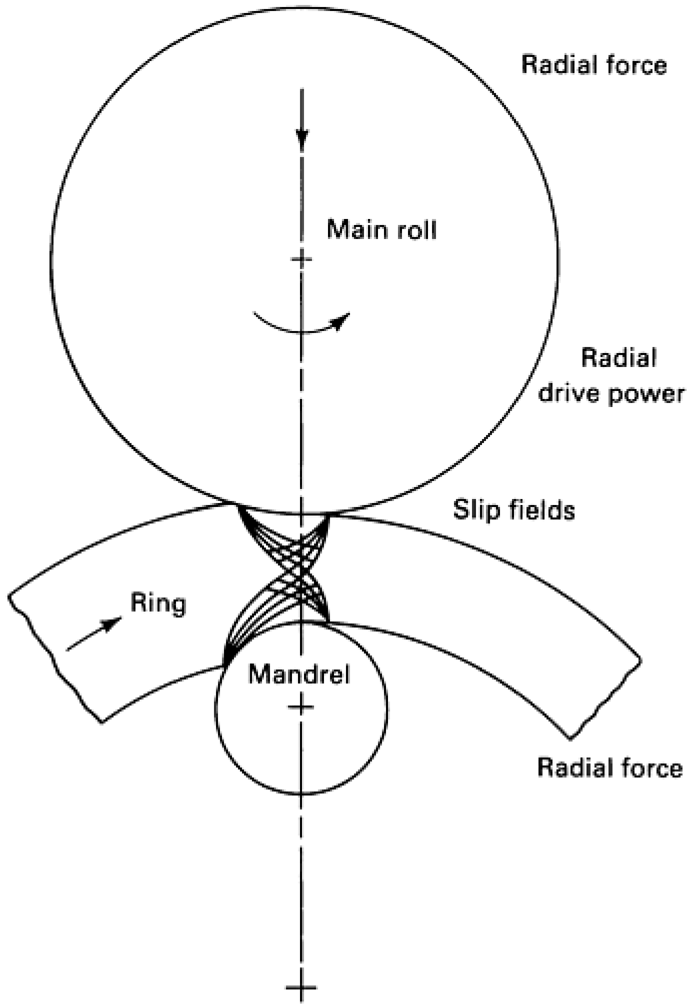


Fig. 11 Slip fields (force cones) generated during ring rolling.

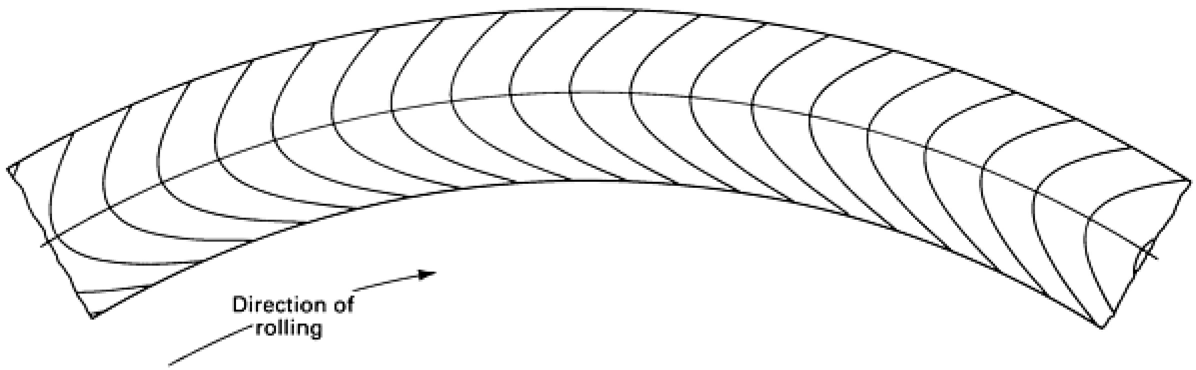


Fig. 12 Relative displacement of material across the ring thickness during rolling. Material near the outer portions of the wall thickness is displaced, while material near the center of the wall is relatively undisturbed.

Basic investigation predicted and confirmed in radial-pass rolling the formation of a plastic hinge (Fig. 13) diametrically opposite the point of roll indentation (Ref 3). This phenomenon was indentified as a possible source of ring roundness control problems at high indentation rates.

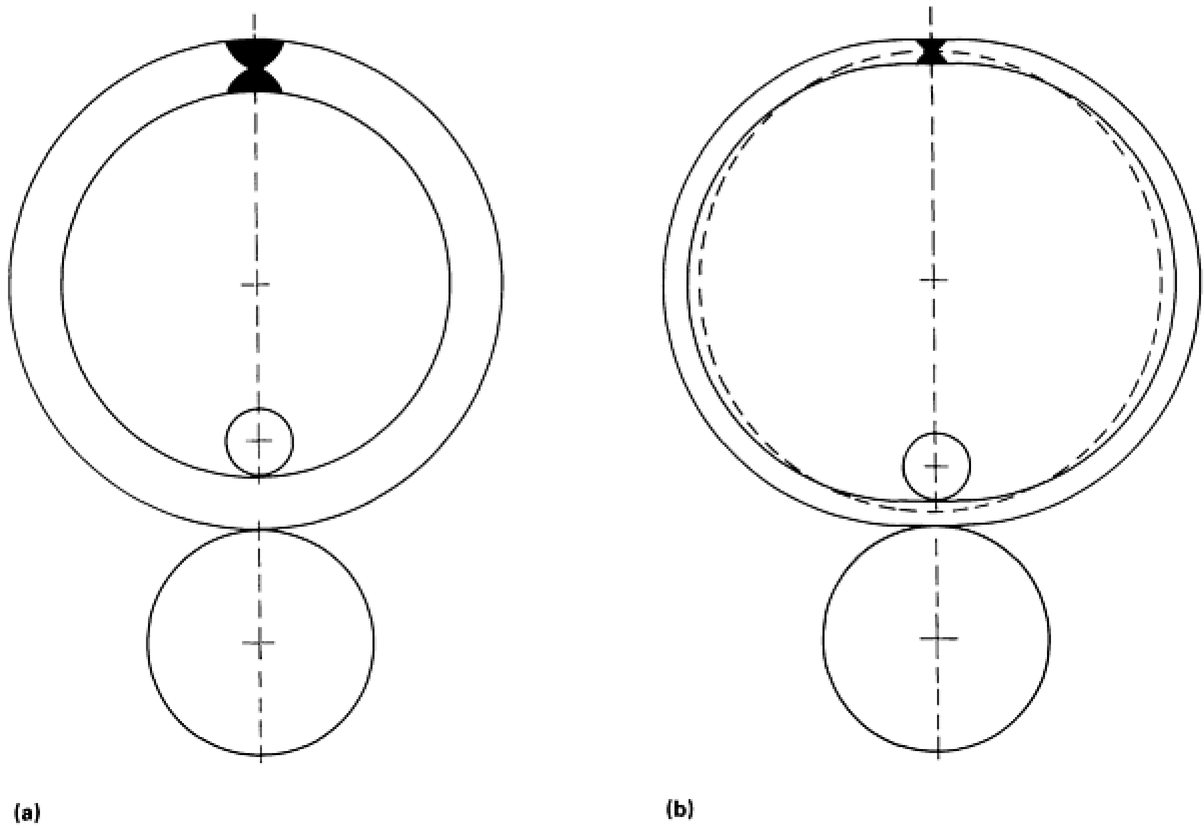


Fig. 13 Formation of a plastic hinge opposite the rolling pass (a) and the resulting ovality (b)

Although this phenomenon is of interest, especially when considering the combined effect of axial rolling and plastic deformation due to the hinge at the same point on a ring, it is of limited practical importance. However, it did represent the first (published) effort to gain a sound theoretical understanding of this complex process. This and further work apparently stimulated experimental and analytical studies into ring rolling at many locations, including Japan, West Germany, Korea, and the United States; this work continues today.

Much of this work is aimed at improving the accuracy of mathematical models of the process so that increasingly realistic computer simulations can be carried out. The ability to roll difficult ring configurations on machines of given characteristics can thus be better predicted, and the direction machine design must take to roll particular ring types and materials can also be determined.

The majority of ring rolling machines installed worldwide since 1960 have originated in West Germany. Not surprisingly, West German companies have been responsible for much of the theoretical and practical development that has occurred in this specialized area of forging. In particular, a researcher at one West German company has developed a combination of theoretical and empirical relationships that has been successfully applied to ring mill design (Ref 4).

A primary objective in two-pass (radial-axial) ring rolling is to achieve diameter growth through cross-sectional reduction (with freedom from surface defects) quickly enough to allow profitable operation. A potential source of end-surface defects and noncircularity arises in the axial roll pass. To avoid slipping and scuffing at the ring end faces, conical roll pairs are necessary for height reduction. In this way, roll and ring surface speeds are matched across the ring faces. To maintain this no-slip condition, the axial roll carriage must withdraw horizontally during rolling at the same speed at which the ring center moves (that is, at one-half the diameter growth rate).

Another benefit of this operational principle is that higher vertical rolling forces can be applied for a given motor power because less power is wasted through slippage. Therefore, flat cross sections with height-to-wall ratios exceeding 1 to 16 can be rolled.

In practice, at the start of rolling, this ideal rolling condition is often not possible, because of mill and blank geometry. The axial roll theoretical centers may be beyond the blank axis to allow the axial rolls to cover the blank end faces completely. In addition, when rolling rings of diameters greater than can be accommodated slip-free by axial rolls of practical dimensions, the axial roll carriage must withdraw as fast as the ring outside diameter grows. This puts the conical roll centers ahead of the ring axis, again preventing an ideal surface speed match between rolls and ring. Because the wall thickness is by this time relatively small in relation to the large diameter of the ring, the speed differential across the ring face is reduced and is usually not a significant disadvantage. With the main roll rotating at a constant preset speed, the rotational speed of the conical axial rolls must decrease as the ring moves onto their progressively larger diameters (Fig. 14).

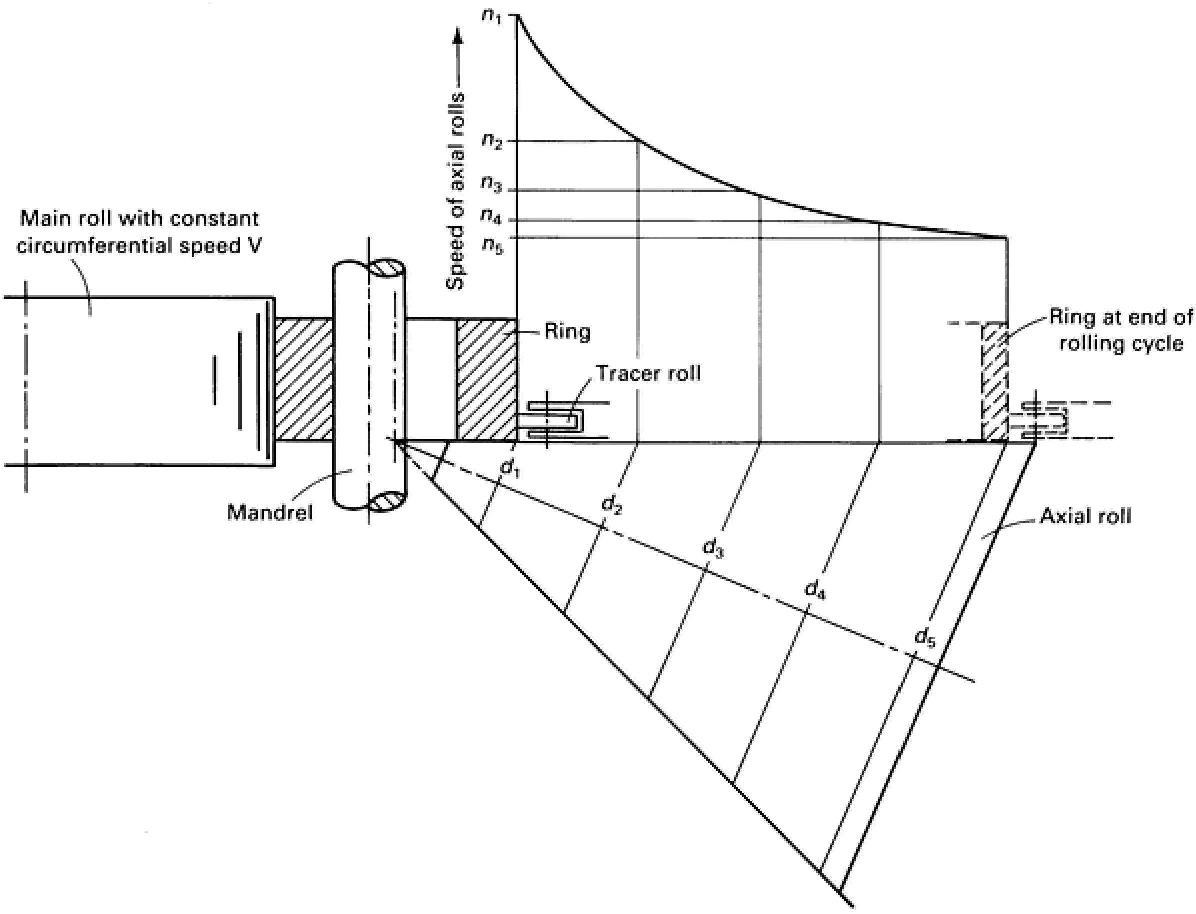


Fig. 14 Control of rpm of axial rolls. The axial rolls must turn more slowly as rolling progresses and the ring moves from d_1 toward d_5 on the axial roll. The rotational speed of the axial roll at a given point is a function of the rpm of the main roll and the diameter of the axial roll.

Depending on the type of hydraulic servovalves used, modern mills may be either force controlled or feed rate controlled. With the latter design, the rate at which the moving rolls are advanced (while not exceeding the capability of the mill) follows a preselected (via computer) pattern. With the former, more common design, horizontal and vertical forces are again applied (via computer) to a predetermined pattern. The objective with both control systems is to:

- Change the cross section of the ring in a specific manner to avoid surface defects
- To control the diameter growth rate in phases to minimize rolling time but to complete rolling with the ring stable and round

With regard to the changes in cross section, the ratio between radial (wall thickness) reduction and axial (ring height) reduction must be constantly maintained according to the following relationship (Ref 4):

$$\frac{\Delta b}{\Delta h} = \frac{h}{b} \quad \textbf{(Eq 1)}$$

where Δb is the wall reduction increment, Δh is the height reduction increment, h is the ring height, and b is the ring wall thickness. Equation 1 is derived from consideration of the spread that occurs when rolling with an open pass (Fig. 15). At the relatively low deformation rates per revolution that occur in ring rolling, plastic deformation takes place in the outer layers of the material, but the center tends to remain rigid/elastic. In the radial pass, this causes beads (Fig. 15) to form because of the lateral spread where the rolls and the ring are in contact. When these beads are rolled by the axial pass, greater circumferential growth takes place at the inner and outer diameters than in the region of the ring mean diameter. The material in this region is stretched, and a further reduction in height results, continuing the formation of hollows in the ring faces. Excess axial rolling removes this defect, but leads to the same type of defect on the inside and outside diameter surfaces of the ring (Fig. 16).

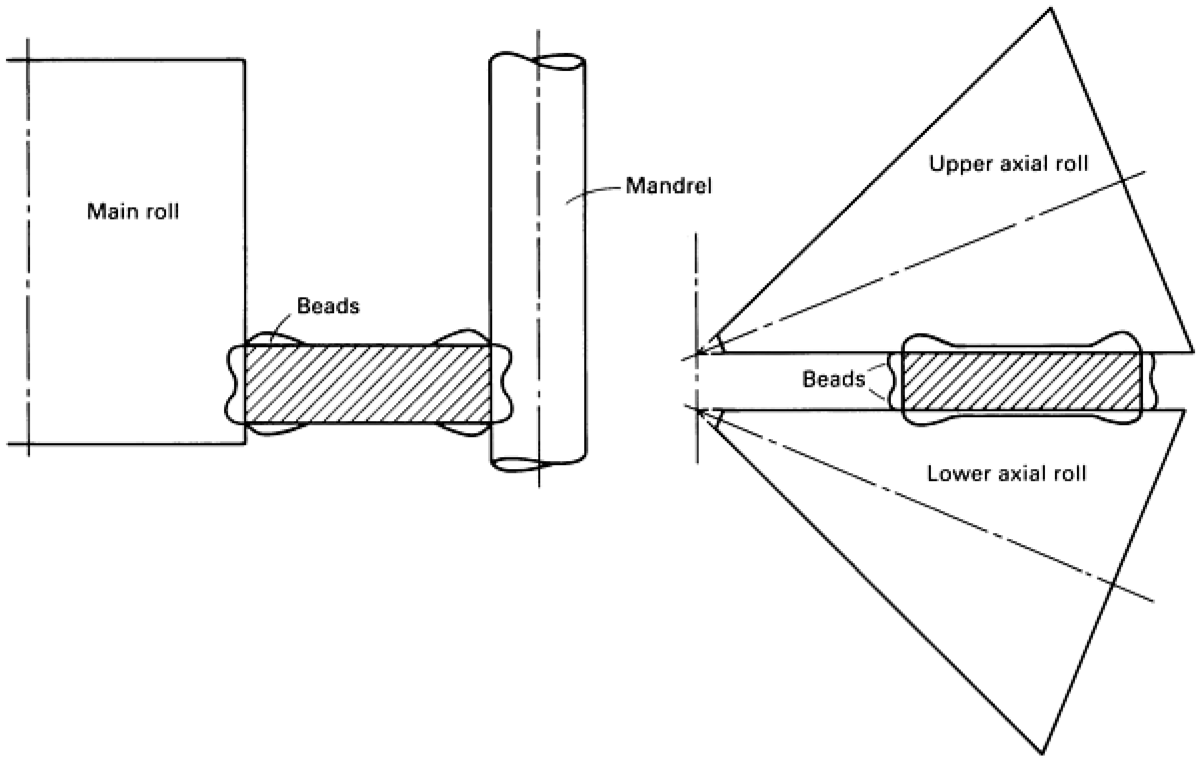


Fig. 15 Formation of beads during radial-axial rolling.

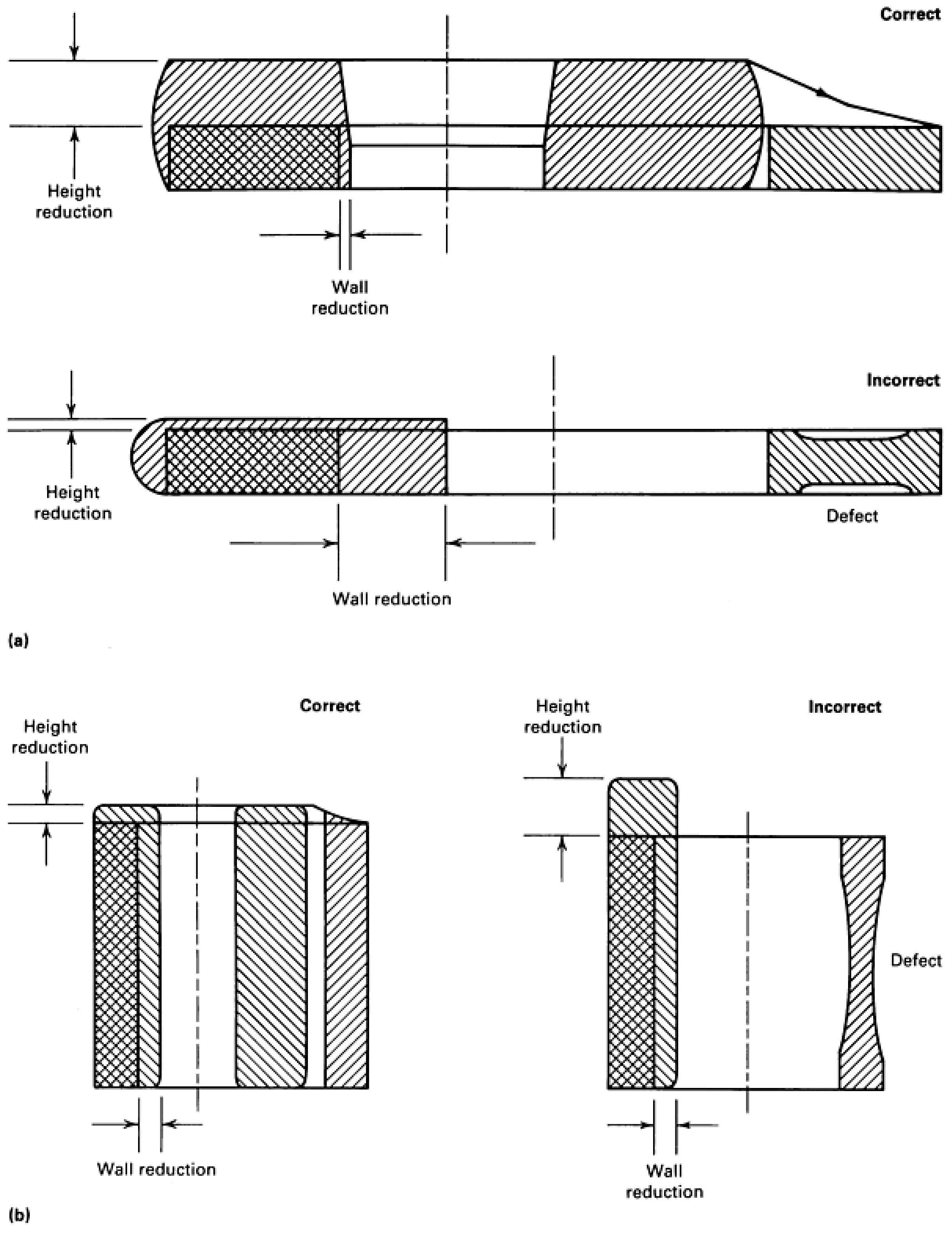


Fig. 16 Effect of blank design and wall-to-height reduction ratios in ring rolling. (a) Rolling of washer-type rings. (b) Rolling of sleeve-type rings

A secondary effect of bead formation caused by excess radial rolling is that ring height on the exit side of the radial pass is significantly greater than that on the ingoing side. Contact between the beaded ring bottom face and the table plate on the exit side of the radial pass causes the ring to lift from the horizontal plane and it attempts to spiral up the radial pass. The ring then either goes out of control and rolling must be halted, or the ring is held down (especially washer-type rings) and the cross section is distorted (takes on a dishlike shape). Maintenance of the Vieregge relationship (Eq 1) between incremental wall-to-height reduction and instantaneous ring-height-to-wall ratio prevents the above defects from forming.

The following relationship results from Eq 1:

$$h^2 - b^2 = \text{constant} \qquad \textbf{(Eq 2)}$$

that is, a hyperbolic relationship exists between wall thickness and ring height. In addition, given a constant volume of material, a hyperbolic relationship must be maintained between instantaneous ring height and diameter (Fig. 17).

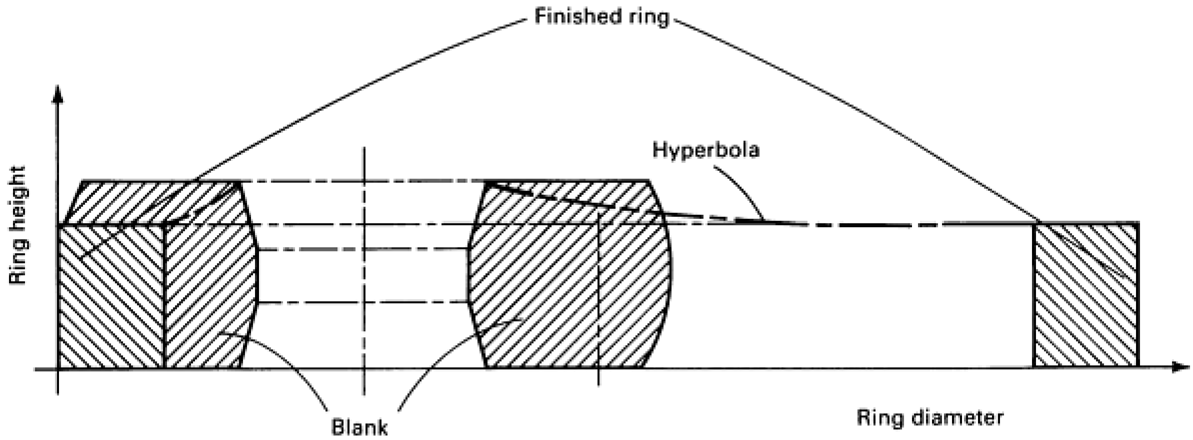


Fig. 17 Hyperbolic relationship between ring height and ring diameter at any instant

Typical cross-sectional rolling curves derived using Eq 1 and 2 are shown in Fig. 18. The critical nature of starting blank design is highlighted by Eq 1 and 2 because there is only one theoretically ideal starting blank cross section for any ring.

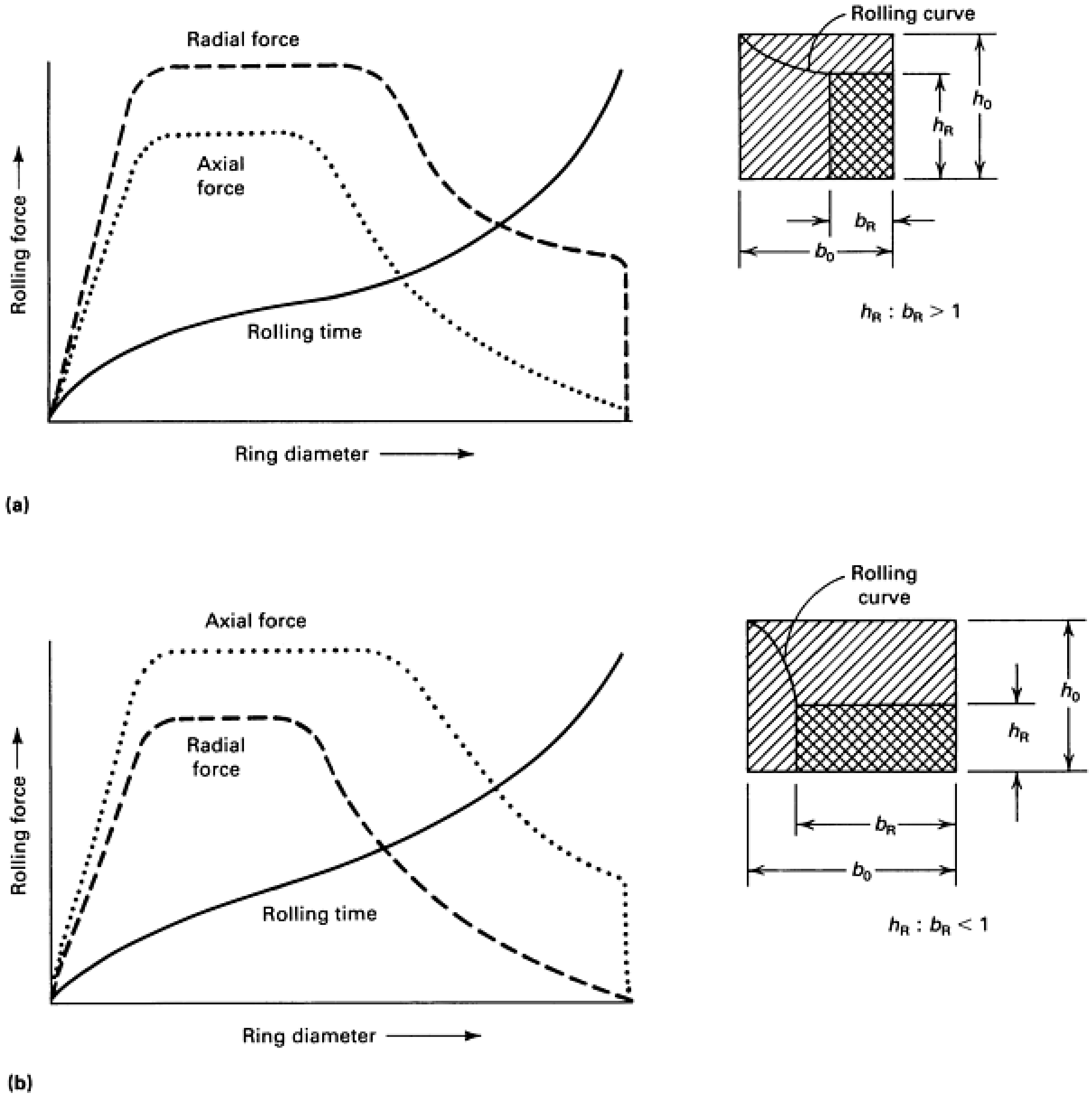


Fig. 18 Rolling strategies for sleeve-type (a) and washer-type (b) rings. b_o, initial wall thickness; h_o, initial height; b_r, final ring wall thickness; h_r, final ring height.

In practice, it has been found that considerable license can be taken with respect to starting blank configuration. This is often necessary because of limitations imposed by the equipment used, both to form blanks and to roll the rings. The most modern rolling mills allow selection of the shape of the height-to-wall reduction curve, enabling the operator to compensate for less-than-ideal blanks and other process variables.

The speed at which the cross section is reduced directly affects diameter growth rate and (depending on ring stiffness) the stability of the ring (roundness) during rolling. Typically, modern mills provide for up to six sequential ring growth control phases, although three are usually adequate (Fig. 19). In the initial phase, the rate of cross-sectional reduction increases from soft contact between rolls and blank to maximum in a few seconds. The second, usually main, phase of rolling involves decreasing cross-sectional reduction rate, resulting in near-constant diameter growth rate. The third phase involves a steadily decreasing diameter growth rate to maintain ring stability with decreasing ring rigidity (cross section versus diameter). The final reduction phase requires very low cross-section reduction rates and therefore low diameter growth rates. Final dimensions are obtained in this phase.

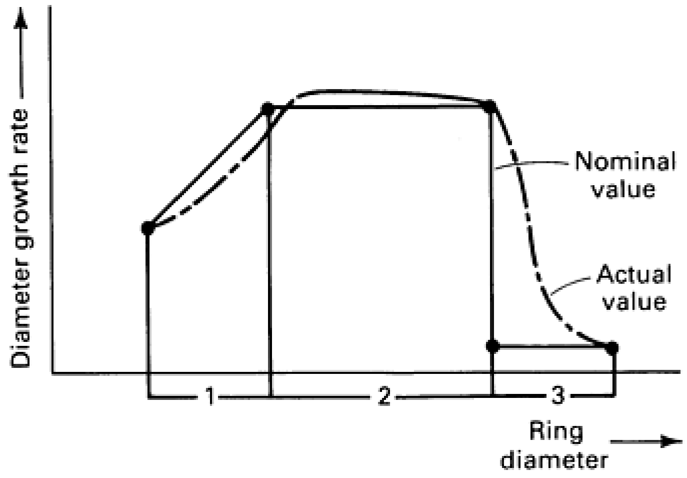


Fig. 19 Schematic of a three-section ring rolling program on a computer numerical controlled ring mill. In section 1, diameter growth rate increases linearly. In section 2, diameter growth rate is constant, compatible with ring stability and machine characteristics. In section 3, diameter growth rate is low as the ring is brought to final dimensions (the diameter is calibrated).

Contour Ring Rolling. With contoured cross sections (Fig. 2), the behavior of the material being worked is even more difficult to predict than with rectangular cross sections. Some experimental and analytical work has been done, mostly at the University of Manchester, England. In addition, a combination of theoretical and empirical relationships has been developed that gives reasonably accurate results when applied to the preforming of blanks and predicting the degree of success in achieving a desired contour from a given blank shape (Ref 4).

The first commercially produced contoured rings were railroad wheels made in the first ring rolling machine, which was built in Manchester, England, in 1852. Then, and in most instances since, blanking design and rolling technique were a matter of trial and error. One of the most important qualities of a successful, modern contour ring rolling company is still the practical experience gained from producing a wide range of shapes in a variety of materials over many years.

Many contours can be rolled from regular rectangular blanks, especially axisymmetric shapes with thinner wall sections at the center (double flanged outside diameter or inside diameter), as shown in Fig. 20. However, once the height of the groove exceeds 50% of the total ring height, the depth of the groove that can be rolled without significant overall shape distortion is progressively reduced. For example, with groove height at 80% of total height, successful groove depth is limited to approximately 20% of final ring wall thickness. This assumes closed-pass rolling and sufficient diameter expansion from blank to ring.

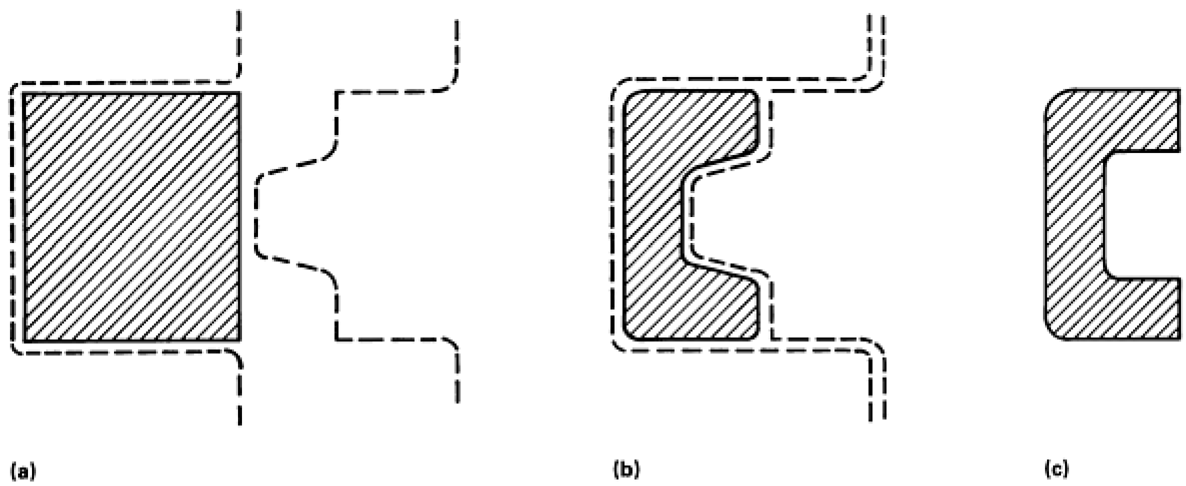


Fig. 20 Stages in the production of a C-profiled ring. (a) Blank. (b) Practical rolled ring. (c) Originally requested

shape. Blank cross-sectional area is approximately twice that of a rolled ring.

When it is found that a rectangular or open-die blank will not yield the desired contour, blank preforming must be used. Typically, the starting point for a new contour shape (from a preformed blank) is the application of a simple volume distribution calculation from ring to blank. The ring is divided into a number of axial slices, or disks, and the volume of each slice is calculated. By knowing the size of the rolling mandrel to be used, and therefore the inside diameter of the blank, a theoretical blank outside diameter can be calculated for each of the corresponding slices (assuming no height change). The theoretical blank outside diameter shape is generated by the aggregate of the individual slice outside diameters.

The resulting blank shape is unlikely to be successful in practice, because it does not take into account the axial flow of the material and because it assumes that each slice is being rolled throughout. For the latter to occur, the shape of the contoured rolls would have to change continually, initially corresponding with blank shape and finishing at ring shape.

In practice, a crude but often effective solution to this requirement consists of two-stage rolling, first using a roll shape intermediate between blank and ring. Some allowance for axial flow of material, when using a radial closed pass, is made by having a blank height lower than the finished ring height.

When using a radial-axial mill, either the blank design must be such that height is reduced to final height before material enters the upper section of the pass or the upper axial roll must operate in reverse of conventional mode and move up during rolling (Fig. 21a).

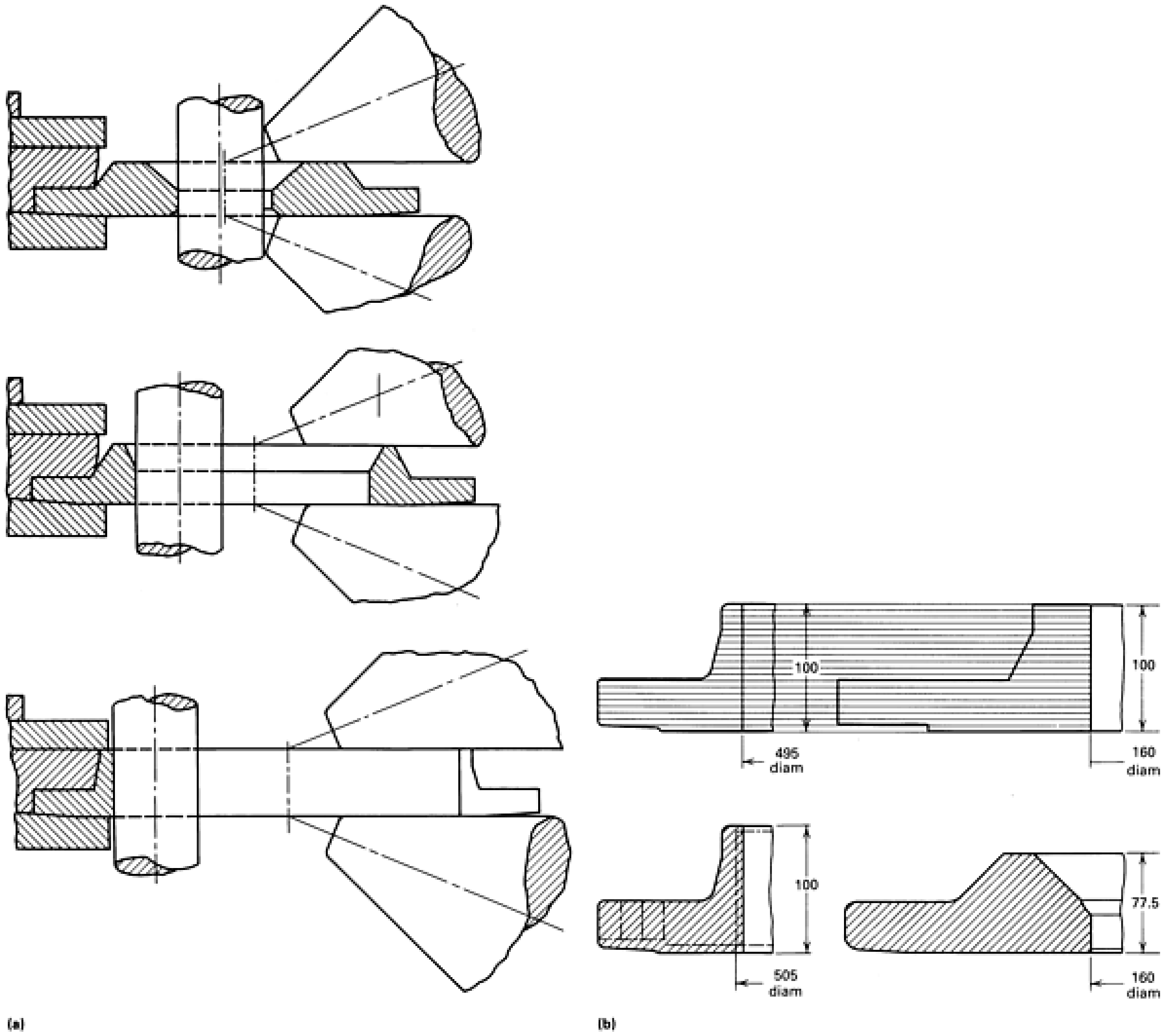


Fig. 21 (a) Rolling of a weld-neck flange in a radial-axial mill with controlled upward movement of the upper

axial roll during rolling. (b) Theoretical (top) and practical (bottom) weld-neck flange preforms (dimensions given in millimeters; 1 in. = 25.4 mm). See text for details.

The practical blank (Fig. 21b, bottom) has a less pronounced flange than that of the simple theoretical blank, but still has the necessary volume of material. A deeper (theoretical) flange (Fig. 21b, top), only partially enclosed by the corresponding groove in the main roll, would result in the folding and lapping of material at the junction of the upper flange face and tapered outside diameter. This is due to localized deformation fields at the junction of the flange and the taper and at the inside diameter, with the core of the ring remaining essentially elastic.

The practical blank is designed to allow for axial flow of material toward the thinner upper section of the ring. Figure 22 illustrates the behavior of simple-theoretical versus practical-successful blank shape.

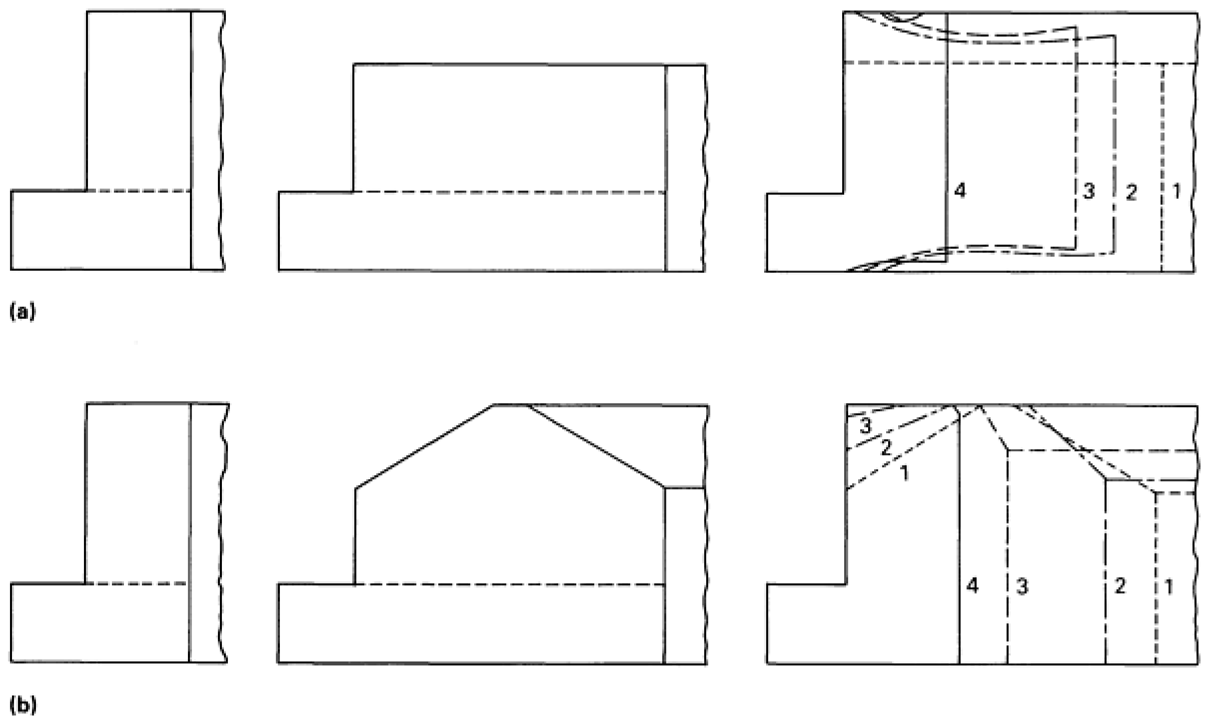


Fig. 22 Development of a finished ring profile from preformed blanks. (a) Theoretical blank shape. (b) Practical blank shape. Shown from left to right are the required profile (which is the same in both cases), the blank shape, and the stages of development during rolling (unsuccessfully in the case of the theoretical blank). Source: Ref 5.

The complete ring cross section must be acquired at the same moment the final diameter is reached for the following reason. Immediately after the pass is filled, the thinner-wall section attempts to grow faster circumferentially than the thicker-wall sections for a given decrease in roll gap. The thicker sections are stretched by the more rapid circumferential growth of the thin sections, and the contour begins to deteriorate in the thicker-wall sections. When preparing to roll an unfamiliar contour shape, blanks are sometimes machined from rough forgings, enabling trial rolling to be carried out without expenditure on possibly inappropriate preforming tools.

Roll diameters are an important consideration in contour forming. The relative curvature between the mandrel and the ring increases throughout rolling, while that between the main roll and the ring decreases. Therefore, as rolling progresses, the penetration of the mandrel into the ring increases, and that of the main roll decreases. Conventional rolling mill design, therefore, lends itself to inside diameter contouring, with mandrel diameters that are small in relation to main rolls.

Special-purpose contouring mills are usually designed to allow use of much smaller diameter main rolls when outside diameter contouring. To a limited extent, the same effect can be achieved by using a large-diameter mandrel sleeve and two-stage rolling on conventional ring mills.

The benefits of contour rolling are reduced material input and reduced machining to finished product. Typically, a weight savings of 15 to 30% can be achieved by using contoured versus rectangular rings.

To determine whether the additional cost of tooling and extended setup time is justified, the break-even point against reduced material and machining cost and minimum order quantity must be determined. Even on lower-cost materials, this quantity may only be 25 to 50 pieces, especially with repeating orders. Where higher-cost materials, such as superalloys, are involved, production of only three or four pieces may justify contouring.

Rolling Forces, Power, and Speeds. Economical production of seamless rings by the radial-axial rolling process requires rings to be rolled as quickly as possible in a manner that is consistent with dimensional accuracy and metallurgical integrity. A primary factor is the resistance of the material to deformation. This is related to the flow stress of the material at a given temperature and to the conditions existing in the rolling pass (roll diameters, frictional resistance, and so on).

With typical ring mill configurations, rolling speeds, and rates of cross-sectional reduction, as well as at temperatures of 1050 to 1100 °C (1920 to 2010 °F), the resistance to deformation of a plain carbon steel is found to be approximately 160 MPa (23 ksi) and that of a bearing steel approximately 196 MPa (28.5 ksi) (Fig. 23). For these materials, a decrease in temperature of 100 °C (212 °F) increases resistance to deformation by approximately 50%. Quite obviously, rolling force requirements can be minimized by operating at the maximum temperature allowable metallurgically. This requires consideration of both the temperature losses due to radiation and conduction as well as the temperature increase caused by plastic deformation.

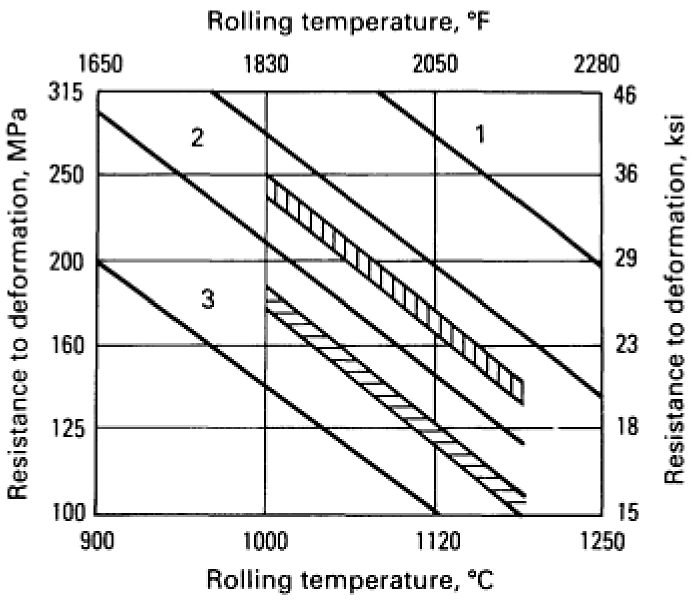


Fig. 23 Resistance to deformation versus rolling temperature for various steels. 1, chromium-nickel steels (>4% alloying elements); 2, bearing steels (chromium-nickel; 1-4% alloying elements); 3, carbon steels.

Rolling forces cannot be dealt with in isolation. The combination of roll force and resistance to deformation determines the extent to which the rolls indent the ring. With increasing indentation, the drive power required increases and, on present-day mills, may reach the mill motor limit well before maximum roll force has been applied. Further, with very heavy indentation, the relatively small diameter, undriven mandrel can exert so much circumferential resistance that the driven main roll is unable to overcome it; the driven roll then slips, and the ring fails to rotate (Fig. 24).

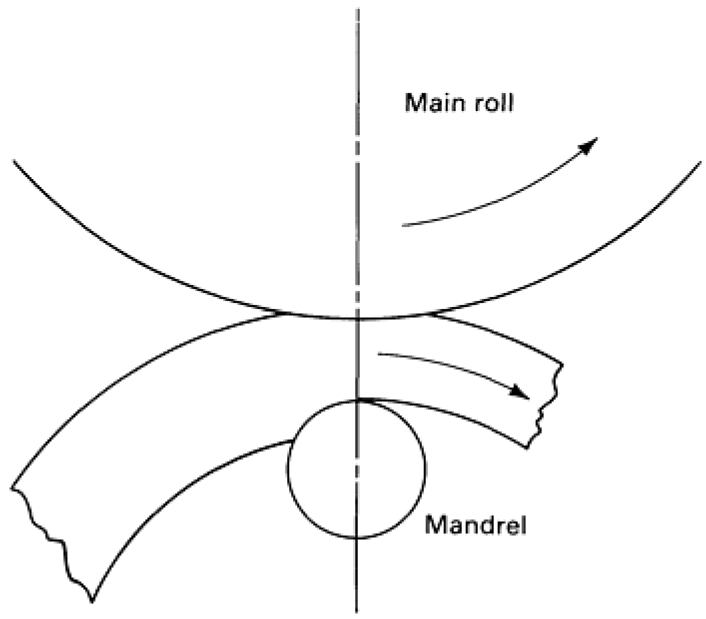


Fig. 24 Excessive indentation by the mandrel, causing the main roll to slip and the ring to stall in the radial pass.

Modern mills apply the principle of adaptive control to avoid such problems. That is, forces and torques are monitored continually by computer, and if they approach the upper limits of the mill and are changing in such a way that these limits are about to be exceeded, then they are automatically reduced in such a way as to maintain predetermined patterns of cross-sectional reduction and diameter growth.

Most theoretical analyses used to date for estimating or simulating the rolling forces and torques required in ring rolling have been derived from the relationships established in the simpler process of the hot rolling of bars. Factors that complicate the situation in ring rolling are:

- Nonsymmetrical rolling due to the differences in roll diameters (radial pass)
- Noncylindrical rolls and changing roll diameters (axial pass)
- One roll only, driven (radial pass)
- Changing ring diameter
- Continuous thickness and height reduction
- Three-dimensional deformation in the direction of roll closure, in the direction of rolling, and lateral spread

It is beyond the scope of this article to present the various complex mathematical relationships involved.

Earlier (three-dimensional) analyses required extensive use of empirically determined factors in order to achieve reasonable agreement between calculated and actual values. By the mid-1980s, extensive experimental work (Ref 6, 7) and considerable theoretical refinement had taken place. The resulting computer-based mathematical models predict material and machine behavior much more realistically. The computer control systems of recent ring mills make direct application of these developments.

A further limiting factor in the speed with which a ring can be rolled is the stability of the ring during rolling. A ring rotating at too high a speed, with excessive speed changes due to extrusion in each rolling pass, may lack the rigidity required to accommodate the various forces and moments acting on it. Gross out of roundness and/or out of flatness can result.

In practice, circumferential speeds to 3.6 m/s (12 ft/s) are used on smaller mills, and 1 to 1.6 m/s (3 to 5 ft/s) on larger mills. Diameter growth rates to 35 mm/s (1.4 in./s) are usually achieved during the main ring expansion phase; growth rates of 1 mm/s (0.4 in./s) are reached during the rounding or calibration phase.

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Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Blank Preparation

The manufacture of seamless rolled rings consists of two basic processes: the production of a preform or blank, and the expansion of that blank on a ring mill. Blank preparation can be carried out adjacent to the ring mill with no reheating before ring rolling, or--as is often the case in older plants--blank forming can be done separately (even in separate buildings) on several different pieces of equipment. These blanks are then gathered together in logical groups to be reheated prior to rolling.

The separate blanking approach is quite often found where aircraft ring materials are involved. This is because rolling cycle time is usually only a fraction of blank preparation time and because cold inspection and rectification of blank defects may be necessary before rolling.

Because many ring rolling operations are an outgrowth of conventional forge shops, the equipment and methods previously used to produce totally forged rings are often employed in more limited fashion to prepare blanks. Open-die hammers and presses, with highly skilled operators and using a wide variety of loose tooling pieces (for example, punches, saddles, and bars), can often produce practically all required blank sizes and shapes. Hammers are especially versatile and have the advantage of much lower initial cost than the equivalent press. However, environmental noise problems have tended to limit new installations. Hammers and general-purpose presses tend to be labor intensive and have relatively low output rates compared with presses designed specifically for producing blanks.

The trend with most installations in recent years, particularly when the more easily worked materials (for example, carbon, alloy, and some stainless steels) are involved, has been for the blanking press to be integrated with the ring mill into a ring production unit. In these installations, the capabilities of the blanking press are matched to those of the specific ring mill.

Theoretical considerations regarding blank dimensions were explained in the section "Product and Process Technology" in this article, and the importance of starting with the correct blank-height-to-wall thickness relationship was stressed. Beyond this, it is important that the methods used to form the blank do not create quality problems (for example, off-center or ragged punching) at the rolling stage.

Simply put, the first objective of blank making is to put a hole in the workpiece that is of sufficient diameter to allow the blank to fit over the rolling mandrel. The diameter of the mandrel has to be such that sufficient force can be applied to reduce the ring wall section at an acceptable rate. The smaller the hole, the less the material wasted.

Starting material is usually round, although round-cornered-square or octagonal billets can be used. When nonround material is used, initial working is required to convert it to round stock. Otherwise, the first blanking operation upsets the billet to reduce height. The second operation consists of indenting with a punch, leaving a thin web at the bottom of the blank. The third operation punches out this web, creating the doughnut-shaped blank that is ready for rolling. This sequence is shown schematically in Fig. 25. Although a wide variety of rings can be rolled from blanks made by this simple process, alternative methods must be used when large ring-height-to-wall ratios are required and for severely contoured rings with limited rolling reduction (and little diameter growth).

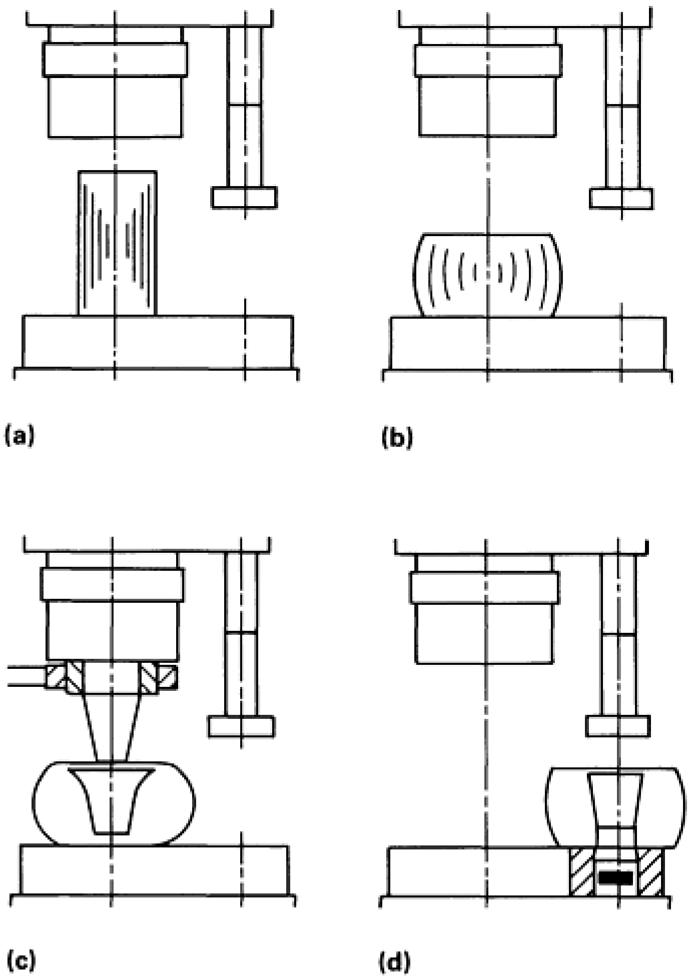

Fig. 25 Schematic showing blank preparation using open dies and a two-station press. (a) Billet centered on press table. (b) Billet upset. (c) Upset blank is indented. (d) Blank is pierced and ready for removal.

With thin-wall sleeves, and even with square cross section rings whose mass is very small in relation to the physical dimensions of the mill, the diameter of the indenting tool may approach that of the upset preform. The indentor then behaves less like a prepiercing tool and more like a flat die. The result is a grossly distorted and unacceptable blank (Fig. 26) with a height less than that of the rolled ring.

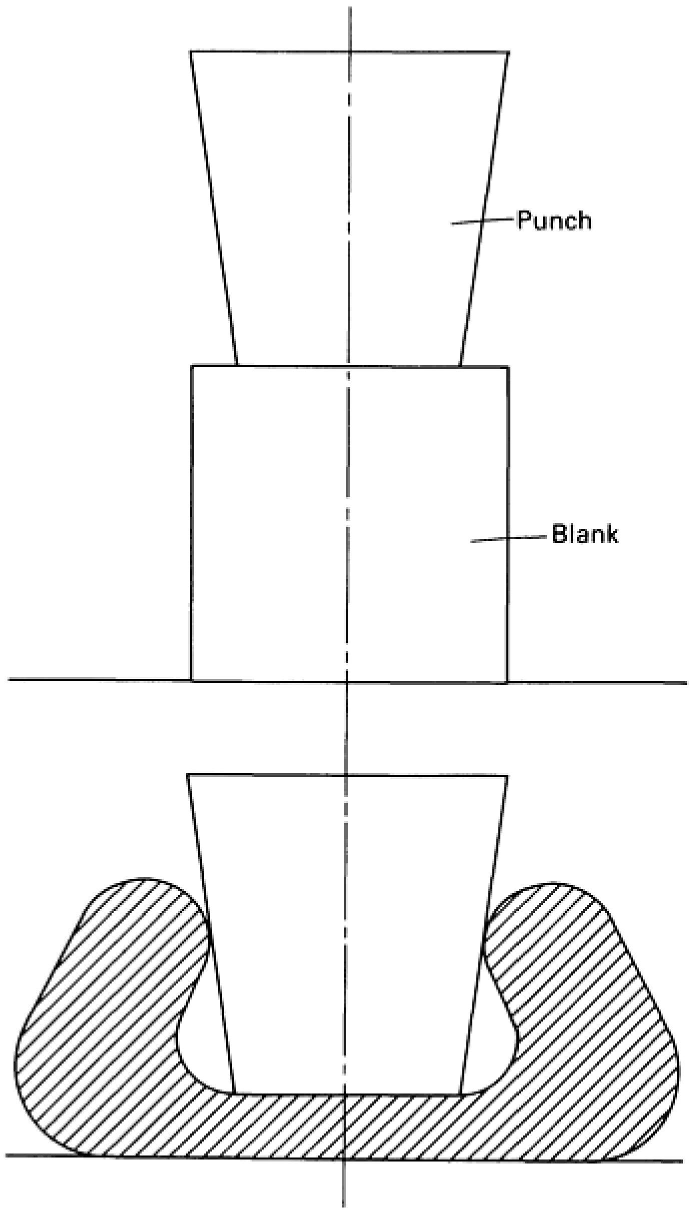

Fig. 26 Blank unacceptably distorted by punch/blank diameter relationship. When the punch diameter is too large in relation to block diameter, it deforms the blank rather than indenting it.

This problem can be overcome either by employing well-tried but slow open-die forging techniques or by indenting the workpiece in a container. The former requires pressing with a loose small-diameter punch. The blank with punch entrapped is then turned onto its outside diameter and forged incrementally so that the inside diameter expands and the height increases.

The use of this method when the press forms part of an integrated line severely curtails output. By using a larger-capacity press and container dies, excellent blanks can be produced at a rate sufficient to maintain full ring mill production. For example, a mill that is rolling rings weighing up to 2000 kg (4400 lb) and using open-die forming blanks from a 15.7 MN (1760 tonf) hydraulic press would require the service of at least a 24.5 MN (2750 tonf) press using container dies to maintain full production on this type of ring.

Figure 27 shows schematically the sequence of operations on a two-station press using a lower container die located in a bolster. A fundamental requirement here is the ease in ejecting the workpiece from the die, using a hydraulic cylinder housed in the lower portion of the press frame. Figure 28 illustrates the use of a two-station press with a shaped upper die to produce blanks for rolling into weld-neck flanges.

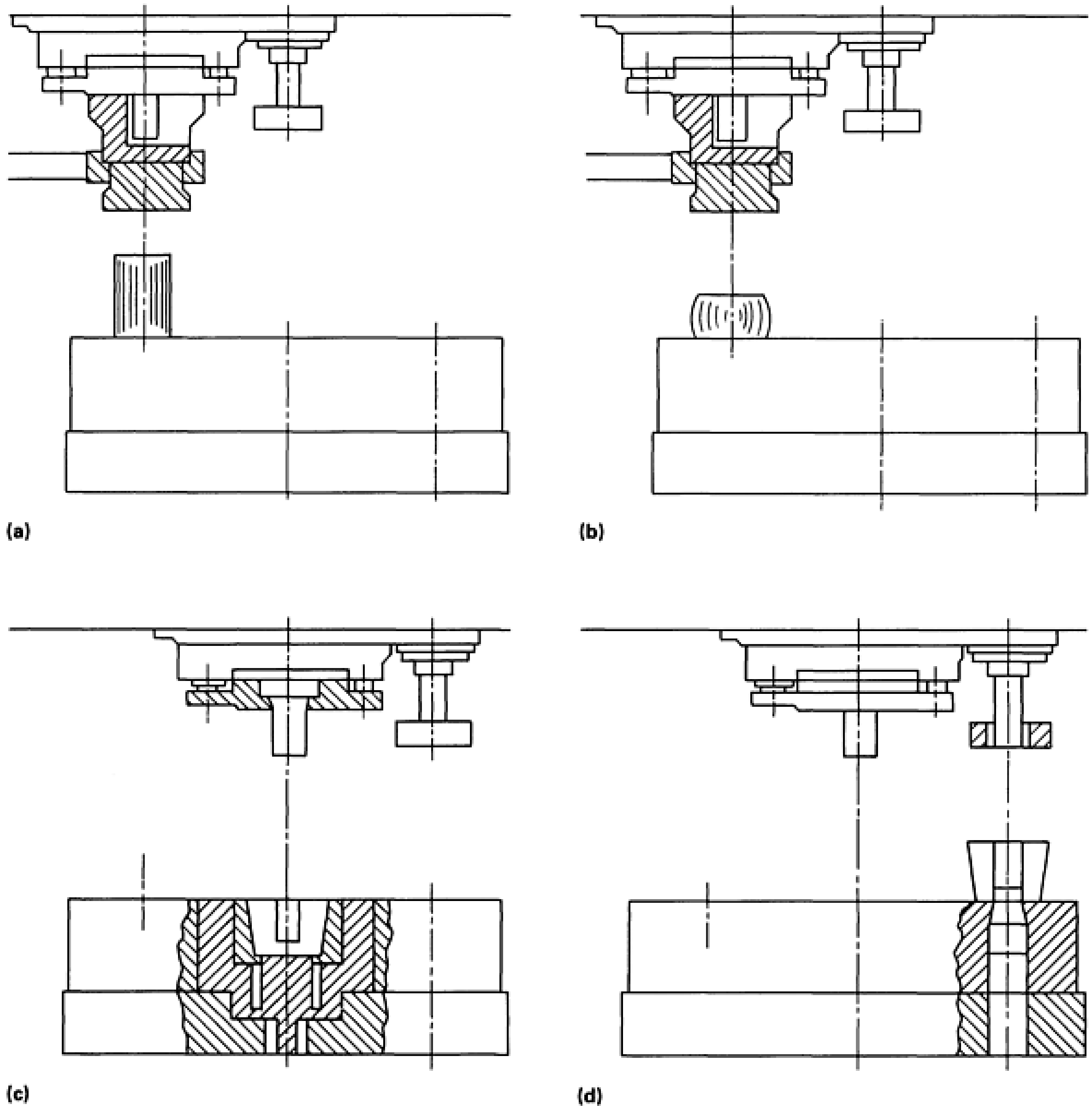

Fig. 27 Manufacture of blanks in a lower container die. (a) Billet centered on press table. (b) Billet is upset. (c) Blank is indented and formed by backward extrusion. (d) Blank is pierced and ready for removal.

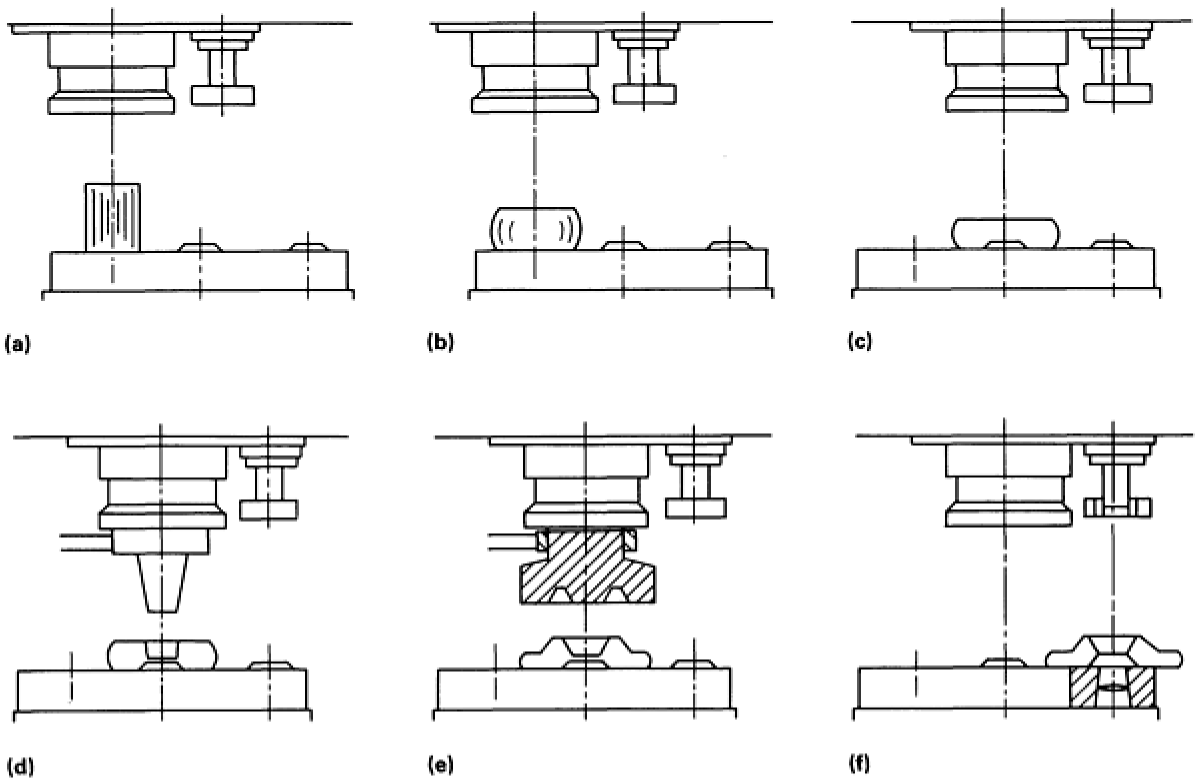

Fig. 28 Manufacture of blanks in a two-station press using profile tools. (a) Billet centered on press table. (b) Billet is pre-upset. (c) Billet is upset. (d), (e), and (f) Blank is indented, formed, and pierced, respectively.

On smaller, high-speed ring mills, a three-station blanking press with an integral workpiece transfer system is required to maintain an adequate supply of blanks. These presses can produce open-die blanks, container-die blanks, and split-die contoured blanks.

Typically, the less demanding operations of initial breakdown and final piercing, which are carried out off-press-center, are done simultaneously on two workpieces. The higher-load main forming operation (indenting, container-die forging, and so on) is done at press center in isolation. Therefore, a block of raw material is loaded on alternate strokes of the press.

A 9.8 MN (400 tonf) press, serving a 390 kN (44 tonf) radial/310 kN (35 tonf) axial mill can produce up to 250 pieces per hour in this manner. On very small rings, all three stations of a three-station press can be used simultaneously, producing one blank per press stroke. This particular press and mill combination can then produce around 300 rings per hour.

Using a modular bottom bolster and top tool holder, tooling can be set up outside the press, and tool sets exchanged in approximately 20 min, thus maximizing the production time available. A wide range of complexly shaped blanks, which may be necessary for rolling rings with complex contours, can be produced using split dies by combining various top and bottom tools at the center station of the press.

Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Ancillary Operations

Ring rolling mills must be supported by an array of ancillary equipment. Most important is a means of forming blanks--usually hammers or presses.

Cutting of Billets. Some method of accurately cutting raw material to the required input weight is necessary. Cold and hot shearing are employed; the latter is usually used when an integrated production line is involved. Circular saws, which are sometimes carbide tipped, tend to predominate. Band saws are often used, particularly on stainless steels, and abrasive saws are used on titanium alloys and superalloys. Blocks for railroad wheels are often cut from ingots on multiple-tool special-purpose lathes, flame cut or flame nicked, and then fractured on a large press.

Heating. Reheating of cut blocks is usually done in box or rotary fossil-fuel furnaces. Induction heating is sometimes used for smaller stock and has the advantage of minimal scale formation. Various methods of hot block descaling are employed, both mechanical (for example, flailing cable, chains, or rotating brushes) and high-pressure (14 to 90 MPa, or 2 to 13 ksi) water spray, which is particularly effective.

Other Operations. Some shops employ devices for sizing rings immediately after rolling. These can be straightforward hydraulic presses, in which the ring is forced through a circular sizing die, or complex more expanders, which stretch a ring by applying force to multiple, appropriately shaped segments acting on the inside diameter of a ring.

Appropriate heat treatment facilities are necessary, whether to render the product more easily machinable or to achieve the mechanical properties specified for the end product. Shotblasting is often used to remove scale formed during hot working. The resulting surface is easier to inspect and to machine.

Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Blanking Tools and Work Rolls

Although hot-work tool steels such as H11 and H13 are frequently used for blanking and rolling tools, especially when working heat-resistant alloys, less expensive alloy steels such as AISI 4140 and AISI 4340 find wide application on less demanding work materials. Various types of blanking and rolling tools are shown in Fig. 29.

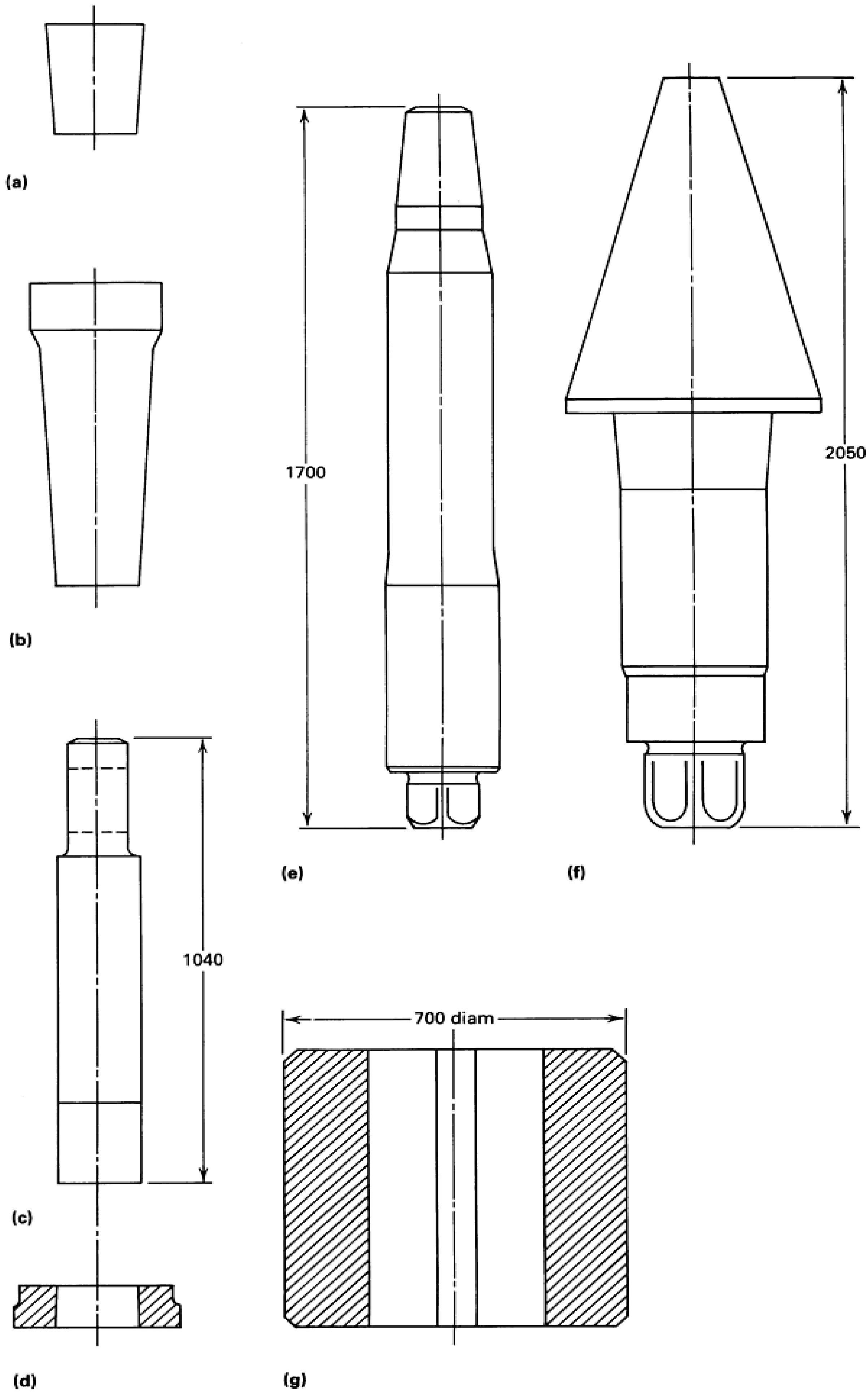
(a)
(b)
1040
(c)
(d)
1700
(e)
2050
(f)
700 diam
(g)

Fig. 29 Blanking and rolling tools used in ring rolling. (a) Tapered indenting punch. (b) Tapered, swing arm mounted indentor. (c) and (d) Piercing punch and support ring for a blanking press. (e) Typical mandrel for a mid-size mill. (f) Axial roll. (g) Main roll. See text for discussion of tool materials. Dimensions given in millimeters (1 in. = 25.4 mm).

When blanks are open-die forged on hammers or presses, simple tapered indenting punches (Fig. 29a) are driven into the preform. The preform is then turned over, allowing the punch to fall out, and the punch is then used to cut out the slug remaining from indenting, thus forming the doughnut-shaped blank.

A wide range of punch diameters and lengths are typically available to accommodate the many different blank dimensions required. With several punches in each size and each cooled in water immediately after use, AISI 4140 or AISI 4340 are quite adequate in terms of life and cost. If special-purpose ring blank presses are used, tool duplication is usually not feasible, and short periods of cooling between each blanking operation may not be sufficient to allow the use of the regular alloy steels above.

Figure 29(b) shows a 3° tapered, swing arm mounted indentor typically used in blanking presses. A low-alloy steel such as ASM 6F2 (see the article "Dies and Die Materials for Hot Forging" in this Volume) at 38 to 43 HRC (350 to 400 HB) may be necessary to withstand the higher tool working temperature.

Figures 29(c) and (d) show the type of piercing punch and support ring that would be used on a two- or three-station blanking press to shear out the slug created by indenting. Almost invariably, the punch is either solid H13 or has an exchangeable tip in H13 heat treated to about 49 HRC (460 HB). The support ring is also usually made of H13. Typically, the radial clearance between the punch and the support ring is of the order of 2 to 5 mm (0.008 to 0.2 in.) for punches 125 to 220 mm (5 to 8.7 in.) in diameter. On high-speed blanking presses, the indenting punch in the center station is so heavily used that even when it is made of H13, continuous internal water cooling is necessary, along with inter-cycle external water-spray cooling.

Container dies used on a slower-speed, larger press (for example, 24.5 MN, or 2750 tonf, capacity) can often be made from AISI 4140 or 4340 if the duty cycle is long enough and inter-cycle water cooling is adequate. Inserts fabricated from H13 tool steel may be necessary on smaller blanks with shorter cycle times.

On presses where no means are available for stripping blanks off (indenting) punches, these punches typically have a taper of 3° per side. Powdered coal or waterborne graphite lubricants are usually employed to ensure release of the punch from the blank. Where stripping mechanisms (depending on the type) are available to eject the blank, release tapers of about 1° can be employed for both punches and containers.

The consumable tools on radial-axial ring rolling mills are principally the mandrel and, to a lesser extent, the axial (conical) rolls and the main roll. Depending on the mill design and force capability, mandrels may be as small as 30 mm (1.2 in.) in diameter (for a 295 kN, or 33 tonf, mill) and as large as 450 mm (18 in.) in diameter for a mill with a radial capacity of 5 MN (550 tonf).

Figure 29(e) shows a typical 165 mm (65 in.) diam mandrel for a midsize mill with 980 kN (110 tonf) radial capacity. Such mandrels are commonly fabricated from ASM 6F3 at 370 to 410 HB. Again, AISI 4340, at 300 to 350 HB, with adequate water-spray cooling, can be used with good results (that is, producing up to 3000 rings before failing through heat check initiated fatigue). Production of 1500 to 2000 rings can be expected from a 70 mm (2.75 in.) H13 tool steel mandrel used on a high-speed multiple-mandrel mill of 390 kN (44 tonf) radial capacity.

Axial rolls (Fig. 29f) on older machines typically had a 45° included angle, along with relatively short working lengths. This severely limited the ring wall thickness they could cover and led to rapid wear of the conical surfaces. With the resultant need to change axial rolls frequently, two part designs were often employed with the working cone bolted to a semipermanently installed roll shaft.

Modern machines have 30 to 40° included-angle axial cones and longer working lengths. Wear is spread over the greater length, and roll changes are required less frequently (for example, after 600 to more than 1000 h of use).

Axial rolls are usually one-piece designs; AISI 4140, ASM 6F2, and ASM 6F3 are typical materials. These rolls are usually welded and reworked to original dimensions many times before being discarded. Extended service life can be obtained by using a cobalt-base hardfacing alloy, approximately 1.5 mm (0.06 in.) thick, on the working surfaces on these axial cones.

Figure 29(g) shows a typical AISI 4140 main roll for a 980 kN (110 tonf) radial capacity ring mill. Such rolls tend to wear most heavily at the point where the bottom corner of the ring is contacted. To prolong use between roll changes, the roll and shaft assembly are periodically adjusted downward from maximum height setting gradually toward minimum, typically over a full range of 30 mm (1.2 in.).

In addition to ensuring maximum roll life, they are initially made to an acceptably larger-than-nominal diameter and are recut at intervals until the minimum mandrel/main roll gap is unacceptable. At this point, if economical, weld repair and hardfacing can be employed.

Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Combined Ring Rolling and Closed-Die Forging

The combination of ring rolling machines and closed-die hammers or presses in integrated manufacturing cells yields a degree of flexibility and economic benefit not achievable by either process separately. With sufficient ingenuity applied to equipment layout and handling devices, a number of process sequences can be used; the final component forming occurs either on the ring mill or the closed-die unit, depending on the particular component shape and size.

Figure 30 compares the equipment required and the economics of producing a starter ring by closed-die forging and ring rolling combined (Fig. 30a) and by closed-die forging only (Fig. 30b). Similarly, Fig. 31 shows the comparison for weld-neck flange forming.

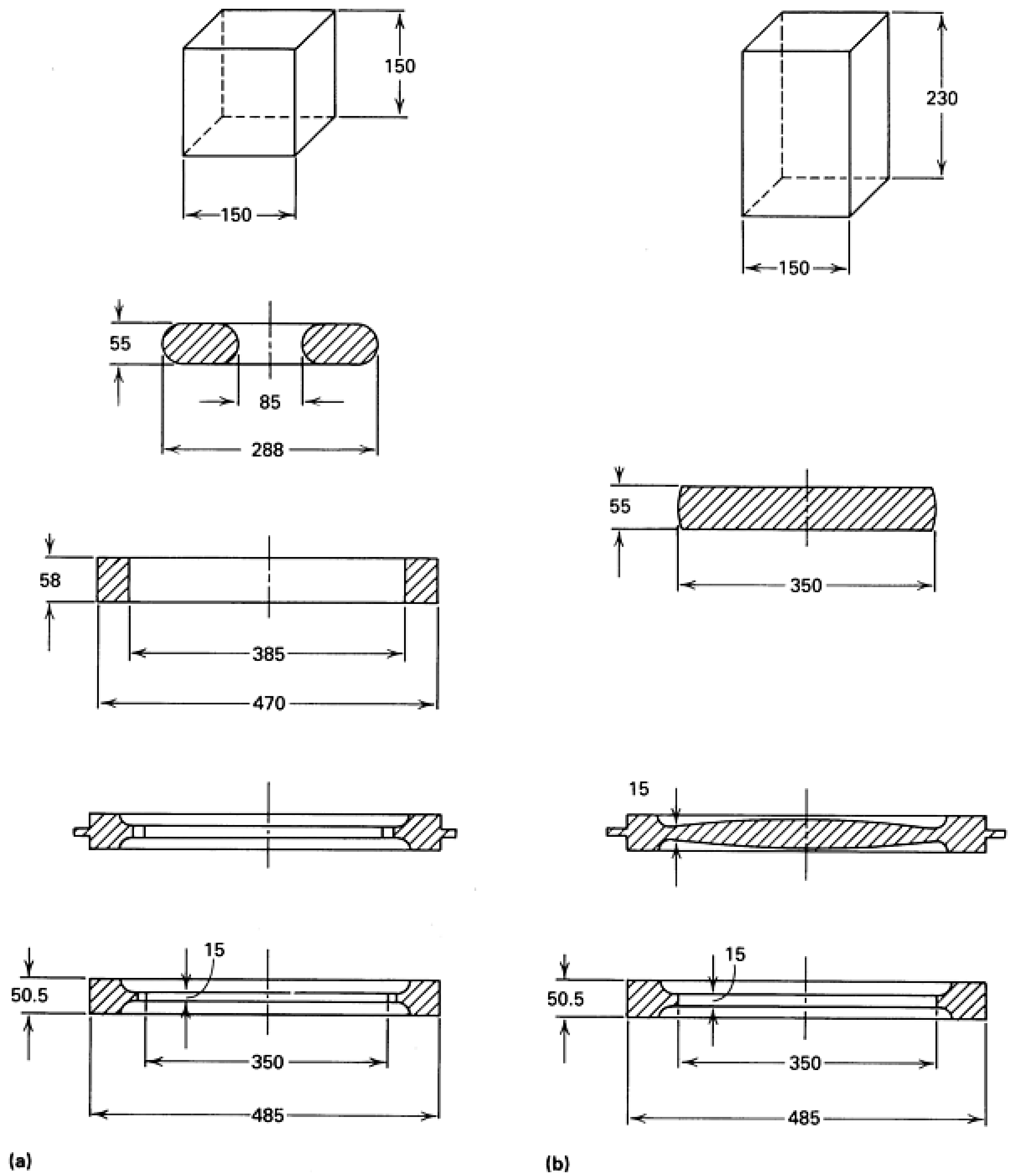

Fig. 30 Comparison of closed-die forging plus ring rolling (a) and closed-die forging only (b) for the production of a starter ring. (a) Top to bottom: billet, pierced blank, rolled ring, die forging, and finished part. (b) Top to bottom: billet, upset disk, die forging, and finished part. Although an additional step is needed when ring rolling is used, the production rate increases from 70 to 110 pieces per hour, and a material saving of 38% per piece is realized. Dimensions given in millimeters (1 in. = 25.4 mm).

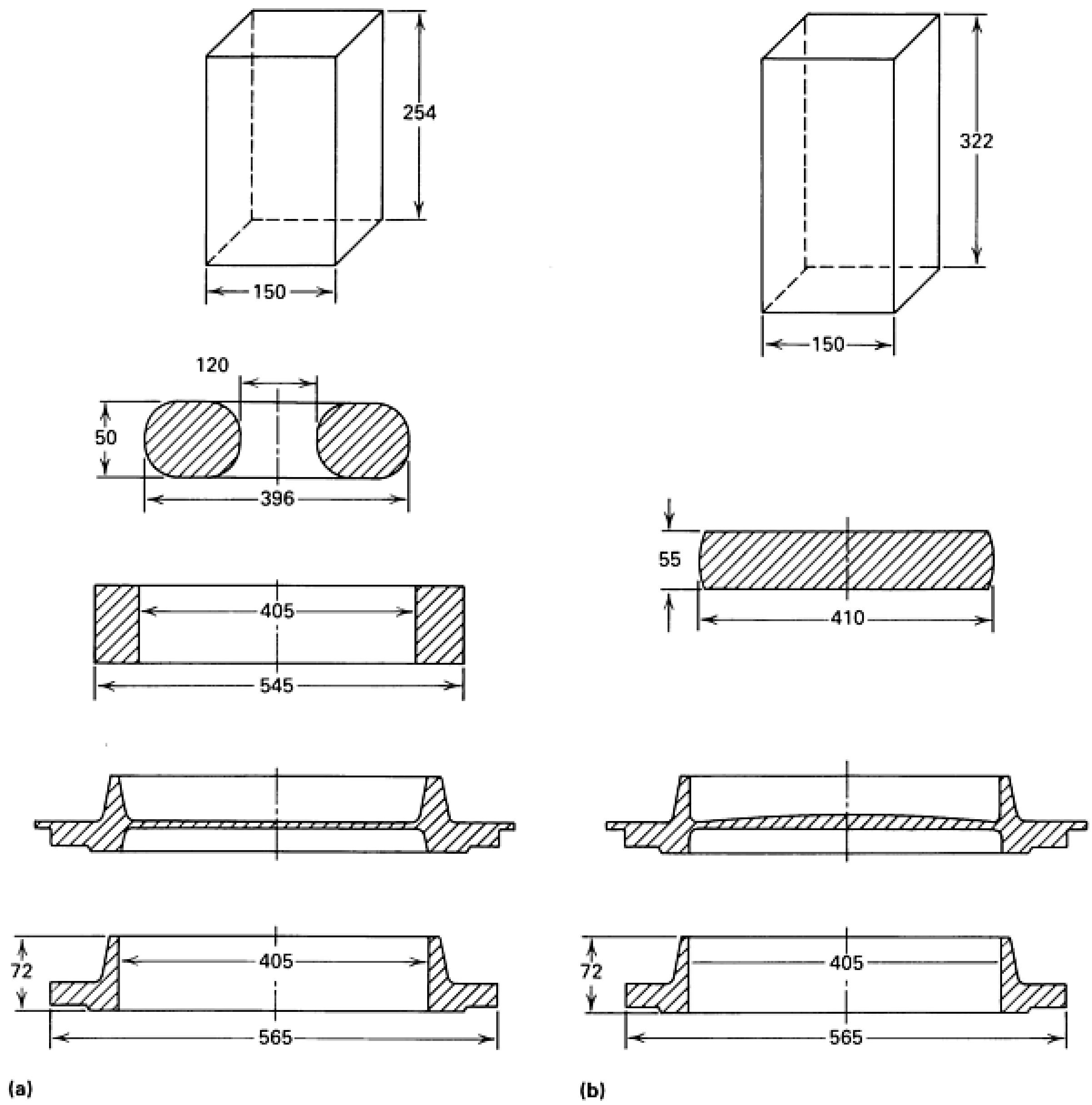

Fig. 31 Comparison of closed-die forging plus ring rolling (a) and closed-die forging only (b) for the production of a weld-neck flange. (a) Top to bottom: billet, pierced blank, prerolled ring, finish-forged part, and trimmed part. (b) Top to bottom: billet, upset disk, finished forging, and trimmed part. Production rate doubled and a material savings of 21% per piece was realized when the ring rolling process was used. Dimensions given in millimeters (1 in. = 25.4 mm).

From these examples, it can be seen that for a given production program new installations can be equipped with appreciably smaller principal forming equipment. Furthermore, despite the additional number of pieces of equipment, the total investment is usually lower than if a 100% closed-die approach were selected. Alternatively, the addition of a ring roller to an existing closed-die plant can extend the production range to substantially larger pieces.

By avoiding large inside flash formation, material input can be reduced by 15 to 35% (Fig. 30 and 31). Production rates 10 to 40% higher than closed-die forging can be achieved through simultaneous operations performed in more, but individually less demanding, steps. The much-reduced inside flash means lower stock-heating costs, and approximately 50% less deformation energy is required at the closed-die stage. The number of operators required usually remains unchanged.

Bevel Gear Manufacture. Figure 32 illustrates five possible methods of manufacturing large bevel gears. Table 3 lists the start/finish material weight relationships for these methods. The capital investment and material yield benefits of ring rolling are obvious from these data. Many bevel gears are therefore manufactured by the preform press plus ring mill approach.

Table 3 Material required for the production of bevel gear blanks by various methods

See Fig. 32 for steps used in each method.

Method	**Required billet weight**		**Weight of unmachined ring**		Weight of scrap	
	kg	**lb**	**kg**	**lb**	**kg**	lb
A (Drop hammer)	44.7	98.5	38.5	84.9	6.2	**13.7**
B (Forging press)	45.9	101.2	38.5	84.9	7.4	**16.3**
C (Drop hammer and ring roll)	42.8	94.4	38.5	84.9	4.3	**9.5**
D (Forging press and ring roll)	43.1	95.0	38.5	84.9	4.6	**10.1**

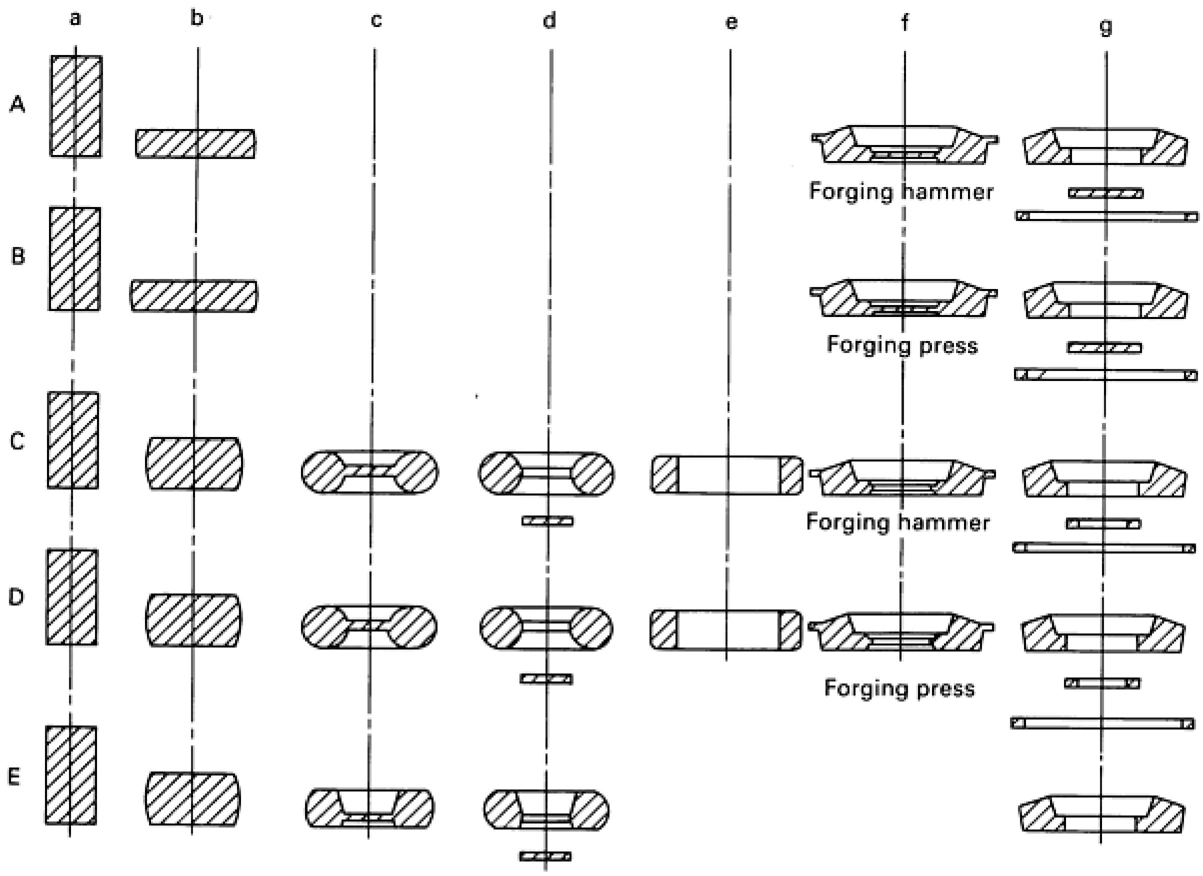

Fig. 32 Five methods of producing bevel gear blanks. A, drop hammer forging; B, press forging; C, ring rolling and hammer forging; D, ring rolling and press forging; E, ring rolling and piercing. Production steps: a, cut billet; b, upset billet; c, ring preform; d, ring blank; e, rolled blank; f, die-forged bevel gear; g, finished,

unmachined bevel gear. See Table 3 for amount of material used in various processes.

However, some bevel gears of complex cross section cannot be easily rolled to near-net shape without the aid of extensive blank preforming on heavier, more expensive presses. Even when appropriate material distribution is achieved in the preform, inside flash diameter may be large so that the resulting hole allows the blank to fit over a deeply contoured rolling mandrel. Material loss can therefore approach that in closed-die forging. The advantage of completing the process on a ring mill is reduced or lost completely.

An alternative method of producing these larger-diameter heavier bevel gears, with the required cross-sectional complexity, is to relegate the ring mill to a preforming role and to finish in closed dies. Equipment size and expenditure are still less than if closed-die forging only were used.

Figure 33 shows schematically a forging line that originally consisted of a 71 MN (8000 tonf) forging press and a 3.5 MN (400 tonf) trimming press. Bevel gears to 440 mm (17.3 in.) in diameter and 50 kg (110 lb) in weight were manufactured. By introducing an 11 MN (1200 tonf) ring blank preforming press and two 390 kN (44 tonf) preforming ring rollers (Fig. 34) plus manipulator, the maximum diameter was extended to 500 mm (20 in.) and maximum weight to 80 kg (175 lb).

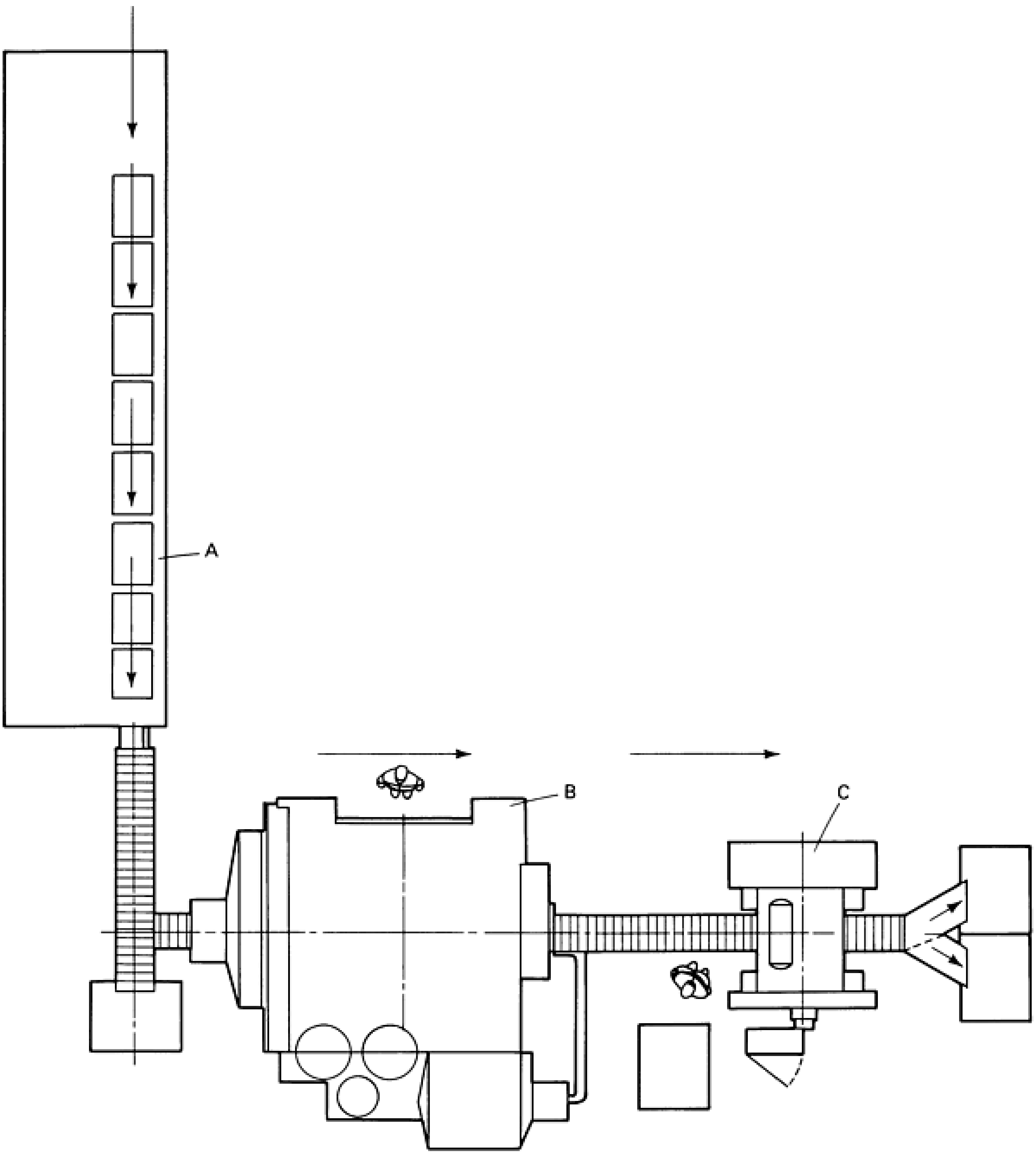

Fig. 33 Schematic of a setup for forging bevel gears. A, induction heater; B, 78 MN (8800 tonf) forging press; C, 3.9 MN (440 tonf) trimming press. See also Fig. 34.

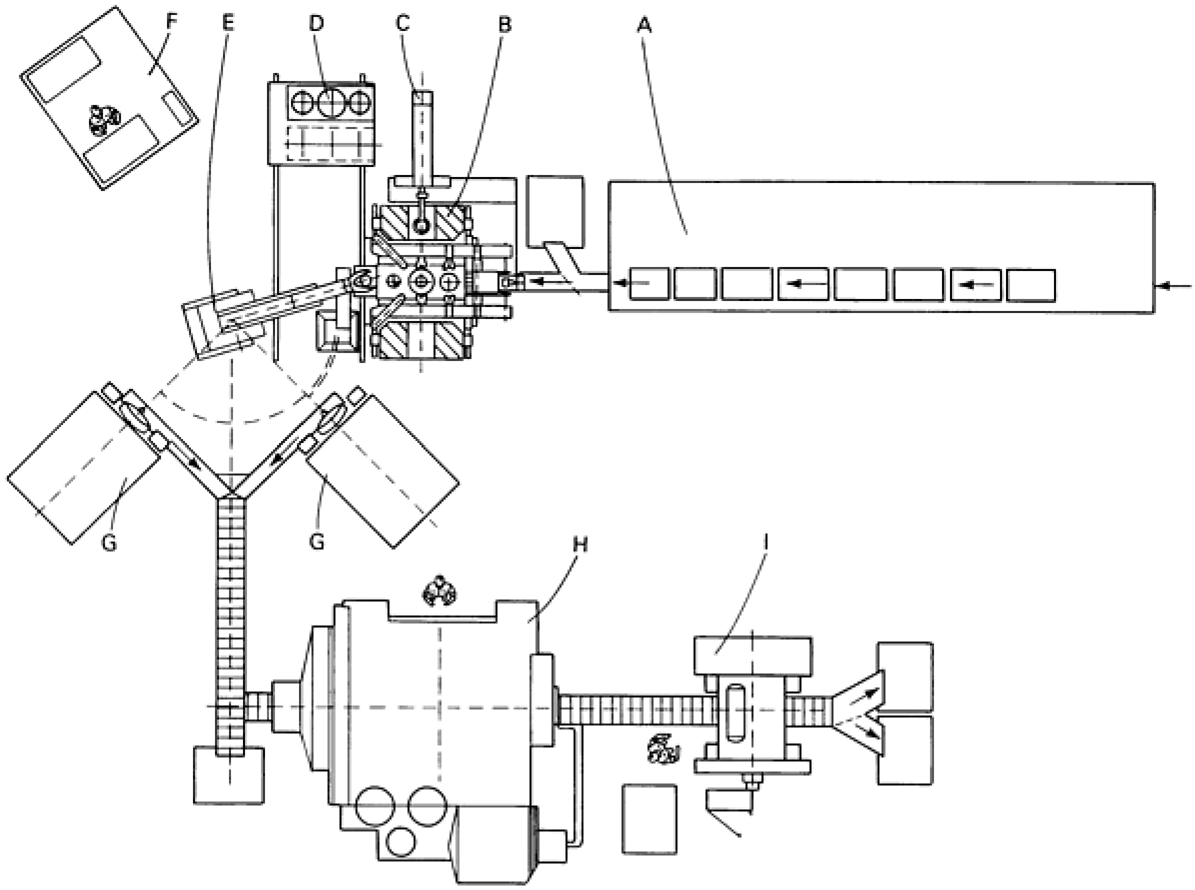

Fig. 34 Extension of die-forging plant for bevel gears shown in Fig. 33 to include a radial ring mill. A, induction heater; B, 12.2 MN (1375 tonf) preforming press; C, die lubrication unit; D, die change carriage; E, manipulator; F, control cabin; G, 390 kN (44 tonf) preforming ring mill; H, 78 MN (8800 tonf) forging press; I, 3.9 MN (440 tonf) trimming press.

Output was increased from 10 to 24 Mg (11 to 26.5 tons) per hour. In addition to the increased material yield on comparable forgings (savings up to 20%), die life was significantly improved because of the reduced material flow needed with a ring rolled preform. In addition, the circumferential grain flow from ring rolling, combined with the radial-axial flow in closed dies, produced a metallurgically improved component.

Ring Rolling

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Rolled Ring Tolerances and Machining Allowances

There are numerous sources of dimensional variation in the ring rolling process. The volume of material rolled is affected by variation in the cut weight of the billet, scale loss fluctuation due to differing heating conditions, and variation in center-web thickness removed at the blanking stage.

Beyond this, dimensions are affected by rolling temperature. Machine deflection, the accuracy of the measuring instrument, ring circularity, distortion in subsequent heat treatment, surface flaws, and cross-sectional shape inaccuracies also must be taken into account.

The degree of precision attainable using the ring rolling process depends on the design characteristics of particular mill types and varies quite widely throughout the ring rolling industry. With modern computer-controlled ring mills, switch-off accuracies in the range of 0.1 mm (0.004 in.) are achievable; this makes machine controllability a minor consideration and emphasizes the contributions of other factors to dimensional variation. However, because there are many machines that rely on operator skill or solely on weight control (mechanical table mills) for dimensional control, it is quite common for the products of these machines to be sized by pressing them through or over a die or by expanding deliberately undersized rings on a segmental expanding machine.

An increasingly common feature of computer-controlled ring mills is the option to distribute material to best advantage. A decision can be made, even during rolling, to place excess material on the inside or outside diameters or on height. Perhaps the most useful version of this feature is the ability to roll to mean ring diameter, with excess material being equally distributed to the inside and outside diameters regardless of the actual material input volume.

Persistent market pressure for near-net shape rings, wider application of statistical process control techniques, and the use of computer numerical controlled ring rolling machines has generated steadily increasing dimensional precision of rolled rings. Information on allowances and tolerances (Fig. 35 and 36) should therefore be taken only as a generalized starting point, and it should be understood that the ability of individual manufacturers of rolled rings to meet or improve on the tabulated allowances and tolerances varies greatly.

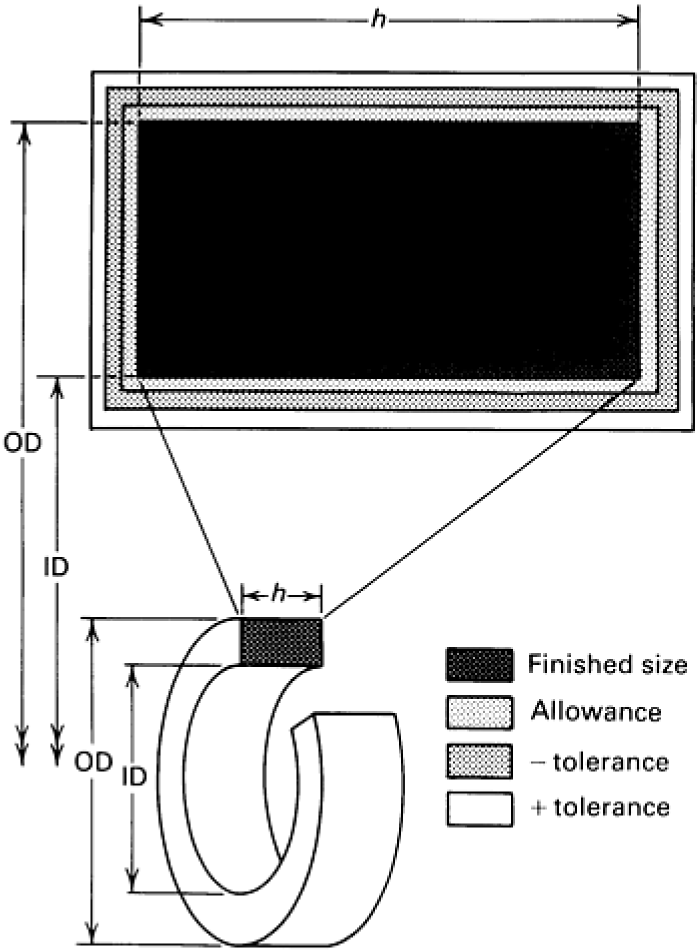
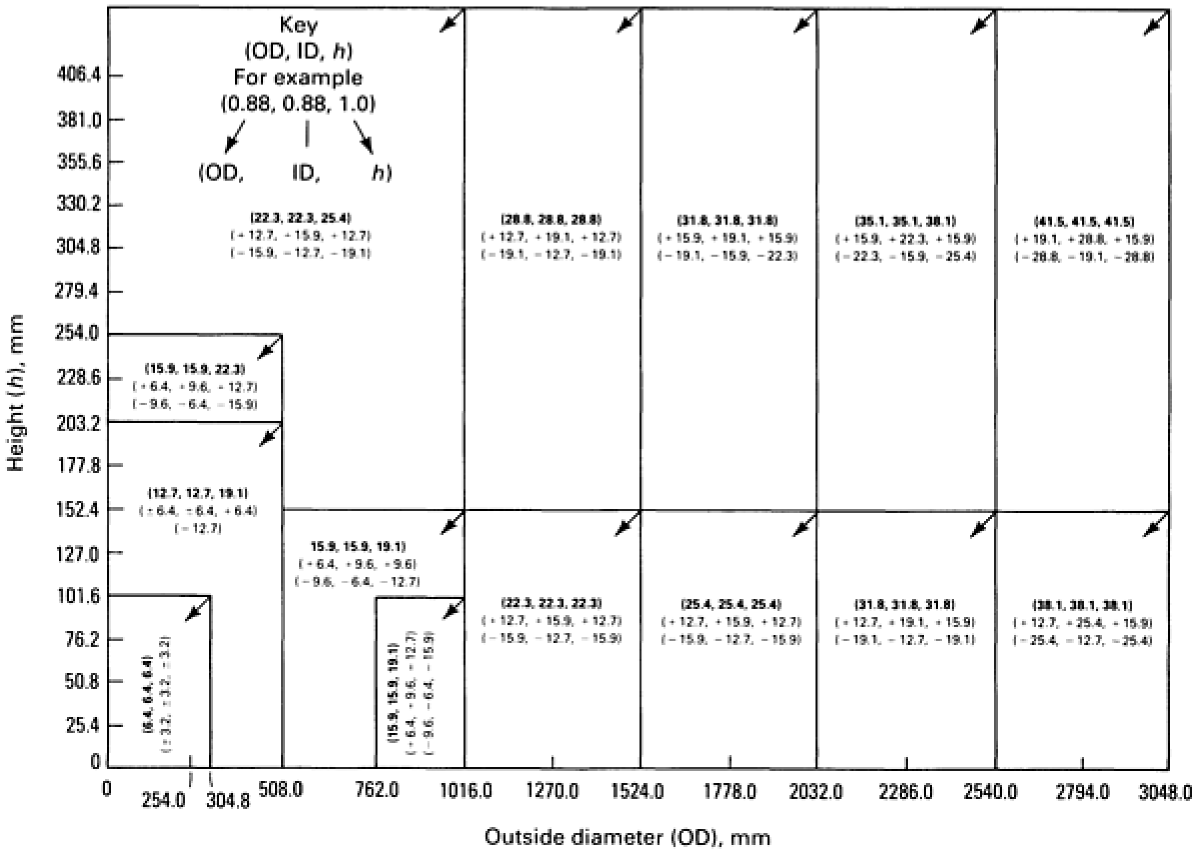
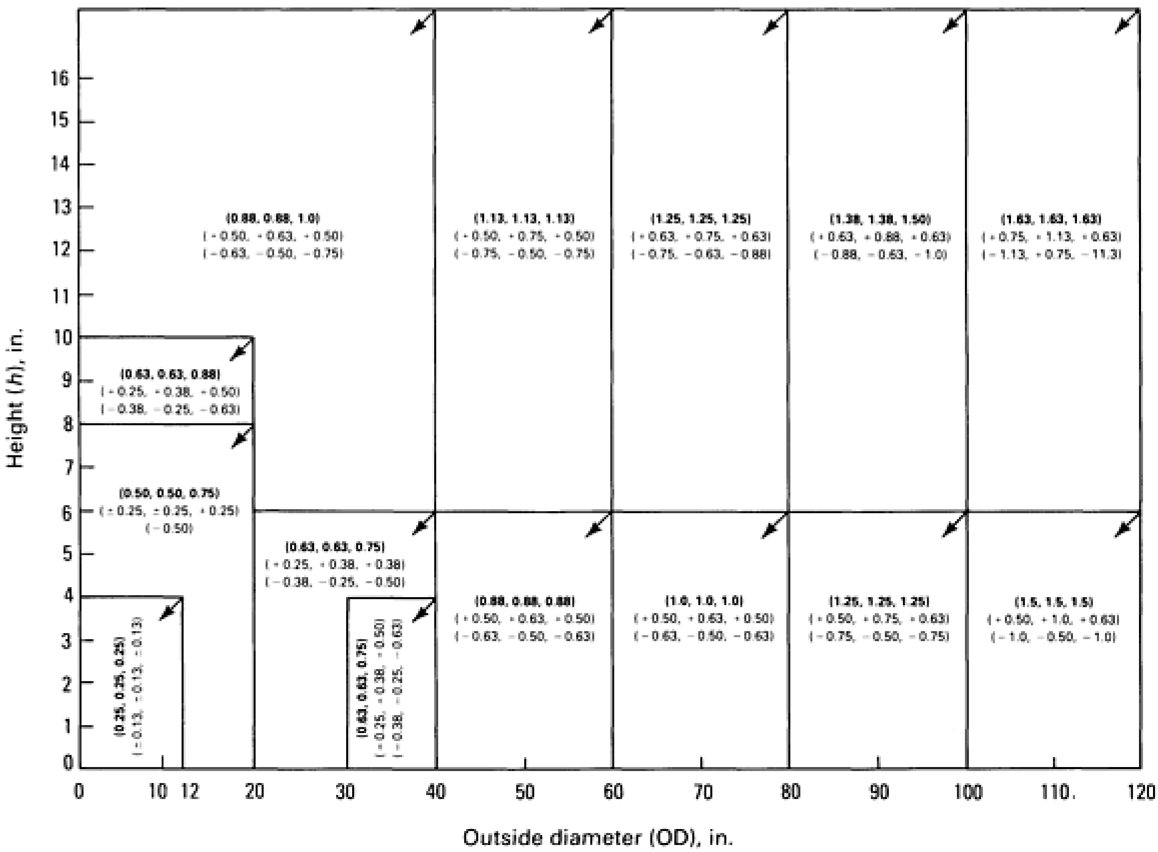


Fig. 35 Allowances and tolerances for seamless rolled rings. Allowance is the amount of stock added to ensure cleanup on any surface that requires subsequent machining. Tolerance is normal dimensional variation limits. See also Fig. 36.



(a)



(b)

Fig. 36 Allowance and tolerance chart for as-rolled carbon, alloy, and stainless steel seamless rings. Allowances

are given in boldface type; tolerances are in regular type. Shaded areas represent allowances and tolerances for sized rings. (a) Chart in millimeters. (b) Chart in inches.

To ensure cleanup of a ring at machining, an envelope is added to the finished (machined) ring dimensions. This envelope, determined by experience, together with a +/- tolerance, is intended to account for the above-mentioned surface condition, cross-sectional inaccuracy, and dimensional variation factors. Figure 35 illustrates the relationship between this machining allowance and dimensional tolerance.

These data (Fig. 35 and 36) are available in Ref 8. Based on historical, averaged industry data, Fig. 36 shows typical machining allowances and as-rolled ring dimensional tolerances for carbon, alloy, and stainless steel rings. Similar data for aluminum, titanium, heat-resistant alloys, brass, and copper are also given in Ref 8.

Reference cited in this section

8. *Facts and Guideline Allowances and Tolerances for Seamless Rolled Rings,* Forging Industry Association, 1979

Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

Alternative Processes

Relatively small rings can be forged in closed dies. Maximum diameter is limited by the distance between hammer legs, or between press columns, and the available forming energy. Material waste is relatively high, and grain flow is radial unless a preform is ring rolled. Larger rings can be open-die forged using a saddle arrangement (Fig. 37). This method is slow, labor intensive, and tends to produce polygonal rather than smooth-faced rings.

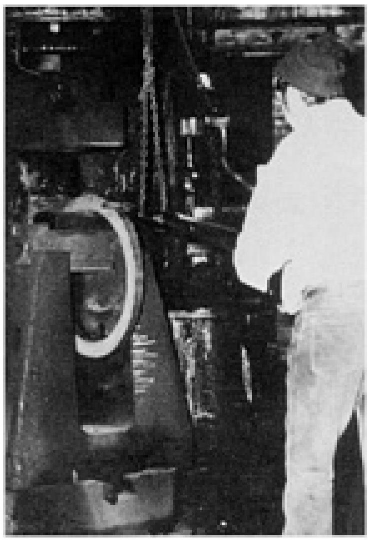


Fig. 37 Open-die forging of a ring using a saddle.

If service conditions are not too demanding, rings of a wide range of dimensions can be gas-cut from plate. Contoured rings are largely impractical to produce by this approach; much material is wasted, and the longitudinal flow from the plate produces variation in mechanical properties around and in the direction of the circumference.

Rings of a wide range of diameters and cross sections can be made by the three-roll forming of bar or plate, followed by welding of the joint. Subsequent cold or warm rolling is sometimes used to form complex thin-wall cross sections. Special-purpose rolling machines have been developed for this purpose.

Small rings up to approximately 330 mm (13 in.) in diameter, especially bearing rings, are sometimes machined from seamless tube. Again, the axial grain flow of the tube may be unacceptable, and maximum wall thickness is quite limited.

Centrifugal casting is sometimes used to produce circular components, and it has its own peculiar advantages and disadvantages. Nonrotating gas-turbine parts are routinely made in heat-resistant materials by this method.

Ring Rolling

C.R. Keeton, Ajax Rolled Ring Company

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Rotary Swaging of Bars and Tubes

Revised by the ASM Committee on Rotary Swaging*

Introduction

ROTARY SWAGING is a process for reducing the cross-sectional area or otherwise changing the shape of bars, tubes, or wires by repeated radial blows with two or more dies. The work is elongated as the cross-sectional area is reduced. The workpiece (starting blank) is usually round, square, or otherwise symmetrical in cross section, although other forms, such as rectangles, can be swaged.

Most swaged workpieces are round, the simplest being formed by reduction in diameter. However, swaging can also produce straight and compound tapers, can produce contours on the inside diameter of tubing, and can change round to square or other shapes.

Note

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Kerr, Fenn Manufacturing Company; Clarence J. Miller, Abbey Etna Machine Company; Robert Wattinger, Manco/Ameco Automation

Rotary Swaging of Bars and Tubes

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Applicability

Swaging has been used to reduce tubes up to 355 mm (14 in.) in initial diameter and bars up to 100 mm (4 in.) in initial diameter. Hardness, tensile strength, and reduction in area of the work metal have the most significant effect on swageability. Type and homogeneity of microstructure also influence the ease of swaging and the degree to which a metal can be swaged. Maximum reduction in area for various metals is given in Table 1.

Table 1 Maximum reductions in area obtainable by cold swaging for several alloy systems

Alloy	Maximum reduction in area, %
Plain carbon steels[(a)]	
Up to 1020	**70**
1020-1050	**50**
1050-1095	**40**
Alloy steels[(b)]	
0.20% C	**50**
0.40% C	**40**
0.60% C	**20**
High-speed tool steels	
All grades	**20**
Stainless steels[(c)]	
AISI 300 series	**50**
AISI 400 series	

Low-carbon	**40**
High-carbon	**10**
Aluminum alloys	
1100-0	**70**
2024-0	**20**
3003-0	**70**
5050-0	**70**
5052-0	**70**
6061-0	**70**
7075-0	**15**
Other alloy systems	
Copper alloys[(c)]	**60-70**
A-286	**60**
Nb-25Zr	**60-70**
Alloy X-750	**60**
Kovar (Fe-29Ni-17Co-0.2Mn)	**80**
Vicalloy (Fe-52Co-10V)	**50**

(a) Low-manganese steels, spheroidize annealed.

(b) Spheroidize annealed.

(c) Annealed

Work Metals. Of the plain carbon steels, those with a carbon content of 0.20% or less are the most swageable. These grades can be reduced up to 70% in cross-sectional area by swaging. As carbon content or alloy content is increased, swageability is decreased. Alloying elements such as manganese, nickel, chromium, and tungsten increase work metal strength and therefore decrease the ability of the metal to flow. Free-machining additives such as sulfur, lead, and phosphorus, cause discontinuities in structure that result in splitting or crumbling of the work metal during swaging.

In the cold swaging of steel (at room temperature), maximum swageability is obtained when the microstructure is in the spheroidized condition. Pearlitic, annealed microstructures are less swageable than spheroidized microstructures, depending on the fineness of the pearlite and on the tensile strength and hardness of the steel. Fine pearlitic microstructures, such as those found in patented music wire and spring wire, can be swaged up to 30 to 40% reduction in area.

Figure 1 shows the relationship between hardness and carbon content for pearlitic and spheroidized microstructures and also shows three zones of swageability, indicating that a maximum hardness of 85 HRB is preferred for carbon steels and that swaging is impractical when hardness exceeds 102 HRB. Figure 2 shows the influence of cold reduction on the tensile and yield strengths of several metals.

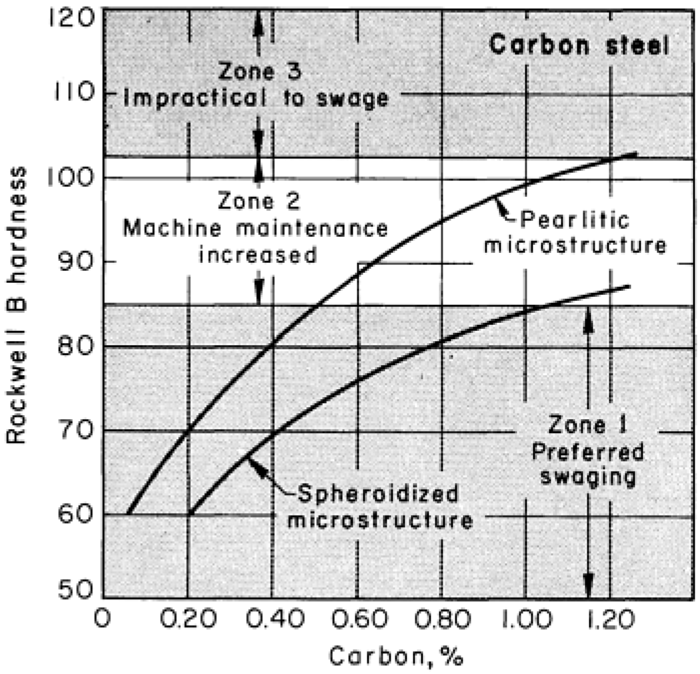

Fig. 1 Swageability of carbon steel as a function of microstructure, hardness, and carbon content.

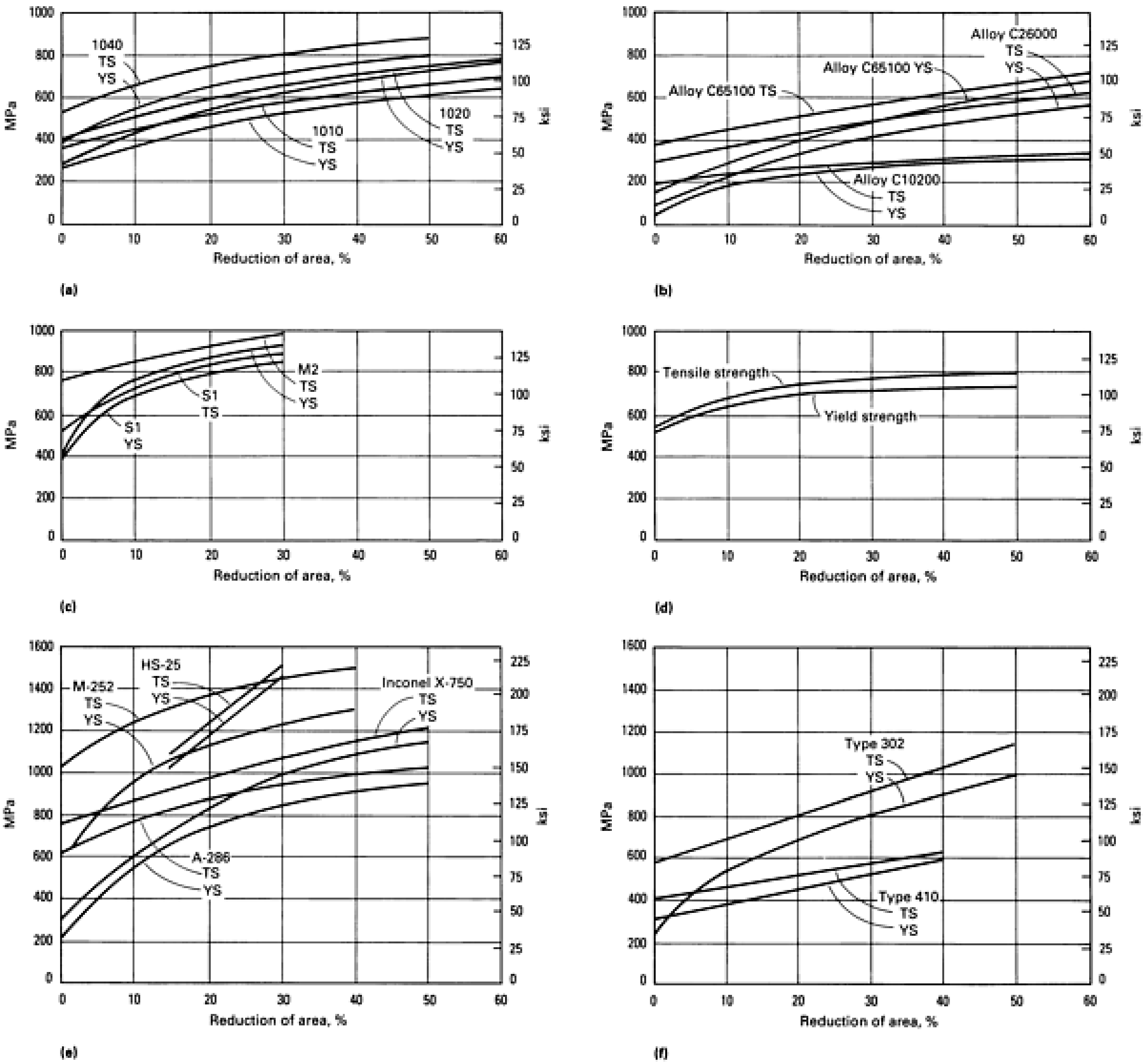

Fig. 2 Influence of cold reduction by swaging on mechanical properties of various alloy systems. (a) Carbon steels. (b) Copper alloys. (c) Tool steels. (d) Commercially pure titanium. (e) Heat-resistant alloys. (f) Stainless steels. TS, tensile strength; YS, yield strength.

Workpieces requiring reductions greater than that which can be accomplished with one swaging pass must be stress relieved or reannealed after the first pass to restore ductility in the metal for further reduction. Stress relieving of steel by heating to 595 to 675 °C (1100 to 1250 °F) often restores ductility, although excessive grain growth may develop when cold working is followed by heating within this temperature range. Stress relieving is of little value under these conditions, and it is necessary to anneal the material fully.

Rotary Swaging of Bars and Tubes

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Metal Flow During Swaging

Metal flow during rotary swaging is not confined to one direction. As shown in Fig. 3, more metal moves out of the taper in a direction opposite to that of the feed than through the straight portion (blade). Some metal flow also occurs in the transverse direction, but it is restricted by the oval or side clearance in the dies (see Fig. 7).

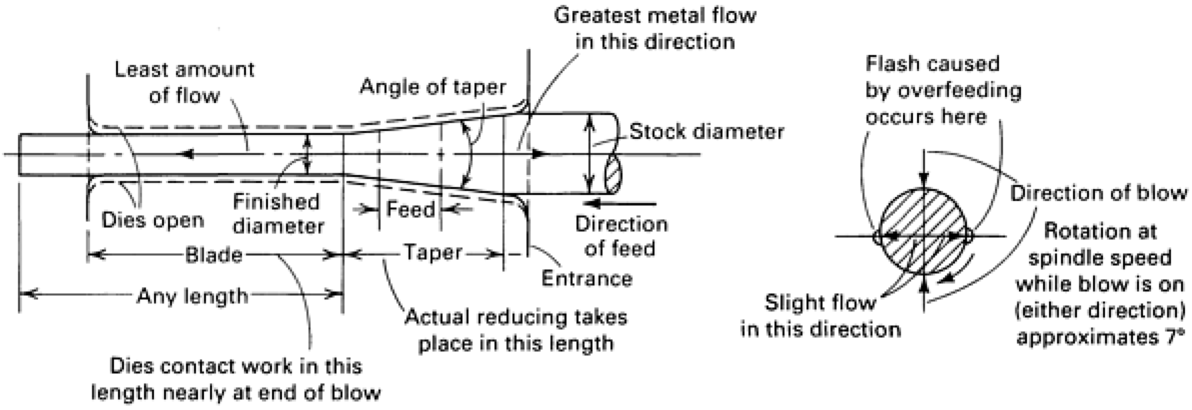

Fig. 3 Metal flow during swaging of a solid bar.

Feedback. The action of the metal moving against the direction of feed is termed feedback, and it results from slippage of the workpiece in the die taper when it is too steep. Feedback manifests itself as a heavy endwise vibration that causes considerable resistance to feeding of the workpiece.

Workpiece Rotation. Unless resisted, rotation is imparted as the dies close on the workpiece, and the speed of rotation is the speed of the roller cage. If rotation is permitted, swaging takes place in only one position on the workpiece, causing ovaling, flash, and sticking of the workpiece in the die. Resistance to rotation is manual when the swager is hand fed; mechanical means are used with automatic feeds.

Rotary Swaging of Bars and Tubes

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Machines

Rotary swaging machines are classified as standard rotary, stationary-spindle, creeping-spindle, alternate-blow, and die-closing types. All these machines are equipped with dies that open and close rapidly to provide the impact action that shapes the workpiece. The five principal machine concepts for swaging are shown in Fig. 4.

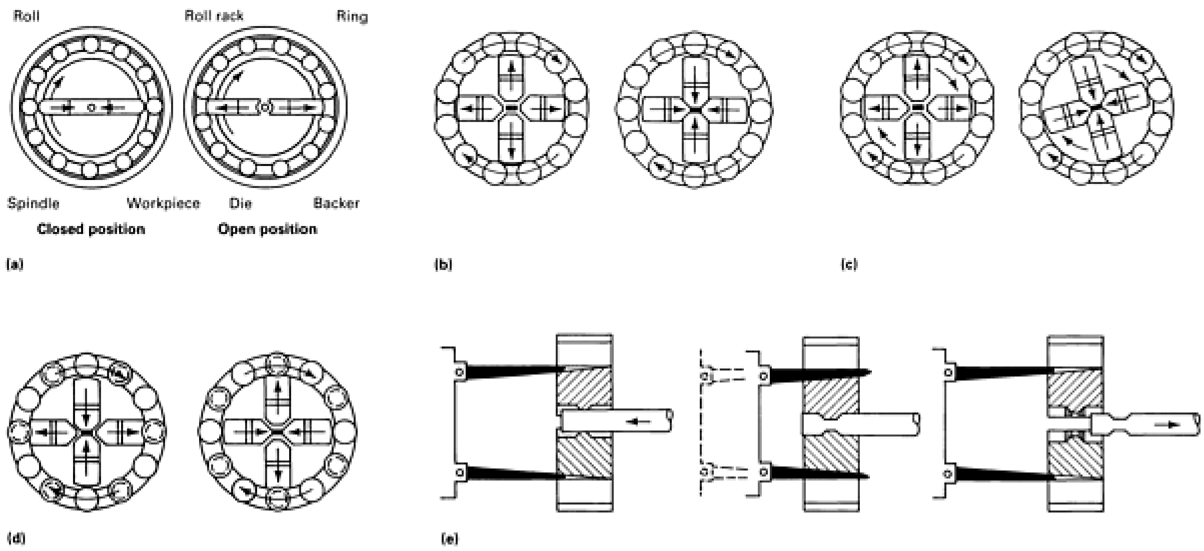

Fig. 4 Principal machine concepts for rotary swaging. (a) Standard rotary swager. (b) Stationary-spindle swager. (c) Creeping-spindle swager. (d) Alternate-blow swager. (e) Die-closing swager.

Swagers allow the work to be fed into the taper entrance of the swaging dies. The amount of diameter reduction per pass is limited by the design of the entrance taper of the dies or the area reduction capability of the machine. The results are expressed in terms of diameter reduction or area reduction.

The two methods of calculating reduction (in percent) are:

$$\text{Diameter reduction} = 100\left[1 - \left(\frac{D_2}{D_1}\right)\right]$$

$$\text{Area reduction} = 100\left[1 - \left(\frac{D_2^2}{D_1^2}\right)\right]$$

A die-closing swager has dies made with side relief that is sufficient to allow the dies to come down directly onto the work. The maximum side relief that can be used limits the reduction in diameter per swaging pass to 25%. The die-closing swager may have a front entrance angle and can be used as a standard rotary swager. When used in this manner, the diameter and area reduction per pass are the same as for a standard rotary swager. However, diameter reduction should not be confused with area reduction.

Standard Rotary Swagers. The basic rotary swager (Fig. 4a) is a mechanical hammer that delivers blows (impact swaging) at high frequency, thus changing the shape of a workpiece by metal flow. This machine is used for straight reducing of stock diameter or for tapering round workpieces.

A standard rotary swager consists of a head that contains the swaging components and a base that supports the head and houses the motor. A hardened and ground steel ring about 0.5 mm (0.020 in.) larger in diameter than the bore of the head is pressed into the head so that the ring is in compression.

The spindle, centrally located within the ring, is slotted to hold the backers and dies and is mounted in a tapered-roller bearing. Flat steel shims are placed between the dies and backers. A roll rack containing a set of rolls is located between the press-fitted ring and the backers. A conventional impact-type backer is shown in Fig. 5. The spindle is rotated by a motor-driven flywheel keyed to the spindle. During rotation of the spindle, the dies move outward by centrifugal force and inward by the action of the backers striking the rolls. The number of blows (impacts) produced by the dies is 1000 to 5000 per minute, depending on the size of the swager. The impact rate is approximately equal to the number of rolls multiplied by the speed (rpm) of the swager spindle multiplied by a correction factor of 0.6, which allows for creep of the roll rack.

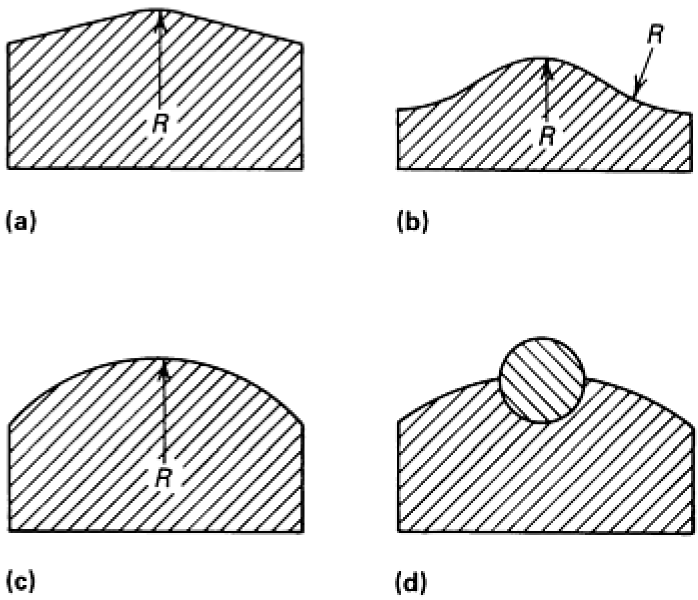

Fig. 5 Designs of four different backer cams used in rotary swaging. (a) Conventional impact-type backer (flat sides). (b) Squeeze-type backer with a sine curve type crown. (c) Squeeze-type backer with large radius on crown. (d) Backer with replaceable insert.

The amount of the die opening when the dies are in the open position--backers positioned between the rolls--can be changed to some extent during operation by a mechanical device that restricts the amount dies and backers can move under centrifugal force. However, the closed position of the dies--backers positioned on the rolls--cannot be changed during operation; the swager must be stopped and shims inserted between the dies and the backers. The severity of the blow can be varied by using shims of different thicknesses. The dies should be shimmed tight enough to obtain a reasonable amount of interference between the backers and the rolls when the dies are in the closed position.

The amount of shimming should be sufficient to bring the die faces together, and generally 0.05 to 0.5 mm (0.002 to 0.020 in.) of preload can be added, according to the size of machine. A swager is shimmed too tightly, or has too great a preload, when it stalls in starting while the swager hammers are off the rolls. The lightest possible shimming should be used; overshimming increases machine maintenance. Additional shimming will not produce a smaller section size, because section size is controlled by the size of the die cavity when the dies are in the closed position. Insufficient shimming, however, will increase the section size and cause variation in results, particularly in dimensions and surface conditions.

Stationary-spindle swagers are sometimes called inverted swagers, because the spindle, dies, and work remain stationary while the head and roll rack rotate. These machines are used for swaging shapes other than round.

The reciprocating action of the dies is the same as in swagers in which the spindle is rotated and the roll rack remains stationary. The principal components of a stationary-spindle machine are shown in Fig. 4(b).

The stationary-spindle swager consists of a base that houses the motor and supports a bearing housing containing two tapered-roller bearings. The head, fastened to a rotating sleeve mounted in the tapered-roller bearings, is motor driven and acts as a flywheel. The spindle is mounted and held stationary by a rear housing that is fastened to a bearing housing.

As the head rotates, the rolls pass over the backers, which in turn cause the dies to strike the workpiece in a pulsating hammer-type action. Die opening can be controlled by the forward feed of the workpiece, although springs are sometimes used to open the dies. The maximum outward travel of the dies in the open position is regulated by a mechanical device in the front of the machine. Shims are used between the dies and the backers, just as they are in swagers with rotating spindles.

Creeping-spindle swaging (Fig. 4c) employs the principles of both standard rotary and stationary-spindle swaging. The spindle and dies are mounted on a shaft that rotates slowly inside the rapidly rotating roller cage, thus permitting more accurately controlled reciprocation of the dies.

Alternate-blow swaging (Fig. 4d) is accomplished by recessing alternate rolls; in this configuration, when two opposing rolls hammer the dies, the rolls 90° away do not. This eliminates fins on the workpiece.

Die-closing swagers (Fig. 4e) are used when the dies must open more than is possible in a standard rotary swager to permit loading. Die-closing swagers are essentially of the same construction as the standard rotary swagers described above. Both have similar components, such as dies, rolls, roll rack, inside ring, spindle, and shims.

The main difference between die-closing and standard rotary swagers is the addition of a reciprocating wedge mechanism that forces closure of the taper-back dies, as shown in Fig. 4(e). The wedge mechanism consists of a wedge for each die that is positioned between the die and the backer. The rotating dies open by centrifugal force and are held open by springs or other mechanical means when the power-actuated wedge mechanism is in the back position. Wedge control of the die opening permits the work to be placed in the machine in a predetermined position when the dies are open. Reduction per pass is limited to 25% of the original diameter of the workpiece, and the wedge angle of the dies should not exceed $7\frac{1}{2}$°.

Swaging by Squeeze Action. The impact action common to standard rotary swagers can be slowed to produce a squeezing action by employing a backer cam. The design of the crown and the width of the backers are such that at least one roll is always in contact with the backer. The shape of the crown can be a single curve or two radii that approximate a sine curve. Both of these backer designs are shown in Fig. 5. Machines that use a sine curve type backer have fewer rolls than a standard swager.

Swaging with squeeze action is used to obtain greater reduction in area than that normally produced by impact action. It is also used to produce intricate profiles on internal surfaces with the aid of a mandrel.

Compared to impact forming with standard swagers, squeeze forming produces less noise and vibration, requires less maintenance of rolls and backers, and can produce greater reduction and closer tolerances. Standard rotary swagers, however, are simpler to operate and lower in cost, require less floor space, and are faster for small reductions.

Rolls and backers used for cold swaging are made from tool steel. The grade of tool steel used varies considerably, although many rolls and backers are made from one of the shock- or wear-resistant grades (depending on application) hardened and tempered to 55 to 58 HRC.

Almost all rolls and backers become work hardened. The degree of work hardening depends on the severity of reduction of the swaged workpiece, the swageability of the work metal, the material used for the rolls and backers, total operating time, and adjustment of the machine. Rolls, backers, and dies used in cold swaging are stress relieved periodically at 175 to 230 °C (350 to 450 °F) for 2 to 3 h in order to reduce the effects of work hardening and to prolong service life. The stress-relieving temperature used must not be higher than the original tempering temperature, or softening will result. The frequency of stress relieving depends on the severity of swaging. Under normal conditions, steel rolls and backers should be stress relieved after every 30 h of operation. Further improvements in tooling life and overall process costs are achieved by using replaceable inserts in the working area of the backers as shown in Fig. 5(d). These inserts can be carbide, and they have contoured forms that improve tool life and precision and reduce noise during swaging.

Stress relieving is usually not required for rolls and backers used for hot swaging, because some stress relief occurs each time heat transfers from the hot workpiece to the rolls and backers. These components are also less susceptible to work hardening than rolls and backers in cold swaging, because less force is required to form the part by hot swaging.

The rolls and roll rack of a four-die machine are subject to about $1\frac{1}{2}$ times as much wear as those in a two-die machine; therefore, they must be replaced more often. Other components, such as the spindle and cap, liner plates, backers, and dies, have about the same rate of wear in both types of machines; however, replacement cost of these components is lower for a two-die machine.

The number of rolls in a four-die machine must be divisible by four, so that they can be placed at 90° spacing. Therefore, a ten-roll machine is limited to using two dies.

Number of Dies. Most swagers have either two or four dies, although three-die machines are available. Most swaging is done in two-die machines, because they are less costly to build and simpler to set up and maintain.

Four-die swaging machines have some advantages. Slightly greater reductions can be made more readily, and cold working of the dies is reduced, because less ovality or side clearance is required than for two dies. Four-die machines are especially useful for swaging workpieces from a round to a square cross section. Four dies are generally not used for workpieces less than 4.8 mm ($\frac{3}{16}$ in.) across (in either round or square section).

A stationary-spindle usually has twelve rolls, and three, four, or six dies can be used. To change the number of dies in a swager, the spindle generally must be changed, because the slots in the spindle accommodate only the number of dies used. Three-die units are typically used to form triangular sections; four-die units, rectangles, squares, and rounds; and six-die units, hexagonal shapes.

Machine Capacity. The rated capacity of a swaging machine is based on the swaging of solid work metal of designated tensile strength and is expressed as the diameter--or the average diameter of a taper--to which the machine can swage a workpiece made from that material. Machine capacity is significantly influenced by the strength of the head. The load on the head is approximately equal to the projected area of the workpiece under compression multiplied by the tensile strength of the work metal.

For example, if the strength of the head limits the safe working load of a two-die machine to 51,000 kg (112 500 lb), the rated capacity (specific diameter) of the machine for a 75 mm (3 in.) long die in swaging solid work metal of 414 MPa (60 ksi) tensile strength can be calculated using:

$$\text{Load} = \text{specific diameter} \cdot \text{die length} \cdot \text{tensile strength}$$

Therefore:

$$\text{Specific diameter} = \frac{\text{Load}}{\text{die length} \cdot \text{tensile strength}}$$

where load is in kilograms (pounds), specific diameter is in millimeters (inches), die length is in millimeters (inches), and tensile strength is in megapascals (pounds per square inch). Therefore, for the process parameters outlined above, and using SI units:

$$\text{Specific diameter} = \frac{51\ 000}{75 \cdot 414} = 16 \text{ mm}$$

Using English units:

$$\text{Specific diameter} = \frac{112\ 500}{3 \cdot 60\ 000} = \tfrac{5}{8} \text{ in.}$$

For work metal of a higher or lower tensile strength, the capacity or specific diameter would be proportionately lower or higher, in accordance with the above formula. For a greater die length, the machine capacity would be lower. To swage parts to a larger final average diameter in this two-die machine, it would be necessary to decrease the working length of the die proportionately and therefore to decrease the area of work metal under compression.

For the swaging of a tube, the capacity of the machine is limited by the cross section of the die, by the compressive strength of the tube, and sometimes by the size of hole through the spindle of the machine. The swaging of tubes with a wall thickness greater than 1 mm (0.040 in.) over a mandrel is considered the same as the swaging of solid bar stock. Tubes with thinner walls require greater force, depending on tube diameter and length of die, because friction traps the metal between the die and mandrel, and there is no bulk metal to move.

Machines with dies that produce a squeezing action are rated according to their radial load capacity. The capacity is usually limited by the stress at the line of contact between the roller and backer. For a reasonable component life, this stress should not exceed about 1170 MPa (170 ksi). Assuming this stress as maximum when rollers and backers are made of steel, the radial load capacity is determined by:

$$L = 0.002\ Nl \left(\frac{D_r D_b}{D_r + D_b} \right)$$

where L is radial load capacity in megagrams, N is the number of backers, l is effective roller length in millimeters, D_r is the diameter of each roller (in millimeters), and D_b is the diameter (in millimeters) of the backer crown contacting the rollers. The coefficient 0.002 converts the Hertz stress formula to megagrams of force based on a value of 1170 MPa for maximum stress. When English units are used, the coefficient is 1.38 based on a maximum stress value of 170 ksi. Radial load capacity would be calculated in tons, and all linear measures would be in inches.

For example, a four-die machine having 100 mm (4 in.) diam rolls with an effective roller length of 250 mm (10 in.) and a 915 mm (36 in.) diam backer crown would have a radial load capacity of 180 Mg (199 tons), determined as follows:

$$L = 0.002 \cdot 4 \cdot 250 \left(\frac{100 \cdot 915}{100 + 915} \right) = 180 \text{ Mg}$$

$$L = 1.38 \cdot 4 \cdot 10 \left(\frac{4 \cdot 36}{4 + 36} \right) = 199 \text{ tons}$$

Rotary Swaging of Bars and Tubes

Revised by the ASM Committee on Rotary Swaging*

Swaging Dies

Resistance to shock and wear are the primary requirements for cold swaging dies. It is sometimes necessary to sacrifice some wear resistance in order to prevent die breakage due to lack of shock resistance. Numerous materials have been used for swaging dies. Typical tool steels for cold swaging include A8, D2, S3, S7, and M2 at hardnesses ranging from 55 to 62 HRC. M2 and H13 are frequently used for hot swaging. Shock-resistant grades of carbide are used for high-production applications. However, the greater density of carbide may lead to increased backer and roll wear.

Types of Dies. Depending on the shape, size, and material of the workpiece, dies range from the simple, single-taper, straight-reduction type to those of special design. Figure 6 illustrates nine typical die shapes. Specific applications for each are outlined below.

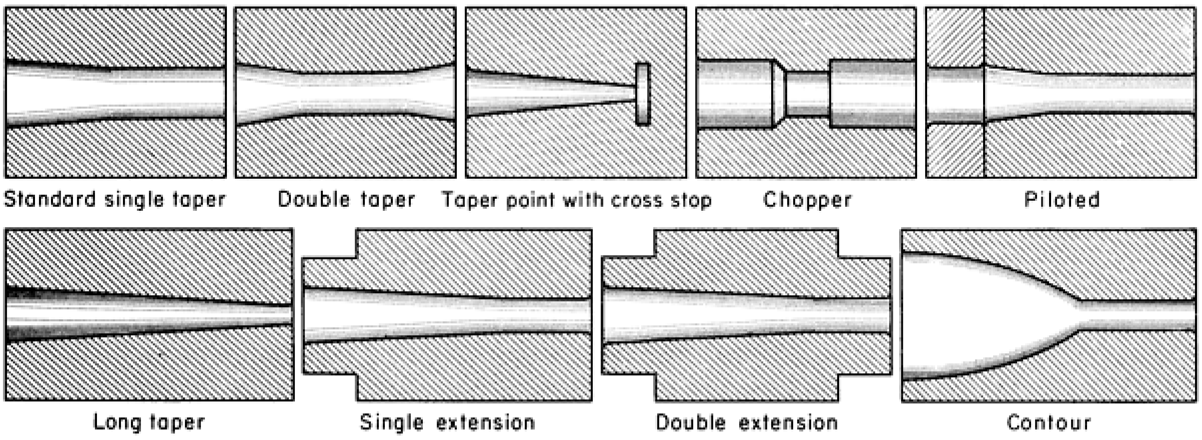

Fig. 6 Typical die shapes used in rotary swaging. See text for discussion.

Standard single-taper dies are the basic swaging dies designed for straight reduction in diameter. One common use is to tag bars for drawbench operations.

Double-taper dies are designed for plain reductions, such as those made in the standard single-taper die described above. A double-taper die can be reversed to obtain twice the life of a single-taper die.

Taper-point dies are used for finish forming a point on the end of the workpiece or for forming a point prior to a drawbench operation. The built-in cross stop ensures equal length of all swaged points.

Chopper dies are fabricated from heat-resistant alloys. These dies are used exclusively for hot swaging.

Piloted dies ensure concentricity between the unswaged section and the reduced section of the workpiece. The front part of the die acts as a guide; reduction occurs only in the taper section.

Long-taper dies are designed with a taper over their entire length. However, the length of the taper produced on the work will be slightly less than that of the die.

Single-extension dies are used for high reduction of solid bars and tubing of low tensile strength. This die produces a longer tapered section than a standard die.

Double-extension dies are extended at both ends to facilitate the swaging of thick-wall tubing and to provide a longer taper section.

Contour dies are used to produce special shapes on tubes and bars.

Die Clearance. Virtually all swaging dies require clearance in the form of relief or ovality in the die cavity. Without clearance, the flow of metal is restricted, and this results in the workpiece sticking to the die.

Ovality in Two-Piece Dies. Dies are oval in both the taper and blade sections. This ovality and side relief provide the necessary clearance for the die to function. Ovality is useful for applications in order to maximize work hardening. The disadvantages of using ovality to obtain clearance are:

- Close tolerances are difficult to maintain
- Dies wear rapidly
- Surface finish on the workpiece is inferior to that produced with dies having side clearance

Ovality in two-piece dies is produced by placing shims between the finished die faces and boring or reaming the assembly to the desired clearance. Smoothly blending the two contours gives an approximately oval shape to the reassembled die. An alternative procedure for producing ovality is to bore the two die blocks oversize and then to grind the die faces until the groove in each half is of the proper depth to produce the desired swaged diameter.

The amount of ovality required varies with the characteristics and size of the work metal to be swaged. Table 2 lists nominal values for determining the amounts of ovality for swaging solid material from 0.8 to 19 mm ($\frac{1}{32}$ to $\frac{3}{4}$ in.) in diameter and tubing covering a range of outside diameters. The following sample calculation shows how Table 2 is used to determine the die ovality required for swaging 12.7 mm (0.5 in.) diam 1020 steel bar to a diameter of 9.5 mm (0.375 in.) using a die with a taper of 8° included angle. From Table 2, the ovality for the die taper for swaging low-carbon steel is 0.025 mm (0.001 in.) per degree of taper plus 0.5% of the maximum diameter of the bar before swaging. Therefore:

$$\begin{aligned} \text{Ovality}_{\text{taper}} &= (0.025 \cdot 8) + (0.005 \cdot 12.7) \\ &= 0.2 + 0.064 \\ &= 0.264 \text{ mm} \end{aligned}$$

Using English units:

$$\begin{aligned} \text{Ovality}_{\text{taper}} &= (0.001 \cdot 8) + (0.005 \cdot 0.5) \\ &= 0.008 + 0.0025 \\ &= 0.0105 \text{ in.} \end{aligned}$$

According to Table 2, ovality of the blade section of the die is 0.075 to 0.1 mm (0.003 to 0.004 in.) less than the ovality of the taper section. Therefore:

$$\text{Ovality}_{\text{blade}} = 0.264 - 0.075 = 0.19 \text{ mm}$$

Using English units:

$$\text{Ovality}_{\text{blade}} = 0.0105 - 0.003 = 0.0075 \text{ in.}$$

Table 2 Nominal values for computing ovality and corner radius on groove of dies for swaging of bars and tubing

Work metal	Percentage of shimming recommended for die diameter of:				
	19-6.4 mm ($\frac{3}{4}$ - $\frac{1}{4}$ in.)	**4.8 mm ($\frac{3}{16}$ in.)**	**3.2 mm ($\frac{1}{8}$ in.)**	**1.6 mm ($\frac{1}{16}$ in.)**	0.8 mm ($\frac{1}{32}$ in.)
Dies for swaging of bars					
Low-carbon steels; hard brass; copper	For die taper: 0.025 mm (0.001 in.) per degree plus 0.5% of max work diameter. For die blade: above value less 0.075-0.1 mm (0.003-0.004 in.)	2[(a)]	3[(a)]	4[(a)]	[(b)]
High-carbon and alloy steels	125% of value for low-carbon steels	2[(a)]	3[(a)]	4[(a)]	[(b)]
Lead	No shimming required	. . .	. . .	. . .	. . .

Dies for swaging of tubing
When OD equals a minimum of 25 times wall thickness, use no shimming.
When OD equals 10 to 24 times wall thickness, use 60% of values for bars (see above).
When OD equals 9 times wall thickness or less, use same values as for bars (see above).
Corner radius on die grooves
For solid work metal, $\frac{1}{16}$ of blade diameter to nearest 0.13 mm (0.005 in.)
For tubing, corner radius should be equal to wall thickness.

(a) Percent of average diameter of work.

(b) Stone edges of die groove

These calculated values determine the thickness of shims that must be used between the die faces during machining of the cavity to produce a die of proper ovality for swaging 1020 steel bars. These values also apply when the alternative method of producing ovality is used.

In addition to ovality, die halves should be provided with corner radius at the exit end of the blade section as well as at the die entrance. Table 2 indicates that the corner radius on the groove should be $\frac{1}{16}$ of the blade diameter to the nearest 0.13 mm (0.005 in.) for swaging solid sections, or equal to wall thickness for the swaging of a tube. Therefore, the die for swaging the 12.7 mm (0.5 in.) diam 1020 steel bar referred to in the sample calculation above would require a corner radius of about 0.64 mm (0.025 in.).

The included angle for the taper section of oval dies should be no more than 30°. An included angle of 8° or less is preferred.

Two-Piece Dies With Side Clearance. Workpieces swaged in 240° contact dies have better surface finish and closer tolerance. The service lives of these dies are longer, and the work metal is cold worked less rapidly than in oval dies. Dies with 240° contact can be used for straight reductions of solid bars or thick-wall tubing.

Figure 7 shows the design of 240° contact dies with die clearance. The dies are first bored or ground without shims to produce the area of work contact. Shims are then inserted to produce side clearance only. Side clearance is then bored or ground until dimension *E* (measured diagonally across the mouth of the die) = $\sqrt{d^2+ds+s^2}$, where *d* is the initial diameter of the taper at the entrance to the die, and *s* is the thickness of the shim stock placed between the die faces. The maximum thickness of the shim should be one-tenth the swaged diameter of the workpiece. This will produce a total contact of 240° along the taper and blade sections. The intersection between the taper and blade must be well blended for best results in feeding and finishing.

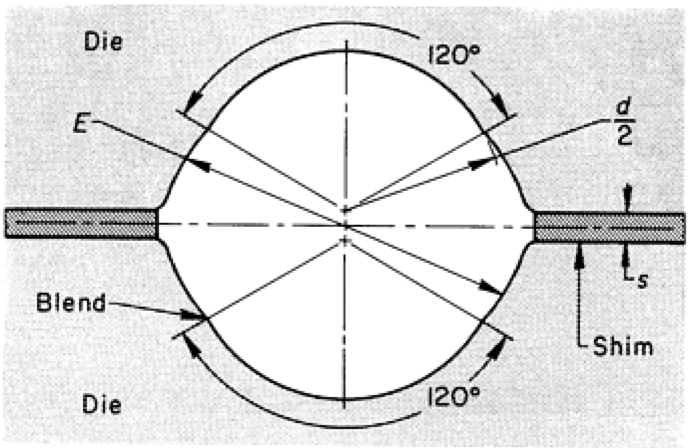

Fig. 7 Design of die with side clearance. See text for discussion.

When swaging tubing, the shim thickness varies with the ratio of outside diameter to wall thickness (D/t ratio) so that the side clearance is nearly zero for thin-wall (D/t = 30 or more) tubing.

The same procedure is followed in determining the side clearance for the blade. The diameter of the swaged workpiece is used instead of the large diameter of the taper. The same shim is used for both taper and blade.

Ovality in Four-Piece Dies. Each piece of a four-piece die makes approximately 90° contact with the surface of the workpiece when the die is not provided with ovality or side clearance. Dies without ovality are used for sizing thin-wall tubes (D/t = 30 or more). For swaging solid sections or thick-wall tubing or for mandrel swaging, oval dies are required; ovality influences circumferential flow of the work metal and reduces the load on the machine.

Oval dies are produced by various methods. A common method involves grinding the dies, which are held by a fixture mounted on the rotating face plate of an internal grinder. The taper is produced by pivoting the grinding wheel slide to the appropriate angle and traversing the surface.

Rotary Swaging of Bars and Tubes

Revised by the ASM Committee on Rotary Swaging*

Auxiliary Tools

Swaging machines may require auxiliary tools for guiding and feeding the workpiece into the die, holding it during swaging, and ejecting it. These tools range from simple hand tools to elaborate power-driven mechanisms. Some of the common types of auxiliary tools are illustrated in Fig. 8, 9, and 10; their uses are described below.

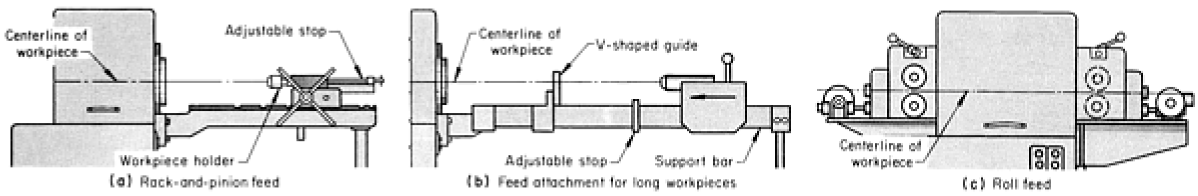

Fig. 8 Three types of mechanisms for feeding the workpiece in rotary swaging. See text for discussion.

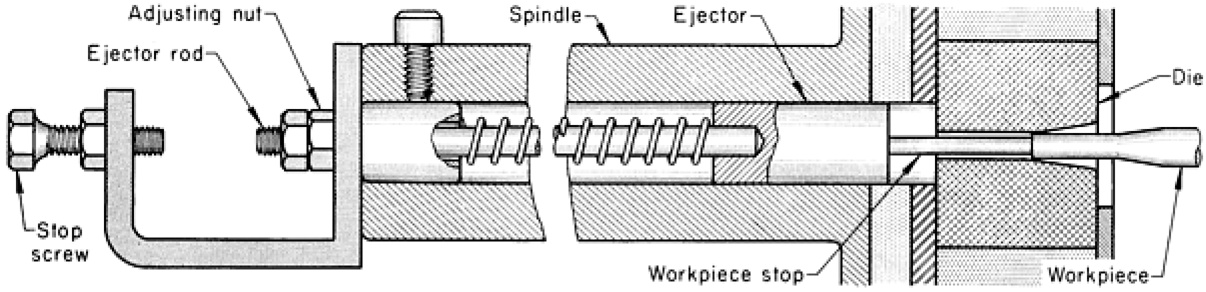

Fig. 9 Principal components of a spring ejector mechanism with an adjustable rear stop.

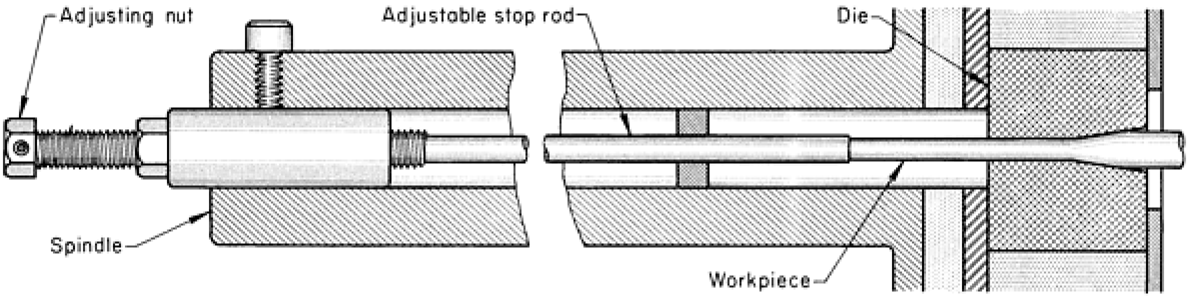

Fig. 10 Principal components of an adjustable stop-rod mechanism.

Rack-and-pinion mechanisms (Fig. 8a) are designed for manual operation and provide more force for feeding the workpiece than can be obtained by hand feeding. Operator fatigue is reduced with these mechanisms, and the workpiece is guided straight along the centerline of the machine.

Feed attachments for long workpieces (Fig. 8b) consist of a carriage with antifriction rollers mounted on a fixed bar that extends from a bracket on the entrance side of the machine for the length of the longest workpiece to be swaged. The outer end of the bar is aligned by leveling screws in the base of a triangular support. The carriage provides a means of attaching plain or antifriction workpiece holders and adapters, as well as a handle for manual feeding toward the swager. An adjustable stop is provided on the support bar to control the length of the swaged section and to reproduce accurate tapers.

Roll-feed mechanisms (Fig. 8c) have rolls at the entrance and at the exit end of the swager. The rolls at the entrance feed the workpiece, and the rolls at the rear pull the workpiece from the machine. Roll-feed mechanisms are used for continuous swaging. Rolls can be made from either metal or a nonmetallic material (such as rubber). Some roll-feed mechanisms have four soft rubber rolls at the entrance to the swager and no rolls at the rear. This arrangement is ideal for swaging small-diameter bars whose surface finish is critical, because it prevents marking of the swaged surfaces when the bars are pulled from the rear of the machine.

V-shape work guides (Fig. 8b) are used to support and center the ends of long tubes or bars as they enter the dies. This type of guide is mounted on the front of the machine and can be adjusted vertically to accommodate a range of workpiece diameters up to the capacity of the machine.

Spring ejectors are required for the removal of short workpieces when size prevents manual withdrawal from the dies or when workpieces are swaged over their entire length and cannot be passed through the spindle to the exit end. Figure 9 shows the principal components of a spring ejector mounted on the rear of a swaging machine spindle. As the workpiece enters the die against the workpiece stop, the ejector rod is forced backward until it contacts the preset stop screw. As soon as the swaging cycle is completed, the spring-loaded ejector forces the workpiece from the front of the machine.

Spring ejectors reduce operator fatigue and shorten the swaging cycle in many applications. A similar mechanism can be used on large machines with power feed. The ejector maintains contact with the workpiece stop on the return stroke, thus supporting the workpiece until it is free from the dies.

Stop rods (Fig. 10) are often used to improve uniformity of swaged pieces in production runs. These rods can be adjusted and locked so that subsequent workpieces will have swaged sections of equal length. The swaged length can be held within 0.025 mm (0.001 in.), depending on the speed and on the feed pressure.

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Automated Swaging Machines

Work-holding fixtures were originally designed for manual operation. These fixtures include a variety of grippers that facilitate workpiece alignment parallel to the feed direction, provide constrained rotation to prevent finning or flashing between the dies, and have capabilities for feed-stroke control. Work holders have been designed for use on a variety of workpiece sizes.

The feed, stop, and ejector mechanisms shown in Fig. 8, 9, and 10, as well as a variety of manual work-holding fixtures, have formed the basis for contemporary automated systems. The range of equipment available includes single-station machines (Fig. 11), which form parts automatically in one or more setups, and multistation transfer machines (Fig. 12) that use different types of swaging heads to perform multiple operations in a single setup.

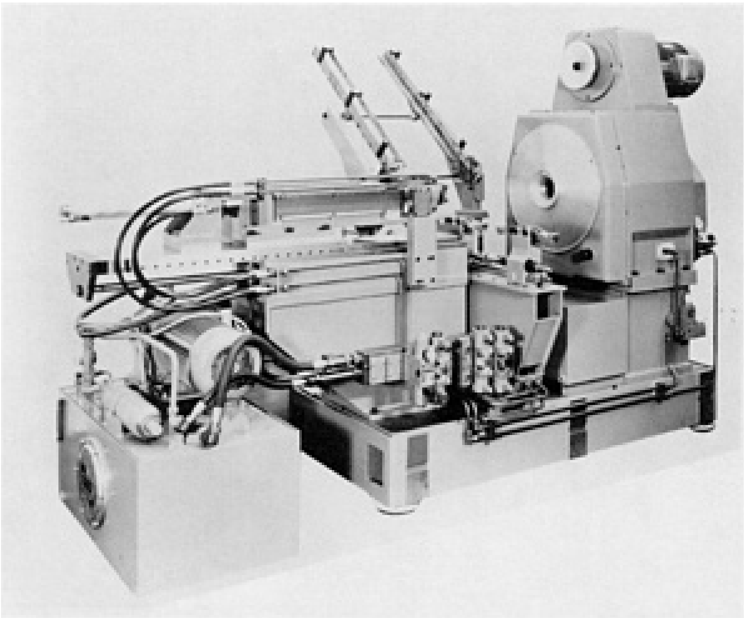

Fig. 11 Automated die-closing swaging machine with a gravity parts feeder, hydraulically operated feeding unit, and part transfer system.

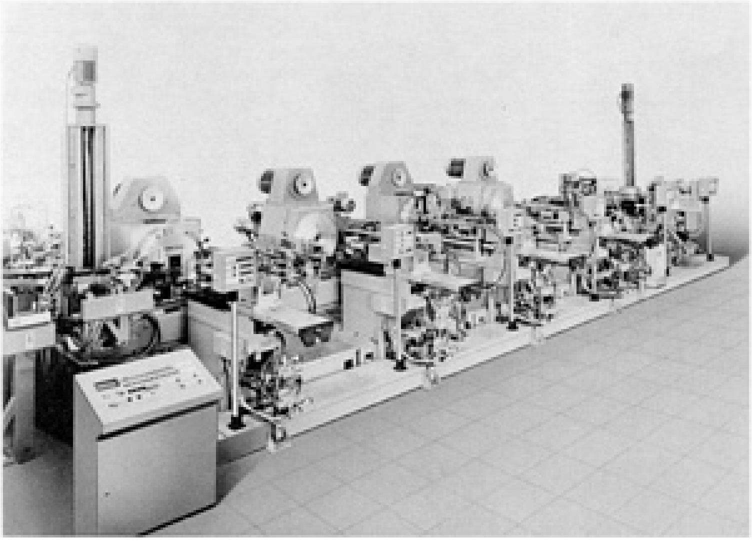

Fig. 12 Multistation automatic swaging transfer machine combining forming and machining operations.

The automatic machines are assembled using a modular concept, and the number of stations can be varied to suit a particular application. Programmable control allows different stations to be actuated, bypassed, or exchanged to process a family of parts on one system.

Secondary operations, such as drilling, turning, reaming, splining, thread rolling, or marking, can also be incorporated into the manufacturing process. Such a sequence of operations is illustrated in Fig. 13 for a torch nozzle.

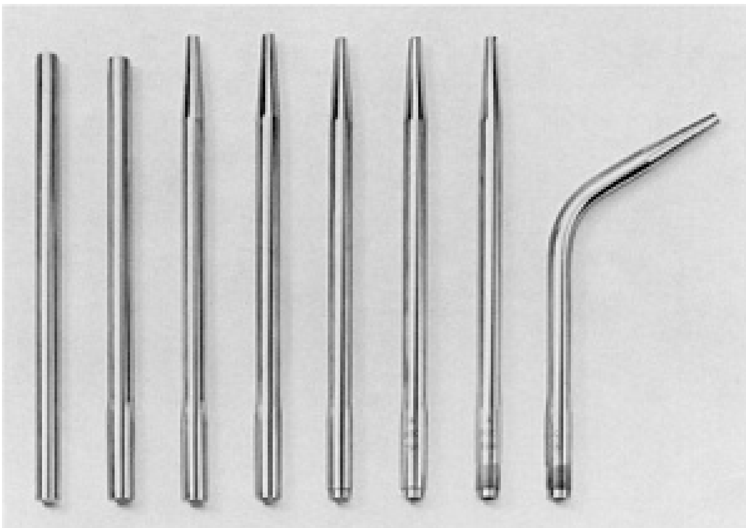

Fig. 13 Torch nozzle produced using a sequence of operations on a multistation transfer machine similar to that shown in Fig. 12.

The material for automatic operation can be supplied either from different types of magazines (such as vibratory feeders, conveyors, and gravity chutes) or directly from coiled stock. This allows the machines to operate unattended for long periods of time, resulting in machine efficiencies of 90% or more. The stroke and speed of each feeding unit can be set according to tolerance and surface finish requirements.

This closely controlled process of automatic swaging provides highly repeatable results and consistent part quality. Automatic operation allows one operator to operate several machines.

Tube Swaging Without a Mandrel

Tubes are usually swaged without a mandrel to attain one or more of the following:

- A reduction in inside and outside diameters or an increase in wall thickness
- The production of a taper
- The conditioning of weld beads for subsequent tube drawing
- Increased strength
- Close tolerances
- A laminated tube produced from two or more tubes

The usual limit on the diameter of tubes that can be swaged without a mandrel is 30 times the wall thickness. Tubes with an outside diameter as large as 70 times their wall thickness can be swaged, but under these conditions, the included angle of reduction must be less than 6°, and the feed rate must be less than 380 mm/min (15 in./min). Under any conditions, the tube must have sufficient column strength to permit feeding. Squareness of the cut ends, roundness, and freedom from surface defects also become more critical as the ratio of outside diameter to wall thickness increases.

Types of Tubes for Swaging. Seamless and welded tubing can be swaged without a mandrel. Seamless tubing is available in greater wall thicknesses in proportion to diameter than welded tubing. However, seamless tubing is the more expensive and may have an irregular and eccentric inside diameter, which will result in excessive variation in wall thickness of the swaged product. When purchasing seamless tubing, it is possible to specify two of the three dimensions: outside diameter, inside diameter, and wall thickness. Therefore, the disadvantage of varying dimensions can be partly overcome by specifying the two dimensions that must be controlled for an acceptable product.

Welded tubing usually has a more uniform wall thickness than seamless tubing and therefore has an inside diameter that is more nearly concentric with the outside diameter. The swaging of certain types of welded tubing (for example, as-welded and flash rolled) can result in bending, because the metal in the weld area flows less readily than the remainder of the tube material. If the weld is defective or if the metal in the weld area is harder than the remainder of the tube, splitting will occur during swaging. Welded tubing must be held in the centerline of the feed direction during swaging to produce a straight product.

Die Taper Angle. In best practice when swaging low-carbon steel, the included angle of die taper should not exceed 8° when using manual feed. For thin-wall tubing of low-carbon steel or for more ductile tubing, such as annealed copper, the included angle may be as great as 15°, provided both pressure and feed are decreased proportionately. When the angle of taper exceeds 15°, mechanical or hydraulic feed should be used.

Reduction per Pass. Multiple passes are necessary to swage tubing in dies with a taper exceeding 30°. Steep taper angles generate excessive heat and feedback and radial pressures. This condition may result in metal pickup by the dies and is more pronounced when swaging aluminum tubing.

Effect of Reduction on Tube Length. In swaging tubes without a mandrel, wall thickening is usually more significant than increase of length. Lengthening of about 5 to 15% can be expected for typical swaging operations on low-carbon steel, copper, aluminum, or other readily swageable metal tubes with outside diameters of 15 to 25 times wall thickness. Lengthening increases as the amount of reduction per pass increases. Because of the uncertainty about the relative amounts of radial and axial movement of metal, percentage reduction is frequently designated in terms of diameter reduction, rather than area reduction. When the tube is reduced to the extent that it approaches a solid, the endwise flow of metal increases. When total reduction in area is greater than 65 to 75% (depending on the ratio of outside diameter to wall thickness), the tube should be considered a solid, and swaging dies should be designed accordingly.

Effect of Reduction on Wall Thickness. Swaging of tubing without a mandrel results in an increase in wall thickness. The increase in wall thickness is greater for larger reductions in outside diameter. Increased ductility of the tube material promotes wall thickening.

The wall thickness that will be produced by swaging a tube without using a mandrel can be calculated to about ±10% from the empirical relation:

$$t_2 = \frac{D_1 t_1}{D_2}$$

where D_1 is outside diameter before swaging, D_2 is outside diameter after swaging, t_1 is wall thickness before swaging, and t_2 is wall thickness after swaging.

Swaging of Long Tapers. The method used for swaging long tapers depends on work metal hardness, outside diameter, wall thickness, and overall length, because these variables determine required machine size, die design, and type of feed mechanism.

Welded tubing sometimes causes difficulty in swaging long tapers because of variations in hardness between the welded seam and the remainder of the tube. Postweld heat treatment is recommended when swaging long tapers from welded tubing.

Almost any reasonable length of taper can be swaged on any length of tube that has a diameter within the capacity of the machine. Long tapers usually require multiple operations.

Table 3 compares the lengths of taper that can be formed in a single operation and in multiple operations on tubes with an outside diameter of 57 mm ($2\frac{1}{4}$ in.) or less, using standard-length and extended-length dies. Standard-length dies refer to manufacturers' catalog sizes; extended lengths are greater than those shown as standard. The longest taper formed in a single operation is fairly close to the length of the die. However, when dies of the same length are used in multiple operations, a smaller portion of the usable length is used for forming the taper, because of the allowance required for blending subsequent passes.

Table 3 Tapers swageable on 57 mm ($2\frac{1}{4}$ in.) maximum OD tubes in single and multiple operations

Die length, mm (in.)	Length of taper swaged, mm (in.)				
	Single operation	Multiple operations			
		First operation	**Intermediate operations**	Final operation
114 ($4\frac{1}{2}$)	105 ($4\frac{1}{8}$)	79 ($3\frac{1}{8}$)	63.5 ($2\frac{1}{2}$)	**89 ($3\frac{1}{2}$)**
162 ($6\frac{3}{8}$)	152 (6)	127 (5)	111 ($4\frac{3}{8}$)	**136.5 ($5\frac{3}{8}$)**
213 ($8\frac{3}{8}$)	203 (8)	178 (7)	162 ($6\frac{3}{8}$)	**187 ($7\frac{3}{8}$)**
380 (15)	375 ($14\frac{3}{4}$)	. . .	. . .	**. . .**

455 (18)	451 (17 $\frac{3}{4}$)	. . .	. . .	. . .
610 (24)	584 (23)	. . .	. . .	. . .
Extended die lengths (standard plus 38 mm, or 1 $\frac{1}{2}$ in.)				
152 (6)	143 (5 $\frac{5}{8}$)	117 (4 $\frac{5}{8}$)	102 (4)	**127 (5)**
200 (7 $\frac{7}{8}$)	190 (7 $\frac{1}{2}$)	165 (6 $\frac{1}{2}$)	149 (5 $\frac{7}{8}$)	**175 (6 $\frac{7}{8}$)**
250 (9 $\frac{7}{8}$)	**241 (9 $\frac{1}{2}$)**	**216 (8 $\frac{1}{2}$)**	**200 (7 $\frac{7}{8}$)**	**225 (8 $\frac{7}{8}$)**

The number of operations needed to produce a specified taper, in addition to the length of taper and length of dies used, is influenced by the following:

- Minimum length of die entrance is 9.5 mm ($\frac{3}{8}$ in.)
- Each succeeding taper must overlap the preceding taper by 25 mm (1 in.) to permit blending
- All operations except the last must allow a straight section (blade), with a minimum length of 25 mm (1 in.) on the tube in addition to the taper being swaged

Example 1: Forming a 760-mm (30-in.) long Taper in Four Operations.

Figure 14 shows the sequence of operations for swaging a 32 mm (1 $\frac{1}{4}$ in.) OD low-carbon steel tube to 12.7 mm ($\frac{1}{2}$ in.) in diameter over a taper length of 760 mm (30 in.). Extended dies 250 mm (9 $\frac{7}{8}$ in.) long were used for the first three operations, and a standard die 210 mm (8 $\frac{3}{8}$ in.) long was used for the final operation. An allowance of 9.5 mm ($\frac{3}{8}$ in.) was made for die entrance, a 25 mm (1 in.) overlap was used for each succeeding taper, and each operation except the last allowed a blade section to remain. The same machine was used for all four operations.

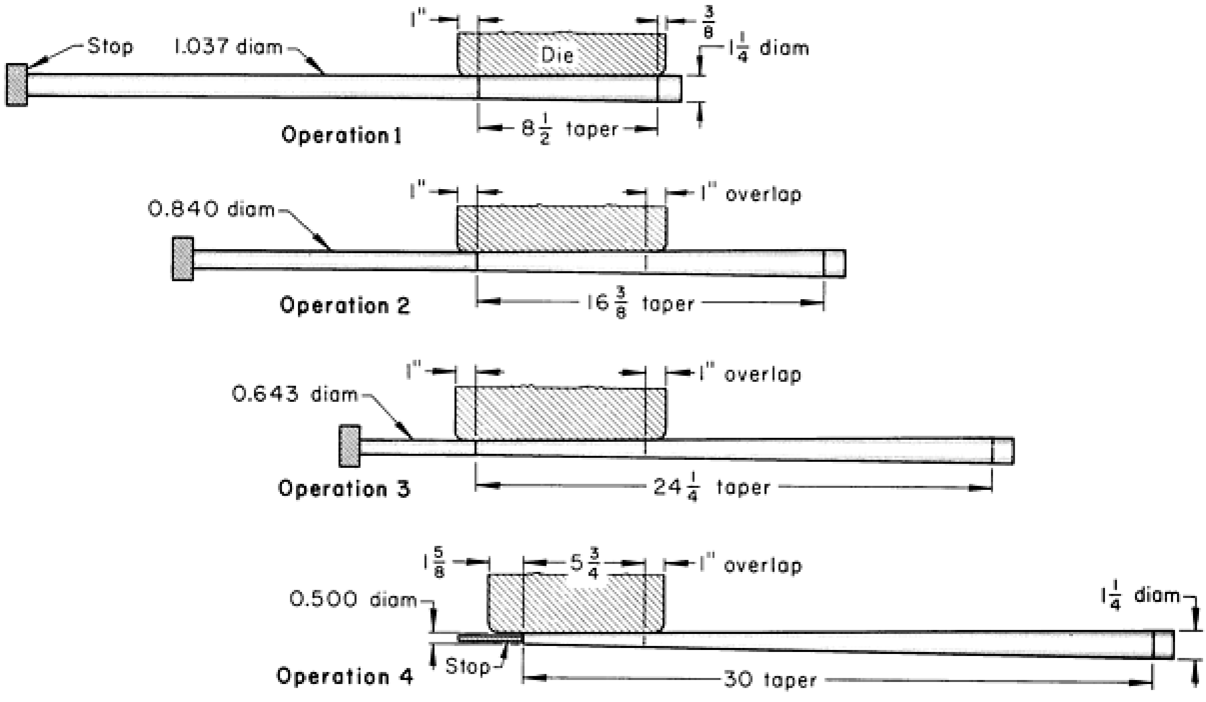

Fig. 14 Sequence of operations for swaging a taper on a long tube. Extended dies are used in the first three operations; the final operation uses standard-length dies. Dimensions given in inches.

In each operation, the tube was fed through the die to a stop, reducing the tube in each operation to the diameters shown in Fig. 14. Each feed length was controlled by a stop so that the newly formed taper blended with the preceding one.

Figure 15 shows how a taper 760 mm (30 in.) long can be formed in two operations by dies 455 mm (18 in.) long. The rate of feed for swaging long tapers is usually 25 mm/s (1 in./s), withdrawal time is 100 mm/s (4 in./s), and handling time requires about 4 s per operation.

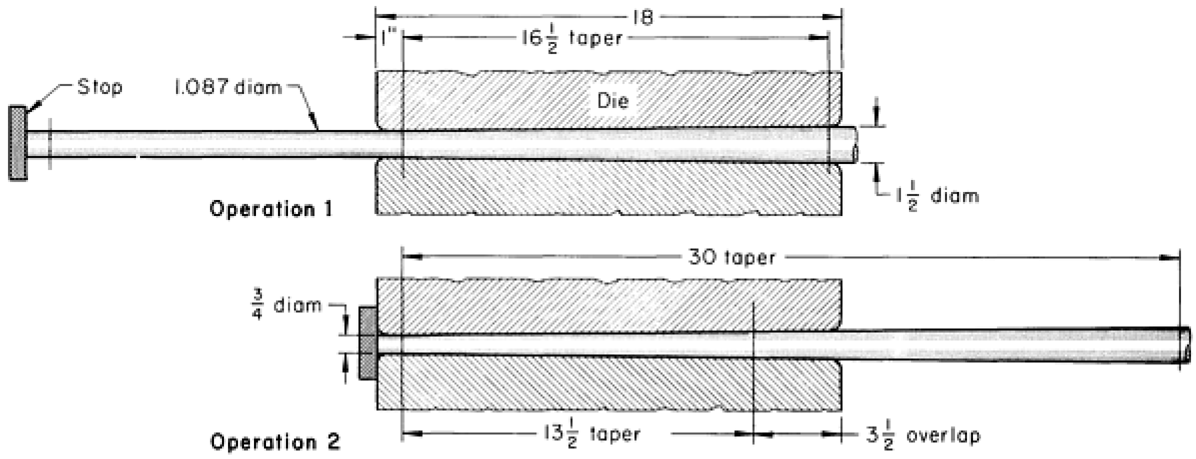

Fig. 15 Swaging a 760 mm (30 in.) long taper in two operations using dies 455 mm (18 in.) long. Dimensions given in inches.

An accurate feeding attachment is necessary to swage long tapers. The attachment must feed the tube to the proper length for each operation to produce a uniform taper. This is accomplished by registering the infeed position of the tube from the butt end by means of stops on the attachment (Fig. 14).

Manually operated feed attachments are generally used for producing tapers longer than 405 mm (16 in.). Either hydraulic or air-actuated feed attachments are more convenient for tapers up to 405 mm (16 in.) in length.

Cost is the deciding factor between using standard or extended dies for swaging a given taper. Cost also usually determines the number of operations to be used. However, when tapers exceed 510 mm (20 in.) in length, there is no alternative but to use multiple operations, because few swaging machines can hold dies longer than 405 mm (20 in.).

Any swaging machine can handle extended dies that are longer than standard for the machine size (see Example 1). A given machine can also accommodate shorter dies when die box fillers are used. Therefore, each machine has considerable flexibility in terms of the length of dies it can handle.

Extended dies cost more than standard dies (usually about one-third more). Therefore, it must be decided if it would be more economical to pay the higher cost for extended dies and use fewer operations, thus increasing productivity, or to use less expensive dies and accept lower productivity. Similar consideration must be given to the use of a larger machine that will accommodate a longer standard die.

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Revised by the ASM Committee on Rotary Swaging*

Tube Swaging With a Mandrel

For some applications, it is necessary to reduce the wall thickness of tubing by swaging over a mandrel. A mandrel is used to maintain the inside diameter of a tube during the swaging of its outside diameter, to support thin-wall tubes during reduction in diameter, and to form internal shapes. When extended through the front of the dies, a mandrel can also serve as a pilot to support one of the tubes that are to be joined by swaging.

Mandrels are made from shock-resistant tool steel and high-speed steels. They are hardened, ground and polished, and sometimes plated with about 5 μm (0.2 mils) of chromium to improve wear resistance and surface finish on the inside diameter of the tube. A combination of hardness and toughness is needed for the larger mandrels. Tungsten carbide mandrels are used for superior wear resistance when production volume justifies their increased cost. Mandrels are commonly produced from S group tool steels hardened to 59 to 61 HRC or from A2 or W1 tool steel hardened to 60 to 62 HRC and ground to a finish of 0.06 to 0.075 μm (2.5 to 3 μin.).

Types of Mandrels. The types of mandrels most often used are illustrated in Fig. 16 and are described in the following sections.

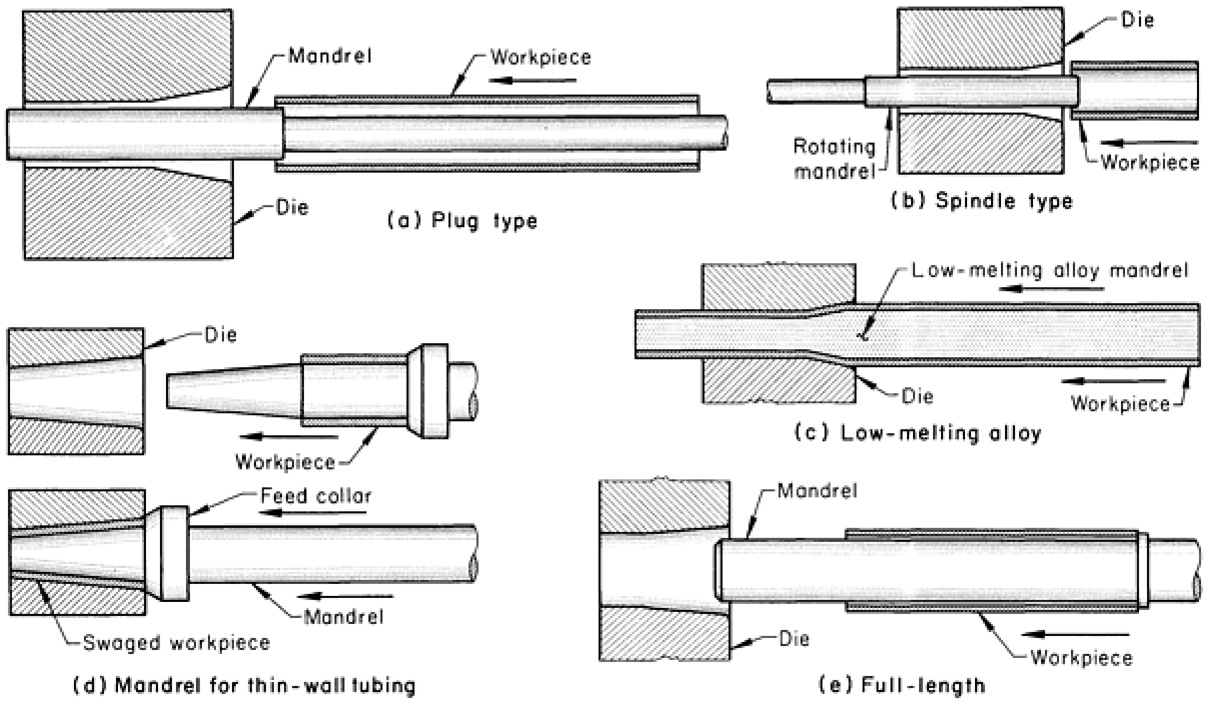


Fig. 16 Five types of mandrels most often used in the rotary swaging of tubes.

Plug-type mandrels (Fig. 16a) are fastened to a mandrel rod that is substantially smaller in diameter than the inside diameter of the tube to be swaged. The mandrel is usually about the same length as the swaging die. The mandrel is placed in the die in a fixed position, and the tube is fed over the mandrel into the swager. The mandrel and mandrel rod are removable to permit loading of the tube.

Spindle-type mandrels (Fig. 16b) are mounted on a rotating mandrel holder that permits the workpiece and mandrel to rotate independently of the machine spindle. The tube is fed into the die while the mandrel is fixed.

Low-melting alloys (Fig. 16c) are sometimes used to support thin-wall tubing during swaging. After swaging, the supporting metal is melted out.

Mandrels for thin-wall tubing (Fig. 16d) are mounted in fixed holders in front of the dies. The mandrel slides back to permit loading of the tube onto the mandrel, after which it slides forward into the die. The feed collar on the mandrel then feeds the tube into the die. Sufficient clearance between the die and mandrel is maintained to permit feeding of the workpiece into the die.

Full-length mandrels (Fig. 16e) are hardened and ground steel bars made slightly longer than the finished length of the swaged tube. The mandrel is inserted into the tube, and both are passed through the machine.

Machine Capacity. Mandrels alter the machine capacity requirement for swaging. When a mandrel is used, the workpiece must be considered a solid bar, and the selection of swaging machine should be based on its capacity to reduce solid work metal. For example, a machine with a capacity sufficient for swaging a 16 mm ($\frac{5}{8}$ in.) diam solid bar is satisfactory for swaging a 25 mm (1 in.) diam tube with a 6.4 mm ($\frac{1}{4}$ in.) wall thickness without a mandrel. However, when a mandrel must be used in the 25 mm (1 in.) tube, a machine capable of swaging a solid bar of the same diameter must be used.

Dies for mandrel swaging must have more ovality than those used for swaging tubing without a mandrel or for swaging a solid bar. Dies that have a nearly round cavity will swage a tube on a mandrel so closely that removing the mandrel is difficult. Ovality overcomes this problem. The amount of die ovality required is proportional to tube wall thickness and diameter.

Internal shapes can be produced in tubular stock by swaging it over shaped mandrels. Workpieces are generally classified as (1) those with uniform cross section along the longitudinal axis and (2) those with axial variations (such as internal tapers or steps.

Workpieces in the first category can be made from long tubular stock swaged over a plug-type mandrel. After swaging, the tube is cut into two or more pieces of the desired length. When swaging shapes with spiral angles, such as rifled tubes, the angles should not exceed 30° as measured from the longitudinal axis, although angles up to 45° have been used for some internal shapes.

Sectional views illustrating the typical internal shapes of workpieces with uniform cross section along the longitudinal axis are shown in Fig. 17. These shapes are made from tubular blanks with the inside diameter 0.5 mm (0.020 in.) larger than the largest diameter of the mandrel. In addition, the difference between the largest and smallest internal diameters of the swaged workpiece is added to the outside diameter of the swaged piece to obtain the correct blank diameter.

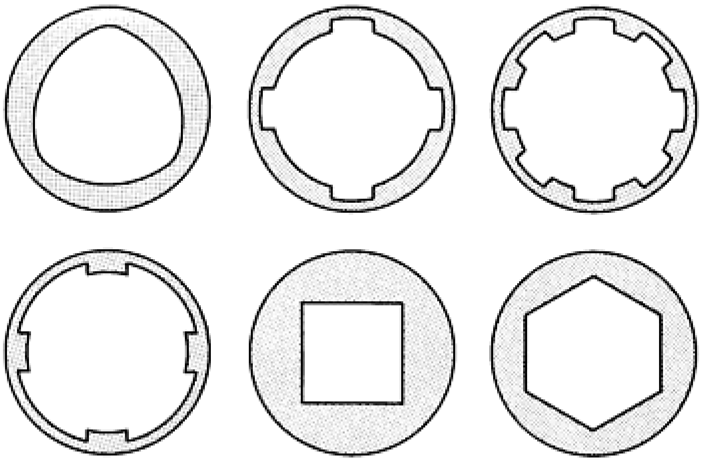


Fig. 17 Typical internal shapes produced in tubular stock by swaging over shaped plug-type mandrels.

For example, an internal 19 mm ($\frac{3}{4}$ in.) square is to be swaged into a 38 mm ($1\frac{1}{2}$ in.) OD tube. The diagonal of a 19 mm ($\frac{3}{4}$ in.) square is 27 mm (1.06 in.). Therefore, the inside diameter of the tubular blank should be 0.5 mm (0.020 in.) larger, or a total of 27.5 mm (1.08 in.). The difference between the maximum and the minimum internal diameters of the swaged piece is 27 - 19 mm (1.06 - 0.75 in.), or 8 mm (0.31 in.). Therefore, the outside diameter of the tubular blank stock should be 38 + 8 mm (1.50 + 0.31 in.), or 46 mm (1.81 in.).

To prevent breakage of the mandrel and to obtain the best tangential flow of metal, a swaging machine equipped with a four-piece die is preferred for producing internal splines in workpieces with the same cross section at any point along the axis. The dimensional accuracy of workpieces with internal splines is improved when they are swaged in a four-die setup rather than a two-die setup, because less work metal is forced into the clearances of four-piece dies. Internal squares or hexagons are less sensitive to the differences between two-piece and four-piece dies.

Figure 18 illustrates several typical work-pieces in which the internal shapes require axial variations of the cross section. Internal shapes that contain stepped contours may require preshaped blanks when the differences between the steps are large. For some shapes that terminate as blind holes, axial back pressure is required to influence metal flow during swaging.

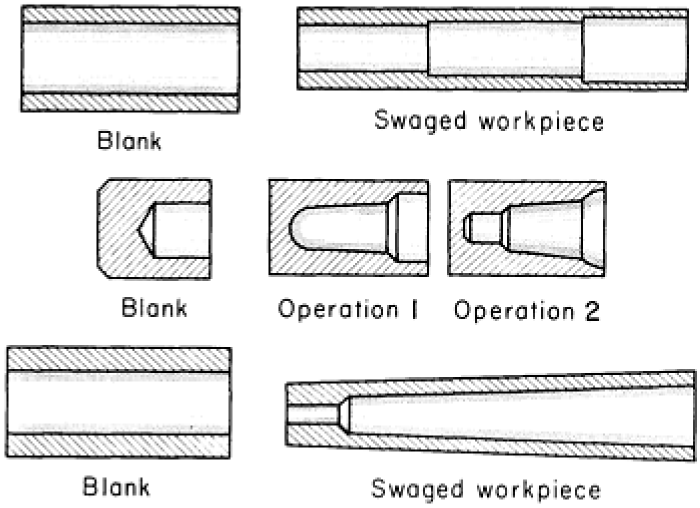


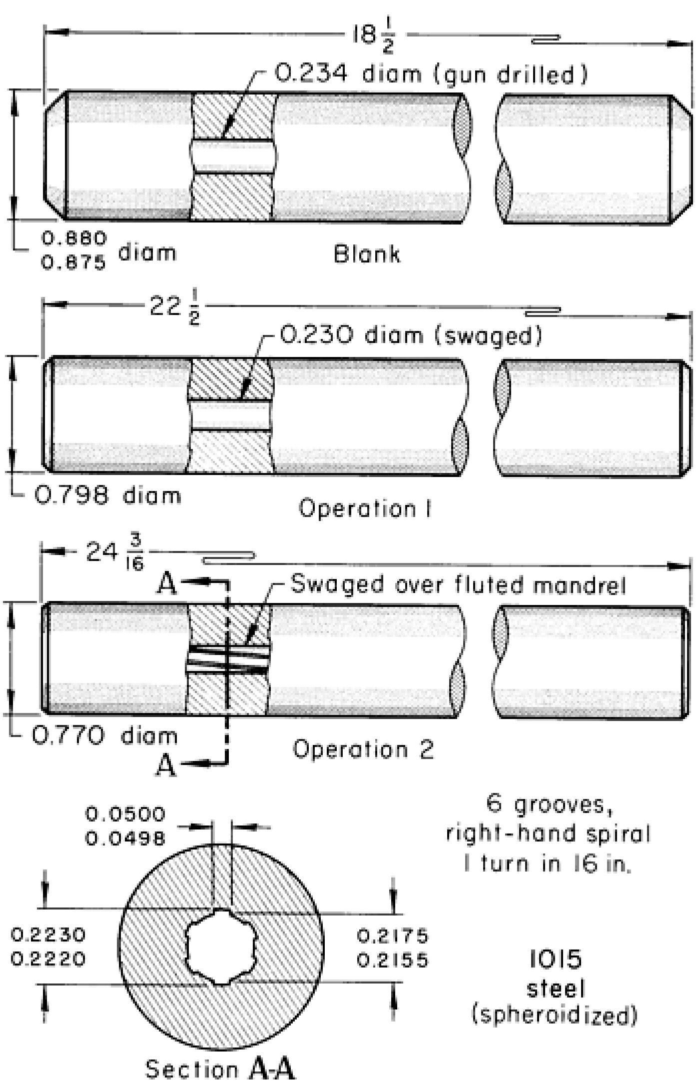
Fig. 18 Internal shapes of nonuniform axial cross section produced by swaging over a mandrel.

Gun barrels are frequently rifled by broaching. They can also be rifled by swaging with a fluted mandrel, as in the next example.

Example 2: Use of a Fluted Mandrel to Rifle the Bore of a Gun Barrel.

Gun barrels were originally produced by gun drilling 5.6 mm (0.222 in.) diam holes in 19 mm ($\frac{3}{4}$ in.) OD bar sections and then rifling the bore by broaching. After broaching, the gun barrels were turned to a 16 mm ($\frac{5}{8}$ in.) outside diameter.

By the improved method, 470 mm ($18\frac{1}{2}$ in.) long blanks (Fig. 19) were gun drilled so that their inside diameter was 5.9 mm (0.234 in.). They were then turned on centers to obtain precise concentricity between inside and outside diameters. In the first swaging operation, the workpieces were reduced in outside diameter to 20.3 mm (0.798 in.) and in inside diameter to 5.8 mm (0.230 in.), while length was increased to 570 mm ($22\frac{1}{2}$ in.) (operation 1, Fig. 19). In operation 2 (Fig. 19), a fluted mandrel was inserted to form the rifling because swaging further reduced the outside and inside diameters of the workpieces and increased the length to 615 mm ($24\frac{3}{16}$ in.).



Operating condition	**Gun drilling**	Turning
Speed, rpm	1750	**500**
Speed, sfm	343	**98**
Feed	$2\frac{3}{4}$ ipm	**0.015 ipr**
Cutting fluid	Sulfurized oil	**None**
Tool material	Carbide	**Carbide**
Setup time, min	10	**10**

Total tool life, pcs	50,000	**100,000**
Production, pcs/h	**19**	**60**

Swaging conditions	
Spindle speed	**300 rpm**
Workpiece speed	**150 rpm**
Feed	**30 ipm**
Lubricant	**None**
Setup time	**10 min**
Die life, total	**40,000 pieces**
Mandrel life, total	**50,000 pieces**
Production rate	**80 pieces per hour**
Surface finish	**Burnished**

Fig. 19 Progression of a gun-drilled and turned blank through two-operation swaging, including rifling with a fluted mandrel, to produce a gun barrel. Dimensions given in inches.

The workpieces were swaged in a $7\frac{1}{2}$ hp two-die machine capable of delivering 1800 blows per minute. Entrance taper of the die was 6° included angle, and the overall length of the die was 75 mm (3 in.). A semiautomatic hydraulic feed mechanism was used; barrels were manually placed into a spring-loaded chuck. The feed was started by the operator, and the mandrel was positioned and held in place by an air cylinder. The workpiece was hydraulically fed over the mandrel and disposed of at the rear of the machine, after which the mandrel returned ready for reloading.

The work metal for the part shown in Fig. 19 was 1015 steel, although other steels ranging from lower-carbon steels (such as 1008) to medium-carbon alloy steel have been used for gun barrels. Gun barrels are swaged from heat-treated blanks to hardnesses as high as 38 HRC.

Tool life is often the limiting factor in producing internal shapes. As the amount of reduction increases and tools (mandrels, specifically) become more delicate, swaging sometimes becomes economically impractical because of short tool life.

Lubrication between the mandrel and the workpiece is essential for most mandrel-swaging operations. Only a thin film, such as that applied with a wiping cloth, is used on the mandrel. The tube and dies are generally wiped clean before the operation begins (see the section "Lubrication" in this article).

Effect of Reduction

Reductions by swaging are limited by machine size; available feed force; die angle and feed rate, which affect the feed force; and the material and its metallurgical condition. Spheroidize-annealed plain carbon steels and other ductile alloys can be swaged to over 40% reduction in area. For larger reductions, however, stress-relief annealing between reductions may be necessary to achieve a crack-free product.

Internal and external surface finishes generally improve with increasing reduction. Figure 20 illustrates the improvement in inside diameter surface finish achieved on tubes by swaging at 20 and 40% reductions in area.

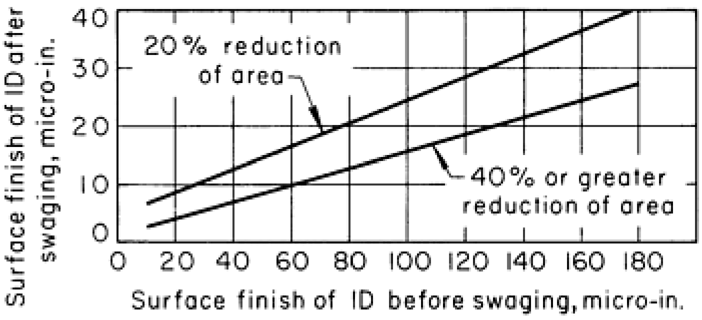


Fig. 20 Correlation between original and swaged surface finishes on the inside diameters of tubes for two different reductions.

Effect of Feed Rate

The feed rate used for rotary swaging may range from 250 to 5000 mm/min (10 to 200 in./min). A common feed rate is approximately 1520 mm/min (60 in./min). The extremely low rate of 250 mm/min (10 in./min) has been used when swaging internal configurations from tubing or for tubing having a diameter to wall thickness ratio of 35 or more. Swaging of simple tapers on an easily swageable material can be performed at feed rates as high as 5000 mm/min (200 in./min).

In general, high feed rates have an adverse effect on dimensional accuracy and surface finish. A spiral pattern on the workpiece surface suggests excessive feed rates.

Effect of Die Taper Angle

In rotary swaging, the angle of the taper at the die entrance influences the method used to feed the workpiece into the die. When the included angle is less than 12°, manual feeding is practical for cold swaging. When the included angle of the die entrance taper is 12° or more, power feeding is required.

Steep die surface angles produce inferior surface finishes and require greater feed force. Steep tapers, therefore, may increase cycle time. Consequently, it may be more cost effective to perform the desired reduction in two passes, first with a shallow taper and then with a steeper taper die or a die-closing swage, rather than in one pass with a steep taper.

Rotary Swaging of Bars and Tubes

Revised by the ASM Committee on Rotary Swaging[*]

Effect of Surface Contaminants

Residues from drawing lubricants, oxides, scales, paint, and other surface contaminants should be removed before swaging. Such contaminants retard feeding of the workpieces into the swager and load the dies and other moving components of the swager.

Abrasive cutoff wheels should not be used in the preparation of tubular products, because abrasive dust from the wheels is detrimental to the swaging dies and to the machine. Although the abrasive dust can be removed from the outside surface of the tube if enough clean wiping cloths are used, it may be difficult to remove the dust from the inside surface and cut edge of the tube.

The workpiece must be cleaned before swaging. Standard cleaning procedures can be used.

Rotary Swaging of Bars and Tubes

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Lubrication

The adverse effect of lubrication on feeding conditions eliminates the use of lubricants in many swaging operations (except between mandrels and workpieces). The main disadvantage in using lubricants is that excessive feedback can occur, especially when dies have a steep entrance angle (generally, more than 6°. Feedback cannot be tolerated in manual feeding. An automatic feed must be sufficiently rigid and powerful to overcome this reaction.

A lubricant can usually be employed when the included entrance angle of the dies does not exceed 6°. If a lubricant can be used, a better surface finish and longer tool life generally result.

Lubricants include oils specifically formulated for swaging operations, phosphate conversion coatings, molybdenum disulfide, and Stoddard solvent. Stoddard solvent is a colorless refined petroleum product that is especially useful for swaging aluminum.

Mandrel lubricants must be used during mandrel swaging to prevent seizure between the work and the mandrel. It is important to select a mandrel lubricant that will adhere to the mandrel and to use the correct amount so that it does not drip into the dies during the swaging operation. Most mandrel lubricants have this adherent quality. The lubricant selected must not contaminate the blade and entrance section of the die by forming gummy residues, because the dies must be kept clean. Resistance to heat is also desirable for mandrel lubricants.

When a mechanical feed and ample power are used, lubricants on the work can enhance surface finish and die life, regardless of the entrance angle of the dies. With manual feeding, lubricants on the outside of the work present a hazardous feeding condition.

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Dimensional Accuracy

Dimensions that can be maintained in the normal swaging of steel products in a wide range of sizes are listed in Table 4. These dimensional tolerances apply to solid bars and to tubes swaged over a mandrel. The tolerances listed in Table 4 apply only to the main sections of swaged workpieces. Dimensions at the ends of swaged sections will vary because metal flow is greater, causing the ends to be slightly bell-mouthed. When uniform dimensions are necessary throughout the entire length of the workpiece, suitable allowances must be made for cutting off the ends of the swaged workpiece. For swaging to close tolerances, the workpiece must be within the capacity of the machine, and the work metal must be as ductile as possible to prevent springback to a larger diameter than required.

Table 4 Tolerances on diameter for swaging solid bar stock or for swaging tubing over a hardened mandrel

Nominal outside diameter		Tolerance	
mm	in.	mm	in.
1.6	$\frac{1}{16}$	±0.025	±0.001
3.2	$\frac{1}{8}$	0.05	0.002
6.4	$\frac{1}{4}$	0.075	0.003
12.7-25.4	$\frac{1}{2}$-1	0.13	0.005
51-76	2-3	0.18	0.007
76-114	3-4$\frac{1}{2}$	0.25	0.01
114	4$\frac{1}{2}$	0.38	0.015

Note: Data were compiled using low-carbon steel samples, but are generally applicable to other swageable metals. Tolerances apply only to main sections of workpieces and are based on a feed rate of 1520 mm/min (60 in./min). Tolerances given here can be reduced by about 50% by reducing feed rate to 760 to 1015 mm/min (30 to 40 in./min).

Tolerance for cold-swaged tubular products can be held to closer limits than the tolerances applicable to the outside diameter of standard tubing. The inside diameter, however, cannot be held as close, because of variations in the original wall thickness and because the wall thickens during swaging. When a tube is swaged without a mandrel or without prior reaming, the tolerance for the inside diameter should be twice that for the outside diameter. An exception is welded tubing made from flat stock held to close tolerances on thickness and width. The dimensional accuracy of the inside diameter can be greatly improved by using a mandrel.

Surface Finish

In general, rotary swaging improves the surface finish of the workpiece. The finishes produced are comparable to those obtained in cold-drawing operations.

Swaging in a squeeze-type machine usually causes a distinct spiral pattern on the outside surface of the workpiece. The pitch of the spiral increases as the rate of axial feed increases and as the relative rotation between the die and workpiece decreases. The intensity of the pattern on the inside surface depends on wall thickness. As the wall thickness increases, the spiral pattern gradually fades out. The surface finish of the inside diameter is related to the surface finish before swaging, the surface finish of the swaging mandrel, the amount of reduction, feed rate, rotational control of the tube during swaging, the lubricant employed, and the mechanical characteristics of the work metal.

Figure 20 correlates the surface finish on the inside diameter of tubes before and after swaging to reductions of 20 and 40%. The values shown are based on tooling that was axially polished to a finish of 0.05 to 0.1 μm (2 to 4 μin.) and on the use of a lubricant that was capable of preventing metal pickup. The higher reduction resulted in a finer surface finish on the inside diameter.

These data were obtained from several different tube materials. Starting material was as-received--sometimes seamless tubing that was pickled and sometimes as-welded tubing. This accounts for the range of finish on the inside diameter before swaging.

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Swaging Versus Alternative Processes

There are numerous applications for which swaging is the best method of producing a given shape and is therefore selected regardless of the quantity to be produced. Conversely, there are many workpiece shapes that can be successfully produced by swaging, but can be produced equally well by other processes, such as press forming, spinning, and machining. Applications comparing swaging with alternative processes are described in the following examples.

Example 3: Swaging Versus Press Forming.

The ferrule illustrated in Fig. 21 was originally produced in a press by drawing disks into cups, redrawing to form the taper, and trimming the ends. With this procedure, 500 ferrules per hour were produced.

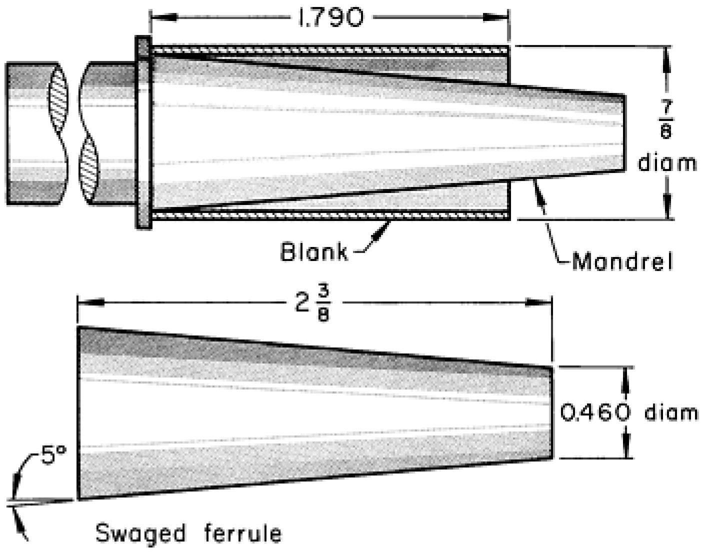

Fig. 21 Swaging a ferrule from tube stock (alloy C26000, cartridge brass, quarter hard, 0.032 in.) in preference to press forming. The change from press forming to swaging lowered tooling costs and resulted in a 50% increase in production. Dimensions given in inches.

The improved method consisted of cutting the blanks from tubing, then swaging them in a 5 hp two-die rotary machine. Dies with an included taper angle of 9° 56' and 0.13 mm (0.005 in.) ovality were used. The production rate was increased to 750 pieces per hour.

Example 4: Swaging Versus Spinning.

Blades for high-voltage switches were swaged from annealed copper tubes (Fig. 22) in three operations using a two-die rotary machine. Each die was 197 mm ($7\frac{3}{4}$ in.) long, 180 mm ($7\frac{1}{8}$ in.) wide, and 127 mm (5 in.) high. The tapered section in each die had a 15° included angle, and side clearance was used instead of ovality. Tubes were fed into the swager by a hydraulically actuated carriage on a long track. An intermediate steady rest moved along the track to help maintain tube alignment.

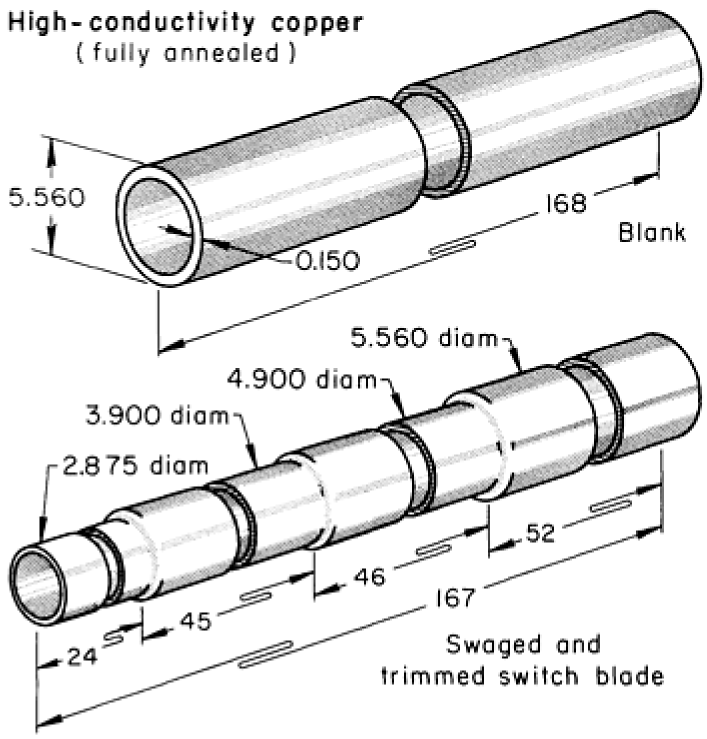

Fig. 22 High-voltage switch blade (bottom) that was swaged from tube stock (top) in three operations. Previously, the part was produced by spinning. Dimensions given in inches.

In the first operation, the tube was swaged through a 124.5 mm (4.900 in.) die up to the first step. In the second operation, a tube length of 1140 mm (45 in.) was swaged to a 99 mm (3.900 in.) outside diameter, and in the third operation, the end portion was swaged to a 73 mm (2.875 in.) outside diameter. In a final operation, the large end was trimmed to obtain an overall workpiece length of 4.2 m (167 in.).

Formerly, these blades had been produced by spinning 4.23 m (168 in.) lengths of annealed copper tubing 73.025 mm (2.875 in.) in outside diameter by 63.5 mm (2.5 in.) in inside diameter. By changing to swaging, production cost was reduced 10%. Swaging provided two additional benefits. First, the center of rotation was shifted toward the large diameter of the workpiece, thus reducing the number of counterweights required to balance the switch blade when in operation, and second, the small end received the most cold work, thus strengthening this portion to the desired condition.

Example 5: Swaging Versus Turning.

The tapered workpiece illustrated in Fig. 23 was originally produced by lathe turning, at the production rate of only 200 pieces per hour. A substantial loss of work metal as chips made this method impractical.

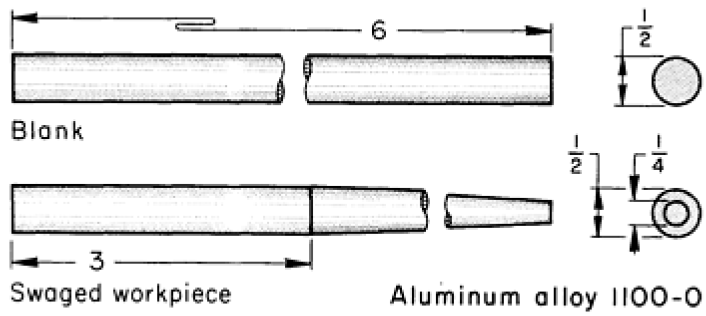

Fig. 23 Tapered aluminum workpiece that was produced by swaging without metal loss. Production increased from 200 to 1200 pieces per hour when the part was fabricated by swaging rather than lathe turning.

Dimensions given in inches.

By changing to swaging, it was possible to produce 1200 pieces per hour with no loss of metal. The operation was performed in a $7\frac{1}{2}$ hp rotary swager using dies with an overall length of 162 mm ($6\frac{3}{8}$ in.), 1° taper, and side clearance (no ovality). An inside spindle stop fastened to a straight rod mounted in and rotated with the spindle allowed adjustment by means of a screw at the rear of the spindle. The work blanks were hand fed, and no special holder or feeding mechanism was used. Additional operating details are listed with Fig. 23.

Swaging Combined With Other Processes

In some applications, the most practical method of producing a given workpiece is to combine two or more processes. Combined processes are used to increase the rate of production, to avoid otherwise costly tooling, to decrease or eliminate the loss of work metal, to provide closer dimensional tolerances, or to provide improved surface finish. The following examples describe applications in which the above advantages influenced the decision to combine machining operations with swaging operations.

Example 6: Combination Turning and Swaging for Increased Production.

The firing pin shown in Fig. 24 (lower view) was originally produced by turning in an automatic lathe at a rate of 60 pieces per hour. Not only was the rate of production unacceptably low, but the required tolerance of ±0.05 mm (±0.002 in.) could not be met consistently. In addition, the finish-turned workpieces showed tool marks.

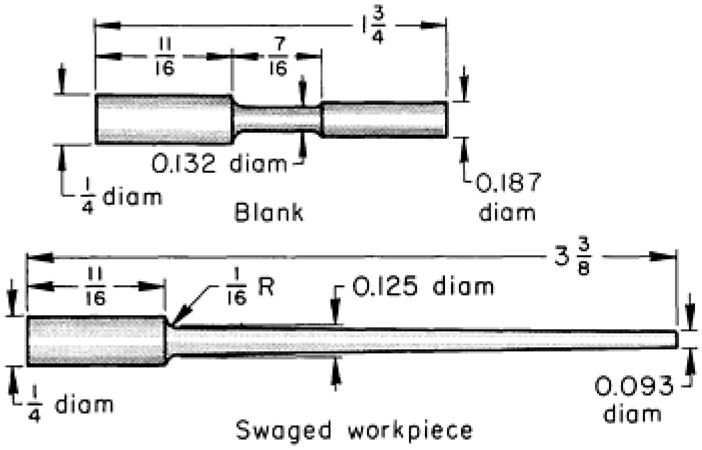

Fig. 24 Rough-turned blank for a firing pin (top) and pin that was produced from the blank by swaging (bottom). Production rate increased more than 200% when the pin was produced by turning and swaging rather than by turning alone. 3140 steel, 85 to 90 HRB. Dimensions given in inches.

The above conditions were improved by rough turning the 3140 steel blank (upper view, Fig. 24) in an automatic lathe and then swaging the blank to the firing pin shape. With this procedure, 180 pieces per hour were produced on the automatic lathe and 300 pieces per hour on the swager (two passes per piece). Other improvements that resulted from the change in method were closer tolerance (±0.025 mm, or 0.001 in.), a burnished finish, and a metal saving of 22%.

The blanks were swaged in a 5-hp rotary swager using dies designed with 30° side clearance and no ovality. The first die had a blade length of 30 mm ($1\frac{5}{16}$ in.); the second a 50 mm (2 in.) blade length.

Example 7: Combining Drilling and Mandrel Swaging to Produce 0.9 mm (0.036 in.) diam Holes.

The copper blank shown in Fig. 25 was produced by drilling six 3.2 mm (0. 125 in.) diam holes in bar sections 17.5 mm ($\frac{11}{16}$ in.) in outside diameter by 89 mm ($3\frac{1}{2}$ in.) long. After drilling, six 0.9 mm (0.036 in.) diam mandrels were inserted into the holes, and the blank was swaged to increase its length to 102 mm (4 in.) to reduce its outside diameter to 15.8 mm ($\frac{5}{8}$ in.) and to reduce the holes to 0.09 mm (0.036 in.) in diameter. The mandrels were withdrawn after swaging.

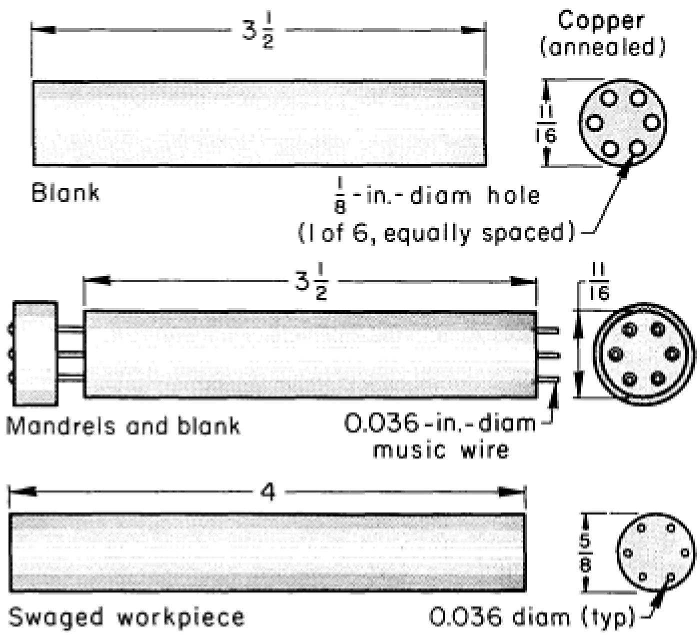

Fig. 25 Blank with drilled holes (top) that was swaged over music wire mandrels (center) to increase length and to reduce outside diameter and hole diameter (bottom). Dimensions given in inches.

The blank was drilled in a specially built horizontal machine and was swaged in a rotary swager using manual feed. The dies had 0.25 mm (0.010 in.) ovality and an included entrance angle of 8°. Overall length of the die was 75 mm (3 in.); blade length was 32 mm ($1\frac{1}{4}$ in.).

Rotary Swaging of Bars and Tubes

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Special Applications

The difficulties of attaching terminals and fittings to cables by welding or soldering are often overcome by the use of swaging. Four types of swaged attachments are illustrated in Fig. 26. The plain ball swaged in position (Fig. 26a) will resist movement from a force equal to 80% of the rated breaking strength of the cable. The ball with single shank (Fig. 26b) is used when the load stress is applied in one direction only. The ball with double shank (Fig. 26c) is used when load stress is applied in opposite directions. In Fig. 26(d) and 26(e), the plain shank terminal is assembled on the cable and staked in position before swaging.

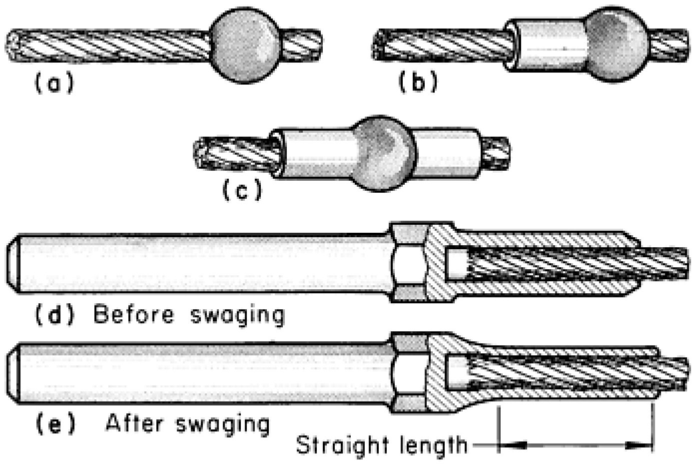

Fig. 26 Four types of terminals that can be attached to cables by rotary swaging. (a) Ball swaged in position. (b) Ball with single shank. (c) Ball with double shank. (d) Shank terminal before swaging. (e) Shank terminal after swaging.

Swaging can also be used to form wire or tubing from metals that are not strong enough to be formed completely by wire drawing or tube drawing. Solder, for example, can be reduced only about 10% in cross-sectional area by wire drawing, but a reduction of up to 60% can be obtained by swaging.

Swaging is applicable to the forming of small-diameter thin-wall shells that are difficult to make by drawing in presses. Shells can be drawn in presses provided the drawing force does not exceed the tensile strength of the material. If the tensile strength is exceeded, the bottom of the shell will be pushed out. This factor limits the length and wall thickness to which small-diameter shells can be formed by drawing. In swaging, the length of shell that can be produced is limited only by the ability of the wall to withstand thinning.

Rotary Swaging of Bars and Tubes

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Hot Swaging

Hot swaging is used for metals that are not ductile enough to be swaged at room temperature or for greater reduction per pass than is possible by cold swaging. The tensile strength of most metals decreases with increasing temperature; the amount of decrease varies widely with-different metals and alloys. The tensile strength of carbon steels at 540 °C (1000 °F) is approximately one-half the room-temperature tensile strength; at 760 °C (1400 °F), about one-fourth the room-temperature strength; and at 980 °C (1800 °F), about one-tenth the room-temperature strength. In practice, reductions greater than those indicated in Table 1 are sometimes possible by cold swaging without intentionally heating the work metal, because sufficient heat is generated during swaging to cause a substantial decrease in strength and increase in the ductility of the work metal.

The decrease in strength at elevated temperature does not make possible unlimited reductions at high temperatures. Because of the design and capabilities of swaging machines, the work metal must be strong enough to permit feeding of the workpiece into the machine. When the work metal has lost so much of its strength that it bends rather than feeds in a straight line, chopper dies must be used (Fig. 6). This type of die limits the reduction in area to 25% regardless of work metal ductility. The temperature to which a work metal is heated for swaging depends on the material being swaged and on the desired reduction per pass.

Alloy steels harder than 90 HRB are difficult to cold swage and can cause premature failure of the dies and machine components. Hot swaging should be considered for these steels. For metals that work harden rapidly and require intermediate annealing during cold swaging, hot swaging is often more economical.

Tungsten and molybdenum must be worked at elevated temperature (900 to 1605 °C, or 1650 to 2925 °F, for tungsten; 605 to 1425 °C, or 1125 to 2600 °F, for molybdenum) because of their low ductility at room temperature. A tungsten ingot is usually swaged to about 3.2 mm ($\frac{1}{8}$ in.) in diameter, although it can be swaged to a diameter of 1 mm (0.040 in.). After this, the ingot is ductile enough to be hot drawn. The procedure for swaging molybdenum is essentially the same as for tungsten.

Equipment for Hot Swaging. All machines employed for cold swaging can be used for hot swaging by incorporating either a water jacket or a flushing system. A water jacket is simply a groove in the bore of the swager head in the area of the inside ring. The groove is connected to a continuous water supply to dissipate heat.

A flushing system introduces a cooling compound at the upper rear of the head. The compound is pumped through the machine and exits at the lower front, from which it flows by gravity through a water cooler before entering the supply tank. This tank is equipped with a filter through which the cooling medium passes before re-entering the machine.

In addition to cooling, the flushing system removes accumulated foreign matter and lubricates the working parts of the swager. Although flushing removes foreign substances such as scale and sludge, the method used for heating the workpiece should produce the least possible oxidation.

Dies for hot swaging must be made of material that will resist softening at elevated temperature. High-speed steels and cemented carbides are satisfactory materials for hot swaging dies.

A common production procedure for hot swaging is the tandem arrangement of several swagers, each of which is equipped with a heating furnace in front of the machine and close to the dies. The furnaces are mounted so that they can be pushed aside for quick changing of the dies. Drag rolls are mounted at the rear of each swager to pull the workpiece through the furnace and the machine. Each drag roll mechanism is equipped with a variable-speed drive to regulate the rate of feed into the swaging machine. Feed for this type of operation ranges from 1520 to 6000 mm/min (5 to 20 ft/min).

Lubrication. In addition to preventing seizure between the dies and the workpiece, lubricants minimize wear of the backers, shims, dies, spindle side plates, back plates, rolls, and swager gate. However, the flow of the lubricant must be controlled to prevent excessive cooling of the workpiece. Lubricants used for hot swaging should be free from chlorine and sulfur.

Material Response

In addition to the effect of inclusions and high initial hardness on promoting fracture during swaging, the cold-swaged products may exhibit unanticipated mechanical properties--for example, reduced hardness, reduced yield stress, and either growth or constriction of the tube inside diameter after machining of the outside diameter. These unanticipated properties have been attributed to the Bauschinger Effect (that is, a reduction of the yield stress following a stress reversal) and to residual stress.

Decreasing yield stress with continued reduction, to a minimum at 20 to 30% area reduction, has been observed during the swaging of rifle barrels. At higher reductions, the yield stress continued to increase.

Radial hardness variations have been observed after tube swaging over a mandrel. The difference between the highest and lowest readings was 8 Rockwell C points, and the readings were typically equal to or less than the original blank hardness. After a low-temperature stress-relief treatment (10 °C, or 50 °F, below the tempering temperature of the steel blank), the swaged tubes had hardnesses greater than the original heat-treated blank by up to 3 Rockwell C points, which would be expected for a 20% area reduction.

Residual stress after cold tube swaging can be controlled by tool design. For example, the same product could be produced with either compressive or tensile residual stresses at the inside diameter or negligible residual stress throughout the product. The significant tool design parameters affecting residual stress are ovality, die angle, blade length, reduction in area, and secondary reductions (small, usually less than 0.05% area reductions near the start of the die exit relief), usually on the die. Ovality (expressed as percent overgrind of the final product diameter relative to the ground die inside diameter) in four-die tube swaging is the most significant parameter affecting residual stress, as shown in Fig. 27. The data in Fig. 27 show the dependence of the outside diameter (diametral expansion) on percent overgrind for 7.9 mm (0.31 in.) ID tubes produced by swaging 33 mm (1.300 in.) OD tubular blanks. The OD expansion was measured after electrochemically machining the 7.9 mm (0.31 in.) inside diameter to 14.2 mm (0.560 in.) and was accompanied by axial expansion. The data for diametral expansion are indicative of the magnitude of residual stress existing in the swaged tubes and were subsequently related to changes in the inside diameter upon machining of the outside diameter.

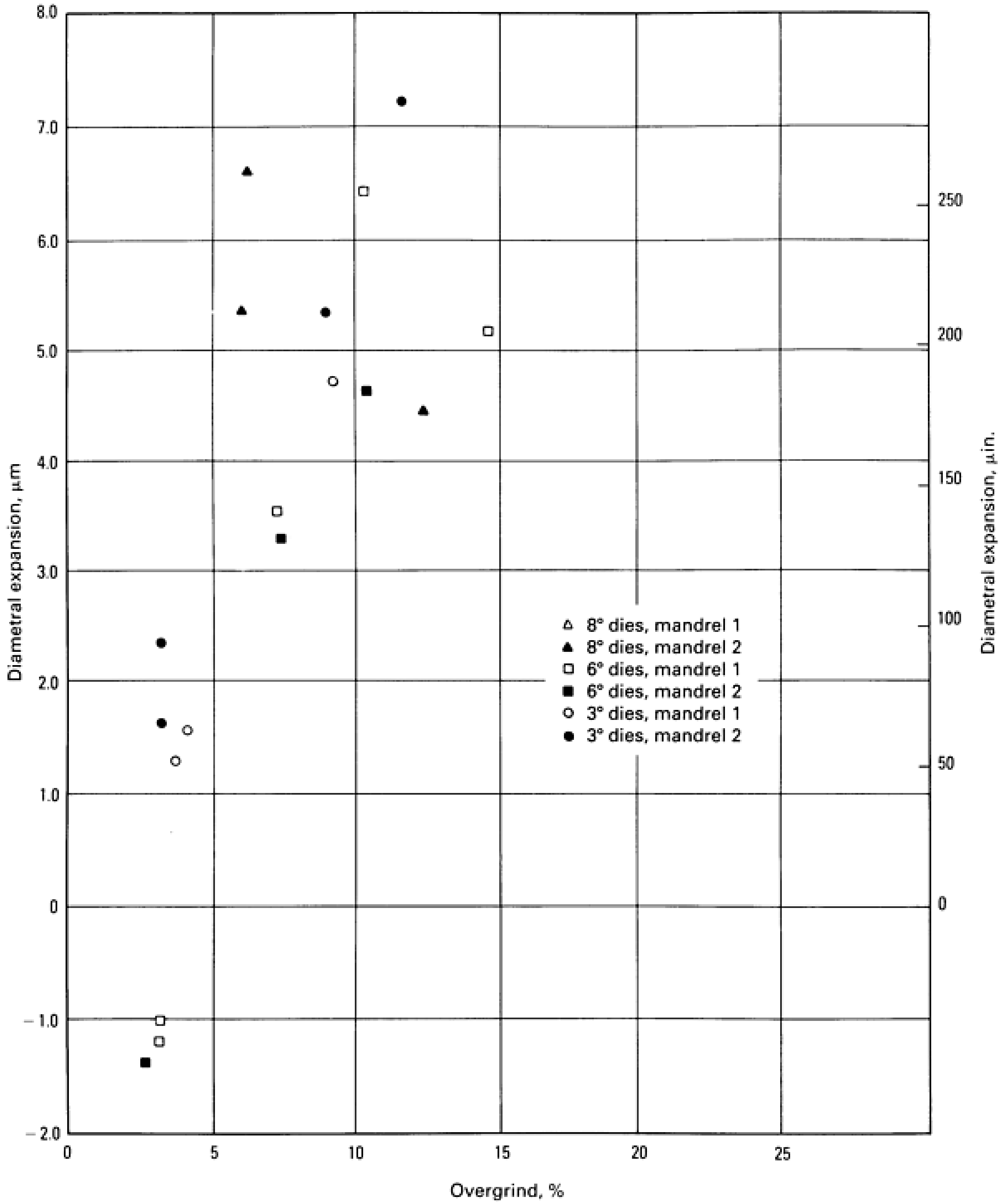


Fig. 27 Dependence of diametral expansion on overgrind.

The data in Fig. 27 were obtained from tubes swaged with 3, 6, and 8° (one-half the included angle) dies and two mandrel designs. Mandrel 1 was a conventional straight mandrel, and mandrel 2 was a tapered mandrel that expanded to 0.025 mm (0.001 in.) outside the die exit relief. The maximum residual stresses were in the range of ±550 MPa (80 ksi), or ±60% of the yield stress of the blank.

Noise Suppression

The noise from rotary swaging operations is so great that special protection of the operator is required. Noise intensity of the average swager in a range of up to 20 hp is about 93 to 95 dB at frequencies of 1000 to 3000 Hz. For most factory conditions, a level no higher than 85 dB should be permitted.

Methods of protecting personnel from excessive noise include the following:

- Earmuffs are effective, but are uncomfortable to wear for long periods
- Earplugs are fairly effective, but can cause ear infections
- Machines can be insulated during manufacture. The use of such insulation can decrease noise to an acceptable level
- Housing the machine is the most effective method of controlling noise. The housing can consist of a wooden frame covered inside and out with 12.7 mm ($\frac{1}{2}$ in.) thick fiberboard insulation

Machines placed on floors above the other work areas should have vibration dampers under the base. Vibration dampers for machines mounted on ground-level floors have no effect on noise levels in the surrounding area if the floors are soundly built.

Rotary Swaging of Bars and Tubes

Revised by the ASM Committee on Rotary Swaging*

Swaging Problems and Solutions

Some of the problems that are commonly encountered in swaging operations include difficult feeding; workpieces with roughened surfaces after swaging; peeling, cracking, and wrinkling of workpieces; sticking in dies and on mandrels; and breaking mandrels. The causes of these problems and suggested solutions are presented in Table 5.

Table 5 Some swaging problems, potential causes, and possible solutions

Problem	**Potential causes**	Solutions
Difficult feeding	Work material too hard	**Anneal or stress relieve to remove effects of cold working.**
	Work material too oily or greasy	**Thoroughly clean workpiece and die grooves.**
	Backer bolt setting improper	**Reset backer bolts so that dies will open one or two thousandths of an inch for each degree of included angle of the die entrance taper.**

	Die entrance too small	**Enlarge die entrance.**
	Steps worn in die taper	**Replace or remachine dies.**
	Inadequate side clearance	**Increase side clearance.**
Work has rough surface	Inadequate side clearance	**Increase side clearance.**
	Work sticks to die entrance taper	**Wipe every fourth or fifth workpiece with graphite or molybdenum disulfide powder.**
	Too much die opening	**Reset machine with proper shims.**
	Dirt and scale in die	**Clean dies and remove loose scale and other contaminants from workpiece.**
Peeling	Die groove too long	**Shorten die groove.**
	Excessive pressure within die groove	**Decrease length of work in the dies with respect to diameter (swaging length should not exceed 10 times the workpiece diameter).**
Cracking of tubing	Material too hard	**Stress relieve or anneal before swaging.**
	Inside surface may have lines or scratches that become cracks as tubing is swaged.	**Improve ID surface finish.**
	Excessive ovality	**Rework dies to remove all ovality; use side clearance only.**
Cracking of bar stock	Seams or pipes in work metal	**Upgrade work metal quality.**
	Material too hard	**Anneal or stress relieve.**
	Excessive reduction per pass	**Reduce amount of reduction; stress relieve between passes.**
Wrinkling or corrugating of tubing	Tube OD more than 30 times wall thickness	**Use a mandrel that is within the solid material capacity of the machine.**
	Feed too fast	**Decrease feed rate.**
	Excessive ovality	**Use round die groove.**

	Material too hard	**Stress relieve or anneal.**
Work sticks in dies and rotates with swager spindle	Side clearance of both taper and blade of die inadequate	**Increase side clearance.**
	Workpiece is crooked.	**Straighten workpiece.**
Workpiece sticks to mandrel	Inadequate ovality	**Increase ovality.**
	Inadequate lubrication	**Use proper lubricant.**
	Mandrel improperly hardened, causing flat spots or sinks	**Be sure mandrel is in correct metallurgical condition.**
Mandrel breaks	**Mandrel material not suited to high shock**	**Use proper mandrel material.**

Radial Forging

Hans Hojas, Gesellschaft für Fertigungstechnik und Maschinenbau mbH

Introduction

RADIAL FORGING was first conceived in Austria in 1946. The first four-hammer machine was built in Austria in the early 1960s. Since then, machine capacities and the number of applications for radial forging have continued to increase. More than 400 radial forging machines have been installed around the world, with maximum forging forces per die of up to 30 MN (3400 tonf) (Table 1).

Table 1 Sizes and capacities of four-hammer radial forging machines

Proprietary designation	Largest possible starting size for steel work metal				Smallest size forgeable for bar materials				Maximum length of finished workpiece		Maximum forging force per die		Number of blows per minute
	Round (diameter)		Square		Round (diameter)		Square						
	mm	in.	mm	in.	mm	in.	mm	in.	m	ft	MN	tonf	
SX-10	100	4	90	3.5	30	1.2	35	1.4	5	16.5	1.25	140	900
SX-13	130	5	115	4.5	35	1.4	40	1.6	6	20	1.6	180	620
SX-16	160	6	140	5.5	40	1.6	45	1.8	7	23	2	225	580
SX-20	200	8	175	7	50	2	50	2	8	26	2.6	300	480
SX-25	250	10	220	8.7	60	2.4	60	2.4	8	26	3.4	380	390
SX-32	320	12	290	11.5	70	2.8	70	2.8	8	26	5	560	310
SX-40	400	16	360	14	80	3.2	80	3.2	10	33	8	900	270
SX-55	550	22	480	19	100	4	100	4	10	33	12	1350	200
SX-65	650	26	570	22.5	120	4.8	120	4.8	12	40	17	1900	175
SX-85	850	34	750	29.5	140	5.5	140	5.5	18	60	30	3400	143

Radial forging is sometimes confused in the literature with rotary (orbital) forging. In the rotary forging process, the axis of the upper die is tilted at a slight angle with respect to the axis of the lower die, and one or both dies rotate. Additional information is available in the article "Rotary Forging" in this Volume.

Radial forging was initially used for the hot forging of small parts and for the cold forging of tubes over mandrels. Current applications include:

- Bars with round, square, or rectangular cross section starting from ingots or blooms
- Stepped solid shafts and axles for locomotives, railroad cars, and trucks
- Stepped hollow shafts for components in the automotive and aircraft industries
- Preforms for turbine shafts or for subsequent closed-die forging
- Thick-wall tubes forged over a water-cooled mandrel
- Necks and bottoms of steel bottles
- Couplings and tool joints

Figure 1 illustrates typical parts formed by radial forging.

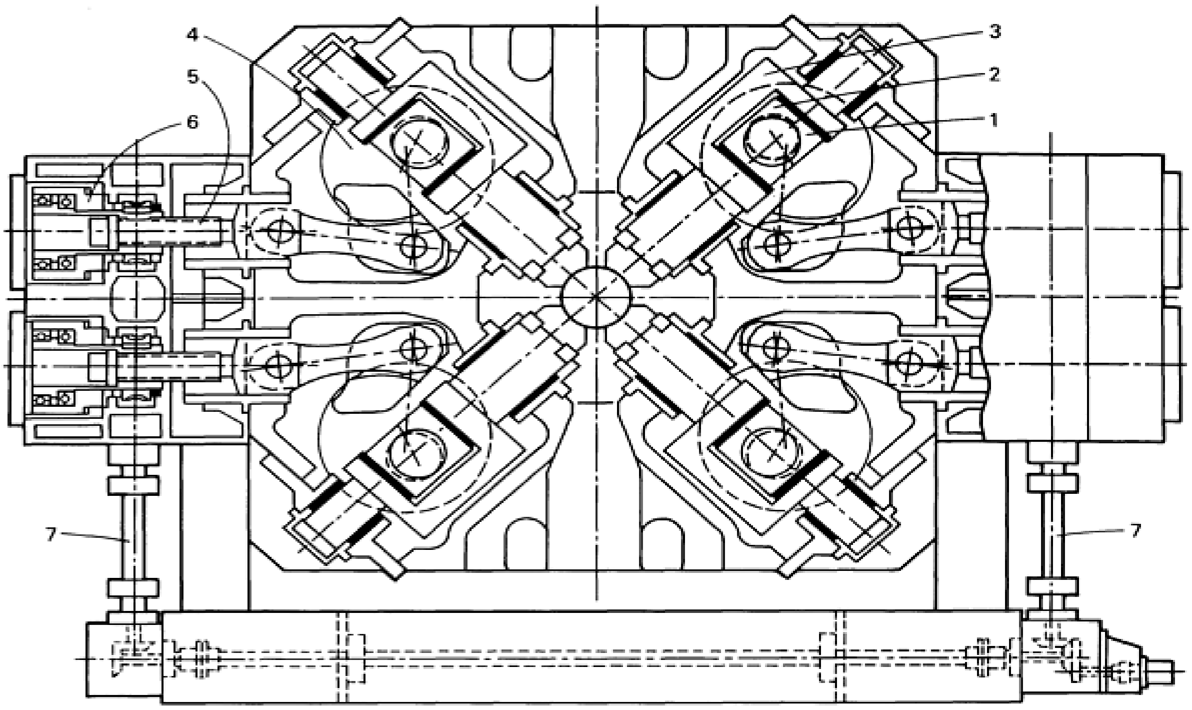
Fig. 1 Typical parts formed by radial forging.

Radial Forging

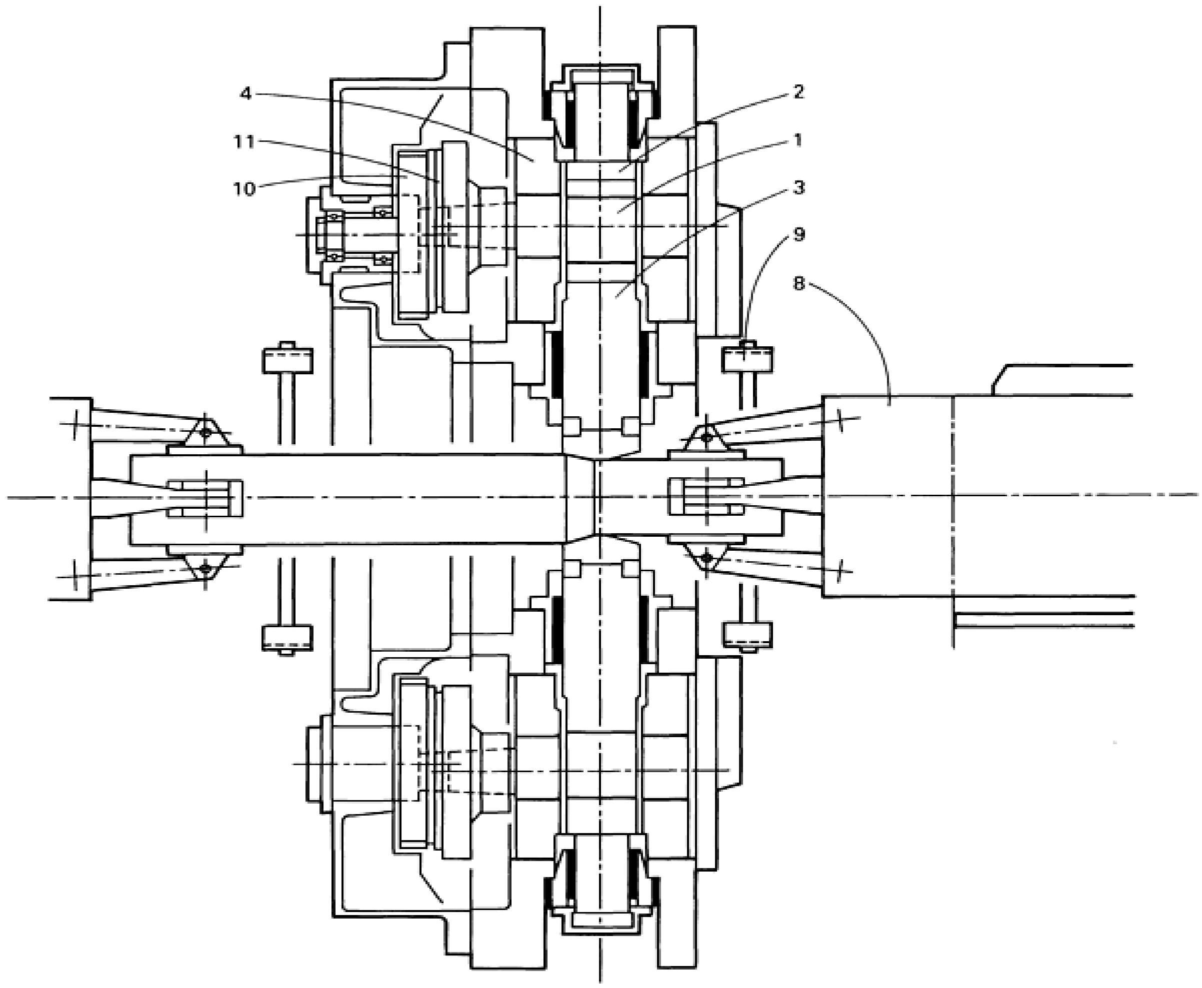
Hans Hojas, Gesellschaft für Fertigungstechnik und Maschinenbau mbH

Equipment and Process

The four-hammer radial forging machine (Fig. 2) is basically a short-stroke mechanical press. The stroke of the forging connecting rods is initiated through eccentric shafts. The eccentric shafts are supported in housings that allow adjustment of the stroke position of the four forging connecting rods. One or two electric motors drive the eccentric shafts through a drive gear, which simultaneously controls the synchronization of the four eccentric shafts. The forging connecting rods can be changed in their stroke position either in unison or in pairs so that round, square, or rectangular cross sections can be forged.



(a)



(b)

Fig. 2 Schematic of four-hammer radial forging machine with mechanical drive. (a) Cross section through

forging box. (b) Longitudinal section through forging box. 1, eccentric shaft; 2, sliding block; 3, connecting rod; 4, adjustment housing; 5, adjusting screw; 6, hydraulic overload protection; 7, hammer adjustment drive shafts; 8, chuckhead; 9, centering arms; 10, clutch; 11, clutch disk.

Depending on its application, the part-handling system of the machine can be equipped with either one or two workpiece manipulators, which differ widely from conventional forging manipulators. In contrast to press or hammer forging, the workpiece axis in radial forging is always maintained on the forging machine centerline, regardless of the diameter. The manipulator moves only in the longitudinal direction. In order to achieve exact guidance, the chuckhead slides on a machine bed. During the forging of round cross sections, the chuckhead rotates the workpiece in cycle with the forging hammers; that is, the rotary movement will be stopped during the time the hammers are in contact with the workpiece. The rotary movement of the chuckhead spindle is synchronized with the hammer blows; therefore, twisting of the workpiece is eliminated. The indexing positions of the chuckhead spindle required for forging squares, rectangles, or hexagons can be set automatically.

In radial forging, the entire forging process, including loading and unloading, can be performed automatically by computer numerical control (CNC). The forging process is no longer dependent on the discretion of the operator, and an optimal forging program is maintained in an unchanged manner. This guarantees the manufacture of uniform forged pieces, which are trimmed to optimal machining allowances. These workpieces are well suited to subsequent machining on CNC lathes because of their consistent dimensional accuracy.

The technology of the four-hammer forging machine differs from that of all other hot-forming methods. Conventional presses and hammers, or even rolling mills, use only two tools per forming operation. In the radial forging machine, however, a workpiece is formed at the same time by four hammers arranged in one plane (Fig. 3). The free spreading that occurs between the two contacting tools in all conventional forging methods is eliminated. A radial press contacts the circumference of the workpiece equally and puts the entire surface of the workpiece under compressive stresses. These compressive stresses prevent the formation of surface cracks during the forging process and improve existing defects.

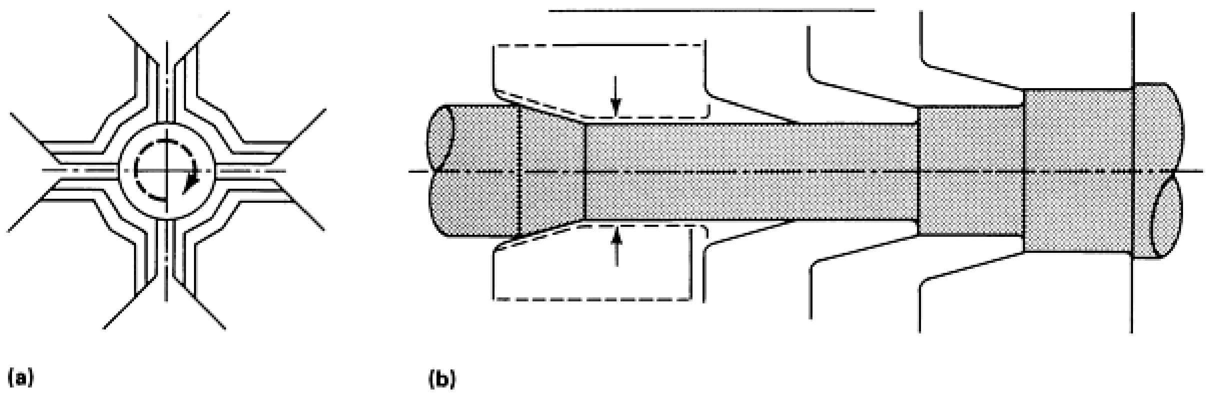


Fig. 3 Arrangement of hammers in a four-hammer radial forging machine. The workpiece rotates intermittently, and the diameter of the forged part is determined by the stroke position of the tools. (a) Front (end) view. (b) Side view

In forging between four hammers, temperature increases will occur in the work material that depend on the deformation rate and the forming resistance of the material. The higher the forming resistance, the higher the temperature increase at each pass. Therefore, the temperature loss of the workpiece (because of heat radiation) can be compensated for by preselecting the correct deformation rate, and forming of the workpiece can take place in temperature ranges with the highest material ductility. In practical terms, this means that all forming can be done in one heat from the ingot to finished bar steel, regardless of the alloy. Chamber furnaces, pit furnaces, and hearth-type furnaces can therefore be replaced by continuously operating furnaces. Material can be transported to and from the machine on roller conveyors, and the entire manufacturing process--heating the ingot, radial forging, dividing and cutting the ends of formed parts, and cooling or annealing--can be done continuously and automatically.

Forging Over a Mandrel. Equipment for mandrel forging (Fig. 4) is available in different designs for the hot and cold forging of tubular workpieces. Figure 4 illustrates the forging of tubular parts over short and long mandrels.

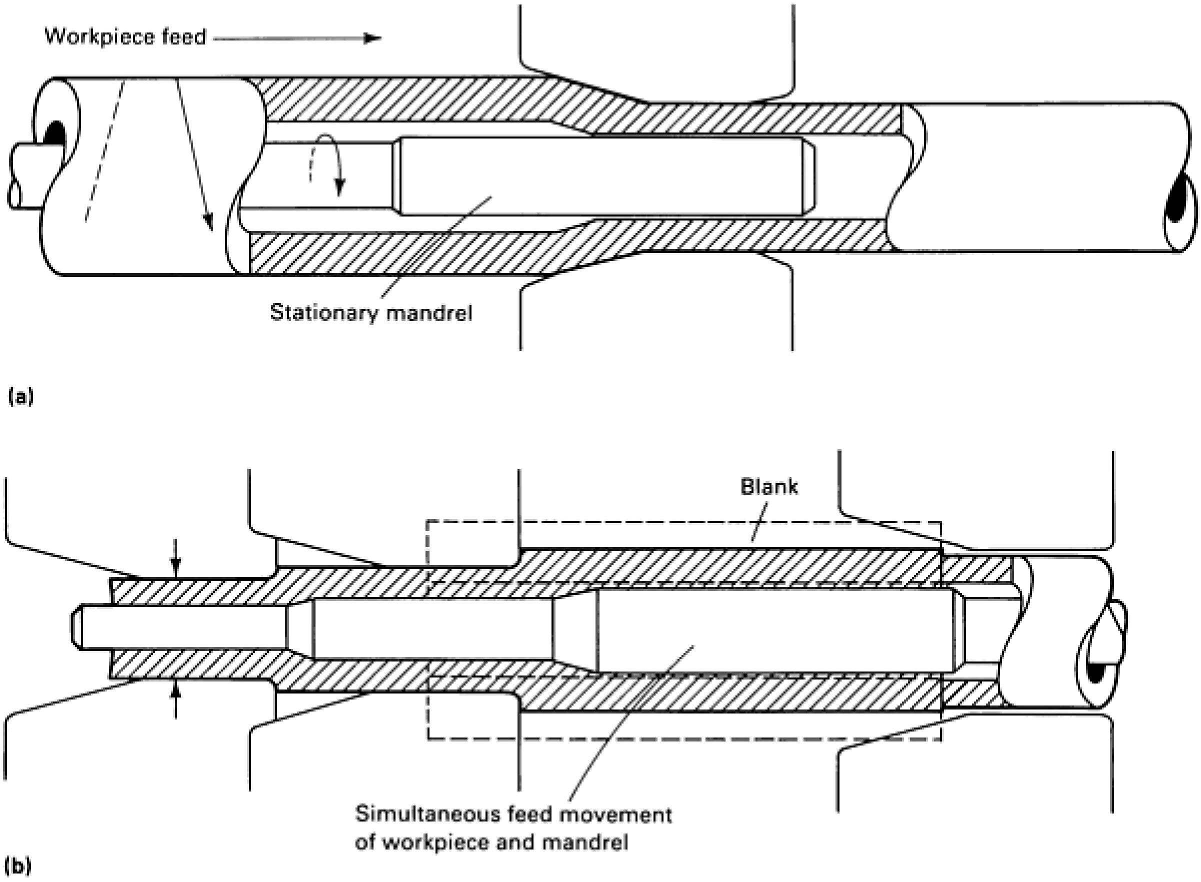


Fig. 4 Forging of tubular parts over a short mandrel (a) and a long mandrel (b)

Long tubes with cylindrical bores are forged over a short mandrel. The short mandrel is held in position between the forging tools with a mandrel rod while the chuckhead moves the workpiece through the forging plane. The mandrel is slightly tapered, and this makes it possible to perform corrections on inside diameter dimensions by changing the position of the mandrel between the hammers. During loading and unloading of the workpiece, the mandrel is automatically retracted into the hollow spindle of the chuckhead.

Forging over a long mandrel is used for relatively short tubes with stepped bores and stepped, cylindrical, or conical contours. The long mandrel is clamped by the chuckhead and moves together with the workpiece feed. After forging, the mandrel is pulled out of the workpiece and retracted into the hollow spindle of the chuckhead.

During hot forging, the mandrel is water cooled while in contact with the workpiece. Tungsten carbide mandrels are often used in cold forging for improved mandrel life.

Radial Forging

Hans Hojas, Gesellschaft für Fertigungstechnik und Maschinenbau mbH

Advantages of Radial Forging

Some of the most important advantages of radial forging are:

- High production. Production of low-alloy steel products using radial forging is approximately four times greater than that of hammer or press forging; high-alloy steel production is six times higher (Fig. 5)
- Low energy consumption as a result of a single ingot heating and continuously operating furnaces
- Close tolerances, which result in less wasted material in subsequent operations. Required machining allowances are approximately 33% of the usual allowances on conventional forged products (Fig. 6)

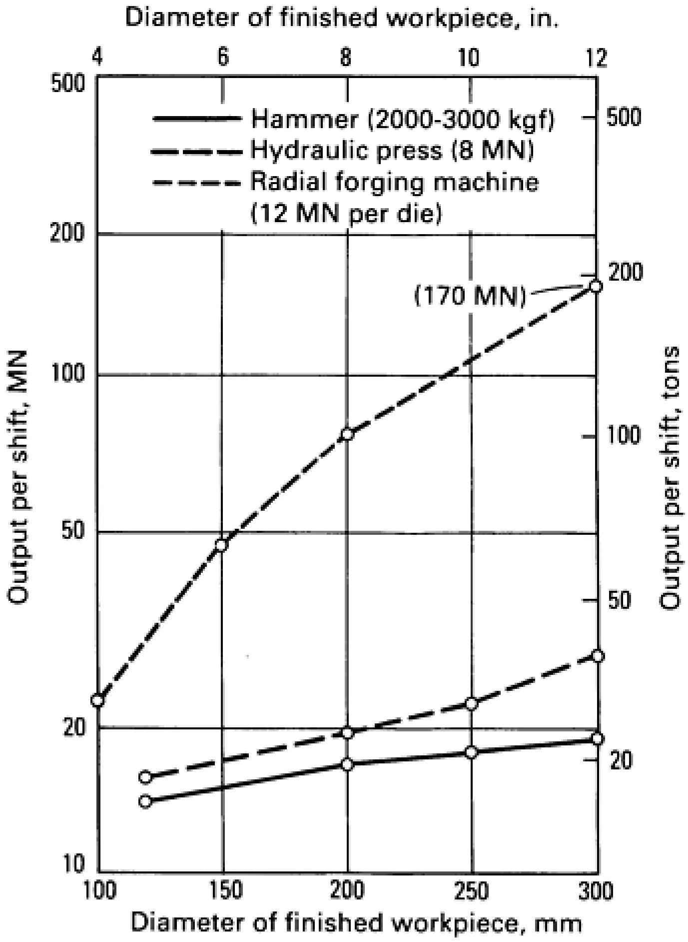


Fig. 5 Comparison of production rates of radial forging and hammer and press forging in the production of alloy steel bars. Starting diameter of the steel was 550 mm (22 in.).

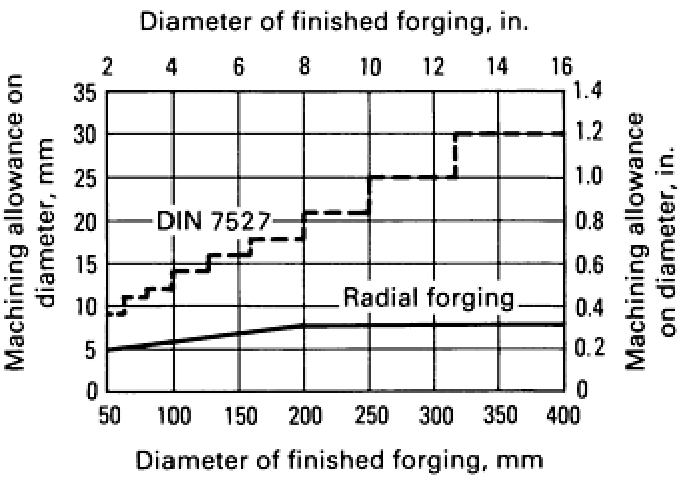


Fig. 6 Typical machining allowance versus diameter for radial forging. Machining allowances are about 33% of

those allowed in a German standard (DIN 7527).

Radial Precision Forging. Full CNC radial precision forging machines are available for hot or cold forging and are built in different capacities with appropriate special equipment as required. Radial precision forging offers the following advantages:

- Forging to net or near-net shape
- Precise, repeatable forging operations
- Low tooling costs
- High flexibility
- Fully automatic operation
- Excellent workpiece surface finish, especially on tube inside diameters
- Forging of internally profiled components to finished inside dimensions

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Radial Forging

Hans Hojas, Gesellschaft für Fertigungstechnik und Maschinenbau mbH

Examples of Applications

Example 1: Automotive Transmission Shaft.

Figure 7 shows a typical transmission shaft used in an automobile automatic transmission. With conventional manufacturing, it is difficult to machine a stepped bore with a surface roughness in the range of 0.4 μm (16 μin.), which is necessary on some areas of the inside diameter. An additional requirement is a maximum run-out of 0.05 mm (0.002 in.) on the inside diameters.

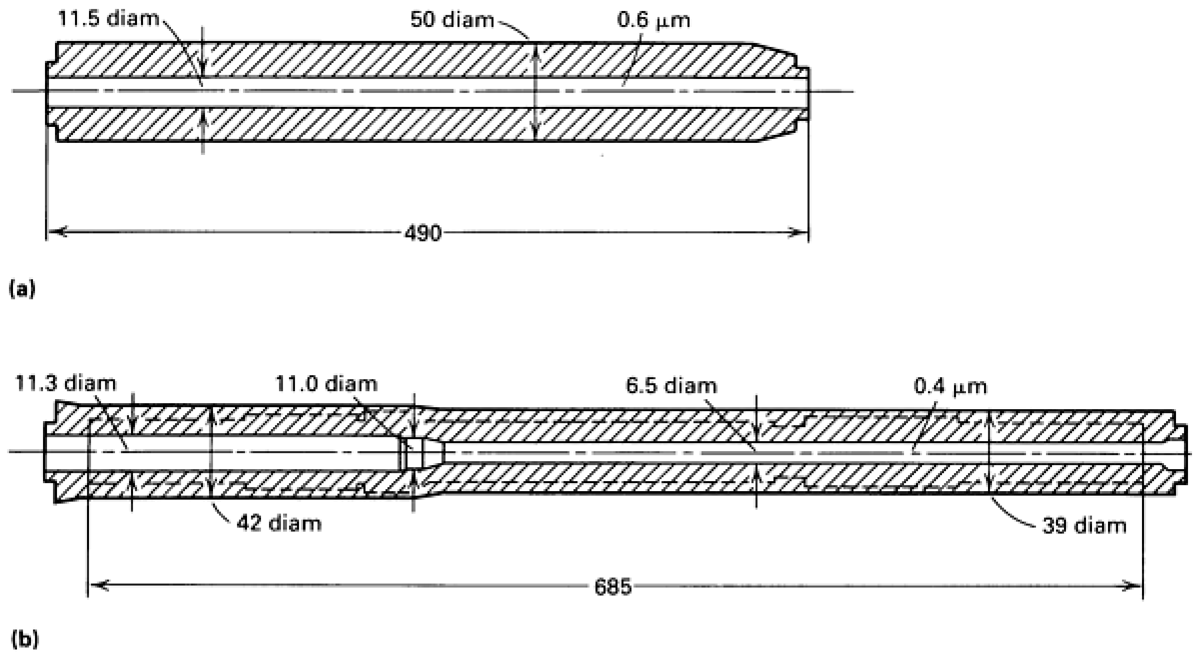

Fig. 7 5120 steel shaft for automobile automatic transmission produced by cold radial forging. The shaft inside diameter is formed to net shape. Dimensions given in millimeters (1 in. = 25.4 mm). (a) Blank. (b) Forged

shaft.

These requirements can be met if the shaft is radial forged over a short tungsten carbide mandrel. Inside diameter surface quality is improved, and the blank can be kept shorter because the reduction in cross-sectional area creates an elongation of the shaft. The proper reduction in area is between 28 and 40%.

The tolerance for the bores of this 5120 steel shaft is ±0.02 mm (±0.0008 in.). The cycle time of the shaft is approximately 2.3 min. Typical cycle times depend on part length.

Example 2: Turbine Shaft Preform.

Figure 8 shows a radial forged turbine shaft preform with the proper volume distribution. The subsequent closed-die forging operation results in an almost flashless workpiece. The material is either a titanium or a nickel-base alloy, both of which have a narrow temperature range for forging. The correct forging temperature range is maintained by varying feed rates (and therefore deformation rates) during the forging process. A higher feed rate creates more deformation per unit time, which results in higher temperatures in the workpiece. Tolerances on the outside diameter are approximately 1% of the diameter.

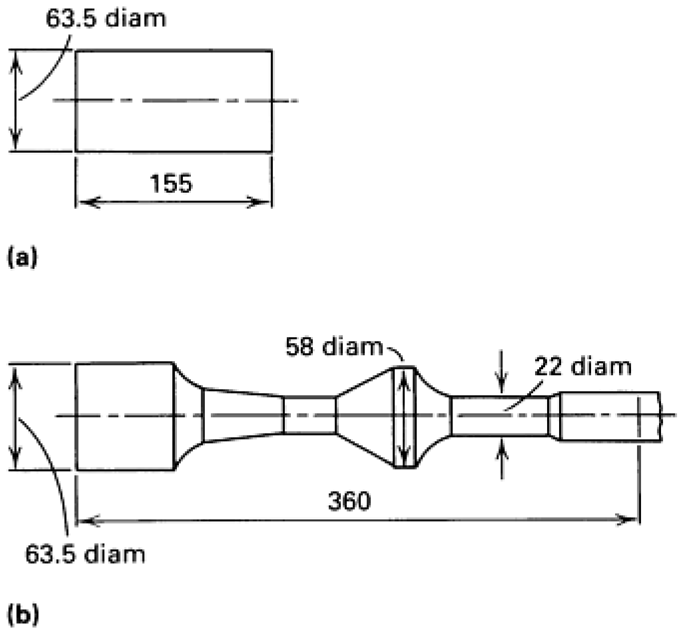

Fig. 8 Blank (a) and turbine shaft preform (b) produced by hot radial forging from titanium or nickel-base alloys. Dimensions given in millimeters (1 in. = 25.4 mm)

Example 3: Hollow Axle With Center Upset.

Figure 9 shows the production sequence for the radial forging of a hollow axle. The upset portion near the center of this part is heated to a higher temperature than the other sections. During upsetting, the forging hammers are closed on the outside diameter of the intermittently rotating workpiece. An axial force applied along with the radial forging force ensures that the work material flows toward the center of the axle.

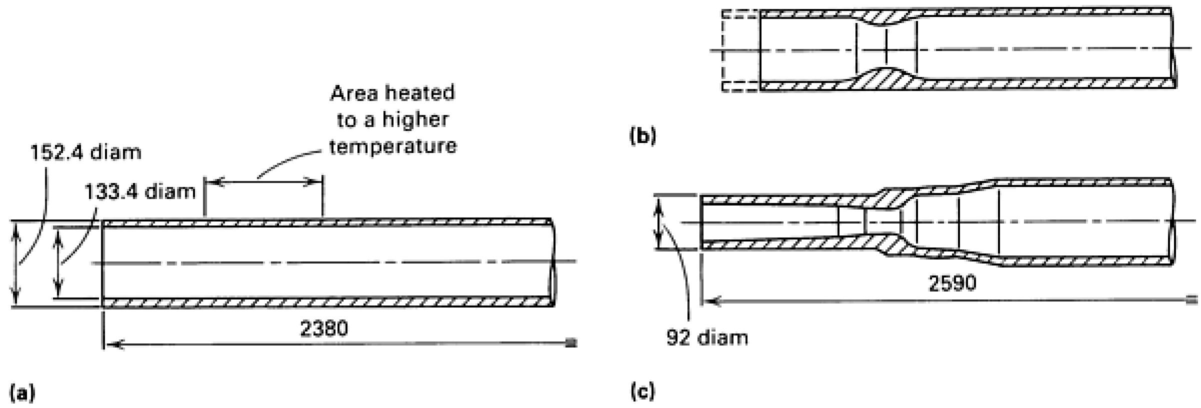

Fig. 9 Steps in the production of a hollow axle with a center upset by radial forging. (a) Tube blank before forging. (b) After center upsetting. (c) Stepped inside diameter contour formed over a water-cooled mandrel. Dimensions given in millimeters (1 in. = 25.4 mm)

After upsetting of the center portion, the stepped contour of the axle end is formed over a water-cooled mandrel. The inside diameter is controlled by the mandrel; normal tolerances on both inside and outside diameter are 1% of the diameter. Total cycle time for one end is approximately 40 s.

Radial Forging

Hans Hojas, Gesellschaft für Fertigungstechnik und Maschinenbau mbH

Two-Hammer Radial Forging Machines

The two-hammer radial forging machine was developed for the forging of unalloyed or low-alloy structural steels. The primary design feature of this machine is the two horizontally arranged, mechanically driven, high stroke rate press rams (Fig. 10) that radially forge the workpiece while it is guided by two forging manipulators. During forging, the workpiece rotates between the two forging tools, as in the four-hammer radial forging machine. The tool layout on the two-hammer radial forging machine is such that the working surfaces of the forging tools are at an obtuse angle to each other; thus, four forming surfaces contact the workpiece with each stroke. Control is numerical, as in the four-hammer precision forging machine. Semiautomatic control is usually used for the forging of bars. Stepped shafts are forged automatically.

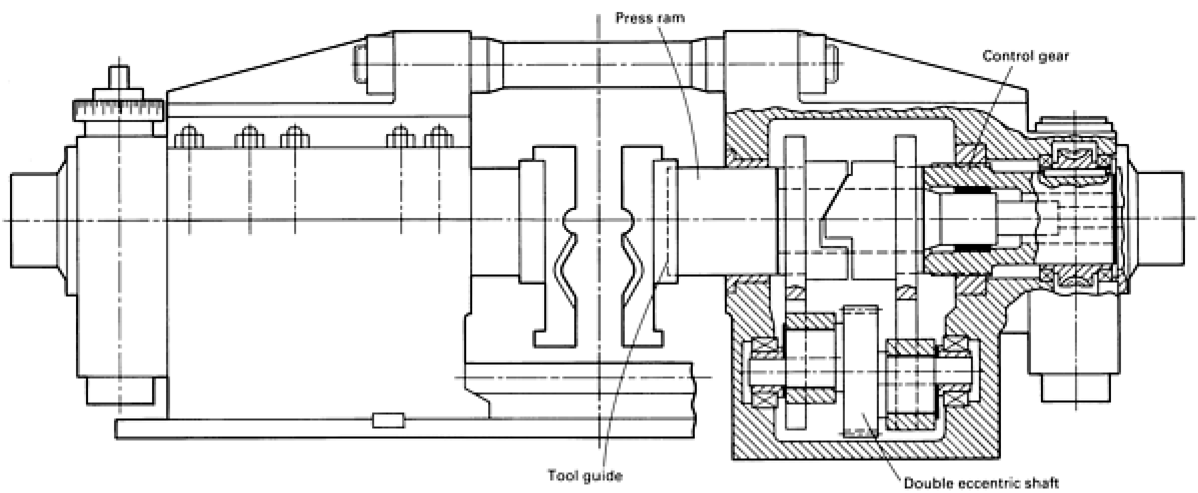

Fig. 10 Schematic showing the mechanical drive of a two-hammer radial forging machine

The stroke motion of the press rams in two-hammer machines is initiated by the rotation of a double eccentric shaft (Fig. 10). The stroke position of the press rams can be changed by means of control gears, making it possible to determine the reduction per pass and therefore the final dimensions of the forged workpieces. The height of the forging tools can be adjusted in a tool guide. It is possible, therefore, to accommodate two different tool impressions in one forging tool. The hammer position can be changed automatically within a few seconds during the process.

Isothermal and Hot-Die Forging

Sanjay Shah, Wyman-Gordon Company

Introduction

HOT-DIE AND ISOTHERMAL FORGING are special categories of forging processes in which the die temperatures are significantly higher than those used in conventional hot-forging processes. This has the advantage of reducing die chill and results in a process capable of producing near-net and/or net shape parts. Therefore, these processes are also referred to as near-net shape forging processes. These processing techniques are primarily used for manufacturing airframe structures and jet-engine components made of titanium and nickel-base alloys, but they have also been used in steel transmission gears and other components.

Isothermal and Hot-Die Forging

Sanjay Shah, Wyman-Gordon Company

Isothermal Forging

In the isothermal forging process, the dies are maintained at the same temperature as the forging stock. This eliminates the die chill completely and maintains the stock at a constant temperature throughout the forging cycle. The process permits the use of extremely slow strain rates, thus taking advantage of the strain rate sensitivity of flow stress for certain alloys. The process is capable of producing net shape forgings that are ready to use without machining or near-net shape forgings that require minimal secondary machining.

Hot-Die Forging

The hot-die forging process is characterized by die temperatures higher than those in conventional forging, but lower than those in isothermal forging. Typical die temperatures in hot-die forging are 110 to 225 °C (200 to 400 °F) lower than the temperature of the stock. When compared with isothermal forging, the lowering of die temperature allows wider selection of die materials, but the ability to produce very thin and complex geometries is compromised.

Advantages

The principal criterion for selecting these processes in production is the economic advantage offered because of reduced input material and/or reduced machining. Therefore, they are primarily used for expensive and difficult-to-machine alloys such as titanium and nickel-base alloys. The main advantages of isothermal and hot-die forging are discussed below.

Reduced Material Costs. These near-net shape processes allow the forging to be designed with smaller corner and fillet radii, a smaller draft angle, and a smaller forging envelope. These design features reduce the additional material incorporated to protect the finished part geometry and therefore reduce the weight of the forging considerably. An example of this weight reduction for the isothermal forging of a nickel-base alloy disk is shown in Fig. 1. A similar comparison for the hot-die forging of a Ti-6Al-4V structural forging is shown in Fig. 2, in which a typical cross section is shown for comparison between conventional and hot-die designs. At current material prices, the reduction in input weight amounts to a significant cost savings.

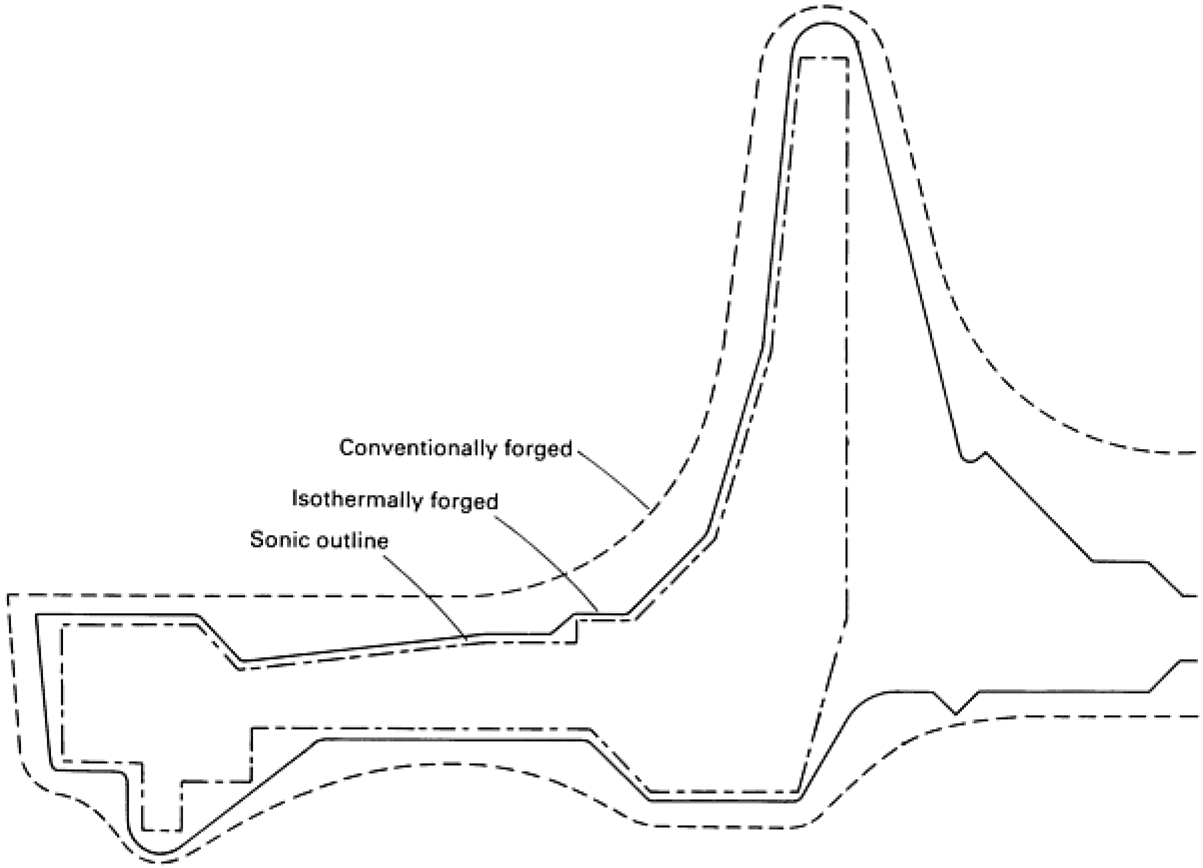

Fig. 1 Weight reduction obtained by the isothermal forging of a disk instead of conventional forging methods. A 27 kg (60 lb) weight reduction was obtained in the production of the nickel-base disk by isothermal forging.

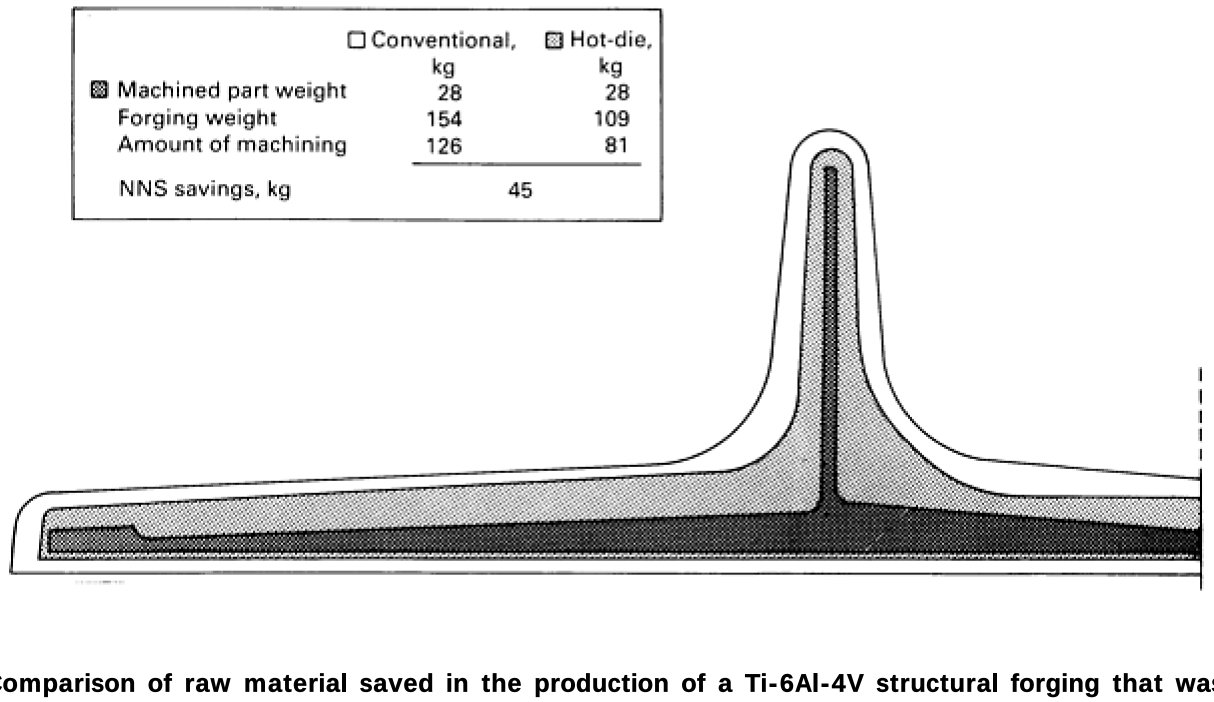

Fig. 2 Comparison of raw material saved in the production of a Ti-6Al-4V structural forging that was hot-die forged versus a conventionally forged part (see cross-sectional areas and legend)

Reduced Machining. Because near-net shape forgings are produced close to end use weight and configuration, less material removal is required during machining when compared with conventional forgings, as shown in Fig. 1 and 2. In most cases, no machining is required, or only finish machining cuts are required to produce the final part. The elimination of complex machining can sometimes justify the use of these processes even for less expensive alloys, as in the case of steel gears forged with net tooth geometry.

Uniformity of Product. The final product produced by isothermal and hot-die forging exhibits more uniform properties because of lower or nonexistent thermal gradients within the forging.

Forgeability. For alloys such as Alloy 100 (Ni-15.0Co-10.0Cr-5.5Al-4.7Ti-3.0Mo-0.6Fe-0.15C-1.0V-0.06Zr-0.015B) that have a narrow range of working temperatures, a conventional forging process will result in severe forge cracking and cannot be used to produce the parts. In these cases, isothermal forging improves the forgeability and makes it possible to forge the alloy.

Isothermal and Hot-Die Forging

Sanjay Shah, Wyman-Gordon Company

Process Description

In conventional forging operations, the dies are heated to 95 to 205 °C (200 to 400 °F) for hammer operations and to 95 to 425 °C (200 to 800 °F) for press operations. These temperatures are significantly lower than the 760 to 980 °C (1400 to 1800 °F) stock temperature for titanium and the 980 to 1205 °C (1800 to 2200 °F) stock temperature for nickel-base alloys and steels. In addition, these operations are performed at relatively high speeds, resulting in high strain rates. Typical strain rates range to 50 mm/mm/min (50 in./in./min) for hydraulic presses, to 700 mm/mm/min (700 in./in./ min) for screw presses, and exceed 12,000 mm/mm/min (12,000 in./in./min) for hammers.

For titanium and nickel-base alloys, the flow stress in general has a high sensitivity to both temperature and strain rate. This effect is illustrated for Ti-6Al-4V in Fig. 3 and for Alloy 95 (Ni-14.0Cr-8.0Co-3.5Mo-3.5W- 3.5Nb - 3.5Al - 2.5Ti - 0.3Fe - 0.16C-0.05Zr-0.01B) in Fig. 4. As shown, a decrease of 110 °C (200 °F) due to die chill can more than double the flow stress. An order of magnitude increase in strain rate has a similar effect. In addition, the workability range for some of these alloys is limited to a narrow temperature range. Therefore, conventional forging for these alloys is characterized by high resistance to deformation, high forging loads, multiple forging operations, and sometimes cracking.

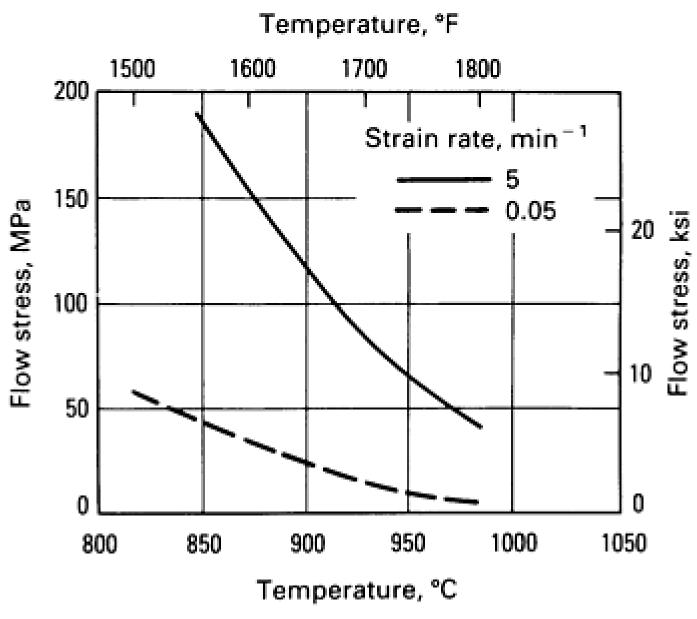

Fig. 3 Effect of temperature and strain rate on flow stress for Ti-6Al-4V

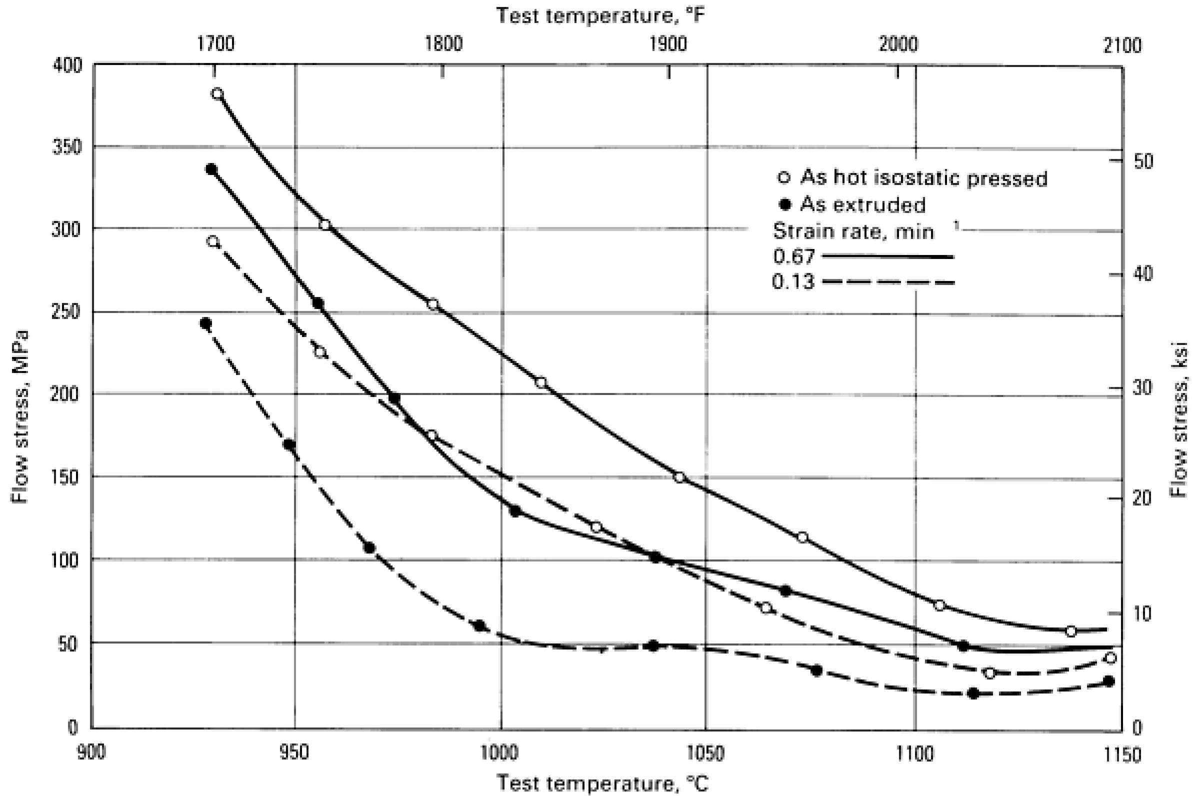

Fig. 4 Effect of temperature and strain rate on flow stress for Alloy 95

The isothermal forging and hot-die forging processes overcome some of these limitations by increasing the die temperature so that it is close to the temperature of the forging stock. The die temperatures are maintained at these high levels through continuous heating of the dies during the forge operation using induction heating, gas-fired infrared heating, resistance heating, and so on. The heating arrangement is combined with the press so that heat can be provided to the dies during the forging operation.

Figure 5 shows a typical arrangement for induction heating. In this setup, a set of induction coils is placed around the dies (Alloy 100, Fig. 5). The electrical power input to the induction coils is controlled by thermocouples buried in the dies, and it maintains the dies at a specified temperature. The arrangement also incorporates a die stack consisting of several plates, some of them made from superalloys, to be placed between the dies and press platen. The die stack protects the press platen from the heat of the dies and maintains the platen below a specified temperature. This arrangement prevents excessive temperature at the press platen, which could severely affect the functioning of the press hydraulics and/or the dimensional stability of the platen.

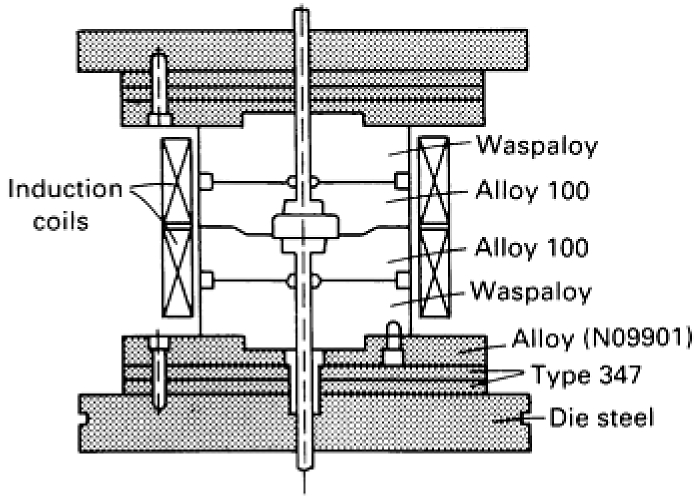

Fig. 5 Schematic of induction heating system for hot-die or isothermal forging

Another heating arrangement, using gas-fired infrared heaters, is shown in Fig. 6. This illustration also shows a resistance heated heater plate situated under the dies.

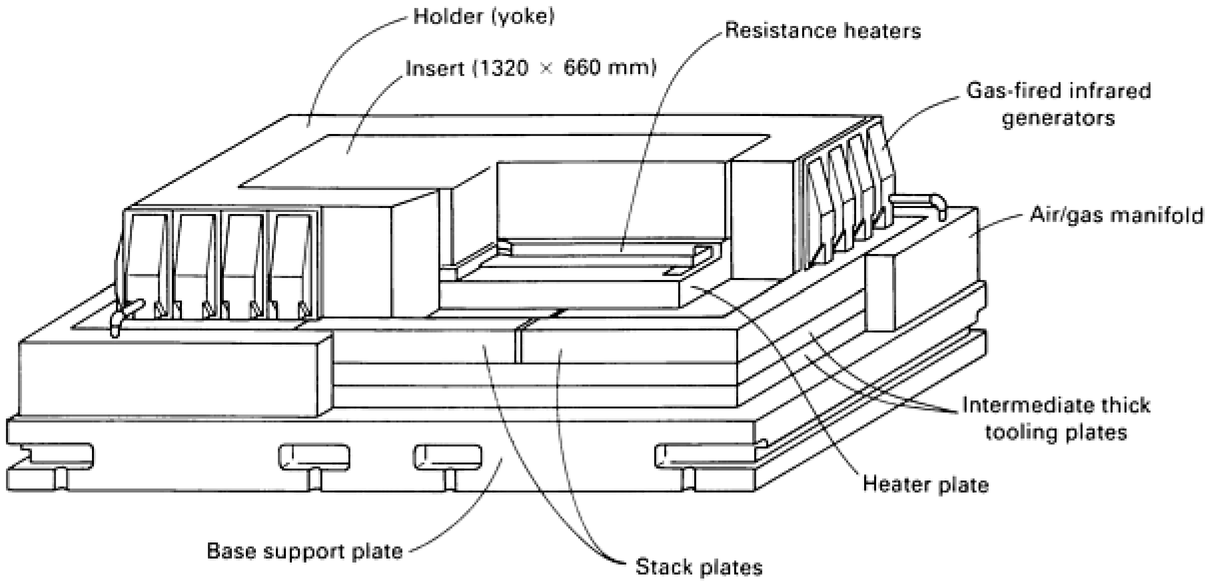

Fig. 6 Gas-fired infrared heating setup for hot-die forging

The higher die temperatures for these processes allow for forging stock to remain at a higher temperature for a longer time during die contact. This has the added advantage of reducing the forging speed, thus lessening the strain rate. The beneficial impact of reduced strain rate on flow stress is shown in Fig. 3 for Ti-6Al-4V and in Fig. 4 for Alloy 95. Typical strain rates used for isothermal forgings are 0.5 mm/mm/min (0.5 in./in./min) or lower, while hot dies use strain rates in the range of 3 to 10 mm/mm/min (3 to 10 in./in./min).

Forging Alloys

The hot-die and isothermal forging processes typically result in higher tooling costs due to higher die temperatures, as well as higher costs for forging operation due to slower strain rate, when compared to conventional forging. However, their ability to forge to a near-net shape results in lower material costs. Therefore, they are typically used for expensive alloys where material content represents a large portion of the total cost of forging.

Alloys forged using these processes include titanium alloys, such as Ti-6Al-4V, Ti-6Al-2Sn-4Zr-2Mo, and Ti-10V-2Fe-3Al and superalloys, such as Alloy 100, Alloy 95, Alloy 718 (UNS07718), and Waspaloy. In the case of β-titanium alloys such as Ti-10V-2Fe-3Al, the typical forging temperatures range from 760 to 815 °C (1400 to 1500 °F), and the near-net shape processes are especially attractive because of the availability of relatively inexpensive alloys for die materials. In the case of superalloys such as Alloy 100, the working temperature range is so small that the isothermal and hot-die methods are the only feasible forging processes currently available. In addition, at specific temperatures and strain rates, Alloy 100 exhibits superplasticity, as shown in Fig. 7. When forged within this temperature range and strain rate range, the alloy can be deformed to large strains at low loads and to fairly complex geometries.

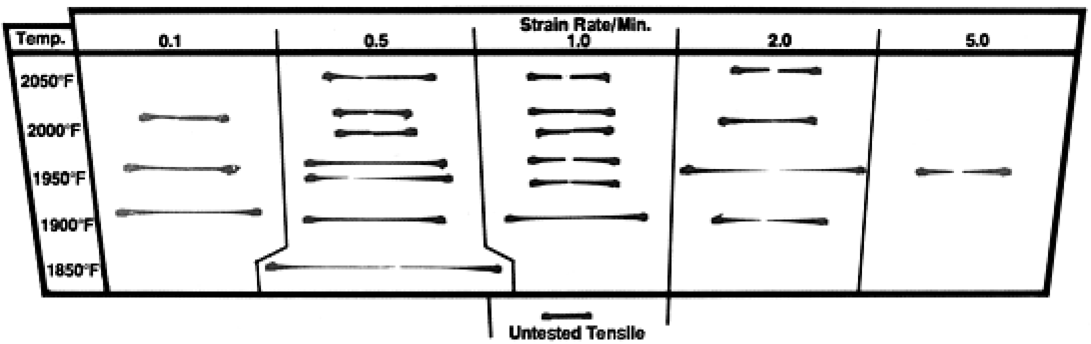

Fig. 7 Superplastic behavior of extruded Alloy 100 at various temperatures and strain rates

Typical parts forged in the above alloys include structural components for air-frames; jet-engine disks, shafts, and seals; and other aerospace components. The processes have also been used for some steel alloys to make complex geometries, such as gears, in order to produce net surfaces and to eliminate expensive machining.

Process Selection

Lower overall cost is one of the major reasons for selecting hot-die or isothermal forging over a conventional forging process. Several factors influence this overall cost, and a complete value analysis is necessary for each part or part family to determine its potential as a candidate for hot-die or isothermal forging. These factors are described in the section "Cost" in this article.

Another criterion for selecting these processes is the need for uniformity and product consistency. In conventional forging processes, there is a temperature gradient from the surface to the center of the forging because of die chill. This gradient results in different areas of the part being forged at different temperatures and could cause a variation in microstructure from the center to the surface of the forging. When this structural variation is not acceptable, the higher die temperature process offers the advantage of a more uniform temperature during deformation and therefore less variation in microstructure. In addition, because the die temperature and strain rate are controlled within a narrow range, there is improved consistency from part to part.

The process selection for some alloys, such as Alloy 95 and Alloy 100, is based on their inherent tendency to develop forge cracking under conventional forging conditions. Hot-die forging and isothermal forging represent the only suitable forging processes available for these alloys.

Isothermal and Hot-Die Forging

Sanjay Shah, Wyman-Gordon Company

Process Design

The same factors that affect conventional forging processes also affect near-net shape processes. However, because of tighter forging designs and the requirements for strict uniformity and consistency, stringent controls on the following process parameters are necessary.

Forging parameters such as forge temperature, strain rate, preform microstructure, forging pressure, and dwell time are all important factors in deciding the degree of dimensional sophistication and the resultant microstructure of the finished part.

In general, lower strain rates and increased dwell time increase the potential degree of shape complexity and shape sophistication of the forging, but could influence microstructure due to exposure to high temperatures for long periods of time during and after deformation. In addition, very low strain rates cannot be used in hot-die forging, because of the potential decrease in the stock temperature. Preform microstructure has a direct influence on the flow stress and superplasticity of the material, sometimes requiring extruded billet with fine-grain structure as the starting material. Some of the alloys that are forged achieve their final mechanical properties by thermomechanical processing; in this case, the selection of the forge temperature and the amount of deformation are controlled by property requirements.

Close control of the above parameters and the entire deformation process is necessary to achieve proper results. New analytical tools, such as deformation mapping and computer simulation of the deformation process, are very useful for optimization of the processes.

Die Temperature. Proper selection of die temperature is one of the critical factors in process design for hot-die and isothermal forging. The effect of die temperature on forging pressure is illustrated in Fig. 8 for Ti-6Al-4V. As shown in Fig. 8, a decrease in die temperature from 955 to 730 °C (1750 to 1350 °F) may result in doubling the forging pressure and may affect the shape capability available. It will also have an impact on the selection of die materials and economics. In addition, for some alloys, the surface microstructure is affected by die temperature.

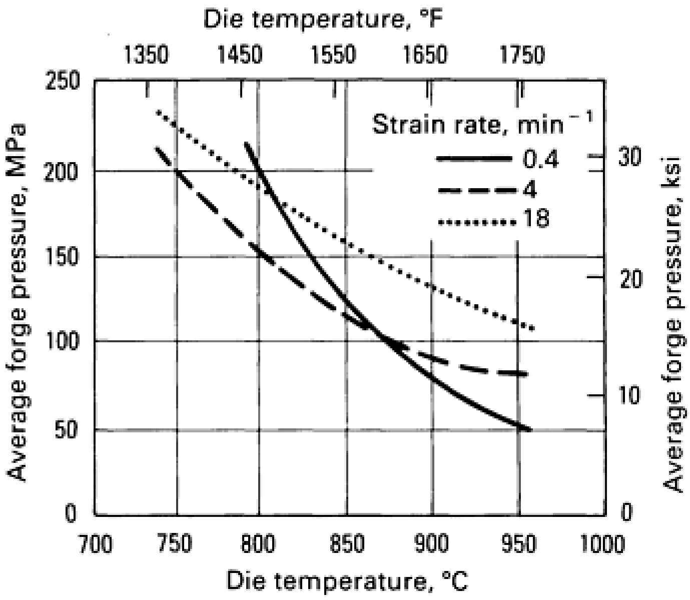

Fig. 8 Effect of die temperature on forging pressure at various strain rates for Ti-6Al-4V

Lubrication. In these near-net shape processes, lubrication plays an important role because of the precision of the forgings, the existence of net surfaces, and the high interface temperatures. Standard practice is to apply coatings to the billet or the preform prior to forge heating. They are sometimes supplemented by die lubrication during the forging operation. The lubrication/coating systems must provide proper lubrication and must act as a good parting agent for the easy removal of the forging from the dies. They also have to protect the forging surface in order to maintain acceptable surface finish for the forgings and must not build up in the dies. For die temperatures to 650 °C (1200 °F), graphite lubricants are acceptable, but for higher die temperatures, glass frits with proper additives or boron-nitride coatings find wider use.

Preform Design. Another significant factor in process design is the design of the preform. One approach is to design a fairly complex preform that is produced by a conventional forging process. The near-net shape process is then used to size the part to tight geometries and tolerances. This approach was prevalent during the early development of hot-die technology. A recent trend is to start with a conventionally forged blocker geometry and to finish forge using a hot-die or isothermal process. In some cases, such as the isothermal forging of superplastic alloys, it is possible to start directly with a billet geometry and produce the finish geometry with a single near-net shape forging operation. Preform design must also take into consideration the amount of deformation needed during the finish forge operation to obtain the desired mechanical properties.

Post-Forge Operations. After the parts are forged using the hot-die or isothermal forging methods, they are subjected to the same clean-up, heat treatment, machining, and nondestructive evaluations as the conventional forgings. These processes are described in detail in the articles "Forging of Nickel-Base Alloys" and "Forging of Titanium Alloys" in this Volume.

Sanjay Shah, Wyman-Gordon Company

Die Systems

The principal difference between conventional forging and hot-die/isothermal forging is the die temperature. Therefore, the die systems affect the successful implementation of these processes.

Die Materials. Conventional die steels do not have adequate strength or resistance to creep and oxidation at near-net shape temperatures. Hot-die/isothermal forging dies must maintain precision while resisting the excessive high-temperature-induced stresses that are caused by tight, complex geometries. Therefore, expensive nickel-base alloys such as Alloy 100, B-1900, MAR-M-247, Astroloy, Alloy 718, and NX-188, as well as molybdenum alloys such as titanium zirconium-modified molybdenum or TZM must be used for these applications. The yield strength and 100-h stress rupture strength of some of these alloys are shown in Fig. 9 and 10 at typical near-net shape temperatures. Table 1 gives the compositions of die materials for isothermal and hot-die forging.

Table 1 Compositions of die materials for isothermal and hot-die forging

Alloy	Composition, %[a]									
	C	**Co**	**Cr**	**Fe**	**Mo**	**Nl**	**Si**	**Tl**	**W**	Others
Nickel-base alloys										
Alloy 100	0.18	15.0	9.5	. . .	3.0	rem	. . .	5.0	. . .	**5.5Al, 0.95V, 0.06Zr, 0.01B**
B-1900	0.10	10.0	8.0	. . .	6.0	rem	. . .	1.0	. . .	**6.0Al, 4.0Ta, 0.10Zr, 0.015B**
Astroloy	0.05	17.0	15.0	. . .	5.0	rem	. . .	3.5	. . .	**4.0Al, 0.06Zr**
Alloy 718	0.05	. . .	18.0	19.0	3.0	rem	. . .	0.4 max	. . .	**. . .**
Alloy 713LC	0.05	. . .	12.0	. . .	4.5	rem	. . .	0.6	. . .	**6.0Al, 2.0 Nb, 0.1Zr, 0.01B**
NX-188	0.04	. . .	. . .	. . .	18.0	rem	. . .	. . .	. . .	**8.0Al**
MAR-M-247	0.15	10.0	8.25	0.5	0.7	rem	. . .	1.0	10.0	**5.5Al, 3.0Ta, 1.5Hf, 0.05Zr, 0.015B**
Molybdenum alloy										

(a) Nominal unless otherwise indicated

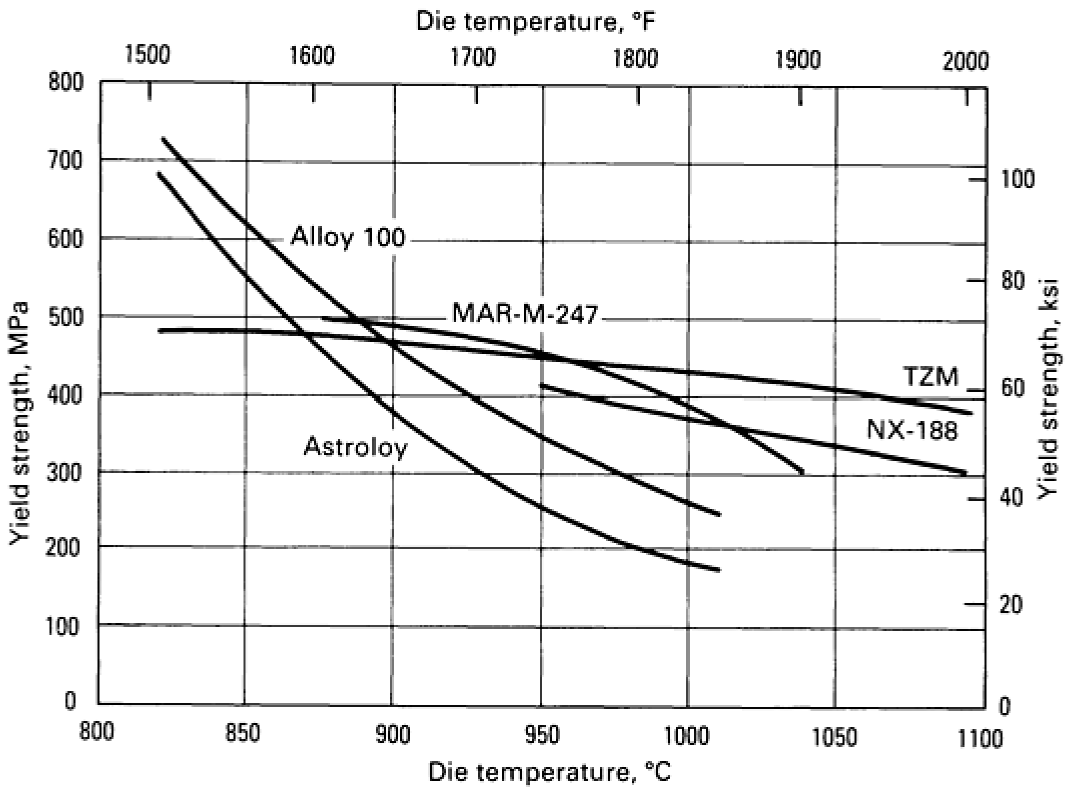

Fig. 9 Yield strength as a function of near-net shape die temperature for numerous nickel-base alloys and a molybdenum alloy (TZM)

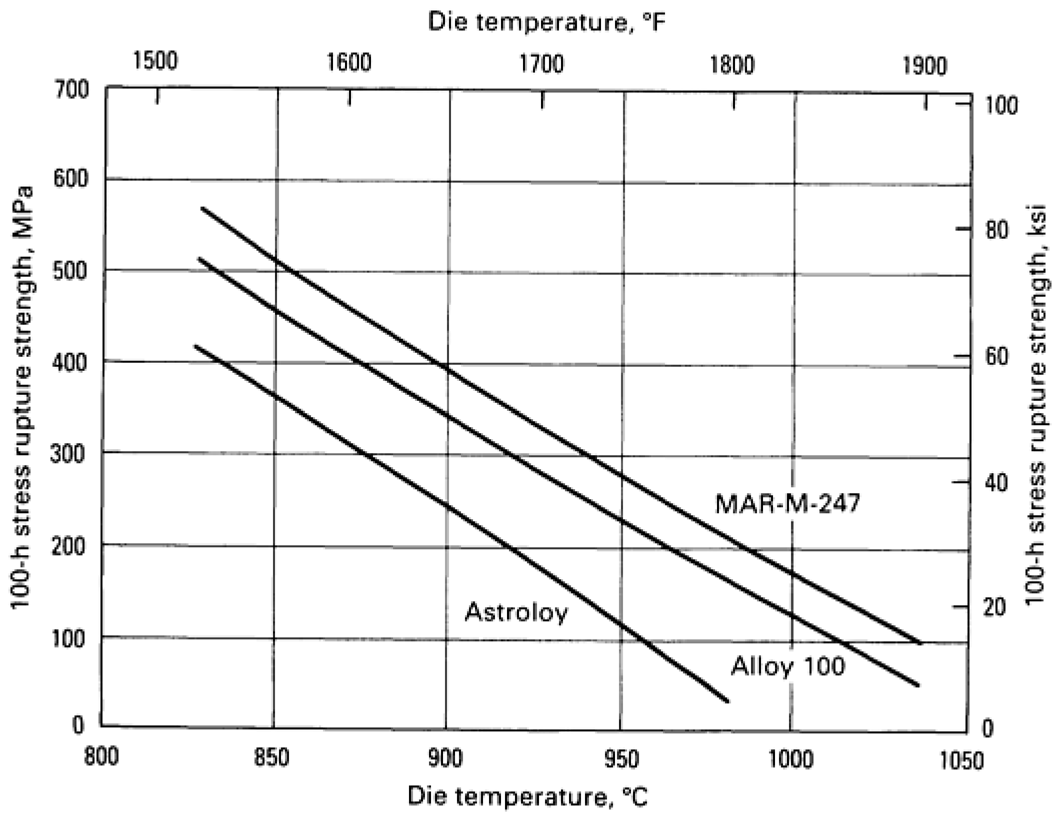

Fig. 10 100-h stress rupture strength as a function of near-net shape die temperature for selected nickel-base alloys

Proper selection of die material for a given application depends on the operating temperature, forging pressure requirements, and anticipated die life. As shown in Fig. 9, TZM is the most practical die material for the isothermal forging of nickel-base alloys (which are forged at 1040 °C, or 1900 °F, or higher), while Alloy 100 and Astroloy are better suited to the hot-die and isothermal forging of α-β titanium alloys, such as Ti-6Al-4V, forged at 925 to 980 °C (1700 to 1800 °F). For β-forged titanium alloys such as Ti-10V-2Fe-3Al, which can be forged at 815 °C (1500 °F) or lower, Alloy 718 or Alloy 713LC dies at 650 to 705 °C (1200 to 1300 °F) may provide a satisfactory cost-effective alternative. Astroloy or Alloy 718 dies have also been successfully used for forging of superalloys such as Alloy 718 at 650 to 760 °C (1200 to 1400 °F). When large quantities of parts are to be produced, die life becomes an important consideration, and the cost of die material becomes a secondary issue.

Die Manufacturing. The die materials used for hot-die and isothermal forging are more difficult to machine than conventional die steels. Most dies manufactured for axisymmetric forgings are turned on a lathe, but dies for asymmetric parts may have to be milled, which can be very expensive. Two approaches have been used in these cases to reduce the cost of die manufacturing. Several early attempts with smaller die sizes and simple geometries used precision cast dies. The more widely used technique is to produce these dies for structural shapes by electrical discharge machining using a precision-machined graphite electrode. The tolerances on die sinking are held to better than ±0.1 mm (±0.005 in.). Because most of the die materials are not weld repairable, accuracy is critical in the machining of the dies.

Atmospheric Control. When TZM is used as a die material, a special atmospheric control with either vacuum or inert gases is necessary because of the tendency of molybdenum alloys to oxidize severely at temperatures greater than 425 °C (800 °F). This necessitates the introduction of a special enclosure in the press around the die system and associated enclosures for heating of multiples and material handling. Therefore, processes using TZM dies (mostly isothermal forgings) have dedicated equipment. On the other hand, most nickel-base alloys can be heated in a normal atmosphere; therefore, most hot-die forging operations that use these die materials are performed in conventional presses, with the only additional requirement being the introduction of the die stack and/or the die heating system described earlier. These presses do not have to be dedicated, and they can be used interchangeably for conventional forging as well as hot-die forging.

Isothermal and Hot-Die Forging

Sanjay Shah, Wyman-Gordon Company

Forging Design Guidelines

The principal criterion in designing hot-die and isothermal forgings is to design the forging as close as possible to the machined part with a potential of using as-forged surfaces if feasible. Beyond this, it is difficult to establish one set of guidelines for a variety of parts that may be considered for near-net shape applications. Each part family must be considered individually in order to ensure the optimal, most cost-effective design. There are, however, some general guidelines that can be used in designing these parts.

Guidelines for forging design parameters, such as minimum web and rib thicknesses, corner and fillet radii, draft angle, and design cover, are presented in Table 2 for various alloys and geometries. These values indicate the current industry capabilities, and a significant amount of research and development effort is being applied to improve them, including an increased size capability, geometries that are closer to the finished part, and the ability to provide negative draft and contour capabilities through the use of split dies.

Table 2 Typical near-net shape forging design parameter

Material	Parameters						
	Miximum plan view area	**Forging envelope**	**Draft angle, degrees**	**Minimum corner radius**	**Minimum fillet radius**	**Minimum web thickness**	Minimum rib width

	m^2	$in.^2$	mm	in.		mm	in.	mm	in.	mm	in.	mm	in.
Near-net axisymmetric Alloy 718	0.645	1000	1.5	0.06	3	6.4	0.25	19	0.75	15	0.60	. . .	**. . .**
Near-net axisymmetric titanium	0.645	1000	1.5	0.06	3	3.3	0.13	6.4	0.25	13	0.50	. . .	**. . .**
Near-net structural titanium (Ref 3)	0.387	600	1.5-2.3	0.06-0.09	3	3.8	0.15	6.4	0.25	10	0.40	6.4	**0.25**
Net structural $\alpha + \beta$ titanium (Ref 3)	0.194	300	0.0	0.0	1-3	1.5	0.06	3.3	0.13	4.8	0.19	4.8	**0.19**
Net structural β titanium (Ref 1)	**0.081**	**125**	**0.0**	**0.0**	**0-1° 30'**	**1.5**	**0.06**	**3.3**	**0.13**	**2.3**	**0.09**	**2.3**	**0.09**

Generally, the tolerances considered for conventional forgings, such as those for length and width, die closure, straightness, contour, radii, and draft angle, must also be considered for near-net shape forgings. For the near-net shape parts, the tolerances are dictated by the process and part size. Tolerances to ±1.5 mm (±0.06 in.) and greater have been acceptable for near-net forgings, while tolerances of ±0.5 mm (±0.02 in.) and tighter have been achieved for small net surface titanium structural parts. In general, they are determined on an individual part basis and are negotiated between the forging vendor and the customer.

In designing the dies for these forgings, accurate calculation of the die shrinkage allowance is important because of the tight tolerances associated with these parts. Typically, the die geometries are machined using less than 20% of the tolerance spread allowed for the forgings. When fairly tight draft wall and/or complex contours are features of the forge design, segmented dies with a holder system (described in the article "Forging of Aluminum Alloys" in this Volume) are used to achieve accuracy while maintaining the ease of removing the forging from the dies. Most hot-die and isothermal forging processes also use a knock-out system for removing forgings from the dies.

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1. G.W. Kuhlman and J.W. Nelson, "Precision Forging Technology: A Change in the State-of-the-Art for Aluminum and Titanium Alloys," Paper 84-256, Society of Manufacturing Engineers, 1984
3. S.N. Shah and J.D. McKeogh, "Status of Near Net Shape Forging for Major Aerospace Applications," Paper MF83-908, Society of Manufacturing Engineers, 1983

Isothermal and Hot-Die Forging

Sanjay Shah, Wyman-Gordon Company

Cost

The total cost basis of producing a part has a major impact on the selection of the hot-die or the isothermal forging process for a given part. This total cost includes not only the cost of the forging material and the forging conversion but also the cost of machining this forging to the final shape, the cost of tooling, and the cost of maintaining the tooling.

The initial cost of these processes is high because of the expensive die materials, such as TZM and Alloy 100, which can sometimes cost in excess of ten times the conventional die materials, and because of the high cost of machining the dies. The setup cost during forging for these processes may also be higher than that for conventional forging because of the need for die stack and die heating and, in case of isothermal forging, the need for an enclosed atmospheric chamber. On a per-part basis, the conversion cost may be higher than that for conventional forging in some cases, but lower in other cases, depending on geometry and the potential for using smaller equipment to make the same part. For these processes to be economically feasible, there must be a significant savings in material costs and machining costs to offset the higher costs of tooling and setup.

To determine whether to use hot-die/isothermal forging or conventional forging and whether to use near-net geometry or net geometry, the following factors should be considered:

- Total part quantity
- Part geometry and complexity
- Forging temperature and die temperature
- Savings in material and machining
- Die sizes and expected die life
- Cost of maintaining tooling to produce desired tolerances

The design and the process are selected by considering the above factors and their influence on the cost of tooling and the cost of individual parts. A break-even analysis is then performed to determine the quantity at which the competing processes break even, and based on the total quantity required for the part, the most economical process is selected.

Example 1: Comparative Costs of Conventional Forging Versus Hot-Die Forging in the Manufacture of a Connecting Link.

Figure 11 shows relative comparison of costs for a conventional forging versus a hot-die forging for a connecting link (Ref 2). This part, 0.048 m^2 (75 $in.^2$) in plan view area (PVA), was made of Ti-6Al-4V. The forging for this part using conventional design weighed 17.4 kg (38.3 lb), while a hot-die forging weighed 13 kg (29 lb). The hot-die design was based on the use of Astroloy dies at approximately 925 °C (1700 °F) with some net surfaces. The die system for this part required a die stack. Figure 11 shows that there was a significant difference in initial tooling costs and that it took over 500 forgings for the savings in material and machining to pay for the difference in the cost of hot-die tooling versus conventional tooling. Hot-die near-net forging was not cost effective for this part at quantities under 500.

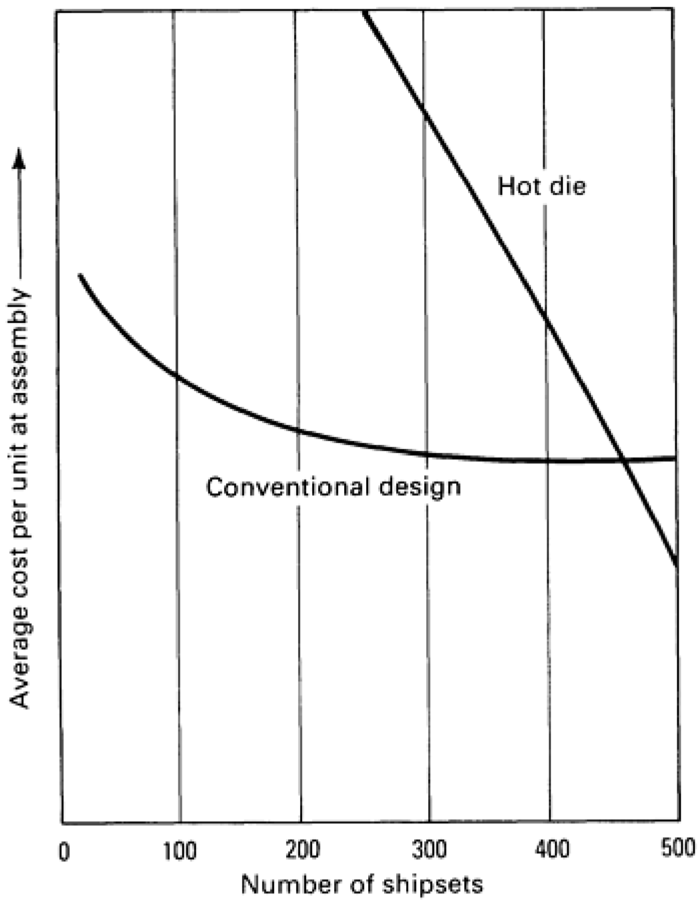

Fig. 11 Cost comparison between conventional design versus hot-die design for the manufacture of a connecting link forging made of Ti-6Al-4V

Example 2: Comparative Costs of Conventional Forging Versus Hot-Die Forging in the Manufacture of a Bearing Support.

Figure 12 shows a comparison similar to that in Fig. 11 but for a different part--a bearing support (Ref 2). This part was also made of Ti-6Al-4V and was 0.178 m^2 (275 $in.^2$) in plan view area. Conventional forging for this part weighed 55.3 kg (122 lb), while hot-die near-net forging using Astroloy dies at 925 °C (1700 °F) weighed 21.1 kg (46.5 lb). Because of the larger size of this part compared to the forging in Example 1, the difference in die costs between conventional forging and hot-die forging was greater for this part. However, because of a significant reduction in material costs and machining costs, the break-even point for the part was at a quantity of less than 200.

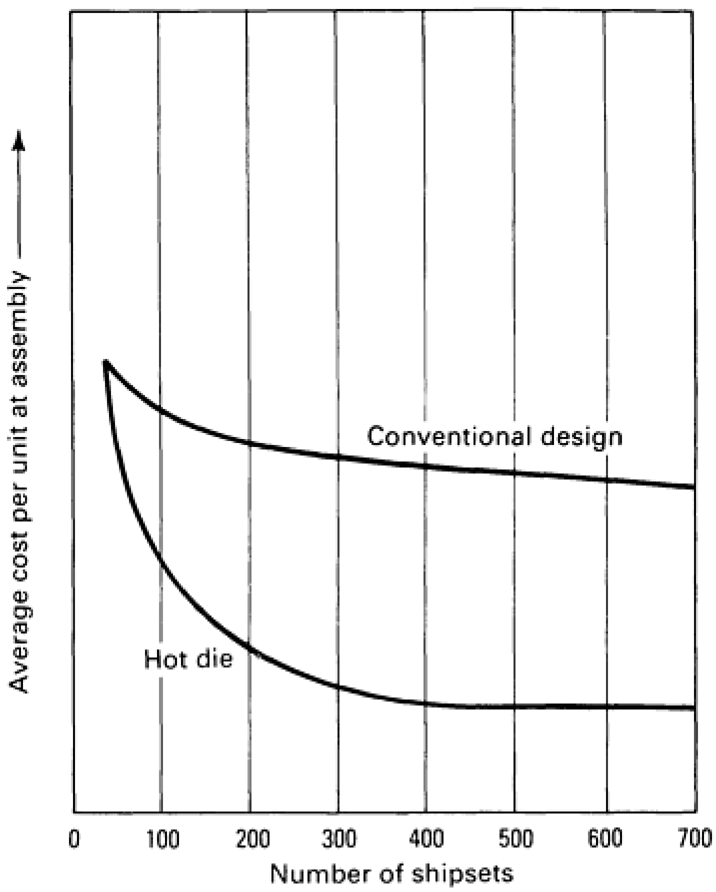

Fig. 12 Cost comparison between conventional design method versus hot-die design for the manufacture of an F-15 bearing support made of Ti-6Al-4V

Reference cited in this section

2. C.C. Chen, W.H. Couts, C.P. Gure, and S.C. Jain, "Advanced Isothermal Forging, Lubrication, and Tooling Process," AFML-TR-77-136, U.S. Air Force Materials Laboratory, Oct 1977

Isothermal and Hot-Die Forging

Sanjay Shah, Wyman-Gordon Company

Production Forgings

The hot-die and isothermal forging technologies emerged as development efforts in the early 1970s and have become production realities since the late 1970s. Some examples of production forgings are given in this section.

Figure 13 shows a Ti-6Al-4V hot-die forging for the F-15 bearing support referred to in Example 2. The part required three closed-die operations to produce. The first two operations--preblock and block--were performed with conventional forging processes, and the parts were then finish forged as doubles (0.355 m^2, or 550 $in.^2$, PVA) in Astroloy hot dies. A cost comparison of this part for conventional forgings versus hot-die forging is shown in Fig. 12.

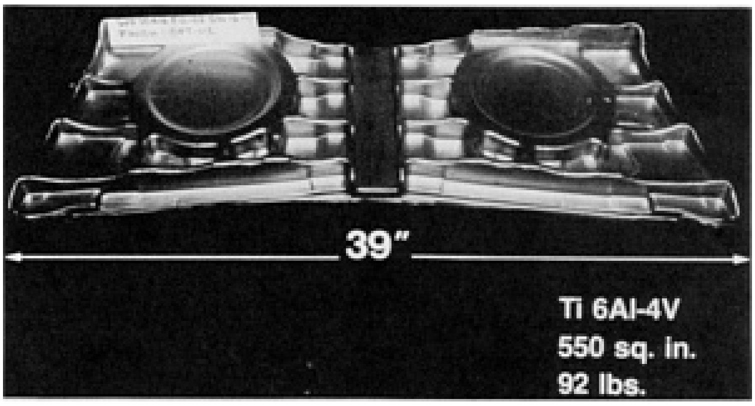

Fig. 13 F-15 bearing supports weighing 21.1 kg (46.5 lb) individually, made of Ti-6Al-4V that was finish forged with a hot-die near-net forging process in Astroloy dies at 925 °C (1700 °F). Bearing supports were finish forged as doubles.

Other examples of these technologies in production mode are presented in Fig. 14, 15, 16, and 17. Figure 14 shows a Ti-6Al-4V engine mount that was hot-die forged with most surfaces being net on the side shown. The back side, which is flat, was machined during final machining operations. Figure 15 shows a Ti-10V-2Fe-3Al engine-mount forging that was hot-die forged with net surfaces. An isothermally forged Alloy 100 disk is shown in Fig. 16. This part was forged in a single forging operation from billet using TZM dies. The forging had no net surfaces and was machined all over to yield the sonic shape. The main criteria for selecting the isothermal forging operation in this case are forgeability and savings in material cost. A hot-die forged Alloy 718 disk is shown in Fig. 17. This forging was machined all over to yield the sonic shape. For this part, the hot-die forging operation reduced the weight by 9 kg (20 lb) as compared to conventional forging.

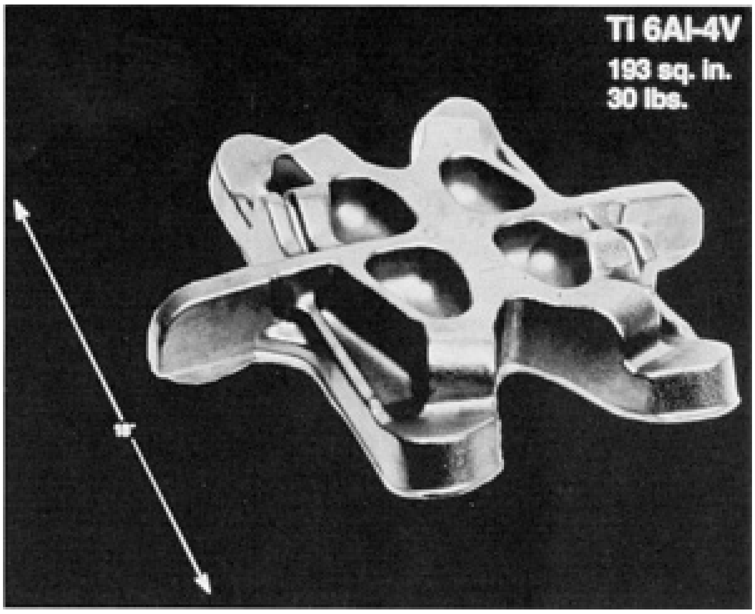

Fig. 14 Hot-die forged Ti-6Al-4V engine mount with net surfaces

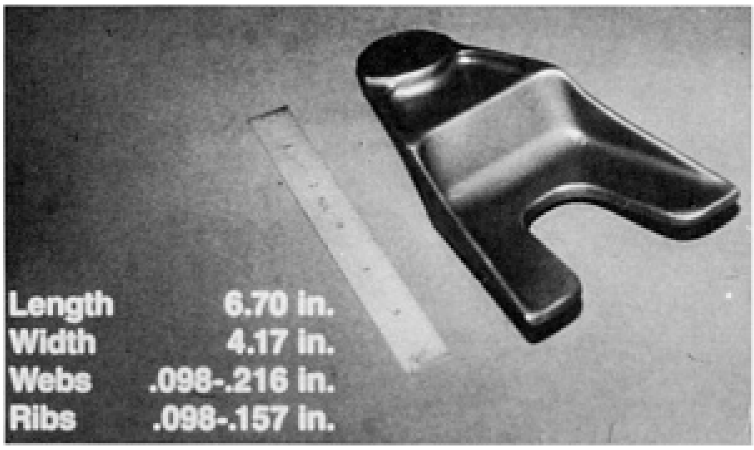

Fig. 15 Hot-die forged engine mount made of Ti-10V-2Fe-3Al with net surfaces

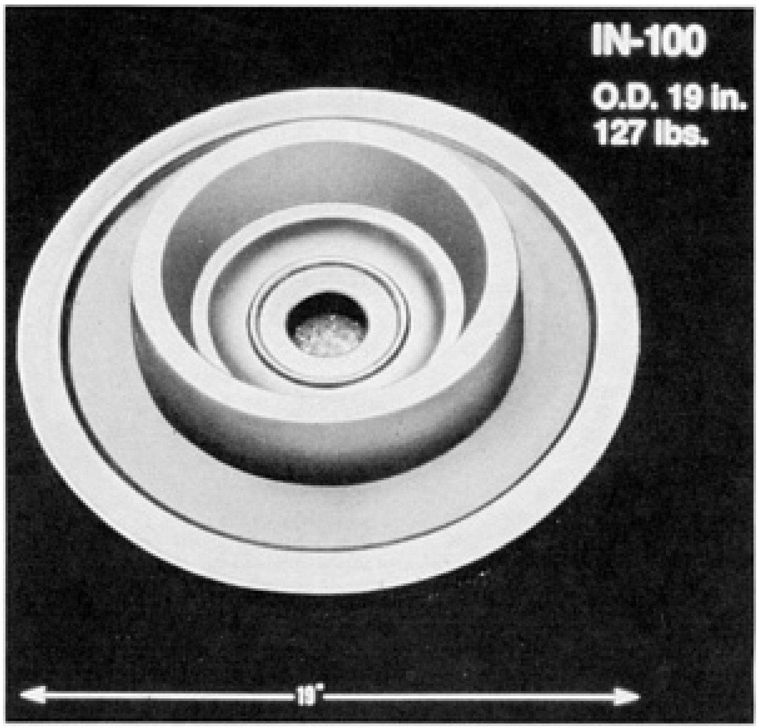

Fig. 16 Isothermally forged Alloy 100 disk

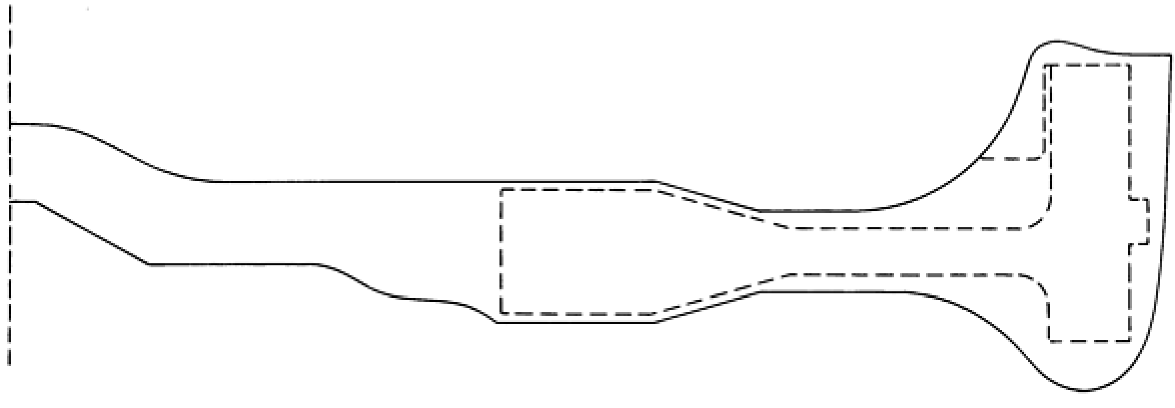

Fig. 17 Schematic cross section of hot-die forged Alloy 718 disk having a 457 mm (18 in.) outside diameter and weighing 38 kg (83 lb).

Isothermal and Hot-Die Forging

Sanjay Shah, Wyman-Gordon Company

References

1. G.W. Kuhlman and J.W. Nelson, "Precision Forging Technology: A Change in the State-of-the-Art for Aluminum and Titanium Alloys," Paper 84-256, Society of Manufacturing Engineers, 1984
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Precision Forging

R.J. Shipley, Textron Inc.

Introduction

THE TERM PRECISION FORGING does not specify a distinct forging process but rather describes a philosophical approach to forging. The goal of this approach is to produce a net shape, or at least a near-net shape, in the as-forged condition.

The term net indicates that no subsequent machining or finishing of a forged surface is required. Thus, a net shape forging requires no further work on any of the forged surfaces, although secondary operations may be required to produce minor holes, threads, and other such details. A near-net shape forging can be either one in which some but not all of the surfaces are net or one in which the surfaces require only minimal machining or finishing. Precision forging is sometimes described as close-tolerance forging to emphasize the goal of achieving, solely through the forging operation, the dimensional and surface finish tolerances required in the finished part.

Cold-forging processes are traditionally precision processes. These are discussed in the Section "Cold Heading and Cold Extrusion" in this Volume and therefore will not be considered further in this article. Similarly, powder forging processes would also be classified as precision forging under the above definition (see the article "Powder Forging" in this Volume). It should be noted at this point, however, that a powder forging approach is often adopted only when it is not economical to precision forge a component from a wrought preform.

In most contexts, including this article, precision forging indicates a hot or warm closed-die forging process that has been upgraded to achieve greater process control. Traditionally, hot forging has not been regarded as a precision process. The term precision warm forging can be regarded as somewhat redundant because one of the motivations for selecting a forging temperature below the hot range is to achieve the advantages in precision associated with lower temperatures.

The examples in this article focus on the precision forging of steel. Detailed information on the precision forging of both aluminum and titanium alloys is available in the articles "Forging of Aluminum Alloys" and "Forging of Titanium Alloys" in this Volume.

Precision Forging

R.J. Shipley, Textron Inc.

Advantages of Precision Forging

Due to difficulties in achieving close tolerance and acceptable surface finish, hot forgings have traditionally been designed with a generous machining allowance, sometimes 3 mm (⅛ in.) or more. The motivation for precision forging is the elimination, or at least the reduction, of the costs associated with this machining allowance. These costs include not only the labor and indirect costs of the machining and finishing operations but also the cost of the excess raw material that is lost during machining.

The savings achieved through material conservation may not be as obvious as the savings obtained by eliminating production machining operations, but it can be quite substantial. Material costs are a significant fraction (often more than half) of the total cost of a forging. The cost of excess material includes not only the purchase price of that material but also the cost associated with handling it in the plant and the energy cost associated with heating it to the forging temperature.

The weight of a traditional forging is often more than twice the weight of the finished part after machining. The machining allowance is responsible for some of this excess material. Significant amounts are also associated with the forging flash. Generous allowances are made in traditional forging for excess material to escape from the die cavity as flash. A study performed by the Forging Industry Association estimated that 20 to 40% of the weight of conventional closed-die forgings is expended as flash. Although flash is sometimes considered necessary for trapping the metal in the die and for ensuring that tight corners or other details are filled, the design of a precision forging usually minimizes and sometimes completely eliminates the flash (see Example 1 in this article).

Another motivation for precision forging is that the mechanical properties of a precision forging are often superior to those of a forging that has undergone extensive machining. This occurs because the forged microstructure is preserved intact in the precision forging. Precision forging may also be attractive to a forge shop because the precision forging represents a higher-value product than a conventional forging; that is, the forge shop achieves a higher "value-added."

Precision Forging

R.J. Shipley, Textron Inc.

Applications of Precision Forging

After it has been decided that a given part will be manufactured by forging, either a traditional or a precision forging process must be selected. Not all part designs are candidates for precision forging.

As presented above, the precision of a forging is defined in terms of its conformity to finished-part requirements concerning overall geometry, dimensional tolerance, and surface finish. These requirements should be derived from the performance of the part that is desired in service. The impact of the requirements on manufacturing options should also be included in the design analysis. Specifically, the application of precision forging can be enhanced by considering the capabilities of the technology during the design process.

Given the nature of forging technology and the wide range of geometries that are forged, the determination of appropriate applications for precision forging processes is best begun through a process of elimination, that is, through consideration of those characteristics that tend not to favor precision forging.

Physical Considerations. A primary consideration is that the forging must be able to be removed from the tooling after the forging process is completed. Thus, geometries that would interlock with the forging dies cannot be forged net. Furthermore, surfaces parallel to the forging axis will often generate high frictional forces with the tooling during ejection of the part. Therefore, forgings are often designed with a slight draft added to such surfaces to facilitate ejection. Although forgings with no draft have been demonstrated, elimination of draft is limited by:

- The capacity of the ejection mechanism of the forging equipment to provide the increased load that will be required
- The strength of the workpiece material at the ejection temperature; the workpiece must also accommodate the increased ejection loads
- Wear of the tooling and/or damage to the surface of the workpiece that might occur because of friction

The physics of the metal flow during the forging process also limit the application of precision forging concepts. For example, it may not be possible for the metal to flow to fill sharp corners or thin sections. Excessively high tooling loads or rupture of the workpiece material may result from problems in metal flow.

Chilling of the workpiece material by the relatively cooler tooling restricts the metal flow. One of the motivations for the development of isothermal and hot-die forging processes (see the article "Isothermal and Hot-Die Forging" in this Volume) is to improve precision.

Alternatively, metal flow issues in precision forging can be addressed by including additional preform steps in the forging process. However, this may not be a practical option in all cases.

Economic considerations also affect the application of precision forging. If only the costs of the forging process itself are considered, precision forging will generally be more costly than traditional forging. This is due to the large number of factors that must be considered in a precision forging process, as discussed in the following sections in this article. Many of these factors are ignored in traditional forging.

The increased cost associated with precision forging will be offset by savings in subsequent manufacturing steps, as discussed above. However, if the number of parts required is relatively small, the savings in material, machining, and so on, may not be sufficient to offset the increased costs of precision forging. This may occur because a significant portion of the cost differential associated with precision forging is a fixed cost, that is, independent of the actual number of pieces forged.

Precision forging is especially attractive in the case of parts with complex surfaces that are difficult or costly to machine. Turning is a relatively inexpensive operation in comparison with milling, grinding, or gear cutting. Not surprisingly, many precision forging applications involve gears and similar types of parts (see Example 2).

Given a geometry that is amenable to precision forging, tolerances of ±0.25 mm (±0.010 in.) can generally be achieved. In many cases, significantly better tolerances have been demonstrated. Comparison of the forging tolerances and surface finish with the part requirements determines whether any machining will be required. Again, economic analysis is critical in determining the benefits of a net shape forging versus a conventional forging or a near-net shape forging versus a conventional forging.

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Tooling Design Considerations

The design of the forging tools must include analysis of all effects that could impact on the precision of the process. Allowance should be made for the thermal expansion of the tooling because it is generally at some elevated temperature during the forging process. Similar allowance should be made for contraction of the workpiece as it cools after forging. Thermal contraction is estimated from the workpiece temperature at die closure (see Eq 1). These allowances are typically of the order of hundredths of a millimeter (thousandths of an inch)--comparable to the tolerances desired in the precision forging process.

Elastic deflection of the tooling and the forging equipment can also occur during the forging process and can affect the tolerance achieved. In many cases, the elastic deflections are small and may be safely neglected. However, this is not always the case, as demonstrated in Example 1. Elastic expansion of the workpiece as the forging load is released is usually not significant and can be neglected, because the flow stress is low at elevated forging temperatures.

The dimensions of the forged part will be decreased relative to the dimensions of the die cavity by the thickness of the forging lubricant at die closure. The thickness at die closure will generally be less than the thickness applied to the dies and/or forging preform. In many cases, the lubricant layer is very thin and can be neglected. In other cases, it may be significant. Thicker coatings are sometimes applied to billets prior to forging as protection against oxidation during subsequent heating. Buildup of the lubricant in the tooling can also be a problem in some cases.

As discussed above, metal flow patterns are an important consideration in precision forging. The design of the tooling must ensure an appropriate preforming sequence to control the metal flow in order to fill the die contours and to achieve an acceptable surface finish. The magnitude of chilling must also be evaluated because the flow stress of the metal is a function of temperature.

To assess the feasibility of a precision forging design, both the forging load and the workability of the workpiece material must be considered. As mentioned above, an estimate of the forging load is necessary for calculating elastic deflections in the tooling and fixturing. Excessively high loads cause premature failure of the tooling, either through increased friction and wear or gross overload.

The workability of the workpiece material is a quantitative measure of how much deformation can be accommodated without cracking or other forms of failure. Workability is more critical in precision forging than in conventional forging because higher deformation levels may be required to achieve the tolerances required in precision forging. Deformation levels can be especially high in localized areas. Furthermore, the workability index of the material can be decreased in a precision forging process if the forging temperature is decreased in an effort to improve precision. (There would be exceptions in the case of materials whose workability actually improves with decreased temperature.) Workability tests and theory are discussed in the Section "Evaluation of Workability" in this Volume.

In practice, consideration of the above-mentioned factors is extremely difficult for all but the simplest forging geometries. Accurate calculation of the temperature gradients in the workpiece and tooling requires a heat transfer analysis. Calculation of elastic deflections requires knowledge of the forging loads and a stress analysis of the tooling and associated fixturing. Calculation of metal flow for preform design is even more complex.

Mathematical models of the forging process based on the finite-element method have been developed to aid the forging design engineer in the required analyses. These models have been implemented through computer programs that provide the required temperature and stress profiles and allow the designer to simulate the metal flow that occurs during forging. Process modeling and simulation are discussed in detail in the article "Modeling Techniques Used in Forging Process Design" in this Volume.

Analysis of a precision forging process through computer-based models is most readily accomplished if the forging tooling is initially designed on a computer-aided design and manufacturing system. Even if computer models are not employed, computer-aided design and manufacturing will still be valuable in the design of precision forging tooling. The goal of net shape, or at least near-net shape, dictates that precision forging tooling will be more detailed and complex in comparison with conventional tooling. Furthermore, the accurate calculation of volumes and surface areas, which is done automatically with computer-aided design and manufacturing, is more critical in precision forging than in conventional forging. Applications for computer-aided design and manufacturing in forging are discussed in the article "Forging Process Design" in this Volume.

Physical modeling is an alternative to mathematical simulation of the forging process on a computer. Physical modeling involves construction of an analog model of the tooling and workpiece material. For example, observation of the flow of Plasticine (a modeling clay) at room temperature has been found to be helpful in understanding metal flow during forging. The tooling in a physical model is typically fabricated of Plexiglas to enable continuous observation during deformation. Metal flow patterns may be highlighted by constructing the preform from different colors of clay. Physical modeling is discussed in the article "Modeling Techniques Used in Forging Process Design" in this Volume.

Even with the most sophisticated analytical techniques, some further development of the precision forge tooling may be necessary on the shop floor. In some cases, forging parameters and/or the dimensions of the die cavity may have to be adjusted to achieve the required tolerances. In other cases, a preforming sequence may have to be redesigned. The amount of development would generally be decreased with a greater amount of analytical work. The optimal approach to implementing a precision forging process will be determined by an economic balance of the costs of analysis versus the costs of some trial and error on the shop floor. This balance will generally be different for every shop.

The analytical approach to design is most appropriate if one has little or no experience in precision forging the type of geometry being considered. The difficulties of implementing the process for a given part are clearly lessened if a forge shop has experience with other parts of similar geometries. In this case, the required analysis and trial and error on the shop floor will both be minimized. The precision forging tooling will be designed based on heuristics, that is, empirical correlations or rules of thumb that have been established through experience. Computer programs known as expert systems represent an attempt to capture and promulgate this type of practical knowledge. Ideally, the empirical and analytical approaches can be combined so that new applications of precision forging technology can be developed by building on the experience base already in place.

Precision Forging

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Process Control Considerations

After a candidate part for precision forging is identified and the tooling is designed, implementation requires increased attention to detail and process control at every step of the manufacturing process. At a minimum, all of the factors discussed below usually must be considered. The significance of a given factor depends on the geometry and tolerance requirements of a given forging. In addition, there may be other factors not listed here that are unique to a particular application.

Precision of the Tooling. A precision forging requires precision tooling. The tolerance achieved in the forging will clearly be no better than the tolerance of the tooling. Because many factors influence the forging tolerance, it will typically be significantly worse than the tooling tolerance. Therefore, the tolerance bands for precision forge tooling must be set at a small fraction (for example, $\frac{1}{10}$ to $\frac{1}{3}$) of the desired forging tolerances. This is similar to the rule of statistical process control that the capability (variation) of a gage must be an order of magnitude better than the allowable variation of the machine or workpiece being measured.

After the precision forge tooling is built, it should be inspected to ensure that it meets the design requirements. This inspection may be difficult if the tooling has contoured surfaces. Nevertheless, if the tooling is not inspected, it will be that much more difficult to determine the causes of any out-of-tolerance condition in the forgings.

Gaging developed for inspection of the forged part generally cannot be used for inspection of the tooling, even if a cast impression of the die cavity is obtained. Due to the various allowances included in the tooling design, the dimensions of the die cavity will be distinct from those of the forging. Coordinate-measuring machines are often used to inspect precision forge tooling. The inspection data can be kept on file and referenced later to determine the extent of wear after the tooling has been in service.

Concern regarding the tolerance of the forging tools may require the forge engineer to consider the capabilities of the machining processes employed to build the tools. In particular, if the die cavities are produced by electrical discharge machining (EDM), the tolerance of the tools will depend on both the tolerance of the electrode and the tolerance achieved in the EDM process itself. In machining the electrode, allowance may have to be made for the spark gap in the EDM process.

After the tooling is placed in service, its precision will deteriorate because of wear. Die wear is an important factor in determining die life in precision forging. Even a small amount of wear can result in an unacceptable loss of precision. The cost of reworking or replacing worn tools must be included in the analysis of the economics of precision forging.

Precision of the Setup. Control of the alignment and setup of the tooling in the forging press is just as important as the tolerance of the tooling itself. The fixtures used to hold the die blocks for precision forging in presses are frequently designed with posts or similar devices for maintaining alignment.

The setup of the tooling affects the thickness of the forged part. Thickness may be important in its own right, if there is a close tolerance on any of the thickness dimensions of the part. However, thickness is also important because the overall volume of the forged part is dependent on thickness. Because precision forgings are usually designed with little or no flash, the volume of the finish forging in relation to the volume of the preform is critical. If the tooling is set up so that the volume of the finish forging would be too great, a lack of fill in the corners would generally result. If the setup is such that the volume of the finish forging cannot accommodate the entire preform, the tooling or the forging equipment could be damaged.

Precision of the Preform. In a one-hit precision forging process, the preform is simply the slug of raw material sheared or cut from bar or coil stock. In a progressive forging operation, the preform is the product of a series of intermediate forging operations. In both cases, the quality of the preform is of concern because it limits the precision of the finished forging.

As discussed above in connection with the setup of the tooling, the relationship between the volume of the preform and the volume of the finish forging is critical. If the geometry of the preform is complex, the distribution of volume in the preform may also be important to ensure the proper metal flow in the finish forging. Thus, precision forging requires a precision preform. In progressive forging, each forging step must be considered to be a precision operation.

With respect to the raw material, the volume of the slug or starting billet is the product of its cross-sectional area and length. Tolerance on the area is controlled by the capability of the mill. Tolerance on length is determined by the capability of the shear or other billet separation equipment employed by the forge shop. In some cases, existing tolerance capabilities may not be adequate for the requirements of precision forging. Machining of the raw material (turning in the case of round stock) and/or saw cutting would be options in this situation, but would generally add significantly to manufacturing cost. Purchasing cold-drawn stock or cold drawing immediately prior to shearing is another way to achieve a precise cross-sectional area, again at some cost penalty.

Volume can also be controlled by weighing the slugs prior to forging and rejecting those outside specified limits. This would be economical only if the rejection rate were not too high. Another approach to controlling volume would be to introduce a simple upset of a few percent as the first forging step. In this approach, the blank could be slightly oversize, and the upset tooling would allow for flash. After removal of any flash, a precision slug would remain, its volume being determined solely by the upset tooling.

The surface condition of the preform is also important because it can affect the surface quality of the finish forging in regions where it is desired to minimize or eliminate machining. Prevention of oxidation (scale) is one concern and is discussed in more detail in the section "Selection of Process Temperature" in this article. The quality of the sheared or cut surfaces on the starting billet is also of special concern. The precision forging may sometimes be designed so that those surfaces will correspond to noncritical areas.

Control of the chemical composition and metallurgical microstructure of the raw material may also be important in some applications of precision forging. For example, in the precision forging of steel, there may be requirements that net surfaces cannot be decarburized. In addition, for certain alloys, variations in microstructure and/or composition may affect the metal flow during forging.

Control of Lubrication. Of all forging variables, the performance of the lubricant may be the most difficult to quantify. However, lubrication is also recognized as one of the factors that is most critical to the success of any forging process, precision or otherwise. Lubrication influences the total forging load, the degree to which the metal will fill the cavities of the dies, the uniformity of the resultant metallurgical microstructure, and the surface quality of the forged product.

Control of lubrication in precision forging can be approached indirectly by stressing consistency in the lubricant composition and application. Samples of the lubricant should be taken upon delivery from the supplier and after any dilution. Samples should also be taken to ensure consistency during production.

Application of the lubricant is also critical. If the lubricant is sprayed manually, variations in the precision of the forging can often be correlated with the different techniques employed by various operators. Automatic lubrication equipment is frequently used to achieve greater consistency. Even in this case, though, attention still must be given to lubrication to ensure that the equipment is functioning properly, that all nozzles are clear, and so on. If a coating is applied to the billet or preform prior to forging for lubrication or other purposes, the same care must be exercised to achieve consistency.

Control of Workpiece Temperature. The temperature of the workpiece is a critical variable in precision forging. This section discusses the control of temperature within the context of total control of the forging process. This assumes that the forging temperature has already been specified. A subsequent section in this article will discuss the selection of an appropriate process temperature.

Strictly speaking, it is not correct to refer to the temperature of the workpiece. With the exception of isothermal forging, there will actually be a temperature gradient in the workpiece that will be continuously changing during the forging process. In most cases, forging temperature refers to the temperature of the workpiece at a point of measurement (for example, in the furnace, as it exits an induction coil, before it is placed in the die, and so on). For a precision process, this temperature must generally be controlled to within ±10 to ±20 °C (±20 to ±35 °F). This tolerance may be tighter in some critical applications, and a slightly less stringent tolerance may be allowed in others.

It is generally not practical or necessary to measure the temperature gradient directly. However, control of the gradient can still be achieved by control of the nominal workpiece temperature before forging and by consistency in all other aspects of the process that could affect the heat transfer from the workpiece.

The workpiece begins to lose heat as soon as it is removed from the furnace or other heating equipment. Variation in the timing of the transfer or variation in the ambient conditions in the forge shop can affect the temperature of the workpiece as it is forged. Automated handling equipment can be employed to achieve greater consistency in transfer of the workpiece into the forging dies.

The workpiece is chilled further when it comes into contact with the tooling. Heat transfer to the tooling is a function of the tooling temperature and the heat transfer coefficient established across the lubricant interface. Heat transfer is increased by the close contact that occurs under forging pressures. Therefore, die contact time under load also affects the extent of chilling that will occur. For a given forging geometry, die contact time should be constant because it is determined by the operating characteristics of the forging equipment. Contact time is an important parameter, however, in comparing different types of forging equipment.

In some analyses of forging temperature, it may also be necessary to account for the heat of deformation. A high percentage (usually over 90%) of the mechanical energy of the forging process is converted into heat within the workpiece. The temperature of the workpiece would tend to increase as a result. Therefore, the temperature of the workpiece during forging is determined by an energy balance involving the heat lost to the environment and the heat generated by deformation.

The workpiece temperature affects the precision of the forging through:

- The effect of thermal contraction
- The effect of temperature on material flow stress and elastic compliance of the tooling and forging equipment
- The effect of temperature on lubricant performance

As discussed above with respect to tooling design, the dimensions of the forged part are directly related to the anticipated forging temperature because of the thermal contraction that occurs as the forging cools. The calculation of the thermal contraction allowance assumes that the workpiece conforms perfectly to the die cavity when the dies are fully closed. An average workpiece temperature must be estimated, taking into account the extent to which the workpiece has cooled up to this point. Analyses of heat transfer in forging have been developed for this purpose. Equation 1 can be used to estimate the allowance for thermal expansion:

$$D_F \cdot [1 + \alpha_F \cdot (T_F - T_o)] = D_D \cdot [1 + \alpha_D \cdot (T_D - T_o)] \quad \textbf{(Eq 1)}$$

where D refers to a linear dimension, T is temperature, and α is the thermal expansion coefficient. The subscripts F and D refer to the forging and the die, respectively. The subscript o refers to ambient temperature. As indicated above, the workpiece temperature will be an average value. The die temperature will also generally be an average value. If thermal expansion is not linear over temperature for the die or workpiece material, an average value should also be used here.

In addition to thermal contraction, temperature affects the precision of the forging process through the variation in the flow stress of the workpiece material that occurs with changes in temperature. As discussed in the section "Selection of Process Temperature" in this article, material flow stress and workability are important considerations in the selection of a forging temperature.

As discussed in the section "Tooling Design Considerations" earlier in this article, once the forging temperature has been selected, flow stress can be estimated and the magnitude of the forging load can be calculated. The elastic deflection of the tooling, fixturing, and in some cases the forging equipment itself can then be estimated, and the appropriate allowance can be made in the tooling design. If elastic deflections are significant and a change in workpiece temperature occurs that significantly alters the flow stress, then the elastic response would also be affected with a resultant change in the as-forged dimensions.

Concern regarding changes in material flow stress with temperature is most likely in the case of precision warm forging (see the section "Selection of Process Temperature" in this article) because the flow stress is most sensitive to changes in temperature in the warm range. In addition, the flow stresses are higher in the warm range, so elastic effects will be more significant.

The temperature of the workpiece can influence the behavior of the forging lubricant. The importance of consistent lubricant performance in achieving a precision process has already been noted above. Workpiece temperature will have an especially important effect on the process lubrication if a lubricant coating is applied directly to the preform in addition to (or instead of) the lubrication of the tooling.

If scaling (oxidation) of the workpiece is of concern, it should be noted that this too will be a function of temperature, as shown in the section "Selection of Process Temperature" in this article. Generally, oxidation must be avoided in precision forging.

Control of forging temperature may also be mandated to control the metallurgical response of the workpiece material. The extent of the work hardening and recrystallization that occurs will depend on forging temperature. Metallurgical transformations may also occur during forging, depending on the process temperature. Consideration of the metallurgical behavior is particularly important if it is desired to minimize or eliminate heat treatment after forging. Metallurgical transformations can also influence the dimensions of the as-forged part if they result in a change in volume. Metallurgical considerations are particularly important in the case of warm forging of steel, in which the warm temperature range is defined as approximately 540 to 815 °C (1000 to 1500 °F).

Steel undergoes a metallurgical phase transformation at temperatures within or slightly above the warm-forging range. This transformation is associated with a change in volume distinct from that which is due to purely thermal effects. It occurs over a range of temperature that is dependent on alloy content. Depending on the requirements of a particular application, the warm-forging temperature may be below, within, or above the transformation temperature range. The relationship of the forging temperature to the transformation temperature determines the metallurgical microstructure that will be developed in the forging upon cooling.

Therefore, variation of process temperature can lead to inconsistent metallurgical response in ferrous warm forging through the influence of temperature on work hardening, recrystallization, and phase transformation processes. This would be an especially significant issue if it were desired to avoid heat treatment after forging. Variation in forging temperature could also lead to lack of control in workpiece volume due to phase transformation, in addition to purely thermal effects.

Finally, concern regarding the workpiece temperature does not end after the precision forging process is completed. Controlled cooling of the workpiece may be necessary after forging to avoid distortion and to control the metallurgical microstructure.

Control of Tooling Temperature. Tooling temperature is important in precision forging for many of the same reasons as workpiece temperature is important. The temperature of the tooling directly affects the workpiece temperature through the heat transfer, which is dependent on the temperature differential between the workpiece and the tooling. The chilling of the workpiece by the tooling is especially significant if thin sections are being forged. Flash, if present, is one example of this effect. The flow stress within the flash is typically much higher than that in the die cavity because of chilling. If the tooling temperature is greater than ambient, the thermal expansion of the tooling will affect the final dimensions of the forged part, as indicated by Eq 1.

The temperature of the tooling also affects the behavior of the forging lubricant. Specifically, the die lubricant is carefully formulated to be applied to tooling at a temperature within a narrow range. A temperature that is too high or too low will affect the quality of the lubricant coating and the performance during the subsequent forging operation.

It was noted above that the workpiece cannot be characterized in terms of a single temperature, because a thermal gradient occurs as heat is lost from the surface. The same type of situation occurs in the tooling, but temperature decreases with distance from the surface because heat is introduced there by contact with the hotter workpiece. When the workpiece and tooling are in contact during forging, the gradient can be imagined to be continuous across the interface.

Dies are usually preheated prior to forging so that the tooling temperature during a production run will be relatively constant. One reason for this is because the toughness of many tool materials is very low at ambient temperatures. With these materials, if the tooling were not preheated, it might crack as the initial pieces were forged.

A second benefit of preheating, important in precision forging, is that variations in tooling temperature do not affect the precision of the process over the course of the production run.

Knowledge of the thermal gradient in the tooling is important because the different effects of temperature discussed above actually depend on the magnitude of the temperature in specific locations. The performance of the lubricant depends on the temperature of the surface to which it is applied. The overall thermal expansion of the tooling will be a function of a volume average temperature. Distortion and thermal stresses may occur if the gradient is too severe.

Toughness is a material property that will vary with the temperature at each location. If toughness is a concern, it is important that the entire die be above a minimum temperature.

The potential for thermal fatigue (heat checking) of the tooling can also be related to the thermal gradient. Stresses can be generated at the surface of the tooling as it is alternately exposed to the workpiece at high temperature and then to the cooling effect of the die lubricant. Thermal fatigue is controlled by selection and heat treatment of the die material so that it is appropriate for the workpiece temperature and lubricant employed.

Control of thermal fatigue is demonstrated in such applications as some of the automated formers to be discussed in the section "Equipment Considerations" in this article as well as the flashless forging in Example 1. The tooling is maintained at essentially ambient temperature by a flood of coolant that also functions as a die lubricant. In the case of the formers, a relatively short contact time also helps to prevent thermal fatigue.

As a first step in obtaining data on tooling temperature, the surface temperature of the dies can be measured with a contact pyrometer as the forging is removed. Temperatures in the interior can be monitored through thermocouples inserted into the tooling or the associated fixtures.

Equipment Considerations

The importance of process control in precision forging was discussed in the preceding sections in this article. An initial step in achieving the required level of control is a careful evaluation of the capability of all equipment to be employed in the precision forging manufacturing process. Requirements for lubrication equipment were summarized previously. This section will discuss equipment for billet separation and heating, as well as the actual forging operation itself. Further details on the various types of forging equipment mentioned can be found in the appropriate Sections in this Volume. The treatment here follows a previous assessment by the author of equipment capabilities within the context of precision forging at warm temperatures (Ref 1).

Billet Separation Equipment. To achieve the required dimensional tolerance and surface finish, precision forging requires greater care in billet preparation than does traditional hot forging. Shearing is the most efficient billet separation method because production rates can be high and there is no material loss. Control of billet length at the shear is critical in order to maintain the precise volume control required by precision forging. Control of billet diameter on the raw material is also critical for the same reason. Material is frequently cold drawn to a slight reduction prior to shearing to ensure a precise diameter. Cold drawing has also been said to improve the microstructure to facilitate the shearing process itself.

Tolerance capabilities claimed by builders of shearing equipment are generally found to be adequate for precision forging. However, this must be verified for each application.

The design of the handling system used to feed bar or coil into the shear affects the precision obtained. Rebound of the raw material, if it is stopped prior to shearing, could be a problem. In the case of a coil, binding at any point in the uncoiling or straightening process could potentially affect billet length.

In addition to billet volume, the quality of the sheared end surfaces is another important consideration for precision forging. Because the as-forged component includes little or no machining allowance, surface imperfections generated during shearing may affect the quality of the forging. In general, the sheared surfaces should be smooth and free from hollows, burrs, or any type of crack. They should also be parallel and perpendicular to the axis of the bar. Where quality requirements associated with a specific surface of the precision forging are particularly stringent, it may be possible to design the tooling so that the critical surface corresponds to the circumferential surface of the billet rather than the sheared end.

When precision forging is done in multiple deformation steps, the first step is often a simple upset, either done in the first station of a transfer press or header or in a separate machine set up just for that purpose. Any deviations in billet volume will be revealed at this point, and upsetting ensures square ends to avoid misalignment and nonuniform loading, which could cause breakage of tooling in subsequent operations.

As indicated above, shearing is generally the method of choice for billet separation. In some instances, larger-diameter billets or higher-strength material might not be able to be sheared with the existing equipment in a particular shop. In these cases, preheating the billet before shearing may enhance the capability of the shear. The automatic formers described later in this article often shear billets from a preheated coil in the first station.

Billets can also be prepared by sawing. Sawing is a slower and generally more costly process than shearing, and burrs may be more of a problem; but it may be easier to address concerns regarding volume control and quality of the cut surface. In addition, sawing is more readily adapted to billets of different sizes, so it may be indicated for relatively short production runs involving larger-diameter or higher-strength billets that cannot be sheared on existing equipment.

Heating Equipment. As is the case with the preparation of the forging billet, heating of the billet to the forging temperature also requires greater care than in traditional hot forging if increased precision and improved surface finish are to be realized. Formation of oxide scale is a particular problem in the precision forging of steel. Any tendency to form scale can be minimized by rapid heating. Although the oxidation rate at the lower process temperatures often used for precision forging is significantly less than at traditional hot-forging temperatures for steel (see Fig. 7), which are in excess of 1100 °C (2010 °F), oxide scale could still be a problem if the time at forging temperature is extended unnecessarily. Where rapid heating is not practical or the thickness of the oxide layer is still unacceptable, an oxygen-free atmosphere (for example, nitrogen) can be used to control oxidation.

As discussed previously in this article, accurate control of temperature is critical in precision forging. The temperature distribution within the billet should also be as even as possible to avoid temperature-dependent variation in flow stress or variation in the metallurgical response within the workpiece. Temperature gradients within the workpiece can arise as the billet is being heated, but they are also influenced by handling of the billet after leaving the furnace. Portions of the billet that are in contact with tongs, conveyors, clamps, or other handling equipment will be cooler than portions that do not make contact. Contact with the tooling itself prior to forging also tends to chill those portions of the workpiece involved. Therefore, the design, consistency, and timing of manual operators or automated billet handling equipment can affect the precision forging process.

Tolerances on temperature of ±10 to ±20 °C (±20 to ±35 °F) have been found to be adequate in most precision forging applications. The tolerance required is dependent on the details of the application. Closer temperature control may be required as increased precision is attempted.

Induction heating is often used for precision forging because it reasonably meets the criteria outlined above. However, resistance-heating, gas-fired continuous, and gas-fired batch furnaces are also successfully used. Control of an induction furnace is not always as straightforward as with other heating systems, especially if the same coil is used with billets of different diameters or cross section and/or multiple billets are being heated within the coil at the same time.

Forging Equipment. Many of the same types of forging equipment used for traditional nonprecision forging can also be used for precision forging. However, if the intention is to reduce the forging temperature to achieve greater precision, the flow stress of the material, and therefore the forging load, can be increased and can exceed the capacity of the equipment previously used successfully for nonprecision forging at a higher temperature. Furthermore, before precision forging is attempted, the operating characteristics of the equipment must be examined from a process control perspective.

No one type of forging equipment will necessarily be best for all precision forging applications. Furthermore, there may be many options for a given application, and the decision, to a great extent, can be reduced to what equipment a particular forge shop may have available. Factors that must be considered in evaluating equipment for a particular application include the size and configuration of the part, type of material, production quantity, production rate, raw material requirements, tolerance, and amount and cost of scrap generated. Labor, overhead, and energy are also important factors. A proper balance of these various considerations will ensure that the part is produced at lowest cost.

Hammers could conceivably be used for precision forging. However, achieving the required level of process control would be difficult because hammers are generally not operated as precision forging machines. Fixed stop blocks would be required in the tooling to control the thickness of the forging. Attention would also need to be given to controlling the stroke(s) to be as reproducible as possible. The sensitivity of flow stress to temperature could cause problems, especially if multiple blows were required and if excessive chilling of the workpiece occurred. The lack of knockouts in hammers would make it difficult or impossible to implement flashless forging with little or no draft.

It is conceivable that hydraulic presses could also be used for precision forging. As with hammers, the thickness of the forging could be controlled with stop blocks incorporated into the tooling. However, stop blocks might not be absolutely necessary with a hydraulic press if the ram position could be precisely controlled. If forging temperatures were relatively high, the relatively slow ram velocity and long dwell time of the hydraulic press would be a concern because of the increased potential for chilling of the workpiece and overheating of the tooling.

Screw presses offer much potential for precision forging, especially in cases in which the thickness of the forging is critical. A screw press has some of the characteristics of a hammer in that the stroke is not fixed. However, the stroke of a screw press can be controlled much more precisely. The thickness tolerance for a part forged on a screw press can be closely controlled through stop blocks or kiss plates built into the tooling.

Because a screw press is an energy-controlled machine (that is, the ram is not forced to move through a fixed stroke as is the case for mechanical presses), the energy and/or load that the ram exerts can be limited to that necessary to form the part. There is less concern that an oversize billet will result in damage to the press or tooling. In most cases, however, an oversize billet will result in an excessively thick forging. Therefore, volume control is still critical to the precision of the process, especially when there is a close tolerance on the thickness dimensions.

Some designs of screw presses may not have sufficient energy for workpieces requiring extensive deformation (for example, extrusion operations). However, higher-energy screw press designs have also been developed. In applying a screw press to a high-speed automated operation, there would be concerns regarding its stroking rate. In a transfer forging operation, there would also be concerns regarding its ability to accommodate off center loading with multiple-cavity tooling. Traditionally, mechanical presses are superior to screw presses in these respects, but improvements in screw press design have been demonstrated.

Many precision forging applications have been developed on mechanical crank type presses. In a mechanical press, the stroke is fixed by the characteristics of the drive mechanism. Therefore, mechanical presses differ in a fundamental way from hammers, hydraulic presses, and screw presses, in which the stroke is not fixed. In a mechanical press, the thickness of the forging will be affected by changes in the stroke. For example, if the temperature of the press increases during a production run, the thermal expansion of the press components could affect the thickness tolerance of the forging.

Furthermore, the components may deflect under the forging load, also affecting thickness. Although these changes are small and are normally not even considered in conventional forging, they can be significant in comparison with tolerances of hundredths of a millimeter (thousandths of an inch).

A mechanical press with a tension knuckle joint drive mechanism has been said to offer advantages for precision flashless forging (see Example 1). The tension knuckle drive pulls, rather than pushes, the ram. The toggle or tension link elongates under forging pressure. The elongation of the tension link results in a press that is less stiff than conventional mechanical presses; that is, the tension link stretches to a greater extent under forging load than the frame of a mechanical press does. In precision flashless forging, the tension link can stretch without damage to accommodate variations in the volume of the forging billet, thus protecting the tooling and the press itself from damage.

In comparison with a crank press operating at a comparable stroking rate over a comparable stroke length, the tension knuckle drive will result in a slower ram velocity during the actual forging process. This results in lower impact forces on the tooling upon striking the forging billet and should tend to increase tool life. The tension link itself also tends to act as a shock absorber to alleviate impact loading imposed on the tool. However, the tendency of the slower ram velocity to increase contact time and heat transfer to the tooling (thus decreasing tool life) must also be considered. The lower impact velocity in this type of press does result in a reduced level of forging noise and vibration, which may be important from an environmental perspective.

Horizontal forging machines (also known as formers, upsetters, or headers) have also been developed and employed for many precision forging applications. The capabilities of formers overlap those of presses. Advantages include high production rate, short dwell times, and good die cooling. Generally, they are limited to smaller workpieces and longer production runs than presses. These machines are designed and built to facilitate automated forging with multiple dies arranged horizontally. In many cases, they are automated coldforming machines that have been modified to operate with the workpiece material at elevated temperatures. Typically, raw material in the form of coiled wire is preheated by induction to the forging temperature before it enters the former. However, heating by resistance or with gas-fired furnaces and raw material in the form of bar stock or precut slugs are also not uncommon.

In the case of coils or bar stock, the incoming material is first sheared and then transferred from die to die until the finish-formed part is ejected. Production rates are dependent on part size and are usually of the order of 1000 to 5000 or more pieces per hour. With these high production rates, formers are most applicable to high-volume production requirements. Material handling to and from the former must also be adequate to ensure continuous production.

To offset the cost of tooling and setup times, automatic forming processes generally require production quantities of about 25,000 pieces for relatively large parts; production quantities can range to 100,000 pieces or more for smaller parts. The specific details of each case may shift these breakpoints. For example, the use of quick die change procedures to minimize setup times and group technology programs to take advantage of commonalities among setups for similar parts may allow for shorter production runs.

Control of die and workpiece temperature is critical with all automated forging equipment and especially with formers. If workpiece temperature is too low, excessive machine and tooling loads and/or cracking problems may be encountered. If workpiece temperature is too high, the metallurgical microstructure of the part may be adversely affected, the metal may smear over the cutoff tooling, and/or metal flow patterns may be uncontrolled. Examples of precision forgings produced on formers are shown in Fig. 1(a), 1(b), 1(c), 1(d), and 1(e).

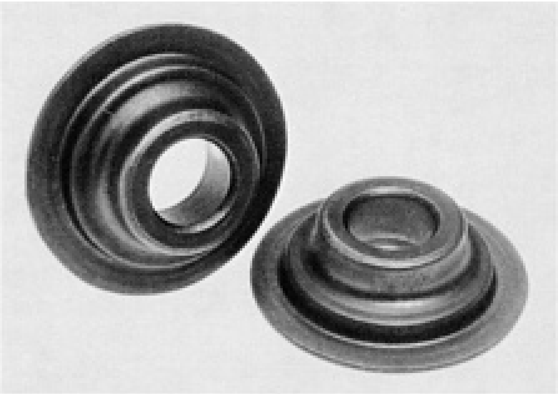

Fig. 1(a) Valve spring retainer with 2.0 mm (0.08 in.) flange thickness. Material is 4115 steel wire that was warm forged on 2200 kN (250 tonf) horizontal forging machine. Courtesy of National Machinery Company.

Fig. 1(b) Differential bevel gear weighing 3.5 kg (7.8 lb) that was hot forged from 16CD4 (similar to 413O) bar material on a 24 MN (2700 tonf) horizontal forging machine. Courtesy of National Machinery Company.

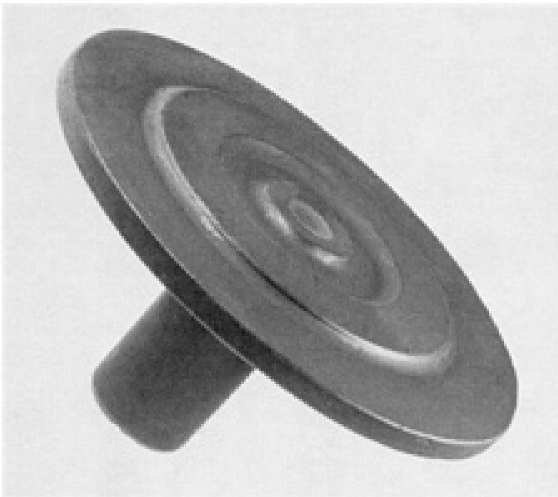

Fig. 1(c) 2.7 kg (6 lb) front wheel hub that was hot forged from 37C4 (similar to 5135 alloy steel) bar material using a 24 MN (2700 tonf) horizontal forging machine. Courtesy of National Machinery Company.

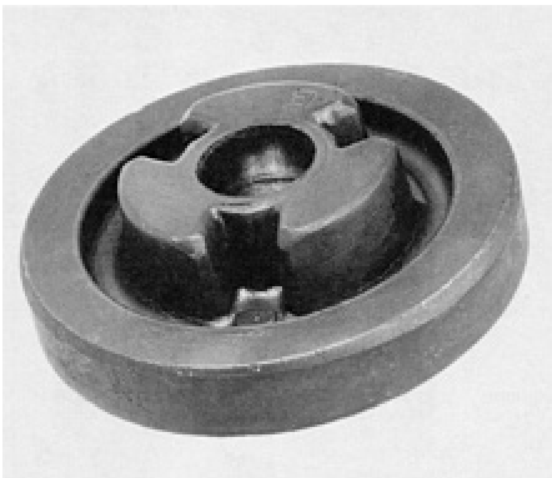

Fig. 1(d) 147 mm (5.8 in.) OD universal pinion for first and second gears that was hot forged from 30CD4 (similar to 4130) bar material using a 12.0 MN (1350 tonf) horizontal forging machine. Courtesy of National Machinery Company.

Fig. 1(e) Connecting rod cap measuring 84 mm (3.3 in.) long that was hot forged from 1038 bar material using a 7100 kN (800 tonf) horizontal forging machine. Courtesy of National Machinery Company.

Reference cited in this section

1. R.J. Shipley, T.G. Kalamasz, W.S. Darden, and D.J. Moracz, "Research on the Energy Conservation Potential of Warm Forging Technology," Final Technical Report, Department of Energy Contract No. DE-AC07-84ID12528, National Technical Information Service, 1985

Precision Forging

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Selection of Process Temperature

As mentioned in the definition of the scope of this article, the greatest precision in forging is almost always achieved in cold forging. Therefore, from the perspective of precision, if it is possible to forge a part cold, that will generally be the method of choice.

Higher forging temperatures are usually employed only if:

- The forging load at ambient temperature would exceed the capacity of existing, economical equipment and/or tools. This could be due to a high flow stress of the workpiece at ambient temperature, the complexity of the metal flow, and/or the overall size of the part
- The material workability at ambient temperature does not allow for the required metal flow
- An excessive number of intermediate anneals would be required to overcome the effects of work hardening

In practice, the above restrictions mean that a wide range of parts and materials must be forged at elevated temperatures. Cold forging cannot even be considered in many cases.

Increasing the workpiece temperature results in decreased flow stress and usually increases workability (ductility). The Section "Evaluation of Workability" in this Volume contains more details regarding workability. Therefore, for a given part configuration, tool stresses will be lower, and the total press load will be reduced. Alternatively, for a given equipment capacity, increased process temperature allows for the production of larger parts.

Some materials that are difficult or impossible to forge cold can be successfully formed at higher temperature, thus expanding the range of materials used in cold forging. Many materials must be annealed prior to cold forging. For example, for medium-carbon to high-carbon steels, a long spheroidize anneal may be necessary. In parts requiring extensive deformation, one or more intermediate anneals may be necessary to counteract the effect of work hardening. Increasing the forging temperature can eliminate the need for these relatively costly and energy-intensive anneals. Within limits determined by the metallurgical response of the workpiece material, the process temperature can be adjusted so that the strength level in the forging is at the desired level. This can help to eliminate the need for heat treatment after forging.

Some geometries that may be difficult to forge cold can be readily accomplished with increased forging temperature. For example, for a given material, thinner flanges and sharper corners and shoulders could usually be produced at increased temperatures. A given material can generally accommodate greater deformation before cracking when forged at higher temperature, and a given geometry can sometimes be forged in fewer stations in comparison with cold forging.

Selection of the process temperature will be based first on the workpiece characteristics to ensure that the metal flow stress is low enough to allow forging on available equipment and that workability is sufficient to allow the required deformation without cracking. Unfortunately, in comparison with what is needed, the literature contains limited data on material flow stress and workability as a function of temperature.

At relatively low temperatures, flow stress is primarily a function of strain. At higher temperatures, strain is less important than strain rate. At intermediate (warm) temperatures, both strain and strain rate may be important. Flow stress data can be presented in either graphical or tabular form. In the case of the latter, values of coefficients for a constitutive equation are tabulated.

Both strain dependent and strain-rate dependent coefficients have been obtained for numerous materials by utilizing the least-mean-square-fit technique to calculate the coefficients from stress-strain curves. An empirical expression for the strain dependency of the flow stress, $\overline{\sigma}$, is:

$$\overline{\sigma} = K(\overline{\epsilon})^n \qquad \textbf{(Eq 2)}$$

where $\overline{\epsilon}$ is the true or logarithmic strain, and K and n are empirical constants. Strain dependent data for carbon steels are shown in Table 1 and data for alloy steels in Table 2.

Table 1 Mechanical properties of carbon steels

Average strain rate: 8 mm/mm/s (8 in./in./s). Source: Ref 2

Steel grade and condition[(a)]	**Property**[(b)]	Testing temperature °C (°F)								
		25 (75)	**205 (400)**	**400 (750)**	**455 (850)**	**510 (950)**	**565 (1050)**	**620 (1150)**	**675 (1250)**	815 (1500)
1005 HR	K_f, MPa (ksi)	. . .	525 (76)	615 (89)	660 (96)	615 (89)	505 (73)	400 (58)	295 (43)	**172 (25)**
	TS, MPa (ksi)	370 (54)	275 (40)	310 (45)	340 (49)	330 (48)	290 (42)	250 (36)	205 (30)	**110 (16)**
	YS, MPa (ksi)	. . .	90 (13)	95 (14)	115 (17)	115 (17)	140 (20)	140 (20)	145 (21)	**55 (8.2)**
	RA, %	80	80	72	70	77	87	93	97	**98**
	n	. . .	0.28	0.30	0.28	0.26	0.21	0.17	0.12	**0.18**

1018 HR	K_f, MPa (ksi)	950 (138)	740 (107)	915 (133)	945 (137)	820 (119)	650 (94)	525 (76)	395 (57)	**360 (52)**
	TS, MPa (ksi)	520 (75)	405 (59)	500 (71)	510 (74)	460 (67)	415 (60)	340 (49)	260 (38)	**180 (26)**
	YS, MPa (ksi)	200 (29)	160 (23)	185 (27)	200 (29)	195 (28)	215 (31)	200 (29)	170 (25)	**45 (6.9)**
	RA, %	65	68	49	57	76	87	93	96	**95**
	n	0.25	0.25	0.26	0.25	0.23	0.18	0.15	0.13	**0.32**
1023 HR	K_f, MPa (ksi)	905 (131)	. . .	. . .	. . .	. . .	. . .	475 (69)	370 (54)	**330 (48)**
	TS, MPa (ksi)	495 (72)	. . .	. . .	. . .	. . .	. . .	305 (44)	240 (35)	**170 (25)**
	YS, MPa (ksi)	195 (28)	. . .	. . .	. . .	. . .	. . .	180 (26)	180 (26)	**60 (8.4)**
	RA, %	63	. . .	. . .	. . .	. . .	. . .	89	92	**94**
	n	0.25	. . .	. . .	. . .	. . .	. . .	0.16	0.12	**0.28**
1040 HR	K_f, MPa (ksi)	1220 (177)	945 (137)	1015 (147)	1035 (150)	950 (138)	805 (117)	605 (88)	455 (66)	**345 (50)**
	TS, MPa (ksi)	690 (100)	545 (79)	595 (86)	595 (86)	595 (82)	505 (73)	400 (58)	317 (46)	**190 (27)**
	YS, MPa (ksi)	305 (44)	255 (37)	290 (42)	270 (39)	285 (41)	285 (41)	260 (38)	215 (31)	**70 (10)**
	RA, %	53	56	42	47	68	80	81	88	**97**
	n	0.23	0.21	0.20	0.22	0.19	0.17	0.14	0.12	**0.26**
1045 HR	K_f, MPa (ksi)	1400 (203)	1110 (161)	1140 (165)	1220 (177)	1075 (156)	860 (125)	725 (105)	545 (79)	**360 (52)**
	TS, MPa (ksi)	785 (114)	660 (96)	675 (98)	705 (102)	640 (93)	565 (82)	470 (68)	360 (52)	**200 (29)**

	YS, MPa (ksi)	350 (51)	315 (46)	345 (50)	325 (47)	330 (48)	325 (47)	260 (38)	235 (34)	**75 (11)**
	RA, %	47	48	33	37	52	57	60	76	**95**
	n	0.22	0.20	0.19	0.22	0.19	0.19	0.18	0.14	**0.26**
1080 HR	K_f, MPa (ksi)	2180 (316)	1650 (239)	1605 (233)	1570 (228)	1450 (210)	1185 (172)	1140 (165)	940 (136)	**360 (52)**
	TS, MPa (ksi)	1035 (150)	850 (123)	860 (125)	915 (133)	840 (122)	705 (102)	600 (87)	460 (67)	**195 (28)**
	YS, MPa (ksi)	435 (63)	420 (61)	495 (72)	545 (79)	505 (73)	440 (64)	330 (48)	215 (31)	**95 (14)**
	RA, %	24	31	18	18	25	23	28	30	**98**
	n	0.26	0.22	0.19	0.17	. . .	0.16	0.20	0.24	**0.21**
1117 N	K_f, MPa (ksi)	910 (132)	. . .	. . .	. . .	. . .	. . .	470 (68)	360 (52)	**360 (52)**
	TS, MPa (ksi)	485 (70)	. . .	. . .	. . .	. . .	. . .	295 (43)	240 (35)	**170 (25)**
	YS, MPa (ksi)	165 (24)	. . .	. . .	. . .	. . .	. . .	180 (26)	165 (24)	**50 (5.9)**
	RA, %	68	. . .	. . .	. . .	. . .	. . .	89	94	**90**
	n	0.27	. . .	. . .	. . .	. . .	. . .	0.15	0.12	**0.35**
1137 HR	K_f, MPa (ksi)	1325 (192)	1040 (151)	1055 (153)	1165 (169)	1010 (146)	880 (128)	655 (95)	540 (78)	**360 (52)**
	TS, MPa (ksi)	765 (111)	625 (91)	635 (92)	675 (98)	625 (91)	545 (79)	435 (63)	330 (48)	**190 (27)**
	YS, MPa (ksi)	365 (53)	325 (47)	330 (48)	315 (46)	350 (51)	345 (50)	275 (40)	200 (29)	**65 (9.2)**
	RA, %	53	53	41	44	63	78	85	91	**94**
	n	0.21	0.19	0.19	0.21	0.17	0.16	0.15	0.16	**0.20**

1213 N	K_f, MPa (ksi)	820 (119)	. . .	. . .	. . .	. . .	585 (85)	435 (63)	. . .	**220 (32)**
	TS, MPa (ksi)	455 (66)	. . .	. . .	. . .	. . .	360 (52)	295 (43)	235 (34)	**130 (19)**
	YS, MPa (ksi)	185 (27)	. . .	. . .	. . .	. . .	185 (27)	195 (28)	. . .	**65 (9.4)**
	RA, %	59	. . .	. . .	. . .	. . .	69	79	87	**87**
	n	0.24	. . .	. . .	. . .	. . .	0.18	0.13	. . .	**0.20**
12L14 HR	K_f, MPa (ksi)	1130 (124)	745 (108)	940 (136)	960 (139)	815 (118)	635 (92)	490 (71)	360 (52)	**230 (33)**
	TS, MPa (ksi)	460 (67)	395 (57)	475 (69)	485 (70)	435 (63)	370 (54)	310 (45)	250 (36)	**140 (20)**
	YS, MPa (ksi)	165 (24)	140 (20)	160 (23)	150 (22)	160 (23)	185 (27)	185 (27)	180 (26)	**75 (11)**
	RA, %	63	60	39	38	52	69	77	85	**86**
	n	0.26	0.27	0.29	0.30	0.26	0.20	0.16	0.13	**0.18**
1524 N	K_f, MPa (ksi)	1130 (164)	. . .	. . .	. . .	. . .	. . .	585 (85)	435 (63)	**400 (58)**
	TS, MPa (ksi)	605 (88)	. . .	. . .	. . .	. . .	. . .	360 (53)	295 (43)	**205 (30)**
	YS, MPa (ksi)	240 (35)	. . .	. . .	. . .	. . .	. . .	215 (31)	220 (32)	**62 (9.0)**
	RA, %	69	. . .	. . .	. . .	. . .	. . .	94	95	**94**
	n	0.25	. . .	. . .	. . .	. . .	. . .	0.16	0.11	**0.30**
1541 HR	K_f, MPa (ksi)	1380 (200)	1100 (159)	1055 (153)	1195 (173)	1050 (152)	965 (140)	785 (114)	600 (87)	**365 (53)**
	TS, MPa (ksi)	820 (119)	685 (99)	660 (96)	695 (101)	650 (94)	570 (83)	470 (68)	350 (51)	**195 (28)**

	YS, MPa (ksi)	415 (60)	380 (55)	380 (55)	330 (48)	360 (52)	295 (43)	275 (40)	250 (36)	**70 (10)**
	RA, %	59	59	45	48	77	87	86	93	**97**
	n	**0.19**	**0.17**	**0.17**	**0.20**	**0.16**	**0.16**	**0.15**	**0.14**	**0.27**

(a) HR, hot rolled; N, normalized.

(b) K_f, strength coefficient; TS, tensile strength; YS, yield strength; RA, reduction of area; *n*, strain-hardening exponent

Table 2 Mechanical properties of alloy steels

Average strain rate: 8 mm/mm/s (8 in/in/s). Source: Ref 2

Steel grade and condition[(a)]	**Property**[(b)]	Testing temperature, °C (°F)								
		25 (75)	**205 (400)**	**400 (750)**	**455 (850)**	**510 (950)**	**565 (1050)**	**620 (1150)**	**675 (1250)**	815 (1500)
4028 HR	K_f, MPa (ksi)	1140 (165)	. . .	. . .	. . .	. . .	795 (115)	745 (108)	685 (99)	**420 (61)**
	TS, MPa (ksi)	650 (94)	. . .	. . .	. . .	. . .	485 (70)	405 (59)	330 (48)	**205 (30)**
	YS, MPa (ksi)	310 (45)	. . .	. . .	. . .	. . .	275 (40)	240 (35)	270 (39)	**75 (11)**
	RA, %	60	. . .	. . .	. . .	. . .	83	88	91	**93**
	n	0.21	. . .	. . .	. . .	. . .	0.17	0.18	0.15	**0.28**
4137 HR	K_f, MPa (ksi)	1825 (265)	. . .	. . .	. . .	. . .	. . .	925 (134)	625 (97)	**425 (62)**
	TS, MPa (ksi)	1040 (151)	. . .	. . .	. . .	. . .	. . .	560 (81)	425 (62)	**220 (32)**
	YS, MPa (ksi)	560 (81)	. . .	. . .	. . .	. . .	. . .	325 (47)	260 (38)	**65 (9.6)**
	RA, %	46	. . .	. . .	. . .	. . .	. . .	88	93	**94**
	n	0.19	. . .	. . .	. . .	. . .	. . .	0.17	0.15	**0.30**

4140 SA	K_f, MPa (ksi)	1070 (155)	820 (119)	740 (107)	765 (111)	780 (113)	725 (105)	635 (92)	495 (72)	**400 (58)**
	TS, MPa (ksi)	620 (90)	495 (72)	460 (67)	485 (70)	475 (69)	435 (63)	395 (57)	310 (45)	**205 (30)**
	YS, MPa (ksi)	330 (48)	295 (43)	315 (46)	324 (47)	295 (43)	260 (38)	260 (38)	205 (30)	**75 (11)**
	RA, %	67	70	68	69	73	82	88	93	**95**
	n	0.19	0.17	0.15	0.15	0.16	0.16	0.14	0.14	**0.27**
4340 HR	K_f, MPa (ksi)	1875 (272)	1825 (265)	1985 (288)	1570 (228)	1270 (184)	1145 (166)	995 (144)	650 (94)	**385 (56)**
	TS, MPa (ksi)	1145 (166)	1070 (155)	965 (140)	915 (133)	820 (119)	705 (102)	600 (87)	440 (64)	**200 (29)**
	YS, MPa (ksi)	738 (107)	700 (101)	495 (72)	585 (85)	560 (81)	485 (70)	415 (60)	310 (45)	**75 (11)**
	RA, %	52	52	43	57	67	76	87	93	**94**
	n	0.15	0.16	0.22	0.16	0.13	0.14	0.14	0.12	**0.26**
4620 HR	K_f, MPa (ksi)	1165 (169)	. . .	. . .	. . .	. . .	. . .	635 (92)	435 (63)	**385 (56)**
	TS, MPa (ksi)	640 (93)	. . .	. . .	. . .	. . .	. . .	395 (57)	305 (44)	**200 (29)**
	YS, MPa (ksi)	275 (40)	. . .	. . .	. . .	. . .	. . .	235 (34)	220 (32)	**60 (8.7)**
	RA, %	62	. . .	. . .	. . .	. . .	. . .	82	90	**86**
	n	0.23	. . .	. . .	. . .	. . .	. . .	0.16	0.11	**0.30**
5120 N	K_f, MPa (ksi)	985 (143)	. . .	. . .	. . .	. . .	670 (97)	525 (76)	460 (67)	**380 (55)**
	TS, MPa (ksi)	600 (87)	. . .	. . .	. . .	. . .	435 (63)	370 (54)	305 (44)	**193 (28)**

	YS, MPa (ksi)	305 (44)	. . .	. . .	. . .	. . .	260 (38)	260 (38)	205 (30)	**60 (8.5)**
	RA, %	67	. . .	. . .	. . .	. . .	83	89	93	**92**
	n	0.19	. . .	. . .	. . .	. . .	0.15	0.11	0.13	**0.30**
51L20 N	K_f, MPa (ksi)	1025 (149)	. . .	. . .	. . .	. . .	670 (97)	595 (86)	460 (67)	**365 (53)**
	TS, MPa (ksi)	605 (88)	. . .	. . .	. . .	. . .	435 (63)	380 (55)	305 (44)	**195 (28)**
	YS, MPa (ksi)	295 (43)	. . .	. . .	. . .	. . .	260 (38)	220 (32)	205 (30)	**65 (9.3)**
	RA, %	63	. . .	. . .	. . .	. . .	68	81	87	**87**
	n	0.20	. . .	. . .	. . .	. . .	0.15	0.16	0.13	**0.28**
8620 HR	K_f, MPa (ksi)	1150 (167)	930 (135)	1185 (172)	1150 (167)	905 (131)	800 (116)	660 (96)	485 (70)	**360 (52)**
	TS, MPa (ksi)	710 (103)	1135 (84)	625 (91)	635 (92)	605 (88)	525 (76)	435 (63)	345 (50)	**185 (27)**
	YS, MPa (ksi)	400 (58)	635 (47)	270 (39)	295 (43)	395 (57)	340 (49)	295 (43)	260 (38)	**65 (9.1)**
	RA, %	60	62	43	44	56	67	77	84	**87**
	n	0.17	0.17	0.24	0.22	0.14	0.14	0.13	0.10	**0.26**
52100 SA	K_f, MPa (ksi)	1100 (160)	903 (131)	895 (130)	950 (138)	945 (137)	795 (115)	620 (90)	560 (81)	**425 (62)**
	TS, MPa (ksi)	685 (99)	551 (80)	495 (72)	530 (77)	515 (75)	450 (65)	380 (55)	330 (48)	**240 (35)**
	YS, MPa (ksi)	435 (63)	340 (49)	270 (39)	305 (44)	270 (39)	235 (34)	215 (31)	220 (32)	**120 (17)**
	RA, %	57	60	60	58	67	76	85	90	**92**
	n	0.15	0.16	0.19	0.19	0.20	0.20	0.17	0.15	**0.21**

EX-33 HR	K_f, MPa (ksi)	1475 (214)	. . .	. . .	. . .	. . .	915 (133)	670 (97)	475 (69)	**400 (58)**
	TS, MPa (ksi)	840 (122)	. . .	. . .	. . .	. . .	525 (76)	435 (63)	345 (50)	**205 (30)**
	YS, MPa (ksi)	240 (35)	. . .	. . .	. . .	. . .	285 (41)	285 (41)	255 (37)	**65 (9.6)**
	RA, %	48	. . .	. . .	. . .	. . .	89	91	93	**94**
	n	**0.22**	**. . .**	**. . .**	**. . .**	**. . .**	**0.19**	**0.14**	**0.10**	**0.29**

(a) HR, hot rolled; N, normalized; SA, spheroidize-annealed.

(b) K_f, strength coefficient; TS, tensile strength; YS, yield strength; RA, reduction of area; *n*, strain-hardening exponent

At higher temperatures, above the recrystallization temperature, the flow stress is influenced mainly by the strain rate, and can be approximated as:

$$\overline{\sigma} = C\,(\dot{\overline{\epsilon}})^m \qquad \textbf{(Eq 3)}$$

where $\dot{\overline{\epsilon}}$ is the strain rate, and *C* and *m* are empirical constants. Strain rate dependent data for numerous steels, aluminum alloys, and titanium alloys are shown in Table 3, Table 4, and Table 5, respectively.

Table 3 Summary of *C* (ksi) and *m* values describing the flow stress relation, $\overline{\sigma} = C(\dot{\overline{\epsilon}})^m$, for steels at various temperatures

Material	**Material history**	**Strain rate range, s^{-1}**	**Strain**	***C***	***m***	***C***	***m***	***C***	***m***	***C***	***m***	***C***	*m*
Test temperature, °C (°F)				**600 (1110)**		**800 (1470)**		**1000 (1830)**		**1200 (2190)**			
1015	Forged, annealed	0.2-30	0.2	36.8	0.112	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.25	. . .	. . .	19.9	0.105	17.0	0.045	7.2	0.137	. . .	**. . .**
			0.4	40.6	0.131	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.5	. . .	. . .	21.5	0.104	18.8	0.058	6.8	0.169	. . .	**. . .**
			0.6	40.0	0.121	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**

			0.7	39.5	0.114	21.1	0.109	18.3	0.068	5.7	0.181	. . .	**. . .**
Test temperature, °C (°F)				**900 (1650)**		**1000 (1830)**		**1100 (2010)**		**1200 (2190)**			
1016	Hot rolled, annealed	. . .	0.05	11.8	0.133	10.7	0.124	9.0	0.117	6.4	0.150	. . .	**. . .**
			0.1	16.5	0.099	13.7	0.099	9.7	0.130	7.1	0.157	. . .	**. . .**
			0.2	20.8	0.082	16.5	0.090	12.1	0.119	9.1	0.140	. . .	**. . .**
			0.3	22.8	0.085	18.2	0.088	13.4	0.109	9.5	0.148	. . .	**. . .**
			0.4	23.0	0.084	18.2	0.098	12.9	0.126	9.1	0.164	. . .	**. . .**
			0.5	23.9	0.088	18.1	0.109	12.5	0.141	8.2	0.189	. . .	**. . .**
			0.6	23.3	0.097	16.9	0.127	12.1	0.156	7.8	0.205	. . .	**. . .**
			0.7	22.8	0.104	17.1	0.127	12.4	0.151	8.1	0.196	. . .	**. . .**
Test temperature, °C (°F)				**870 (1600)**		**980 (1800)**		**1090 (2000)**		**1205 (2200)**		1180 (2150)	
1018	. . .	. . .	. . .	25.2	0.07	15.8	0.152	11.0	0.192	9.2	0.20	. . .	**. . .**
1025	Forged, annealed	3.5-30	0.25	. . .	. . .	33.7	0.004	16.2	0.075	9.3	0.077	. . .	**. . .**
			0.50	. . .	. . .	41.4	-0.032	17.2	0.080	9.6	0.094	. . .	**. . .**
			0.70	. . .	. . .	41.6	-0.032	17.5	0.082	8.8	0.105	. . .	**. . .**
1043	Hot rolled, as-received	0.1-100	0.3/0.5/0.7	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	10.8	**0.21**
Test temperature, °C (°F)				**900 (1650)**		**1000 (1830)**		**1100 (2010)**		**1200 (2190)**			
1045	. . .	. . .	0.05	25.4	0.080	15.1	0.089	11.2	0.100	8.0	0.175	. . .	**. . .**
			0.10	28.9	0.082	18.8	0.103	13.5	0.125	9.4	0.168	. . .	**. . .**
			0.20	33.3	0.086	22.8	0.108	15.4	0.128	10.5	0.167	. . .	**. . .**

			0.30	35.4	0.083	24.6	0.110	15.8	0.162	10.8	0.180	. . .	**. . .**
			0.40	35.4	0.105	24.7	0.134	15.5	0.173	10.8	0.188	. . .	**. . .**
Test temperature, °C (°F)				**600 (1110)**		**800 (1470)**		**1000 (1830)**		**1200 (2190)**			
1055	Forged, annealed	3.5-30	. . .	. . .	. . .	29.4	0.087	14.9	0.126	7.4	0.145	. . .	**. . .**
			. . .	. . .	. . .	32.5	0.076	13.3	0.191	7.4	0.178	. . .	**. . .**
			. . .	. . .	. . .	32.7	0.066	11.5	0.237	6.4	0.229	. . .	**. . .**
Test temperature, °C (°F)				**900 (1650)**		**1000 (1830)**		**1100 (2010)**		**1200 (2190)**			
1060	. . .	. . .	0.05	16.2	0.128	10.8	0.168	8.7	0.161	6.5	0.190	. . .	**. . .**
			0.10	18.3	0.127	13.2	0.145	10.1	0.149	7.5	0.165	. . .	**. . .**
			0.20	21.8	0.119	16.1	0.125	12.1	0.126	8.5	0.157	. . .	**. . .**
			0.30	23.3	0.114	17.1	0.125	12.8	0.132	8.8	0.164	. . .	**. . .**
			0.40	23.7	0.112	16.8	0.128	12.5	0.146	8.8	0.171	. . .	**. . .**
			0.50	23.6	0.110	16.6	0.133	12.7	0.143	8.7	0.176	. . .	**. . .**
			0.60	22.8	0.129	17.1	0.127	11.7	0.169	8.4	0.189	. . .	**. . .**
			0.70	21.3	0.129	16.2	0.138	10.7	0.181	7.8	0.204	. . .	**. . .**
1095	Hot rolled, annealed	1.5-100	0.10	18.3	0.146	13.9	0.143	9.8	0.159	7.1	0.184	. . .	**. . .**
			0.30	21.9	0.133	16.6	0.132	11.7	0.147	8.0	0.183	. . .	**. . .**
			0.50	21.8	0.130	15.7	0.151	10.6	0.176	7.3	0.209	. . .	**. . .**
			0.70	21.0	0.128	13.6	0.179	9.7	0.191	6.5	0.232	. . .	**. . .**
Test temperature, °C (°F)				**930 (1705)**		**1000 (1830)**		**1060 (1940)**		**1135 (2075)**		1200 (2190)	
1115	Hot rolled, as-received	4.4-23.1	0.105	16.3	0.088	13.0	0.108	10.9	0.112	9.1	0.123	7.6	**0.116**

	as-received	23.1	0.223	19.4	0.084	15.6	0.100	12.9	0.107	10.5	0.129	8.6	**0.122**
			0.338	20.4	0.094	17.3	0.090	14.0	0.117	11.2	0.138	8.8	**0.141**
			0.512	20.9	0.099	18.0	0.093	14.4	0.127	11.0	0.159	8.3	**0.173**
			0.695	20.9	0.105	16.9	0.122	13.6	0.150	9.9	0.198	7.6	**0.196**
Test temperature, °C (°F)				**900 (1650)**		**1000 (1830)**		**1100 (2010)**		**1200 (2190)**			
Alloy steel (0.35C-0.27Si-1.49Mn-0.041S-0.037P-0.03Cr-0.11Ni-0.28Mo)	. . .	. . .	0.05	16.6	0.102	12.2	0.125	9.4	0.150	7.4	0.161	. . .	**. . .**
			0.10	19.9	0.091	14.8	0.111	11.5	0.121	8.1	0.149	. . .	**. . .**
			0.20	23.0	0.094	17.6	0.094	13.5	0.100	9.4	0.139	. . .	**. . .**
			0.30	24.9	0.092	19.1	0.093	14.4	0.105	10.2	0.130	. . .	**. . .**
			0.40	26.0	0.088	19.6	0.095	14.5	0.112	10.4	0.139	. . .	**. . .**
			0.50	25.9	0.091	19.6	0.100	14.4	0.112	10.1	0.147	. . .	**. . .**
			0.60	25.9	0.094	19.5	0.105	14.2	0.122	9.7	0.159	. . .	**. . .**
			0.70	25.5	0.099	19.2	0.107	13.9	0.126	9.2	0.165	. . .	**. . .**
4337	Hot rolled, annealed	1.5-100	0.10	22.1	0.080	16.6	0.109	12.1	0.115	8.2	0.165	. . .	**. . .**
			0.30	28.1	0.077	20.8	0.098	15.0	0.111	10.7	0.138	. . .	**. . .**
			0.50	29.2	0.075	21.8	0.096	15.7	0.112	11.3	0.133	. . .	**. . .**
			0.70	28.1	0.080	21.3	0.102	15.5	0.122	11.3	0.135	. . .	**. . .**
9261	Hot rolled, annealed	1.5-100	0.10	22.9	0.109	17.1	0.106	11.8	0.152	8.6	0.168	. . .	**. . .**
			0.30	28.2	0.101	20.4	0.106	14.3	0.140	10.1	0.162	. . .	**. . .**
			0.50	27.8	0.104	20.0	0.120	13.8	0.154	9.1	0.193	. . .	**. . .**
			0.70	25.8	0.112	18.2	0.146	11.8	0.179	7.5	0.235	. . .	**. . .**

Test temperature, °C (°F)				**900 (1650)**		**1000 (1830)**		**1100 (2010)**		**1200 (2190)**			
50100	. . .	. . .	0.05	16.1	0.155	12.4	0.155	8.2	0.175	6.3	0.199	. . .	. . .
			0.10	18.6	0.145	14.1	0.142	9.5	0.164	6.8	0.191	. . .	. . .
			0.20	20.9	0.135	15.9	0.131	11.4	0.141	8.1	0.167	. . .	. . .
			0.30	21.8	0.135	16.6	0.134	11.7	0.142	8.0	0.174	. . .	. . .
			0.40	22.0	0.134	16.8	0.134	11.2	0.155	8.4	0.164	. . .	. . .
			0.50	21.5	0.131	15.6	0.150	11.1	0.158	7.4	0.199	. . .	. . .
			0.60	21.3	0.132	14.6	0.163	10.0	0.184	7.0	0.212	. . .	. . .
			0.70	20.9	0.131	13.5	0.176	9.7	0.183	6.7	0.220	. . .	. . .
52100	Hot rolled, annealed	1.5-100	0.10	20.9	0.123	14.3	0.146	9.5	0.169	6.7	0.203	. . .	. . .
			0.30	25.5	0.107	17.7	0.127	12.0	0.143	8.3	0.171	. . .	. . .
			0.50	25.9	0.107	17.7	0.129	12.3	0.143	8.3	0.178	. . .	. . .
			0.70	23.3	0.131	16.8	0.134	12.0	0.148	7.7	0.192	. . .	. . .
Manganese-silicon steel (0.61C-1.58Si-0.94Mn-0.038S-0.035P-0.12Cr-0.27Ni-0.06Mo)	. . .	. . .	0.05	19.2	0.117	14.8	0.119	9.7	0.172	7.5	0.181	. . .	. . .
			0.10	22.6	0.112	17.1	0.108	11.8	0.151	8.7	0.166	. . .	. . .
			0.20	25.7	0.108	19.5	0.101	13.5	0.139	9.7	0.160	. . .	. . .
			0.30	27.6	0.108	20.5	0.109	14.8	0.126	10.0	0.161	. . .	. . .
			0.40	27.6	0.114	20.2	0.114	14.4	0.141	9.5	0.179	. . .	. . .
			0.50	27.2	0.113	19.8	0.125	14.1	0.144	9.1	0.188	. . .	. . .
			0.60	26.0	0.121	18.8	0.137	12.8	0.162	8.2	0.209		
			0.70	24.7	0.130	17.8	0.152	11.9	0.178	7.5	0.228	. . .	. . .

Chromium-silicon steel (0.47C-3.74Si-0.58Mn-8.20Cr-0.20Ni)	. . .	. . .	0.05	19.9	0.118	23.9	0.104	15.1	0.167	10.0	0.206	. . .	. . .
			0.10	19.9	0.136	25.6	0.120	16.8	0.162	11.1	0.189	. . .	. . .
			0.20	19.9	0.143	27.6	0.121	18.5	0.153	11.9	0.184	. . .	. . .
			0.30	19.9	0.144	28.4	0.119	19.1	0.148	12.1	0.182	. . .	. . .
			0.40	19.3	0.150	28.2	0.125	18.9	0.150	12.1	0.178	. . .	. . .
			0.50	18.5	0.155	26.6	0.132	18.5	0.155	11.8	0.182	. . .	. . .
			0.60	17.5	0.160	25.2	0.142	17.5	0.160	11.5	0.182	. . .	. . .
			0.70	16.1	0.163	23.3	0.158	16.1	0.162	10.7	0.199	. . .	. . .
D3	Hot rolled, annealed	1.5-100	0.10	39.2	0.087	29.0	0.108	21.0	0.123	14.6	0.121	. . .	. . .
			0.30	43.7	0.087	30.4	0.114	21.0	0.139	13.9	0.130	. . .	. . .
			0.50	39.7	0.101	27.1	0.125	18.4	0.155	12.2	0.124	. . .	. . .
			0.70	33.3	0.131	22.5	0.145	15.3	0.168	10.7	0.108	. . .	. . .
Test temperature, °C (°F)				**700 (1290)**		**820 (1510)**		**900 (1650)**		**1000 (1830)**			
H-13	. . .	290-906	0.1	19.1	0.232	10.2	0.305	6.0	0.373	4.8	0.374	. . .	. . .
			0.2	30.1	0.179	13.7	0.275	8.2	0.341	9.0	0.295	. . .	. . .
			0.3	31.0	0.179	15.1	0.265	10.8	0.305	11.6	0.267	. . .	. . .
			0.4	25.9	0.204	12.3	0.295	12.5	0.287	11.8	0.269	. . .	. . .
Test temperature, °C (°F)				**900 (1650)**		**1000 (1830)**		**1100 (2010)**		**1200 (2190)**			
H-26	Hot rolled, annealed	1.5-100	0.10	46.7	0.058	37.4	0.072	26.2	0.106	18.7	0.125	. . .	. . .
			0.30	49.6	0.075	38.1	0.087	26.0	0.121	18.3	0.140	. . .	. . .
			0.50	44.6	0.096	33.7	0.102	23.6	0.131	16.2	0.151	. . .	. . .

			0.70	39.1	0.115	27.9	0.124	20.1	0.149	13.8	0.162	. . .	. . .
Test temperature, °C (°F)				**600 (1110)**		**800 (1470)**		**1000 (1830)**		**1200 (2190)**			
Type 301	Hot rolled, annealed	0.8-100	0.25	. . .	. . .	40.5	0.051	16.3	0.117	7.6	0.161	. . .	. . .
			0.50	. . .	. . .	39.3	0.062	17.8	0.108	7.6	0.177	. . .	. . .
			0.70	. . .	. . .	37.8	0.069	17.4	0.102	6.6	0.192	. . .	. . .
Type 302 (0.07C-0.71Si-1.07Mn-0.03P-0.005S-18.34Cr-9.56Ni)	Hot rolled, annealed	310-460	0.25	26.5	0.147	25.1	0.129	11.0	0.206	4.6	0.281	. . .	. . .
			0.40	31.3	0.153	30.0	0.121	13.5	0.188	4.7	0.284	. . .	. . .
			0.60	17.5	0.270	45.4	0.063	16.8	0.161	4.1	0.310	. . .	. . .
Type 302 (0.08C-0.49Si-1.06Mn-0.037P-0.005S-18.37Cr-9.16Ni)	Hot rolled, annealed	0.2-30	0.25	52.2	0.031	36.6	0.042	23.1	0.040	12.8	0.082	. . .	. . .
			0.40	58.9	0.022	40.4	0.032	24.7	0.050	13.6	0.083	. . .	. . .
			0.60	63.2	0.020	41.9	0.030	24.9	0.053	13.5	0.091	. . .	. . .
			0.70	64.0	0.023	42.0	0.031	24.7	0.052	13.4	0.096	. . .	. . .
Test temperature, °C (°F)				**900 (1650)**		**1000 (1830)**		**1100 (2010)**		**1200 (2190)**			
Type 302 (0.07C-0.43Si-0.48Mn-18.60Cr-7.70Ni)	. . .	1.5-100	0.05	24.6	0.023	16.8	0.079	13.7	0.093	9.7	0.139	. . .	. . .
			0.10	28.4	0.026	21.2	0.068	15.6	0.091	11.1	0.127	. . .	. . .
			0.20	33.6	0.031	25.2	0.067	18.1	0.089	12.5	0.120	. . .	. . .
			0.30	35.3	0.042	26.3	0.074	19.5	0.089	13.5	0.115	. . .	. . .
			0.40	35.6	0.055	26.9	0.084	19.9	0.094	14.2	0.110	. . .	. . .
			0.50	35.6	0.060	27.0	0.093	19.6	0.098	14.2	0.115	. . .	. . .
			0.60	34.1	0.068	26.4	0.092	19.3	0.102	13.8	0.118	. . .	. . .
			0.70	33.6	0.072	25.7	0.102	18.9	0.108	13.9	0.120	. . .	. . .

Test temperature, °C (°F)				600 (1110)		800 (1470)		1000 (1830)		1200 (2190)			
Type 309	Hot drawn, annealed	200-525	0.25	. . .	. . .	39.4	0.079	. . .	. . .	8.7	0.184	. . .	**. . .**
			0.40	. . .	. . .	45.1	0.074	. . .	. . .	9.6	0.178	. . .	**. . .**
			0.60	. . .	. . .	48.1	0.076	. . .	. . .	9.5	0.185	. . .	**. . .**
Type 310	Hot drawn, annealed	310-460	0.25	50.3	0.080	32.3	0.127	27.5	0.101	12.0	0.154	. . .	**. . .**
			0.40	56.5	0.080	32.2	0.142	22.8	0.143	10.8	0.175	. . .	**. . .**
			0.60	61.8	0.067	21.9	0.212	9.7	0.284	4.5	0.326	. . .	**. . .**
Type 316	Hot drawn, annealed	310-460	0.25	13.5	0.263	22.2	0.149	6.4	0.317	8.0	0.204	. . .	**. . .**
			0.40	28.8	0.162	26.8	0.138	3.7	0.435	7.4	0.227	. . .	**. . .**
			0.60	39.3	0.128	30.1	0.133	6.1	0.365	6.5	0.254	. . .	**. . .**
Test temperature, °C (°F)				**600 (1110)**		**800 (1470)**		**1000 (1830)**		**1200 (2190)**		900 (1650)	
Type 403	Hot rolled, annealed	0.8-100	0.25	. . .	. . .	26.3	0.079	15.4	0.125	7.3	0.157	. . .	**. . .**
			0.50	. . .	. . .	26.9	0.076	16.0	0.142	7.8	0.152	. . .	**. . .**
			0.70	. . .	. . .	24.6	0.090	15.3	0.158	7.5	0.155	. . .	**. . .**
Stainless steel (0.12C-0.12Si-0.29Mn-0.014P-0.016S-12.11Cr-0.50Ni-0.45Mo)	Hot rolled, annealed	0.8-100	0.25	. . .	. . .	28.7	0.082	17.2	0.082	11.9	0.079	. . .	**. . .**
			0.50	. . .	. . .	29.1	0.093	20.7	0.073	11.6	0.117	. . .	**. . .**
			0.70	. . .	. . .	28.7	0.096	22.5	0.067	11.2	0.131	. . .	**. . .**
Stainless steel (0.08C-0.45Si-0.43Mn-0.031P-0.005S-17.38Cr-0.31Ni)	Hot rolled, annealed	3.5-30	0.25	. . .	. . .	. . .	. . .	19.5	0.099	8.9	0.128	28.3	**0.114**
			0.50	. . .	. . .	. . .	. . .	22.3	0.097	9.5	0.145	34.9	**0.105**
			0.70	. . .	. . .	. . .	. . .	23.2	0.098	9.2	0.158	37.1	**0.107**
Test temperature, °C (°F)				**870 (1600)**		**925 (1700)**		**980 (1800)**		**1095 (2000)**		1150 (2100)	

Maraging 300	. . .	. . .	. . .	43.4	0.077	36.4	0.095	30.6	0.113	21.5	0.145	18.0	**0.165**
Test temperature, °C (°F)				**1205 (2200)**									
Maraging 300	**. . .**	**. . .**	**. . .**	**12.8**	**0.185**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**

Source: Ref 3

Table 4 Summary of *C* (ksi) and *m* values describing the flow stress-strain rate relation, $\overline{\sigma} = C(\dot{\epsilon})^m$, for aluminum alloys at various temperatures

Material	**Material history**	**Strain rate range, s^{-1}**	**Strain**	***C***	***m***	***C***	***m***	***C***	***m***	***C***	***m***	***C***	*m*
Test temperature, °C (°F)				**200 (390)**		**300 (570)**		**400 (750)**		**500 (930)**		600 (1110)	
Super-pure (99.98Al-0.0017Cu-0.0026Si-0.0033Fe-0.006Mn)	Cold rolled, annealed 30 min at 600 °C (1110 °F)	0.4-311	0.288	5.7	0.110	4.3	0.120	2.8	0.140	1.6	0.155	0.6	**0.230**
			2.88	8.7	0.050	4.9	0.095	2.8	0.125	1.6	0.175	0.6	**0.215**
Test temperature, °C (°F)				**200 (390)**		**400 (750)**		**500 (930)**					
1100	Cold drawn, annealed	0.25-40	0.25	9.9	0.066	4.2	0.115	2.1	0.211	. . .	. . .	. . .	**. . .**
			0.50	11.6	0.071	4.4	0.132	2.1	0.227	. . .	. . .	. . .	**. . .**
			0.70	12.2	0.075	4.5	0.141	2.1	0.224	. . .	. . .	. . .	**. . .**
Test temperature, °C (°F)				**150 (300)**		**250 (480)**		**350 (660)**		**450 (840)**		550 (1020)	
1100	Extruded, annealed 1 h at 400 °C (750 °F)	4-40	0.105	11.4	0.022	9.1	0.026	6.3	0.055	3.9	0.100	2.2	**0.130**
			0.223	13.5	0.022	10.5	0.031	6.9	0.061	4.3	0.098	2.4	**0.130**
			0.338	15.0	0.021	11.4	0.035	7.2	0.073	4.5	0.100	2.5	**0.141**
			0.512	16.1	0.024	11.9	0.041	7.3	0.084	4.4	0.116	2.4	**0.156**
			0.695	17.0	0.026	12.3	0.041	7.4	0.088	4.3	0.130	2.4	**0.155**

Test temperature, °C (°F)				**200 (390)**		**400 (750)**		**500 (930)**					
2017	Cold drawn, annealed	0.2-30	0.250	34.5	0.014	14.8	0.110	5.8	0.126	. . .	. . .	. . .	**. . .**
			0.500	32.2	-0.025	13.2	0.121	5.2	0.121	. . .	. . .	. . .	**. . .**
			0.700	29.5	-0.038	12.5	0.128	5.1	0.119	. . .	. . .	. . .	**. . .**
Test temperature, °C (°F)				**300 (570)**		**350 (660)**		**400 (750)**		**450 (840)**		500 (930)	
2017	Solution treated 1 h at 510 °C (950 °F), water quenched, annealed 4 h at 400 °C (750 °F)	0.4-311	0.115	10.8	0.695	9.1	0.100	7.5	0.110	6.2	0.145	5.1	**0.155**
			2.660	10.0	0.100	9.2	0.100	7.7	0.080	6.8	0.090	4.6	**0.155**
Test temperature, °C (°F)				**240 (465)**		**360 (645)**		**480 (825)**					
5052	Annealed 3 h at 420 °C (790 °F)	0.25-63	0.20	14.3	0.038	8.9	0.067	5.6	0.125	. . .	. . .	. . .	**. . .**
			0.40	15.9	0.035	9.3	0.071	5.3	0.130	. . .	. . .	. . .	**. . .**
			0.60	16.8	0.035	9.0	0.068	5.1	0.134	. . .	. . .	. . .	**. . .**
			0.80	17.5	0.038	9.4	0.068	5.6	0.125	. . .	. . .	. . .	**. . .**
5056	Annealed 3 h at 420 °C (790 °F)	0.25-63	0.20	42.6	-0.032	20.9	0.138	11.7	0.200	. . .	. . .	. . .	**. . .**
			0.40	44.0	-0.032	20.8	0.138	10.5	0.205	. . .	. . .	. . .	**. . .**
			0.60	44.9	-0.031	19.9	0.143	10.3	0.202	. . .	. . .	. . .	**. . .**
			0.80	45.6	-0.034	20.3	0.144	10.3	0.203	. . .	. . .	. . .	**. . .**
5083	Annealed 3 h at 420 °C (790 °F)	0.25-63	0.20	43.6	-0.006	20.5	0.095	9.3	0.182	. . .	. . .	. . .	**. . .**
			0.40	43.6	-0.001	19.7	0.108	8.3	0.208	. . .	. . .	. . .	**. . .**

			0.60	41.9	0.003	18.8	0.111	8.5	0.201	. . .	. . .	. . .	**. . .**
			0.80	40.2	0.002	19.1	0.105	9.7	0.161	. . .	. . .	. . .	**. . .**
5454	Annealed 3 h at 420 °C (790 °F)	0.25-63	0.20	33.6	-0.005	16.8	0.093	10.8	0.182	. . .	. . .	. . .	**. . .**
			0.40	36.0	-0.009	16.3	0.104	10.7	0.188	. . .	. . .	. . .	**. . .**
			0.60	36.9	-0.009	16.0	0.102	10.0	0.191	. . .	. . .	. . .	**. . .**
			0.80	37.0	-0.009	16.2	0.097	10.2	0.183	. . .	. . .	. . .	**. . .**
Test temperature, °C (°F)				**400 (750)**		**450 (840)**		**500 (930)**		**550 (1020)**			
7075	Solution treated 1 h at quenched, 465 °C (870 °F), water aged at 140 °C (285 °F) for 16 h	0.4-311	0.115	10.0	0.090	6.0	0.135	3.9	0.150	2.9	0.170	. . .	**. . .**
			2.66	**9.7**	**0.115**	**6.2**	**0.120**	**4.8**	**0.115**	**2.7**	**0.115**	**. . .**	**. . .**

Source: Ref 3

Table 5 Summary of *C* (ksi) and *m* values describing the flow stress-strain relation, (= *C*( )^m, for titanium alloys at various temperatures

Material	**Material history**	**Strain rate range, s^{-1}**	**Strain**	***C***	***m***	***C***	***m***	***C***	***m***	***C***	***m***	***C***	***m***	***C***	***m***	***C***	*m*
Test temperature, °C (°F)				**20 (68)**		**200 (392)**		**400 (752)**		**600 (1112)**		**800 (1472)**		**900 (1652)**		1000 (1832)	
Type 1 (Ti-0.04Fe-0.02C-0.005H_2-0.01N_2-0.04O_2)	Annealed 15 min at 650 °C (1200 °F) in high vacuum	0.25-16.0	0.2	92.8	0.029	60.9	0.046	39.8	0.074	25.3	0.097	12.8	0.167	5.4	0.230	3.0	**0.387**
			0.4	113.7	0.029	73.3	0.056	48.8	0.061	29.6	0.115	14.6	0.181	5.5	0.248	3.6	**0.289**
			0.6	129.6	0.028	82.2	0.056	53.9	0.049	32.1	0.105	14.9	0.195	5.5	0.248	3.5	**0.289**
			0.8	142.5	0.027	87.7	0.058	56.3	0.042	32.7	0.099	15.4	0.180	5.9	0.186	3.2	**0.264**
			1.0	150.6	0.027	90.7	0.054	56.6	0.044	32.5	0.099	15.9	0.173	5.9	0.167	3.0	**0.264**
Type 2 (Ti-0.15Fe-0.02C-0.005H_2-0.02N_2-0.12O_2	Annealed 15 min at 650 °C (1200 °F) in high vacuum	0.25-16.0	0.2	143.3	0.021	92.7	0.043	54.5	0.051	33.6	0.092	17.5	0.167	6.9	0.135	4.2	**0.220**
			0.4	173.2	0.021	112.1	0.042	63.1	0.047	36.3	0.101	18.4	0.190	7.2	0.151	4.9	**0.167**
			0.6	193.8	0.024	125.3	0.045	65.6	0.047	36.9	0.104	18.4	0.190	7.8	0.138	4.5	**0.167**
			0.8	208.0	0.023	131.9	0.051	66.0	0.045	37.0	0.089	18.4	0.190	7.6	0.106	3.9	**0.195**
			1.0	216.8	0.023	134.8	0.056	65.3	0.045	36.9	0.092	18.6	0.190	6.8	0.097	3.7	**0.167**
Test temperature, °C (°F)				**600 (1110)**		**700 (1290)**		**800 (1470)**		**900 (1650)**							

Unalloyed (Ti-0.03Fe-0.0084N-0.0025H	Hot rolled, annealed at 800 °C (1470 °F) for 90 min	0.1-10	0.25	23.4	0.062	14.3	0.115	8.2	0.236	1.8	0.324	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.50	27.9	0.066	17.8	0.111	10.0	0.242	2.1	0.326	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.70	30.1	0.065	20.0	0.098	12.2	0.185	2.5	0.316	. . .	. . .	. . .	. . .	. . .	**. . .**
Test temperature, °C (°F)				**20 (68)**		**200 (392)**		**400 (752)**		**600 (1112)**		**800 (1472)**		**900 (1652)**		1000 (1832)	
Ti-5Al-2.5Sn	Annealed 30 min at 800 °C (1470 °F) in high vacuum	0.25-16.0	0.1	173.6	0.046	125.6	0.028	97.6	0.028	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.2	197.9	0.048	138.8	0.022	107.4	0.026	86.1	0.025	58.5	0.034	44.2	0.069	5.4	**0.308**
			0.3	215.6	0.046	147.4	0.021	112.5	0.027	92.8	0.020	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.4	230.6	0.039	151.4	0.022	116.0	0.022	95.6	0.019	58.7	0.040	44.8	0.082	5.1	**0.294**
			0.5	. . .	. . .	. . .	. . .	. . .	. . .	96.7	0.021	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.6	. . .	. . .	. . .	. . .	. . .	. . .	96.6	0.024	55.6	0.042	43.0	0.078	5.2	**0.264**
			0.8	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	50.2	0.033	39.1	0.073	5.2	**0.264**
			0.9	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	46.8	0.025	. . .	. . .	. . .	**. . .**
			1.0	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	35.2	0.056	5.3	**0.280**
Ti-6Al-4V	Annealed 120 min at 650 °C (1200 °F) in high vacuum	0.25-16.0	0.1	203.3	0.017	143.8	0.026	119.4	0.025	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**

	(1200 °F) in high vacuum	16.0	0.2	209.7	0.015	151.0	0.021	127.6	0.022	94.6	0.064	51.3	0.146	23.3	0.143	9.5	**0.131**
			0.3	206.0	0.015	152.0	0.017	126.2	0.017	91.2	0.073	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.4	. . .	. . .	. . .	. . .	118.7	0.014	84.6	0.079	39.8	0.175	21.4	0.147	9.4	**0.118**
			0.5	. . .	. . .	. . .	. . .	. . .	. . .	77.9	0.080	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.6	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	30.4	0.205	20.0	0.161	9.6	**0.118**
			0.8	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	26.6	0.199	19.5	0.172	9.3	**0.154**
			0.9	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	24.9	0.201	. . .	. . .	. . .	**. . .**
			1.0	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	20.3	0.146	8.9	**0.192**
Test temperature, °C (°F)				**843 (1550)**		**954 (1750)**		**982 (1800)**									
Ti-6Al-4V	. . .	. . .	. . .	38.0	0.064	12.3	0.24	9.4	0.29								
Test temperature, °C (°F)				**20 (68)**		**200 (392)**		**400 (752)**		**600 (1112)**		**800 (1472)**		**900 (1652)**		1000 (1832)	
Ti-13V-11Cr-3Al	Annealed 30 min at 700 °C (1290 °F) in high vacuum	0.25-16.0	0.1	173.1	0.041	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.2	188.2	0.037	150.5	0.030	136.5	0.035	118.4	0.040	65.4	0.097	44.6	0.147	32.4	**0.153**
			0.3	202.3	0.034	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**

			0.4	215.2	0.029	174.2	0.024	153.9	0.030	107.5	0.039	59.5	0.096	42.1	0.139	30.9	**0.142**
			0.5	226.3	0.026	181.1	0.023	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.6	. . .	. . .	183.5	0.026	147.9	0.046	92.8	0.045	56.7	0.088	40.9	0.127	29.2	**0.155**
			0.7	. . .	. . .	181.4	0.029	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	**. . .**
			0.8	. . .	. . .	. . .	. . .	136.3	0.045	84.7	0.036	53.9	0.081	39.3	0.125	27.8	**0.167**
			0.9	. . .	. . .	. . .	. . .	. . .	. . .	. . .	. . .	52.9	0.080	. . .	. . .	. . .	**. . .**
			1.0	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**	**38.8**	**0.127**	**28.0**	**0.159**

Source: Ref 3

The elevated-temperature flow stress data in Table 3, 4, and 5 primarily cover the traditional hot-forging temperature ranges for the various materials. With respect to ferrous materials, warm forging is becoming more and more common as a means of increasing precision. Tables 1 and 2 cover the warm-forging temperature range for many steels. Trends can be more easily discerned if the data are plotted. Figures 2, 3, 4(a), and 4(b) are typical examples of a graphical presentation.

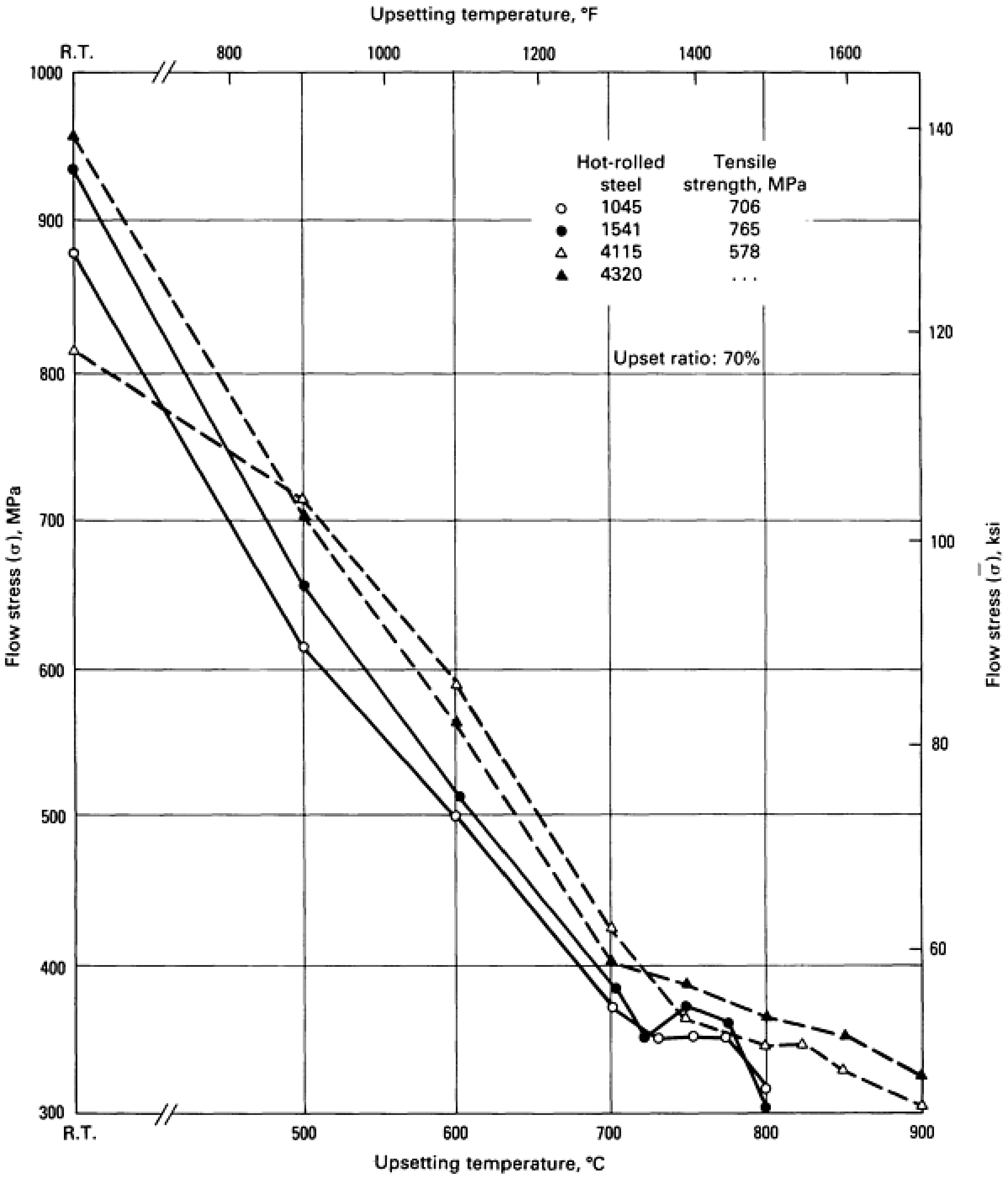

Fig. 2 Effect of upsetting temperature on flow stress. Source: Kobe Steel Ltd.

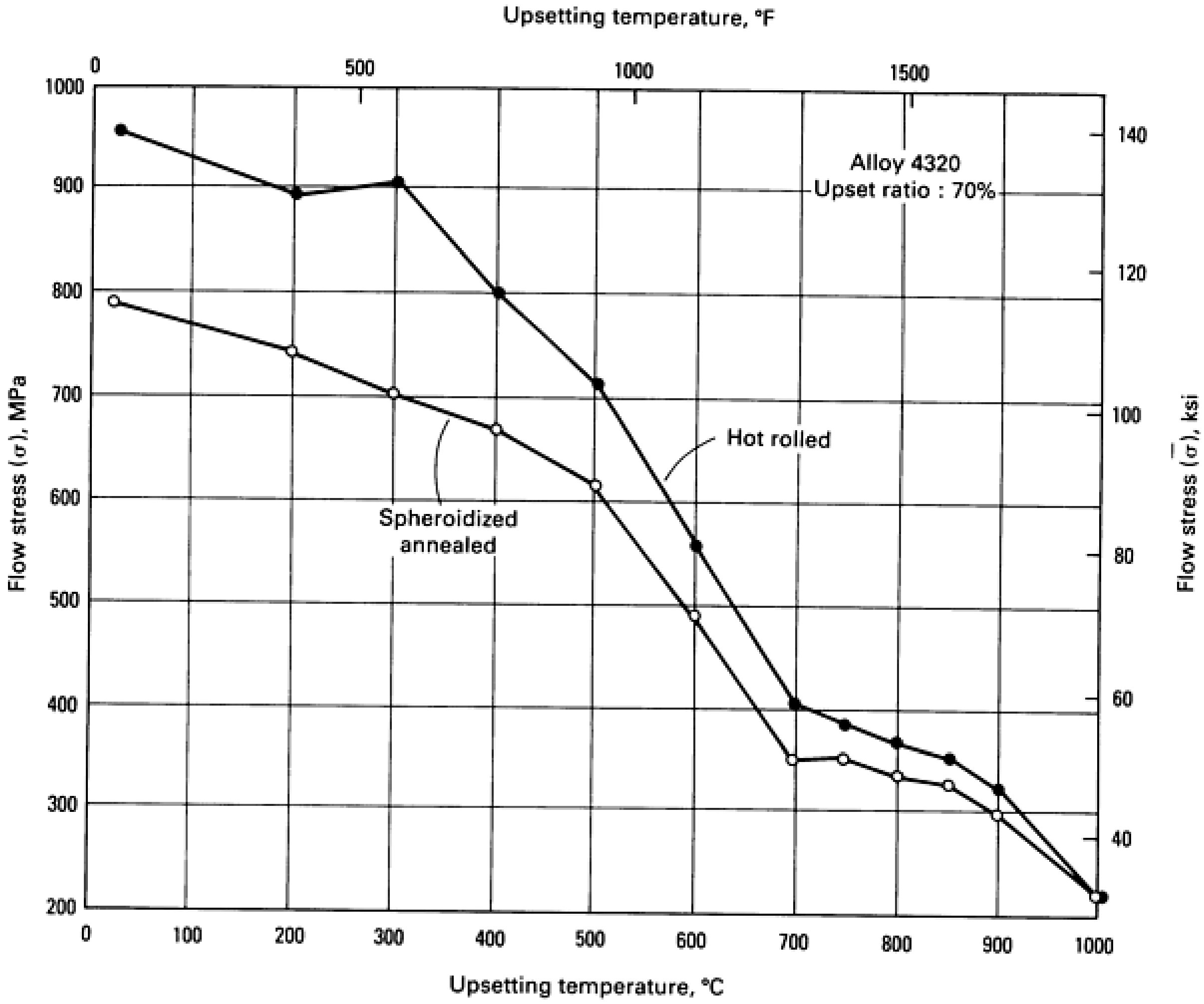

Fig. 3 Effect of structure on flow stress. Source: Kobe Steel Ltd.

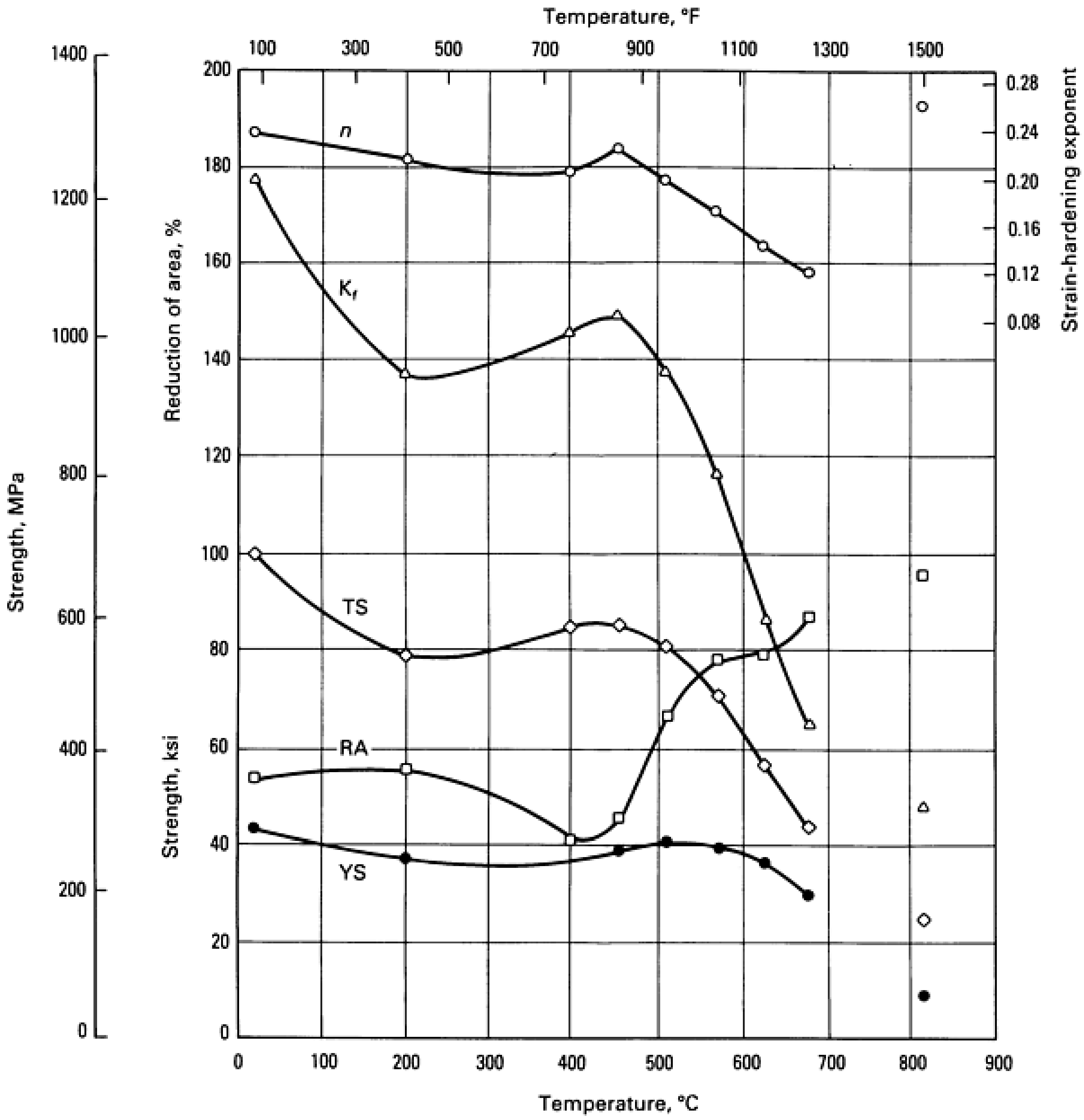

Fig. 4(a) Mechanical properties of 1040 hot-rolled bar from room temperature to 815 °C (1500 °F). Source: Ref 2.

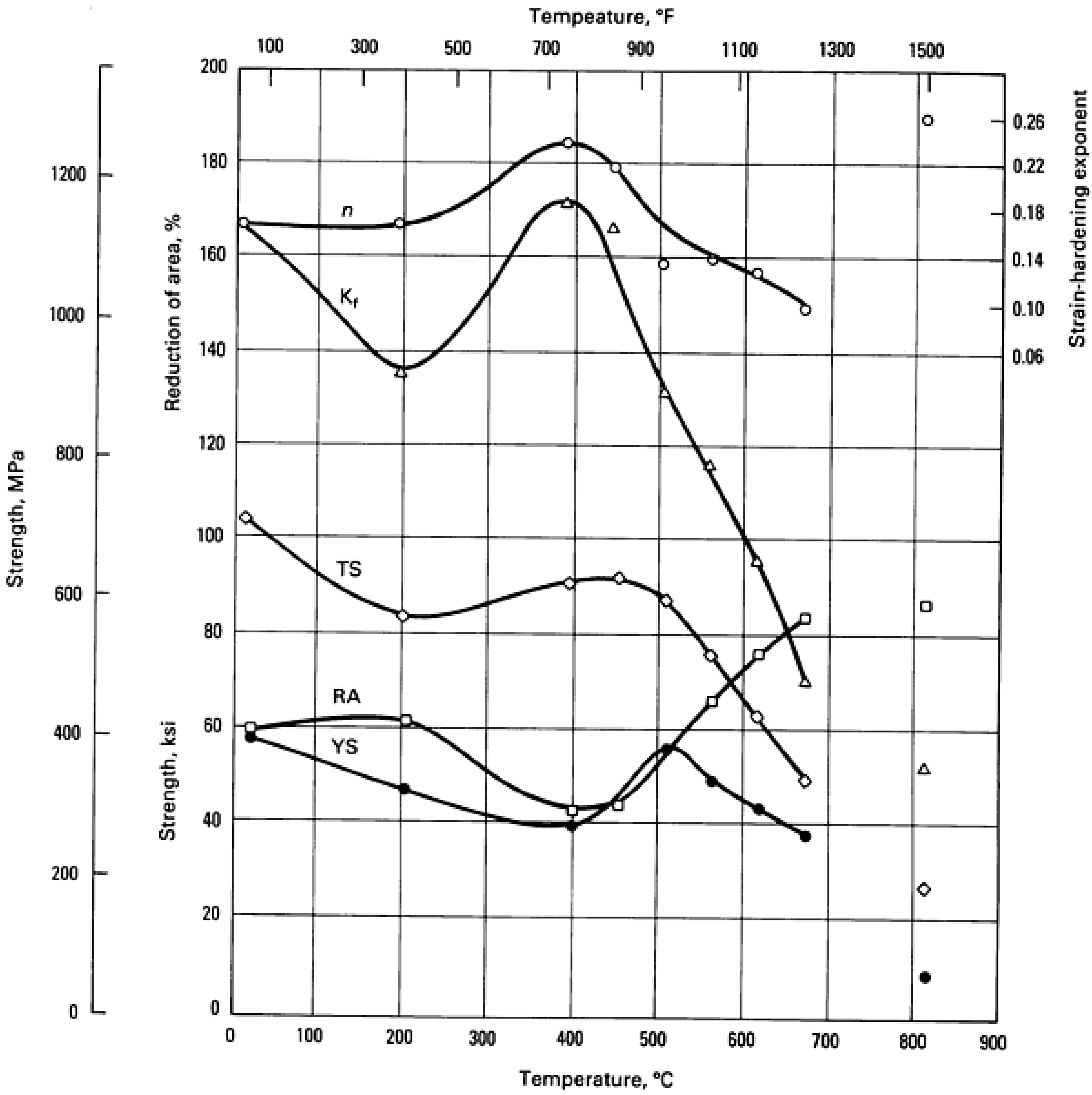

Fig. 4(b) Mechanical properties of 8620 hot-rolled bar from room temperature to 815 °C (1500 °F). Source: Ref 2.

Workability data for common forging alloys are also scarce. Some data for various steel alloys are shown in Fig. 5, which illustrates the effect of working temperature on warm workability, and in Fig. 6, which illustrates the effect of the carbon content of steel on warm workability.

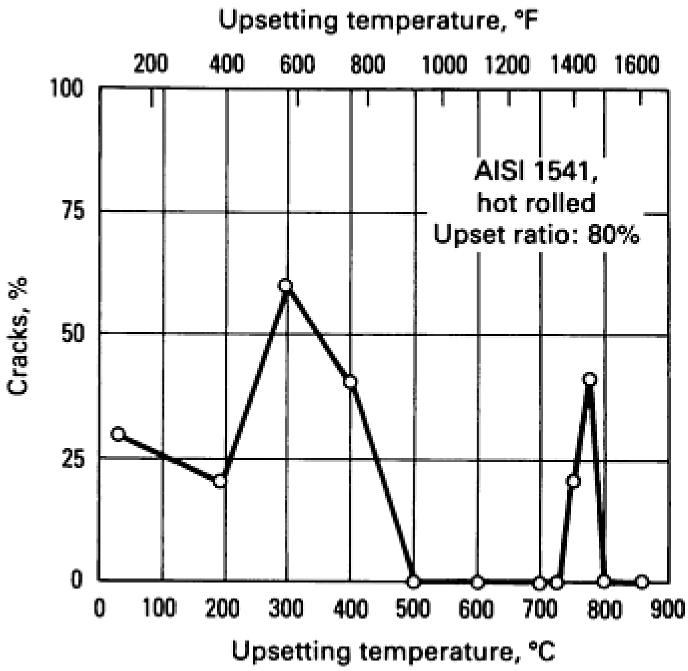

Fig. 5 Effects of working temperature on warm workability. Source: Kobe Steel Ltd.

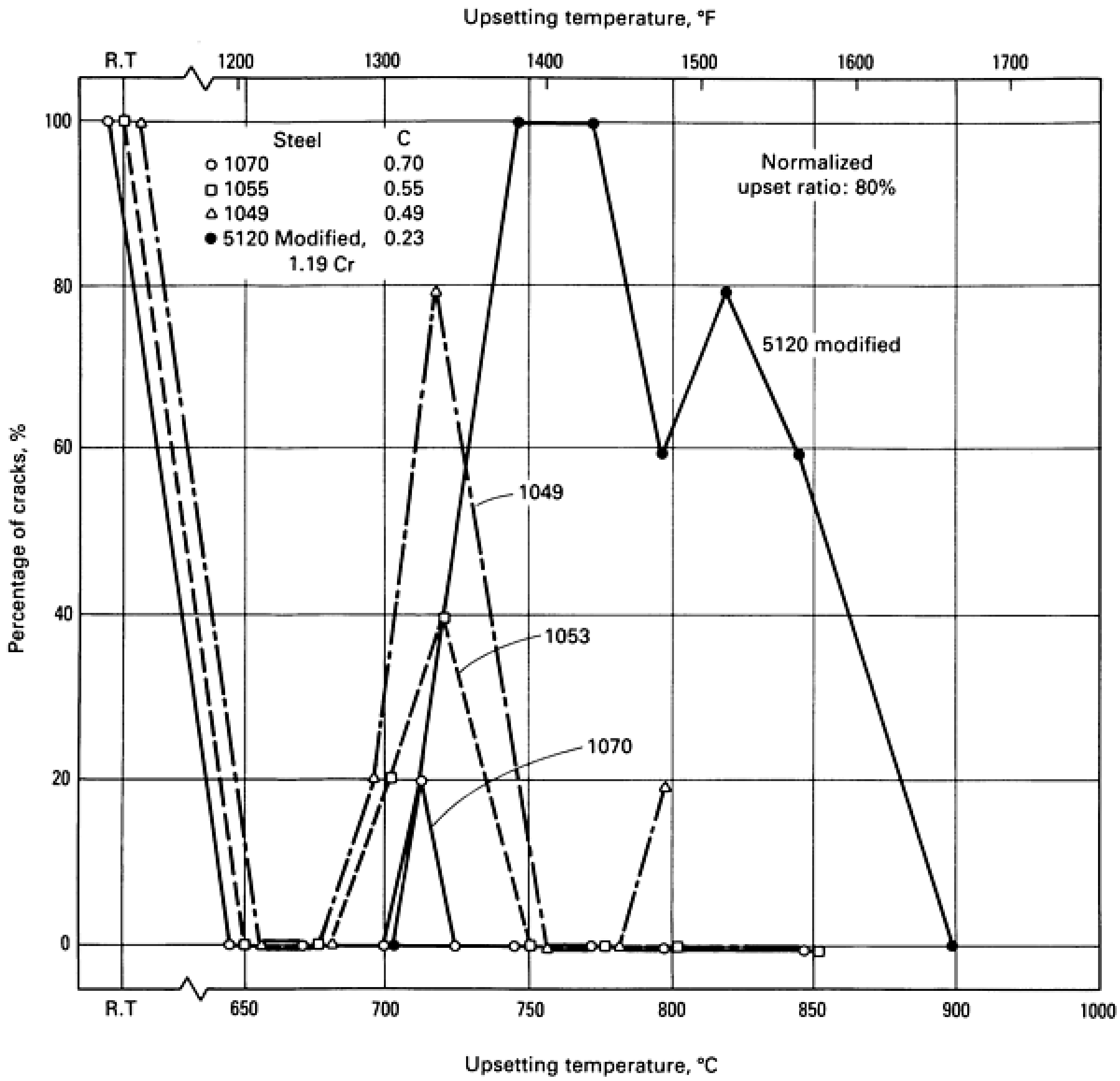

Fig. 6 Effect of carbon content in carbon and alloy steels on warm workability. Source: Kobe Steel Ltd.

Although higher forging temperatures may be desired to decrease flow stress and to improve workability, lower temperatures are favored if oxidation or scaling is a problem. For the forging of steel, the effect of temperature on scale formation is shown in Fig. 7. Scale can also be controlled by heating in an inert atmosphere.

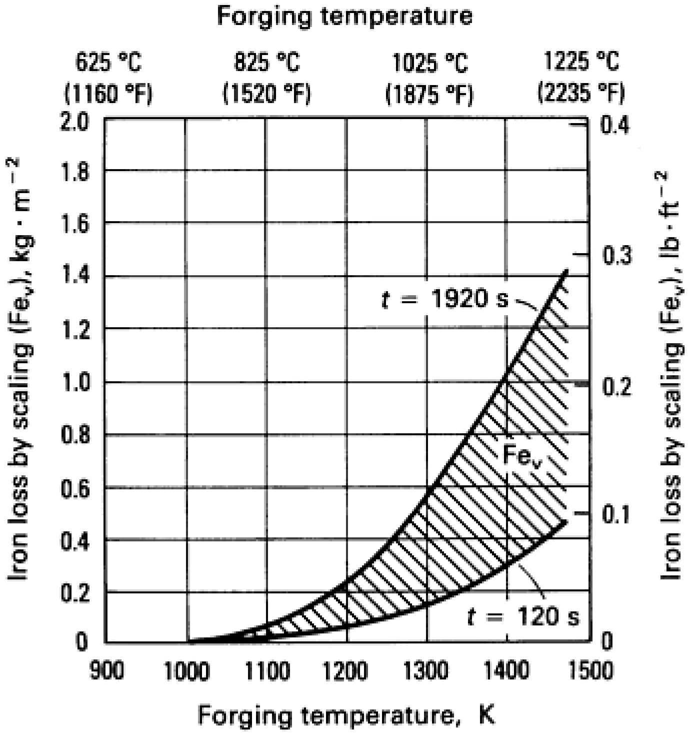

Fig. 7 Effects of temperature on scale formation for the forging of steel. Source: Ref 4.

The effect of workpiece temperature on the tooling is also an important consideration for both selection of the process temperature and specification of the tool materials and heat treatment. Lower temperatures minimize the problems associated with overheating and heat checking (thermal fatigue) of the tooling. The process temperature also affects the performance of the forging lubricant.

Finally, a lower process temperature is desirable from the standpoint of energy conservation. The energy required to heat material to a higher forging temperature is generally much greater than the savings in mechanical energy due to a lower flow stress.

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Precision Forging

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Precision Forming Applications

Example 1: Flashless Forging with a Tension-Knuckle-Drive Mechanical Press.

A closed-die flashless warm-forging process was developed with the capability to generate vertical sides (no draft) and square (filled) corners (Ref 5). In the context of applying this process for ferrous forging, the warm temperature range was considered to extend to approximately 1000 °C (1830 °F). The process is also applicable to the forging of brass, aluminum, copper, and titanium.

Dimensional tolerances of this process are ±0.25 mm (±0.010 in.). All forged surfaces have a finish of 3.20 μm (125 μin.) rms or better.

Because there is no flash to absorb variations in the billet material volume, control of that volume is critical. Any material in excess of the volume of the die cavity must be accommodated by elastic deflection of the tooling and the press. During the development of the flashless process, it was found that a mechanical press with a tension knuckle drive (Fig. 8) system would be an advantage because it would have a higher compliance than other types of mechanical presses. Specifically, it was determined that a 5300 kN (600 tonf) tension knuckle press would stretch 2 mm (0.080 in.) when fully loaded. A comparable top (compression) driven mechanical press would deflect only 0.2 mm (0.008 in.), an order of magnitude less.

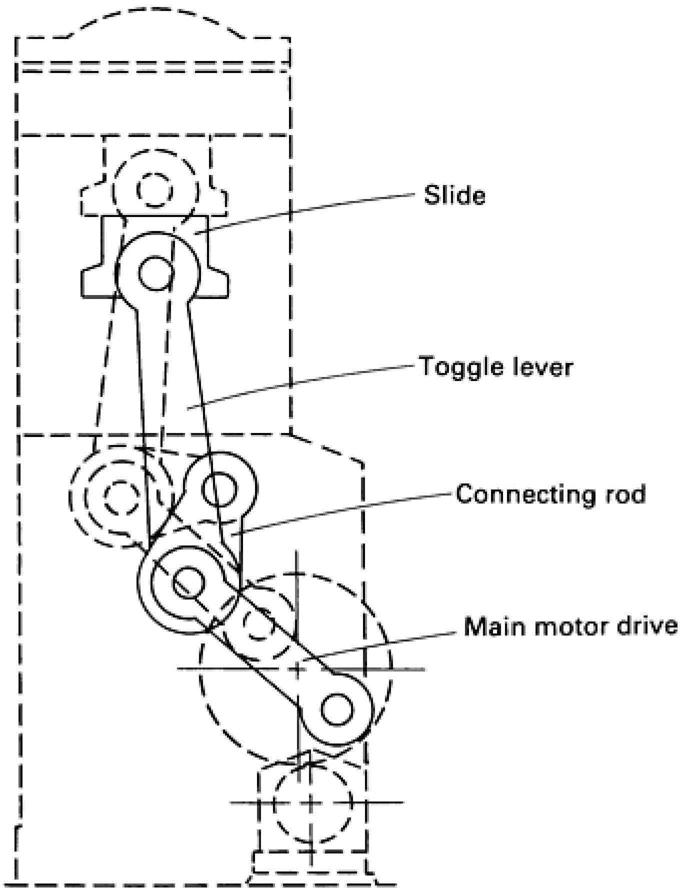

Fig. 8 Schematic of tension knuckle drive forging press. Source: Komatsu, Ltd.

With the tension knuckle drive press, the allowable variation in preform volume is -0.0/+1.4%. Preforms are headed (upset) prior to forging to control weight within this tolerance. Preform volume is also affected by temperature because of the effect of thermal expansion. Temperature control within ±28 °C (±50 °F) was found to be acceptable.

The relationship of the volume of the preform to the volume of the die cavity is also affected by any changes in the tooling itself. Therefore, the tooling temperature is held within 17 °C (30 °F) of ambient by using a flood of coolant. The coolant also contains graphite and therefore functions as a lubricant. Buildup of lubricant within the tooling would effectively decrease the volume of the die, and the lubricant is controlled to prevent this. Tool wear is also closely monitored because this increases the die volume. Such an increase would result in an underfill condition because there is no excess of raw material.

Selection of tool material and heat treatment, which is considered proprietary by the developer, was a critical factor in the success of this flashless forging process. Very high tool loads are encountered, and thermal fatigue is also a problem. With respect to thermal fatigue, in comparison with a stiffer mechanical press of conventional design, the increased deflection of the tension knuckle press will result in longer die contact times during the forging process and therefore increased heat transfer from the workpiece to the tooling.

Flashless forging can be implemented somewhat more easily if region(s) of nonfill (for example, corners) are permitted to allow for some variation in preform volume. The simultaneous achievement of filled corners and zero flash represented the real challenge in the above example.

Example 2: Precision Forging of Spiral Bevel Gear.

A research program was conducted to develop a precision forging process for the manufacture of 250 mm (10 in.) diam spiral bevel gears (Ref 6). The design of the forging dies included correction of the geometry for:

- The elastic deflection of the tooling under mechanical loading
- Bulk contraction due to the shrink fitting of the die assembly
- Thermal contraction of the workpiece from the forging temperature
- Thermal expansion of the tooling under forging conditions

The calculation of the correction for elastic deflection was based on the forging stress distribution and total forging load. These were estimated using both the slab method and the finite-element method of analysis. The average forging pressure at die closure, estimated in terms of the material flow stress at forging temperature, was p = 3.5 = 620.5 MPa (90 ksi). The calculations assumed an average value for the friction coefficient of μ= 0.35. Flow stress of the workpiece material, 8620 steel, at the forging temperature was in turn estimated based on the results of compressive flow stress tests. These results were similar to the results of the tension tests shown in Fig. 4(b).

Using the average forging pressure and the dimensions of the gear, the stresses in the horizontal (x) and vertical (y) directions were estimated. The elastic deflections of the tooling were then expressed as:

$$e_x = \frac{\sigma_x}{E} - \nu \frac{\sigma_y}{E}$$
$$e_y = \frac{\sigma_y}{E} - \nu \frac{\sigma_x}{E} \qquad \textbf{(Eq 4)}$$

where ν is Poisson's ratio and E is the modulus of elasticity.

Estimation of the elastic contraction of the tooling due to shrink fitting was formulated in terms of the classical mechanical engineering analysis of thick circular cylinders under internal pressure.

Calculation of the thermal effects and the estimation of the material flow stress was based on the temperature distributions within the forging and the die. These were estimated through a heat transfer analysis employing the finite-difference method. The thermal profiles after 0.1 s are shown in Fig. 9. To simplify the computations, average temperatures were calculated and used to estimate the thermal corrections and material flow stress. The equations used were based on the same concept as Eq 1.

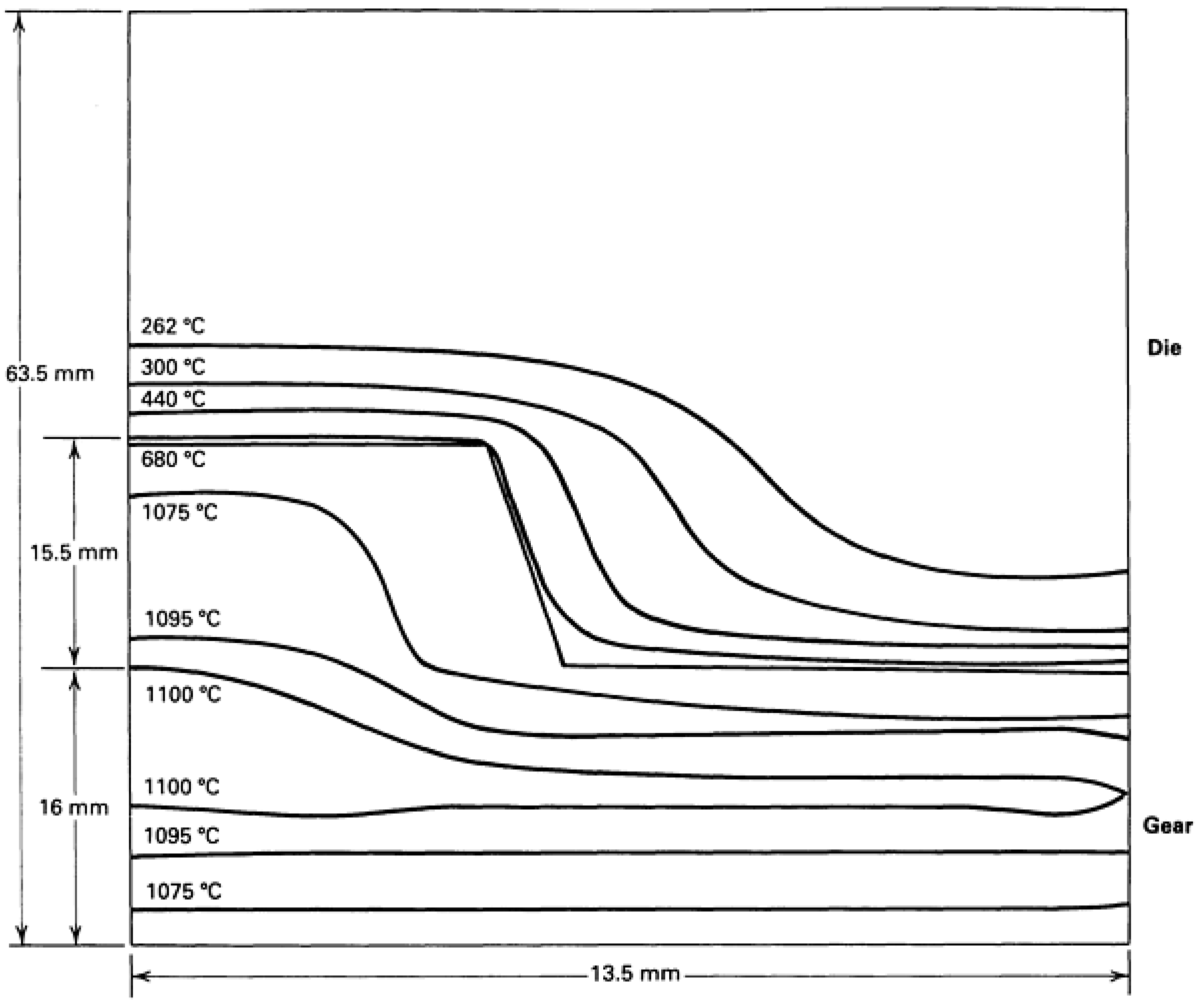

Fig. 9 Example temperature distribution (isotherms) in gear and die after 0.1 s. Initial billet temperature: 1100 °C (2012 °F). Initial die temperature: 260 °C (500 °F)

The results of the analytical work are summarized below. In this case, the thermal effects are the most significant. The elastic deflection in the horizontal direction is compensated by the radial contraction because of shrink fitting of the tooling. It does not always follow, however, that elastic deflections can be neglected. In this example, the most critical dimensions of the tooling were those associated with the gear teeth, the most difficult surfaces to machine. The thickness dimension was not as critical, because allowance was made for machining of the back of the gear after forging:

Effect	Correction mm/mm (in./in.)
Difference between thermal contraction of workpiece and thermal expansion of tooling	**0.02**
Elastic deflection in vertical direction due to forging load	**0.002**
Elastic deflection in horizontal direction due to forging load	**0.001**

Elastic deflection in radial direction due to shrink fitting	**0.001**

An interactive graphics system of computer programs was developed to integrate the geometric representation of the gear and all of the above analyses required for tooling design. The dies were manufactured by the EDM process. It was deemed uneconomical to machine the EDM electrode with numerically controlled machine tools. Instead, the computer system generated parameters for a gear-cutting machine that would result in an electrode incorporating all of the necessary correction factors as described above, as well as allowances for electrode overburn and wear during the EDM process. A total of six EDM electrodes were used in sequence for sinking of the dies.

The precision forging of the spiral bevel gears was done on a 29 MN (3300 tonf) mechanical forging press. Total forging load was estimated at 22 MN (2500 tonf).

The die was vented to allow entrapped gases and lubricant to escape during the forging operation. The preforms were ring shaped, with the outer diameter as close as possible to the outer dimensions of the forged gear. The preforms were heated to 1095 °C (2000 °F) by induction with a nitrogen atmosphere. A schematic of the tooling is shown in Fig. 10. Flash is formed only in the center portion of the forging.

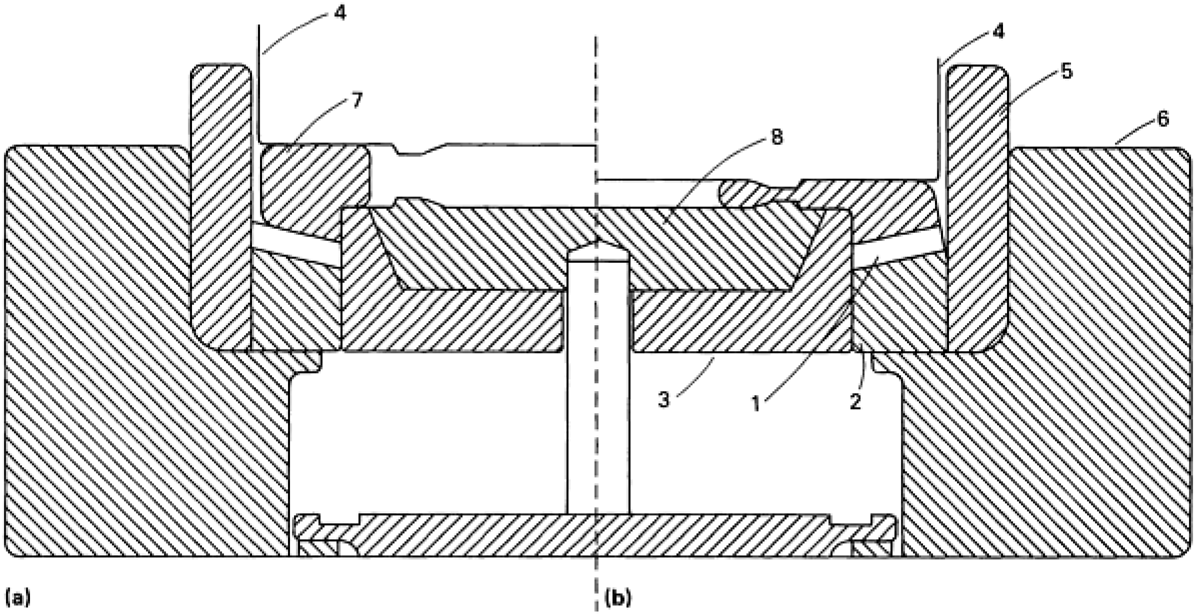

Fig. 10 Schematic of tooling for a preform before (a) and after (b) forging. 1, ring gear; 2, die bottom (with teeth); 3, inner die bottom; 4, punch; 5, die ring; 6, die holder; 7, preform; and 8, kick-out ring

Both H-11 and H-13 die materials were used in this program. The dies were lubricated with a water-base graphite die lubricant sprayed on the die surfaces. In early trials, the billets were precoated with a different water-dispersed graphite lubricant by dipping the billets in a bath containing the lubricant. However, for later trials, it was determined that precoating was not required when the protective atmosphere was used during induction heating. Die temperature was 150 °C (300 °F).

The precision forgings were cooled with the teeth buried in a mixture of sand and graphite. Because the forging lot sizes were small (approximately 20 gears), no data were obtained on die wear under anticipated production conditions.

Two lots of precision forgings were produced in this research program. In the first, the gears were forged with a 0.18 mm (0.007 in.) machining allowance on both sides of the tooth surfaces. In the second lot of forgings, the teeth were forged net. In this case, a maximum variation on the tooth form of 0.08 mm (0.003 in.) was acceptable.

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Precision Forging

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Rotary Forging

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Introduction

ROTARY FORGING, or orbital forging, is a two-die forging process that deforms only a small portion of the workpiece at a time in a continuous manner. Unfortunately, the term rotary forging is sometimes used to describe the process that is more commonly referred to as radial forging, causing some confusion in terminology. Radial forging is a hot- or cold-forming process that uses two or more radially moving anvils or dies to produce solid or tubular components with constant or varying cross sections along their lengths. The differences between rotary and radial forging are illustrated in Fig. 1. Radial forging is discussed in detail in the article "Radial Forging" in this Volume.

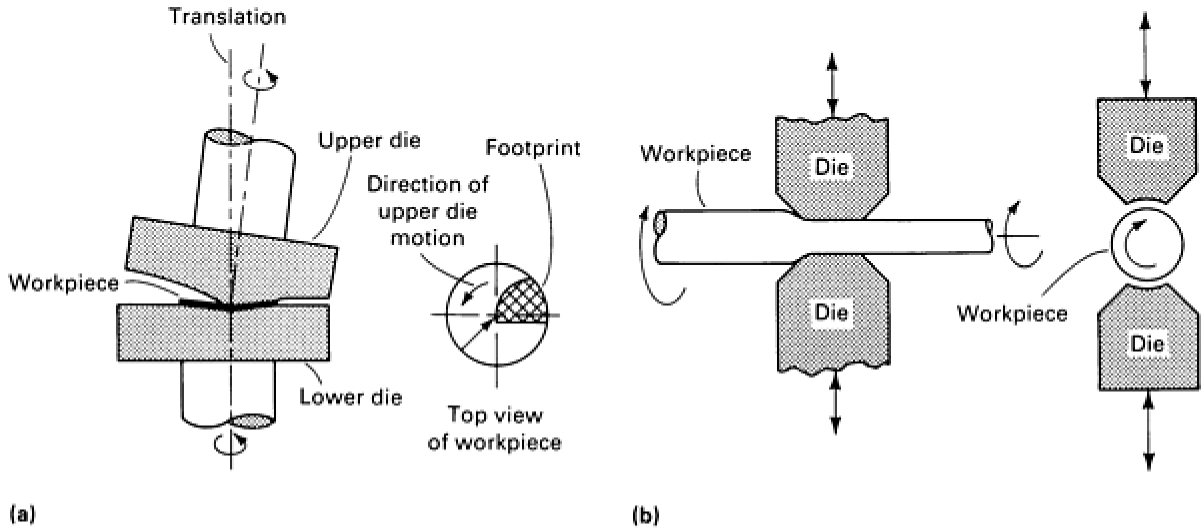

Fig. 1 Differences between rotary and radial forging. (a) In rotary forging, the upper die, tilted with respect to the lower die, rotates around the workpiece. The tilt angle and shape of the upper die result in only a small area of contact (footprint) between the workpiece and the upper die at any given time. Because the footprint is typically only about one-fifth the workpiece surface area, rotary forging requires considerably less force than conventional forging. (b) In radial forging, the workpiece is fed between the dies, which are given a rapid periodic motion as the workpiece rotates. In this manner, the forging force acts on only a small portion of the workpiece at any one time.

In rotary forging (Fig. 1a), the axis of the upper die is tilted at a slight angle with respect to the axis of the lower die, causing the forging force to be applied to only a small area of the workpiece. As one die rotates relative to the other, the contact area between die and workpiece, termed the footprint, continually progresses through the workpiece, gradually deforming it until a final shape is formed. As is evident in Fig. 1(a), the tilt angle between the two dies plays a major role in determining the amount of forging force that is applied to the workpiece. A larger tilt angle results in a smaller footprint; consequently, a smaller amount of force is required to complete the same amount of deformation as compared to a larger contact area. Tilt angles are commonly about 1 to 2°. The larger the tilt angle, however, the more difficult are the machine design and maintenance problems, because the drive and bearing system for the tilted die is subjected to large lateral loads and is more difficult to maintain. In addition, a larger tilt angle causes greater frame deflection within the forge, making it difficult to maintain a consistently high level of precision.

Rotary forges can be broadly classified into two groups, depending on the motion of their dies. In rotating-die forges, both dies rotate about their own axis, but neither die rocks or precesses about the axis of the other die. In rocking-die, or orbital, forges, the upper die rocks across the face of the lower die in a variety of fashions. The most common form is where the upper die orbits in a circular pattern about the axis of the lower die. In this case, the upper die can also either rotate or remain stationary in relation to its own axis. Other examples of rocking-die motion include the rocking of the upper die across the workpiece in a straight, spiral, or planetary pattern (Fig. 2).

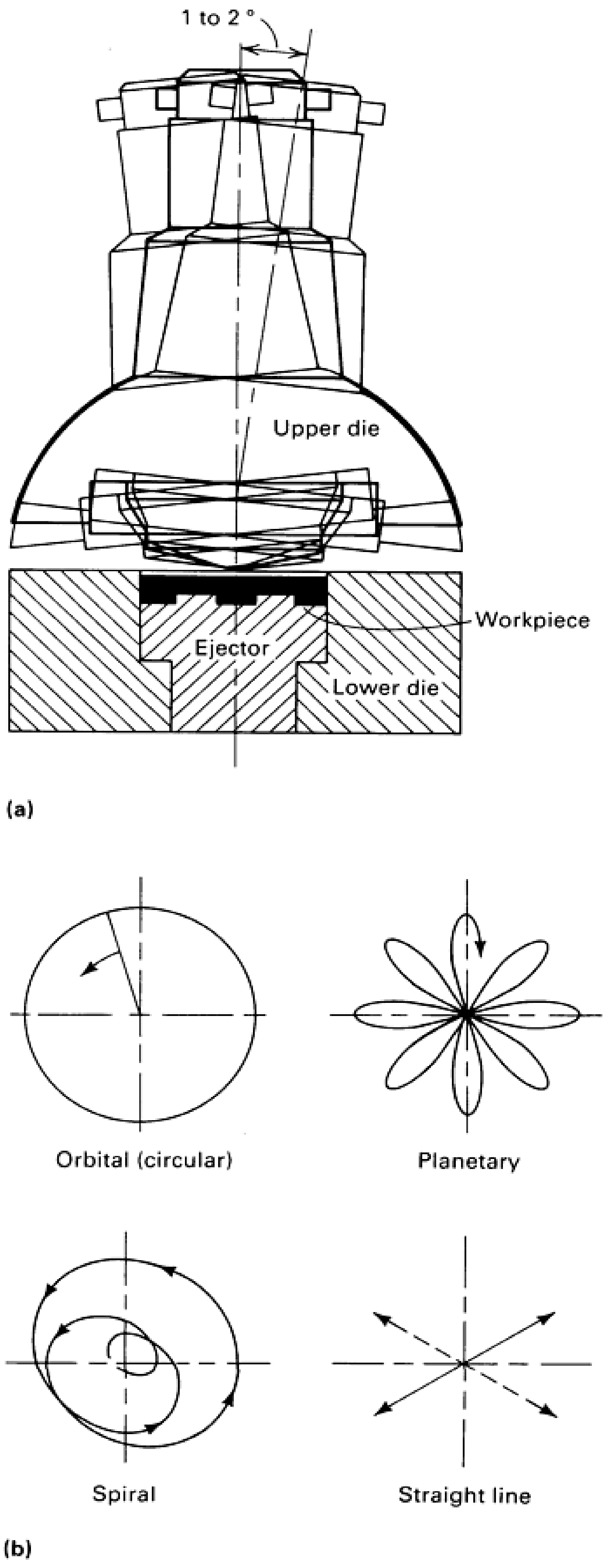

Fig. 2 Schematic of rocking-die forge (a) and sample patterns of upper die motion (b).

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Applications

Rotary forging is generally considered to be a substitute for conventional drop-hammer or press forging. In addition, rotary forging can be used to produce parts that would otherwise have to be completely machined because of their shape or dimensions. Currently, approximately one-quarter to one-third of all parts that are either hammer or press forged could be formed on a rotary forge. These parts include symmetric and asymmetric shapes. In addition, modern rotary forge machines use dies that are 152 to 305 mm (6 to 12 in.) in diameter, limiting the maximum size of a part. In the current technology, rotating or orbiting die forges are mainly limited to the production of symmetrical parts. Through a more complex die operation, rocking-die forges are able to produce both symmetric and asymmetric pieces.

Workpiece Configuration. Parts that have been found to be applicable to rotary forging include gears, flanges, hubs, cams, rings, and tapered rollers, as well as thin disks and flat shapes. These parts are axially symmetric and are formed by using an orbital die motion. More complex parts can be forged through the use of such rocking-die motions as straight-line, planetary, and spiral. Straight-line die motion is most commonly used to produce asymmetric pieces, such as T-flanges.

Rotary forging is especially effective in forging parts that have high diameter-to-thickness ratios. Thin disks and large flanges are ideally suited to this process because of the ability of rotary forging to produce a higher ratio of lateral deformation per given downward force than conventional forging. There is also very little friction between the dies. Therefore, the lateral movement of workpiece material in rotary forging is as much as 30% more than that in impact forging.

Rotary forging is also used to produce intricate features on workpiece surfaces. Parts such as gears, hubs, and hexagonal shapes have traditionally been difficult to produce by conventional forging because die-workpiece friction made it difficult to fill tight spots properly on the dies.

Workpiece Materials. Any material, ferrous or nonferrous, that has adequate ductility and cold-forming qualities can be rotary forged. These materials include carbon and alloy steels, stainless steels, brass, and aluminum alloys. In the past, cold-forged production parts were primarily steels with a Rockwell C hardness in the mid-30s or lower. Generally, harder materials should be annealed before forging or should be warm forged.

Warm rotary forging is used when the material has a Rockwell C hardness greater than the mid-30s or when an unusually large amount of lateral movement in the workpiece is required. Materials are heated to a point below their recrystallization temperature; for steels, this is generally in the range of 650 to 800 °C (1200 to 1470 °F). Because the working temperature is below the recrystallization temperature, the inherent structure and properties of the metal are preserved.

Warm rotary forging results in an increased forging capability compared to cold rotary forging. However, some disadvantages are inherent in higher temperature forging. The work-hardening effects on the material that are associated with cold working are not as prominent, even though the working temperature is below the recrystallization temperature. In addition, as with any forging process, higher working temperatures result in increased die wear. Dies not only wear at a faster rate but also must be fabricated from more durable, more expensive materials.

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Advantages and Limitations

Advantages. The primary advantage of rotary forging is in the low axial force required to form a part. Because only a small area of the die is in contact with the workpiece at any given time, rotary forging requires as little as one-tenth the force required by conventional forging techniques.

The smaller forging forces result in lower machine and die deformation and in less die-workpiece friction. This low level of equipment wear makes rotary forging a precision production process that can be used to form intricate parts to a high degree of accuracy.

Rotary forging achieves this high level of accuracy in a single operation. Parts that require subsequent finishing after conventional forging can be rotary forged to net shape in one step. The average cycle time for a moderately complex part is 10 to 15 s, which is a relatively short time of deformation from preform to final part. In addition, it is unnecessary to transfer the workpiece between die stations; this facilitates the operation of an automatic forging line. A cycle time in the range of 10 to 15 s will yield approximately 300 pieces per hour. The resulting piece is also virtually flash free. Therefore, rotary forging results in a much shorter operation from start to finish.

Tooling costs for rotary forging are often lower than those for conventional forging. Because of the lower forging loads, die manufacture is easier, and the required die strength is much lower. Die change and adjustment times are also much lower; dies can be changed in as little as 15 min. These moderate costs make the process economically attractive for either short or long production runs, thus permitting greater flexibility in terms of machine use and batch sizes.

Because impact is not used in rotary forging, there are fewer environmental hazards than in conventional forging techniques. Complications such as noise, vibrations, fumes, and dirt are virtually non-existent.

The smaller forging forces allow many parts to be cold forged that would conventionally require hot forging, resulting in decreased die wear and greater ease in handling parts after forging. This is in addition to the favorable grain structure that results from the cold working of metals.

Disadvantages. The principal disadvantages of rotary forging lie in the relative newness of the current technology. First, there is a need for a convenient method of determining whether or not a piece can be produced by rotary forging. Like other forging processes, the current process is basically one of trial and error. A set of dies must be constructed and tested for each part not previously produced by rotary forging in order to determine whether or not the part is suitable for rotary forging. This need, however, is inherent in any forging operation that uses a specific set of dies for every different part that is produced. This obviously creates a greater initial capital investment than that required in machining, which does not require specific die construction. Depending on the material as well as the specific shape and geometry, parts that are usually machined may not be suitable for rotary forging for a variety of reasons. For example, the material may experience cracking during the forging process; the finished part may undergo elastic spring-back; or there may be areas on the workpiece that do not conform to the die contour, leaving a gap between die and workpiece, such as central thinning.

Second, the rotary forges that are currently in use are adequate for forming the parts that they presently produce, but the accuracy of these parts is not as great as it can be. Further research and additional production experience are necessary before these forges reach their full practical potential.

Finally, a major problem lies in the design of rotary forge machines. The large lateral forces associated with the unique die motion make the overall frame design of the machines more difficult. These large forces must be properly supported by the frame in order for the forge to maintain a consistent level of accuracy. Conventional forges present a less troublesome design problem because they do not experience such a wide range of die motion.

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Machines

As previously discussed, rotary forging machines are classified by the motion of their dies. These dies have three potential types of motion: rotational, orbital, and translational. Rotational motion, or spin, is defined as the angular motion of the die about its own axis. Rocking, or orbital, motion is the precession of a die about the axis of the other die without rotation about its own axis. Rocking patterns that are currently in use include orbital (circular), straight-line, spiral, and planetary. Translational motion, or feed, is the motion of a die in a linear direction indenting into the workpiece. Machines with three different combinations of these motions are illustrated in Fig. 3.

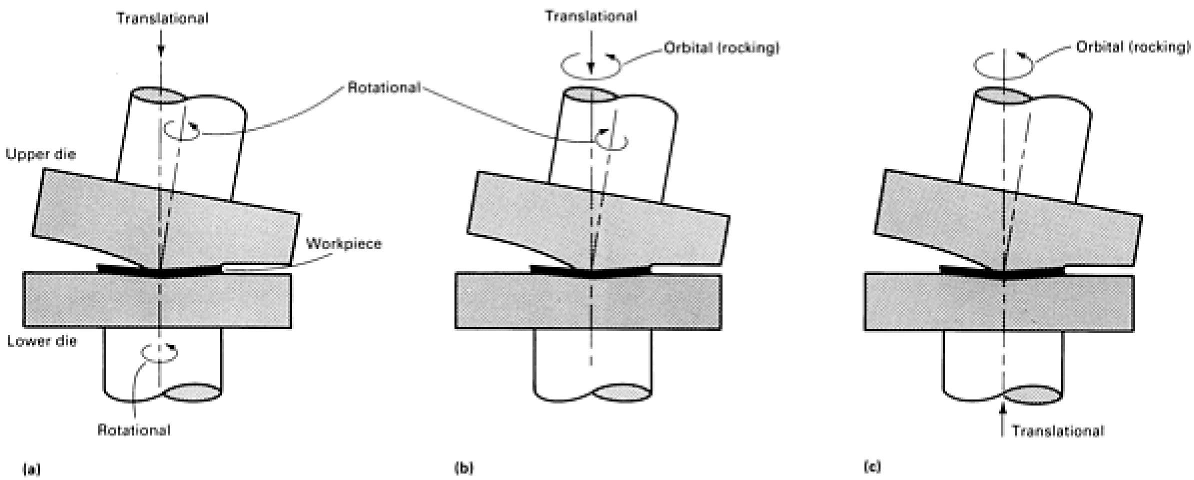

Fig. 3 Examples of die motion in rotary forging. (a) Upper die has both translational and rotational motion, while lower die rotates. (b) Upper die has translational, rotational, and orbital (rocking) motion; lower die is stationary. (c) Upper die has orbital (rocking) motion only; lower die has translational motion.

In modern rotating-die machines, the upper, or tilted, die has rotational and translational motion, while the lower die has only rotational motion (Fig. 3a). Depending on the specific machine, both dies can be independently driven or only the lower die is power driven while the upper die (the follower) responds to the motion of the lower die.

In modern rocking-die forges, the upper die always has rocking motion. In addition, the upper die has both translational and rotational motion (Fig. 3b) or has neither motion (Fig. 3c). In cases in which the upper die does not have translational motion, the lower die has the ability to translate.

The selection of machine type is primarily based on the construction and maintenance of the machine. In general, the machines that use more involved die movement are more difficult to maintain, particularly because of the loss of accuracy due to die and frame deflection.

Rocking-die machines are able to produce parts in a larger variety of shapes and geometries (particularly asymmetric parts). However, because of the large amount of die and frame movement, these parts may not be as precise as those produced with rotating-die machines. In addition, rocking-die machines require more frequent maintenance in order to retain their original level of accuracy.

Rotating-die machines are commonly used to forge symmetric parts. Included among these types of machines is the rotary forge that has the simplest die motion, in which both dies have rotational motion and one also has translational motion. In this case, the forging force always acts in one direction; therefore, the press design is simplified, and the minimal amount of frame deflection results in maximum precision. In addition, any error in the part is uniformly distributed around the circumference of the part, thus facilitating the alteration of die design to compensate for the error.

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Dies

Rotary forging dies will typically produce 15,000 to 50,000 pieces before they must be refinished. Naturally, die life depends on the material being forged and on the complexity of the piece.

Because rotary forging dies experience a much lower forging force than normal, they are generally small and are usually made of inexpensive materials, typically standard tool steels. Therefore, die cost is lower than in other conventional forging methods. Lubrication of the dies, although not essential, is suggested in order to increase die life.

Both dies can be changed within 15 min. Complete job change and adjustments require approximately 30 min. This makes rotary forging particularly attractive for short production runs.

Examples

Example 1: Rotary Forging of a Bicycle Hub Bearing Retainer.

A rocking-die forge was used to produce the bearing retainer shown in Fig. 4(a). This part is used in bicycle hubs.

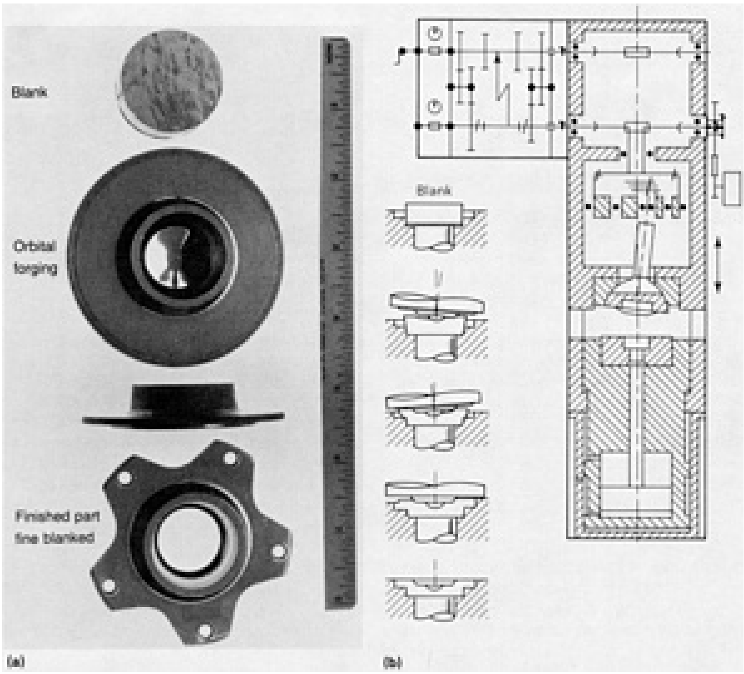


Fig. 4 Rotary-forged aluminum alloy 6061 bearing retainer (a) used in bicycle hubs. (b) Schematics of the rotary forge used to produce the bearing retainer and the workpiece deformation process (left).

The material of construction was aluminum alloy 6061. The aluminum was first saw cut from 33.3 mm ($1\frac{5}{16}$ in.) diam bar stock into 19 mm (0.75 in.) thick pucks. The material was heat treated from an initial hardness of T4 to a hardness of

T6. The puck was then placed on the lower die, and the upper die, using an orbital rocking pattern, deformed the material to fill the lower die mold. A schematic of the forge and the workpiece deformation is shown in Fig. 4(b).

After the deformation was complete, the upper die was raised, and the piece was ejected from the lower die. The resulting part had an outside diameter of 88.9 mm (3.5 in.). The retainer was then fine blanked to the final shape.

The production rate was approximately 6 to 7 parts per minute. This process is noticeably faster and less expensive than the conventional alternative of turning these parts down from 88.9 mm (3.5 in.) diam preforms, which involves a large amount of material waste. In addition, the rotary-forged pieces exhibit a higher density and a more beneficial grain structure as a result of the cold working of the material.

Example 2: Warm Rotary Forging of a Carbon Steel Clutch Hub.

A rocking-die press with a capacity of 2.5 MN (280 tonf) was used to warm forge a clutch hub. The medium-carbon steel (0.5% C) blank was first heated to a temperature of 1000 °C (1830 °F) and then placed on the lower die. Both upper and lower dies were preheated to about 200 °C (390 °F) and maintained in the range of 150 to 250 °C (300 to 480 °F) during forging. The lower die was raised until die-workpiece contact was made, and the upper die was rocked in an orbital pattern. Water-soluble graphite was sprayed onto the dies as a lubricant. The working time for forging was approximately 1.5 s per piece. The working load was about 0.75 MN (84 tonf), or about one-tenth the load required for conventional hot forging.

The quenching, tempering, and finish machining processes associated with conventional hot forging are not required for the rotary-forged part. After forging, the piece is merely cooled and then blanked to final dimensions. The surfaces of the piece have the same smoothness as the two dies. Flange flatness deviation and thickness variation are less than 0.1 mm (0.004 in.). An additional benefit of the lower forging temperature (conventional hot forging of these parts is done at 1250 °C, or 2280 °F) is a reduced grain size, which improves the strength of the part. A comparison between conventionally and rotary warm forged hubs is shown in Fig. 5. The rotary-forged hub requires a smaller billet weight, thus decreasing the amount of material waste. The rotary-forged hub also has closer tolerances than the conventionally forged hub, demonstrating the precision of the rotary process.

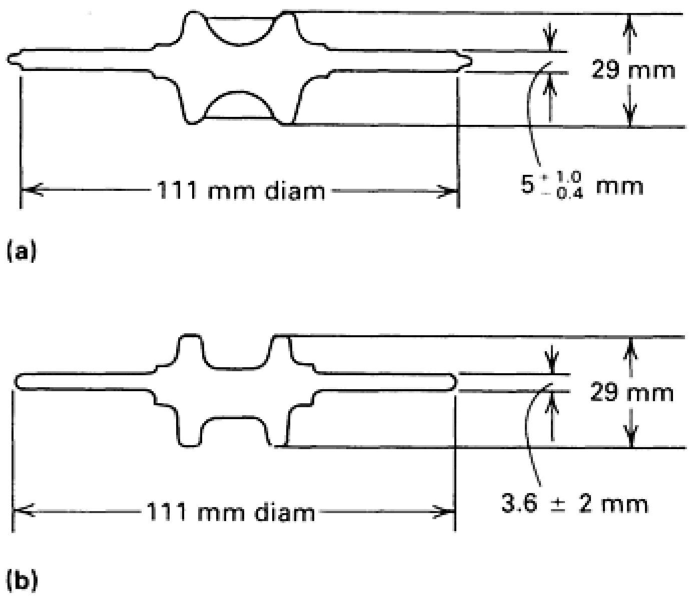


Fig. 5 Comparison of conventionally forged (a) and rotary hot forged (b) carbon steel clutch hubs. Billet weight: 0.63 kg (1.39 lb) for conventional forging, 0.44 kg (0.97 lb) for rotary forging.

The higher temperatures associated with warm rotary forging cause more rapid die-wear than that found in cold rotary forging. In this example, the dies, made of AISI H13 tool steel with a hardness of 50 HRC, exhibited noticeable wear after only 50 pieces had been forged.

Example 3: Rotary Forging of a Copper Alloy Seal Fitting.

A rotating-die machine was used to cold forge a naval brass seal fitting. This fitting is used in high-pressure piping, such as in air conditioners or steam turbines.

The initial preforms were 86.4 mm (3.4 in.) lengths of 44.5 mm (1.75 in.) OD, 24.1 mm (0.95 in.) ID tube stock. As shown in Fig. 6, the tube preform was fitted over a cylindrical insert that protrudes from the lower die. The upper die was lowered until indentation was made. Die rotation then began. The workpiece was deformed to fit the dimensions of the lower die and then ejected. The rotary-forged product was 39.7 MM ($1\frac{9}{16}$ in.) long with a minimum inside diameter of 23.6 mm (0.93 in.) and a maximum outside diameter of 55.6 mm ($2\frac{3}{16}$ in.). Minimal machining was required to bring the part to final dimensions.

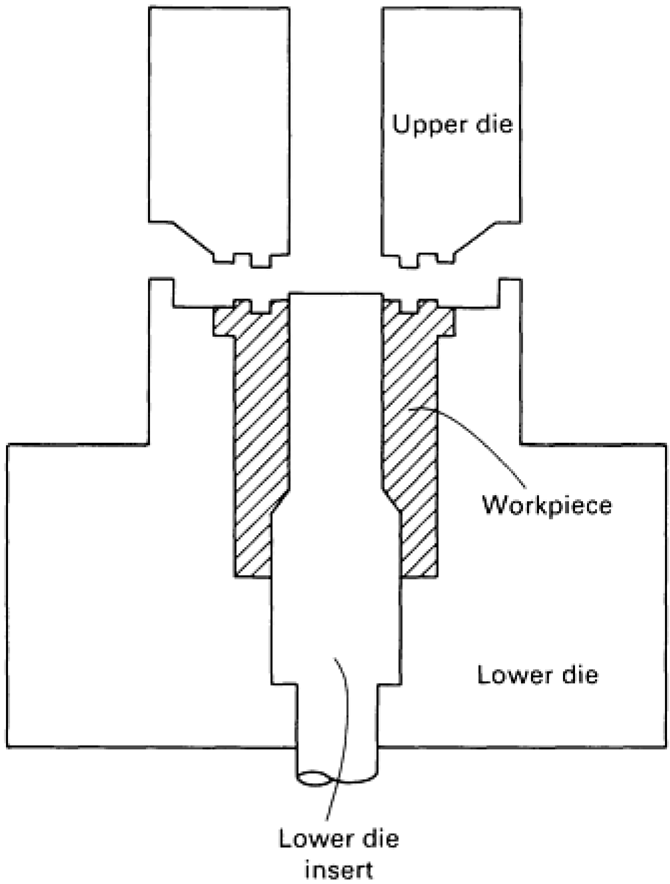


Fig. 6 Schematic of rotary forging setup for the forming of a copper alloy seal fitting used in high-pressure piping.

The machine used to produce these fittings is a rotating-die forge in which both dies rotate only about their own axis. The upper die is motor driven, while the lower die merely follows the rotation of the upper die after contact is made. The dies are constructed of A2 tool steel heat treated to a hardness of 58 to 62 HRC. The expected life of these dies is approximately 20,000 pieces.

In conventional processing, these fittings would be machined from 75 mm (3 in.) solid bar stock. This results in a large amount of wasted material, and machining time is approximately 17 min per piece. The tube stock used for rotary forging is more expensive than bar stock, but material waste is minimal. In addition, rotary forging requires only 20 s per piece, with an additional 3 to 4 min per piece needed for subsequent machining to final form.

Introduction

COINING is a closed-die forging operation, usually performed cold, in which all surfaces of the workpiece are confined or restrained, resulting in a well-defined imprint of the die on the workpiece. It is also a restriking operation (called, depending on the purpose, sizing or bottom or corner setting) used to sharpen or change a radius or profile. Ordinarily, coining entails the following steps:

- *Preliminary Workpiece Preparation.* Full contact between the blank and die surfaces, which is necessary for coining, usually requires some preliminary metal redistribution by other processes, such as forging or extrusion, because only a small amount of metal redistribution can take place in the coining dies in single-station coining. In progressive-die operations, coining is done as in single-station dies, but it is preceded by other operations such as blanking, drawing, piercing, and bending. Coining is often the final operation in a progressive-die sequence, although blanking or trimming, or both, frequently follow coining
- *Development of Detail in the Workpiece.* In coining dies, the prepared blank is loaded above the compressive yield strength and is held in this condition during coining. Dwell time under load is important for the development of dimensions in sizing and embossing; it is also necessary for the reproduction of fine detail, as in engraving
- *Trimming.* Flash that develops during coining and any hangers used to carry the blank through coining, especially in progressive-die coining, must be trimmed from the piece

Applicability

In coining, the surface of the workpiece copies the surface detail in the dies with dimensional accuracy that is seldom obtained by any other process. It is because of this that the process is used for coin minting.

Decorative items, such as patterned tableware, medallions, and metal buttons are also produced by coining. When articles with a design and a polished surface are required, coining is the only practical production method to use. Also, coining is well suited to the manufacture of extremely small items, such as interlocking-fastener elements.

Dimensional accuracy equal to that available only with the very best machining practice can often be obtained in coining. Many automotive components are sized by coining. Sizing is usually done on semifinished products, and provides significant savings in material and labor costs relative to machining.

Workpiece Size. Practical limits on workpiece size are mainly imposed by available press capacities and properties of the die material. For example, work metal with a compressive yield strength of 690 MPa (100 ksi) loaded in a press of 22 MN (2500 tonf) capacity can be coined in a maximum surface area of 0.032 m^2 (50 $in.^2$) . As the yield strength increases, the area that can be coined using the same press decreases proportionately. However, an increase in strength of the workpiece must be limited so that plastic failure of the die does not take place.

Hammers and Presses

In coining, the workpiece is squeezed between the dies so that the entire surface area is simultaneously loaded above the yield strength. To achieve the desired deformation of metal, the load determined from the compressive yield strength must be increased three to five times. Because of the area loading requirement and the great stress needed to ensure metal movement, press loading for coining is very severe, frequently approaching the capacity of the equipment used, with consequent danger of overloading.

Some coining equipment, such as drop hammers, cannot be readily overloaded, but presses (especially mechanical presses) can be severely overloaded. This is most likely to happen if more than one blank is fed to the coining dies at a time. Such overloading can break the press and the dies, and it will certainly shorten the life of the dies.

Overloading may be prevented by the use of overload release devices, and many presses are equipped with such devices. However, the usual means for preventing overloading in presses is careful control of workpiece thickness, which must be sufficient to allow acceptable coining, but not enough to lead to press overloading. Such thickness control, combined with blank-feeding procedures designed to minimize double blanking, is normally adequate to prevent overloading.

Coining may be satisfactorily undertaken in any type of press that has the required capacity. Metal movement, however, is accomplished during a relatively short portion of the stroke, so that a coining load is required only during a small portion of the press cycle.

Drop hammers, and knuckle-type and eccentric-driven mechanical presses are extensively used in coining. High-speed hydraulic presses also are well adapted for coining, especially when progressive dies are used. Large-capacity hydraulic presses are ideal for coining and sizing operations on large workpieces. On the other hand, when it is feasible to coin large numbers of small, connected parts, as in a continuous strip of work metal, roll coining is the most economical method.

Drop Hammers. Gravity drop hammers with ram weights in the range of 410 to 910 kg (900 to 2000 lb) are extensively used in the tableware industry. Board hammers can be used, although pneumatic-lift hammers predominate for this type of coining. In producing tableware, reproduction of detail and finish are more important than dimensional control.

Capacities of drop hammers are determined by ram weight and drop height, and coining pressures are stated in terms of these two quantities. Ram weight is usually selected in relation to the thickness and area of the blank. Drop height and the number of blows are determined by the complexity of the detail that is to be developed in the workpiece.

Mechanical presses with capacities ranging from a few tons to several hundred tons are widely used in coining. The larger presses are usually of the knuckle type, with production rates up to about 7500 pieces per hour. Small, specially built eccentric-driven presses are used for high-production coining of tiny parts.

Mechanical presses are well adapted for controlling size. Also, one-stroke sizing is generally preferred to a process requiring multiple blows, because there is less likelihood of fracturing the work metal.

Crank-driven mechanical presses have been successfully used in progressive-die coining. For these processes, coining usually follows combinations of piercing, forming, and blanking.

Hydraulic presses are extensively used for sizing operations, especially for workpieces with large surfaces to be coined. Spacers are required for maintaining close tolerances on the final dimensions of the part being sized. Hydraulic presses are sometimes favored because they are readily equipped with limiting devices that prevent overloading and possible die breakage.

Smaller hydraulic presses (about 70 kN, or 8 tonf, capacity) can be operated at speeds of up to 250 strokes per minute. These small high-speed presses are extensively used with progressive dies.

Capacity required for a coining operation, for open-die forming, or for sizing can be determined either by measuring in a compression machine the forces necessary to cause metal movement or by measuring the compressive yield strength and multiplying three to five times this value by the coined area of the part.

Strip of closely controlled thickness used in high-speed coining machines is frequently produced by rolling from round wire. The strain history and consequent strain-hardening behavior of progressively flattened round wire are usually not known. Also, because interaction between die and workpiece changes continuously with deformation, the loads required to flatten round wire are difficult to calculate and should be measured.

Lubricants

Whenever possible, coining without a lubricant is to be preferred. If entrapped in the coining dies, lubricants can cause flaws in the workpieces. For example, under conditions of constrained plastic flow, an entrapped lubricant will be loaded in hydrostatic compression and will interfere with the transfer of die detail to the workpiece. In many coining operations, however, because of work metal composition or the severity of coining, or both, the use of some lubricant is mandatory to prevent galling or seizing of the dies and the work metal.

No lubricant is used for coining teaspoons, medallions, or similar items from sterling silver. Some type of lubricant is ordinarily used for coining copper and aluminum and their alloys and for coining stainless, alloy, and carbon steels. When coining intricate designs, such as the design on the handles of stainless steel teaspoons, the lubricant must be used sparingly. A film of soap solution is usually sufficient. Excessive amounts of lubricant adversely affect workpiece finish and interfere with transfer of the design.

When coining items that do not require transfer of intricate detail, the type and amount of lubricant are less critical. A mixture of 50% oleum spirits and 50% medium-viscosity machine oil has been successful for prevention of galling and seizing for a large variety of coining operations. When coining involves maximum metal movement and high pressure, a commercial deep-drawing compound is sometimes used.

Die Materials

Coining dies may fail by wear, deformation due to compression, or cracking. With low coining pressures and soft work metal, wear failures predominate. With some combinations of die metal and work metal, dies may fail by adhesion (wear caused by metal pickup).

Failure of dies from deformation or cracking is usually caused by coining extremely intricate designs, attempts to coin large areas that confine the metal and build up excessive pressure, or coining of oversize slugs.

Constraints due to the pattern being produced may limit die life and cause premature cracking. If the obverse and reverse artwork of a decorative medal are not aligned properly, metal flow will be restricted and the die will not fill properly. As a result, excess tonnage (pressure) must be used to obtain fill, which sharply reduces die life. Stress raisers such as straight lines and sharp edges, which often are present in designs for decorative medals, also reduce die life unless the tonnage can be lowered. Low tonnage requirements often can be achieved by striking softer blanks, provided the blank is not so soft that a fin is extruded on coining.

Dies for Decorative Coining

Selection of tool steels for fabrication of dies used for striking high-quality coins and medals requires consideration of several important properties and characteristics. Among these are machinability, hardenability, distortion in hardening, hardness, wear resistance, and toughness. In dies used for decorative coining, materials that can be through hardened to produce a combination of good wear resistance, high hardness, and high toughness are preferred.

A smooth, polished background surface on the die is required for striking proof-type coins and medals. Massive undissolved carbides or nonmetallic inclusions make it more difficult to obtain this smooth background. Special processing and inspection should be required for tool steels to be used for coining dies (particularly in large sections), because any such imperfections can be troublesome. The stringent controls ordinarily applied to tool steels may not be sufficient to ensure that the required die surface condition will be obtainable.

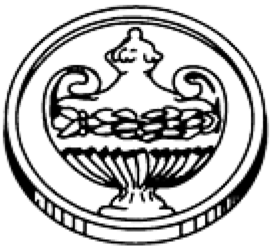
Typical Die Materials. For dies up to 50 mm (2 in.) in diameter, consumable-electrode vacuum-melted or electroslag remelted 52100 steel provides the clean microstructure necessary for the development of critical polished die surfaces. When heat treated to a hardness of 59 to 61 HRC, 52100 steel provides optimum die life. This steel is also suitable for photochemical etching, a process used in place of mechanical die sinking for engraving many low-relief dies. L6 tool steel at a hardness of 58 to 60 HRC is suitable for dies up to 102 mm (4 in.) in diameter. It can be through hardened, has enough toughness for long-life applications, and is suitable for photochemical etching of low-relief patterns. Air-hardening tool steels are preferred for coining and embossing dies greater than 102 mm (4 in.) in diameter. One of the chief reasons for choosing air-hardening tool steels is their low degree of distortion during heat treatment. Tool steel A6 is a nondeforming, deep-hardening material that is often used for large dies that must be hardened to 59 to 61 HRC. Air-hardening hot-work steels such as H13 are used at a hardness of 52 to 54 HRC for applications requiring especially high toughness.

For dies containing high-relief impressions, the lowest die cost is obtained by machining the impressions directly into the dies when the die life is anticipated to outlast the number of pieces to be coined. For longer runs that require two or more identical dies, it is less expensive to produce the impressions by hubbing. Hubbing is done by cutting the pattern into a male master plug (hub), hardening this hub, and pressing the hardened hub into a die block to make the coining impression. Highly alloyed tool steels are relatively difficult to hub. When coining dies are made from these steels, it may be necessary to form the impression by hot hubbing or by hubbing in several stages with intermediate anneals between stages.

Table 1 gives typical materials used to make the punches and dies for coining small pieces such as the 13 mm (½ in.) diam emblem shown in the accompanying sketch. The choice of tool material often depends less on the alloy to be coined than on the way the tools are made and the type of stamping equipment to be used.

Table 1 Typical materials for dies used to coin small emblems

Type of tool	Tool material[a] for striking a total quantity of:		
	1000	**10,000**	100,000
Machined dies for use on drop hammers	W1	W1	**O1[b], A2**
Machined dies for use on presses	O1	O1, A2	**O1, A2**
Hubbed dies for use on drop hammers	W1	W1	**W1[c]**
Hubbed dies for use on presses	O1	O1, A2	**A2, D2[d]**



(a) For coining the emblem from aluminum, copper, gold, or silver alloys, or from low-carbon, alloy, or stainless steel.

(b) O1 recommended only for coining low-carbon steel and alloys of copper, gold, or silver.

(c) The average life of W1 dies in coining alloys of copper, gold, or silver softer than 60 HRB would be about 40,000 ± 10,000 pieces. Life of W1 dies in coining harder materials would be about half as great; therefore, more than one set of dies would be needed for 100,000 parts or more.

(d) Hot hubbed

Tool steels O1 and A2 are alternative choices for machined dies in production quantities up to about 100,000 pieces. The small additional cost of A2 is often justified because A2 gives longer life, especially when aluminum alloys, alloy steels, stainless steels, or heat-resistant alloys are being coined.

Production of coins and medallions frequently involves quantities much greater than 100,000 pieces. Coins are usually produced on high-speed mechanical presses using dies containing impressions that have relatively low relief above the background plane. Dies for this type of operation must be easily hubbed, inexpensive, wear resistant, and made of nondeforming materials. Tool steel W1 is often selected for small dies, and 52100 is used for either small or large dies. Average die life can be expected to range from 200,000 to more than 1,000,000 strikes, depending on the type of coinage alloy and on coin diameter.

Dies for Coining Silverware. Probably the greatest amount of industrial coining is done with drop hammers in the silverware industry. Water-hardening steels such as W1 are almost always used for making such coining dies, whether the product is made of silver, a copper alloy, or stainless steel. Water-hardening grades are selected because die blocks made of these steels can be repeatedly reused. After a die block fails--either by shallow cracking of the hardened shell or by wear of the high points of the impressed pattern--the block is annealed, the impression is machined off, and a new impression is hubbed before the die is re-hardened. Dies made of deep-hardening tool steels such as O1, A2, and D2 are not reused (as are W1 dies), because they fail by deep cracking.

For ordinary designs requiring close reproduction of dimensions, dies may be made of A2 or of the high-carbon high-chromium steels D2, D3, and D4, to obtain greater compression resistance. For coining designs with deep configurations and either coarse or sharp details, where dies usually fail by cracking, a deep-hardening carbon tool steel may be used at lower hardness, or O1, S5, or S6 may be selected. In some instances, it may be desirable to select an air-hardening type such as A2, which provides improved dimensional stability and wear resistance. A hot-work steel such as H11, H12, or H13 may prove to be best when extreme toughness is the predominant requirement. When die failure occurs by rapid wear, a higher-hardness steel or a more highly alloyed wear-resistant steel such as A2 may solve the problem.

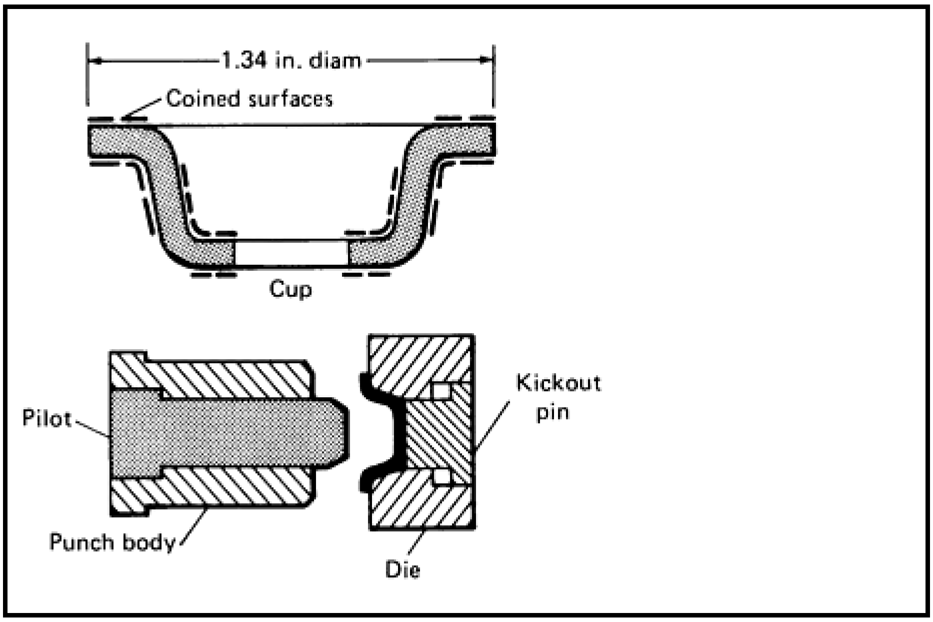
For articles coined on drop hammers from AISI 300 series austenitic stainless steels, it has sometimes been found advantageous to use steels of the S1, S5, S6, and L6 types, oil quenched and tempered to 57 and 59 HRC. Because the carbon contents of these grades are between 0.50 and 0.70%, they are less resistant to wear than are W1, A2, or D2, but are tougher and more resistant to chipping and splitting. If necessary, the wear resistance of S5 tool steel dies can be slightly improved by carburizing to a depth of 0.13 to 0.25 mm (0.005 to 0.010 in.).

Coining in Progressive Dies

Tool steels recommended for coining a cup-shape part to final dimensions in the last stages of progressive stamping are shown in Table 2. This press coining operation involves partial confinement of the entire cup within the die. This produces high radial die pressures and thus requires pressed-in inserts on long runs, to prevent die cracking. Quantities up to about 10,000 can be made with the steels given in Table 2 without danger of failure by cracking; the D2 steel listed for quantities greater than 10,000 pieces is used in the form of an insert pressed into the die plate.

Table 2 Typical tool steels for coining a preformed cup to final size on a press

Metal to be coined	Die material for total quantity of:		
	1000	**10,000**	100,000
Aluminum and copper alloys	W1	W1	**D2**
Low-carbon steel	W1	O1	**D2**
Stainless steel, heat-resisting alloys, and alloy steels	O1	A2	**D2**



(a) For quantities over 10,000, the materials are given for die inserts. All selections shown are for machined dies. The same material would be used for the punch, except that O1 should be substituted for W1 in applications in which W1 might crack during heat treating.

The punch material can be the same as the die material, except that O1 should be substituted for W1 in applications in which W1 might crack during quenching.

The coining illustrated in the sketch accompanying Table 2 is typical of the coining stage for articles stamped from strip material through progressive forming operations employing die and punch inserts for each stage. Frequently, the inserts are near, or even below, the minimum size that provides the amount of die stock required by good practice. Dies often cannot be any larger or they will not fit in the overall space available, as shown in the sketch in Table 2. In such instances, hot-work steels give better life than do W1, O1, A2, or S2. The separate pieces of the punch body and pilot in the tooling setup illustrated in Table 2 might be made of H12, at 49 to 52 HRC--a compromise between lower hardnesses that result in scoring deterioration and higher hardnesses that lead to failure by splitting. Scoring of the pilot part of the punch is best prevented by hard chromium plating 0.008 to 0.01 mm (0.0003 to 0.0004 in.) thick that has been baked at least 3 h at 150 to 200 °C (300 to 400 °F) to minimize hydrogen embrittlement.

In the coining die, type H12 hot-work tool steel at 45 to 48 HRC would probably be more resistant to splitting stresses than any of the cold-coining die steels. For the kickout pin, an L6 tool steel at a hardness of 40 to 45 HRC is recommended.

Tool steels H11, H12, H13, H20, and H21 at or near their full hardness of 50 to 54 HRC often perform well in coining dies having circular grooves, beads, thin sections, or any configuration that demands improved resistance to breakage and that can tolerate some sacrifice of wear resistance.

Working Hardnesses

The normal working hardnesses of the tool steels listed in Tables 1 and 2 are:

W1	**59-61 HRC**
O1	**58-60 HRC**
A2	**56-58 HRC**
D2	**56-58 HRC**

D2 might be used at 60 to 62 HRC for coining small aluminum parts.

Other Die Materials

Powder Metallurgy (P/M) Steels. The application of hot isostatic processing to powder metallurgy (P/M) production of high-speed steels and special high-alloy steels has expanded the range of tool steel grades available for long-run coining dies. Dramatic increases in toughness and grindability have been achieved. Type M4 is an excellent example. When made by P/M processing, M4 has approximately twice the toughness and two to three times the grindability of conventionally processed M4. Consequently, P/M M4 heat treated to 63 to 64 HRC has better toughness, wear resistance, and compressive strength than conventionally processed D2 at 62 HRC.

Cemented carbides are occasionally used to make coining dies, but generally only for light coining of small pieces in very large production quantities. The successful application of cemented carbides for this service depends to a great extent on the design of the die (or die insert), and to an even greater extent on the design of the hardened tool steel supporting and backup members that surround the carbide dies or inserts. It is most important that the supporting and backup members counteract any tensile stresses imposed on the carbide by the coining operation and that they ensure minimum movement of the die parts.

For light-load applications with minimal shock or impact loading, cemented tungsten carbide containing at least 13% cobalt is used. For applications involving greater shock loading, higher cobalt contents (up to 25%) are required.

Coinability of Metals

Limits to coining are established mainly by the unit loads that the coining dies will withstand in compression before deforming. Deformation of the dies results in dimensions that are out of tolerance in the work-piece as well as premature die failure.

In coining, deformation of the work metal is accomplished largely in a compression strain cycle, which leads to a progressive increase in compression flow strength as deformation progresses. This deformation cycle results in a product that has good bearing properties and wear resistance in service, but in the coining operation it can raise the yield strength to a level that approaches the maximum permissible die load, and the coining action stops.

Deformation strengthens the workpiece. It also increases the area of contact between the die and workpiece. As this contact area increases, radial displacement of the metal becomes increasingly difficult. Significant radial displacement is practical only for relatively soft metals such as sterling silver.

In general, if significant metal movement is required, this should be effected before coining by processes such as rolling or machining. To allow preliminary deformation to take place readily, the metal being coined should be soft and should have a low rate of strain hardening. If a metal lacks these characteristics, it can still be coined if first softened by annealing.

Steels and Irons. Steels that are most easily coined include carbon and alloy grades with carbon content up to about 0.30%. Coinability decreases as carbon or alloy content increases. Steels with carbon content higher than about 0.30% are not often coined, because they are likely to crack. Leaded steels usually coin as well as their nonleaded counterparts. However, other free-machining grades, such as those containing substantial amounts of sulfur, are not recommended for coining because they are susceptible to cracking. When steels are annealed for coining, full annealing is recommended. Process annealing is likely to result in excessive grain growth, which impairs the coined finish. A grain size no coarser than ASTM No. 6 is recommended.

Malleable iron castings are frequently sized by coining. The amount of coining that is practical mainly depends on the hardness.

Stainless steels of types 301, 302, 304, 305, 410, and 430 are those generally preferred for coining. Free-machining type 303Se (selenium-bearing) is sometimes coined.

For tableware, types 301 and 430 have been extensively used in coining of spoons and forks. Type 302 has also been used for such items. Type 305 coins well, but is not widely used because the stock costs more than types 301 and 302.

Stainless steels are relatively hard to coin and are consequently preferred in the soft annealed condition, in the hardness range of 75 to 85 HRB. For type 301 or similar austenitic stainless steels, the variation in nickel content permitted by the composition specifications significantly influences the strain-hardening characteristics of the steel. The low-nickel compositions work harden more than do the high-nickel compositions. For example, in low-nickel and high-nickel lots of type 301 stainless steel, the hardnesses after graded rolling to form a teaspoon bowl were, respectively, 45 and 40 HRC. Harder metal leads to shortened life of the blanking die.

The surface roughness of a well-finished piece of coined stainless steel is about 0.02 to 0.1 μm (1 to 4 μin.); this must be developed in the coining operation, because no major finishing can be done after coining without damage to design details. For functional parts, in which the item is coined only for sizing, surface finish may be less important. In general, however, the surface of the blank must be free from seams, pits, or scratches.

Copper, silver, gold, and their alloys have excellent coinability and are widely used in coin and medallion manufacture. These metals were the first to be minted, and the process of coining developed while working them.

The pure metals are sufficiently soft and coinable to allow extreme deformation in coining, but even after such deformation they are too soft to wear well. As a consequence, important coining metals are prepared by alloying; thus, a relatively wide range of hardness is obtainable.

Composite metals are being coined, principally in the minting of coins. Pressures for coining composites are slightly modified, in accordance with the bulk properties of the metal laminates used, but otherwise the coining operation is unaffected.

Coinability ratings of metals and alloys are difficult to establish on a quantitative basis, although the conditions under which a ductile metal will not coin can be stated in terms of the compressive loads that the die system can exert on the workpiece.

For simple die contours, coining loads can be determined readily, but for complex, incised die contours, coining behavior is a function of both the strength and deformation characteristics of the metal. The relations are so complex that stress calculations alone are not meaningful, and decorative items are coined in sequences that are established largely by experience. In addition, the coinability of a metal is frequently established by the difficulty encountered in preparing the blank for coining. Therefore, it is evident that a number of somewhat arbitrary factors enter into a determination of the coinability of a possible series of metals for a given item. This is especially true for tableware, which is required to be both decorative and useful.

Production Practice

Although coining operations are done as a part of many metalworking processes, by convention the operations narrowly designated as coining processes are of fairly limited scope. The range of coining processes is illustrated by the following examples. In these examples, coining processes fall into two broad categories. In the first category, the objective is the reproduction of ornate detail with a prescribed surface finish. In the second category, the objective is the close size control of an element, again with a prescribed surface finish.

Tableware. Most tableware is coined in single-station dies after extensive preparation of blanks. Each coined item must bear a reproduced ornate design and a polished finish.

Table knives may be made with flat or graded-thickness blades and solid or hollow handles. Flat blades are made by contour blanking followed by coining to develop the cutting edge and a desired surface finish. These blades are then soldered into handles. A stainless steel blade will be blanked, rolled to a graded thickness, outline blanked in one or more stages, and then coined. Type 410 stainless steel hardens to a point that it will not move in the coining operation. Therefore, blades made from type 410 stainless steel are usually heated to permit successful coining.

Sheet metal blanks for hollow handles are manually fed to a coining die mounted in a drop hammer. The blank is coined into an ornamented and polished knife half-handle, and then trimmed. Matched half-handles are soldered together, and the blade is soldered or cemented to the handle, as in the following example.

Example 1: Production of a Nickel Silver Knife Handle by Forming and Coining in a Drop Hammer.

Figure 1 shows the sequence of shapes in the production of a hollow handle for a table knife formed and coined in a 410 kg (900 lb) pneumatic drop hammer. The work metal was 0.81 mm (0.032 in.) thick copper alloy C75700 (nickel silver, 65-12) annealed to a hardness of 35 to 45 HRB; blank size was 25 by 230 mm (1 by 9 in.).

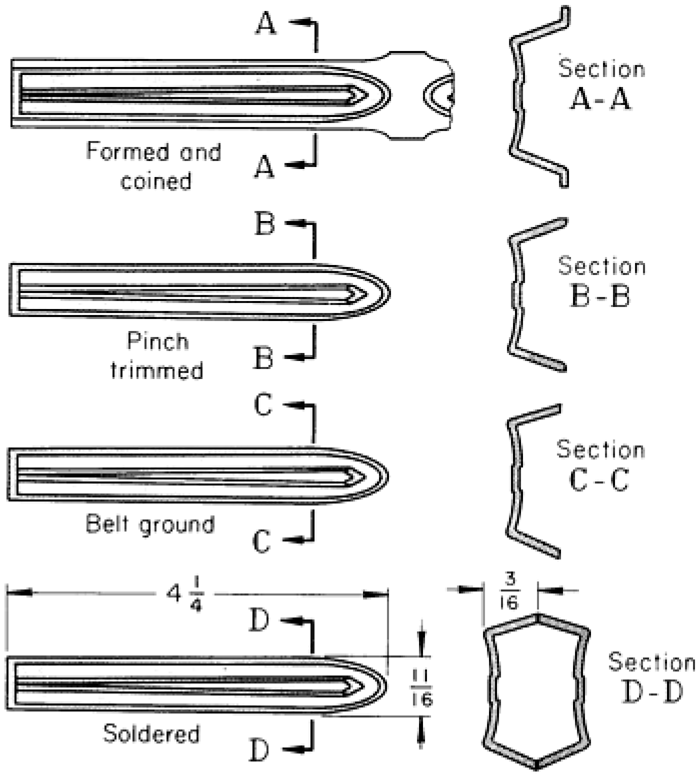


Fig. 1 Production of a hollow copper alloy C75700 knife handle by forming and coining. Dimensions given in inches.

Two workpieces were formed and coined simultaneously from one blank, in two blows of the drop hammer. The two-cavity die permitted easy loading and unloading of parts and also provided symmetry to prevent shifting of the punch. A volatile, fatty oil-base lubricant was applied to the blank by rollers.

The formed and coined halves were separated by slitting with a rotating cutter made of T1 tool steel, and the flange was removed in a pinch-trim operation. After belt grinding to deburr and provide a smooth, flat surface, the half handles were fluxed along the edges and soldered together. The soldered handles were then pickled, washed, and finished by a light emery on the soldered seams, and then were silver plated. The handle and blade were assembled and finish buffed.

Coins and medallions are produced by closed-die coining, in which a prepared blank is compressed between the coining dies while it is retained and positioned between the dies by a ring or collar. The volume of metal in the workpiece is equal to the volume of the die space when the die is closed. The volume of metal cannot exceed the closed-die space without developing excessive loads that may break the die and press. The simplest means of ensuring volume control in a coin blank is by carefully controlling the weight, which is easily measured and converted to volume.

In general, coins are needed in large quantities (about 300,000 before die dressing). To facilitate production and minimize die wear, the detail incorporated into the coin design is in low relief. The coin should have good wear resistance, which is achieved by the compressive working of the metal during coining. Wear of the coin face is prevented by raising the edge of the coin, which is usually serrated to have a so-called milled edge. This edge detail is machined into the retaining ring and is transferred to the expanding workpiece during coining. A typical procedure for coin manufacture is as follows:

- Coin disks are blanked from sheet of prescribed thickness and surface finish
- The disks are barrel tumbled to deburr, to develop a suitable surface finish, and to control weight
- The disks are edge rolled
- The disks are fed, one at a time, to the coining station for coining
- The coins are ejected from the retaining ring. This may be done by movement of the upper or lower die rather than by use of a conventional ejector

The steps employed to manufacture coins may also be used for medallions, with some added steps. Usually the processing of medallions does not require edging operations, but if the design details are in high relief, the full development of details may require restriking. Coined blanks are usually annealed before restriking. The blank must be reinserted into the coining dies in its initial position and then restruck. The use of this method for the manufacture of a medallion is described in the following example.

Example 2: Coining of Sterling Silver Alloy Medallions.

Medallions made from sterling silver alloy (92.5Ag-7.5Cu) and weighing 28 g (1 oz) (±1%) were made by coining, using the die setup illustrated in Fig. 2. Disks were blanked from strip and barrel finished. Following the first coining operation, the workpiece was annealed at 690 °C (1275 °F), repositioned in the die, and restruck. The single-station tooling consisted of the upper and lower incised O1 tool steel dies (60 HRC) and a retaining ring. After coining without lubrication, the medallion was manually removed from the retaining ring, because of the low production requirements (48 pieces per hour). Coining was done in a 3.6 MN (400 tonf) knuckle-type mechanical press.

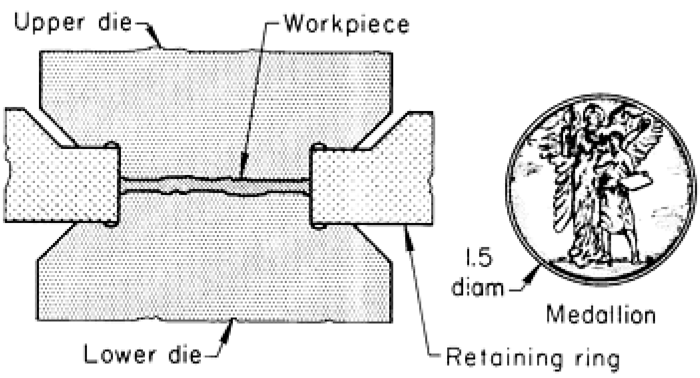


Fig. 2 Die setup used to produce sterling silver medallions by coining and restriking. Dimensions given in inches.

Minute parts are frequently produced in volume by coining in high-speed presses. For such operations, it is difficult to obtain commercial flat stock to the tolerances required, so it is common practice to prepare strip by rolling wire of the required material on precision rolls. The strip thus prepared is coiled and fed to the coining die as needed.

Also, in the manufacture of small, precise parts, the transfer of the workpiece into and out of the coining station is an important operation. To accomplish this, progressive-die tooling is used. The manufacture of a metal interlocking-fastener element can be done as described in the following example. In this example, strip was of a copper alloy; however, aluminum alloy has also been used for the same application.

Example 3: Coining Interlocking-Fastener Elements in a Progressive Die.

The interlocking-fastener element shown in Fig. 3 was manufactured from a precision-rolled, lubricated, flat strip of copper alloy C22600 (jewelry bronze; Cu-12.5Zn) 4.57 mm (0.180 in.) wide.

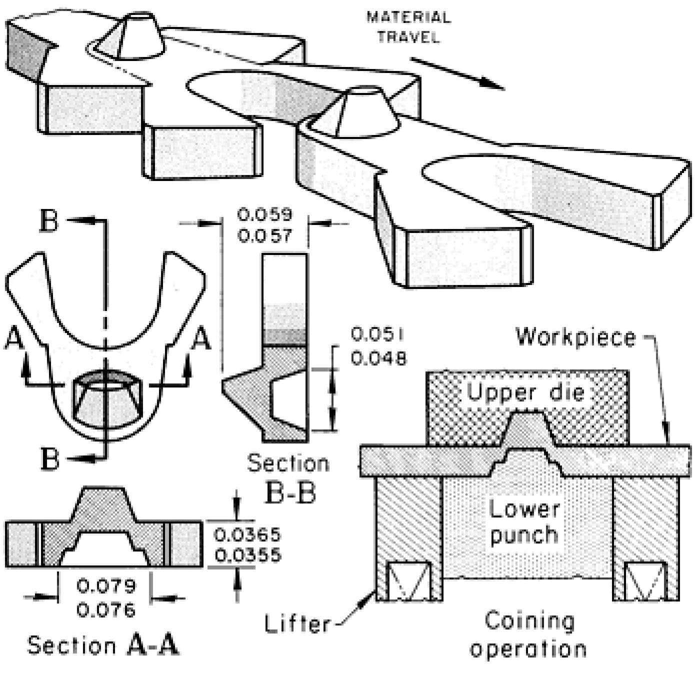


Fig. 3 Copper alloy C22600 interlocking-fastener element produced by coining and notching in a progressive die. Dimensions given in inches.

A special high-speed eccentric-shaft mechanical press with a 4.8 MM ($\frac{3}{16}$ in.) stroke was used. Tooling consisted of a D2 steel progressive die (59 to 61 HRC) that had edge-notching and coining stations. A ratchet-type roll feed was used. The coining portion of the die consisted of an upper die and a lower punch, with a spring-loaded stock lifter. The element was made at a production rate of 120,000 pieces per hour by notching, coining, and blanking, and then was attached to a tape.

Recesses, or mounting and locating features, are coined into high-production parts in a variety of products. Countersinks for screw heads and offsets for mating parts are regularly produced by coining. Often, one piece will have several mounting or assembly details coined into its face, as in the following example.

Example 4: Assembly and Mounting Details Coined Into a Mounting Plate.

The mounting recess for an oval post and the countersink for locating the end of a spring were coined into a mounting plate that was part of an automobile door lock (Fig. 4). The oval was coined to a depth of 1.27 mm (0.050 in.) in the third station of a six-station progressive die.

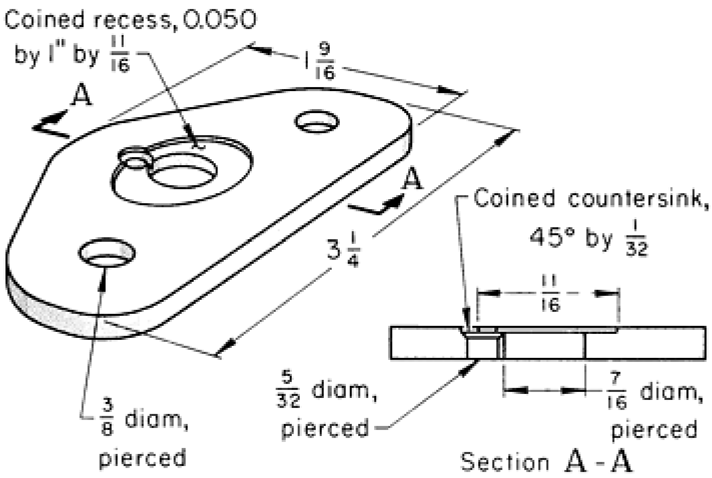


Fig. 4 1010 steel mounting plate with assembly and mounting features coined into its face. Dimensions given in inches.

The plate, as shown in Fig. 4, was made of 1010 hot-rolled steel 4.75 mm (0.187 in.) thick. The first station of the progressive die pierced the two end holes, which were then used as pilot holes for the other stations.

The location of these two holes took into consideration the growth in length of the part during coining. The second station pierced the center hole and the hole for the spring. The recess and the countersink were coined in the third station; the fourth station repierced the center hole. The plate was flattened in the fifth station and blanked in the sixth station. Later, the two end holes were countersunk, and the oval post was assembled to the oval recess. Production rate was 7500 plates per hour; annual production was five million pieces.

The dies were made of air-hardening and oil-hardening tool steels and had a life of 250,000 pieces before reconditioning was required. The piercing punch for the small hole had a life of about 50,000 pieces and could be changed without removing the die from the press.

Roll coining may be used when large numbers of very small items are to be produced and when the coining die is a repetitive single-station die that can be placed on a small roll. This method of coining is an advantage when coining parts in a strip, because the roll serves as both the feed control mechanism and the coining station. This procedure eliminates problems that develop in handling a continuous strip in a press. In press coining, the strip must be brought to a full stop during a prescribed portion of the press stroke.

Roll coining has been used for producing small parts to close dimensional tolerances. In the following example, multiple dies on rolls, together with the method of stock feeding used with roll coining dies, gave rates of production that were unattainable in presses.

Example 5: Roll Coining of Small Interlocking-Fastener Elements From Round Wire.

Copper alloy C22600 (jewelry bronze; Cu-12.5Zn) wire was fed into coining rolls to form elements of an interlocking-fastener strip (Fig. 5).

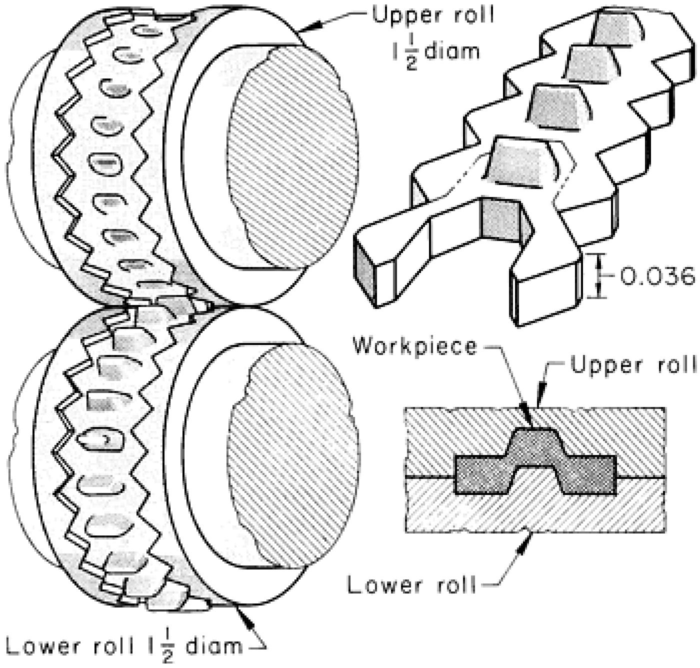


Fig. 5 Copper alloy C22600 interlocking-fastener element produced on coining rolls. Dimensions given in inches.

The rolls illustrated in Fig. 5 were geared together so that the male and female forms hubbed into the roll peripheries were accurately matched. Roll peripheries were a whole-number multiple of the lengths of the article coined. Diameters were kept as small as possible to minimize the expense of replacement of the rolls if premature failure occurred. The rolls enclosed a coining space nominally equal in cross section to that of the wire fed into them. This wire was forged and coined to fill the section presented in the roll space, to give the configuration shown in Fig. 5.

Sizing to close dimensional tolerances on several nonparallel surfaces can be readily achieved in the manufacture of small parts, such as the interlocking-fastener elements discussed in Examples 3 and 5. For large workpieces, ingenuity may be required to develop a coining process for sizing--ingenuity in the design of tooling to minimize the effect of distortion in the press, and ingenuity in the preparation of the workpiece to ensure a minimum of metal flow during coining.

For the flange-sizing operation in the following example, no surface finish requirement was specified because of the conditions under which the surfaces of the workpiece and the dies made contact. However, the finish of the surfaces coined was refined to 1 to 1.1 μm (40 to 45 μin.) from the typical shot-blasted finish of 9 to 10 μm (350 to 400 μin.).

The coining die setup described in the next example was designed to ensure control of thickness and parallelism.

Example 6: Sizing an Automobile Front-Wheel Hub by Coining.

The die setup illustrated in Fig. 6 was used to coin flanges of forged 1030 or 1130 steel front-wheel hubs using single-station tooling in an 18 MN (2000 tonf) knuckle-type mechanical press with a six-station feed table. Tolerances on the coined flange were: thickness within 0.127 mm (±0.005 in.) and parallelism within 0.10 mm (0.004 in.). To maintain these tolerances, the as-forged flange thickness could be not more than 1.40 mm (0.055 in.) greater than the coined dimension and had to be parallel within (0.43 mm) 0.017 in. The flange was coined to tolerances by centering the forged hub on the lower ring-shaped die. The top die, with a cavity depth equal to the specified flange thickness, was positioned over the wheel hub, and the coining load was applied. The top die was brought into contact with the lower die and, because the bearing surfaces of the upper and lower dies were parallel, the required parallelism was developed in the flange surfaces as the thickness was brought to the specified dimension.

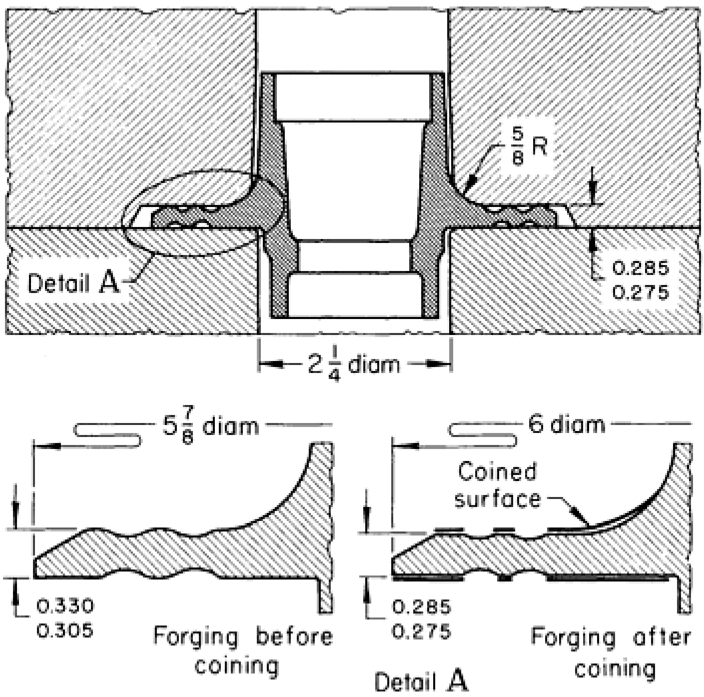


Fig. 6 Coining the flange on a forged 1030 or 1130 steel wheel hub to final size, at less cost than sizing the flange by machining. Dimensions given in inches.

Coining Versus Machining. In general, sizing by coining may be desirable when parallel surfaces are required in a workpiece, even if the workpiece is so large that maximum press capacities are required. However, sizing operations on nonparallel surfaces are feasible only if the work metal can be moved by the sizing-die surface without distorting the dies. Such metal movement is possible if the width of the metal being coined is about the same as the thickness. (For very soft metals, this movement is usually possible to a pronounced degree, but a sizing operation is of little significance for such materials.) In general, gross movement of the metal should not be required, and machining or forging should be used to bring the workpiece to approximate dimensions before sizing is attempted. When this is done, sizing by coining can produce workpieces having dimensional tolerances that are acceptable in good machining practice, often with significant savings in material and labor costs.

Coining

Processing Problems and Solutions

Establishment of a suitable blank preparation sequence is required to give the desired results from coining operations. Blank preparation may simply consist of annealing the blanks before or after coining, or both, followed by restriking to permit transfer of die detail to the workpiece.

Faulty coining may occur because die surfaces are not clean. Directing a jet of air across the die to remove loose dirt can eliminate some causes of incomplete detail in coined parts. Regular and frequent inspection of finished parts and dies is necessary to ensure that dies have not picked up stock or lubricant that can damage the surfaces of subsequently coined pieces.

Another frequent source of trouble in coining is faulty die alignment. Coining dies must be aligned to the degree of precision expected in the coined item.

Excessive tool breakage from die overloading is a common problem in coining, and it is difficult to suggest steps to eliminate it. In the manufacture of tableware, tool breakage is accepted, because replacement of tools is inexpensive and inspection procedures are adequate to prevent the buildup of large numbers of rejected items. When this approach to the problem is undesirable, the alternative is to establish the nature and magnitude of the overload and to relieve it by changing die design or process variables.

Control of Dimensions, Finish, and Weight

The quality of coined items is judged by various criteria, depending on end use. For decorative items, surface finish and transfer of detail are usually the primary objectives. For functional items, such as machinery components, dimensional accuracy and consistency are usually the most important factors.

Weight in coining sterling silver or other precious metals is important, mainly for economic reasons, and must be controlled. Controlling the weight of a blank is also a convenient way to control the volume of metal in a blank.

Dimensional Tolerances. Sizing is used to maintain dimensions to close tolerances and to refine the surface finish. In the following example, coining was used to hold the flange thickness to a total variation of 0.25 mm (0.010 in.). The same coining operation also controlled parallelism between the same two surfaces (see Example 6).

Coining was used to form the steel cam described in the example. To hold the dimensions to the specified tolerance, the part was annealed and coined again.

Example 7: Intermediate Annealing Before Coining to Dimension.

The breaker cam shown in Fig. 7 was made of 3.25/3.18 mm (0.128/0.125 in.) thick 1010 cold-rolled special-killed steel. Strips 86 mm (3⅜ in.) wide and 2.4 m (96 in.) long with a maximum hardness of 65 HRB and a No. 2 bright finish were purchased.

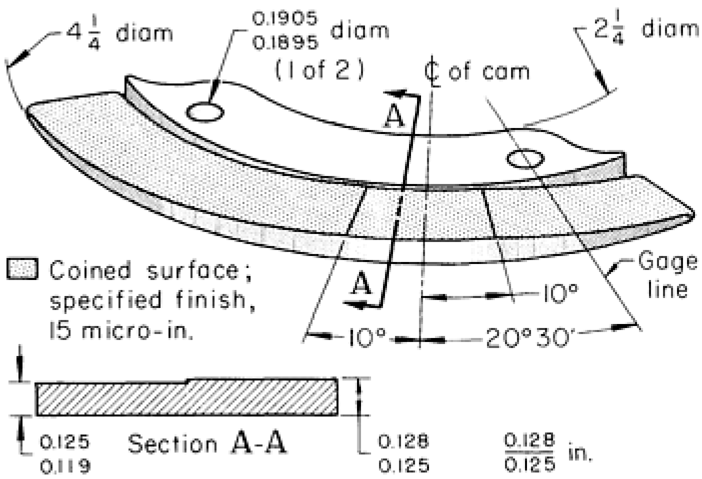


Fig. 7 Cold-rolled 1010 steel breaker cam that was given an intermediate anneal before being coined to final dimensions. Dimensions given in inches.

Cold working the cam surface by coining made it necessary to anneal the parts before flattening and restriking. The part contour and dimensions were extremely difficult to maintain. The surface finish was 0.4 μm (15 μin.). A pack anneal was used to minimize distortion and scale. The sequence of operations to make the part was:

- Shear strips to 1.2 m (48 in.) lengths
- Coin cam contour, pierce, and blank in a progressive die. High point on the cam was 3.12 to 3.15 mm (0.123 to 0.124 in.) thick
- Pack anneal at 900 to 925 °C (1650 to 1700 °F). The part had to be free of heat checks and scale
- Restrike to flatten and coin to 3.09 ± 0.076 mm (0.122 ± 0.003 in.) at the high point. Gage point (at 20.5° on open side) was 0.292 ± 0.0127 mm (0.0115 ± 0.0005 in.) below the high point on the cam

- Ream holes to 4.81 to 4.84 mm (0.1895 to 0.1905 in.) diam
- Case harden 0.020 mm (0.0008 in.) deep for wear-resistant surface (73 to 77 HR15-N)
- Wash and clean after case hardening
- Inspect dimensions and flatness

The cam was made in four lots of 2500 for a total of 10,000 per year. A 1.8 MN (200 tonf) mechanical press operating at 18 strokes per minute was used for the coining operations. The lubricant was an equal-parts mixture of mineral oil and an extreme-pressure chlorinated oil.

The die was made of D2 tool steel and had a life of 50,000 pieces between sharpenings for the cutting elements. The coining dies required more frequent attention because of the tolerance and finish requirements.

Other methods of making the part were machining and powder metallurgy. Parts machined to the required tolerances cost four times as much as coined parts. A powder metallurgy part did not meet the wear-resistance requirements.

Surface Finish. Tableware, coins, medallions, and many other coined items require an excellent surface finish. To achieve this, the dies must have an excellent surface, and the finish on the blank also must be good. Dies are carefully matched, tooled, stoned, and polished by hand. Polishing is done by wood sticks, lard oil, and various grits of emery. Typical surface finishing of sterling silver, before and after coining, when using the above practice, is illustrated in the following example.

Example 8: Effect of Die Finish on Finish of Coined Sterling Silver Fork.

Seven surface finish readings taken in the fork portion of uncoined sterling silver blanks showed an average surface roughness of 0.28 μm (11 μin.) (upper sketch of Fig. 8). When coining with dies that were not hand polished, the average finish in the fork section was reduced to 0.2 μm (9 μin.).

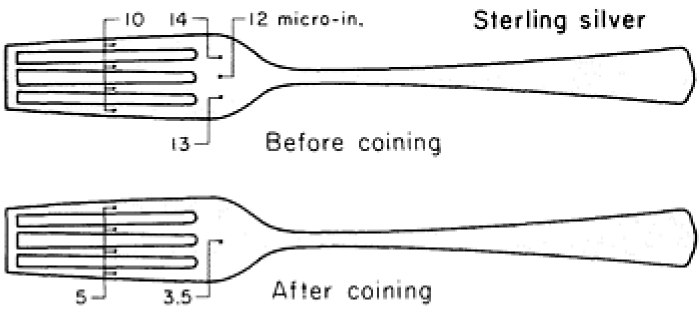


Fig. 8 Surface finish (in micro-inches) of a sterling silver fork before and after coining with hand-stoned and polished dies.

Dies were hand stoned and polished before a production run of 4000 forks. Workpiece surface finish improved to an average of 0.1 μm (5 μin.), as shown in Fig. 8 (lower sketch). To maintain this finish, hand polishing of the dies after each 1000 piece run was required. Coining was done in a 540 kg (1200 lb) air lift gravity drop hammer using a drop height of 610 mm (24 in.). Production rate was 500 pieces per hour.

Weight of the blanks for items coined from precious metals is often specified to close tolerances. These metals are soft and can be coined to intricate detail. However, the volume of metal placed in the die must be carefully controlled so that the metal can completely fill the design but not overload the die and press. A convenient method of controlling the volume of metal in a blank is to specify the weight, thickness, width, and length of the blank to close tolerances.

Not only is sterling silver flatware inspected for perfection of design detail and surface finish, but the blank is periodically checked for weight, which usually is held to ±1%.

Powder Forging

W. Brian James, Michael J. McDermott, and Robert A. Powell, Hoeganaes Corporation

Introduction

POWDER FORGING is a process in which unsintered, presintered, or sintered powder metal preforms are hot formed in confined dies. The process is sometimes called P/M (powder metallurgy) forging, P/M hot forming, or is simply referred to by the acronym P/F. When the preform has been sintered, the process is often referred to as "sinter forging."

Powder forging is a natural extension of the conventional press and sinter (P/M) process, which has long been recognized as an effective technology for producing a great variety of parts to net or near-net shape. Figure 1 illustrates the powder forging process. In essence, a porous preform is densified by hot forging with a single blow. Forging is carried out in heated, totally enclosed dies, and virtually no flash is generated. This contrasts with the forging of wrought steels, in which multiple blows are often necessary to form a forging from bar stock and considerable material is wasted in the form of flash.

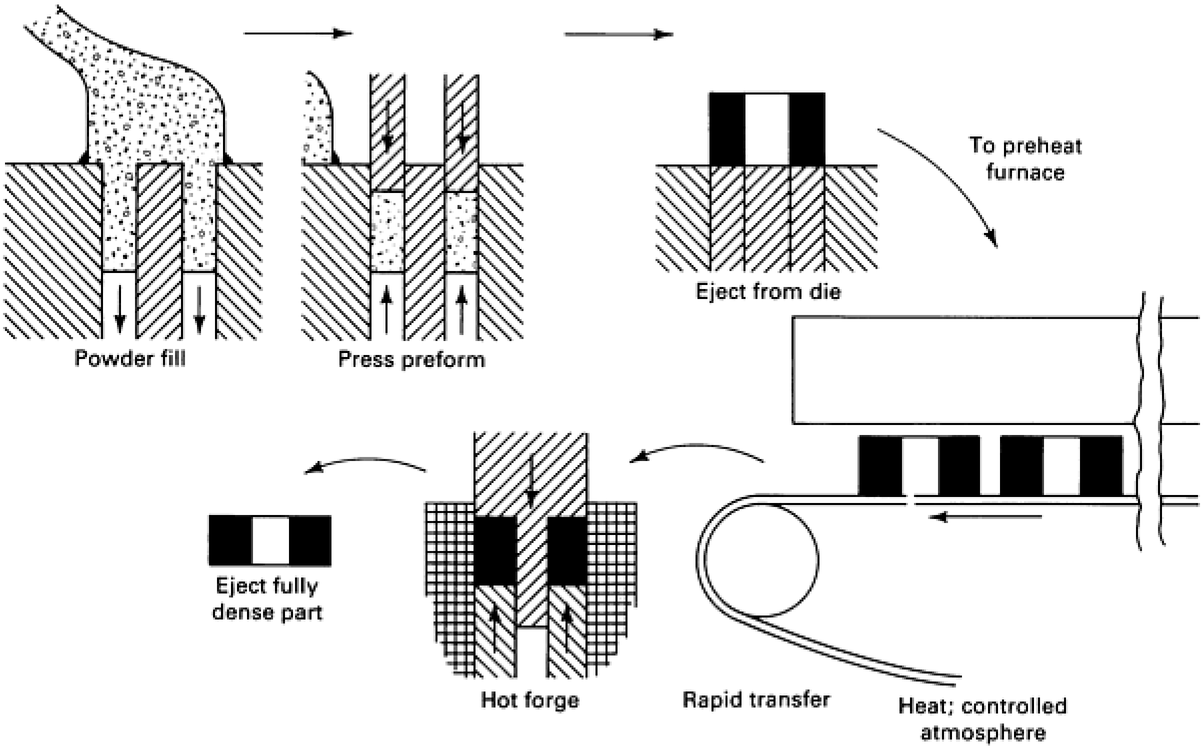


Fig. 1 The powder forging process.

The shape, quantity, and distribution of porosity in P/M and P/F parts strongly influence their mechanical performance. The effect of density on the mechanical properties of as-sintered iron and powder forged low-alloy steel is illustrated in Fig. 2. Powder forging, therefore, is a deformation processing technology aimed at increasing the density of P/M parts and thus their performance characteristics.

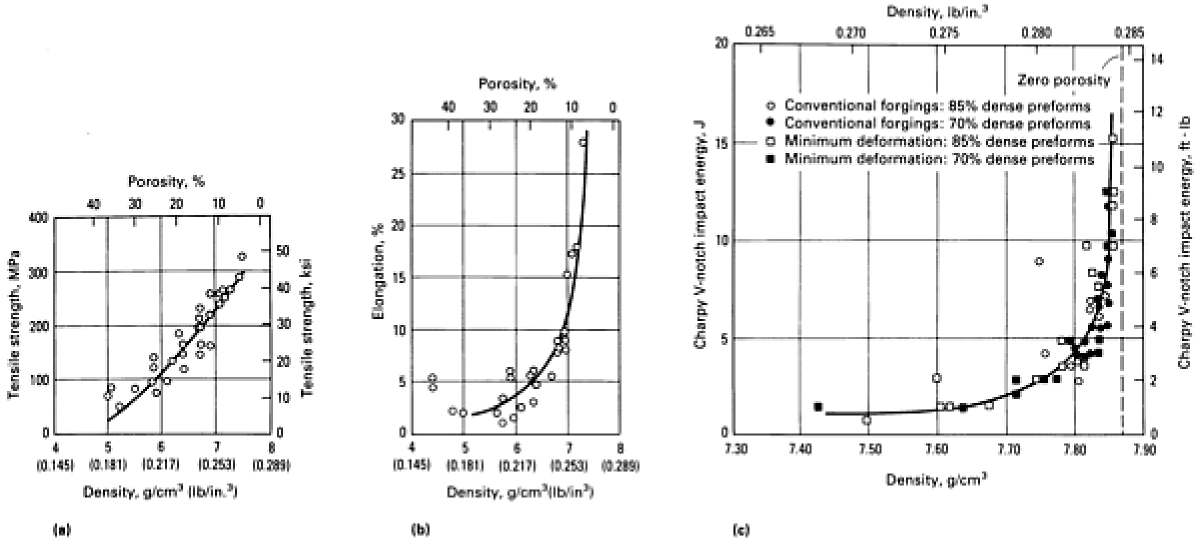


Fig. 2 Effect of density on mechanical properties. (a) and (b) As-sintered iron. Source: Ref 1. (c) Powder forged low-alloy steel. Source: Ref 2.

There are two basic forms of powder forging:

- Hot upsetting, in which the preform experiences a significant amount of lateral material flow
- Hot re-pressing, in which material flow during densification is mainly in the direction of pressing. This form of densification is sometimes referred to as hot restriking, or hot coining

These two deformation modes and the stress conditions they impose on pores are illustrated in Fig. 3.

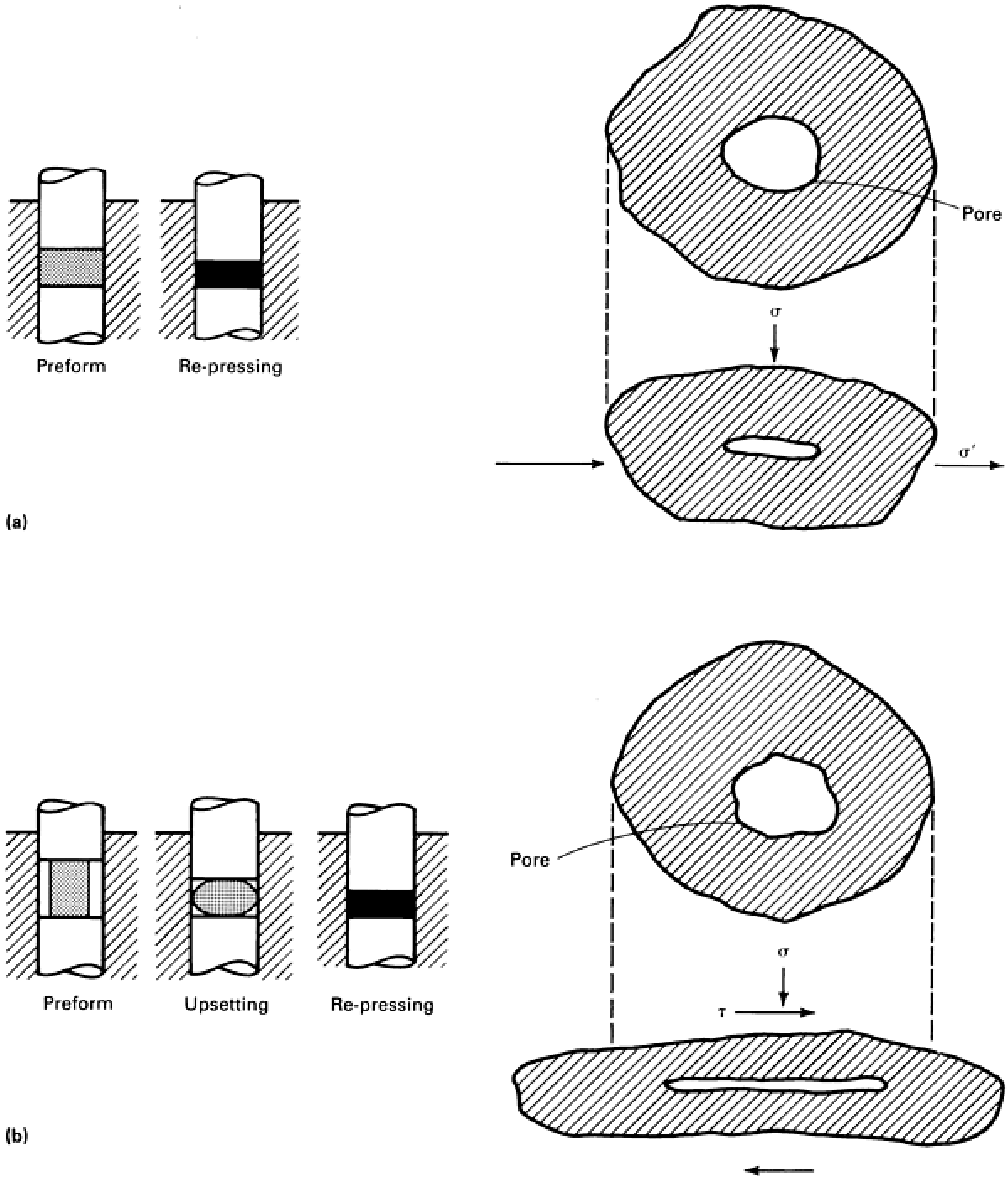


Fig. 3 Forging modes and stress conditions on pores for (a) re-pressing and (b) upsetting. Source: Ref 3.

In hot upset powder forging, the extensive unconstrained lateral flow of material results in a stress state around the pores that is a combination of normal and shear stresses. A spherical pore becomes flattened and elongated in the direction of lateral flow. The sliding motion due to shear stresses breaks up any residual interparticle oxide films and leads to strong metallurgical bonding across collapsed pore interfaces. This enhances dynamic properties such as fracture toughness and fatigue strength.

The stress state during hot re-press powder forging consists of a small difference between vertical and horizontal stresses, which results in very little material movement in the horizontal direction and thus limited lateral flow. As densification proceeds, the stress state approaches a pure hydrostatic condition. A typical pore simply flattens, and the opposite sides of the pore are brought together under pressure. Hot re-press forging requires higher forging pressures than does hot upset forging for comparable densification. The decreased interparticle movement compared with upsetting reduces the tendency to break up any residual interparticle oxide films and may result in lower ductility and toughness.

While powder forged parts are primarily used in automotive applications where they compete with cast and wrought products, parts have also been developed for military and off-road equipment.

The economics of powder forging have been reviewed by a number of authors (Ref 4, 5, 6, 7, 8, 9). Some of the case histories included in the section "Applications of Powder-Forged Parts" in this article also compare the cost of powder forging with that of alternative forming technologies.

The discussion of powder forging in this article is limited to ferrous alloys. Information on the forging of aluminum, nickel-base, and titanium powders is available in the articles "Forging of Aluminum Alloys," "Forging of Nickel-Base Alloys," and "Forging of Titanium Alloys" in this Volume. Detailed information on all aspects of powder metallurgy is available in *Powder Metal Technologies and Applications,* Volume 7 of the *ASM Handbook.*

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Material Considerations

The initial production steps of powder forging (preforming and sintering) are identical to those of the conventional press and sinter P/M process. Certain defined physical characteristics and properties are required in the powders used in these processes. In general, powders are classified by particle shape, particle size, apparent density, flow, chemistry, green strength, and compressibility. More information on testing of powders is available in the Section "Metal Powder Production and Characterization" in *Powder Metal Technologies and Applications*, Volume 7 of the *ASM Handbook.*

Powder Characteristics. Shape, size distribution, apparent density, flow, and composition are important characteristics for both conventional P/M and powder forging processes. The shape of the particles is important in relation to the ability of the particles to interlock when compacted. Irregular particle shapes such as those produced by water atomization are typically used. In P/M parts, surface finish is related to the particle size distribution of the powder. In powder forging, however, the surface finish is directly related to the finish of the forging tools. This being the case, it might be considered possible to use coarser powders for powder forging (Ref 10). Unfortunately, the potential for deeper surface oxide penetration is greater when the proportion of coarser particles is increased. Typical pressing grades are -80 mesh with a median particle size of about 75 μm (0.003 in.). The apparent density and flow are important to maintain fast and accurate die filling. The chemistry affects the final alloy produced as well as the compressibility.

Green strength and compressibility are more critical in P/M than they are in P/F applications. Although there is a need to maintain edge integrity in P/F preforms, there are rarely thin, delicate sections that require high green strength. Because P/F preforms do not require high densities (typically 6.2 to 6.8 g/cm^3, or 0.22 to 0.25 lb/in.^{3}), the compressibility obtainable with prealloyed powders is sufficient. However, carbon is not prealloyed because it has an extremely detrimental effect on compressibility (Fig. 4).

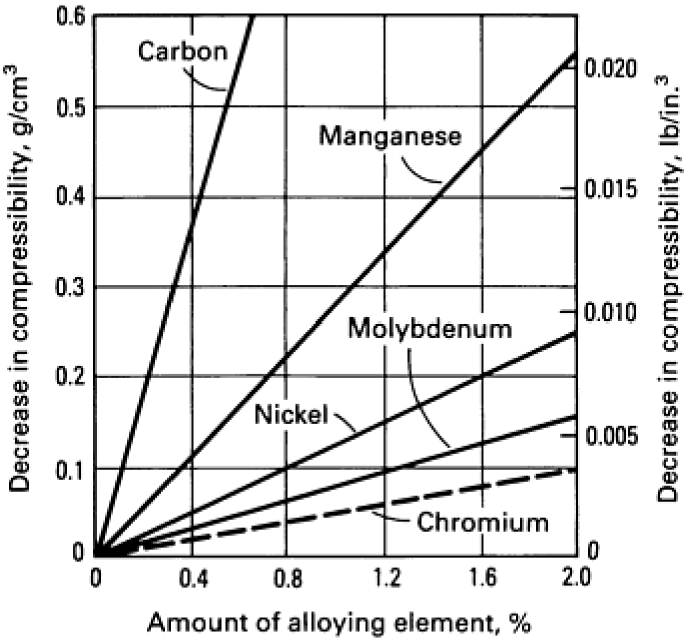

Fig. 4 Effect of alloying elements on the compressibility of iron powder. Source: Ref 10.

Alloy Development. Several investigators have shown that forged conventional elemental powder mixes result in poor mechanical properties, such as fatigue resistance, impact resistance, and ductility (Ref 11, 12). This is almost entirely due to the chemical and metallurgical heterogeneity that exists in materials made by this method. To overcome this, very long diffusion times or higher processing temperatures are required to fully homogenize the material, particularly when elements such as nickel are used. Samples forged from prealloyed powder have also been shown to have better hardenability than samples forged from admixed powders (Ref 13). Fully prealloyed powders have therefore been produced by several manufacturers. Each particle in these powders is uniform in composition, thereby alleviating the necessity for extensive alloy diffusion.

Powder purity and the precise nature and form of impurities are also extremely important. In a conventional powder metal part, virtually all properties are considerably lower than those of equivalent wrought materials. The effect of inclusions is overshadowed by the effect of the porosity. For a powder forging at full density, as in a conventional forging, the dominant effect of residual porosity on properties is replaced by the form and nature of impurity inclusions.

The two principal requirements for powder forged materials are an ability to develop an appropriate hardenability to guarantee strength and to control fatigue performance by microstructural features such as inclusions.

Hardenability. Manganese, chromium, and molybdenum are very efficient promoters of hardenability, whereas nickel is not. In terms of their basic cost, nickel and molybdenum are relatively expensive alloying additions compared with chromium and manganese. On this basis, it would appear that chromium/manganese-base alloys would be the most cost-effective materials for powder forging. However, this is not necessarily the case, because these materials are highly susceptible to oxidation during the atomization process. In addition, during subsequent powder processing, high temperatures are required to reduce the oxides of chromium and manganese, and special care must be taken to prevent reoxidation during handling and forging. If the elements become oxidized, they do not contribute to hardenability. Nickel and molybdenum have the advantage that their oxides are reduced at conventional sintering temperatures. Alloy design is therefore a compromise and the majority of atomized prealloyed powders in commercial use are nickel/molybdenum based, with manganese present in limited quantities. The compositions of three commercial powder metallurgy steels are listed below.

Alloy	Composition, wt %[(a)]		
	Mn	**Ni**	Mo
P/F-4600	0.10-0.25	1.75-1.90	**0.50-0.60**
P/F-2000	0.25-0.35	0.40-0.50	**0.55-0.65**

(a) All compositions contain balance of iron.

The higher cost of nickel and molybdenum along with the higher cost of powder compared with conventional wrought materials is often offset by the higher material utilization inherent in the powder forging process.

More recently, P/F parts have been produced from iron powders (0.10 to 0.25% Mn) with copper and/or graphite additions for parts that do not require the heat-treating response or high strength properties achieved through the use of the low-alloy steels. Detailed descriptions of alloy development for powder forging applications have been published previously (Ref 14, 15).

Inclusion Assessment. Because the properties of material powder forged to near full density are strongly influenced by the composition, size distribution, and location of nonmetallic inclusions (Ref 16, 17, 18), a method has been developed for assessing the inclusion content of powders intended for P/F applications (Ref 19, 20, 21, 22). Samples of powders intended for forging applications are re-press powder forged under closely controlled laboratory conditions. The resulting compacts are sectioned and prepared for metallographic examination. The inclusion assessment technique involves the use of automatic image analysis equipment. The automated approach is preferred because it is not sensitive to operator subjectivity and can be used routinely to obtain a wider range of data on a reproducible basis.

In essence, an image analyzer consists of a good-quality metallurgical microscope, a video camera, a display console, a keyboard, a microprocessor, and a printer. The video image is assessed in terms of its gray-level characteristics, black and white being extremes on the available scale. The detection level can also be set to differentiate between oxides and sulfides.

Compared with wrought steels, only a limited amount of material flow is present in powder forged components. Inclusion stringers common to wrought steel are therefore not found in powder forged materials. Figure 5(a) illustrates an inclusion type encountered in powder forged low-alloy steels. The fragmented nature of these inclusions makes size determination by image analysis more complex than would be the case with the solid exogenous inclusion shown in Fig. 5(b). Basic image analysis techniques tend to count the inclusion shown in Fig. 5(a) as numerous small particles rather than as a single larger entity. Amendment of the detected video image is required to classify such inclusions; the method used is discussed in Ref 22.

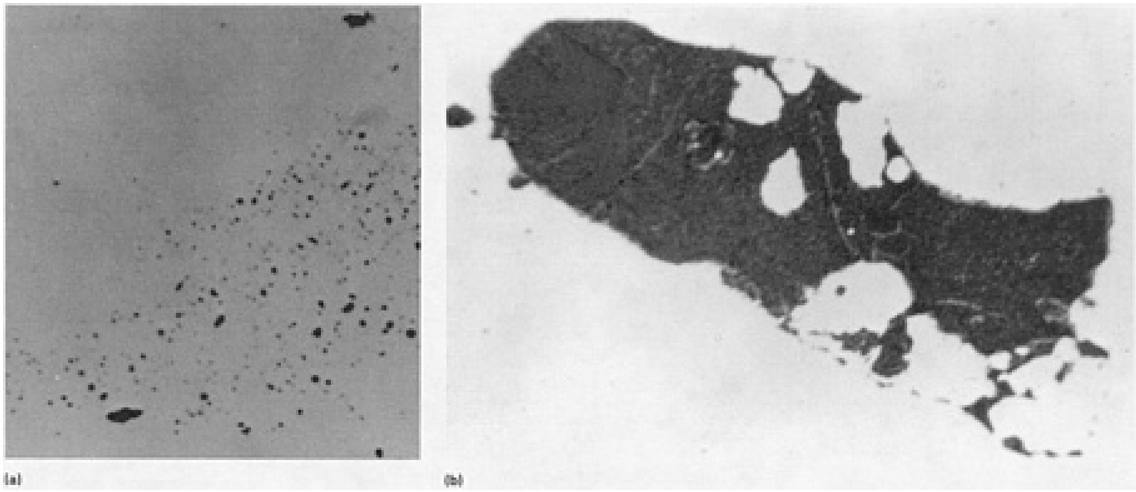

Fig. 5 Two types of inclusions. (a) "Spotty particle" oxide inclusion. 800×. (b) Exogenous slag inclusion. 590×. Source: Ref 21.

Iron Powder Contamination. Water-atomized low-alloy steel powders are generally produced and processed in a plant that also manufactures pure iron powders. In the early days of alloy development when alloy powder production was limited, procedures were developed to minimize cross-contamination of powders. Considerable care is still taken to prevent cross-contamination, and iron powder contamination of low-alloy powders is typically less than 1%. Studies have shown that for "through hardening" applications, up to 3% iron powder contamination has little effect on the strength and ductility of powder forged material (Ref 23, 24).

The compact used for inclusion assessment may also be used to measure the amount of iron powder particles present. The sample is lightly pre-etched with 2% nital. Primary etching is with an aqueous solution of sodium thiosulfate and potassium metabisulfite. This procedure darkens the iron particles and leaves the low-alloy matrix very light (Fig. 6).

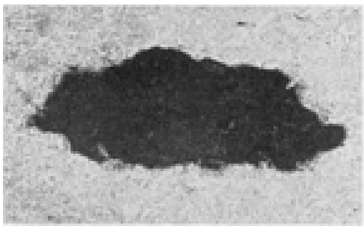

Fig. 6 Iron powder contamination of water-atomized low-alloy steel powder. Source: Ref 21.

The etched samples are viewed on a light microscope at a magnification of 100×. The total number of points of a 252-point grid that intersect iron particles for ten discrete fields is divided by the total number of points in the ten fields (2520) to determine the percentage of iron contamination.

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Powder Forging

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Process Considerations

Development of a viable powder forging system requires consideration of many process parameters. The mechanical, metallurgical, and economic outcomes depend to a large extent on operating conditions, such as temperature, pressure, flow/feed rates, atmospheres, and lubrication systems. Equally important consideration must be given to the types of processing equipment, such as presses, furnaces, dies, and robotics, and to secondary operations, in order to obtain the process conditions that are most efficient. This efficiency is maintained by optimizing the process line layout. Examples of effective equipment layouts for preforming, sintering, reheating, forging, and controlled cooling have been reviewed in the literature (Ref 4, 25). Figure 7 shows a few of the many possible operational layouts. Each of these process stages is reviewed in the following sections.

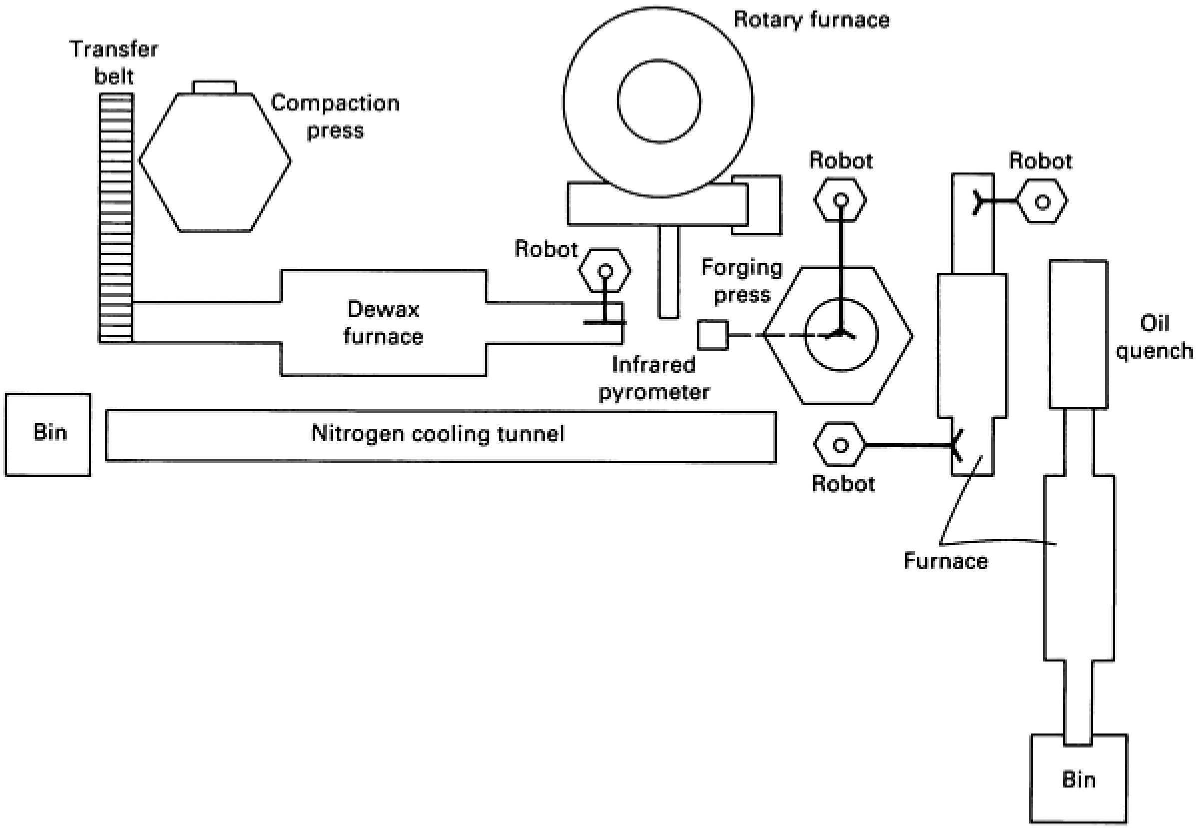


Fig. 7 A powder forging process line. Source: Ref 26.

Preforming. Preforms are manufactured from admixtures of metal powders, lubricants, and graphite. Compaction is predominantly accomplished in conventional P/M presses that use closed dies. In order to avoid the necessity of thermally removing the lubricant, preforms can be compacted without admixed lubricants in an isostatic press. However, even though they produce uniform weight and density distributions, the pressure and rate limitations of high-production isostatic presses (414 MPa, or 60 ksi, pressure and 120 cycles per hour) have severely restricted their commercial use for compacting P/F preforms.

Control of weight distribution within preforms is essential to produce full density and thus maximize performance in the critical regions of the forged component. Excessive weight in any region of the preform may cause overload stresses that could lead to tool breakage.

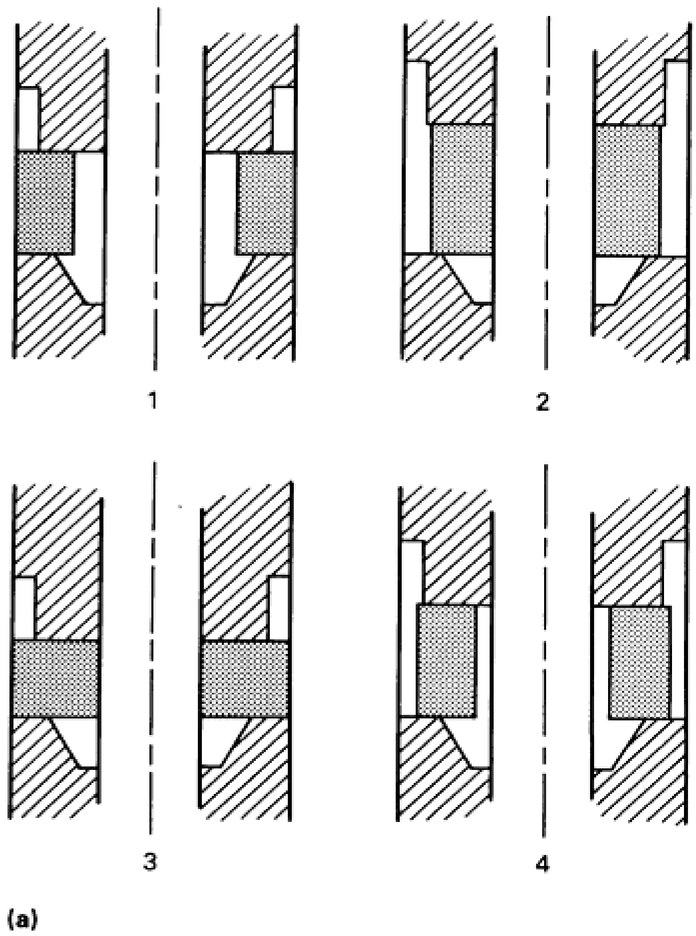
Successful preform designs have been developed by an iterative trial and error procedure, using prior experience to determine the initial shape. More recently, computer-aided design (CAD) has been used for preform design (Ref 27, 28, 29, 30).

Preform design is intimately related to the design and dimensions of the forging tooling, the type of forging press, and the forging process parameters. Among the variables to be considered for the preforming tools are:

- Temperature, that is, preform temperature, die temperature, and, when applicable, core rod temperature
- Ejection temperature of the forged part
- Lubrication conditions--influence on compaction/ejection forces and tooling temperatures
- Transfer time and handling of the preform from the preheat furnace to the forging die cavity

Correct preform design not only entails having the right amount of material in the various regions of the preform but also is concerned with material flow between the regions and prevention of potential fractures and defects.

An example of the effect of preform geometry on forging behavior can be taken from the work of C.L. Downey and H.A. Kuhn (Ref 31). Figure 8(a) shows four possible preforms that could be forged to produce the axisymmetric part having the cup and hub sections shown in Fig. 8(b).



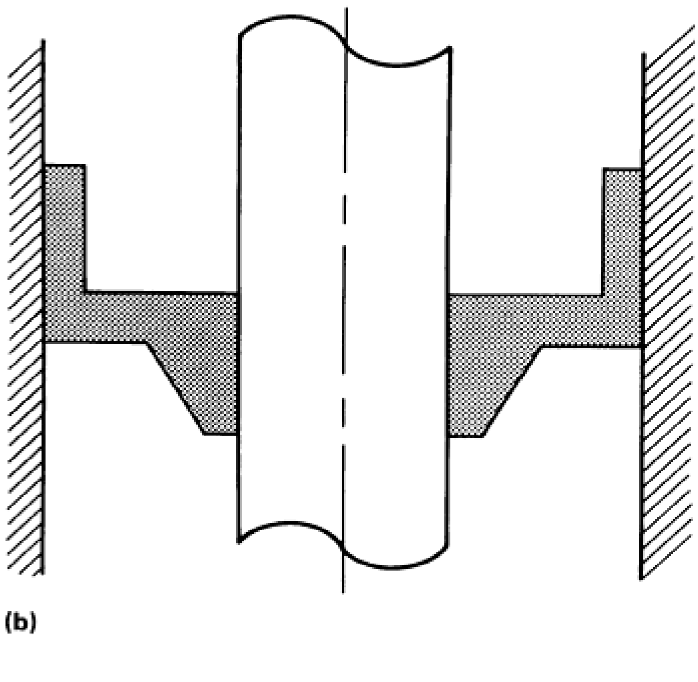


Fig. 8 (a) Possible configurations for the ring preform for forging the part shown in (b). See text for details. (b) Cross section of the part under consideration for powder forging. Source: Ref 31.

Preform geometries 2 and 4 in Fig. 8(a) result in defective forgings due to cracking at the outer rim as metal flows around the upper punch radius. This occurs because the deforming preform is expanding in diameter as the metal flows around the corner, even though there is axial compression to help compensate for the circumferential tension. This type of cracking can be avoided by using a preform that fills the die with no clearance at the outside diameter, as in preforms 1 and 3.

Preform 3 can be rejected because it is similar to hub extrusion, and this may lead to cracking at the top surface of the hub. Allowing some clearance between the bore diameter of the preform and the mandrel eliminates this type of crack.

Preform 1 overcomes these problems. Use of this preform has resulted in defect-free parts, while the expected cracking occurred-with use of the other preforms (Ref 31).

Sintering and Reheating. Preforms may be forged directly from the sintering furnace; sintered, reheated, and forged; or sintered after the forging process. The basic requirements for sintering in a ferrous powder forging system are: lubricant removal, oxide reduction, carbon diffusion, development of particle contacts, and heat for hot densification. Oxide reduction and carbon diffusion are the most important aspects of the sintering operations. For most ferrous powder forging alloys, sintering takes place at about 1120 °C (2050 °F) in a protective reducing atmosphere with a carbon potential to prevent decarburization. The time required for sintering depends upon the number of sintering stages for delubrication, diffusion of carbon, reduction of oxides, and the type of sintering equipment used. Typical P/M sintering has been commonly performed at 1120 °C (2050 °F) for 20 to 30 min; these conditions may be required to help diffuse elements such as copper and nickel. In the prealloyed systems used for powder forging, only the diffusion of carbon is usually required. It has been shown that the time required to diffuse carbon and reduce the oxides is about 3 min at 1120 °C (2050 °F) (Ref 32, 33, 34). This is illustrated in Fig. 9. Increases in temperature will of course reduce the time required for sintering by improving oxide reduction and increasing carbon diffusion. Chromium-manganese steels have been limited in their use because of the higher temperatures required to reduce their oxides and the greater care needed to prevent reoxidation.

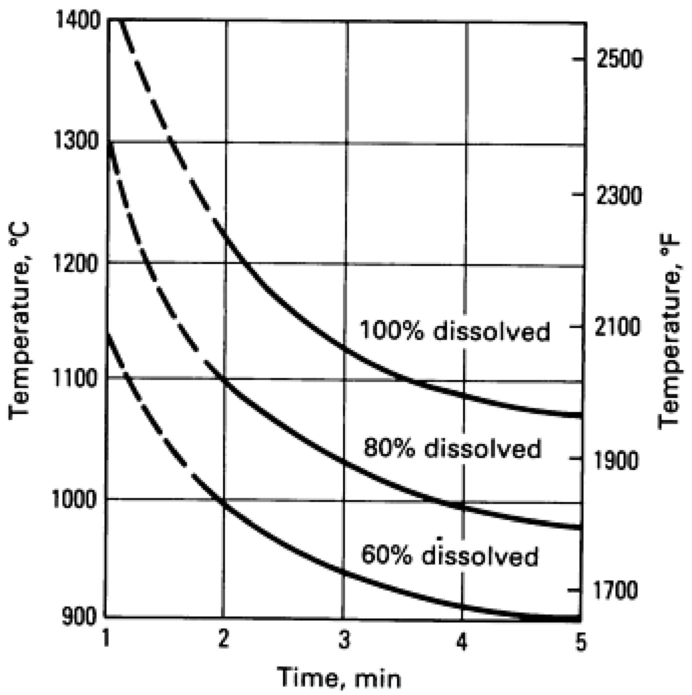


Fig. 9 Carbon dissolution as a function of time and temperature. Data are for an iron-graphite alloy at a density of 6.2 to 6.3 g/cm^3 (0.224 to 0.228 lb/in.^{3}). Source: Ref 34, Ref 8.

Any of the furnaces used for sintering P/M parts, such as vacuum, pusher, belt, rotary hearth, walking beam, roller hearth, and batch/box, may be used for sintering or reheating P/F preforms. Delubrication can be accomplished in any of these types of furnaces or in separate delubrication furnaces before entering the sintering furnace. Typically, belt, rotary hearth, and batch/box furnaces have been used for sintering and reheating preforms. However, the choice of sintering furnace largely depends upon the following conditions:

- Material being forged
- Size and weight of parts
- Forging process route (sinter/reheat versus sinter/forge)
- Forging temperature
- Atmosphere capabilities
- Delubrication capabilities
- Furnace loading capabilities/sintering rate
- Sintering time
- Robotics

The sintered preforms may be forged directly from the sintering furnace, stabilized at lower temperatures and forged, or cooled to room temperature, reheated, and forged. All cooling, temperature stabilization, and reheating must occur under protective atmosphere to prevent oxidation.

Induction furnaces are often used to reheat axisymmetric preforms to the forging temperature because of the short time required to heat the material. Difficulties may be encountered in obtaining uniform heating throughout asymmetric shapes because of the variation in section thickness.

Powder forging involves removing heated preforms from a furnace, usually by robotic manipulators, and locating them in the die cavity for forging at high pressures (690 to 965 MPa, or 100 to 140 ksi). Preforms may be graphite coated to prevent oxidation during reheating and transfer to the forging die. These dies are typically made from hot-work steels such as AISI H13 or H21. Lubrication of the die and punches is usually accomplished by spraying a water-graphite suspension into the cavity (Ref 35, 36, 37).

The forging presses commonly used in conventional forging (Ref 38, 39, 40, 41), including hammers, high-energy-rate forming (HERF) machines, mechanical presses, hydraulic presses, and screw presses, have been evaluated for use in powder forging (Ref 8, 42). The essential characteristics that differentiate presses are: contact time, stroke velocity, available energy and load, stiffness, and guide accuracy. Mechanical crank presses are the most widely used because of their short, fast strokes; short contact times; and guide accuracy. Hydraulic presses have also been used for applications in the 7.7 g/cm^3 (0.28 lb/in.^{3}) density range, and screw presses are starting to be used because of their lower cost and short contact times. More information on forging equipment is available in the articles "Hammers and Presses for Forging" and "Selection of Forging Equipment" in this Volume.

Metal Flow in Powder Forging. Some of the problems encountered in powder forging, and their probable causes, are described in Table 1. These problems are related to the aforementioned sintering and reheating equipment and to the deformation processing described below.

Table 1 Common powder forging problems and their probable causes

Forging problem	Probable causes
Surface oxidation	**Extensive transfer time from furnace**
Surface decarburization	**Overly high forging temperature**
	Entrapped liquid/graphite coating during reheat
	Excessive die lubrication (water)

	Oxidation during sintering or reheating
Surface porosity	**Excessive contact time**
	Low forging temperature
Tool wear	**Low preform temperatures**
	High or low tool temperature
	Excessive contact time
Poor tolerances	**Temperature variations in tools and preforms**
Excessive flash/tool jamming	**Excessive preform temperature**
	High preform weight/incorrect distribution
	Improper tool design
Excessive forging loads	**Low preform temperature**
	High preform weight
Low densities	**Oxidation**
	Low forging temperature/pressure
	Low preform weight
	Die chill
Cracks,laps	**Improper tool or preform design**
Improper die fill	**Improper preform weight distribution/material flow**

Draft angles, which facilitate forging and ejection in conventional forging, are eliminated in powder forged parts. This means that greater ejection forces--on the order of 15 to 20% of press capacity as a minimum--are required for the powder forging of simple shapes. However, the elimination of draft angles permits P/F parts to be forged more closely to net shape. Figure 10 illustrates the ejection forces required for a P/F gear as a function of residual porosity (Fig. 10a) and preform temperature (Fig. 10b). To be suitable for powder forging, standard forging presses must be modified to have stronger ejection systems.

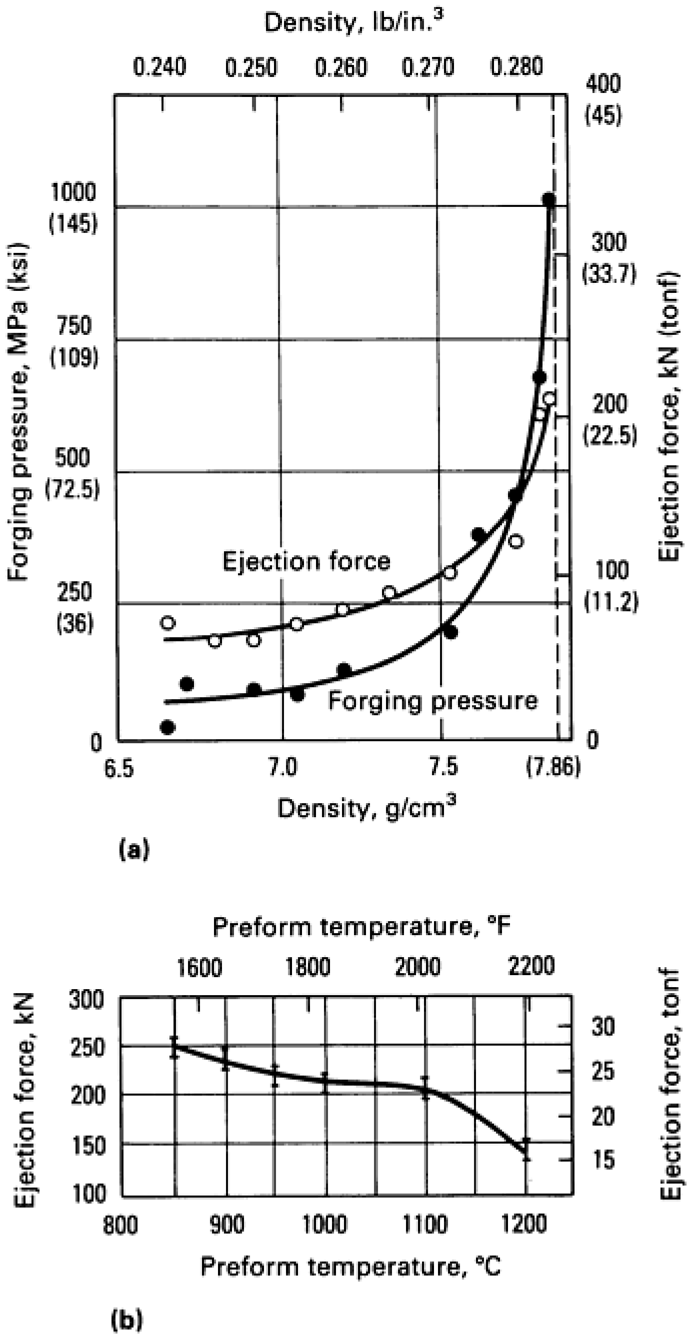


Fig. 10 (a) Forging pressure and ejection force as functions of density for the P/F-4600 powder forged gear shown in Fig. 12. Preform temperature: 1100 °C (2010 °F). (b) Ejection force after forging as a function of preform temperature for a powder forged gear. Forging pressure ranged from 650 to 1000 MPa (94 to 145 ksi).

The deformation behavior of sintered, porous materials differs from that of wrought materials because porous materials densify during the forming operation. As a consequence, a porous preform will appear to have a higher rate of work hardening than its wrought counterpart. The work-hardening exponent, *m*, can be defined in terms of the true stress-true strain diagram:

$$\sigma = K\epsilon^m \qquad \textbf{(Eq 1)}$$

where σ is true stress, ϵ is true strain, and K is a proportionality constant. An empirical relationship between m and density ρ for a ferrous preform has been shown (Ref 44):

$$m = 0.31\rho^{-1.91} \quad \textbf{(Eq 2)}$$

where ρ is expressed as a fraction of the density of pore-free material. The value of m for pore-free pure iron is 0.31, and any excess over this value for porous iron is due to geometric work hardening.

A further consequence of the densification of porous preforms during deformation is reflected in Poisson's ratio for the porous material. Poisson's ratio is a measure of the lateral flow behavior of a material; for compression of a cylinder, it is expressed as diametral strain ϵ_d divided by height strain $-\epsilon_z$. For a pore-free material, Poisson's ratio for plastic deformation ν is 0.5. This is a direct result of the fact that the volume of the material remains constant during deformation. For example, equating the volume of a cylinder before and after deformation (Ref 44):

$$H_0[\pi D_0^2)/4] = H_f[(\pi D_f^2)/4] \quad \textbf{(Eq 3)}$$

where H_0 and H_f are initial and final cylinder heights, respectively, and D_0 and D_f are initial and final cylinder diameters, respectively. Dividing by $H_f D_0^2$ yields:

$$H_0/H_f = (D_f/D_0)^2 \quad \textbf{(Eq 4)}$$

and taking logarithms

$$\ln (H_0/H_f) = \ln (D_f/D_0)^2 = 2 \ln (D_f/D_0) \quad \textbf{(Eq 5)}$$

or

$$-\epsilon_z = 2\epsilon_d \quad \textbf{(Eq 6)}$$

and, from the definition of Poisson's ratio:

$$\nu = \frac{-\epsilon_d}{\epsilon_z} = 0.5 \quad \textbf{(Eq 7)}$$

During compressive deformation of a sintered metal powder preform, some material flows into the pores, and there is a volume decrease. For a given reduction in height, the diameter of a sintered P/M cylinder will expand less than that of an identical cylinder of a pore-free material. Poisson's ratio for a P/M preform will therefore be less than 0.5 and will be a function of the pore volume fraction. H.A. Kuhn (Ref 45) has established an empirical relationship between Poisson's ratio and part density:

$$\nu = 0.5\,\rho^a \quad \textbf{(Eq 8)}$$

The best fit to experimental data is obtained with the exponent a = 1.92 for room-temperature deformation and a = 2.0 for hot deformation. The slight difference in this exponent may be due to work hardening (Ref 44).

In deformation processing of materials, plasticity theory is useful for calculating forming pressures and stress distributions. The above mentioned idiosyncrasies in the deformation behavior of sintered, porous materials have been taken into account in the development of a plasticity theory for porous materials. This has been of benefit in applying workability analysis to porous preforms (Ref 31, 44, 46, 47, 48, 49, 50, 51, 52, 53, 54, 55, 56, 57, 58, 59).

A typical workability line is shown in Fig. 11, which also indicates the way processing variables affect the location of the line. The line has a slope of -0.5, and workability improves as the plane strain intercept (y-axis intercept) value increases. For a given material, workability can be improved through either temperature adjustment or a change in preform density. Figure 12(b) to (e) show the effects of temperature and pressure on the densification and forming of the powder forged gear shown in Fig. 12(a) (Ref 61). While Fig. 12(b) to (e) indicate that higher temperatures reduce the forging pressures required, Fig. 13 illustrates a region of forging pressure at lower temperatures that is comparable to that for higher-temperature forging. The ability to forge at lower temperatures may be beneficial in extending the life of the forging dies.

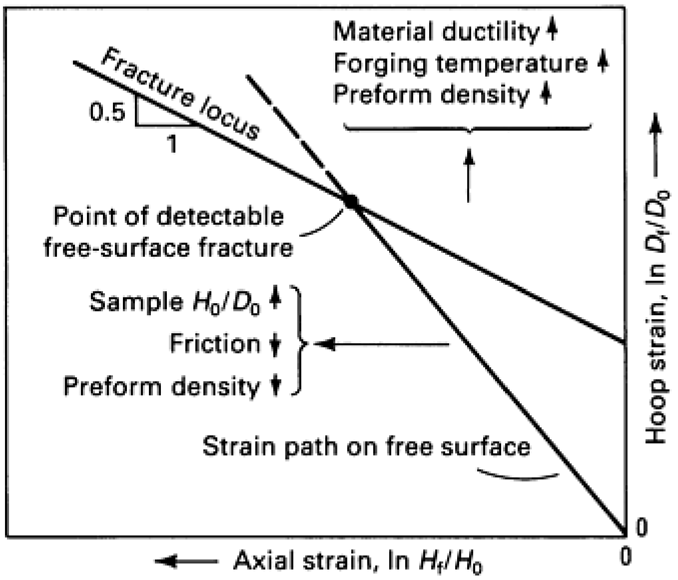


Fig. 11 Effects of forging variables on the workability of porous preforms in hot forging. Source: Ref 60.

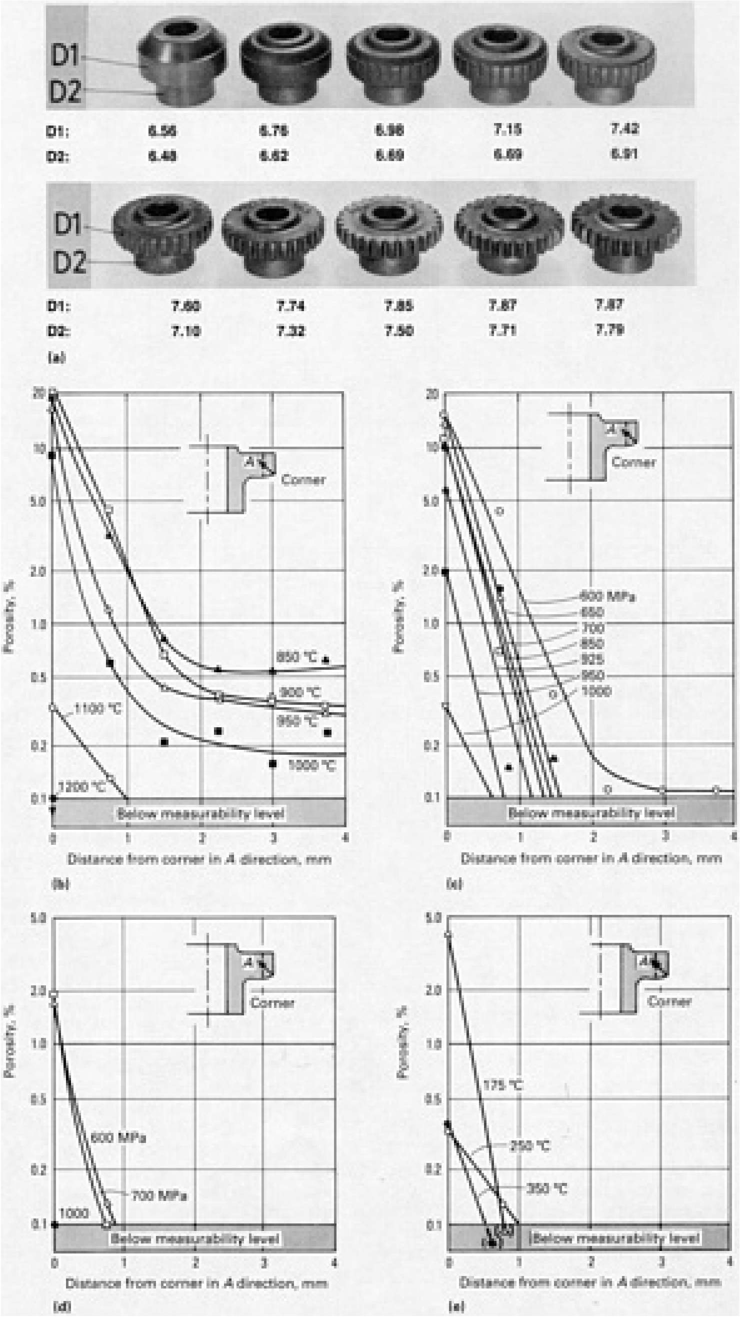


Fig. 12 Influence of process variables on residual porosity in critical corner areas of a powder forged gear tooth. (a) Powder forged gear; D1 and D2 are average densities in grams per cubic centimeter. (b) Preform temperature at a forging pressure of 1000 MPa (145 ksi). (c) and (d) Forging pressure at preform temperatures of 1100 °C (2010 °F) and 1200 °C (2190 °F), respectively. (e) Die temperature at a forging pressure of 1000 MPa (145 ksi) and a preform temperature of 1100 °C (2010 °F). Source: Ref 43, 61.

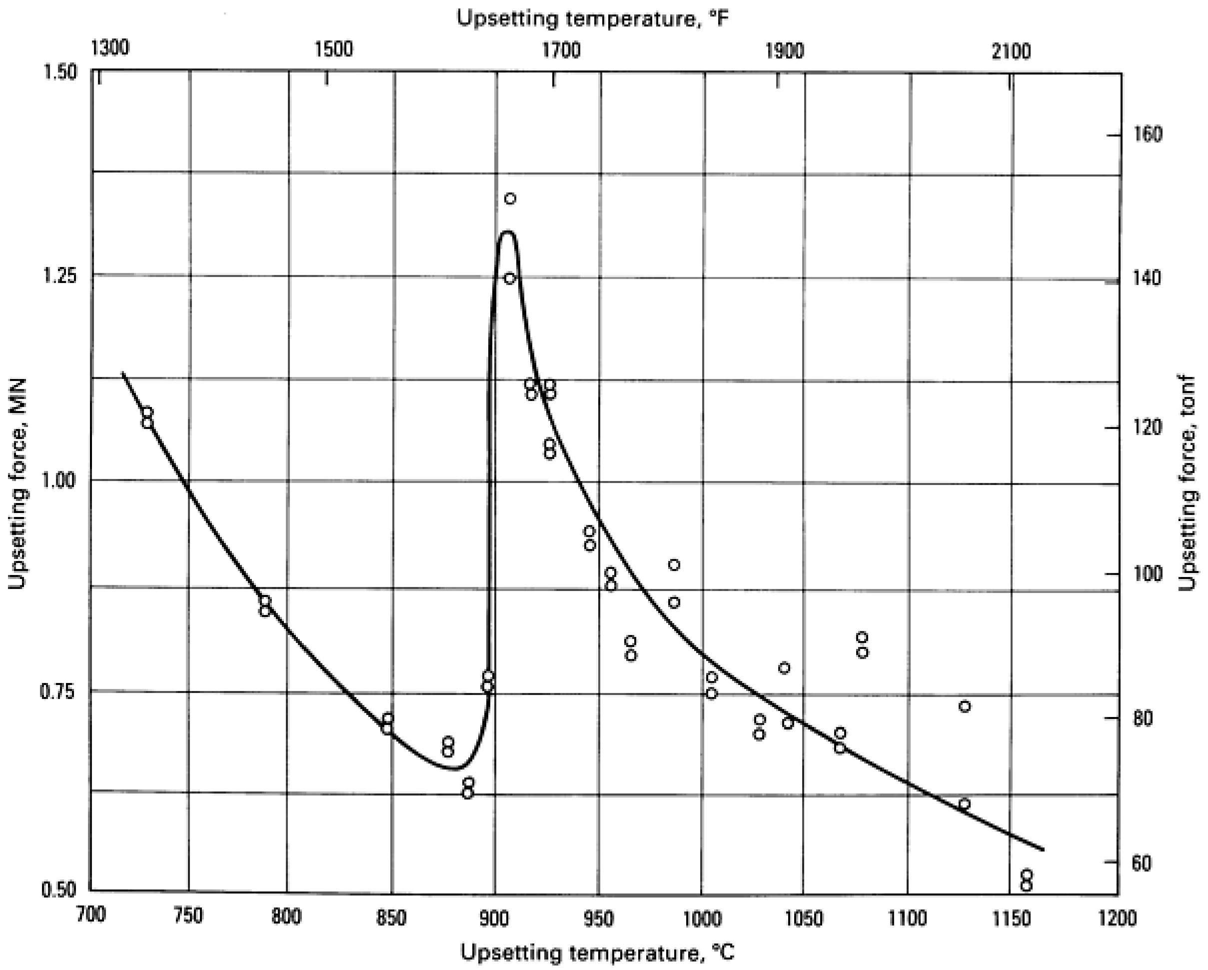


Fig. 13 Force required for a 50% reduction in height of water-atomized iron powder preforms as a function of deformation temperature. Source: Ref 62.

The data presented in Fig. 13 relate to a pure iron with no added graphite. The dramatic increase in the force required for densification around 900 °C (1650 °F) is due to the phase transformation from body-centered cubic (bcc) α iron to face-centered cubic (fcc) austenite. In this temperature range, the flow stress of austenite is higher than that of ferrite. However, although materials are fully austenitic at conventional forging temperatures (1000 to 1130 °C, or 1830 to 2065 °F), the flow stress of austenite at 1100 °C (2010 °F) is less than that of ferrite at 850 °C (1560 °F).

A similar low flow stress regime has been observed for prealloyed material (Fig. 14). However, depending on the amount of solution of graphite, the dip in the flow stress versus temperature curve becomes less pronounced and eventually is no longer observed. The presence of carbon in solution alters the phase distribution, and the observed flow stress depends on the relative proportions of ferrite and austenite in the microstructure.

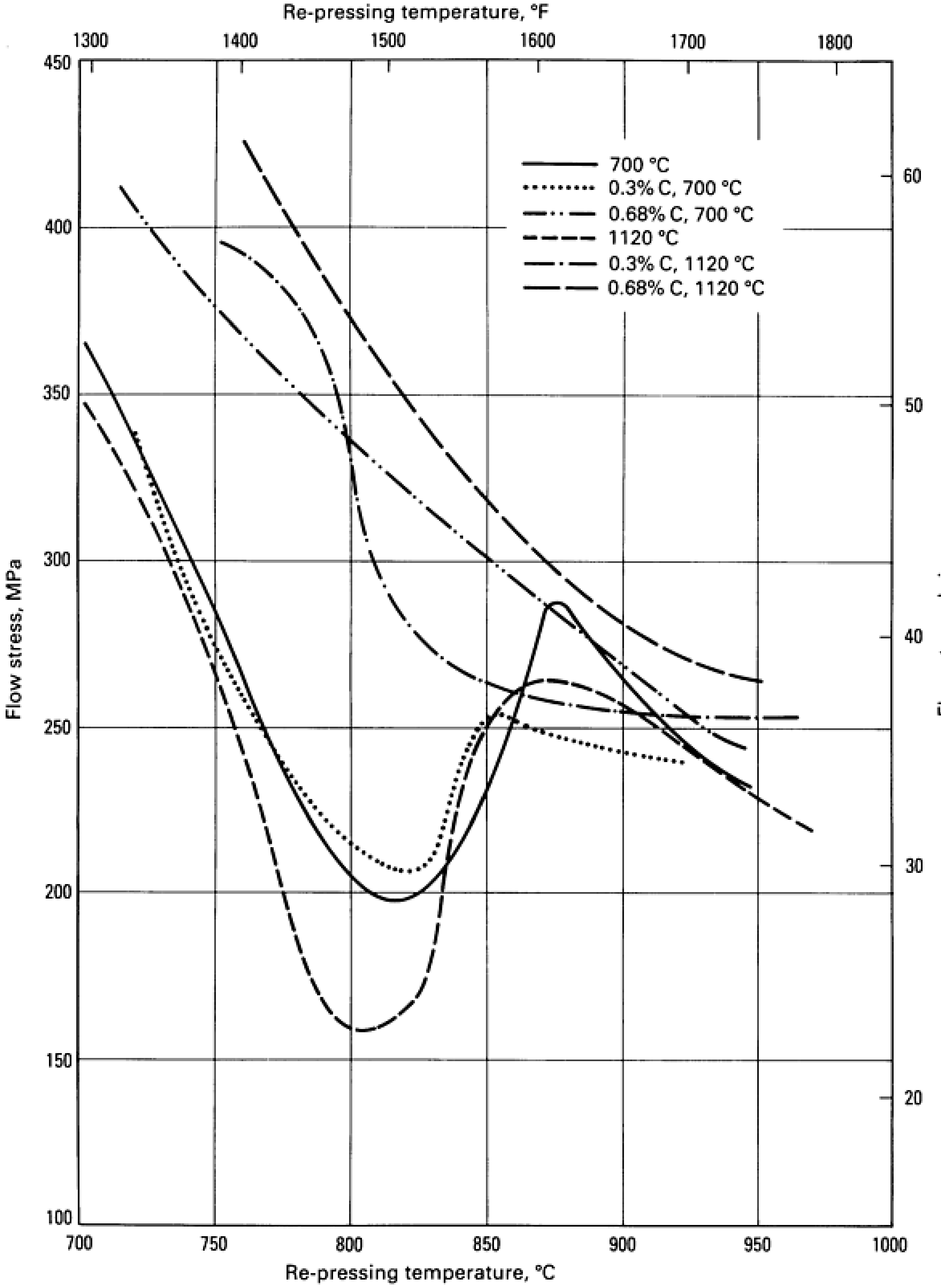


Fig. 14 Influence of hot re-pressing temperature on flow stress for P/F-4600 at various carbon contents and presintering temperatures. Data are for density of 7.4 g/cm^3 (0.267 lb/in.^{3}). Source: Ref 63, 64.

In order to take any advantage of the low flow stress, the thermomechanical processing of preforms that contain added graphite must therefore be such that the graphite does not go into solution. Even under such conditions, in the data reported by Q. Jiazhong, O. Grinder, and Y. Nilsson (Ref 65), the mechanical properties of low temperature forged material are considerably inferior to those of material forged at higher temperatures (Table 2). The low-temperature forging resulted in incomplete densification, and this degraded the mechanical properties. G. Bockstiegel and H. Olsen observed a similar dependence of forged density on preform temperature (Ref 66). They pointed out that the presence of free graphite might impede densification. During subsequent heat treatment, when the graphite goes into solution, it could leave fine porosity, which would degrade the mechanical properties of the material.

Table 2 Tensile and impact properties of P/F-4600 hot re-pressed at two temperatures

Re-pressing temperature		**Re-pressing stress**		**Re-pressed density**		**0.2% offset yield strength**		**Ultimate tensile strength**		**Elongation, %**	**Reduction in area, %**	**Hardness, HV(a)**	Charpy V-notch impact energy	
°C	**°F**	**MPa**	**ksi**	**g/cm^3**	**lb/in.^{3}**	**MPa**	**ksi**	**MPa**	**ksi**				**J**	ft · lb
870	1600	406	59	7.65	0.276	1156	168	1634	237	2.6	2.8	519	2.9	**2.13**
870	1600	565	82	7.72	0.279	1243	180	1641	238	2.1	2.8	538	2.8	**2.06**
870	1600	741	107	7.78	0.281	1316	191	1702	247	2.4	2.4	564	3.1	**2.29**
870	1600	943	137	7.79	0.282	1349	196	1705	248	2.3	2.4	562	3.5	**2.58**
1120	2050	344	50	7.83	0.283	1364	198	1750	254	6.4	20.5	549	6.8	**5.01**
1120	2050	593	86	7.86	0.2840	1450	210	1777	258	6.7	17.3	566	6.2	**4.57**
1120	2050	856	124	7.87	0.2844	1592	231	1782	259	5.3	14.1	565	6.2	**4.57**

(a) 30-kgf load

Metal flow can cause surface fractures. These are generally associated with contact between the deforming preform and the forging tooling. Surface fracture problems may be avoided by changing the preform geometry or the lubrication conditions.

Frictional constraint at the interface between the preform and the forging die generates undesirable stress states in the preform that can lead to fracture. The types of fracture encountered in powder forging are:

- Free-surface fracture
- Die contact surface fracture
- Internal fracture

Production of metallurgically sound forgings requires the prediction and elimination of fracture. An excellent review of the subject is given in Ref 44, 47, 57, and 58.

Tool Design. In order to produce sound forged components, the forging tooling must be designed to take into account:

- Preform temperature
- Die temperature
- Forging pressure
- The elastic strain of the die
- The elastic/plastic strain of the forging
- The temperature of the part upon ejection
- The elastic strain of the forging upon ejection
- The contraction of the forging during cooling
- Tool wear

Specified part dimensional tolerances can only be met when the above parameters have been taken into account. However, there is still some flexibility in the control of forged part dimensions even after die dimensions have been selected. Higher preform ejection temperatures result in greater shrinkage during cooling. Increases in die temperature expand the die cavity and thus increase the size of the forged part. Therefore, if the forgings are undersize for a given set of forging conditions, a lower preform preheat temperature and/or a higher die preheat temperature can be used to produce larger parts. On the other hand, if the forged parts are oversize, the preform preheat temperature could be raised and/or the die temperature lowered to bring the parts to the desired size.

Secondary Operations. In general, the secondary operations applied to conventional components such as plating and peening, may be applied to powder forged components. The most commonly used secondary operations involve deburring, heat treating, and machining.

The powder forged components may require deburring or machining to remove limited amounts of flash formed between the punches and the die. This operation is considerably less extensive than that required for wrought forgings.

The heat treatment of P/M products is the same as that required for conventionally processed materials of similar composition. The most common heat-treating practices involve treatments such as carburizing, quench-and-temper cycles, or continuous-cooling transformations.

The amount of machining required for P/F components is generally less than the amount required for conventional forgings because of the improved dimensional tolerances, shown in Table 3. Standard machining operations may be used to achieve final dimensions and surface finish (Ref 67). One of the main economic benefits of powder forging is the reduced amount of machining required, as illustrated in Fig. 15.

Table 3 Comparison of powder forging with competitive processes

Process	**Range of weights**		**Height-to-diameter ratio**	**Shape**	**Material use, %**	**Surface roughness µm**	**Quantity required for economical production[(a)]**	Cost per unit[(b)]
	kg	**lb**						
Powder forging	0.1-5	0.22-11	1	No large variations in cross section; openings limited	100	5-15	20,000	**200**
Precision forging	0.3-5	0.66-11	2	Any; openings limited	80-90	10-20	20,000	**200**
Cold forging	0.01-35	0.022-77	Not limited	Mostly rotational symmetry	95-100	1-10	5,000	**150**
Precision casting	0.1-10	0.22-22	Not limited	Any; no limits on openings	70-90	10-30	2,000	**100**

Sintering	0.01-5	0.022-11	1	No large variations in cross section; openings limited	100	1-30	5,000	**100**
Drop forging	**0.05-1000**	**0.11-2200**	**Not limited**	**Any; openings limited**	**50-70**	**30-100**	**1,000**	**150**

(a) For 0.5 kg (1.1 lb) parts.

(b) Sintering = 100%

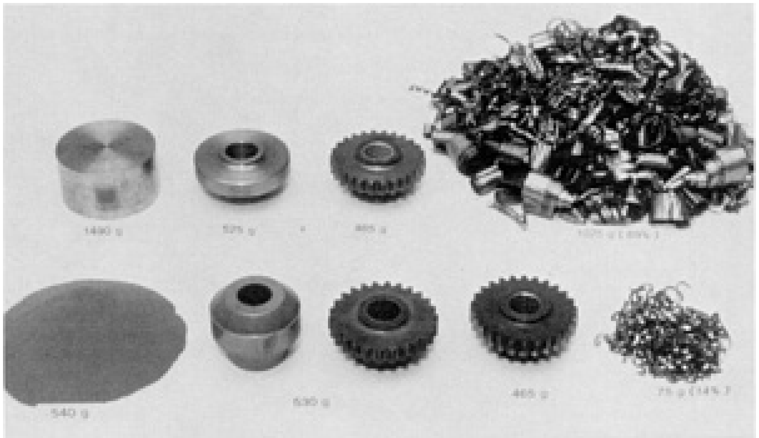


Fig. 15 Comparison of material use for a conventionally forged reverse idler gear (top) and the equivalent powder forged part (bottom). Material yield in conventional forging is 31%; that for powder forging is 86%. 1 lb = 453.6 g. Source: Ref 61.

In general, pore-free P/F materials machine as readily as conventional forgings processed to achieve identical composition, structure, and hardness. Difficulties are encountered, however, if P/F components are machined with the same cutting speeds, feed rates, and tool types as conventional components. These differences in machinability have been related to inclusion types and microporosity (Ref 16, 68). These studies conclude that P/F materials can exhibit equal or greater machinability than wrought steels. Improved machinability can be accomplished by the addition of solid lubricants such as manganese sulfide.

However, the presence of microporosity and low-density noncritical areas in P/F components leads to reduced machinability. The machinability behavior for these areas is similar to that of conventional P/M materials (Ref 69). The overall machinability of a powder forged component may be said to depend on the amount, type, size, shape, and dispersion of inclusions and/or porosity, as well as on the alloy and heat-treated structure.

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Powder Forging

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Mechanical Properties

Wrought steel bar stock undergoes extensive deformation during cogging and rolling of the original ingot. This creates inclusion stringers and leads to planes of weakness, which affect the ductile failure of the material. The mechanical properties of wrought steels vary considerably according to the direction test pieces are cut from the wrought billet. Powder forged materials, on the other hand, undergo relatively little material deformation, and their mechanical properties have been shown to be relatively isotropic (Ref 70). The directionality of properties in wrought steel is illustrated in Table 4.

Table 4 Comparison of transverse and longitudinal mechanical properties of wrought steels

Material	Specimen orientation	Ultimate tensile strength		Yield strength, 0.2% offset		Impact energy		Fatigue endurance limit		Elongation, %	Reduction of area, %
		MPa	ksi	MPa	ksi	J	ft·lb	MPa	ksi		
5046	Longitudinal	820	119	585	85	. . .	. . .	. . .	. . .	25.5	**64**
	Transverse	825	120	600	87	. . .	. . .	. . .	. . .	11.5	**21**
4340	Longitudinal	1095	159	1005	146	. . .	. . .	. . .	. . .	19.0	**55**
	Transverse	1095	159	1000	145	. . .	. . .	. . .	. . .	13.5	**30**
8620	Longitudinal	1060-1215	154-176	905-1070	131-155	. . .	. . .	. . .	. . .	12-15	**53-57**
	Transverse	1070-1240	155-180	905-1240	131-157	. . .	. . .	. . .	. . .	4-8	**10-15**
EN-16[(a)], lot Y	Longitudinal	920-980	133-142	. . .	. . .	100	74	310	45	17-19	**60-62**
	Transverse	910-950	132-138	. . .	. . .	10	7.4	250	36	5-12	**8-24**
EN-16[(a)], lot Z	Longitudinal	960-1000	139-145	. . .	. . .	100	74	400	58	17-18	**58-62**
	Transverse	**950-970**	**138-141**	**. . .**	**. . .**	**10**	**7.4**	**290**	**42**	**7-10**	**6-15**

Data on 5046 and 4340 are from Ref 71; data on 8620 are from Ref 72; data on EN-16 are from Ref 73.

(a) Composition of EN-16: Fe-1.7Mn-0.27Mo.

Mechanical properties of powder forged materials are usually intermediate to the transverse and longitudinal properties of wrought steels. The rotating-bending fatigue properties of powder forged material also have been shown to fall between the longitudinal and transverse properties of wrought steel of the same tensile strength (Ref 74). This is illustrated in Fig. 16.

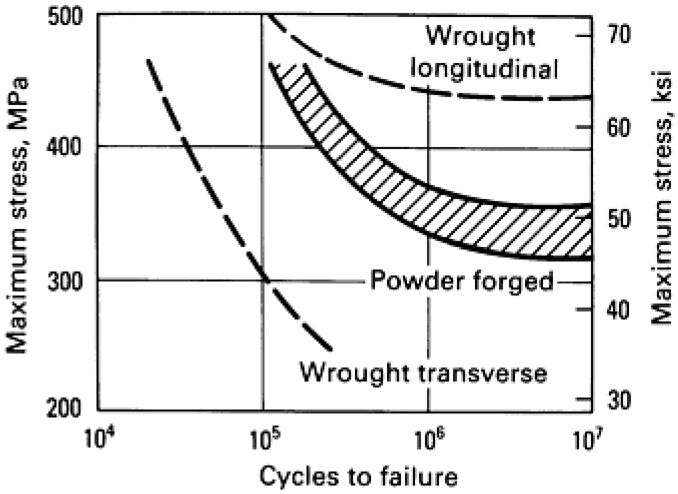

Fig. 16 Comparison of fatigue resistance of powder forged and wrought materials. Source: Ref 74.

While the performance of machined laboratory test pieces follows the intermediate trend described above, in the case of actual components, powder forged parts have been shown to have superior fatigue resistance (Fig. 17). This has generally been attributed not only to the relative mechanical property isotropy of powder forgings but also to their better surface finish and finer grain size.

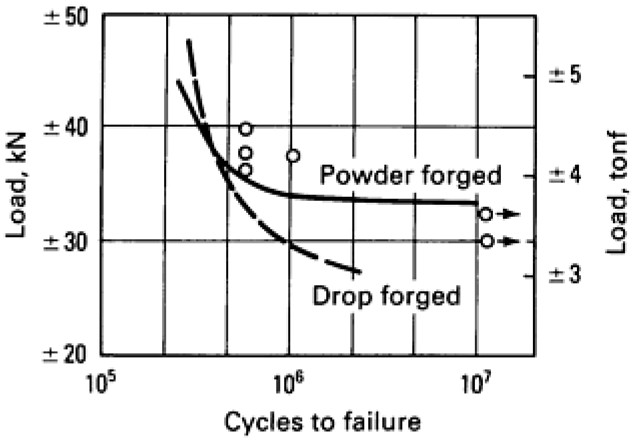

Fig. 17 Fatigue curves for powder forged and drop forged connecting rods. Source: Ref 75.

The present section reviews the mechanical properties of powder forged materials. The data presented represent results obtained on machined standard laboratory test pieces. Data will be reported for four primary materials. The first two material systems are based on prealloyed powders (P/F-4600 and P/F-2000). The third material based on an iron-copper-carbon alloy, was used by Toyota in 1981 to make P/F connecting rods; Ford Motor Company introduced powder forged rods with a similar chemistry in 1986. Mechanical property data are therefore presented for copper and graphite powders mixed with an iron powder base to produce materials that generally contain 2% Cu. Some powder forged components are made from plain carbon steel. This is the fourth and final material for which mechanical property data are presented.

Forging Mode. It is well known that the forging mode has a major effect on the mechanical properties of components. With this in mind, the mechanical property data reported in this section were obtained on specimens that were either hot upset or hot re-press forged. The forging modes used to produce billets for mechanical property testing are shown in Fig. 18.

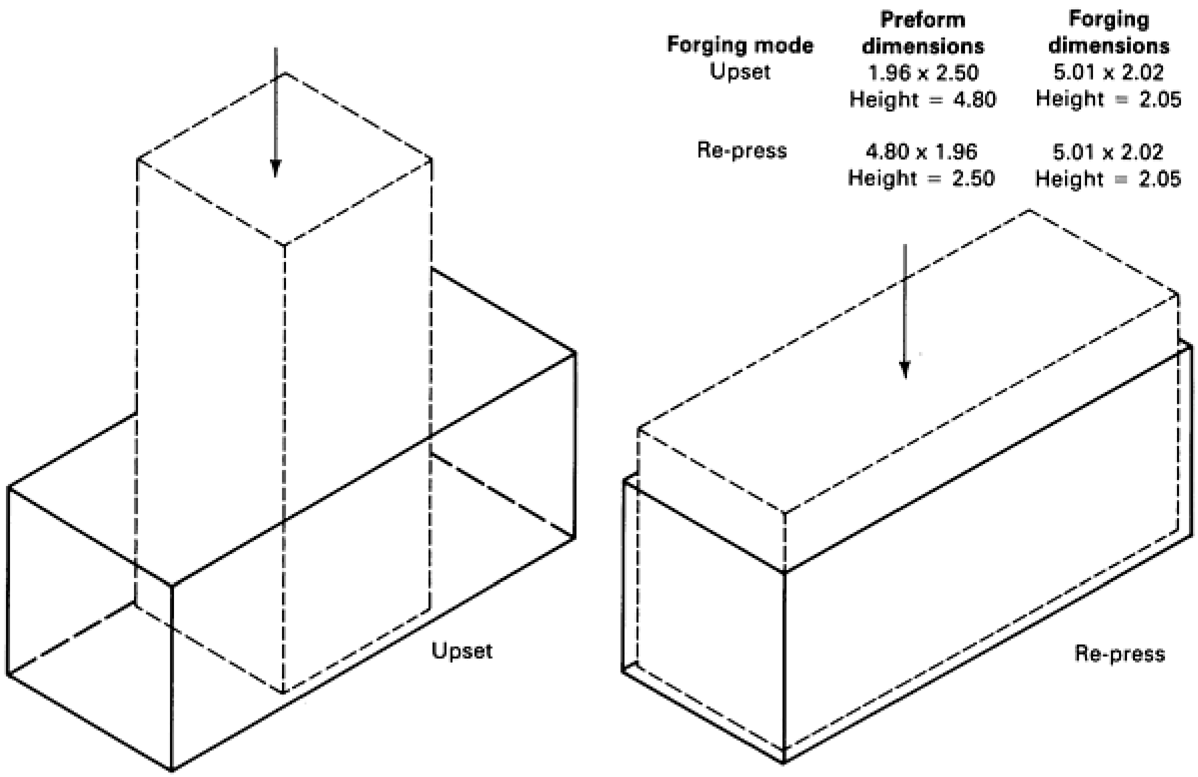

Fig. 18 Forging modes used in production of billets for mechanical testing. Dimensions, given in inches, are average values.

Longitudinal test specimens 10 mm (0.4 in.) in diameter (for tensile and fatigue testing) and 10.8 × 10.8 mm (0.425 × 0.425 in.) square (for impact testing) were then cut from the forged billets. These specimen sizes represent comparable 10 mm (0.4 in.) diam ruling sections used for heat treatment and were the section sizes used unless otherwise noted.

Heat Treatments. There were three heat treatments used in developing the properties of the prealloyed powder forged materials: case carburizing, blank carburizing, and through-hardening (quenching and tempering).

Case carburizing was applied to materials with a nominal core carbon content of 0.20 to 0.25%. Blank carburizing is intended to produce a microstructure similar to that found in the core of case carburized samples. At the 0.20 to 0.25% C level, this results in a core hardness of 45 to 55 HRC.

Quenching and tempering was applied to achieve through hardened microstructures over a range of forged carbon contents. A low-temperature temper or stress relief at 175 °C (350 °F) resulted in core hardnesses in the range of 55 to 65 HRC for materials with carbon contents of 0.4% and above. In addition, higher-temperature tempers were designed to achieve core hardnesses of 45 to 55 HRC and 25 to 30 HRC in these higher-carbon samples. Details of these heat treatments are given below.

Case Carburizing. Specimens were austenitized for 8 h at 955 °C (1750 °F) in an endothermic gas atmosphere with a dew point of -11 °C (+12 °F). They were then cooled to 830 °C (1525 °F) and stabilized at temperature in an endothermic gas atmosphere with a dew point of +2 °C (+35 °F). The specimens were quenched in a fast quench rate oil with agitation at a temperature of 65 °C (150 °F). They were then stress relieved at 175 °C (350 °F) for 2 h. This heat treatment resulted in a case depth of about 1.52 mm (0.060 in.), with a 1.0% carbon content in the case and a nominal core carbon of 0.25%.

Blank Carburizing. The forged samples were austenitized for 2 h at 955 °C (1750 °F) in a dissociated ammonia and methane atmosphere. They were quenched with agitation in a fast quench rate oil at 65 °C (150 °F). The samples were reaustenitized at 845 °C (1550 °F) for 30 min in a dissociated ammonia and methane atmosphere, followed by oil quenching with agitation in oil held at 65 °C (150 °F). They were then stress relieved at 175 °C (350 °F) for 2 h in a nitrogen atmosphere.

Through-Hardening. This quench and temper heat treatment consisted of austenitizing the specimens for 1 h at 955 °C (1750 °F) in a dissociated ammonia and methane atmosphere, followed by quenching with agitation in a fast quench rate oil at 65 °C (150 °F). The specimens were reaustenitized at 845 °C (1550 °F) for 30 min in a dissociated ammonia and methane atmosphere, followed by quenching with agitation in oil at 65 °C (150 °F). They were stress relieved for 1 h at 175 °C (350 °F) in a nitrogen atmosphere or tempered at the various temperatures listed in the tables. This procedure resulted in a uniform microstructure throughout the cross section.

Hardenability. Jominy hardenability curves are presented in Fig. 19 for the P/F-4600, P/F-2000, and iron-copper-carbon alloys. Testing was carried out according to ASTM A 255. Specimens were machined from upset forged billets that had been sintered at 1120 °C (2050 °F) in dissociated ammonia.

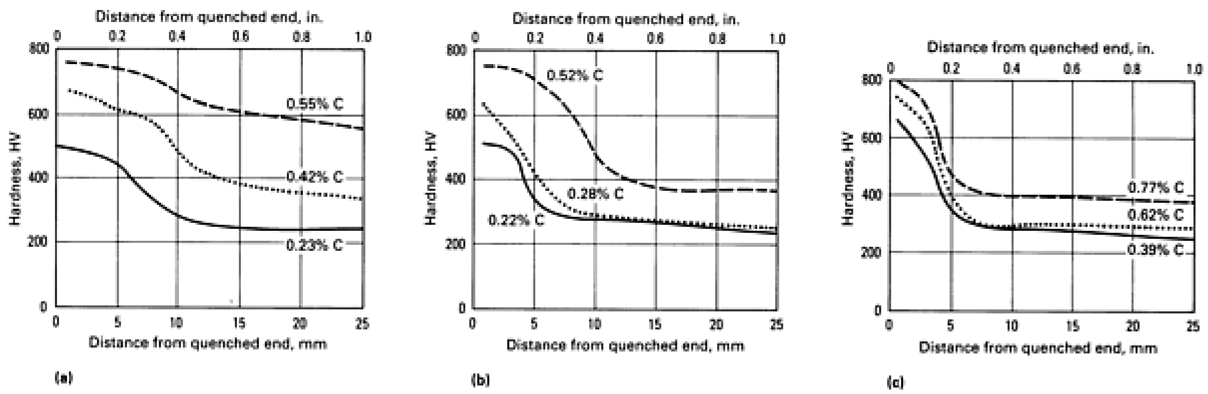

Fig. 19 Jominy hardenability curves for (a) P/F-4600, (b) P/F-2000, and (c) iron-copper-carbon materials at various forged-carbon levels. Vickers hardness was determined at a 30 kgf load.

Tempering Response. Tempering curves (core hardness versus carbon content and tempering temperature) are presented in Fig. 20 for P/F-2000 and P/F-4600. The curves for P/F-4600 cover ruling sections of 10 mm (0.40 in.) to 25.4 mm (1.0 in.).

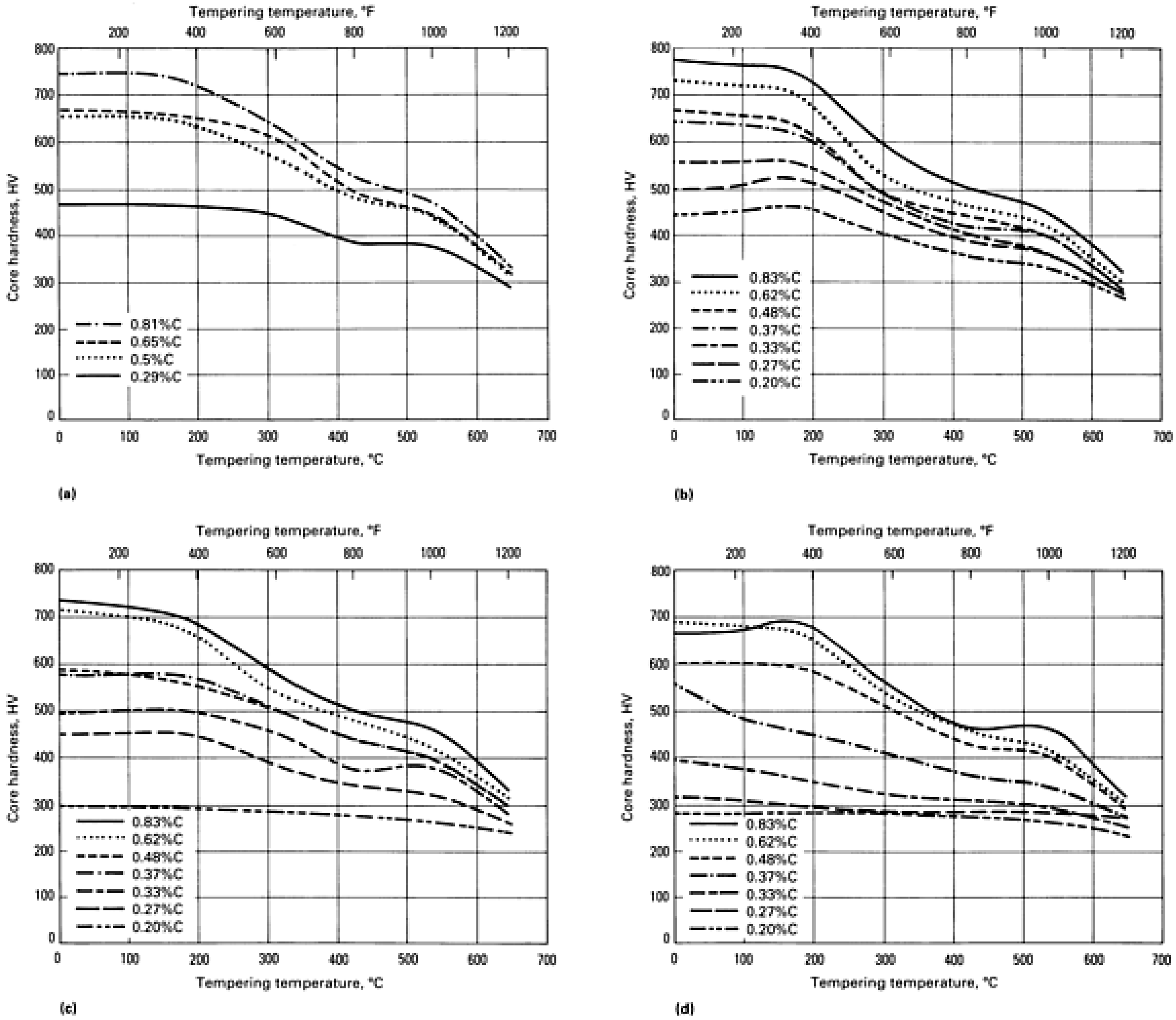

Fig. 20 Effect of tempering temperature and carbon content on the core hardness of (a) P/F-2000 for a ruling section of 10 mm (0.40 in.), and of P/F-4600 materials for ruling sections of (b) 10 mm (0.40 in.), (c) 19 mm (0.75 in.), and (d) 25.4 mm (1.0 in.).

Tensile, Impact, and Fatigue Properties. Tensile properties were determined on test pieces with a gage length of 25.4 mm (1 in.) and a gage diameter of 6.35 mm (0.25 in.). Testing was carried out according to ASTM E 8 using a crosshead speed of 0.5 mm/min (0.02 in./min). Room-temperature impact testing was carried out on standard Charpy V-notch specimens according to ASTM E 23. Rotating-bending fatigue (RBF) testing was performed using single-load, cantilever, rotating fatigue testers. Dimensions of the RBF test specimen are shown in Fig. 21.

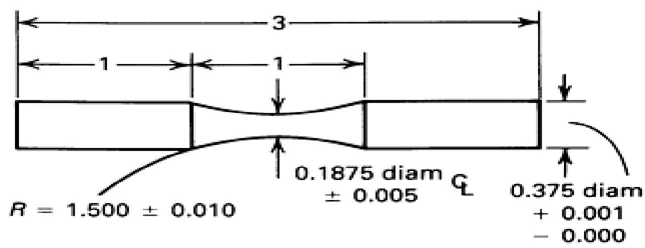

Fig. 21 Dimensions (in inches) of RBF test specimens.

The tensile, impact, and fatigue data for the various materials are summarized in Tables 5, 6, 7 and Fig. 22 and 23.

Table 5 Mechanical property and fatigue data for P/F-4600 materials

Sintered at 1120 °C (2050 °F) in dissociated ammonia unless otherwise noted.

Forging mode	Carbon, %	Oxygen, ppm	Ultimate tensile strength		0.2% offset yield strength		Elongation, % in 25 mm (1 in.)	Reduction of area, %	Room-temperature Charpy V-notch impact energy		Core hardness, HV30	Fatigue endurance limit		Ratio of fatigue endurance to tensile strength
			MPa	**ksi**	**MPa**	**ksi**			**J**	**ft · lb**		**MPa**	**ksi**	
Blank carburized														
Upset	0.24	230	1565	227	1425	207	13.6	42.3	16.3	12.0	487	565	82	**0.36**
Re-press	0.24	210	1495	217	1325	192	11.0	34.3	12.9	9.5	479	550	80	**0.37**
Upset[(a)]	0.22	90	1455	211	1275	185	14.8	46.4	22.2	16.4	473	550	80	**0.38**
Re-press[(a)]	0.25	100	1455	211	1280	186	12.5	42.3	16.8	12.4	468	510	74	**0.36**
Upset[(b)]	0.28	600	1585	230	1380	200	7.8	23.9	10.8	8.0	513	590	86	**0.37**
Re-press[(b)]	0.24	620	1580	229	1305	189	6.8	16.9	6.8	5.0	464	455	66	**0.29**
Quenched and stress relieved														
Upset	0.38	270	1985	288	1505	218	11.5	33.5	11.5	8.5	554	. . .	. . .	**. . .**
Re-press	0.39	335	1960	284	1480	215	8.5	21.0	8.7	6.4	. . .	. . .	. . .	**. . .**
Upset	0.57	275	2275	330	. . .	. . .	3.3	5.8	7.5	5.5	655	. . .	. . .	**. . .**

Re-press	0.55	305	1945	282	. . .	. . .	0.9	2.9	8.1	6.0	. . .	. . .	. . .	. . .
Upset	0.79	290	940	136	. . .	. . .	0.0	0.0	1.4	1.0	712	. . .	. . .	. . .
Re-press	0.74	280	1055	153	. . .	. . .	0.0	0.0	2.4	1.8	. . .	. . .	. . .	. . .
Upset	1.01	330	800	116	. . .	. . .	0.0	0.0	1.3	1.0	672	. . .	. . .	. . .
Re-press	0.96	375	760	110	. . .	. . .	0.0	0.0	1.6	1.2	. . .	. . .	. . .	. . .
Quenched and tempered														
Upset[(c)]	0.38	230	1490	216	1340	194	10.0	40.0	28.4	21.0	473	. . .	. . .	. . .
Re-press[(c)]	. . .	. . .	1525	221	1340	194	8.5	32.3	. . .	. . .	. . .	. . .	. . .	. . .
Upset[(d)]	0.60	220	1455	211	1170	170	9.5	32.0	13.6	10.0	472	. . .	. . .	. . .
Re-press[(d)]	. . .	. . .	1550	225	1365	198	7.0	23.0	. . .	. . .	. . .	. . .	. . .	. . .
Upset[(e)]	0.82	235	1545	224	1380	200	8.0	16.0	8.8	6.5	496	. . .	. . .	. . .
Re-press[(e)]	. . .	. . .	1560	226	1340	194	6.0	12.0	. . .	. . .	. . .	. . .	. . .	. . .
Upset[(f)]	1.04	315	1560	226	1280	186	6.0	11.8	9.8	7.2	476	. . .	. . .	. . .
Re-press[(f)]	. . .	. . .	1480	215	1225	178	6.0	11.8	. . .	. . .	. . .	. . .	. . .	. . .

Upset(g)	0.39	260	825	120	745	108	21.0	57.0	62.4	46.0	269	. . .	. . .	. . .
Upset(g)	0.58	280	860	125	760	110	20.0	50.0	44.0	32.5	270	. . .	. . .	. . .
Upset(h)	0.80	360	850	123	600	87	19.5	46.0	24.4	18.0	253	. . .	. . .	. . .
Upset(i)	**1.01**	**320**	**855**	**124**	**635**	**92**	**17.0**	**38.0**	**13.3**	**9.8**	**268**	**. . .**	**. . .**	**. . .**

(a) Sintered at 1260 °C (2300 °F) in dissociated ammonia.

(b) Sintered at 1120 °C (2050 °F) in endothermic gas atmosphere.

(c) Tempered at 370 °C (700 °F).

(d) Tempered at 440 °C (825 °F).

(e) Tempered at 455 °C (850 °F).

(f) Tempered at 480 °C (900 °F).

(g) Tempered at 680 °C (1255 °F).

(h) Tempered at 695 °C (1280 °F).

(i) Tempered at 715 °C (1320 °F)

Table 6 Mechanical property data for P/F-2000 materials

Forging mode	Carbon, %	Oxygen, ppm	Ultimate tensile strength		0.2% offset yield strength		Elongation, % in 25 mm (1 in.)	Reduction of area, %	Core hardness, HV[(a)]
			MPa	ksi	MPa	ksi			
Blank carburized									
Upset[(b)]	0.19	450	1205	175	. . .	. . .	10.0	37.4	**390**
Re-press[(b)]	0.23	720	1110	161	. . .	. . .	6.3	17.0	**380**
Upset[(c)]	0.25	130	1585	230	. . .	. . .	13.0	47.5	**489**
Re-press[(c)]	0.25	110	1460	212	. . .	. . .	11.3	36.1	**466**
Quenched and stress relieved									
Upset[(b)]	0.31	470	1790	260	. . .	. . .	9.0	27.3	**532**
Re-press[(b)]	0.32	700	1745	253	. . .	. . .	4.0	9.0	**538**
Upset[(b)]	0.54	380	2050	297	. . .	. . .	1.3	. . .	**694**
Re-press[(b)]	0.50	520	2160	313	. . .	. . .	2.0	. . .	**653**
Upset[(c)]	0.65	120	1605	233	. . .	. . .	. . .	. . .	**710**
Re-press[(c)]	0.67	130	1040	151	. . .	. . .	. . .	. . .	**709**
Upset[(b)]	0.73	270	1110	161	. . .	. . .	. . .	. . .	**767**
Re-press[(b)]	0.85	370	1345	195	. . .	. . .	. . .	. . .	**727**
Upset[(b)]	0.70	420	600	87	. . .	. . .	. . .	. . .	**761**
Re-press[(b)]	0.67	320	540	78	. . .	. . .	. . .	. . .	**778**
Upset[(c)]	0.91	120	910	132	. . .	. . .	. . .	. . .	**820**
Re-press[(c)]	0.86	120	840	122	. . .	. . .	. . .	. . .	**825**

Quenched and tempered									
Upset[d]	0.28	720	1050	153	895	130	10.6	42.8	**336**
Upset[e]	0.37	1200	1450	210	1385	201	10.2	33.0	**447**
Upset[e]	0.56	580	1680	244	7560	226	9.8	28.6	**444**
Upset[f]	0.70	760	1805	262	1565	227	5.0	11.8	**531**
Upset[g]	0.86	790	1425	207	1310	190	10.4	30.0	**450**
Upset[h]	0.26	920	835	121	705	102	22.6	57.6	**269**
Upset[i]	0.38	860	860	125	785	114	20.8	56.5	**288**
Upset[j]	0.55	840	917	133	820	119	17.8	49.5	**305**
Upset[k]	0.73	820	965	140	855	124	15.4	42.7	**304**
Upset[k]	**0.87**	**920**	**995**	**144**	**850**	**123**	**15.6**	**33.9**	318

(a) 30-kgf load.

(b) Sintered in dissociated ammonia at 1120 °C (2050 °F).

(c) Sintered in dissociated ammonia at 1260 °C (2300 °F).

(d) Tempered at 175 °C (350 °F).

(e) Tempered at 315 °C (600 °F).

(f) Tempered at 345 °C (650 °F).

(g) Tempered at 425 °C (800 °F).

(h) Tempered at 620 °C (1150 °F).

(i) Tempered at 650 °C (1200 °F).

(j) Tempered at 660 °C (1225 °F).

(k) Tempered at 675 °C (1250 °F)

Table 7 Mechanical property and fatigue data for iron-copper-carbon alloys

Sintered at 1120 °C (2050 °F) in dissociated ammonia, reheated to 980 °C (1800 °F) in dissociated ammonia, and forged

Forging mode	Carbon, %	Oxygen, ppm	Ultimate tensile strength		0.2% offset yield strength		Elongation, % in 25 mm (1 in.)	Reduction of area, %	Room-temperature Charpy V-notch impact energy		Core hardness, HV30	Fatigue endurance limit		Ratio of fatigue endurance to tensile strength
			MPa	ksi	MPa	ksi			J	ft · lb		MPa	ksi	
Upset[a]	0.39	250	670	97	475	69	15	37.8	4.1	3.0	228	. . .	. . .	. . .
Upset[b]	0.40	210	805	117	660	96	12.5	38.3	5.4	4.0	261	325	47	**0.40**
Re-press[a]	0.39	200	690	100	490	71	15	35.4	2.7	2.0	227	. . .	. . .	. . .
Re-press[b]	0.41	240	795	115	585	85	10	36.5	4.1	3.0	269	345	50	**0.43**
Upset[a]	0.67	170	840	122	750	109	10	22.9	2.7	2.0	267	. . .	. . .	. . .
Upset[b]	0.66	160	980	142	870	126	15	24.9	4.1	3.0	322	470	68	**0.48**
Re-press[a]	0.64	190	825	120	765	111	10	24.8	3.4	2.5	266	. . .	. . .	. . .
Re-press[b]	0.67	170	985	143	875	127	10	20.6	4.7	3.5	311	460	67	**0.47**
Upset[a]	0.81	240	1025	149	625	91	10	19.2	2.7	2.0	337	. . .	. . .	. . .
Upset[b]	0.85	280	1130	164	625	91	10	16.6	4.1	3.0	343	525	76	**0.46**
Re-press[a]	0.81	200	1040	151	640	93	10	16.2	2.7	2.0	335	. . .	. . .	. . .

Re-press[b]	**0.82**	**220**	**1170**	**170**	**745**	**108**	**10**	**12.8**	**2.7**	**2.0**	**368**	**475**	**69**	**0.41**

(a) Still-air cooled.

(b) Forced-air cooled

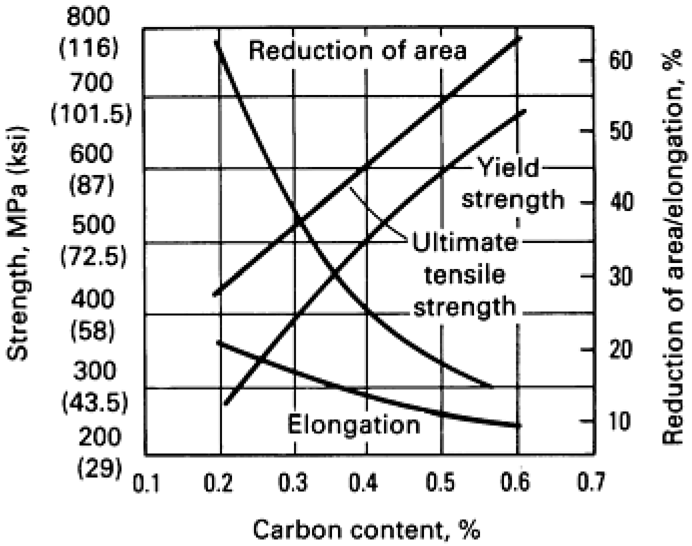

Fig. 22 Mechanical properties versus carbon content for iron-carbon alloys. Source: Ref 76.

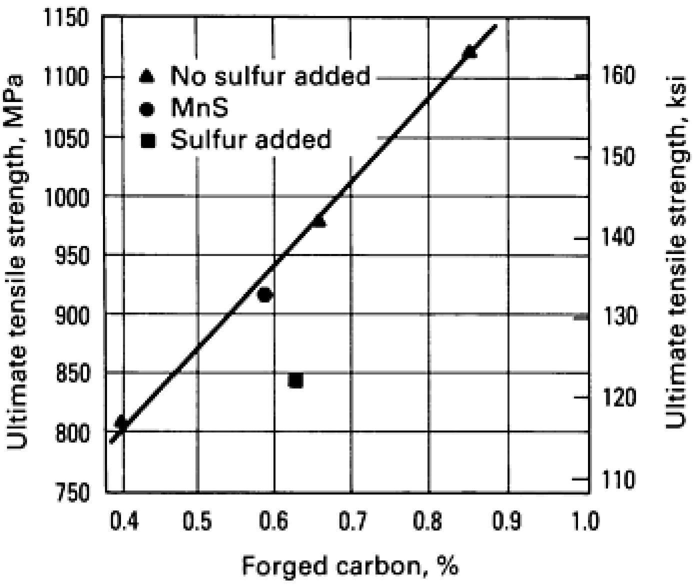

Fig. 23 Effect of sulfur and carbon on the ultimate tensile strength of iron-copper-carbon alloys. Samples were upset forged and forced-air cooled.

The iron-copper-carbon alloys were either still-air cooled or forced-air cooled from the austenitizing temperature of 845 °C (1550 °F). Cooling rates for these treatments are shown in Fig. 24. The austenitizing temperature influences core hardness. These iron-copper-carbon alloys are often used with manganese sulfide additions for enhanced machinability. The tensile, impact, and fatigue properties for a sample with a 0.35% manganese sulfide addition are compared with a material without sulfide additions in Table 8. The results obtained for a sulfurized powder sample are included for comparison. The tensile properties for iron-copper-carbon alloys with a range of forged carbon content are summarized in Fig. 23. Data from the samples with manganese sulfide and sulfurized powders are included for comparison. The manganese sulfide addition had little influence on tensile strength, whereas the sulfurization process degraded tensile properties.

Table 8 Mechanical property and fatigue data for iron-copper-carbon alloys with sulfur additions

Sintered at 1120 °C (2050 °F) in dissociated ammonia, reheated to 980 °C (1800 °F) in dissociated ammonia, and forged

Addition	**Carbon, %**	**Oxygen, ppm**	**Sulfur, %**	**Ultimate tensile strength**		**0.2% offset yield strength**		**Elongation, % in 25 mm (1 in.)**	**Reduction of area, %**	**Room-temperature Charpy V-notch impact energy**		**Core hardness, HV30**	**Fatigue endurance limit**		Ratio of fatigue endurance to tensile strength
				MPa	**ksi**	**MPa**	**ksi**			**J**	**ft · lb**		**MPa**	**ksi**	
Manganese sulfide	0.59	270	0.13	915	133	620	90	11	23.2	6.8	5.0	290	430	62	**0.47**
Sulfur	0.63	160	0.14	840	122	560	81	12	21.4	6.8	5.0	267	415	60	**0.50**
None	**0.66**	**160**	**0.013**	**980**	**142**	**870**	**126**	**15**	**24.9**	**4.1**	**3.0**	**322**	**470**	**68**	**0.48**

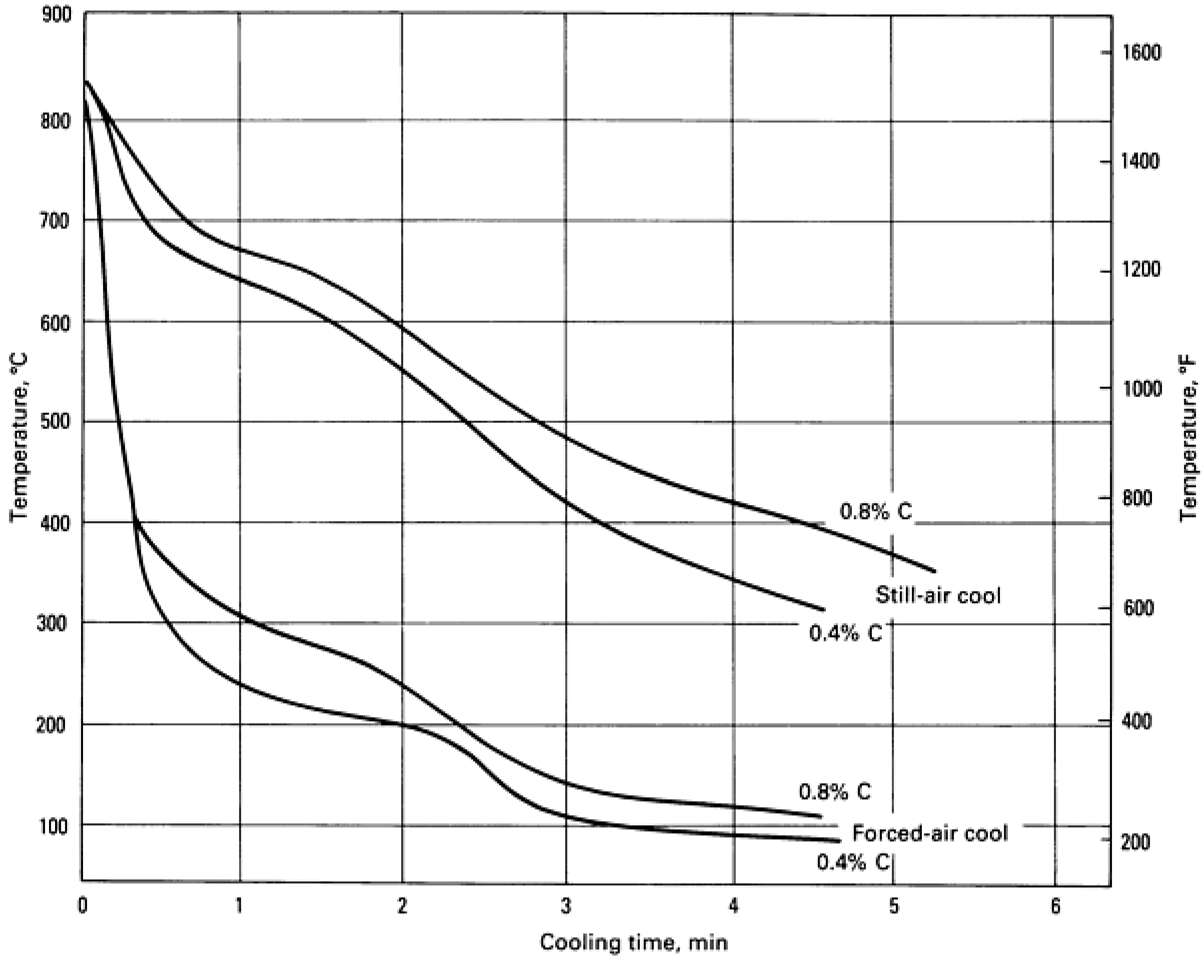

Fig. 24 Cooling rates used for iron-copper-carbon alloys.

Compressive Yield Strength. The 0.2% offset compressive yield strengths for P/F-4600 at various forged carbon levels and after different heat treatments are summarized in Table 9. A comparison of 0.2% offset tensile yield strength with the compressive yield strength for P/F-4600 with a range of carbon contents is given in Fig. 25 for samples stress relieved at 175 °C (350 °F).

Table 9 Compressive yield strengths of P/F-4600 materials

Sintered at 1120 °C (2050 °F) in dissociated ammonia

Forged carbon content, %	**Forged oxygen content, ppm**	**Heat treatment**	Compressive yield strength (0.2% offset)	
			MPa	ksi
0.22	460	Stress relieved at 175 °C (350 °F)	1240	**180**
0.22	350	Tempered at 370 °C (700 °F)	1155	**168**
0.22	440	Tempered at 680 °C (1255 °F)	575	**84**

0.29	380	Stress relieved at 175 °C (350 °F)	1440	**209**
0.35	430	Stress relieved at 175 °C (350 °F)	1670	**242**
0.43	410	Stress relieved at 175 °C (350 °F)	1690	**245**
0.41	410	Tempered at 370 °C (700 °F)	1360	**197**
0.41	460	Tempered at 680 °C (1255 °F)	680	**99**
0.46	480	Stress relieved at 175 °C (350 °F)	1780	**259**
0.44	380	Tempered at 370 °C (700 °F)	1275	**185**
0.44	400	Tempered at 680 °C (1255 °F)	685	**100**
0.57	330	Stress relieved at 175 °C (350 °F)	1980	**287**
0.66	400	Tempered at 440 °C (825 °F)	1325	**192**
0.60	330	Tempered at 680 °C (1255 °F)	700	**101**
0.75	300	Stress relieved at 175 °C (350 °F)	2000	**290**
0.80	480	Tempered at 455 °C (850 °F)	1355	**196**
0.77	**410**	**Tempered at 695 °C (1280 °F)**	**700**	**101**

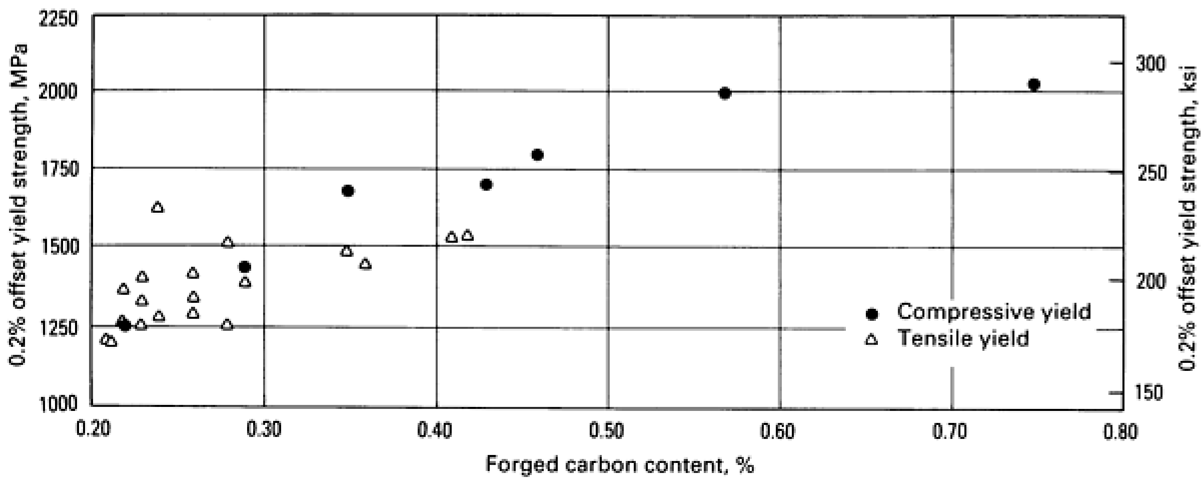

Fig. 25 Comparison of the tensile and compressive yield strengths of quenched and stress relieved P/F-4600 at

various carbon levels

Rolling-Contact Fatigue. Powder forged materials have been used in bearing applications. Rolling-contact fatigue testing is an accelerated bearing test used to rank materials with respect to potential performance in bearing applications. Rolling-contact fatigue testing of both case carburized and through hardened P/F-4600 and P/F-2000 materials was carried out using ball/rod testers according to the procedure described in Ref 77. Weibull analysis data are summarized in Table 10.

Table 10 Rolling-contact fatigue data for carburized and through-hardened P/F-4600 and P/F-2000

Sintering conditions	**Forging mode**	**Carbon, %**	**Oxygen, ppm**	**Life to 10% failure rate, 10^6 cycles**	**Life to 50% failure rate, 10^6 cycles**	**Slope of Weibull plot**	Surface hardness, HRC
Carburized P/F-4600							
1120 °C, DA[(a)]	Upset	. . .	. . .	4.31	12.59	1.78	. . .
1120 °C, DA	Re-press	. . .	. . .	4.95	16.40	1.59	
1260 °C, DA	Upset	. . .	. . .	4.27	16.70	1.38	. . .
1260 °C, DA	Re-press	. . .	. . .	12.50	23.00	3.18	. . .
1120 °C, ENDO[(b)]	Upset	. . .	. . .	13.80	27.20	2.82	. . .
1120 °C, ENDO	Re-press	. . .	. . .	6.37	22.24	1.52	. . .
Through-hardened P/F-4600							
1120 °C, DA	Upset	0.81	220	5.77	9.70	3.66	. . .
1120 °C, DA	Re-press	0.81	210	6.35	11.16	3.35	. . .
1120 °C, DA	Upset	1.03	220	5.60	12.97	2.26	. . .
1120 °C, DA	Re-press	0.98	330	3.89	11.31	1.78	. . .
1260 °C, DA	Upset	0.79	75	11.62	17.61	4.58	. . .
1260 °C, DA	Re-press	0.78	85	9.00	18.38	2.66	. . .
1260 °C, DA	Upset	1.02	99	10.39	24.23	2.24	. . .

1260 °C, DA	Re-press	0.99	110	3.96	17.53	1.27	**. . .**
Carburized P/F-2000							
1120 °C	Upset	. . .	. . .	1.13	6.06	1.13	**64.0**
1120 °C	Re-press	. . .	. . .	1.34	5.30	1.38	**63.0**
1260 °C	Upset	. . .	. . .	2.79	8.28	1.74	**63.5**
1260 °C	Re-press	. . .	. . .	1.11	6.52	1.07	**63.0**
Through-hardened P/F-2000							
1120 °C	Upset	0.67	450	1.75	5.93	1.56	**60.5**
1120 °C	Re-press	0.70	460	1.97	6.28	1.64	**61.0**
1120 °C	Upset	0.84	345	0.59	3.14	1.14	**62.0**
1260 °C	Re-press	0.86	425	2.22	7.49	1.56	**61.0**
1260 °C	Upset	0.64	190	4.32	10.40	2.16	**. . .**
1260 °C	Re-press	0.66	160	3.45	9.55	1.86	**60.0**
1260 °C	Upset	0.84	200	4.04	11.53	1.81	**61.0**
1260 °C	**Re-press**	**0.84**	**195**	**2.54**	**11.16**	**1.28**	**61.0**

1120 °C = 2050 °F. 1260 °C = 2300 °F

(a) DA, dissociated ammonia.

(b) ENDO, endothermic atmosphere

Effect of Porosity on Mechanical Properties. The mechanical property data summarized in the previous sections are related to either hot re-press or hot upset forged pore-free material. The general effect of density on mechanical properties was illustrated in Fig. 2, and the properties of material incompletely densified because of forging at 870 °C (1600 °F) were presented in Table 2. The tensile and impact properties of P/F-4600 with two levels of residual porosity are summarized in Fig. 26 and 27. In one instance, the material was at a density of 7.84 g/cm^3 (0.283 lb/in.^{3}) and had a background of very fine porosity (Ref 78). The other series of samples had been purposely forged to a density of 7.7 g/cm^3 (0.278 lb/in.^{3}) (Ref 79). The performance of these materials is compared with that for pore-free samples at two levels of core hardness: 25 to 30 HRC (Fig. 26) and 45 to 50 HRC (Fig. 27). At the lower hardness, porosity has no effect on tensile strength, but even fine microporosity significantly reduces tensile ductility and impact strength. Tensile ductility at the higher core hardness is slightly influenced by the fine microporosity, and is significantly reduced for the material with a density of 7.7 g/cm^3 (0.278 lb/in.^{3}). The presence of porosity diminishes impact performance.

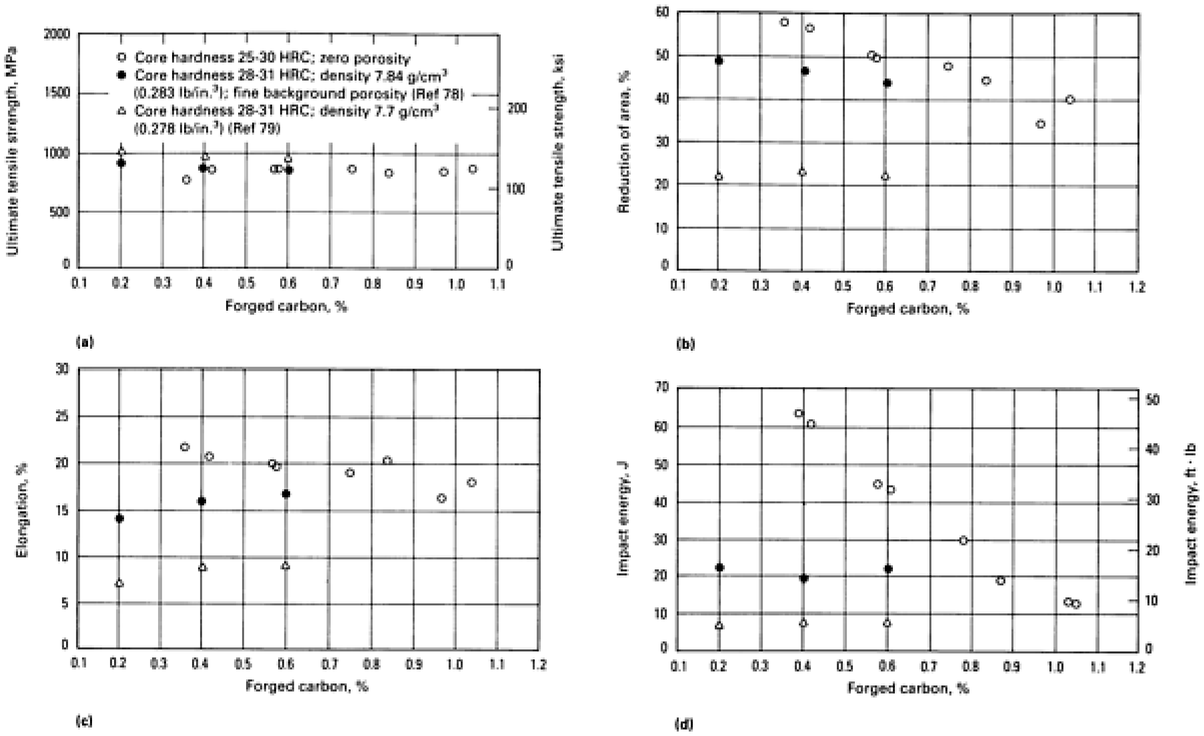

Fig. 26 Influence of density on the tensile and impact properties of P/F-4600 materials with core hardnesses of 25 to 30 HRC and 28 to 31 HRC. (a) Ultimate tensile strength. (b) Percent reduction of area. (c) Percent elongation. (d) Room-temperature impact energy. See also Fig. 27.

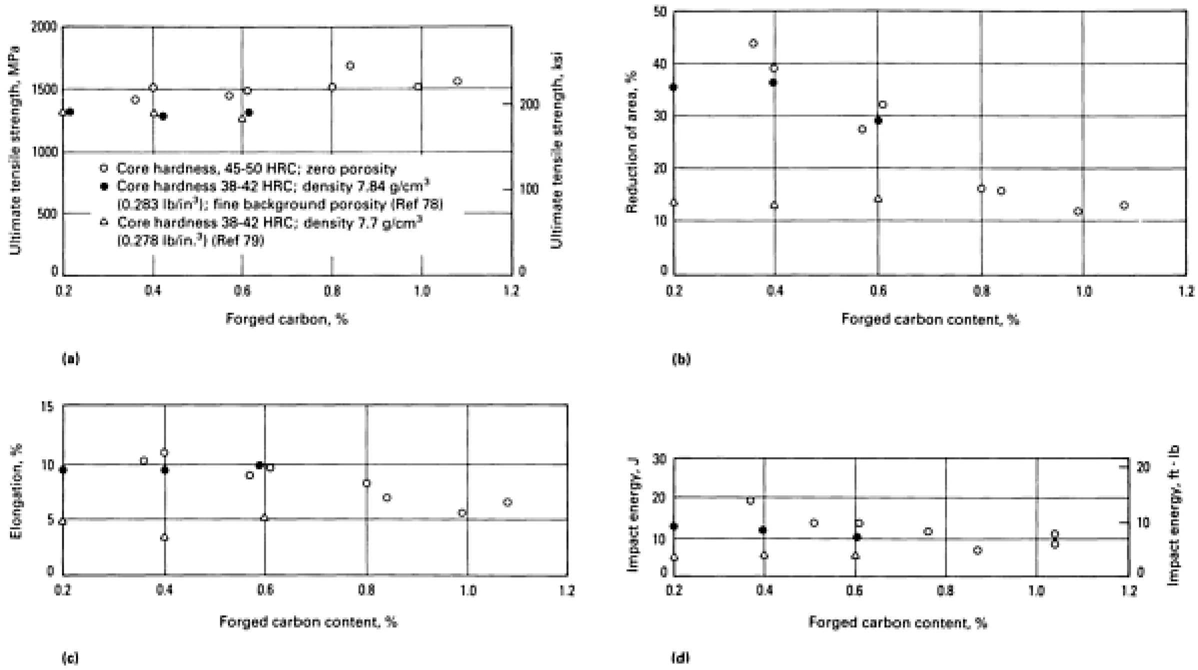

Fig. 27 Influence of density on the tensile and impact properties of P/F-4600 materials with core hardnesses of 38 to 42 HRC and 45 to 50 HRC. (a) Ultimate tensile strength. (b) Percent reduction of area. (c) Percent

elongation. (d) Room-temperature impact energy. See also Fig. 26.

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Powder Forging

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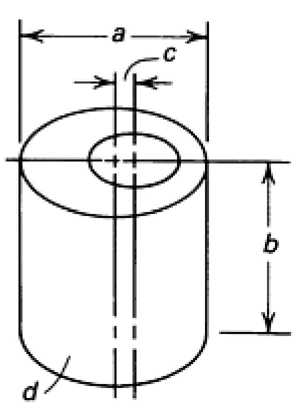
Quality Assurance for P/F Parts

Many of the quality assurance tests applied to wrought parts are similar to those used for powder forged parts. Among the parameters specified are: part dimensions, surface finish, magnetic particle inspection, composition, density, metallographic analysis, and nondestructive testing. These are discussed below.

Part Dimensions and Surface Finish. Typical tolerances for powder forged parts are summarized in Table 11. The as-forged surface finish of a powder forged part is directly related to the surface finish of the forging tool. Surface finish is generally better than 0.8 μm (32 μin.), which is better than that obtained on wrought forged parts. This good surface finish is beneficial to the fatigue performance of P/F parts.

Table 11 Typical tolerances for powder forged parts

Dimension or characteristic	Description	Typical tolerance		Minimum tolerance		
		mm/mm	in./in.	mm	in.	

a	Linear dimension perpendicular to the press axis	0.0025	0.0025	0.08	0.003	
b	Linear dimensions parallel to the press axis	±0.25	±0.10	0.20	0.008	
c	Concentricity of holes to external dimensions	. . .	. . .	0.10	0.004	
d	**Surface finish**	. . .	. . .	**Normally better than 0.8 μm (32 μin.)**		

Source: Ref 80

Magnetic particle inspection is used to detect surface blemishes such as cracks and laps.

Composition. Parts are generally designed to a specified composition. The forged carbon and oxygen contents are of particular interest. The specified carbon level is required to achieve the desired heat treatment response, and forged oxygen levels have a significant influence on dynamic properties (Fig. 28).

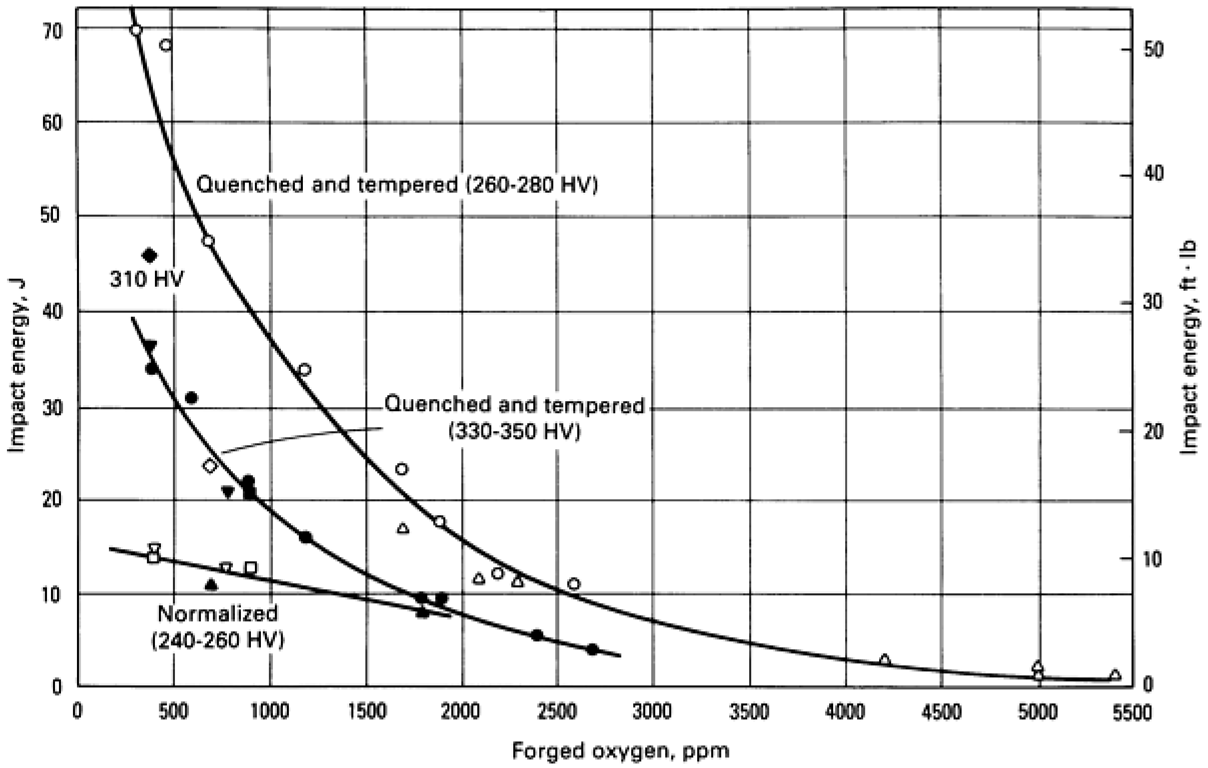


Fig. 28 Room-temperature impact energy as a function of forged oxygen content for various powder forged alloys. Heat treatments and hardnesses are indicated on the curves. Source: Ref 81.

Density. Sectional density measurements are taken to ensure that sufficient densification has been achieved in critical areas. Displacement density checks are generally supplemented by microstructural examination to assess the residual porosity level. For a given level of porosity, the measured density will depend on the exact chemistry, thermomechanical condition, and microstructure of the sample. Parts may be specified to have a higher density in particular regions than is necessary in less critical sections of the same component.

Metallographic Analysis. Powder forged parts are subjected to extensive metallographic evaluation. The primary parameters of interest include those discussed below.

The extent of surface decarburization permitted in a forged part will generally be specified. The depth of decarburization may be estimated by metallographic examination, but is best quantified using microhardness measurements as described in ASTM E 1077.

Surface finger oxides are defined as oxides that follow prior particle boundaries into the forged part from the surface and cannot be removed by physical means such as rotary tumbling. An example of surface finger oxides is shown in Fig. 29. Metallographic techniques are used to determine the maximum depth of surface finger oxide penetration.

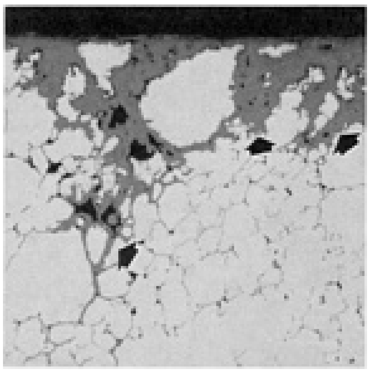


Fig. 29 Surface finger oxides (arrows at upper right) and interparticle oxide networks (arrow near lower left) in a powder forged material.

Interparticle oxides follow prior particle boundaries. They may sometimes form a continuous three-dimensional network but more often will, in a two-dimensional plane of polish, appear to be discontinuous. An example is presented in Fig. 29.

Most parts have what may be defined as functionally critical areas. The fabricator and end-user decide upon the maximum permissible depth of surface finger oxide penetration and whether oxide networks can be tolerated in critical regions. These decisions are then specified on the part drawing or in the purchase agreement.

The microstructure of a powder forged part depends on the thermal treatment applied after the forged part has been ejected from the die cavity. Most parts are carburized, quenched and stress relieved, or quenched and tempered. Other heat treatments used on wrought steels may also be applied to powder forged materials.

Iron powder contamination in low-alloy powder forged parts can be quantified by means of the etching procedure described in the section "Material Considerations" in this article.

The nonmetallic inclusion level in a powder forged part may also be quantified using the image analysis technique described in the section "Material Considerations." However, if the section of a component selected for inclusion assessment is not pore-free, image analysis procedures are not applicable (pores and oxide inclusions have similar gray level characteristics for feature detection). In fact, the presence of porosity makes it difficult for even visual quantitative determination of inclusion size.

Nondestructive Testing. Although metallographic assessment of powder forged parts is common, it is also useful to have a nondestructive method for evaluating the microstructural integrity of components. It has been demonstrated that this can be achieved with a magnetic bridge comparator.

Magnetic bridge sorting can be used to compare the eddy currents developed within a forging placed in a coil that carries an alternating current with the eddy currents produced in a randomly selected reference sample from the same forging batch (Ref 21). Differences are indicated by the displacement of a light spot from its balanced position in the center of the measuring screen of the system. If the part being tested is similar to the reference sample, the light spot returns to the center of the screen. The screen can be arbitrarily divided into a number of zones, as illustrated in Fig. 30. Testing of randomly selected samples can then be used to establish a typical frequency distribution of components within a forged batch relative to the reference sample.

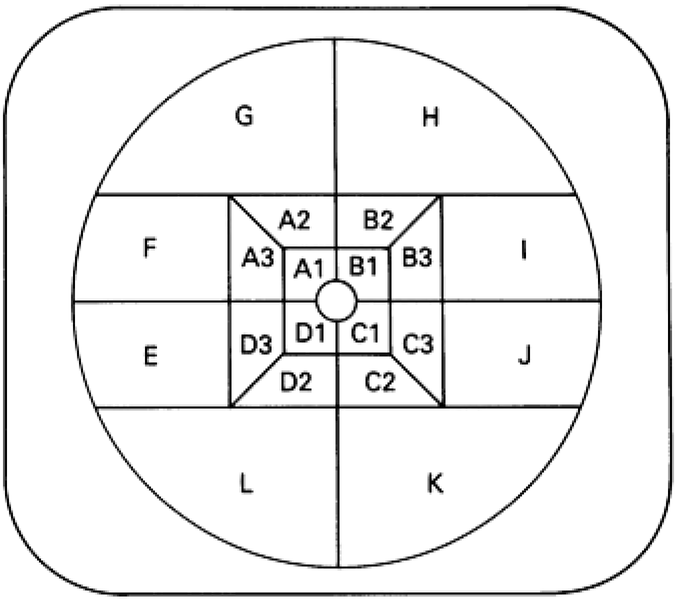


Fig. 30 Sorting grid categories arbitrarily assigned to the measuring screen of the magnetic bridge comparator. See text for details.

Once the frequency distribution has been established for a limited number of components within a forging batch, selected components that are representative of several zones on the screen are subjected to metallographic examination. Limited metallographic testing thus can be used to check the metallurgical integrity of parts from various zones.

Once acceptable zones have been defined, the entire forging batch can be assessed by means of the magnetic bridge. Components in unacceptable categories are automatically rejected. Experience with this technique minimizes the number of parts requiring sectioning for metallographic examination. Core hardness, surface decarburization, surface oxide penetration, and porosity can also be evaluated using this technique.

Magnetic bridge sorting, an adaptation of the technique used to test drop forged parts, enables potentially defective components to be eliminated from a batch of forgings. It also can be used to provide 100% inspection of the metallurgical integrity of a forging batch.

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Applications of Powder Forged Parts

Previous sections in this article compared powder forging and drop forging and illustrated the range of mechanical property performance that can be achieved in powder forged material. The various approaches to the powder forging process were reviewed, as was the influence of process parameters on the metallurgical integrity of the forged parts. The present section concentrates on examples of powder forged components and highlights some of the reasons for selecting powder forged parts over those made by competing forming methods.

Example 1: Converter Clutch Cam.

The automotive industry is the principal user of powder forged parts, and components for automatic transmissions represent the major area of application. One of the earliest powder forgings used in such an application is the converter clutch cam (Fig. 31). The primary reason powder forging was chosen over competitive processes was that it reduced manufacturing costs by 58%, compared with the conventional process of machining a forged gear blank. This cost saving resulted from substantially lower machining cost and lower total energy use.

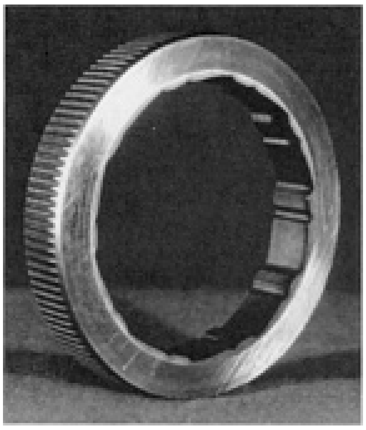

Fig. 31 Powder forged converter clutch cam used in an automotive automatic transmission. Courtesy of Precision Forged Products Division, Federal Mogul Corporation.

Powder forged cams are made from a water-atomized steel powder (P/F 2000) containing 0.6% Mo, 0.5% Ni, 0.3% Mn, and 0.3% graphite. Preforms weighing 0.33 kg (0.73 lb) are compacted to a density of 6.8 g/cm^3 (0.246 lb/in.^{3}). The preforms are sintered at 1120 °C (2050 °F) in an endothermic gas atmosphere with a +2 °C (+35 °F) dewpoint. The sintered preforms are graphite coated before being induction heated and forged to near full density (less than 0.2% porosity) using both axial and lateral flow. After forging, the face of the converter clutch cam is ground, carburized to a depth of 1.78 mm (0.070 in.), and surface hardened by means of induction. The part requires a high density to withstand the high Hertzian stress the inner cam surface experiences in service. Machining requires only one step on the P/F cam; seven machining operations were required for the conventionally processed part. Production of P/F cams began in 1971. Since then, well over 30 million P/F converter clutch cams have been made without a single service failure.

Example 2: Inner Cam/Race.

A part that illustrates the complex shapes that can be formed on both the inner and outer surfaces of a powder forged component is the inner cam/race shown in Fig. 32 (Ref 82). The part is the central member in an automotive automatic transmission torque converter centrifugal lock-up clutch.

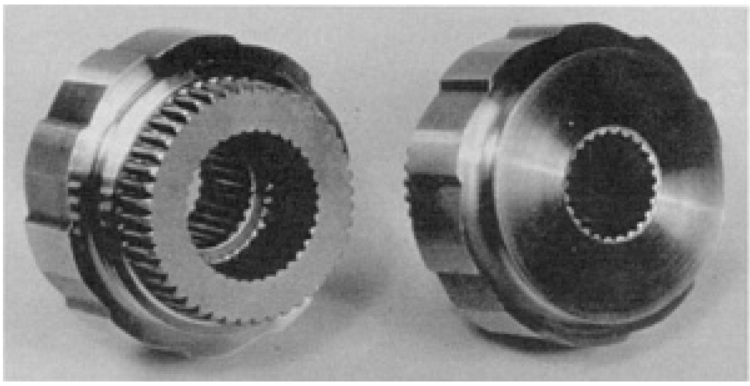

Fig. 32 Powder forged inner cam/race for an automotive automatic transmission. Courtesy of Precision Forged Products Division, Federal Mogul Corporation.

The inner cam/race is forged to a minimum density of 7.82 g/cm^3 (0.283 lb/in.^{3}) from a P/F-4662 material. The part has a minimum quenched and stress-relieved hardness of 58 HRC and a tensile strength of 2070 MPa (300 ksi). The application imposes high stresses on the cams and splines.

Example 3: Internal Ring Gear.

The powder forged internal ring gear shown in Fig. 33 is used in automatic transmissions for trucks with a maximum gross vehicle weight of 22,700 kg (50,000 lb) (Ref 83). The gear transmits 1355 N · m (1000 ft · lb) of torque through the gear and spline teeth.

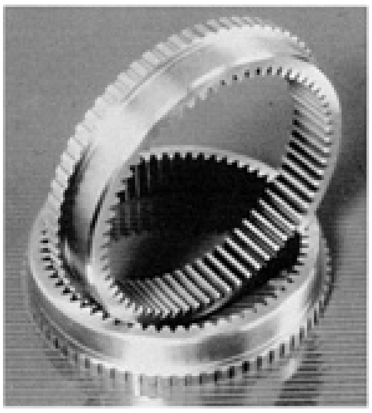

Fig. 33 Powder forged internal ring gear used in automatic transmission for trucks of up to 22,700 kg (50,000 lb) gross vehicle weight. Courtesy of Precision Forged Products Division, Federal Mogul Corporation.

Originally, the gear was produced by forging an AISI 5140M tubing blank. The conventionally forged blank required rough machining, gear tooth shaping, spline machining, core heat treating, carburizing, and deburring. The only secondary operations required on the powder forged part are surface grinding, hard turning, shot blasting, and vibratory tumbling.

The P/F-4618 ring gear is produced to a minimum density of 7.82 g/cm^3 (0.283 lb/in.^{3}). The part is selectively carburized using a proprietary process (Ref 84, 85, 86) and quench hardened. Minimum surface hardness is 57 HRC (2070 MPa, or 300 ksi, ultimate tensile strength), while the core hardness is 25 HRC (825 MPa, or 120 ksi, ultimate tensile strength). The internal gear teeth are produced to AGMA Class 7 tolerances.

Example 4: Powder Forged Tapered Bearing Race.

The use of powder forging for production of tapered roller bearing races has resulted in considerable cost savings. The economy of the P/F process results from material savings, elimination of machining, energy savings from the elimination of subsequent carburizing, and raw material inventory reduction.

In some cases, up to 80% of the material is lost to machining when a bearing race is produced from bar stock. Material savings resulting from powder forging average 50% on bearing cup and cone production. In the example shown in Fig. 34, a material savings of 1.25 kg (2.74 lb) is realized using powder forging; nearly 62% of the feedstock is wasted when this component is machined from hot rolled tube stock.

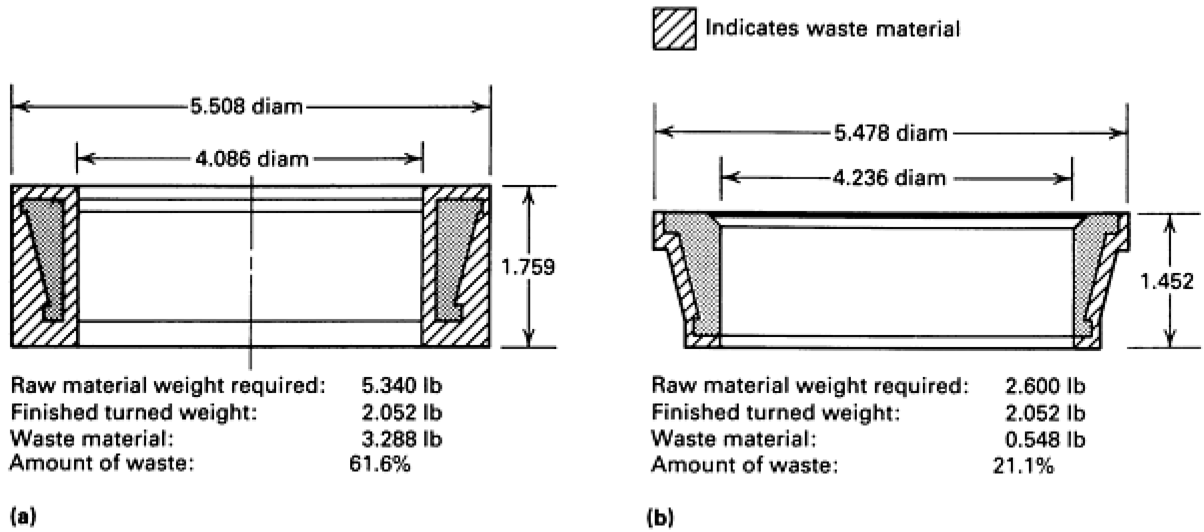

Fig. 34 Raw material utilization in the production of a tapered roller bearing race. (a) Produced from hot rolled tube stock. (b) Powder forged from preform. Source: Ref 87.

In addition to the cost savings, the fatigue life of powder forged cups and cones was found to be greater than that of similar cups produced from wrought bearing steels (Fig. 35).

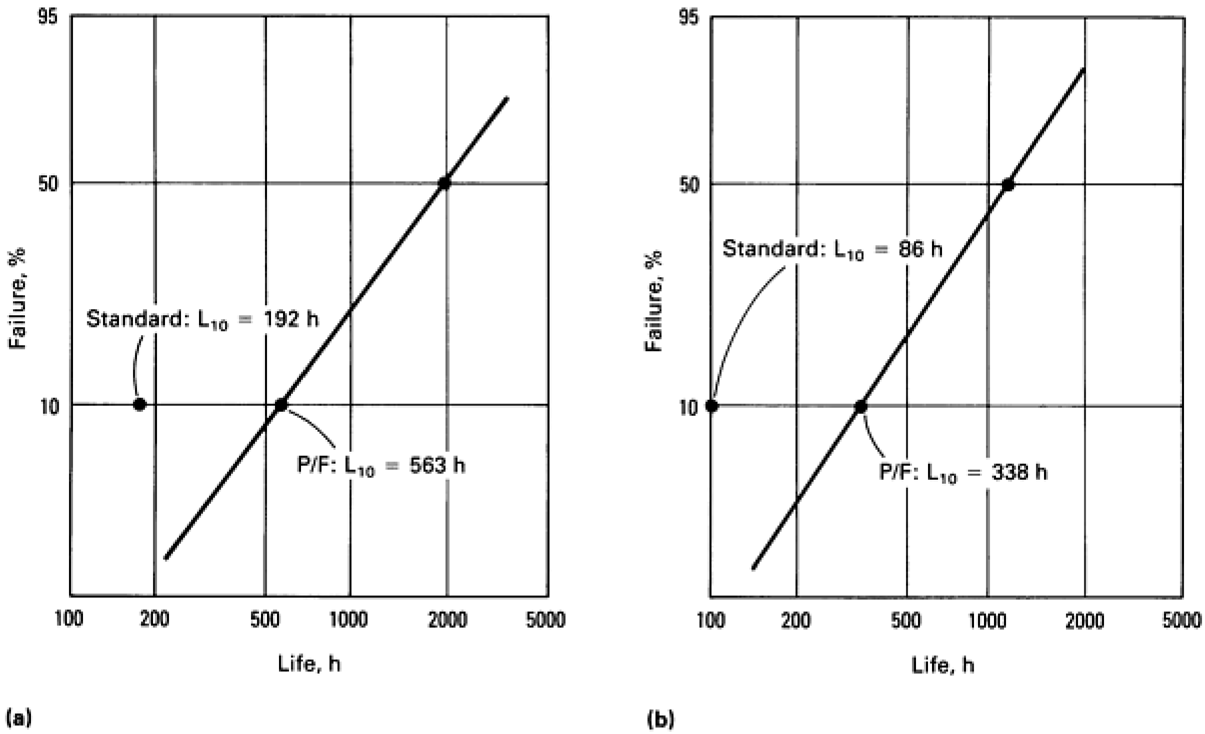

Fig. 35 Weibull plots of L_{10} life of P/F bearing races compared with L_{10} of wrought and machined races. (a) Cups. (b) Cones. Source: Ref 88.

Example 5: Powder Forged Connecting Rods.

Connecting rods were among the components selected for a number of powder forging development programs in the 1960s (Ref 5, 7, 18, 89, 90, 91, 92, 93). However, it was not until 1976 that the first powder forged connecting rod was produced commercially. This was the connecting rod for the Porsche 928 V-8 engine (Fig. 36a).

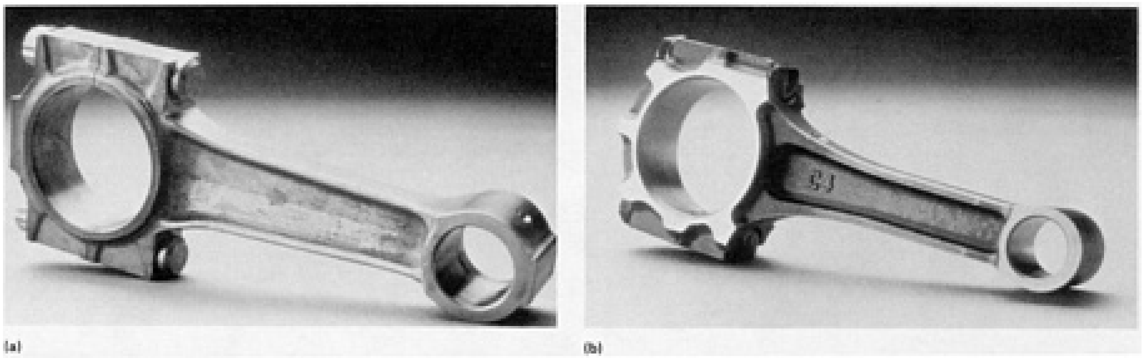

Fig. 36 Powder forged connecting rods. (a) Rod for Porsche 928 V-8 engine. Note reduced size of balance pads. Courtesy of Powder Forging Division, GKN Forgings. (b) Rod for Toyota 1.9 L engine; balance pads are completely eliminated.

The powder forged connecting rod for the Porsche 928 engine was made from a water-atomized low-alloy steel powder (0.3 to 0.4% Mn, 0.1 to 0.25% Cr, 0.2 to 0.3% Ni, and 0.25 to 0.35% Mo) to which graphite was added to give a forged carbon content of 0.35 to 0.45%. The forgings were oil quenched and tempered to a core hardness of 28 HRC (ultimate tensile strength of 835 to 960 MPa, or 121 to 139 ksi), followed by shot peening to a surface finish of 11 to 13 on the Almen scale.

The preform was designed such that the powder forged component had less than 0.2% porosity in the critical web region. The powder forged connecting rod had considerably better fatigue properties than did conventional drop forged rods. Its weight control was good enough to allow a reduction in the size of the balance pads (Fig. 36a), resulting in about a 10% weight saving (it weighed ~1 kg, or 2 lb). Powder forged connecting rods are currently used in both the Porsche 928 and 944 engines.

The first high-volume commercialization of powder forged connecting rods was in the 1.9 L Toyota Camry engine. In this design, the balance pads were completely eliminated (Fig. 36b). Despite the publication of the results of development trials in 1972 (Ref 91), it was not until the summer of 1981 that production rods were introduced (Ref 9, 93).

Toyota selected a copper steel (Fe-0.55C-2Cu) based on a water-atomized iron powder to replace conventional forgings, which had been made from a quenched and tempered 10L55 free-machining steel. The preform, which has a preshaped partial I-beam web section, has an average green density of 6.5 g/cm^3 (0.235 lb/in.^{3}). The preform shape is such that forging is predominantly in the re-pressing mode. However, some lateral flow does take place where required in critical regions, such as the web.

Preforms are sintered for 20 min at 1150 °C (2100 °F) in an endothermic gas atmosphere in a specially designed rotary hearth furnace. During sintering, the preforms are supported on flat, ceramic plates. The preforms are allowed to stabilize at about 1010 °C (1850 °F) before closed-die forging.

Exposure of the preform to the atmosphere during transfer to the forging dies is limited to 4 to 5 s. The forging tooling is illustrated in Fig. 37. An ion nitriding treatment is applied to the punches and dies in the regions at which forging deformation occurs (Ref 9). The connecting rods are forged at the rate of 10 per minute, and tool lives of over 100,000 pieces have been reported (Ref 94).

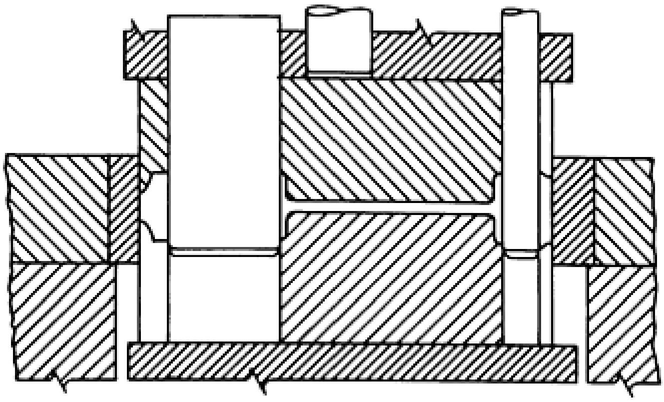

Fig. 37 Tooling used for powder forging of the Toyota connecting rod. Source: Ref 93.

The forged rods are subjected to a thermal treatment after forging. This results in a ferrite/pearlite microstructure with a core hardness of 240 to 300 HV (30 kgf load). Subsequent operations include burr removal, shot peening, straightening, sizing, magnetic particle inspection, and finish machining.

Savings in material and energy are substantial for the powder forged rods (Ref 9). Billet weight for conventional forging is 1.2 kg (2.65 lb); the powder forging preform weighs 0.7 kg (1.54 lb) and requires little machining. In addition to the benefits in process economics, the variability in fatigue performance for the powder forged rods is reported to be half that of conventionally forged parts (Ref 93).

Ford Motor Company has recently introduced powder forged connecting rods in the 1.9 L four-cylinder engine used in the Ford Escort and Mercury Lynx models. Ford has also announced plans to use powder forged rods in its modular engine, which is scheduled for production in 1992 (Ref 94).

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Introduction

CARBON AND ALLOY STEELS are by far the most commonly forged materials, and are readily forged into a wide variety of shapes using hot-, warm-, or cold-forging processes and standard equipment (see the Sections "Forging Processes" and "Forging Equipment and Dies" in this Volume). Despite the large number of available compositions, all of the materials in this category exhibit essentially similar forging characteristics. Exceptions to this are steels containing free-machining additives such as sulfides; these materials are more difficult to forge than are nonfree machining grades.

Generally, the hot forgeability of carbon and alloy steels improves as deformation rate increases. The improvement in workability has been primarily attributed to the increased heat of deformation generated at high deformation rates.

Selection of forging temperatures for carbon and alloy steels is based on carbon content, alloy composition, the temperature range for optimum plasticity, and the amount of reduction required to forge the workpiece. Of these factors, carbon content has the most influence on upper-limit forging temperatures. Table 1 lists the typical hot forging temperatures for a variety of carbon and alloy steels; it can be seen that, in general, forging temperatures decrease with increasing carbon and alloy content.

Table 1 Typical forging temperatures for various carbon and alloy steels

Steel	Major alloying elements	Typical forging temperature	
		°C	°F
Carbon steels			
1010	. . .	1315	2400
1015	. . .	1315	2400
1020	. . .	1290	2350
1030	. . .	1290	2350
1040	. . .	1260	2300
1050	. . .	1260	2300
1060	. . .	1180	2160
1070	. . .	1150	2100
1080	. . .	1205	2200

1095	. . .	1175	2150
Alloy steels			
4130	Chromium, molybdenum	1205	2200
4140	Chromium, molybdenum	1230	2250
4320	Nickel, chromium, molybdenum	1230	2250
4340	Nickel, chromium, molybdenum	1290	2350
4615	Nickel, molybdenum	1205	2200
5160	Chromium	1205	2200
6150	Chromium, vanadium	1215	2220
8620	Nickel, chromium, molybdenum	1230	2250
9310	Nickel, chromium, molybdenum	1230	2250

Source: Ref 1

Steels have been forged in quantity since near the beginning of the Industrial Revolution. Despite (or perhaps because of) this long history, the forging of steels is an intuitive, empirical process, and literature on the subject is relatively scarce. This article will attempt to present forgeability data for carbon and alloy steels whenever possible, and to provide some general guidelines for the forging of these materials. The thermomechanical processing of high-strength low-alloy (microalloyed) forging steels also will be discussed.

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Hot Forging Behavior

The hot forging of carbon and alloy steels into intricate shapes is rarely limited by forgeability aspects with the exception of the free-machining grades mentioned earlier. Section thickness, shape complexity, and forging size are limited primarily by the cooling that occurs when the heated workpiece comes into contact with the cold dies. For this reason equipment that has relatively short die contact times, such as hammers, is often preferred for forging intricate shapes in steel.

Forgeability

Hot-Twist Testing. One common means of measuring the forgeability of steels is the hot-twist test. As the name implies, this test involves twisting of heated bar specimens to fracture at a number of different temperatures selected to cover the possible hot working temperature range of the test material. The number of twists to fracture, as well as the torque required to maintain twisting at a constant rate, are reported. The temperature at which the number of twists is the greatest, if such a maximum exists, is assumed to be the optimal hot working temperature of the test material. Figure 1 shows forgeabilities of several carbon steels as determined by hot-twist testing. More information on the hot-twist test is available in Ref 2, 3, and 4.

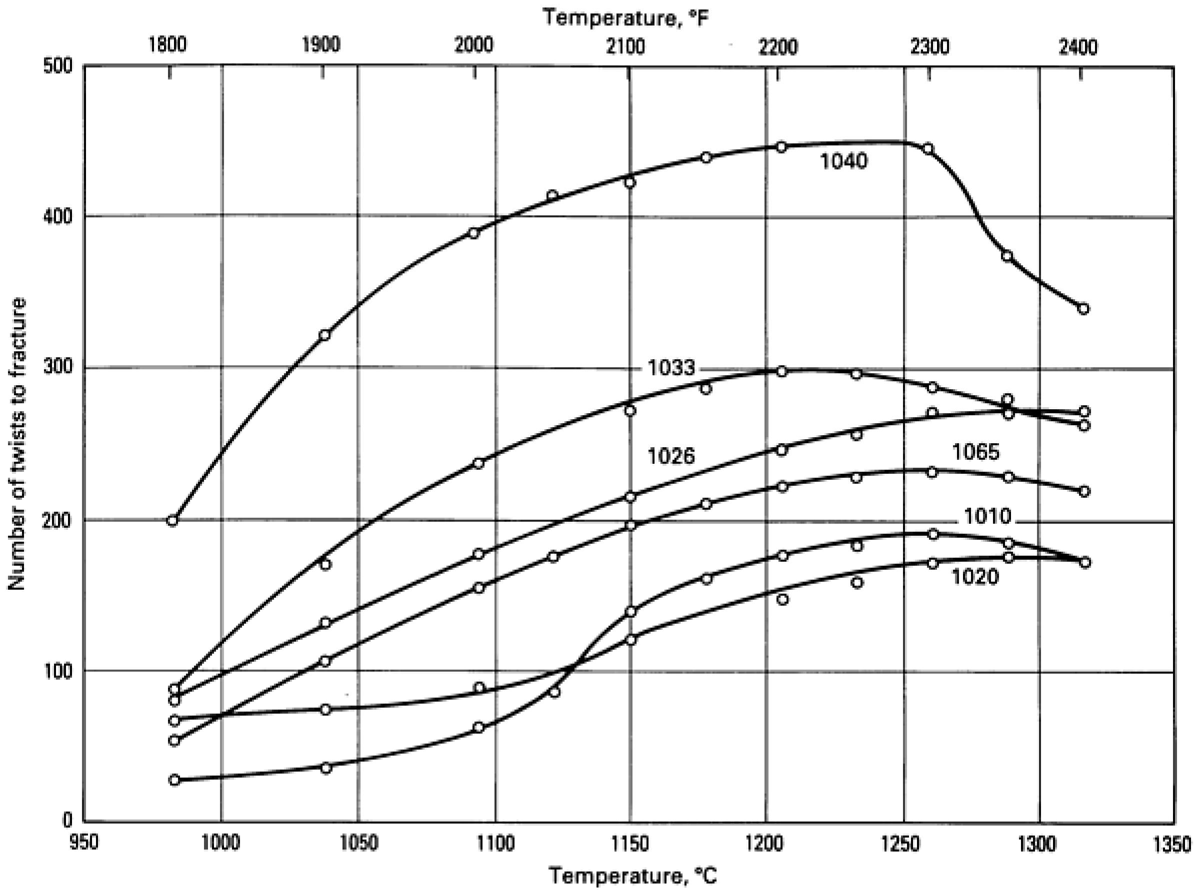

Fig. 1 Forgeabilities of various carbon steels as determined using hot-twist testing. Source: Ref 2.

Other Forgeability Tests. Numerous other tests are used to evaluate the forgeability of steels, including:

- *The wedge-forging test,* in which a wedge-shape specimen is forged between flat dies and the vertical deformation that causes cracking is established
- *The side-pressing test,* which consists of compressing a cylindrical bar specimen between flat, parallel dies with the axis of the cylinder parallel to the dies. The ends of the cylinder are unconstrained, and forgeability is measured by the amount of deformation obtained before cracking
- *The upset test,* in which a cylinder is compressed between flat dies and the surface strains at fracture at the equator of the cylinder are measured
- *The notched-bar upset test,* which is similar to the upset test except that axial notches are machined into the test specimen to introduce high local stress levels. These higher stresses may be more indicative of the stresses experienced during actual forging operations than those produced in the standard upset test
- *The hot tensile test,* which often uses a special test apparatus to vary both strain rates and temperatures over a wide range

More detailed information on these test procedures, as well as other techniques used to evaluate the bulk workability of materials, is available in the articles in the Section "Evaluation of Workability" in this Volume and in Ref 5 and 6.

Effect of Strain Rate on Forgeability. As previously stated, the forgeability of steels generally increases with increasing strain rate. This effect has been shown for low-carbon steel in hot-twist testing (Fig. 2), where the number of twists to failure increases with increasing twisting rate. It is believed that this improvement in forgeability at higher strain rates is due to the increased heat of deformation produced at higher strain rates. Excessive temperature increases from heat of deformation, however, may lead to incipient melting, which can lower forgeability and mechanical properties.

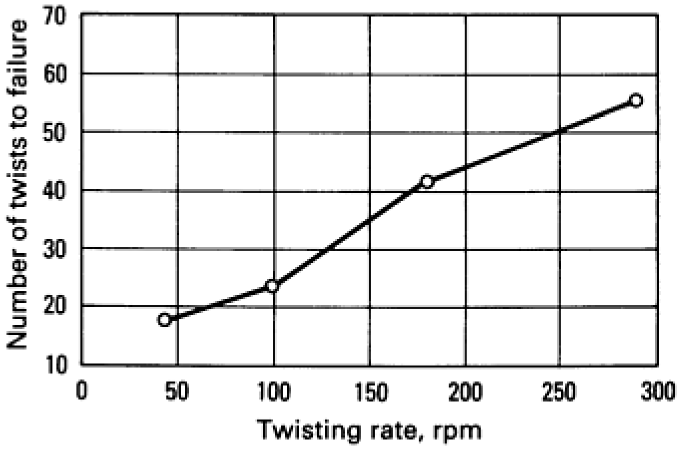

Fig. 2 Influence of deformation rate on hot-twist characteristics of low-carbon steels at 1095 °C (2000 °F). Source: Ref 7.

Flow Stress and Forging Pressure

Flow stresses and forging pressures can be obtained from torque curves generated in hot-twist tests or from hot-compression or tension testing. Figure 3 shows torque versus temperature curves for several carbon and alloy steels obtained from hot-twist testing. These data show that the relative forging pressure requirements for this group of alloys do not vary widely at normal hot-forging temperatures. A curve for AISI type 304 stainless steel is included to illustrate the effect of higher-alloy content on flow strength.

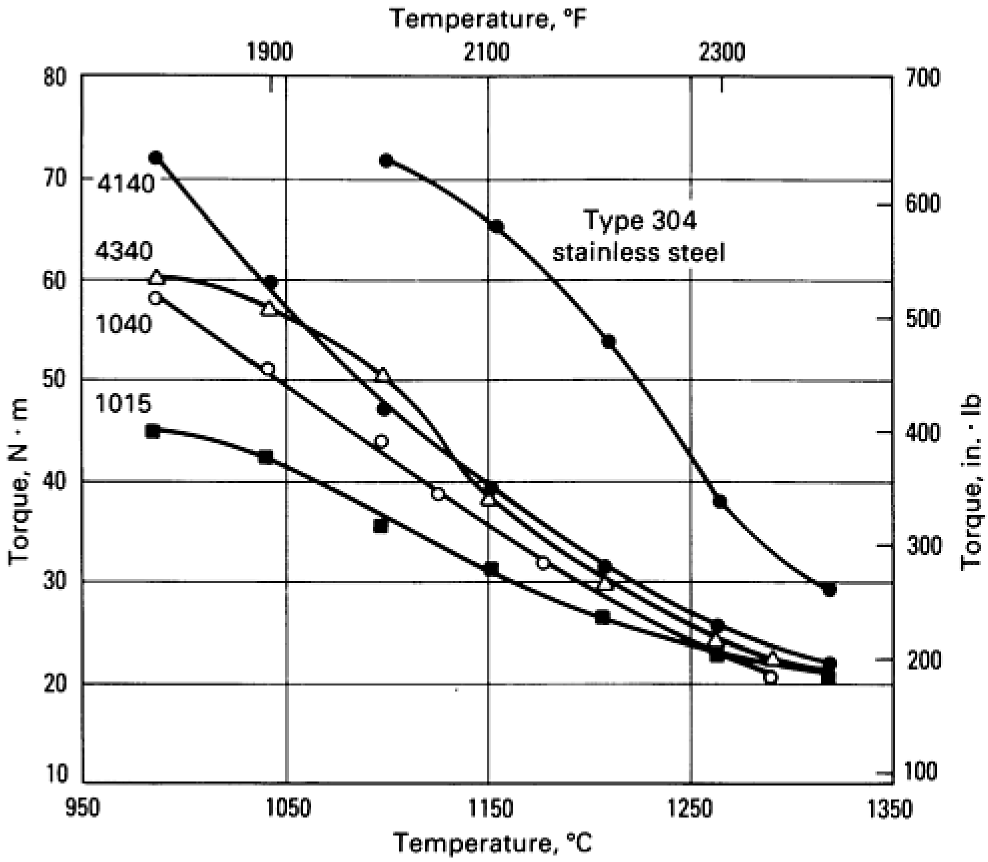

Fig. 3 Deformation resistance versus temperature for various carbon and alloy steels. Source: Ref 7.

Figure 4 shows actual forging pressure measurements for 1020 and 4340 steels and AISI A6 tool steel for reductions of 10 and 50%. Forging pressures for 1020 and 4340 vary only slightly at identical temperatures and strain rates. Considerably greater pressures are required for the more highly-alloyed A6 material, and this alloy also exhibits a more significant increase in forging pressure with increasing reduction.

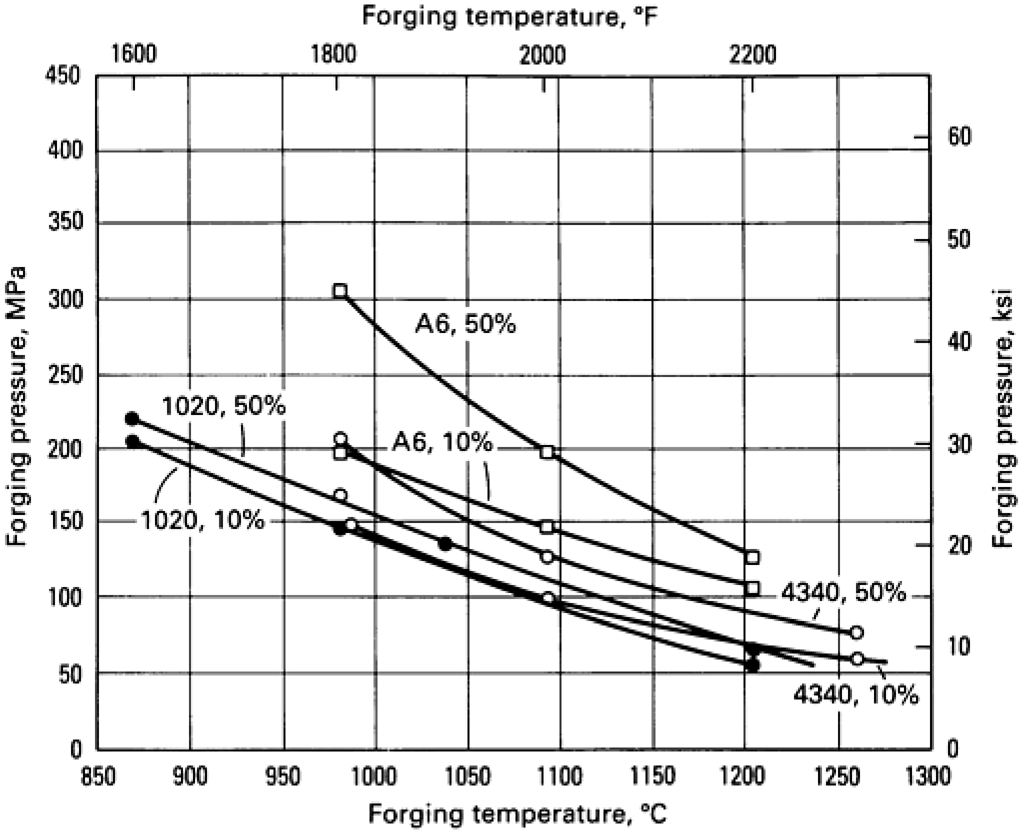

Fig. 4 Forging pressure versus temperature for three steels. Data are shown for reductions of 10 and 50%. Strain rate was constant at 0.7 s^{-1}. Source: Ref 9.

Effect of Strain Rate on Forging Pressure. Forging pressures required for a given steel increase with increasing strain rate. Studies of low-carbon steel (Ref 8) indicate that the influence of strain rate is more pronounced at higher forging temperatures. This effect is illustrated in Fig. 5, which gives stress-strain curves for a low-carbon steel forged at various temperatures and strain rates.

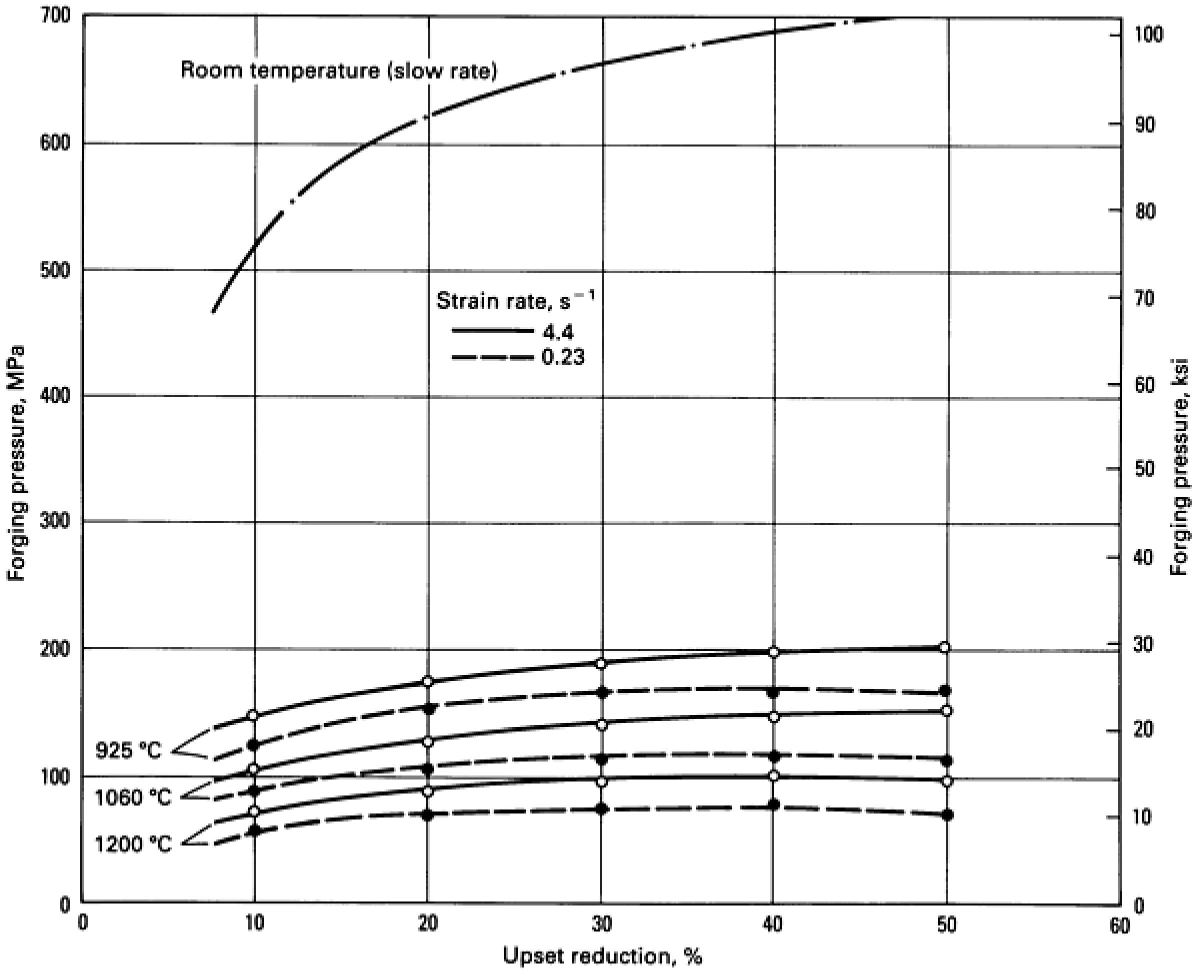

Fig. 5 Forging pressure for low-carbon steel upset at various temperatures and two strain rates. Source: Ref 8.

Similar effects have been observed in alloy steels. Figure 6 shows the forging pressures required upset 4340 steel at several temperatures and strain rates.

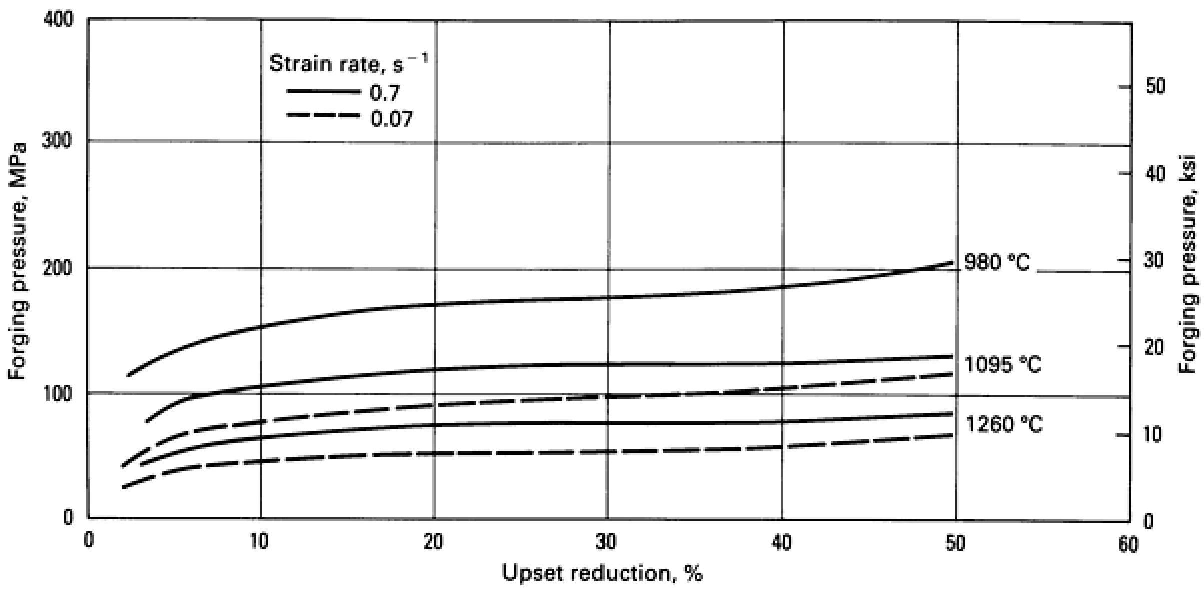

Fig. 6 Forging pressure for AISI 4340 steel upset at various temperatures and two strain rates. Source: Ref 9.

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Effects of Forging on Properties

The shaping of a complex configuration from a carbon or alloy steel bar or billet requires first that the steel be "arranged" into a suitable starting shape (preformed) and then that it be caused to flow into the final part configuration. This rearrangement of the metal has little effect on hardness and strength of the steel, but certain mechanical properties, such as ductility, impact strength, and fatigue strength, are enhanced. This improvement in properties is thought to take place because forging:

- Breaks up segregation, heals porosity, and aids homogenization
- Produces a fibrous grain structure (Fig. 7) that enhances mechanical properties parallel to the grain flow
- Reduces as-cast grain size

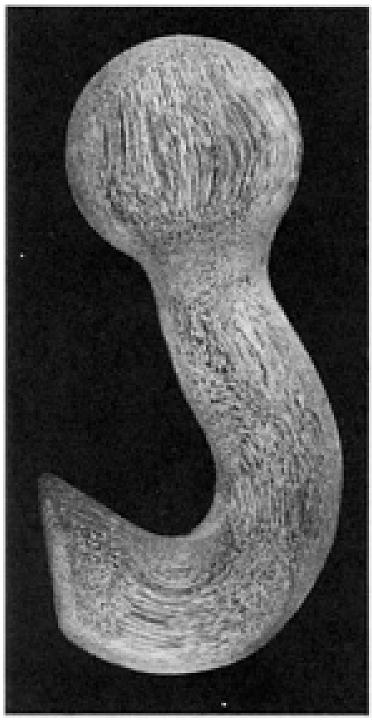

Fig. 7 4140 steel forged hook showing fibrous structure (flow lines) resulting from hot forging. Etched using 50% hot aqueous HCl. 0.5×

Typical improvements in ductility and impact strength of heat-treated steels as a function of forging reduction are shown in Fig. 8 and 9. These data illustrate that maximum improvement in each case occurs in the direction of maximum elongation.Toughness and ductility reach maximums after a certain amount of reduction, after which further reduction is of little value.

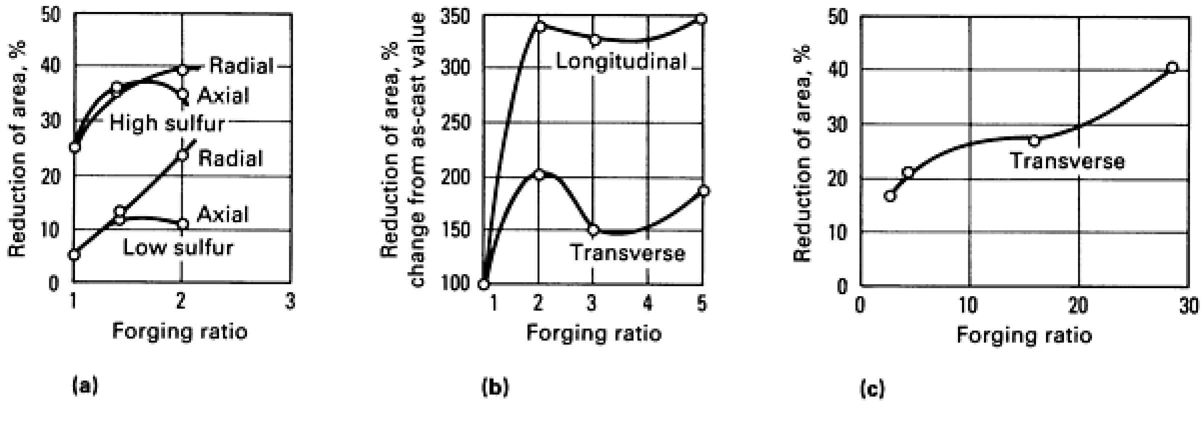

Fig. 8 Effect of forging ratio on reduction of area of heat-treated steels. (a) 4340 steel at two sulfur levels. (b) Manganese steel. (c) Vacuum melted 4340 with ultimate tensile strength of 2000 MPa (290 ksi). Forging ratio is ratio of final cross-sectional area to initial cross-sectional area. Source: Ref 8, 10, and 11.

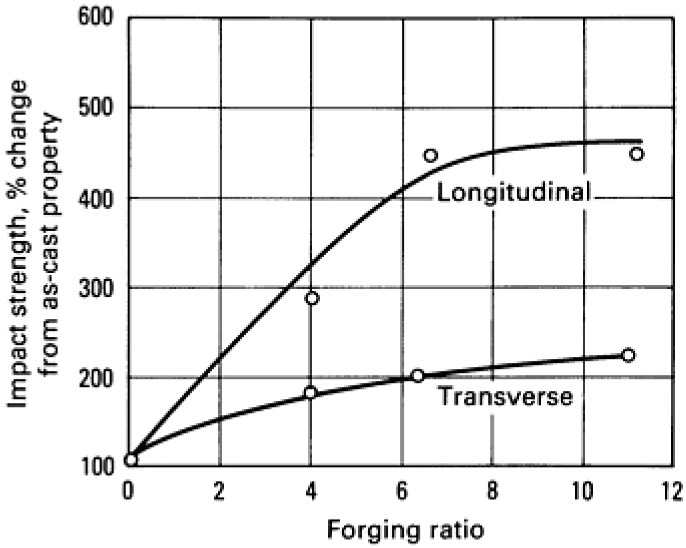

Fig. 9 Effect of hot-working reduction on impact strength of heat-treated nickel-chromium steel. Forging ratio is the ratio of initial cross-sectional area to final cross-sectional area. Source: Ref 12.

The typical longitudinal mechanical properties of low- and medium-carbon steel forgings in the annealed, normalized, and quenched and tempered conditions are listed in Table 2. As might be expected, strength increases with increasing carbon content, while ductility decreases.

Table 2 Longitudinal properties of carbon steel forgings at four carbon contents

Carbon content, %	Ultimate tensile strength		Yield strength, 0.2% offset		Elongation, %	Reduction of area, %	Fatigue strength[a]		Hardness, HB
	MPa	ksi	MPa	ksi			MPa	ksi	
Annealed									
0.24	438	63.5	201	29.1	39.0	59	185	26.9	**122**
0.30	483	70.0	245	35.6	31.5	58	193	28.0	**134**
0.35	555	80.5	279	40.5	24.5	39	224	32.5	**157**
0.45	634	92.0	348	50.5	24.0	42	248	35.9	**180**
Normalized									
0.24	483	70.0	247	35.8	34.0	56.5	193	28.0	**134**
0.30	521	75.5	276	40.0	28.0	44	209	30.3	**148**

0.35	579	84.0	303	44.0	23.0	36	232	33.6	**164**
0.45	690	100.0	355	51.5	22.0	36	255	37.0	**196**
Oil quenched and tempered at 595 °C (1100 °F)									
0.24	500	72.5	305	44.2	35.5	62	193	28.0	**144**
0.30	552	80.0	301	43.7	27.0	52	224	32.5	**157**
0.35	669	97.0	414	60.0	26.5	49	247	35.8	**190**
0.45	**724**	**105.0**	**386**	**56.0**	**19.0**	**31**	**277**	**40.2**	**206**

Source: Ref 13

(a) Rotating beam test at 10^7 endurance limit.

It should be recognized that closed-die forgings for the most part are made from wrought billets that have received considerable prior working. Open-die forgings, however, may be made from either wrought billets or as-cast ingots. Metal flows in various directions during closed-die forging. For example, in the forging of a rib and web shape such as an airframe component, nearly all metal flow is in the transverse direction. Such transverse flow improves ductility in that direction with little or no reduction in longitudinal ductility. Transverse ductility could conceivably equal or surpass longitudinal ductility if forging reductions were large enough and if metal flow were primarily in the transverse direction.

Similar effects are observed in the upsetting of wrought billets. In this case, however, the original longitudinal axis of the material is shortened by upsetting, and lateral displacement of metal is in the radial direction. When upset reductions exceed about 50%, ductility in the radial direction usually exceeds that in the axial direction (Fig. 10).

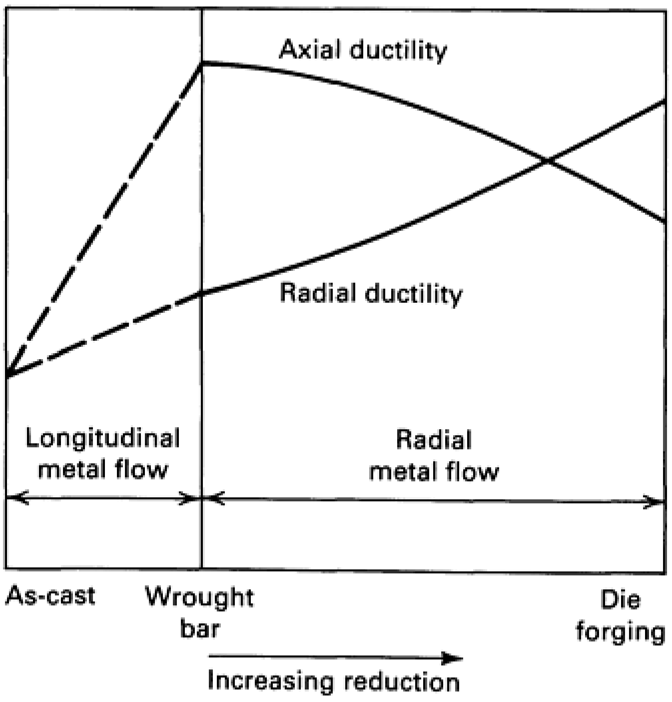

Fig. 10 Typical influence of upset reduction on axial and radial ductility of forged steels.

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Forging Lubricants (Ref 14)

For many years, oil-graphite mixtures were the most commonly used lubricants for forging carbon and alloy steels. Recent advances in lubricant technology, however, have resulted in new types of lubricants, including water/graphite mixtures and water-base synthetic lubricants. Each of the commonly used lubricants has advantages and limitations (Table 3) that must be balanced against process requirements.

Table 3 Advantages and limitations of the principal lubricants used in the hot forging of steels

Type of lubricant	**Advantages**	Limitations

Water-base micro-graphite	Eliminates smoke and fire; provides die cooling; is easily extended with water	**Must be applied by spraying for best results**
Water-base synthetic	Eliminates smoke and fire; is cleaner than oils or water-base graphite; aids die cooling; is easily diluted, and needs no agitation after initial mixing; reduces clogging of spray equipment; does not transfer dark pigment to part	**Must be sprayed; lacks the lubricity of graphite for severe forging operations**
Oil-base graphite	**Fluid film lends itself to either spray or swab application; has good performance over a wide temperature range (up to 540 °C, or 1000 °F)**	**Generates smoke, fire, and noxious odors; explosive nature may shorten die life; has potentially serious health and safety implications for workers**

Source: Ref 14

Selection Criteria. Lubricant selection for forging is based on several factors, including forging temperature, die temperature, forging equipment, method of lubricant application, complexity of the part being forged, and environmental and safety considerations. At normal hot-forging temperatures for carbon and alloy steels, water-base graphite lubricants are used almost exclusively, although some hammer shops may still employ oil-base graphite.

The most common warm-forming temperature range for carbon and alloy steels is 540 to 870 °C (1000 to 1500 °F). Because of the severity of forging conditions at these temperatures, billet coatings are often used in conjunction with die lubricants. The billet coatings used include graphite in a fluid carrier or water-base coatings used in conjunction with phosphate conversion coating of the workpiece.

For still lower forging temperatures (less than about 400 °C, or 750 °F), molybdenum disulfide has a greater load-carrying capacity than does graphite. Molybdenum disulfide can either be applied in solid form or dispersed in a fluid carrier. More information on lubricant chemistry, application, and selection is available in Ref 14.

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Forging of Carbon and Alloy Steels

Steels for Forging

Carbon and alloy steel ingots, blooms, billets, and slabs for forging are hot rolled or cast to approximate cross sectional dimensions; therefore, straightness, camber, twist, and flatness tolerances do not apply. Semifinished steel products for forging are produced to either specified piece weights or specified lengths.

Surface Conditioning. Semifinished steel products for forging can be conditioned by scarfing, chipping, or grinding to remove or minimize surface imperfections. It should be kept in mind that, regardless of surface conditioning, the product is still likely to contain some surface imperfections.

Weight tolerances for billets, blooms, and slabs are often ±5% for individual pieces or for lots weighing less than 18 Mg (20 tons). Lots weighing more than that are frequently subject to weight tolerances of ±2.5%.

Cutting. Semifinished steel products for forging are generally cut to length by hot shearing. Depending on the steel composition, hot sawing or flame cutting may also be used.

Quality, as the term is applied to semifinished steel products for forging, is dependent on many different factors, including the degree of internal soundness, relative uniformity of chemical composition, and relative freedom from surface imperfections.

Forging quality semifinished steel is used in hot forging applications that may involve subsequent heat treatment or machining operations. Such applications require relatively close control of chemical composition and steel manufacture. Forging-quality carbon and alloy steel products are produced to the guidelines described in Ref 15.

Powder metallurgy (P/M) steels are also forged from both sintered preforms and green (unsintered) preforms. Detailed information on the forging of P/M steels and the properties of the resulting products is available in the article "Powder Forging" in this Volume.

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Heat Treatment of Carbon and Alloy Steel Forgings (Ref 16)

Usually steel forgings are specified by the purchaser in one of four principal conditions: as forged with no further thermal processing; heat treated for machinability; heat treated for final mechanical/physical properties; or specially heat treated to enhance dimensional stability, particularly in more complex part configurations.

As Forged. Although the vast majority of steel forgings are heat treated before use, a large tonnage of low-carbon steel (0.10 to 0.25% C) is used in the as-forged condition. In such forgings, machinability is good, and little is gained in terms of strength by heat treatment. In fact, a number of widely used ASTM and federal specifications permit this economic option. It is also interesting to note that, compared to the properties produced by normalizing, strength and machinability are slightly better, which is most likely attributable to the fact that grain size is somewhat coarser than in the normalized condition.

Heat Treated for Machinability. When a finished machined component must be produced from a roughly dimensioned forging, machinability becomes a vital consideration to optimize tool life, increase productivity, or both. The purchase specification or forging drawing may specify the heat treatment. However, when specifications give only maximum hardness or microstructural specifications, the most economical and effective thermal cycle must be selected. Available heat treatments include full anneal, spheroidize anneal, subcritical anneal, normalize, or normalize and temper. The heat treatment chosen depends on the steel composition and the machine operations to be performed. Some steel grades are inherently soft, others become quite hard in cooling from the finishing temperature after hot forging. Some type of annealing is usually required or specified to improve machinability.

Heat Treated to Final Physical Properties. Normalizing or normalizing and tempering may produce the required minimum hardness and minimum ultimate tensile strength. However, for most steels, a hardening (austenitize) and quenching (in oil, water, or some other medium, depending on section size and hardenability) cycle is employed, followed by tempering to produce the proper hardness, strength, ductility, and impact properties. For steel forgings to be heat treated above the 1034 MPa (150 ksi) strength level and having section size variations, it is general practice to normalize before austenitizing to produce a uniform grain size and minimize internal residual stresses. In some instances, it is common practice to use the heat for forging as the austenitizing cycle and to quench at the forge unit. The forging is then tempered to complete the heat treat cycle. Although there are obvious limitations to this procedure, definite economies are possible when the procedure is applicable (usually for symmetrical shapes of carbon steels that require little final machining).

Special heat treatments are sometimes used to control dimensional distortion, relieve residual stresses before or after machining operations, avoid quench cracking, or prevent thermal shock or surface (case) hardening. Although most of the heat-treating cycles discussed above can apply, very specific treatments may be required. Such treatments usually apply to complex forging configurations with adjacent differences in section thickness, or to very high hardenability steels and alloys. When stability of critically dimensioned finished parts permits only light machining of the forging after heat treatment to final properties, special treatments are available, including marquenching (martempering), stress relieving, and multiple tempering.

Many applications, such as crankshafts, camshafts, gears, forged rolls, rings, certain bearings, and other machinery components, require increased surface hardness for wear resistance. The important surfaces are usually hardened after machining by flame or induction hardening, carburizing, carbonitriding, or nitriding. These processes are listed in the approximate order of increasing cost and decreasing maximum temperature. The latter consideration is important in that dimensional distortion usually decreases with decreasing temperature. This is particularly true of nitriding, which is usually performed below the tempering temperature for the steel used in the forging. Detailed information on heat treatment practices for carbon and alloy steels is available in *Heat Treating*, Volume 4 of the *ASM Handbook.*

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Microalloyed Forging Steels

Microalloying--the use of small amounts of elements such as vanadium and niobium to strengthen steels--has been in practice since the 1960s to control the microstructure and properties of low-carbon steels (Ref 17). Most of the early developments were related to plate and sheet products in which microalloy precipitation, controlled rolling, and modern steelmaking technology combined to increase strength significantly relative to that of low-carbon steels.

The application of microalloying technology to forging steels has lagged behind that of flat-rolled products because of the different property requirements and thermomechanical processing of forging steels. Forging steels are commonly used in applications in which high strength, fatigue resistance, and wear resistance are required. These requirements are most often filled by medium-carbon steels. Thus, the development of microalloyed forging steels has centered around grades containing 0.30 to 0.50% C.

The driving force behind the development of microalloyed forging steels has been the need to reduce manufacturing costs. This is accomplished in these materials by means of a simplified thermomechanical treatment (that is, a controlled cooling following hot forging) that achieves the desired properties without the separate quenching and tempering treatments required by conventional carbon and alloy steels. In Fig. 11 the processing sequence for conventional (quenched and tempered) steels is compared with the microalloyed steel-forging process.

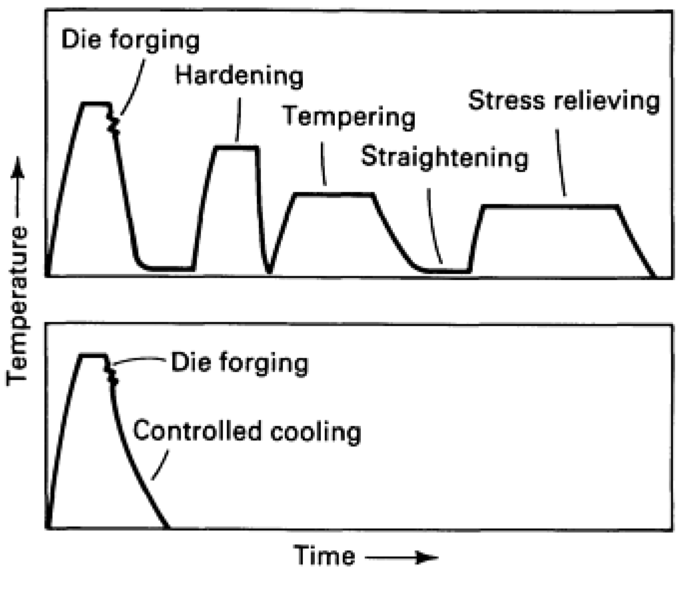

Fig. 11 Processing cycles for conventional (quenched and tempered; top) and microalloyed steels (bottom). Source: Ref 26.

Effects of Microalloying Elements (Ref 18)

Carbon. Most of the microalloyed steels developed for forging have carbon contents ranging from 0.30 to 0.50%, which is high enough to form a large amount of pearlite. The pearlite is responsible for substantial strengthening. This level of carbon also decreases the solubility of the microalloying constituents in austenite.

Niobium, Vanadium, and Titanium. Formation of carbonitride precipitates is the other major strengthening mechanism of microalloyed forging steels. Vanadium, in amounts ranging from 0.05 to 0.2%, is the most common microalloying addition used in forging steels. Niobium and titanium enhance strength and toughness by providing control of austenite grain size. Often niobium is used in combination with vanadium to obtain the benefits of austenite grain size control (from niobium) and carbonitride precipitation (from vanadium).

Manganese is used in relatively large amounts (1.4 to 1.5%) in many microalloyed forging steels. It tends to reduce the cementite plate thickness while maintaining the interlamellar spacing of pearlite developed (Ref 19); thus, high manganese levels require lower carbon contents to retain the large amounts of pearlite required for high hardness. Manganese also provides substantial solid solution strengthening, enhances the solubility of vanadium carbonitrides, and lowers the solvus temperature for these phases.

The silicon content of most commercial microalloyed forging steels is about 0.30%; some grades contain up to 0.70% (Ref 20). Higher silicon contents are associated with significantly higher toughness, apparently because of an increased amount of ferrite relative to that formed in ferrite-pearlite steels with lower silicon contents.

Sulfur. Many microalloyed forging steels, particularly those destined for use in automotive forgings in which machinability is critical, have relatively high sulfur contents. The higher sulfur contents contribute to their machinability, which is comparable to that of quenched and tempered steels (Ref 21, 22).

Aluminum and Nitrogen. As in hardenable fine-grain steels, aluminum is important for austenite grain size control in microalloyed steels (Ref 19). The mechanism of aluminum grain size control is the formation of aluminum nitride particles. It has been shown that nitrogen is the major interstitial component of vanadium carbonitride (Ref 23). For this reason, moderate to high nitrogen contents are required in vanadium-containing microalloyed steels to promote effective precipitate strengthening.

Controlled Forging (Ref 24)

The concept of grain size control has been used for many years in the production of flat-rolled products. Particularly in plate rolling, the ability to increase austenite recrystallization temperature using small niobium additions is well known; the process used to produce these steels is usually referred to as controlled rolling (see the article "Flat, Bar, and Shape Rolling" in this Volume).

The benefits of austenite grain size control are not, of course, limited to flat-rolled products. Although the higher finishing temperatures required for rolling of bars limit the usefulness of this approach to microstructural control, finishing temperatures for microalloyed bar steels must nonetheless be controlled. It has been shown that, although strength is not significantly affected by finishing temperature, toughness of vanadium-containing microalloyed steels decreases with increasing finishing temperature (Ref 25, 26). This effect is shown in Fig. 12, which compares Charpy V-notch impact strength for a microalloyed 1541 steel finished at three temperatures. This detrimental effect of a high finishing temperature on impact toughness also carries over to forging operations, that is, the lower the finish temperature in forging, the higher the resulting toughness, and vice versa. After extensive testing, the investigators in Ref 26 recommended that finishing temperature for forging be reduced to near 1000 °C (1800 °F). Such treatment resulted in impact properties equal to or better than those of hot-rolled bar (Ref 26). The same investigators concluded that rapid induction preheating was beneficial for microalloyed forging steels, and that cost savings of 10% (for standard microalloyed forgings) to 20% (for resulfurized grades) were possible.

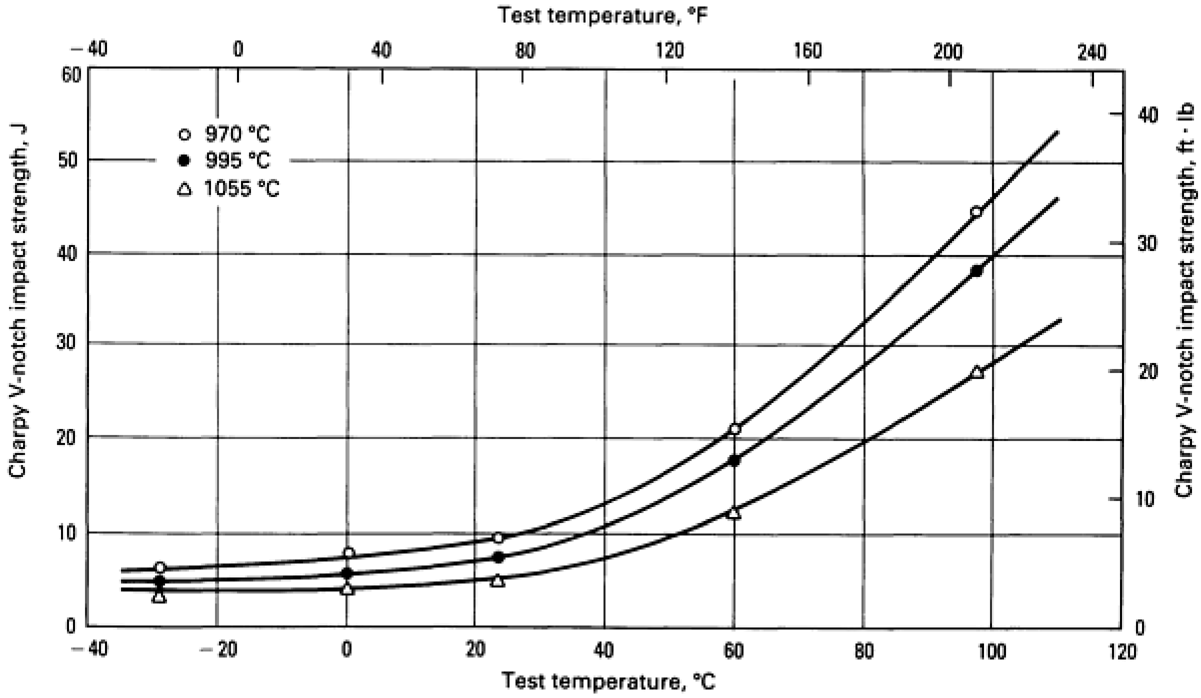

Fig. 12 Effect of hot finishing temperature on impact strength of microalloyed 1541 steel (AISI 1541 plus 0.10% V). Source: Ref 25.

Lower finishing temperatures, however, take their toll in terms of higher required forging pressures (and thus higher machine capacities needed) and increased die wear. The improved toughness resulting from lower finishing temperatures, as well as any cost savings that may be achieved as a result of the elimination of heat treatment, must be weighed against the cost increases caused by these factors.

Microalloyed Cold Heading Steels

Steels used in the production of high-strength fasteners by cold heading were previously produced from quenched and tempered alloy steels. To obtain sufficient strength with adequate ductility required six processing steps. Recent developments have led to the use of microalloyed niobium-boron steels that require no heat treatment (Ref 27). These steels make use of niobium and boron additions to develop bainitic structures with high work-hardening rates. In most cases they use the deformation of cold heading to achieve the required strength levels without heat treatment. Table 4 lists the compositions and selected properties of these materials.

Table 4 Compositions and selected properties of three microalloyed cold heading steels

Steel	**Nominal composition, %**	**Yield strength**		**Ultimate tensile strength**		**Elongation, %**	Reduction of area, %
		MPa	**ksi**	**MPa**	**ksi**		
Grade 1	Fe-0.20C-1.2Mn-25-50 ppm B	350	51	600	87	35	**68**

Grade 3	**Fe-0.12C-1.6Mn-0.08Nb-25-50 ppm B**	**550**	**80**	**720**	**104**	**23**	**62**

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Forging of Stainless Steel

Revised by Thomas Harris and Eugene Priebe, Armco Inc.

Introduction

STAINLESS STEELS, based on forging pressure and load requirements, are considerably more difficult to forge than carbon or low-alloy steels, primarily because of the greater strength of stainless steels at elevated temperatures and the limitations on the maximum temperatures at which stainless steels can be forged without incurring microstructural damage. Forging load requirements and forgeability vary widely among stainless steels of different types and compositions; the most difficult alloys to forge are those with the greatest strength at elevated temperatures.

Forging Methods

Open-die, closed-die, upset and roll forging, and ring rolling are among the methods used to forge stainless steel. As in the forging of other metals, two of these methods are sometimes used in sequence to produce a desired shape.

Open-die forging (hand forging) is often used for smaller quantities for which the cost of closed dies cannot be justified and in cases in which delivery requirements dictate shortened lead times. Generally, products include round bars, blanks, hubs, disks, thick-wall rings, and square or rectangular blocks or slabs in virtually all stainless grades. Forged stainless steel round bar can also be produced to close tolerances on radial forge machines.

Although massive forgings are normally associated with open-die forging, most stainless steel open-die forgings are produced in the range of 10 to 900 kg (25 to 2000 lb). Additional information on product types is available in the article "Open-Die Forging" in this Volume.

Closed-die forging is extensively applied to stainless steel in order to produce blocker-type, conventional, and close-tolerance forgings. Selection from the above closed-die types invariably depends on quantity and the cost of the finished part. Additional information on these types of products is available in the article "Closed-Die Forging in Hammers and Presses" in this Volume.

Upset forging is sometimes the only suitable forging process when a large amount of stock is needed in a specific location of the workpiece. For many applications, hot upset forging is used as a preforming operation to reduce the number of operations, to save metal, or both when the forgings are to be completed in closed dies.

The rules that apply to the hot upset forging of carbon and alloy steels are also applicable to stainless steel; that is, the unsupported length should never be more than $2\frac{1}{2}$ times the diameter (or, for a square, the distance across flats) for single-blow upsetting. Beyond this length, the unsupported stock may buckle or bend, forcing metal to one side and preventing the formation of a concentric forging. Exceeding this limitation also causes grain flow to be erratic and nonuniform around the axis of the forging and encourages splitting of the upset on its outside edges. The size of an upset produced in one blow also should not exceed $2\frac{1}{2}$ diameters (or, for a square, $2\frac{1}{2}$ times the distance across flats). This varies to some extent, depending on the thickness of the upset. For extremely thin upsets, the maximum size may be only two diameters, or even less. Without reheating and multiple blows, it is not possible to produce an upset in stainless steel that is as thin or with corner radii as small as that which can be produced when a more forgeable metal such as carbon steel is being upset (see the article "Hot Upset Forging" in this Volume).

Roll forging can be used to forge specific products, such as tapered shafts. It is also used as a stock-gathering operation prior to forging in closed dies. Details on this process are available in the article "Roll Forging" in this Volume.

Ring rolling is used to produce some ringlike parts from stainless steel at lower cost than by closed-die forging. The techniques used are essentially the same as those for the ring rolling of carbon or alloy steel (see the article "Ring Rolling" in this Volume). More power is required to roll stainless steel, and it is more difficult to fill corners. A large ring mill capable of rolling carbon steel rings with a face height of 2 m (80 in.) can roll stainless steel rings up to about 1.25 m (50 in.) in height. Because stainless steel is more costly than carbon or alloy steel, the savings that result from using ring rolling are proportionately greater for stainless steel.

Ingot Breakdown

In discussing the forgeability of the stainless steels, it is critical to understand the types of primary mill practices available to the user of semifinished billet or bloom product.

Primary Forging and Ingot Breakdown. Most stainless steel ingots destined for the forge shop are melted by the electric furnace argon oxygen decarburization process. They will usually weigh between 900 and 13,500 kg (2000 to 30,000 lb), depending on the shop and the size of the finished piece. Common ingot shapes are round, octagonal, or fluted; less common ingot shapes include squares. Until recently, all of these ingots would have been top poured. Increasing numbers of producers are switching to the bottom-poured ingot process. This process is slightly more expensive to implement in the melt shop, but it more than pays for itself in extended mold life and greatly improved ingot surface.

Some stainless steel grades used in the aircraft and aerospace industries are double melted. The first melt is done with the electric furnace and argon oxygen decarburization, and these "electrodes" are then remelted by a vacuum arc remelting (VAR) or electroslag remelting (ESR) process. This remelting under a vacuum (VAR) or a slag (ESR) tends to give a much cleaner product with better hot workability. For severe forging applications, the use of remelt steels can sometimes be a critical factor in producing acceptable parts. These double-melted ingots are round in shape and will vary in diameter from 450 to 900 mm (18 to 36 in.), and in some cases, they weigh in excess of 11,000 kg (25,000 lb). The breakdown of ingots is usually done on large hydraulic presses (13,500 kN, or 1500 tonf). A few shops, however, still use large hammers, and the four-hammer radial forging machine is being increasingly used for ingot breakdown.

Heating is the single most critical step in the initial forging of ingots. The size of the ingot and the grade of the stainless steel will dictate the practice necessary to reduce thermal shock and to avoid unacceptable segregation levels. It is essential to have accurate and programmable control of the furnaces used to heat stainless steel ingots and large blooms.

Primary forging or breakdown of an ingot is usually achieved using flat dies. However, some forgers work the ingot down as a round using "V" or swage dies. Because of the high hot hardness of stainless steel and the narrow range of working temperatures for these alloys, light reductions, or saddening (an operation in which an ingot is given a succession of light reductions in a press or rolling-mill or under a hammer in order to break down the skin and overcome the initial fragility due to a coarse crystalline structure preparatory to reheating prior to heavier reductions), is the preferred initial step in the forging of the entire surface of the ingot.

After the initial saddening of the ingot surface is complete, normal reductions of 50 to 100 mm (2 to 4 in.) can be taken. If the chemistry of the heat is in accordance with specifications and if heating practices have been followed and minimum forging temperatures observed, no problems should be encountered in making the bloom and other semifinished product.

If surface tears occur, the forging should be stopped, and the workpiece conditioned. Some forgers use hot powder scarfing, but this presents environmental problems. The most common method is to grind out the defect. The ferritic, austenitic, and nitrogen-strengthened austenitic stainless steels can be air cooled, ground, and reheated for reforging. The martensitic and precipitation-hardening grades must be slow cooled and overaged before grinding and reheating. The ingot surface is important, and many producers find it advantageous to grind the ingots before forging to ensure good starting surfaces.

Billet and Bloom Product. Forgers buy bars, billets, or blooms of stainless steel for subsequent forging on hammers and presses. Forged stainless steel billet and bloom products tend to have better internal integrity than rolled product, especially with larger-diameter sections (>180 mm, or 7 in.). Correctly conditioned billet and bloom product should yield acceptable finished forgings if good heating practices are followed and if attention is paid to the minimum temperature requirements. Special consideration must be given to sharp corners and thin sections, because these tend to cool off very rapidly. Precautions should be taken when forging precipitation-hardening or nitrogen-strengthened austenitic grades.

Revised by Thomas Harris and Eugene Priebe, Armco Inc.

Forgeability

Closed-Die Forgeability. The relative forging characteristics of stainless steels can be most easily depicted through examples of closed-die forgings. The forgeability trends these examples establish can be interpreted in light of the grade, type of part, and forging method to be used.

Stainless steels of the 300 and 400 series can be forged into any of the hypothetical parts illustrated in Fig. 1. However, the forging of stainless steel into shapes equivalent to part 3 in severity may be prohibited by shortened die life (20 to 35% of that obtained in forging such a shape from carbon or low-alloy steel) and by the resulting high cost. For a given shape, die life is shorter in forging stainless steel than in forging carbon or low-alloy steel.

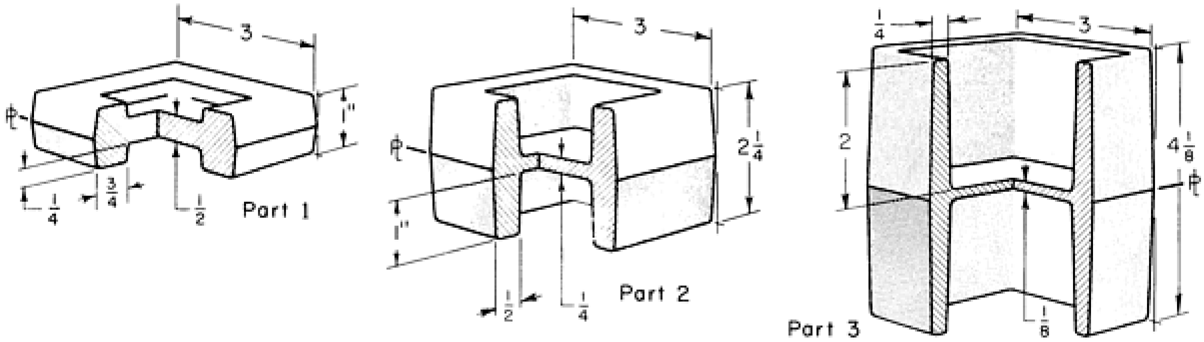

Fig. 1 Three degrees of forging severity. Dimensions given in inches.

Forgings of mild severity, such as part 1 in Fig. 1, can be produced economically from any stainless steel with a single heating and about five blows. Forgings approximating the severity of part 2 can be produced from any stainless steel with a single heating and about ten blows. For any type of stainless steel, die life in the forging of part 1 will be about twice that in the forging of part 2.

Part 3 represents the maximum severity for forging all stainless steels and especially those with high strength at elevated temperature; namely, types 309, 310, 314, 316, 317, 321, and 347. Straight-chromium types 403, 405, 410, 416, 420, 430, 431, and 440 are the easiest to forge into a severe shape such as part 3 (although type 440, because of its high carbon content, would be the least practical). Types 201, 301, 302, 303, and 304 are intermediate between the two previous groups.

One forge shop has reported that part 3 would be practical and economical to produce in the higher-strength alloys if the center web were increased from 3 to 6 mm ($\frac{1}{8}$ to $\frac{1}{4}$ in.) and if all fillets and radii were increased in size. It could then be forged with 15 to 20 blows and 1 reheating, dividing the number of blows about equally between the first heat and the reheat.

Hot Upsetting. Forgings of the severity represented by hypothetical parts 4, 5, and 6 in Fig. 2 can be hot upset in one blow from any stainless steel. However, the conditions are similar to those encountered in hot die forging. First, with a stainless steel, die wear in the upsetting of part 6 will be several times as great as in the upsetting of part 4. Second, die wear for the forming of any shape will increase as the elevated-temperature strength of the alloy increases. Therefore, type 410, with about the lowest strength at high temperature, would be the most economical stainless steel for forming any of the parts, particularly part 6. Conversely, type 310 would be the least economical.

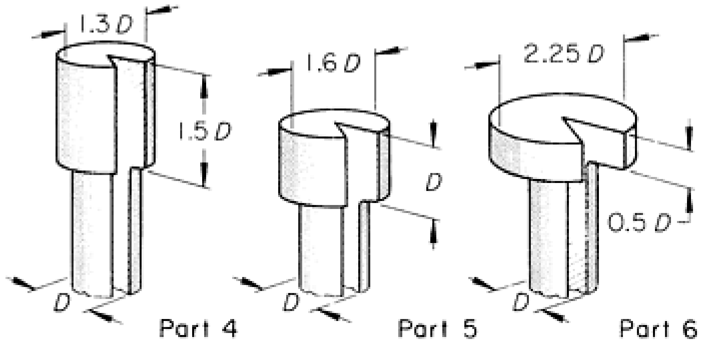

Fig. 2 Three degrees of upsetting severity.

Upset Reduction Versus Forging Pressure. The effect of percentage of upset reduction (upset height versus original height) on forging pressure for low-carbon steel and for type 304 stainless steel at various temperatures is illustrated in Fig. 3. Temperature has a marked effect on the pressure required for any given percentage of upset, and at any given forging temperature and percentage of upset, type 304 stainless requires at least twice the pressure required for 1020 steel.

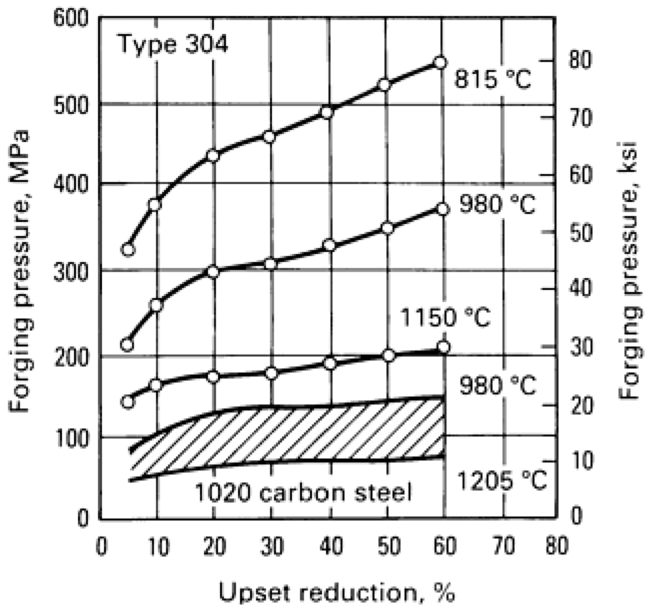

Fig. 3 Effect of upset reduction on forging pressure for various temperatures. Source: Ref 1.

The effects of temperature on forging pressure are further emphasized in Fig. 4(a). These data, based on an upset reduction of 10%, show that at 760 °C (1400 °F) type 304 stainless steel requires only half as much pressure as A-286 (an iron-base heat-resistant alloy), although the curves for forging pressure for the two metals converge at 1100 °C (2000 °F). However, at a forging temperature of 1100 °C (2000 °F), the pressure required for a 10% upset reduction on type 304 is more than twice that required for a carbon steel (1020) and about 60% more than that required for 4340 alloy steel. Differences in forgeability, based on percentage of upset reduction and forging pressure for type 304 stainless steel, 1020, and 4340 at the same temperature (980 °C, or 1800 °F), are plotted in Fig. 4(b).

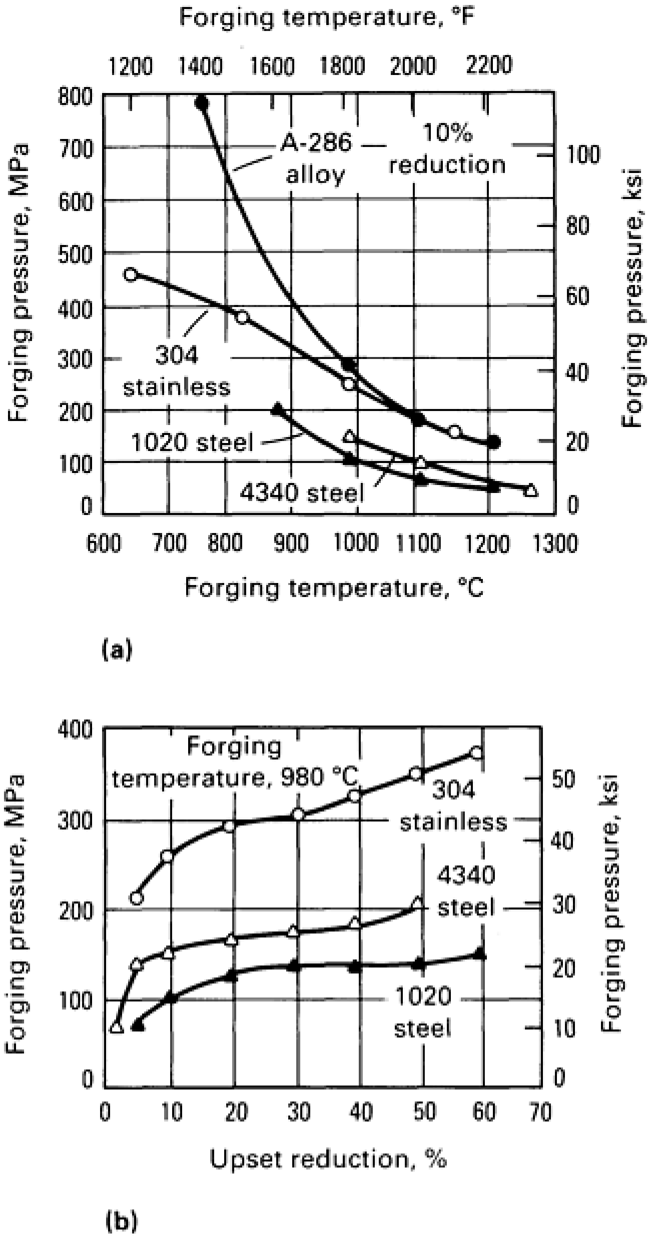

Fig. 4 Forging pressure required for upsetting versus (a) forging temperature and (b) percentage of upset reduction. Source: Ref 2.

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Forging of Stainless Steel

Revised by Thomas Harris and Eugene Priebe, Armco Inc.

Austenitic Stainless Steels

The austenitic stainless steels are more difficult to forge than the straight-chromium types, but are less susceptible to surface defects. Most of the austenitic stainless steels can be forged over a wide range of temperatures above 930 °C (1700 °F), and because they do not undergo major phase transformation at elevated temperature, they can be forged at higher temperatures than the martensitic types (Table 1). Exceptions to the above statements occur when the composition of the austenitic stainless steel promotes the formation of δ-ferrite, as in the case of the 309S, 310S, or 314 grades. At temperatures above 1100 °C (2000 °F), these steels, depending on their composition, may form appreciable amounts of δ-ferrite. Figure 5 depicts these compositional effects in terms of nickel equivalent (austenitic-forming elements) and chromium equivalent. Delta-ferrite formation adversely affects forgeability, and compensation for the amount of ferrite present can be accomplished with forging temperature restrictions.

Table 1 Typical compositions and forging temperature ranges of high-temperature alloys

Alloy	**Typical composition, %**						Temperature	
	C	**Cr**	**Ni**	**Mo**	**Co**	**Other**	**°C**	°F
More difficult to hot work								
Carpenter 41	0.09	19.0	Bal	10.0	11.0	3.1 Ti, 1.5 Al, 0.005 B	1040-1175	**1900-2145**
Pyromet 718	0.10	18.0	55.0	3.0	. . .	1.3 Ti, 0.6 Al, 5.0 Nb	925-1120	**1700-2050**
M252	0.15	18.0	38.0	3.2	20.0	2.8 Ti, 0.2 Al	980-1175	**1800-2145**
Waspaloy	0.07	19.8	Bal	4.5	13.5	3.0 Ti, 1.4 Al, 0.005 B	1010-1175	**1850-2145**
Pyromet 860	0.1	14.0	45.0	6.0	4.0	3.0 Ti, 1.3 Al, 0.01 B	1010-1120	**1850-2050**
Carpenter 901	0.05	12.5	42.5	6.0	. . .	2.7 Ti, 0.2 Al, 0.015 B	1010-1120	**1850-2050**
N155	0.12	21.0	20.0	3.0	19.5	2.4 W, 1.2 Nb, 0.13 N	1040-1150	**1900-2100**
V57	0.05	15.0	27.0	1.3	. . .	3.0 Ti, 0.2 Al, 0.01 B, 0.3 V	955-1095	**1750-2000**
A-286	0.05	15.0	25.0	1.3	. . .	2.1 Ti, 0.2 Al, 0.004 B, 0.3 V	925-1120	**1700-2050**
Carpenter 20Cb-3	0.05	20.0	34.0	2.5	. . .	3.5 Cu	980-1230	**1800-2245**
Pyromet 355	0.12	15.5	4.5	3.0	. . .	0.10 N	925-1150	**1700-2100**
Type 440F	1.0	17.0	. . .	0.5	. . .	0.15 Se	925-1150	**1700-2100**
Type 440C	1.0	17.0	. . .	0.5	. . .	. . .	925-1150	**1700-2100**

19-9DL/19DX	0.32	18.5	9.0	1.5	. . .	1.4 W plus Nb or Ti	870-1150	**1600-2100**
Types 347 and 348	0.05	18.0	11.0	. . .	. . .	0.07 Nb	925-1230	**1700-2245**
Type 321	0.05	18.0	10.0	. . .	. . .	0.40 Ti	925-1260	**1700-2300**
AMS 5700	0.45	14.0	14.0	. . .	. . .	2.5 W	870-1120	**1600-2050**
Type 440B	0.85	17.0	. . .	0.5	. . .	. . .	925-1175	**1700-2145**
Type 440A	0.70	17.0	. . .	0.5	. . .	. . .	925-1200	**1700-2200**
Type 310	0.15	25.0	20.0	. . .	. . .	. . .	980-1175	**1800-2145**
Type 310S	0.05	25.0	20.0	. . .	. . .	. . .	980-1175	**1800-2145**
17-4 pH	0.07	17.0	4.0	. . .	. . .	3.0-3.5 Cu, 0.3 Nb + Ta	1095-1175	**2000-2145**
15-5 pH	0.07	15.0	5.0	. . .	. . .	3.5 Cu, 0.3 Nb + Ta	1095-1175	**2000-2145**
13-8 Mo	0.05	13.0	8	2.25	. . .	0.90-1.35 Al	1095-1175	**2000-2145**
Type 317	0.05	19.0	13.0	3.5	. . .	. . .	925-1260	**1700-2300**
Type 316L	0.02	17.0	12.0	2.5	. . .	. . .	925-1260	**1700-2300**
Type 316	0.05	17.0	12.0	2.5	. . .	. . .	925-1260	**1700-2300**
Type 309S	0.05	23.0	14.0	. . .	. . .	. . .	980-1175	**1800-2145**
Type 309	0.10	23.0	14.0	. . .	. . .	. . .	980-1175	**1800-2145**
Type 303	0.08	18.0	9.0	. . .	. . .	0.30 S	925-1260	**1700-2300**
Type 303Se	0.08	18.0	9.0	. . .	. . .	0.30 Se	925-1260	**1700-2300**
Type 305	0.05	18.0	12.0	. . .	. . .	. . .	925-1260	**1700-2300**
Easier to hot work								
Types 302 and 304	0.05	18.0	9.0	. . .	. . .	. . .	925-1260	**1700-2300**

UNS S21800	0.06	17	8.5	. . .	. . .	8.0 Mn, 0.12 N	1095-1175	**2000-2145**
No. 10	0.05	16.0	18.0	. . .	. . .	. . .	925-1230	**1700-2245**
Lapelloy	0.30	11.5	0.30	2.8	. . .	0.3 V	1040-1150	**1900-2100**
Lapelloy C	0.20	11.5	0.40	2.8	. . .	2.0 Cu, 0.08 N	1040-1150	**1900-2100**
636	0.23	12.0	0.8	1.0	. . .	0.3 V, 1.0 W	1040-1175	**1900-2145**
H46	0.17	12.0	0.5	0.8	. . .	0.4 Nb, 0.07 N, 0.3 V	1010-1175	**1850-2145**
AMS 5616 (Greek Ascoloy)	0.17	13.0	2.0	0.2	. . .	3.0 W	955-1175	**1750-2145**
Type 431	0.16	16.0	2.0	. . .	. . .	. . .	900-1200	**1650-2200**
Type 414	0.12	12.5	1.8	. . .	. . .	. . .	900-1200	**1650-2200**
Type 420F	0.35	13.0	. . .	. . .	. . .	0.2 S	900-1200	**1650-2200**
Type 420	0.35	13.0	. . .	. . .	. . .	. . .	900-1200	**1650-2200**
Pyromet 600	0.08	16.0	74.0	. . .	. . .	8.0 Fe	870-1150	**1600-2100**
Type 416	0.1	13.0	. . .	. . .	. . .	0.3 S	925-1230	**1700-2245**
Type 410	0.1	12.5	. . .	. . .	. . .	. . .	900-1200	**1650-2200**
Type 404	0.04	11.5	1.8	. . .	. . .	. . .	900-1150	**1650-2100**
Type 501	0.2	5.0	. . .	0.5	. . .	. . .	980-1200	**1800-2200**
Type 502	0.05	5.0	. . .	0.5	. . .	. . .	980-1200	**1800-2200**
HiMark 300	0.02	. . .	18.0	4.8	9.0	0.7 Ti, 0.1 Al	815-1260	**1500-2300**
HiMark 250	0.02	. . .	18.0	4.8	7.5	0.4 Ti, 0.1 Al	815-1260	**1500-2300**
Carpenter 7-Mo (Type 329)	0.08	28.0	5.8	1.6	. . .	. . .	925-1095	**1700-2000**
Type 446	0.1	25.0	. . .	. . .	. . .	. . .	900-1120	**1650-2050**

Type 443	0.1	21.0	. . .	. . .	. . .	1.0 Cu	900-1120	**1650-2050**
Type 430F	0.08	17.0	. . .	. . .	. . .	0.3 S	815-1150	**1500-2100**
Type 430	**0.06**	**17.0**	**. . .**	**. . .**	**. . .**	**. . .**	**815-1120**	**1500-2050**

Source: Ref 3

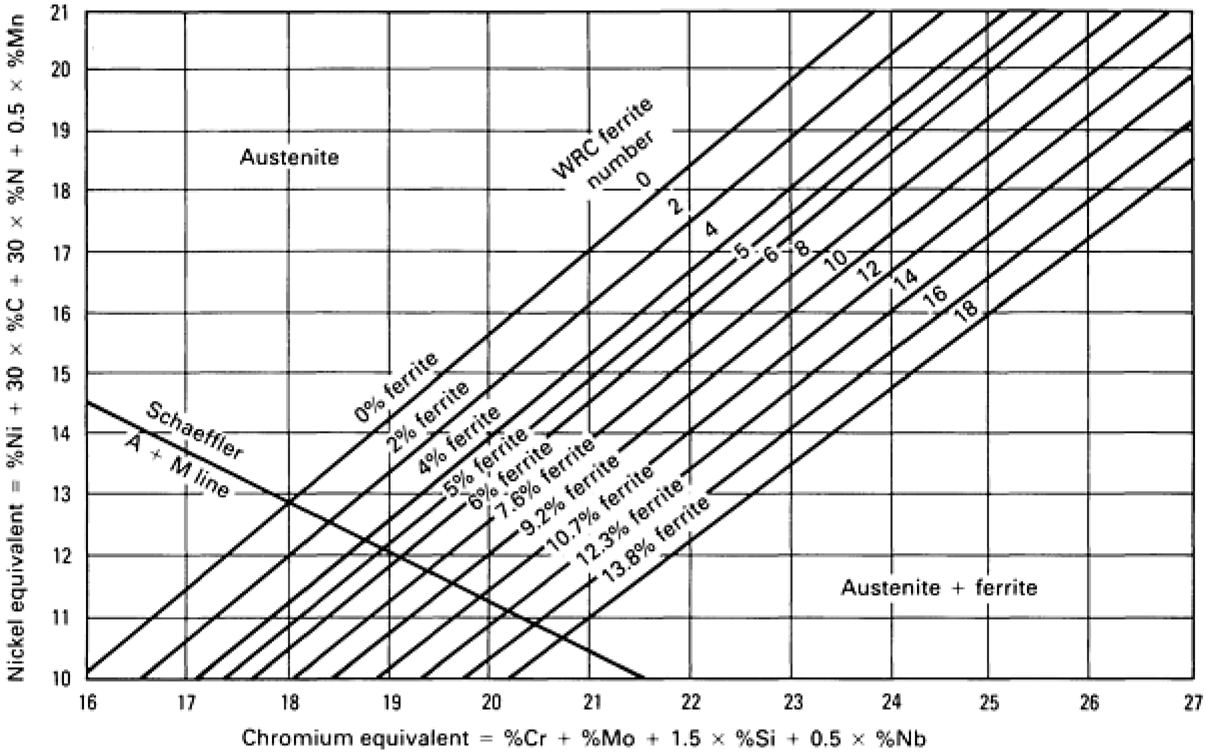


Fig. 5 Schaeffler (constitution) diagram used to predict the amount of δ-ferrite that will be obtained during elevated-temperature forging or welding of austenitic/ferritic stainless steels. A, austenite; M, martensite. WRC, Welding Research Council. Source: Ref 4.

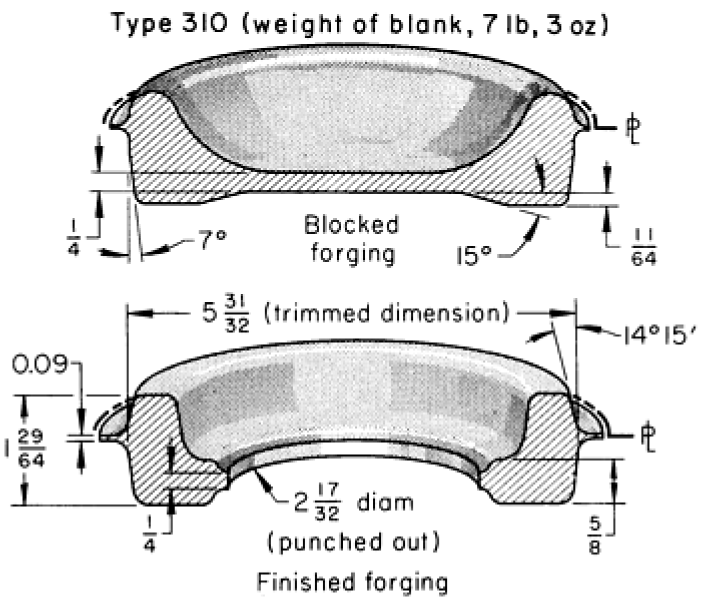
Equally important restrictions in forging the austenitic stainless steels apply to the finishing temperatures. All but the stabilized types (321, 347, 348) and the extralow-carbon types should be finished at temperatures above the sensitizing range (~815 to 480 °C, or 1500 to 900 °F) and cooled rapidly from 870 °C (1600 °F) to a black heat. The highly alloyed grades, such as 309, 310, and 314, are also limited with regard to finishing temperature, because of their susceptibility at lower temperatures to hot tearing and σ formation. A final annealing by cooling rapidly from about 1065 °C (1950 °F) is generally advised for nonstabilized austenitic stainless steel forgings in order to retain the chromium carbides in solid solution.

Finishing temperatures for austenitic stainless steels become more critical where section sizes increase and ultrasonic testing requirements are specified. During ultrasonic examination, coarse-grain austenitic stainless steels frequently display sweep noise that can be excessive due to a coarse-grain micro-structure. The degree of sound attenuation normally increases with section size and may become too great to permit detection of discontinuities. Careful control of forging conditions, including final forge reductions of at least 5%, can assist in the improvement of ultrasonic penetrability.

A typical procedure for the hammer forging of one of the more difficult-to-forge austenitic steels (type 310) is given in the following example.

Example 1: Forging a Ringlike Part From Type 310 Steel.

The ringlike part shown in Fig. 6 was forged in a 13,500 N (3000 lbf) steam hammer by upsetting a piece of round bar and completing the shape in one blocking and one finishing impression. Because of its small size and symmetrical shape, the workpiece could be handled rapidly and completed without reheating. The effect of forging severity, however, is reflected in the short die life. Die life and other forging details are given in the table in Fig. 6.



Sequence of operations
Upset on flat portion of die to approximately 115 mm (4 $\frac{1}{2}$ in.) in diameter. Forge in blocker impression. Forge in finisher impression. Hot trim (900 to 925 °C, or 1650 to 1700 °F) and punch out center. Air cool. Clean (shot blast)

Processing conditions	
Blank preparation	**Cold sawing**
Stock size	**90 mm (3 $\frac{1}{2}$ in.) in diameter**
Blank weight	**3.25 kg (7 lb, 3 oz)**
Heating method	**Gas-fired, slot-front box furnace**
Heating time	**1 h**

Atmosphere	**Slightly oxidizing**
Die material	**6G at 388-429 HB[(a)]**
Die life, total	**507-2067 forgings[(b)]**
Die lubricant	**Graphite-oil**
Production rate	**50 forgings per hour[(c)]**

(a) Inserts at this hardness were used in die blocks of the same material, but softer (341-375 HB).

(b) Average life was 1004 forgings. Life to rework and total life were the same, because worn die inserts were not reworked.

(c) Based on a 50 min working hour

Fig. 6 Typical procedure for forging a ringlike part from an austenitic stainless steel. Dimensions given in inches.

The stabilized or extralow-carbon austenitic stainless steels, which are not susceptible to sensitization, are sometimes strain hardened by small reductions at temperatures well below the forging temperature. Strain hardening is usually accomplished at 535 to 650 °C (1000 to 1200 °F) (referred to as warm working or hot-cold working). When minimum hardness is required, the forgings are solution annealed.

Sulfur or selenium can be added to austenitic stainless steel to improve machinability. Selenium, however, is preferred because harmful stringers are less likely to exist. Type 321, stabilized with titanium, may also contain stringers of segregate that will open as surface ruptures when the steel is forged. Type 347, stabilized with niobium, is less susceptible to stringer segregation and is the stabilized grade that is usually specified for forgings.

When heating the austenitic stainless steels, it is especially desirable that a slightly oxidizing furnace atmosphere be maintained. A carburizing atmosphere or an excessively oxidizing atmosphere will impair corrosion resistance, either by harmful carbon pickup or by chromium depletion. In types 309 and 310, chromium depletion can be especially severe.

Nitrogen-strengthened austenitic stainless steels are iron-base alloys containing chromium and manganese. Varying amounts of nickel, molybdenum, niobium, vanadium, and/or silicon are also added to achieve specific properties. Nitrogen-strengthened austenitic stainless steels provide high strength, excellent cryogenic properties and corrosion resistance, low magnetic permeability (even after cold work or subzero temperature), and higher elevated-temperature strengths as compared to the 300 series stainless steels. These alloys are summarized as follows:

- UNS S24100 (Nitronic 32) ASTM XM-28. High work hardening while remaining nonmagnetic plus twice the yield strength of type 304 with equivalent corrosion resistance
- UNS S24000 (Nitronic 33) ASTM XM-29. Twice the yield strength of type 304, low magnetic permeability after severe cold work, high resistance to wear and galling as compared to standard austenitic stainless steels, and good cryogenic properties
- UNS S21904 (Nitronic 40) ASTM XM-11. Twice the yield strength of type 304 with good corrosion resistance, low magnetic permeability after severe cold working, and good cryogenic properties
- UNS S20910 (Nitronic 50)ASTM XM-19. Corrosion resistance greater than type 316L with twice the

yield strength, good elevated and cryogenic properties, and low magnetic permeability after severe cold work

- UNS S21800 (Nitronic 60). Galling resistance with the corrosion resistance equal to that of type 304 and with twice the yield strength, good oxidation resistance, and cryogenic properties

A forgeability comparison, as defined by dynamic hot hardness, is provided in Fig. 7.

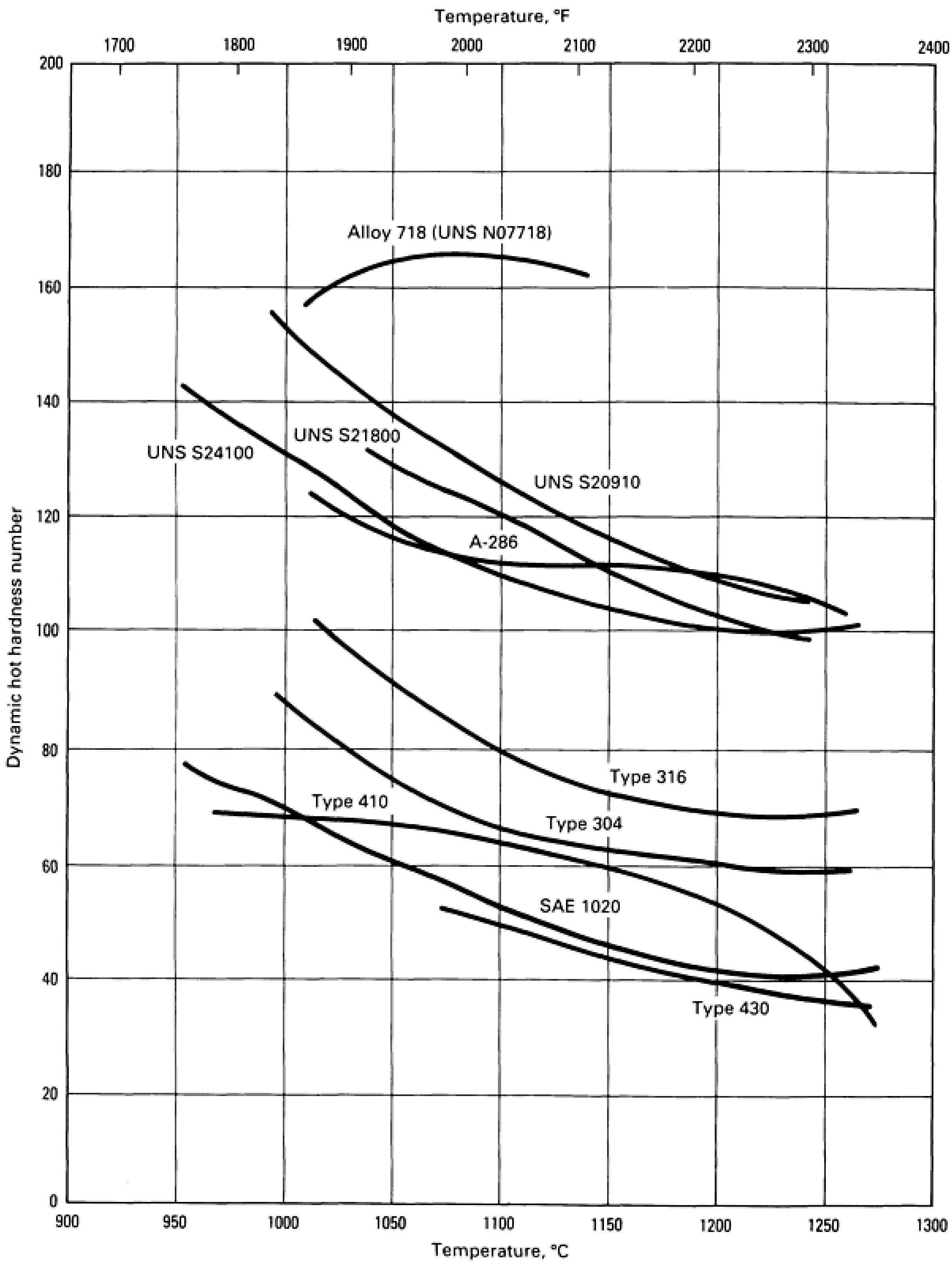


Fig. 7 Comparative dynamic hot hardness versus temperature (forgeability) for various ferrous alloys.

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Revised by Thomas Harris and Eugene Priebe, Armco Inc.

Martensitic Stainless Steels

Martensitic stainless steels have high hardenability to the extent that they are generally air hardened. Therefore, precautions must be taken in cooling forgings of martensitic steels, especially those with high carbon content, in order to prevent cracking. The martensitic alloys are generally cooled slowly to about 590 °C (1100 °F), either by burying in an insulating medium or by temperature equalizing in a furnace. Direct water sprays, such as might be employed to cool dies, should be avoided, because they would cause cracking of the forging.

Forgings of the martensitic steels are often tempered in order to soften them for machining. They are later quench hardened and tempered.

Maximum forging temperatures for these steels are low enough to avoid the formation of δ-ferrite. If δ-ferrite stringers are present at forging temperatures, cracking is likely to occur. Delta-ferrite usually forms at temperatures from 1095 to 1260 °C (2000 to 2300 °F). Care must be exercised so as not to exceed this temperature during forging and to avoid rapid metal movement that might result in local overheating. Surface decarburization, which promotes ferrite formation, must be minimized.

The δ-ferrite formation temperature decreases with increasing chromium content, and small amounts of δ-ferrite reduce forgeability significantly. As the δ-ferrite increases above about 15% (Fig. 5), forgeability improves gradually until the structure becomes entirely ferritic. Finishing temperatures are limited by the allotropic transformation, which begins near 815 °C (1500 °F). However, forging of these steels is usually stopped at about 925 °C (1700 °F), because the metal is difficult to deform at lower temperatures.

Sulfur or selenium can be added to type 410 to improve machinability. These elements can cause forging problems, particularly when they form surface stringers that open and form cracks. This can sometimes be overcome by adjusting the forging temperature or the procedure. With sulfur additions, it may be impossible to eliminate all cracking of this type. Therefore, selenium additions are preferred.

Revised by Thomas Harris and Eugene Priebe, Armco Inc.

Ferritic Stainless Steels

The ferritic straight-chromium stainless steels exhibit virtually no increase in hardness upon quenching. They will work harden during forging; the degree of work hardening depends on the temperature and the amount of metal flow. Cooling from the forging temperature is not critical.

The ferritic stainless steels have a broad range of forgeability, which is restricted somewhat at higher temperature because of grain growth and structural weakness but is closely restricted in finishing temperature only for type 405. Type 405 requires special consideration because of the grain-boundary weakness resulting from the development of a small amount of austenite. The other ferritic stainless steels are commonly finished at any temperature down to 705 °C (1300 °F). For type 446, the final 10% reduction should be made below 870 °C (1600 °F) to achieve grain refinement and room-temperature ductility. Annealing after forging is recommended for ferritic steels.

Precipitation-Hardening Stainless Steels

The semiaustenitic and martensitic precipitation-hardening stainless steels can be heat treated to high hardness through a combination of martensite transformation and precipitation. They are the most difficult to forge and will crack if temperature schedules are not accurately maintained. The forging range is narrow, and the steel must be reheated if the temperature falls below 980 °C (1800 °F). They have the least plasticity (greatest stiffness) at forging temperature of any of the classes and are subject to grain growth and δ-ferrite formation. Heavier equipment and a greater number of blows are required to achieve metal flow equivalent to that of the other types.

During trimming, the forgings must be kept hot enough to prevent the formation of flash-line cracks. To avoid these cracks, it is often necessary to reheat the forgings slightly between the finish-forging and trimming operations. Cooling, especially the cooling of the martensitic grades, must be controlled to avoid cracking.

Forging of Stainless Steel

Revised by Thomas Harris and Eugene Priebe, Armco Inc.

Forging Equipment

Stainless steels are generally forged with the same types of hammers, presses, upsetters, and rolling machines used to forge carbon and alloy steels. Descriptions of these machines are provided in the articles "Hammers and Presses for Forging," "Hot Upset Forging," "Roll Forging," and "Ring Rolling" in this Volume.

Hammers. Simple board-type gravity-drop hammers are not extensively used for the forging of stainless steel, because of their low capacity and because greater control is obtained with other types of equipment. Power-drop hammers (steam or air) are widely used for open-die forgings, as well as for all types of large and small closed-die forgings. The service life of the die is usually longer in hammers than in hydraulic presses; in a hammer, the hot workpiece is in contact with the dies (particularly the upper die) for a shorter length of time. Hammers cost less than presses of equivalent capacity and are generally more flexible than presses in the variety of functions they can fulfill.

Presses. Mechanical presses are extensively used for small forgings; they are used less often for forgings weighing as much as 45 kg (100 lb) each and are seldom used for forgings weighing more than 70 kg (150 lb). Mechanical presses cost more than hammers of equivalent capacity, but they require less operator skill and can produce forgings at a higher rate than hammers.

Hydraulic presses can be used for all steps in the forging of stainless steel. However, they are more often used to complete intricate forgings after preforming in other types of equipment. Die life is usually shorter in a hydraulic press than in a hammer; in a press, the work metal contacts the dies for a longer period of time. However, there is less danger of local overheating of the metal in hydraulic presses, because their action is slower than that of hammers.

Radial Forging Machines. Another tool that is increasing in use is the radial forging machine. This is a precision four-hammer forging machine that is capable of forging all grades of stainless steel into round, rectangular, square, and octagonal shapes. Different cross sections on the same piece are possible including the forging of complicated step-down shafts.

The machine uses four axial symmetrical hammers, which are in opposing pairs and are electromechanically controlled by a pre-programmed processor, that simultaneously deliver 200 blows per minute to the work. Two hydraulically controlled manipulators, one in each side of the hammer box, rotate and position the workpiece during forging.

Each hammer delivers up to approximately 9000 kN (1000 tonf) of force per blow, depending on the size of the machine. As a result of the counter-blow configuration, the workpiece receives enough energy so that isothermal reductions are possible, an advantage in the forging of grades with narrow hot-working ranges. The piece loses very little temperature during forging and sometimes actually increases in temperature. Therefore, everything is finished in one heat. The feed and rotation motions of the chuck head are synchronized with the hammers to prevent twisting or stretching during forging.

In operation, the manipulator or chuck head on the entry side of the hammer box positions the workpiece between the four hammers and supports it until the length is increased so as to be grasped by the manipulator or chuck head at the exit side. Forging then continues in a back and forth mode until the desired finished cross section is achieved. At the end of each forging pass, the trailing manipulator relinquishes its grip so that the end receives the same reduction as the rest of the workpiece. This results in uniformity in mechanical properties as well as dimensions. In general, experience with the radial forging machine indicates an oversize of 0.015 times the cold-finish dimension and typical tolerances for hot-forged products to be approximately one-half the ASTM A 484 or one-fourth the DIN 7527 standards.

Forging of Stainless Steel

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Dies

In most applications, dies designed for the forging of a given shape from carbon or alloy steel can be used to forge the same shape from stainless steel. However, because of the greater force used in forging stainless steel, more strength is required in the die. Therefore, the die cannot be resunk as many times for the forging of stainless steel, because it may break. When a die is initially designed for the forging of stainless steel, a thicker die block is ordinarily used in order to obtain a greater number of resinkings and therefore a longer total life. Die practice for the forging of stainless steel varies considerably among different plants, depending on whether forging is done in hammers or presses and on the number of forgings produced from other metals in proportion to the number forged from stainless steel.

Multiple-cavity dies for small forgings (less than about 10 kg, or 25 lb) are more commonly used in hammers and less commonly used in presses. If multiple-cavity dies are used, the cavities are usually separate inserts, because some cavities have longer service lives than others. With this practice, individual inserts can be changed as required. Larger forgings (more than about 10 kg, or 25 lb) are usually produced in single-cavity dies, regardless of whether a hammer or a press is used.

In forge plants in which carbon and alloy steels comprise the major portion of the metals forged, the usual practice is to use the same die system (single-cavity versus multiple-cavity) for stainless steel, accepting the fact that die life will be shorter. This approach is generally more economical than using a separate die practice for a relatively small tonnage of forgings.

Practice is likely to be entirely different in shops in which most of the forgings produced are from stainless steel or from some other difficult-to-forge metal, such as heat-resistant alloys. For example, in one plant in which mechanical presses are used almost exclusively, most of the dies are of the single-cavity design. Tolerances are always close, so practice is the same regardless of the quantity to be produced. A die is made with a finishing cavity, and after it is worn to the extent that it can no longer produce forgings to specified tolerances, the cavity is recut for a semifinishing, or blocker, cavity. When it can no longer be used as a blocker die, its useful life is over because resinking would result in a thin die block.

Die Materials. In shops in which die practice is the same for stainless steel as for carbon and alloy steels, die materials are also the same (see the article "Dies and Die Materials for Hot Forging" in this Volume). In shops in which special consideration is given to dies for stainless steel, small dies (for forgings weighing less than 9 kg, or 20 lb) are made solid from hot-work tool steel, such as H11, H12, or H13. For large dies, regardless of whether they are single or multiple impression, common practice is to make the body of the block from a conventional die block low-alloy steel, such as 6G or 6F2 (see the article "Dies and Die Materials for Hot Forging" in this Volume). Inserts are of H11, H12, or H13 hot-work tool steel (or sometimes H26, where it has proved a better choice). In many specialty applications, nickel- and cobalt-base superalloys are fabricated for die inserts on conventional hot-work tool steel dies. Welded inlays of these alloys are also being used in critical areas for improved wear resistance and much higher hot strength.

Gripper dies and heading tools used for the hot upsetting of stainless steel are made from one of the hot-work tool steels. Small tools are machined from solid tool steel. Larger tools are made by inserting hot-work tool steels into bodies of a lower-alloy steel, such as 6G or 6F2.

Roll dies for roll forging are usually of the same material used for the roll forging of carbon or alloy steels. A typical die steel composition is Fe-0.75C-0.70Mn-0.35Si-0.90Cr-0.30Mo.

Die hardness depends mainly on the severity of the forging and on whether a hammer or a press is used. Die wear decreases rapidly as die hardness increases, but some wear resistance must always be sacrificed for the sake of toughness and to avoid breaking the dies.

Most solid dies (without inserts) made from such steels as 6G and 6F2 for use in a hammer are in the hardness range of 36.6 to 40.4 HRC. This range is suitable for forgings as severe as part 3 in Fig. 1. If severity is no greater than that of part 1 in Fig. 1, die hardness can be safely increased to the next level (41.8 to 45.7 HRC). If forging is done in a press, the dies can be safely operated at higher hardnesses for the same degree of forging severity. For example, dies for forgings of maximum severity would be 41.8 to 45.7 HRC, and dies for minimum severity would be 47.2 to 50.3 HRC.

Inserts or solid dies made from hot-work tool steel are usually heat treated to 40 to 47 HRC for use in hammers. For forgings of maximum severity (part 3, Fig. 1), hardness near the low end of the range is used. For minimum severity (part 1, Fig. 1), die hardness will be near the high end of the range. Adjustment in die hardness for different degrees of forging severity is usually also needed for forging in presses, although a higher hardness range (usually 47 to 55 HRC) can be safely used.

The hardness of gripper-die inserts for upset forging is usually 44 to 48 HRC. For the heading tools, hardness is 48 to 52 HRC.

Roll-forging dies are usually heat treated to 50 to 55 HRC. Rolls for ring rolling, when made from hot-work tool steel, are usually operated in the hardness range of 40 to 50 HRC.

Die Life. Because of the differences in forgeability among stainless steels, die life will vary considerably, depending on the composition of the metal being forged and the composition and hardness of the die material. Other conditions being equal, the forging of types 309, 310, and 314 stainless steel and the precipitation-hardening alloys results in the shortest die life. The longest die life is obtained when forging lower-carbon ferritic and martensitic steels. Die life in forging type 304 stainless steel is usually intermediate. However, die life in forging any stainless steel is short compared to the die life obtained in forging the same shape from carbon or alloy steel.

Example 2: Die Life in the Upset Forging of Type 304 versus 4340 versus 9310.

The 100 mm (4 in.) upset shown in Fig. 8 was, at different times, produced from three different metals in the same 150 mm (6 in.) upsetter and in the same gripper dies (H12 hot-work tool steel at 44 HRC). From the bar chart shown in Fig. 8, the effect of work metal composition on die life is obvious. Die life for upsetting type 304 stainless steel was less than one-fifth the die life for upsetting the low-carbon alloy steel (9310) and less than one-third that for upsetting 4340.

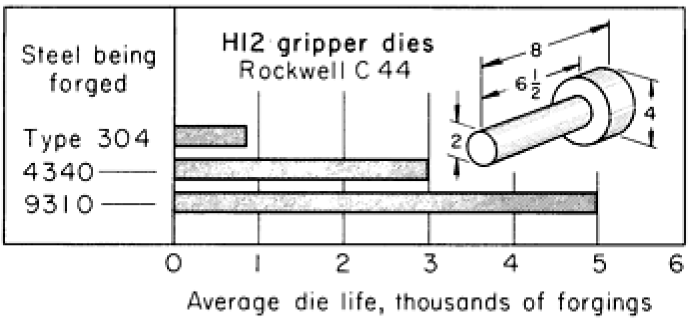

Fig. 8 Effect of steel being forged on the life of gripper dies in upsetting. Dimensions given in inches.

Example 3: Effect of Forging Severity on Die Life.

The effect of the forging shape (severity) on die life for forging type 431 stainless steel is shown in Fig. 9. When forging to the relatively mild severity of shape A, the range of life for five dies was 6000 to 10,000 forgings, with an average of 8000. When forging severity was increased to that of shape B, the life of three dies ranged from approximately 700 to 2200 forgings, with an average of 1400.

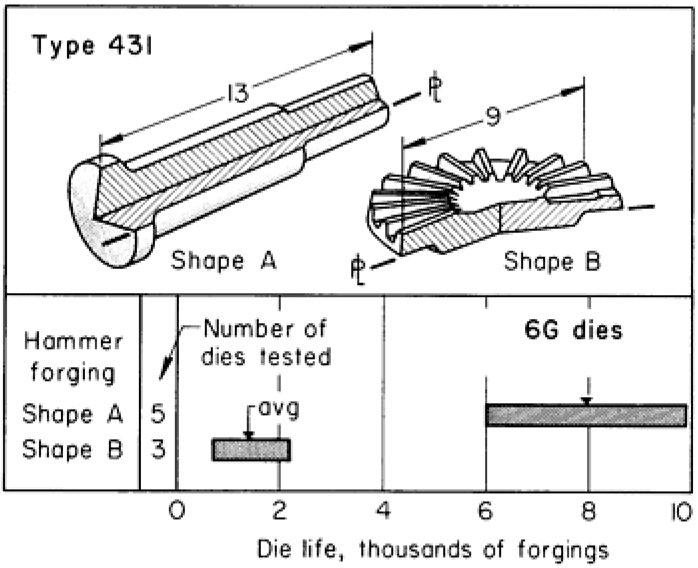

Fig. 9 Effect of severity of forging on die life. Dies: 341-375 HB. Dimensions given in inches.

Shapes A and B were both forged in the same hammer. Tool material and hardness were also the same for both shapes (6G die block steel at 341 to 375 HB).

Forging of Stainless Steel

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Heating for Forging

Recommended forging temperatures for most of the standard stainless steels are listed in Table 1. The thermal conductivity of stainless steels is lower than that of carbon or low-alloy steels. Therefore, stainless steels take longer to reach the forging temperature. However, they should not be soaked at the forging temperature, but should be forged as soon as possible after reaching it. The exact time required for heating stock of a given thickness to the established forging temperature depends on the type of furnace used. Time and stock thickness relationships for three types of furnaces are shown in Fig. 10.

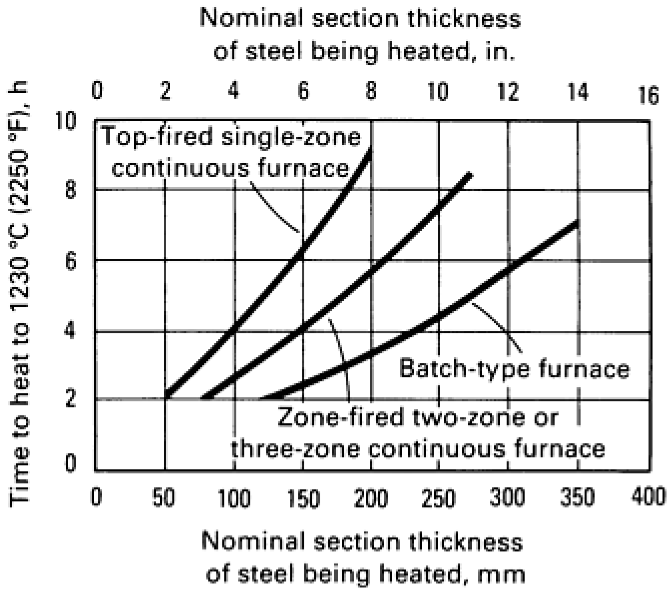

Fig. 10 Effect of section thickness on time for heating stainless steel in various types of furnaces. Source: Ref 5.

The preheating of forging stock will be dictated by the grade, size, and condition of the stock to be forged. Austenitic and ferritic grades, for example, are generally considered safe from thermal shock and can be charged directly into hot furnaces. Certain martensitic grades and precipitation-hardening grades should be preheated, with the preheat temperatures in the range of 650 to 925 °C (1200 to 1700 °F), depending on section size and the condition of the material.

Section sizes larger than 150 × 150 mm (6 × 6 in.) require consideration, because the rapid heating of larger sections will result in differential expansion that could locally exceed the tensile strength of the interior of the section. The resulting internal crack, frequently termed klink, will often open transversely upon further reductions. Generally, the greater the ability of the stainless grade to be hardened to high hardness levels, the more susceptible it is to thermal shock.

The physical condition of the stainless steel must also be taken into consideration. Cast material (that is, ingot or continuous cast) will be more susceptible to thermal shock than semiwrought or wrought product.

Equipment. Gas-fired and electric furnaces are used with equal success for heating the stock. Gas-fired furnaces are more widely used, because heating costs are usually lower. The gas employed should be essentially free from hydrogen sulfide and other sulfur-bearing contaminants. Oil-fired furnaces are widely used for heating the 400-series stainless steels and the 18-8 varieties, but because of the danger of contamination from sulfur in the oil, they are considered unsafe for heating the high-nickel grades. Trace amounts of vanadium present in the fuel oil can also cause surface problems because the resulting vanadium oxide will fuse with the high chrome scale.

Although not absolutely necessary, heating of stainless steel is preferably done in a protective atmosphere. When gas heating is used, an acceptable protective atmosphere can usually be obtained by adjusting the fuel-to-air ratio. When the furnace is heated by electricity, the protective atmosphere (if used) must be separately generated. Induction heating is most often used to heat local portions of the stock for upsetting.

Temperature control within ±5 °C (10 °F) is achieved by the use of various types of instruments. A recording instrument is preferred, because it enables the operator to observe the behavior of the furnace throughout the heating cycle.

It is recommended that the temperature of the pieces of forging stock be checked occasionally with an optical or probe-type pyrometer as the pieces are removed from the furnace. This practice not only provides a check on the accuracy of the furnace controls but also ensures that the stock is reaching the furnace temperature.

Control of Cooling Rate. Cooling from the forging operations should also be considered in terms of grade and size. Austenitic grades are usually quenched from the forge. This is done to minimize the formation of intergranular chromium carbides and to facilitate cutting and machining after forging. Because martensitic grades are characterized by high hardenability, special precautions are taken in cooling them from forging temperatures. Common practice is to place hot forgings in insulating materials for slow cooling. For parts that have either heavy sections or large variation in section, it is often desirable to charge the forged parts into an annealing furnace immediately after forging.

In particular, the higher-carbon grades, such as 440A, 440B, and 440C, and the modified 420 types, such as UNS 41800 (ASTM A565, Grade 615), must be carefully slow cooled after forging. These steels often require furnace-controlled interrupted cooling cycles to ensure against cracks. A suitable cycle consists of air cooling the forgings to temperatures at which the martensite transformation is partially complete (between 150 and 250 °C, or 300 and 500 °F), then reheating the forgings in a furnace at a temperature of about 650 °C (1200 °F) before final cooling to room temperature. This procedure also prevents the formation of excessive grain-boundary carbides, which sometimes develop during continuous slow cooling.

The control cooling of 17-4 PH, 15-5 PH, and PH 13-8 Mo grades after forging must also be considered. These grades are austenitic upon cooling from forging or solution-treating temperatures until a temperature of approximately 120 to 150 °C (250 to 300 °F) is reached. At this temperature, transformation to martensite begins; this transformation is not complete until the piece has reached approximately 30 °C (90 °F) for 17-4 PH and 15-5 PH and 15 °C (60 °F) for PH 13-8 Mo. Cooling in this transformation range should be as uniform as possible throughout the cross section of the piece to prevent thermal cracking.

Upon completion of the forging of precipitation hardening grades, sections less than 75 mm (3 in.) in thickness should be air cooled to between 30 and 15 °C (90 and 60 °F) before any further processing. Intricate forgings should first be equalized for a short period of time (30 min to 1 h, depending on size) in the temperature range of 1040 °C (1900 °F) to the forging temperature. The part can then be allowed to air cool to between 30 and 15 °C (90 and 60 °F). This equalization relieves forging stresses and improves temperature uniformity on the part. Nonuniformity in cooling may promote cracking. Forgings that are more than 75 mm (3 in.) in section, after equalizing, should be air cooled until dull red or black, covered immediately and completely on all sides with a light gage metal cover (do not use galvanized) or thin ceramic thermal sheeting, then allowed to cool undisturbed to between 30 and 15 °C (90 and 60 °F). Cooling should be done in areas that are free from drafts and away from furnaces where temperatures in the surrounding area are above 30 °C (90 °F). The covered, cooling steel should not be placed too near other large forged sections that have been cooled or are practically cooled, because this can interfere with the uniformity of the cooling.

Furnace cooling of 17-4 PH and 15-5 PH large or intricate sections may be desirable in cold weather. This extends the cooling time considerably, but if necessary, the heated forgings should be air cooled to approximately 315 to 370 °C (600 to 700 °F), charged into a furnace, and equalized at that temperature. The furnace is then shut off, and the furnace and forgings should be allowed to cool to room temperature.

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Revised by Thomas Harris and Eugene Priebe, Armco Inc.

Heating of Dies

Dies are always heated for the forging of stainless steel. Large dies are heated in ovens; small dies, by burners of various design.

There is no close agreement among forge shops on the maximum die temperature that should be maintained, although it is generally agreed that 150 °C (300 °F) should be the minimum temperature. A range of 150 to 205 °C (300 to 400 °F) is common. Dies are sometimes heated to 315 °C (600 °F). Die temperature is determined by means of temperature-sensitive crayons or surface pyrometers.

Die Lubrication

Dies should be lubricated before each blow. For forging in shallow impressions, a spray of colloidal graphite in kerosene or in low-viscosity mineral oil is usually adequate. Ordinarily, dies are sprayed manually, but in press forging, automatic sprays timed with the press stroke are sometimes used. For deeper cavities, however, it is often necessary to use a supplemental spray (usually manual) to reach the deep areas of the cavity or to swab the cavity with a conventional forging oil. Forging oils are usually mixtures of oil and graphite; the oil should be free of lead and sulfur. Forging oils are often purchased as greases and are then diluted with mineral oil to the desired viscosity. Any volatile lubricant should be used sparingly. With even a slight excess, vapor explosions are likely, and greater amounts can cause explosions that will eject the workpiece, possibly injuring personnel.

Glass is sometimes used as a lubricant or billet coating in press forging. The glass is applied by dipping the heated forging in molten glass or by sprinkling the forging with glass frit. Glass is an excellent lubricant, but its viscosity must be compatible with the forging temperature used. For optimal results, the viscosity of the glass should be maintained at 10 Pa · s (100 cP). Therefore, when different forging temperatures are used, a variety of glass compositions must be stocked. Another disadvantage of glass is that it will accumulate in deep cavities, solidify, and impair metal flow. Therefore, the use of glass is generally confined to shallow forgings that require maximum lateral flow.

Forging of Stainless Steel

Revised by Thomas Harris and Eugene Priebe, Armco Inc.

Trimming

When production quantities justify the cost of tools, forgings are trimmed in dies. Hot trimming is preferred for all types of stainless steel, because less power is required and because there is less danger of cracking than in cold trimming. The precipitation-hardening stainless steels must be hot trimmed to prevent flash-line cracks, which can penetrate the forging.

It is often practical to hot trim immediately after the forging operation, before the workpiece temperature falls below a red heat. Less often, forgings are reheated to 900 to 950 °C (1650 to 1750 °F) and then trimmed.

Tool Materials. Punches for the hot trimming of closed-die forgings are often made of 6G or 6F2 die block steel at 41.8 to 45.7 HRC, and the blades are made of a high-alloy tool steel, such as D2, at 58 to 60 HRC (compositions of tool steels are given in the article "Dies and Die Materials for Hot Forging" in this Volume). In some forge shops, both punches and blades for hot trimming are made of a carbon or low-alloy steel (usually with less than 0.30% C) and then hard faced, generally with a cobalt-base alloy (a typical composition is Co-1.10C-30Cr-3Ni-4.50W).

Upset forgings can be hot trimmed in a final pass in the upsetter or in a separate press. For trimming in the upsetter, H11 tool steel at 46 to 50 HRC has performed successfully on a variety of forgings with a normal flash thickness. For the trimming of heavy flash in the upsetter, H21 at 50 to 52 HRC is recommended. Tools for hot trimming in a separate press are usually made of a 0.30% C carbon or low-alloy steel and are hard faced with a cobalt-base alloy (a typical composition is Co-1.10C-30Cr-3Ni-4.50W).

Forging of Stainless Steel

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Cleaning

Stainless steels do not form as much scale as carbon or alloy steels, especially when a protective atmosphere is provided during heating. However, the scale that does form is tightly adherent, hard, and abrasive. It must be removed prior to machining, or tool life will be severely impaired.

Mechanical or chemical methods, or a combination of both, can be used to remove scale. Abrasive blast cleaning is an efficient method and is applicable to forgings of various sizes and shapes in large or small quantities. When surfaces will not be machined or passivated, blasting must be done with only silica sand; the use of steel grit or shot will contaminate the surfaces and impair corrosion resistance.

Abrasive blast cleaning is usually followed by acid pickling. The forgings are then thoroughly washed in water.

Barrel finishing (tumbling) is sometimes used for descaling. Acid pickling is recommended after tumbling.

Wire brushing is sometimes used for removing scale from a few forgings. Brushes with stainless steel wire must be used unless the forgings will be machined or passivated.

Salt bath descaling followed by acid cleaning and brightening is an efficient method of removing scale. A typical procedure is detailed in Table 2. Additional information on scale removal is available in the articles "Classification and Selection of Cleaning Processes" and "Surface Engineering of Stainless Steels" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Table 2 Cycle for sodium hydride (reducing) descaling of annealed stainless steel forgings

Operation sequence	**Bath composition**	**Bath temperature, °C (°F)**	Treatment time, min
Descale	1.5 to 2.0% NaH	400-425 (750-800)	**20**
Quench	Water (circulated in tank)	Cold	**1-3**
Acid clean	10% H_2SO_4	65 (145)	**20**
Acid brighten	10% HNO_3-2% HF	65 (145)	**30**
Rinse	Water (high-pressure spray)	Ambient	**2**
Rinse	**Water**	**80 (175)**	**1-2**

Forging of Stainless Steel

Revised by Thomas Harris and Eugene Priebe, Armco Inc.

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Forging of Heat-Resistant Alloys

Revised by S.K. Srivastava, Haynes International, Inc.

Introduction

THE FORGING INDUSTRY has incorporated numerous technological innovations during the last two decades. The use of computer-aided design, manufacture, and engineering is particularly significant in the forging of heat-resistant alloys because of the premium placed on higher quality and lower cost. On one hand, the thrust of alloy development has been to increase the service temperature, which means lower forgeability of the alloys. On the other hand, near-net shape manufacturing demands even closer control on the final shape. Machining of these alloys is difficult and expensive and can sometimes amount to 40% of the cost of production. The complexity of these demands makes computers more relevant to the portion of the forging industry concerned with heat-resistant alloys. Computers can analyze and simulate the forging process, predict material flow, optimize the energy consumption, and perform design and manufacturing functions. More information on the use of computers in the modeling of the forging process is available in the Section "Computer-Aided Process Design for Bulk Forming" in this Volume.

Forgings of heat-resistant alloys are widely used in the power, chemical, and nuclear industries; as structural components for aircraft and missiles; and for gas-turbine and jet-engine components such as shafts, blades, couplings, and vanes. Because of their greater strength at elevated temperatures, these alloys are more difficult to forge than most metals. Heat-resistant alloys are more difficult to forge than stainless steels (see the article "Forging of Stainless Steel" in this Volume). Generally, these alloys can be grouped into two categories:

- Solid solution strengthened alloys such as Alloy X (UNS N06002)
- γ' strengthened alloys such as Waspaloy (UNS N07001)

The latter group is much more difficult to forge than the former.

Forging of Heat-Resistant Alloys

Revised by S.K. Srivastava, Haynes International, Inc.

Forging Methods

The three critical factors in any method of forging are reduction (strain), rate of reduction (strain rate), and temperature of the workpiece at any time during forging. Regardless of the method used, the forging of heat-resistant alloys should be done as part of total thermomechanical processing. In some cases, forgings are deliberately processed for better stress rupture, creep, and low-cycle fatigue life. Therefore, the objectives for the forgings are uniform grain refinement, controlled grain flow, and structurally sound components. These objectives often depend on melting practices, ingot-mold design, and ingot-billet breakdown practices. The soundness and uniformity of the forging billets must be ensured. In order to impart optimal work during each stage, it may even be necessary to include redundant work if work penetration in the subsequent processing sequence is not likely to be uniform.

Recrystallization must be achieved in each operation to obtain the desired grain size and flow characteristics. Recrystallization also helps to eliminate the grain- and twin-boundary carbides that tend to develop during static heating or cooling. Nonuniform distribution of inhomogeneities will likely lead to problems. Up to 80% of metal reduction accompanying recrystallization is usually completed over falling temperatures; the remaining 20% can be warm worked at lower temperatures for additional strengthening. The current trend in the forging of heat-resistant alloys is to lower the strain rate and to heat the dies. Faster strain rates lead to frictional heat buildup, nonuniform recrystallization, and metallurgical instabilities, and are also likely to cause radial-type ruptures, especially in high-γ' alloys such as Astroloy (UNS N13017) and U-700. Heat-resistant alloys can be forged by a variety of methods, and two or more of these methods are often used in sequence.

Open-die forging (hand or flat-die forging) can be used to produce preforms for relatively large parts, such as wheels and shafts for gas turbines. Many such preforms are completed in closed dies. Open-die forging is seldom used for producing forgings weighing less than 9 kg (20 lb). More information on forging with open dies is available in the article "Open-Die Forging" in this Volume.

Closed-die forging is widely used for forging heat-resistant alloys. The procedures, however, are generally different from those used for similar shapes from carbon or low-alloy steels (see the article "Closed-Die Forging in Hammers and Presses" in this Volume). For example, preforms made by open-die forging, upsetting, rolling, or extrusion are used to a greater extent for the closed-die forging of heat-resistant alloys than for steel. Because of the greater difficulties encountered in forging heat-resistant alloys as compared to forging similar sizes and shapes from steel, diemaking is also different (see the section "Dies" in this article).

Upset forging is commonly applied to heat-resistant alloys--sometimes as the only forging operation but more often to produce preforms (as for turbine buckets and blades). In the upset forging of heat-resistant alloys, the maximum unsupported length of upset is about two diameters. Additional information is available in the article "Hot Upset Forging" in this Volume.

Extrusion is also used to produce preforms for subsequent forging in closed dies, and it often competes with upsetting. Whether the preform is produced by extruding a slug or by forming an upset on the end of a smaller cross section depends mainly on the equipment available. Information on the extrusion process for heat-resistant alloys is available in the article "Conventional Hot Extrusion" in this Volume.

Roll forging is sometimes used to produce preforms for subsequent forging in closed dies. The rolling techniques used for preforming heat-resistant alloys are basically the same as those employed for preforming steel (see the article "Roll Forging" in this Volume). Roll forging saves material and decreases the number of closed-die operations required.

Ring rolling is sometimes used to save material when producing annular parts from hollow billets. The general method used for heat-resistant alloys is essentially the same as that for steel and is described in the article "Ring Rolling" in this Volume. Heat-resistant alloys with forgeability ratings of 1 or 2 (see Table 1) can be ring rolled using the same procedures as those carbon and low-alloy steels. Alloys with forgeability ratings of 3, 4, and 5 require more steps in ring rolling as well as supplemental heating with auxiliary torches.

Table 1 Forging temperatures and forgeability ratings for heat-resistant alloys

Alloy	UNS designation	Forging temperature[(a)]				Forgeability rating[(b)]
		Upset and breakdown		Finish forging		
		°C	°F	°C	°F	
Iron-base alloys						
A-286	S66286	1095	2000	1040	1900	**1**
Alloy 556	R30556	1175	2150	1175	2150	**3**
Alloy 800	N08800	1150	2100	1040	1900	**1**
Nickel-base alloys						
Astroloy	N13017	1120	2050	1120	2050	**5**
Alloy X	N06002	1175	2150	1175	2150	**3**
Alloy 214	. . .	1160	2125	1040	1900	**3**
Alloy 230	. . .	1205	2200	1205	2200	**3**
Alloy 600	N06600	1150	2100	1040	1900	**1**
Alloy 718	N07718	1095	2000	1040	1900	**2**
Alloy X-750	N07750	1175	2150	1120	2050	**2**
Alloy 751	N07751	1150	2100	1150	2100	**3**
Alloy 901	N09901	1150	2100	1095	2000	**2**
M-252	N07252	1150	2100	1095	2000	**3**
Alloy 41	N07041	1150	2100	1120	2050	**4**
U-500	N07500	1175	2150	1175	2150	**3**

U-700	. . .	1120	2050	1120	2050	**5**
Waspaloy	N07001	1160	2125	1040	1900	**3**
Cobalt-base alloys						
Alloy 25	. . .	1230	2250	1230	2250	**3**
Alloy 188	**R30188**	**1205**	**2200**	**1175**	**2150**	**3**

(a) Lower temperatures are often used for specific forgings to conform to appropriate specifications or to achieve structural uniformity.

(b) Based on the considerations stated in the section "Forging Alloys" in this article. 1, most forgeable; 5, least forgeable.

Isothermal forging and hot-die forging of heat-resistant alloys offer a number of advantages. Closer tolerances than those possible in conventional forging processes can be achieved, resulting in reduced material and machining costs. Because die chilling is not a problem in isothermal or hot-die forging, lower strain rates (hydraulic presses) can be used. This lowers the flow stress of the work material; therefore, forging pressure is reduced, and larger parts can be forged in existing hydraulic presses. Additional information is available in the article "Isothermal and Hot-Die Forging" in this Volume, and a specific type of isothermal forging process is briefly discussed in the section "Powder Alloys" in this article.

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Forging Alloys

Table 1 lists the most commonly forged heat-resistant alloys, and their forging temperatures and forgeability ratings.

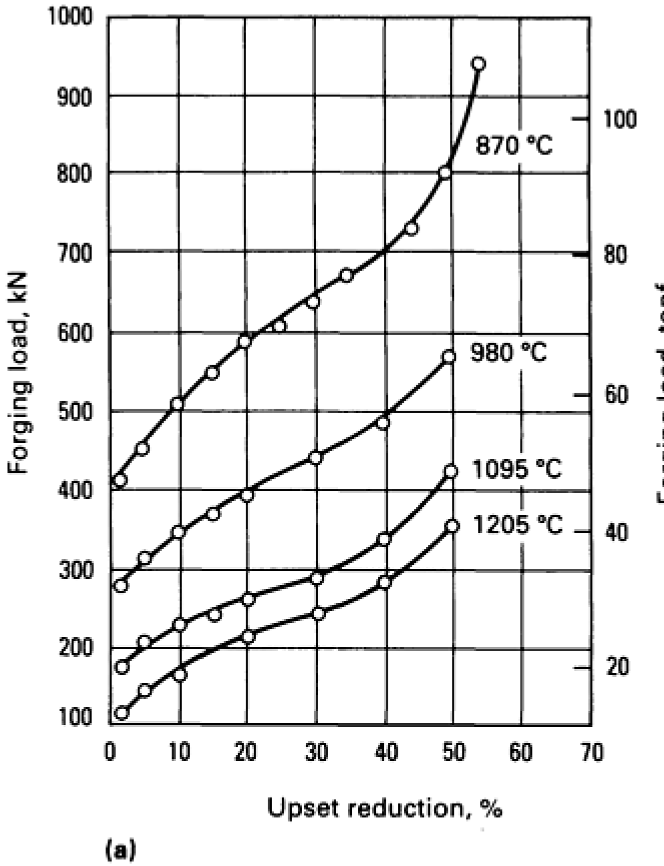
General Characteristics. The two basic material characteristics that greatly influence the forging behavior of heat-resistant alloys are flow stress and ductility. Because these alloys were designed to resist deformation at high temperatures, it is not surprising that they are very difficult to hot work; ductility is limited, and the flow stress is high. Further, any alloying addition that improves the service qualities usually decreases workability. These alloys are usually worked with the precipitates dissolved; the higher concentration of dissolved alloying elements (40 to 50% total) gives rise to higher flow stress, higher recrystallization temperature, and lower solidus temperature, thus narrowing the useful temperature range for hot forming. Where ductility is defined as the amount of strain to fracture, the ductility of these alloys is influenced by the deformation temperature, strain rate, prior history of the material, composition, degree of segregation, cleanliness, and the stress state imposed by the deformation process.

Temperature limits for forging nickel-base heat-resistant alloys are largely determined by melting and precipitation reactions. As with all heat-resistant alloys, an intermediate temperature region of low ductility is likely to be encountered in attempts to forge metals near a temperature between regimes of low- and high-temperature deformation. The region of low ductility often occurs at temperatures around 0.5 of the melting point as measured on the Kelvin scale. The dividing temperature has a physical basis. At hot-working temperatures, self-diffusion rates are high enough for recovery and recrystallization to counteract the effects of strain hardening.

Iron-Base Alloys. Stock for forgings of the iron-base alloys is generally furnished as press-forged squares or hot-rolled rounds, depending on size. As-cast ingots are sometimes used.

The inclusion content of the alloys has a significant effect on their forgeability. Alloys containing titanium and aluminum can develop nitride and carbonitride segregation, which later appears as stringers in wrought bars and affects forgeability. This type of segregation has been almost completely eliminated through the use of vacuum melting. Therefore, iron-base alloys can be forged into a greater variety of shapes with greater reductions, approaching the forgeability of AISI type 304 stainless steel.

Temperature has an important effect on forgeability. The optimal temperature range for forging A-286 and similar iron-base alloys is narrow. The forgeability of A-286, based on the forging load required for various upset reductions at four forging temperatures, is shown in Fig. 1(a). Figure 1(b) shows that, on the basis of forging pressure, A-286 is considerably more difficult to forge than 1020 steel, even though A-286 is among the most forgeable of the heat-resistant alloys (Table 1). For example, as shown in Fig. 1(b), 1020 steel at 1205 °C (2200 °F) requires only about 69 MPa (10 ksi) for an upset reduction of 30%, but for the same reduction at the same temperature, A-286 requires approximately 172 MPa (25 ksi).



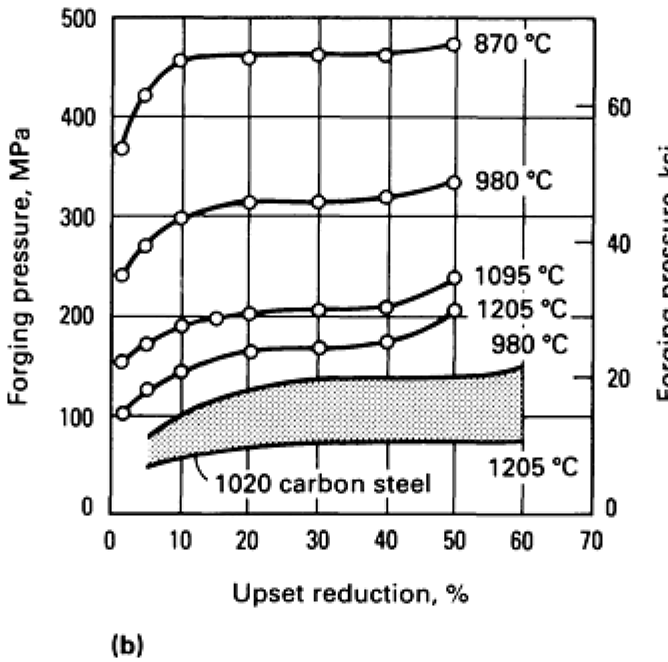


Fig. 1 Effect of upset reduction at four temperatures on forging load in the forging of A-286 (a), and the forging pressure for A-286 compared with that for 1020 steel (b). Source: Ref 1.

Forging pressures increase somewhat for greater upset reductions at normal forging temperatures. As shown in Fig. 2, the pressure for a 20% upset reduction of A-286 at 1095 °C (2000 °F) is about 193 MPa (28 ksi), but for an upset reduction of 50% the pressure increases to about 241 MPa (35 ksi). Figure 2 also shows that forging pressure is up to 10 or 12 times greater than the tensile strength of the alloy at forging temperature.

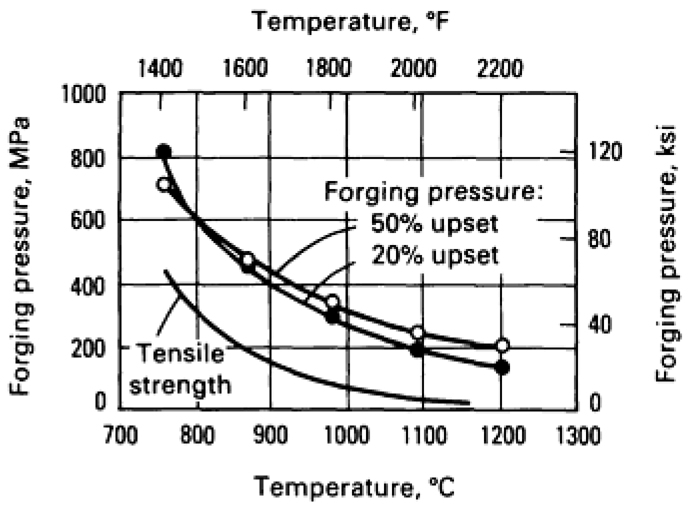


Fig. 2 Forging pressure versus temperature for A-286. Also shown is the effect of increasing temperature on the tensile strength of the material. Upset strain rate: 0.7 s^{-1}. Source: Ref 2.

Strain rates also influence forging pressures. Figure 3 shows that as strain rate increases, more energy is required in presses and hammers.

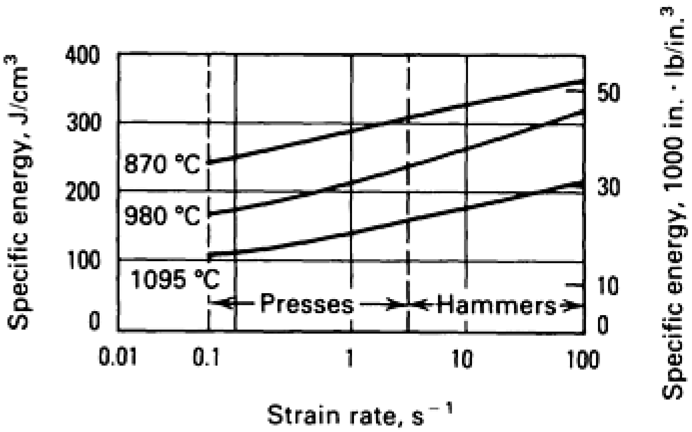


Fig. 3 Specific energy versus strain rate in the press and hammer forging of A-286 at three temperatures. Source: Ref 2.

Nickel-base alloys initially consisted of relatively simple nickel-chromium alloys hardened by small additions of titanium and aluminum for service to 760 °C (1400 °F). With the development of production vacuum-melting techniques, workable alloys can be produced that contain relatively large amounts of titanium, aluminum, zirconium, niobium, and other reactive elements. Nitrogen and oxygen levels are reduced by vacuum melting, which eliminates most of the nitrides and oxides that contribute to poor forgeability. Therefore, current nickel-base alloys consist of numerous compositions containing larger amounts of hardening elements.

The nickel-base alloys are available in various cogged billet and bar sizes for forging. The alloys are ordinarily melted by one of the following methods:

- Air melting, followed by vacuum induction melting or vacuum consumable-electrode arc melting
- Vacuum induction melting followed by vacuum consumable-electrode arc melting

- Consumable-electrode arc melting under slag

Compared with ordinary arc-melting techniques, these three melting procedures have produced marked improvements in forgeability by reducing the levels of segregation. However, most ingots made on a production basis still contain enough segregation to influence forgeability. Ingots produced by vacuum induction melting solidify progressively toward the center and take longer to freeze than ingots manufactured by other methods; therefore, the alloying elements and impurities concentrate at the center. The segregation is generally less in ingots produced by consumable-electrode arc melting.

As shown in Table 1, the nickel-base alloys are, in general, less forgeable than the iron-base alloys; almost all of the nickel-base alloys require more force for producing a given shape. Astroloy (UNS N13017) and Alloy U-700 are the two most difficult-to-forge nickel-base alloys. For a given percentage of upset reduction at a forging temperature of 1095 °C (2000 °F), these alloys require about twice the specific energy needed for the iron-base A-286.

In the forgeability ratings listed in Table 1, Astroloy and U-700 alloys have about one-fifth the forgeability of Alloy 600 (UNS N06600). However, these ratings reflect only a relative ability to withstand deformation without failure; they do not indicate the energy or pressure needed for forging, nor can the ratings be related to low-alloy steels and other alloys that are considerably more forgeable.

The forging of nickel-base alloys requires close control over metallurgical and operational conditions. Particular attention must be given to control of the work metal temperature. Figure 4 shows ductility (measured by percentage of reduction in area) versus temperature curves for several nickel-base alloys. Data on transfer time, soaking time, finishing temperature, and percentage of reduction should be recorded. Critical parts are usually numbered, and precise records are kept. These records are useful in determining the cause of defective forgings, and they permit metallurgical analysis so that defects can be avoided in future products.

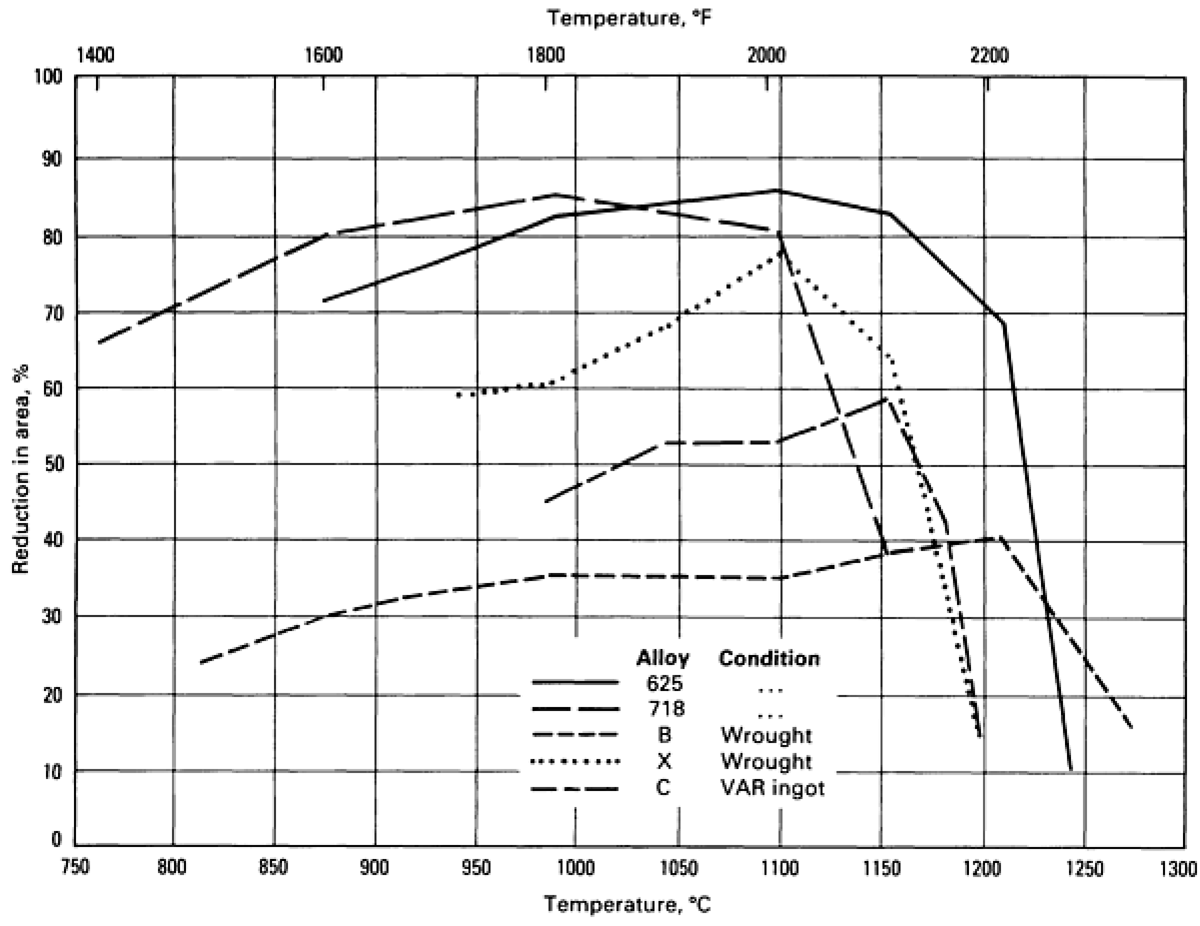


Fig. 4 Ductility (measured by percentage of reduction in area) versus temperature for several nickel-base heat-

resistant alloys. Source: Ref 3, 4, 5, 6, 7, 8, and 9.

The nickel-base alloys are sensitive to minor variations in composition, which can cause large variations in forgeability, grain size, and final properties. In one case, wide heat-to-heat variations in grain size occurred in parts forged from Alloy 901 (UNS N09901) in the same sets of dies. For some parts, optimal forging temperatures had to be determined for each incoming heat of material by making sample forgings and examining them after heat treatment for variations in grain size and other properties.

In the forging of nickel-base alloys, the forging techniques developed for one shape usually must be modified when another shape is forged from the same alloy; therefore, development time is often necessary for establishing suitable forging and heat-treating cycles. This is especially true for such alloys as Waspaloy (UNS N07001), Alloy 41 (UNS N07041), U-500 (UNS N07500), and U-700.

Cobalt-Base Alloys. Many of the cobalt-base alloys cannot be successfully forged because they ordinarily contain more carbon than the iron-base alloys and therefore greater quantities of hard carbides, which impair forgeability.

The two cobalt-base alloys listed in Table 1 are forgeable. The strength of these alloys at elevated temperatures, including the temperatures at which they are forged, is considerably higher than that for iron-base alloys; consequently, the pressures required in forging them are several times greater than those for the iron-base alloys.

Even when forged at its maximum forging temperature, Alloy-25 work hardens; therefore, forging pressure must be increased with greater reductions. Accordingly, this alloy generally requires frequent reheating during forging to promote recrystallization and to lower the forging pressure for subsequent steps.

Forging conditions (temperature and reduction) have a significant effect on the grain size of cobalt-base alloys. Because low ductility, notch brittleness, and low fatigue strength are associated with coarse grains, close control of forging and of final heat treatment is important.

Cobalt-base alloys are susceptible to grain growth when heated above about 1175 °C (2150 °F). They heat slowly and require a long soaking time for temperature uniformity. Forging temperatures and reductions, therefore, depend on the forging operation and the part design.

The alloys are usually forged with small reductions in initial breakdown operations. The reductions are selected to impart sufficient strain to the metal so that recrystallization (and usually grain refinement) will occur during subsequent reheating. Because the cross section of a partly forged section has been reduced, less time is required to reach temperature uniformity in reheating. Consequently, because reheating time is shorter, the reheating temperature may sometimes be increased 30 to 85 °C (50 to 150 °F) above the initial forging temperature without harmful effects. However, if the part receives only small reductions in subsequent forging steps, forging should be continued at the lower temperatures. These small reductions, in turn, must be in excess of about 5 to 15% to avoid abnormal grain growth during subsequent annealing. The forging temperatures given in Table 1 are usually satisfactory.

Powder Alloys. Some alloys, such as Alloy IN-100 and Alloy 95, contain very high proportions of γ', and their cast ingots cannot be forged. Powders of these alloys, however, can be compacted by a number of techniques to produce billets having a very fine grain structure. Such billets can then be superplastically forged. Pratt and Whitney Aircraft has used its patented Gatorizing process to produce preforms for engine compressor and turbine disks with IN-100 billets. In Gatorizing, which is a type of isothermal forging process, both the workpiece and the dies are maintained at 1175 °C (2150 °F). Boron nitride is used as the lubricant. The process is done in vacuum in order to protect the heated dies from oxidation. The use of Gatorizing has led to substantial reductions in material use and finish machining.

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Forging of Heat-Resistant Alloys

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Machines

The hammers, presses, upsetters, roll and ring forging machines, and rotary forging machines used in the forging of steel are also used in the forging of heat-resistant alloys, except that more power is needed for forging a given shape from a heat-resistant alloy than for steel. Detailed information on hammers and presses is available in the articles "Hammers and Presses for Forging" and "Selection of Forging Equipment" in this Volume.

Steam or air hammers are extensively used for producing preforms in open dies, particularly for forgings that weigh 45 kg (100 lb) or more. For smaller forgings, particularly for those weighing less than 9 kg (20 lb), preforms are more often produced in rolls, presses, or upsetters.

Steam hammers are also extensively used for producing large forgings (generally over 45 kg, or 100 lb, and up to about 910 kg, or 2000 lb) in closed dies. A distinct advantage of a power hammer for this type of work is the short time of contact between the dies and the hot work metal; therefore, less heat is transferred to the dies than in press forging. A disadvantage of hammer forging is that, because of the severe impact blows, temperature may be excessively increased locally in the metal being forged. As a result, localized grain growth can take place. Also, the very high strain rates experienced in hammer forging can be detrimental in forging of strain-rate sensitive materials.

Mechanical presses are most often used for producing closed-die forgings that weigh less than 9 kg (20 lb)--turbine buckets and blades, for example. Mechanical presses are used less often for forgings that weigh 9 to 45 kg (20 to 100 lb) and are seldom used for closed-die forgings weighing over 45 kg (100 lb). Mechanical presses are preferred for small forgings that require close tolerances because closer control of dimensions and longer die life can be obtained in presses than in hammers.

Hydraulic presses are used for producing large forgings (up to several tons) from heat-resistant alloys. One advantage of a hydraulic press is that the temperature throughout the metal being forged remains more nearly uniform than in hammer forging.

The main disadvantage of forging in a hydraulic press is the long die contact time with the hot workpiece. This causes cooling of the workpiece (cracks may occur in chilled regions) and buildup of heat in the dies.

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Dies

Because of the forces required for forging heat-resistant alloys, special attention must be given to die design, die material, and diemaking practice (see also the article "Dies and Die Materials for Hot Forging" in this Volume).

Die Design. Die cavities need not be different from those used to forge the same shape from steel. However, because of the greater forces required for forging heat-resistant alloys, more attention must be given to the strength of the die in order to prevent breakage; the original dies must be thicker or the number of resinkings will be fewer. For very deep dies, support rings must be used to prevent die breakage.

Iron-base alloys have been forged in dies previously used for producing the same shapes from steel. For forging some nickel-base alloys, however, the dies formerly used for steel are not used; these alloys require more rugged dies.

Die Material. Die life is a major problem in forging heat-resistant alloys, and dies often must be reworked after forging as few as 400 pieces. In contrast, if carbon steel were forged to the same shape, the dies would generally produce 10,000 to 20,000 forgings before major rework. The difference is due to the greater strength of heat-resistant alloys at high temperature and the closer tolerances that are usually required for heat-resistant alloy forgings. As a result, every effort is made through the selection of die material and hardness to prolong die life.

Most dies for forging in hammers and mechanical presses are made of hot-work tool steel such as AISI H11, H12, or H13. Optimal die life can be obtained by heat treating dies to as high a hardness as possible, although some hardness must be sacrificed to obtain toughness and to prevent the possibility of premature die breakage. For example, in forging turbine buckets in a mechanical press, the hardness of the bottom die may range from 47 to 56 HRC. For forgings of minimum severity, the bottom die is heat treated to 53 to 56 HRC. As severity increases, the hardness of the bottom die is decreased; 47 to 49 HRC is used for forgings of maximum severity.

The bottom die is always given primary consideration because it is in contact with the heated workpiece longer than the top die and is more likely to break from the wedging effect. The top die is operated at a lower temperature than the bottom die; therefore, it can be made from a die steel having greater wear resistance--but at some sacrifice of shock resistance.

When hydraulic presses are used, as in the forging of large turbine disks, it may be necessary to use heat-resistant alloys as the die material. If die temperatures do not exceed 595 °C (1100 °F), dies made from steels such as H11 or H13 are generally satisfactory. However, in hydraulic presses, it is not unusual for the dies, or parts of dies, to reach 925 °C (1700 °F). To resist such high temperatures, dies or die inserts are sometimes made from nickel-base alloys such as Alloy 41. Inserts are used in areas that are excessively heated during forging.

Isothermal forging requires strength and integrity of the dies at temperatures of the workpiece. In the superplastic forging of Alloy IN-100, TZM molybdenum alloy dies have been used. However, this requires either a vacuum or an inert atmosphere to prevent oxidation of the die.

Diemaking Practice. Multiple-cavity dies, such as those used in the forging of steel, are seldom used in the forging of heat-resistant alloys. Blocking, semifinishing, and finishing operations are performed separately in single-cavity dies, often in different hammers or presses and at different times. This procedure is used because:

- The heating range is usually quite narrow, so that there is time for only one operation before the workpiece is too cold
- Tolerances are usually close, so that all forging is best done in the center of the hammer or press
- Because of the short die life, a more economical diemaking and die reconditioning program can be established by using single-cavity dies

Almost without exception, the dies used for the forging of heat-resistant alloys are made of the same materials and by approximately the same practice without regard for the number of forgings to be produced. Parts forged from heat-resistant alloys are costly and are intended for critical end uses; therefore, no downgrading can be permitted in tooling. Further, tolerances are usually the same for both small and large numbers of forgings.

In addition, because heat-resistant alloys are difficult to forge and close dimensional tolerances are usually demanded, life of the finishing dies is short. The finishing die is often used until tolerances can no longer be met and is then recut for a semifinishing impression or for the blocker impression.

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Preparation of Stock

Shearing is widely used for cutting small bars in preparing stock for forging. The maximum size of bar that can be sheared depends mainly on the available equipment. A cross section of approximately 25 mm (1 in.) is often the maximum size cut by shearing. For cutting thicker cross sections, an abrasive cutoff wheel is satisfactory and economical.

Because heat-resistant alloys are relatively hard, sheared surfaces are generally smooth without excessive distortion, provided shear blades are kept sharp. However, shear blades wear rapidly and often must be reconditioned after shearing 50 to 100 pieces.

Heating. Forging temperature varies widely, depending on the composition of the alloy being forged (Table 1) and to some extent on the heat treatment and end use. Forging-temperature ranges are relatively narrow, but temperatures can be increased for better forgeability if the end use permits. Excessively high forging temperatures cause grain growth in most heat-resistant alloys and adversely affect subsequent heat treatment. Therefore, when maximum properties are required for end use, forging temperatures must be precisely controlled. Lower forging temperatures are less likely to cause damage to the workpiece, but the additional forging blows required will shorten die life.

Atmosphere protection for heating the forging stock is desirable but not essential, because heat-resistant alloys have high resistance to oxidation at elevated temperature. Protective atmospheres provide cleaner surfaces on finished forgings and therefore minimize subsequent cleaning problems.

Electrically heated furnaces are often preferred for heating forging stock because their temperatures can be closely controlled and the possibility of contaminating the work metal is minimized. Fuel-fired furnaces are used less frequently than those heated by electrical resistance. If fuel-fired furnaces are used, the fuel must have extremely low sulfur content, especially when heating the nickel-base alloys, or contamination may occur.

Any type of pyrometric control that can maintain temperature within ±6 °C (±10 °F) is suitable for temperature control. Recording types are preferred because they allow the operator to observe the behavior of the furnace. As the pieces of stock are discharged from the furnace, periodic checks should be made with an optical pyrometer. This permits a quick comparison of work metal temperature with furnace temperature.

The time at temperature is less critical than the necessity for precise temperature control. Grain growth takes place slowly in heat-resistant alloys (unless the temperature is increased above the normal forging temperature), and oxidation is at a minimum; consequently, heating time is less critical than for carbon or alloy steel. In the event of a major breakdown in the equipment while at elevated temperature, the best practice is to remove heated stock from the furnace.

Reheating. Because of the narrow heating range, temperatures of the partly finished forgings must be checked carefully, and the workpieces must be reheated as required to keep them within the prescribed temperature range. This is one reason for using single-cavity dies. It is usually necessary to reheat the work after each forging operation, even when the operations immediately follow each other.

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Heating of Dies

Dies are always heated for the forging of heat-resistant alloys. The heating is usually done with various types of burners, although embedded elements are sometimes used. Optimal die temperature for conventional hot forging varies from 150 to 260 °C (300 to 500 °F); the lubricant used is an important limitation on maximum die temperature. Die temperature is controlled by the use of temperature-sensitive crayons or surface pyrometers.

Lubricants

Dies should be lubricated before each forging. For shallow impressions, a spray of colloidal graphite in water or in mineral oil is usually adequate. Dies are usually sprayed manually, although some installations include automatic sprays that are timed with the press stroke. Deeper cavities, however, may require the use of a supplemental spray (usually manually controlled) to ensure coverage of all surfaces, or they can be swabbed with a conventional forging oil. These oils are readily available as proprietary compounds.

Cooling Practice

Specific cooling procedures are rarely, if ever, needed after the forging of heat-resistant alloys. If forging temperatures are correctly maintained, the forgings can be cooled in still air, after which they will be in suitable condition for heat treating.

Heat Treatment

The heat treatment of wrought heat-resistant alloy forgings consists largely of solution annealing and precipitation-hardening treatments. Iron- and nickel-base heat-resistant alloys consist of a face-centered cubic (fcc) matrix at room and elevated temperatures. This phase is typically referred to as γ, or austenite, and is analogous to the high-temperature fcc phase formed during heat treatment of steels.

Alloying additions lead to the precipitation of various phases, including γ'[Ni_3(Al, Ti)], γ'', and various carbides such as MC (M = titanium, niobium, and so on), M_6C (M = molybdenum and/or tungsten), or $M_{23}C_6$ (M = chromium). In general, the primary strength of heat-resistant alloys is derived from the γ' and γ'' dispersion developed through heat treatment. In nickel-base alloys such as Waspaloy and Astroloy, aluminum and, to some degree, titanium combine with nickel to form γ'. In nickel-iron-base alloys, (for example, Alloy 718 and Alloy 901) and iron-base alloys (for example, A286), titanium, niobium, and, to a lesser extent, aluminum combine with nickel to form γ' or γ''. Further, the nickel-iron and iron-base alloys are all prone to the formation of other phases, such as those referred to as η(Ni_3Ti) and δ(Ni_3Nb).

The solution annealing and precipitation temperature regimes for several of the important superalloys are shown in the pseudo binary phase diagrams in Fig. 5. For both Waspaloy and Alloy 901, the solvus temperatures depend primarily on the aluminum and titanium contents, not on other alloying elements such as molybdenum and chromium, which provide solid-solution strength to the γ matrix.

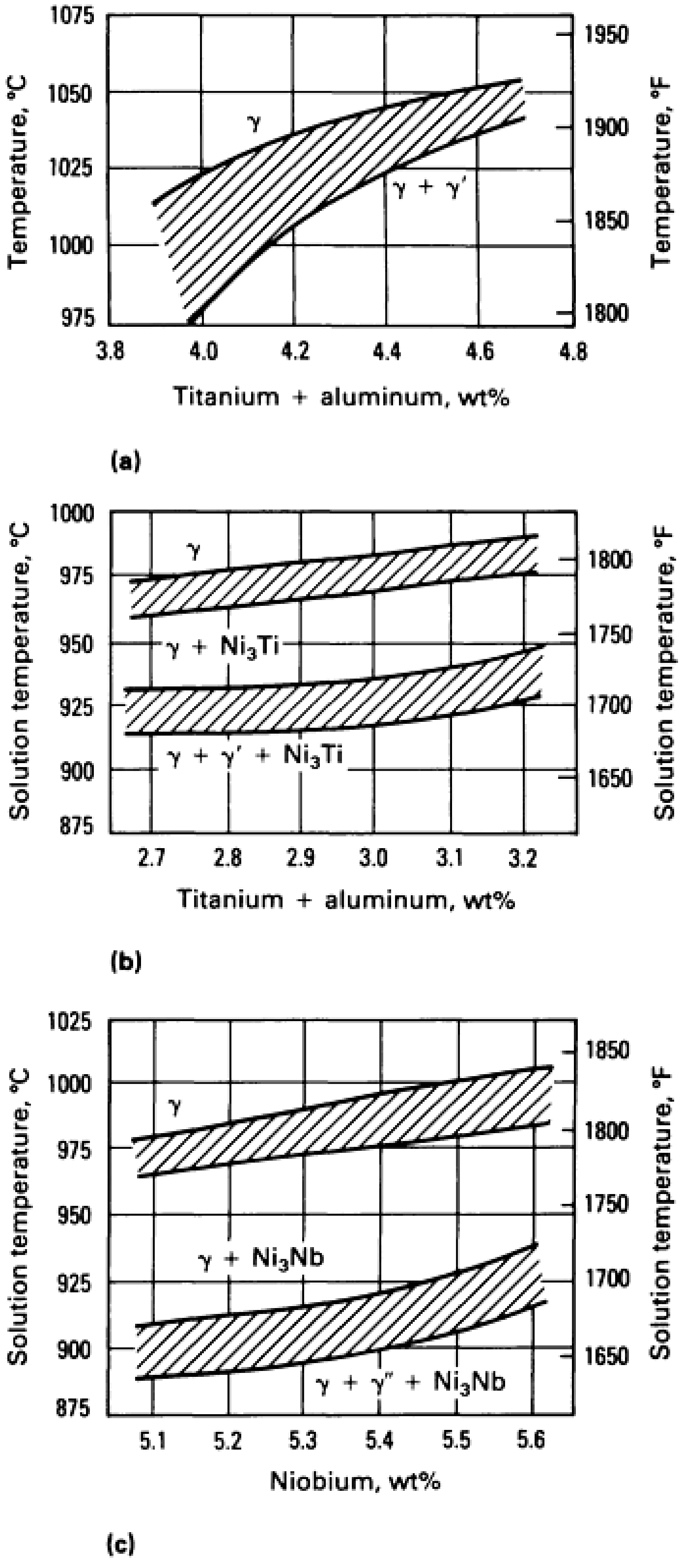

Fig. 5 Portions of pseudo binary phase diagrams for Waspaloy alloy held at temperature for 4 h and oil quenched (a), Alloy 901 held at solution temperature for 1 h and oil quenched (b), Alloy 718 held at solution temperature for 1 h and air cooled (c). Source: Ref 10.

Similarly, the solution and precipitation temperatures in Alloy 718 are strongly dependent on niobium content. It can also be seen in Fig. 5 that the heat treatment of the alloys must be carried out at very high temperatures. These temperatures are usually only several hundred degrees Fahrenheit below those at which incipient melting occurs. Therefore, the forging of these alloys is quite difficult. However, these same characteristics enable superalloy forgings to be used at very high temperatures that are often substantially above those at which high-strength quenched-and-tempered steels are appropriate. Heat treatments for several heat-resistant alloys are summarized in Table 2.

Table 2 Heat treatments for several wrought heat-resistant alloys

Alloy	**UNS designation**	Heat treatment	
		Solution treatment	Aging treatment
Waspaloy	N07001	Hold at 1080 °C (1975 °F) for 4 h; air cool.	**Hold at 840 °C (1550 °F) for 24 h and air cool; hold at 760 °C (1400 °F) for 16 h and air cool.**
Astroloy	N13017	Hold at 1175 °C (2150 °F) for 4 h and air cool; hold at 1080 °C (1975 °F) for 4 h and air cool.	**Hold at 840 °C (1550 °F) for 24 h and air cool; hold at 760 °C (1400 °F) for 16 h and air cool.**
Alloy 901	N09901	Hold at 1095 °C (2000 °F) for 2 h and water quench.	**Hold at 790 °C (1450 °F) for 2 h and air cool; hold at 720 °C (1325 °F) for 24 h and air cool.**
Alloy 718	N07718	Hold at 980 °C (1800 °F) for 1 h and air cool.	**Hold at 720 °C (1325 °F) for 8 h and furnace cool; hold at 620 °C (1150 °F) for 8 h and air cool.**
A-286	**S66286**	**Hold at 980 °C (1800 °F) for 1 h and air cool.**	**Hold at 720 °C (1525 °F) for 16 h and air cool.**

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Surface Finish

Because heat-resistant alloys resist scaling, better surface finishes can be produced on forgings than are possible with most other forged metals. Die finish is a major factor affecting surface finish; to produce the best finish on forgings, all dies, new or reworked, must be carefully polished and stoned. The type of alloy forged and the amount of draft have only minor influence on final surface finish.

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Forging of Refractory Metals

Introduction

REFRACTORY METALS are forged from as-cast ingots or from billets that have been previously broken down by forging or extrusion. Forgeability depends to some extent on the method used to work the ingot into a billet. The forging characteristics of refractory metals and alloys are listed in Table 1.

Table 1 Forging characteristics of refractory metals and alloys

Metal or alloy	**Approximate solidus temperature**		**Recrystallization temperature, minimum**		**Hot-working temperature, minimum[(a)]**		**Forging temperature**		Forgeability
	°C	**°F**	**°C**	**°F**	**°C**	**°F**	**°C**	**°F**	
Niobium and niobium alloys									

99.2% Nb	2470	4475	1040	1900	815	1500	20-1095	70-2000	**Excellent**
Nb-1Zr	2400	4350	1040	1900	1150	2100	20-1260	70-2300	**Excellent**
Nb-33Ta-1Zr	2520	4570	1205	2200	1315	2400	1040-1480	1900-2700	**Good**
Nb-28Ta-10W-1Zr	2590	4695	1230	2250	1315	2400	1260-1370[(b)]	2300-2500	**Good[(b)]**
Nb-10Ti-10Mo-0.1C	2260	4100	1205	2200	1370	2500	1040-1480	1900-2700	**Moderate**
Nb-10W-1Zr-0.1C	2595	4700	1150	2100	1205	2200	1095-1205[(b)]	2000-2200	**Moderate[(b)]**
Nb-10W-2.5Zr	. . .	. . .	1150	2100	1260	2300	1205-1425[(b)]	2200-2600	**Good[(b)]**
Nb-15W-5Mo-1Zr	2480	4500	1425	2600	1650	3000	1315-1650	2400-3000	**Fair**
Nb-10Ta-10W	2600	4710	1150	2100	1315	2400	925-1205	1700-2200	**Good**
Nb-5V-5Mo-1Zr	2370	4300	1150	2100	1315	2400	1205-1650	2200-3000	**Moderate[(b)]**
Nb-10W-10Hf-0.1Y	. . .	. . .	1095	2000	1205	2200	1095-1650[(b)]	2000-3000	**Good[(b)]**
Nb-30Ti-20W	>2760	>5000	1260	2300	1150	2100	1150-1260	2100-2300	**Good**
Tantalum and tantalum alloys									
99.8% Ta	2995	5425	1095	2000	1315	2400	20-1095[(b)]	70-2000	**Excellent[(b)]**
Ta-10W	3035	5495	1315	2400	1650	3000	980-1260[(b)]	1800-2300	**Good[(b)]**
Ta-12.5W	3050	5520	1510	2750	>1650	>3000	>1095[(b)]	>2000	**Good[(b)]**
Ta-30Nb-7.5V	2425	4400	1150	2200	1540	2800	1150-1315[(b)]	2200-2400	**Good[(b)]**
Ta-8W-2Hf	2980	5400	1540	2800	>1650	>3000	>1095[(b)]	>2000	**Good[(b)]**
Ta-10Hf-5W	2990	5420	1315	2400	1650	3000	>1095[(b)]	>2000	**Good[(b)]**
Ta-2.5W	>2760	>5000	1260	2300	1150	2100	20-1150	70-2100	**Excellent**
Molybdenum and molybdenum alloys									

Unalloyed Mo	2610	4730	1150	2100	1315	2400	1040-1315	1900-2400	**Good**
Mo-0.5Ti	2595	4700	1315	2400	1480	2700	1150-1425	2100-2600	**Good-fair**
Mo-0.5Ti-0.08Zr	2595	4700	1425	2600	1650	3000	1205-1480	2200-2700	**Good**
Mo-25W-0.1Zr	2650	4800	1425	2600	1650	3000	1040-1315	1900-2400	**Fair**
Mo-30W	2650	4800	1260	2300	1370	2500	1150-1315	2100-2400	**Fair**
Tungsten and tungsten alloys									
Unalloyed W	3410	6170	1370-1595	2500-2900	. . .	. . .	1205-1650	2200-3000	**. . .**
W-1ThO$_2$	3410	6170	1595-1650	2900-3000	. . .	. . .	1315-1925	2400-3500	**. . .**
W-2ThO$_2$	3410	6170	1650-1760	3000-3200	. . .	. . .	1315-1370	2400-2500	**. . .**
W-2Mo	3385	6125	1540-1650	2800-3000	. . .	. . .	1150-1370	2200-2500	**. . .**
W-15Mo	3300	5970	1480-1595	2700-2900	. . .	. . .	1095-1370	2000-2500	**. . .**
W-26Re	3120	5650	>1870	>3400	. . .	. . .	>1480	>2700	**. . .**
W-0.5Nb	**3405**	**6160**	**1705-1870**	**3100-3400**	**. . .**	**. . .**	**1205-1650**	**2200-3000**	**. . .**

(a) Minimum hot-working temperature is the lowest forging temperature at which alloys begin to recrystallize during forging.

(b) Based on breakdown forging and rolling experience

Niobium and Niobium Alloys

Niobium and several of its alloys, notably, Nb-1Zr and Nb-33Ta-1Zr, can be forged directly from the as-cast ingot. Most impression-die forging experience, however, has been with unalloyed niobium.

Unalloyed niobium and the two alloys mentioned above can be cold worked. Other alloys, such as Nb-15W-5Mo-1Zr, generally require initial hot working by extrusion to break down the coarse grain structure of as-cast ingots before finish forging.

The billets are usually heated in a gas furnace using a slightly oxidizing atmosphere. Niobium alloys tend to flow laterally during forging. This results in excessive flash that must be trimmed from forgings.

Niobium and its alloys can be protected from oxidation during hot working by dipping the billets in an Al-10Cr-2Si coating at 815 °C (1500 °F), then diffusing the coating in an inert atmosphere at 1040 °C (1900 °F). The resulting coating is about 0.05 to 0.1 mm (2 to 4 mils) thick and provides protection from atmospheric contamination at temperatures to 1425 °C (2600 °F). Glass frit coatings can also be applied to the workpiece before heating in a gas-fired furnace.

Molybdenum and Molybdenum Alloys

The forging behavior of molybdenum and molybdenum alloys depends on the preparation of the billet. Billets prepared by pressing and sintering can be forged directly. Large billets are open die forged or extruded before closed-die forging. Arc-cast billets are usually brittle in tension; they cannot be forged before extruding, except at extremely high temperatures. A minimum extrusion ratio for adequate forgeability is 4 to 1.

Workpieces subjected to large reductions usually exhibit anisotropy and will recrystallize at lower temperatures than parts given less reduction. Forging temperature and reduction must be carefully controlled to avoid premature recrystallization in service and the resulting loss in strength.

Gas- or oil-fired furnaces can be used to heat molybdenum and its alloys to approximately 1370 °C (2500 °F). Induction heating is required for higher forging temperatures. Above 760 °C (1400 °F), molybdenum forms a liquid oxide that volatilizes rapidly enough that surface contamination is rarely a problem. If metal losses are excessive, protective atmospheres such as argon, carbon monoxide, or hydrogen can be used during heating. The liquid oxide formed during heating also serves as a lubricant. Glass coatings are also used; in addition to providing lubrication, glass coatings reduce heat losses during forging. Molybdenum disulfide and colloidal graphite are suitable lubricants for small forgings.

Tantalum and Tantalum Alloys

Unalloyed tantalum and most of the single-phase alloys listed in Table 1 can be forged directly from cast ingots. However, breakdown operations are usually required in order to avoid laps, wrinkles, internal cracks, and other forging defects. The breakdown temperature is 1095 to 1315 °C (2000 to 2400 °F). After about 50% reduction, the forging temperature may be permitted to drop slightly below 1095 °C (2000 °F). Billets produced by powder metallurgy techniques do not lend themselves to direct forging and must be subjected to breakdown.

Most of the forging experience to date has been with the Ta-10W alloy. Billets are heated to 1150 to 1205 °C (2100 to 2200 °F) in gas-fired furnaces using an oxidizing atmosphere. Breakdown forging below 980 °C (1800 °F) or continued working below 815 °C (1500 °F) can cause internal cracking. Forgeability of the tantalum alloys decreases sharply as tungsten content exceeds about 12.5%. Interstitial elements such as carbon, oxygen, and nitrogen also have a deleterious effect on forgeability.

Two types of coatings--glasses and aluminides--have been successfully used to protect tantalum from oxidation during forging. A 0.076 mm (3 mil) thick coating of aluminum has provided protection for the Ta-10W alloy when it was heated in air at 1370 °C (2500 °F) for 30 min. Glass coatings are generally preferred for their lubricating properties. Various borosilicate glasses are available that can be used for forging operations carried out in the range of 1095 to 1315 °C (2000 to 2400 °F).

Tungsten and Tungsten Alloys

Tungsten-base materials, like the other refractory alloy systems, can be classified into two broad groups: unalloyed tungsten, and solid-solution or dispersion-strengthened alloys. These classifications are convenient because they group the alloys in terms of metallurgical behavior and applicable consolidation methods. Solid-solution alloys and unalloyed tungsten can be produced by powder metallurgy or conventional melting techniques; dispersion-strengthened alloys can be produced only by powder metallurgy methods.

The forgeability of tungsten alloys, like that of molybdenum alloys, is dependent on the consolidation technique used. Billet density, grain size, and interstitial content all affect forgeability.

Metallurgical principles in the forging of tungsten are much the same as those for molybdenum. Tungsten is usually forged in the hot/cold-working temperature range, in which hardness and strength increase with increasing reductions. Both systems exhibit increasing forgeability with decreasing grain size.

Tungsten requires considerably higher forging pressures than molybdenum; therefore, in-process annealing is often necessary in order to reduce the load requirements for subsequent forging operations. Because the need for lateral support during forging is greater for tungsten than for molybdenum, the design of preliminary forging tools is more critical. This is especially true for pressed and sintered billets, which have some porosity and are less than theoretical density.

Tungsten oxide, which becomes molten and volatilizes at forging temperatures, serves as an effective lubricant in the forging of tungsten. Mixtures of graphite and molybdenum disulfide are also used. Sprayed on the dies, these films provide lubricity and facilitate removal of the part from the dies. Glass coatings are also used, but they can accumulate in the dies and interfere with complete die filling.

Forging of Aluminum Alloys

G.W. Kuhlman, Aluminum Company of America

Introduction

ALUMINUM ALLOYS can be forged into a variety of shapes and types of forgings with a broad range of final part forging design criteria based on the intended application. Aluminum alloy forgings, particularly closed-die forgings, are usually produced to more highly refined final forging configurations than hot-forged carbon and/or alloy steels, reflecting differences in the high-temperature oxidation behavior of aluminum alloys during forging, the forging engineering approaches used for aluminum, and the higher material costs associated with aluminum alloys in comparison to carbon steels. For a given aluminum alloy forging shape, the pressure requirements in forging vary widely, depending primarily on the chemical composition of the alloy being forged, the forging process being employed, the forging strain rate, the type of forging being manufactured, the lubrication conditions, and the forging and die temperature.

Figure 1 compares the flow stresses of some commonly forged aluminum alloys at 350 to 370 °C (660 to 700 °F) and at a strain rate of 4 to 10 s^{-1} to 1025 carbon steel forged at an identical strain rate but at a forging temperature typically employed for this steel. Flow stress represents the low limit of forging pressure requirements; however, actual forging pressures are usually higher because of the other forging process factors outlined above. For some low-to-intermediate strength aluminum alloys, such as 1100 and 6061, flow stresses are lower than those of carbon steel. For high-strength alloys, particularly 7*xxx* series alloys such as 7075, 7010, 7049, and 7050, flow stresses, and therefore forging pressures, are considerably higher than those of carbon steels. Finally, other aluminum alloys, such as 2219, have flow stresses quite similar to those of carbon steels. As a class of alloys, however, aluminum alloys are generally considered to be more difficult to forge than carbon steels and many alloy steels. The chemical compositions, characteristics, and typical mechanical properties of all wrought aluminum alloys referred to in this article are reviewed in the article "Properties of Wrought Aluminum and Aluminum Alloys" in *Properties and Selection: Nonferrous Alloys and Special-Purpose Materials*, Volume 2 of the *ASM Handbook.*

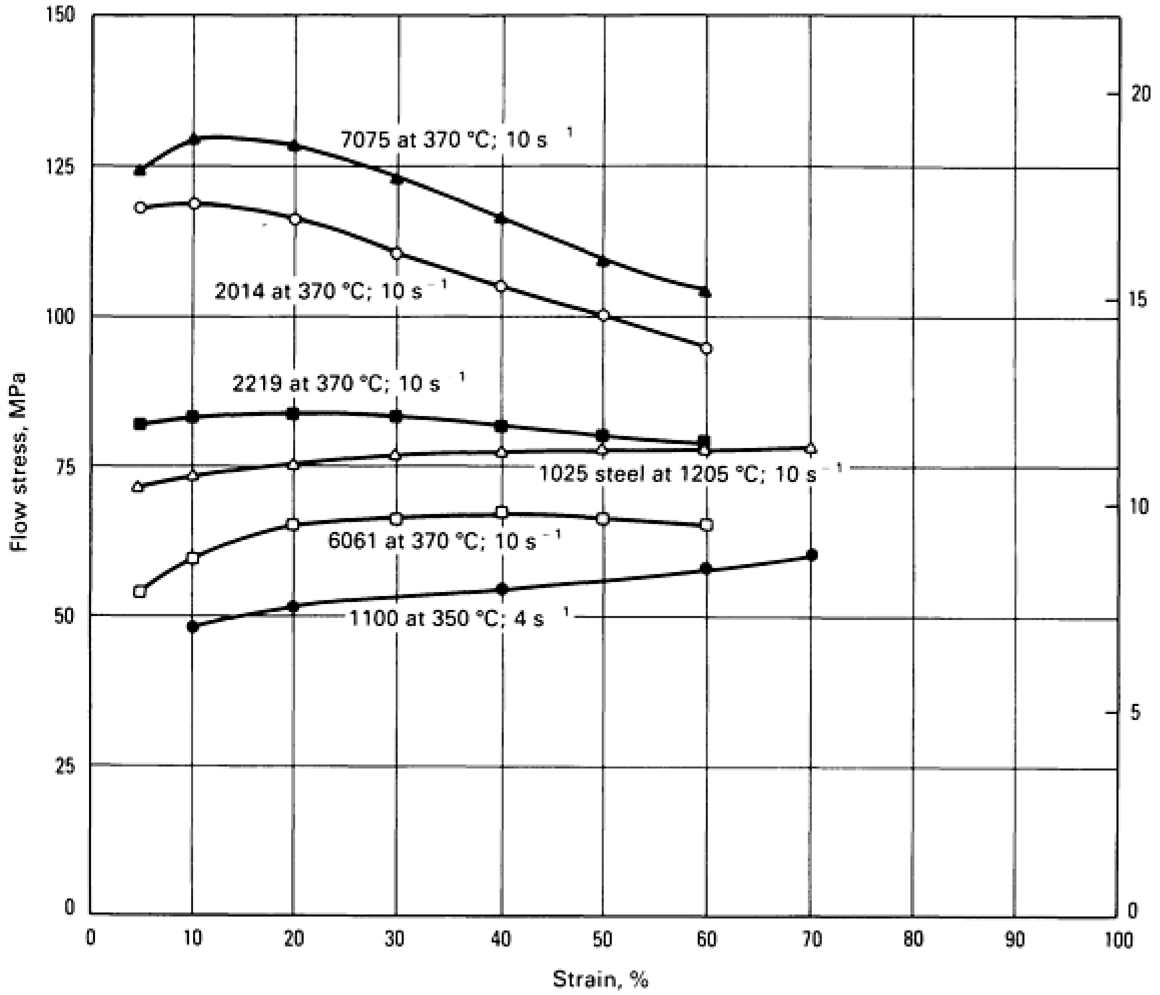


Fig. 1 Flow stresses of commonly forged aluminum alloys and of 1025 steel at typical forging temperatures and various levels of total strain.

Forging of Aluminum Alloys

G.W. Kuhlman, Aluminum Company of America

Forgeability

Compared to the nickel/cobalt-base alloys and titanium alloys, aluminum alloys are considerably more forgeable, particularly in conventional forging process technology, in which dies are heated to 540 °C (1000 °F) or less. Figure 2 illustrates the relative forgeability of ten aluminum alloys that constitute the bulk of aluminum alloy forging production. This arbitrary unit is principally based on the deformation per unit of energy absorbed in the range of forging temperatures typically employed for the alloys in question. Also considered in this index is the difficulty of achieving specific degrees of severity in deformation as well as the cracking tendency of the alloy under forging process conditions. There are wrought aluminum alloys, such as 1100 and 3003, whose forgeability would be rated significantly above those presented; however, these alloys have limited application in forging because they cannot be strengthened by heat treatment.

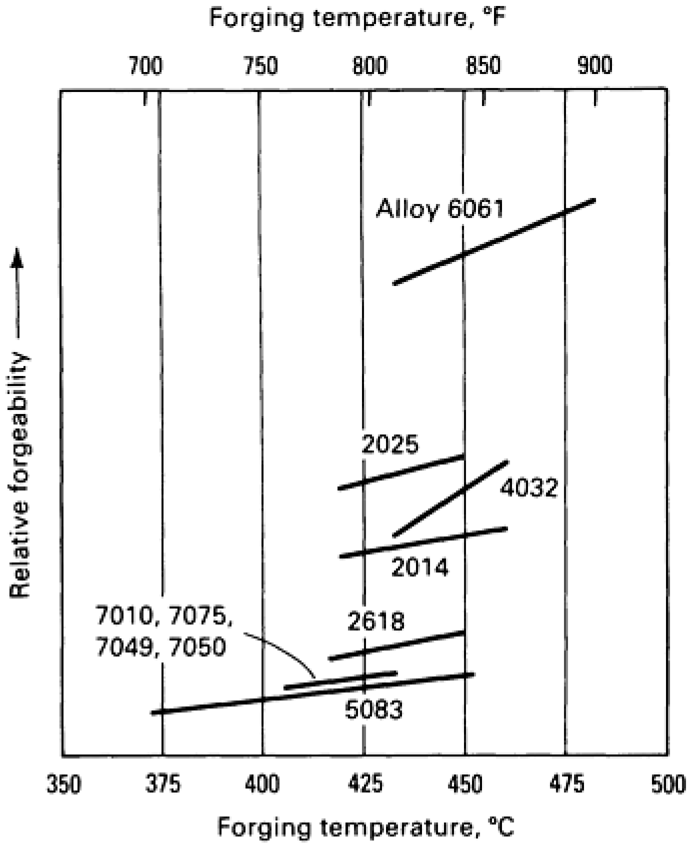


Fig. 2 Forgeability and forging temperatures of various aluminum alloys

Effect of Temperature. As shown in Fig. 2, the forgeability of all aluminum alloys improves with increasing metal temperature, and there is considerable variation in the effect of temperature for the alloys plotted. For example, the high-silicon alloy 4032 shows the greatest effect, while the high-strength Al-Zn-Mg-Cu 7*xxx* alloys display the least effect. Figure 3 shows the effect of temperature on flow stress at a strain rate of 10 s^{-1} for alloy 6061, a highly forgeable aluminum alloy. There is nearly a 50% increase in flow stress between the highest temperature (480 °C, or 900 °F, the top of the recommended forging range for 6061) and 370 °C (700 °F), which is below the minimum temperature recommended for 6061. For other, more difficult-to-forge alloys, such as the 2*xxx* and 7*xxx* series, the change in flow stress with temperature is even greater, indicating the principal reason for the relatively narrow metal temperature ranges.

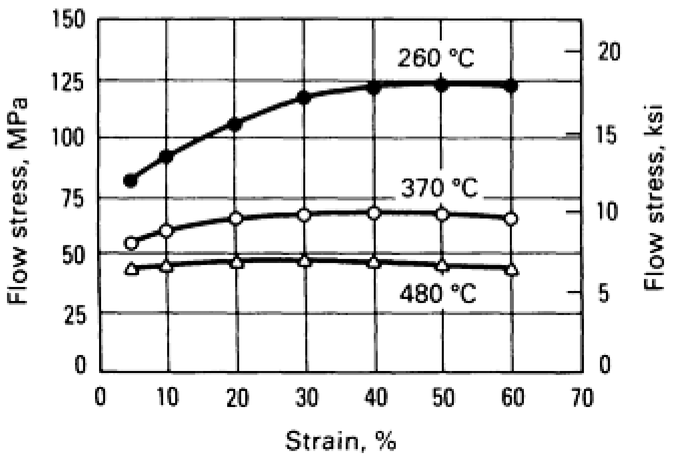


Fig. 3 Flow stress versus strain rate for alloy 6061 at three temperatures and a strain rate of 10 s^{-1}

The 15 aluminum alloys that are most commonly forged, as well as recommended temperature ranges, are listed in Table 1. All of these alloys are generally forged to the same severity, although some alloys may require more forging power and/or more forging operations than others. The forging temperature range for most alloys is relatively narrow (generally <55 °C, or 100 °F), and for no alloy is the range greater than 85 °C (155 °F). Obtaining and maintaining proper metal temperatures in the forging of aluminum alloys is critical to the success of the forging process. Die temperature and deformation rates play key roles in the actual forging temperature achieved.

Table 1 Recommended forging temperature ranges for aluminum alloys

Aluminum alloy	**Forging temperature range**	
	°C	**°F**
1100	315-405	600-760
2014	420-460	785-860
2025	420-450	785-840
2219	425-470	800-880
2618	410-455	770-850
3003	315-405	600-760
4032	415-460	780-860
5083	405-460	760-860
6061	430-480	810-900
7010	370-440	700-820
7039	380-440	720-820
7049	360-440	680-820
7050	360-440	680-820
7075	380-440	720-820

Effect of Deformation Rate. Aluminum alloy forgings are produced on a wide variety of forging equipment (see the section "Forging Equipment" in this article). The deformation or strain rate imparted to the deforming metal varies considerably, ranging from very fast (for example, $\geq$10 s^{-1} on equipment such as hammers, mechanical presses, and high-energy-rate machines) to relatively slow (for example, $\leq$0.1 s^{-1} on equipment such as hydraulic presses). Therefore, deformation or strain rate is also a critical element in the successful forging of given alloy.

Figure 4 presents the effect of two strain rates--10 s^{-1} and 0.1 s^{-1}--on the flow stresses of two aluminum alloys--6061 and 2014--at 370 °C (700 °F). It is clear that higher strain rates increase the flow stresses of aluminum alloys and that the increase in flow stress with increasing strain rate is greater for more difficult-to-forge alloys, such as the 2*xxx* and 7*xxx* series. For 6061, the more highly forgeable alloy, the increase in flow stress with the rapid strain rate is of the order of 70%; for 2014, the higher strain rate virtually doubles the flow stress. Although aluminum alloys are generally not considered to be as sensitive to strain rate as other materials, such as titanium and nickel/cobalt-base superalloys, selection of the strain rate in a given forging process or differences in deformation rates inherent in various types of equipment affect the forging pressure requirements, the severity of deformation possible, and therefore the sophistication of the forging part that can be produced.

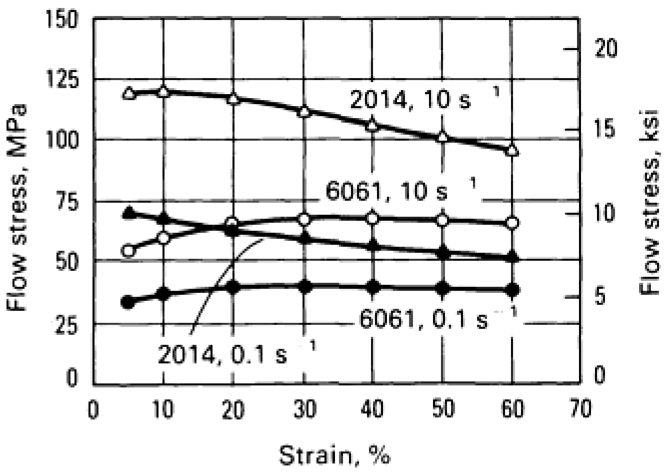


Fig. 4 Flow stress versus strain rate for alloys 2014 and 6061 at 370 °C (700 °F) and two different strain rates

Effect of Die Temperature. Unlike some forging processes for carbon and alloy steels, the dies used in virtually all hot-forging processes for aluminum alloys are heated in order to facilitate the forging process. Therefore, die temperature is another critical element in the forgeability and forging process optimization of this alloy class. Table 2 summarizes the die temperature ranges typically used for several aluminum forging processes. The criticality of die temperature in the optimization of the process depends on the forging equipment being employed, the alloy being forged, and the severity of the deformation or the sophistication of the forging design. For slower deformation processes, such as hydraulic press forging, die temperature frequently controls the actual metal temperature during deformation, and in fact, aluminum alloys forged in hydraulic presses are isothermally forged; that is, metal and dies are at the same temperature during deformation. Therefore, the die temperatures employed for hydraulic press forging exceed those typical of more rapid deformation processes, such as hammers and mechanical presses. Die heating techniques are discussed in the section "Heating of Dies" in this article.

Table 2 Die temperature ranges for the forging of aluminum alloys

Forging process/equipment	Die temperature	
	°C	°F
Open-die forging		

Ring rolling	95-205	**200-400**
Mandrel forging	95-205	**200-400**
Closed-die forging		
Hammers	95-150	**200-300**
Upsetters	150-260	**300-500**
Mechanical presses	150-260	**300-500**
Screw presses	150-260	**300-500**
Orbital (rotary) forging	150-260	**300-500**
Spin forging	150-315	**200-600**
Roll forging	95-205	**200-400**
Hydraulic presses	**315-430**	**600-800**

Forging of Aluminum Alloys

G.W. Kuhlman, Aluminum Company of America

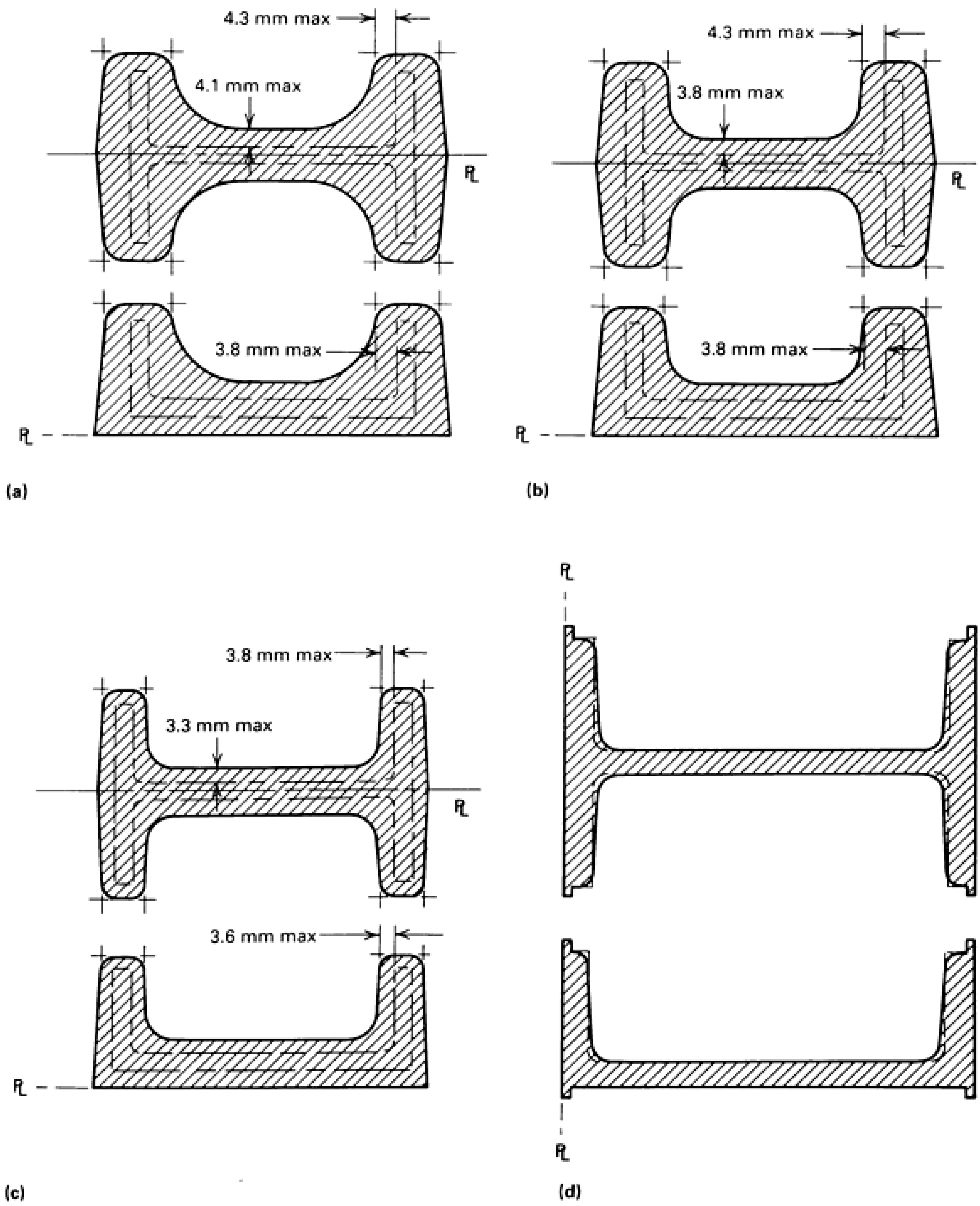
Forging Methods

Aluminum alloys are produced by all of the current forging methods available, including open-die (or hand) forging, closed-die forging, upsetting, roll forging, orbital (rotary) forging, spin forging, mandrel forging, ring rolling, and extrusion. Selection of the optimal forging method for a given forging shape is based on the desired forged shape, the sophistication of the forged-shape design, and cost. In many cases, two or more forging methods are combined in order to achieve the desired forging shape and to obtain a thoroughly wrought structure. For example, open-die forging frequently precedes closed-die forging in order to prework the alloy (especially when cast ingot forging stock is being employed) and in order to preshape (or preform) the metal to conform to the subsequent closed dies and to conserve input metal.

Open-die forging is frequently used to produce small quantities of aluminum alloy forgings when the construction of expensive closed dies is not justified or when such quantities are needed during the prototype fabrication stages of a forging application. The quantity that warrants the use of closed dies varies considerably, depending on the size and shape of the forging and on the application for the part. However, open-die forging is by no means confined to small or prototype quantities, and in some cases, it may be the most cost-effective method of aluminum forging manufacture. For example, as many as 2000 pieces of biscuit forgings have been produced in open dies when it was desired to obtain the properties of a forging but closed dies did not provide sufficient economic benefits.

Open-die forgings in aluminum alloys can be produced to a wide variety of shapes, ranging from simple rounds, squares, or rectangles to very complex contoured forgings (see the article "Open-Die Forging" in this Volume). In the past, the complexity and tolerances of the open-die forging of aluminum and other materials depended on the skill of the press-operator; however, with the advent of programmable computer-controlled open-die forging presses, it is possible to produce such shapes to overall thickness/width tolerances bands of 1.27 mm (0.050 in.). Because the open-die forging of aluminum alloys is also frequently implemented to produce preforms for closed-die forgings, these state-of-the-art forging machines also provide very precise preform shapes, improving the dimensional consistency and tolerances of the resulting closed-die forging and reducing closed-die forging cost through further input material conservation. More information on open-die forging is available in the article "Open-Die Forging" in this Volume.

Closed-Die Forging. Most aluminum alloy forgings are produced in closed dies. The four types of aluminum forgings shaped in closed dies are blocker-type (finish forging only), conventional (block and finish forging or finish forging only), high-definition (near-net shape), and precision (no draft, net shape). These closed-die forging types are illustrated in Fig. 5.



Characteristic	Tolerance, mm (in.)			
	Blocker-type	**Conventional**	**High-definition**	Precision
Die closure	+2.3, -1.5 (+0.09, -0.06)	+1.5, -0.8 (+0.06, -0.03)	+1.25, -0.5 (+0.05, -0.02)	**+0.8, -0.25 (+0.03, -0.01)**
Mismatch	0.5 (0.02)	0.5 (0.02)	0.25 (0.01)	**0.38 (0.015)**
Straightness	0.8 (0.03)	0.8 (0.03)	0.5 (0.02)	**0.4 (0.016)**
Flash extension	3 (0.12)	1.5 (0.06)	0.8 (0.03)	**0.8 (0.03)**
Length and width	±0.8 (±0.03)	±0.8 (±0.03)	±0.8 (±0.03)	**+0.5, -0.25 (+0.02, -0.01)**

Fig. 5 Types of aluminum closed-die forgings and tolerances for each. (a) Blocker-type. (b) Conventional. (c) High-definition. (d) Precision

Blocker-type forgings (Fig. 5a) are produced in relatively inexpensive, single sets of dies. In dimensions and forged details, they are less refined and require more machining than conventional or high-definition closed-die forgings. A blocker-type forging costs less than a comparable conventional or high-definition forging, but it requires more machining.

Conventional closed-die forgings (Fig. 5b) are the most common type of aluminum forging. They are produced with either a single set of finish dies or with block and finish dies, depending on the design criteria. These forgings have less machine stock and tighter tolerances than blocker-type forgings, but require additional cost (both for the dies and for fabrication) to produce.

High-Definition Forgings. With the advent of improved forging equipment and process control, as discussed below, high-definition near-net shape closed-die forgings (Fig. 5c) can be produced and offer forging design and tolerance enhancement over conventional or blocker-type forgings to effect further reduction in machining costs. High-definition forgings are produced with multiple die sets, consisting of one or more blocker dies and finish dies, and are frequently used with some as-forged surfaces remaining unmachined by the purchaser.

Precision forgings (Fig. 5d) represent the most sophisticated aluminum forging design produced. These forgings, for which the forger may combine forging and machining processes in the fabrication sequence, cost more than other aluminum forging types, but by definition require no subsequent machining by the purchaser and therefore may be very cost effective. Net shape aluminum forgings are produced in two-piece, three-piece through-die, and/or multiple-segment wrap-die systems to very restricted design and tolerances necessary for assembly. Net shape aluminum forgings are discussed more thoroughly in the section "Aluminum Alloy Precision Forgings" in this article and in the article "Precision Forging" in this Volume; more information on the closed-die forging process is available in the article "Closed-Die Forging in Hammers and Presses" in this Volume.

Upset forging can be accomplished in specialized forging equipment called upsetters (a form of mechanical press) or high-speed multiple-station formers and is frequently used to produce forging shapes that are characterized by surfaces of revolution, such as bolts, valves, gears, bearings, and pistons. Upset forging may be the sole process used for the shape, such as pistons, or it can be used as a preliminary operation to reduce the number of impressions, to reduce die wear, or to save metal when the products are finished in closed dies. Wheel and gear forgings are typical products for which upsetting is advantageously used in conjunction with closed-die forging. As a rule, in the upset forging of aluminum alloys, the unsupported length of forgings must not exceed three diameters for a round shape or three times the diagonal of the cross section for a rectangular shape. The article "Hot Upset Forging" in this Volume contains more information on upsetting.

Roll forging can be used as a preliminary preform operation to reduce metal input or to reduce the number of closed-die operations. In roll forging, the metal is formed between moving rolls, either or both containing a die cavity, and is most often used for parts, such as connecting rods, where volume is high and relatively restricted cross-sectional variations typify the part. Roll forging is discussed at length in the article "Roll Forging" in this Volume.

Orbital (rotary) forging is a variant of closed-die mechanical or hydraulic press forging in which one or both of the dies is caused to rotate, usually at an angle, leading to the incremental deformation of the workpiece. Orbital forging is used to produce parts with surfaces of revolution with both hot and cold aluminum alloy forging processes, and it provides highly refined close-tolerance final shapes. Additional information on orbital forging is available in the article "Rotary Forging" in this Volume.

Spin forging, a relatively new aluminum alloy forging technique, combines closed-die forging and computer numerically controlled (CNC) spin forgers to achieve close-tolerance axisymmetric hollow shapes such as those illustrated in Fig. 6 through the use of either hot- or cold-forging techniques. Because spin forging is accomplished over a mandrel, inside diameter contours are typically produced to net shape, requiring no subsequent machining. Outside diameter contours can be produced net or with very little subsequent machining and to much tighter out-of-round and concentricity tolerances than competing forging techniques, such as forward or reverse extrusion (see below), resulting in material savings. Parts with both ends open, one end closed, or both ends closed can also be produced.

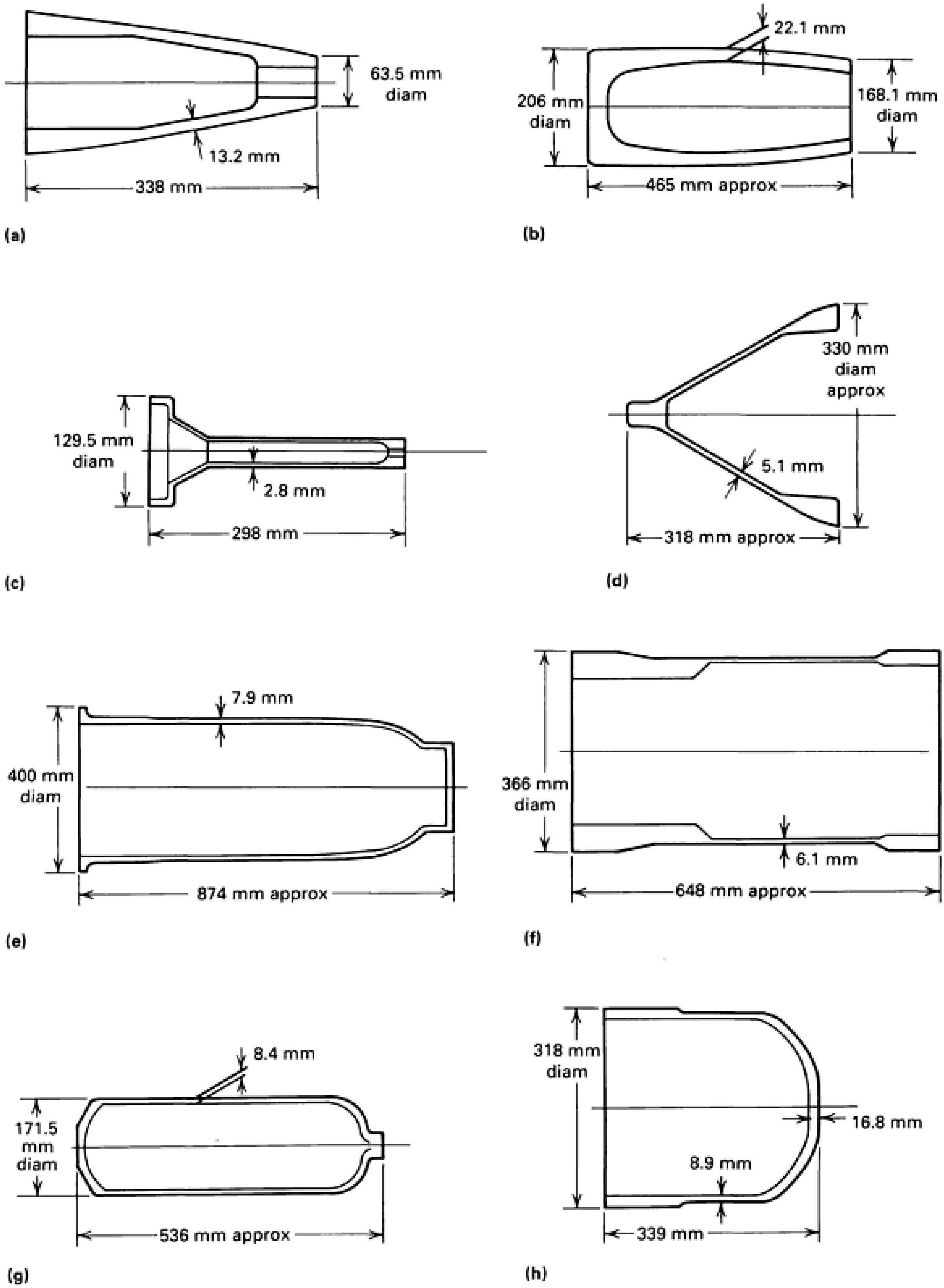


Fig. 6 Examples of spin-forged aluminum alloy shapes. (a) Ordnance ogive. (b) Ordnance center section. (c) Ordnance fuse. (d) Jet engine spinner. (e) Missile nose cone. (f) Missile center section. (g) Bottle. (h) Missile forward case.

Ring rolling is also used for aluminum alloys to produce annular shapes. The procedure used to ring roll aluminum alloys is essentially the same as that used for steel (see the article "Ring Rolling" in this Volume). Both rectangular and contoured cross section rolled rings, with or without subsequent machining by the forger, are produced in many aluminum alloys. The temperatures employed for the ring rolling of aluminum alloys are quite similar to those for other forging processes, although care must be taken to maintain metal temperature. The deformation achieved in the ring rolling of aluminum typically results in the predominant grain flow in the tangential or circumferential orientation. If predominant grain flow is desired in other directions, such as axial or radial, other ring-making processes, such as hollow-biscuit open-die forgings, mandrel forging, or reverse/forward extrusion, can be employed. The economy of ring rolling in aluminum alloys depends on the volume, size, and contour of the forging. For some ring parts, it may be more economical to produce the shape by mandrel forging or to cut rings from hollow extruded cylinders. Both techniques are discussed below.

Mandrel forging is used in aluminum alloys to produce axisymmetric, relatively simple, hollow ring or cylindrical shapes, in which the metal is incrementally forged, usually on a hammer or hydraulic press, over a mandrel. In the incremental forging process, the wall thickness of the preform is reduced, and this deformation enlarges the diameter of the piece. The mandrel forging of aluminum has been found to be economical for relatively low-volume part fabrication and/or in the fabrication of very large ring shapes (up to 3.3 m, or 130 in., in diameter). With control of the working history of the input material and the mandrel-forging process, mandrel-forged rings can be produced with either circumferential or axial-predominant grain orientations.

Reverse or forward extrusion, a variant of closed-die forging for aluminum, can be used to produce hollow, axisymmetric shapes in aluminum alloys with both ends open or with one end closed. The terminology of reverse or forward extrusion refers to the direction of metal movement in relation to the movement of the press head. In forward extrusion, the metal is extruded (typically downward) in the same direction as the press head. Conversely, for reverse extrusion, metal moves opposite the motion of the cross-head. Selection of forward versus reverse extrusion is usually based on part geometry and the opening restrictions of the press. Some presses are specifically equipped with openings in the upper cross-head to accommodate the fabrication of very long reverse extrusions, either solid or hollow. Extrusion as a metal deformation process also frequently plays an important role in the closed-die forging of aluminum alloy parts other than hollow shapes (such as wheels). More information on extrusion is available in the articles "Cold Extrusion" and "Conventional Hot Extrusion" in this Volume.

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Forging Equipment

Aluminum alloy forgings are produced on the full spectrum of forging equipment, ranging from hammers and presses to specialized forging machines. Selection of forging equipment for a given forging shape and type is based on the capabilities of the equipment, forging design sophistication, desired forging process, and cost. Additional information on the types of equipment used in the manufacture of forgings is available in the Section "Forging Equipment and Dies" in this Volume.

Hammers. Gravity- and power-drop hammers are used for both the open-die and closed-die forging of aluminum alloys because of the relatively low fabrication costs associated with such equipment, although the power requirements for aluminum frequently exceed those for steel. Hammers deform the metal with high deformation speeds; therefore, control of the length of the stroke and of the force and speed of the blows is particularly useful in forging aluminum alloys, because of their sensitivity to strain rate and their exothermic nature under rapid deformation processes. Power-drop hammers are used to manufacture closed-die forgings if an applied draft of about 5 to 7° can be tolerated. Hammers are frequently used as a preliminary operation for subsequent closed-die forging by other forging processes, and for some products, such as forged aluminum propellers, power-assisted hammers are the optimal forging process equipment because of their capacity for conserving input material and their ability to produce a finished blade that has essentially net airfoil contours.

Mechanical and Screw Presses. Both mechanical and screw presses are extensively used for the closed-die forging of aluminum alloys. They are best adapted to aluminum forgings of moderate size, high volume (cost consideration), and relatively modest shape that do not require extensive open-die preforming. In forging aluminum alloys on mechanical or screw presses, multiple-die cavities, frequently within the same die block, and multiple forging stages, frequently without reheating, are used to enhance the deformation process, to increase the part design sophistication, and to improve tolerance control. The automotive rear knuckle suspension component shown in Fig. 7 illustrates the complexity of the high-volume aluminum alloy forging producible on a mechanical press.

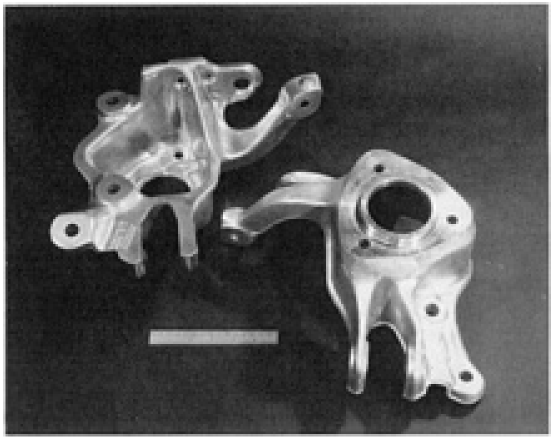

Fig. 7 Complex aluminum alloy automotive suspension components forged on a mechanical press.

Mechanical and screw presses combine impact with a squeezing action that is more compatible with the flow characteristics of aluminum alloys than hammers. Screw presses differ from mechanical presses in that the former have a degree of strain rate control that can be exploited to enhance the deformation control of aluminum alloys. State-of-the-art mechanical and screw presses have press load and operation monitoring and press control systems. These systems, combined with automated handling and supporting equipment, such as reheat furnaces and trim presses, can be used to achieve full forging process automation and highly repeatable and precise forging conditions in order to enhance the uniformity of the resulting aluminum alloy forgings. Typically, the minimum applied draft for mechanical or screw press forged aluminum alloys is 3°; however, both press types have been used to manufacture precision, net shape aluminum alloy forgings with draft angles of 1°. Screw presses are particularly well suited to the manufacture of the highly twisted, close-tolerance aluminum blades used in turbine engines.

Hydraulic Presses. Although the fastest hydraulic presses are slower acting than mechanical or screw presses, hydraulic presses are frequently best suited to producing either very large aluminum closed-die forgings (Fig. 8) or very intricate aluminum alloy forgings. The deformation achieved in a hydraulic press is more controlled than that typical of mechanical and screw presses or hammers. Therefore, hydraulic presses are particularly well adapted to the fabrication of conventional, high-definition, and precision no-draft, net shape aluminum alloy forgings in which slow or controlled strain rates minimize the resistance of the aluminum alloy to deformation, reduce pressure requirements, and facilitate achieving the desired shape.

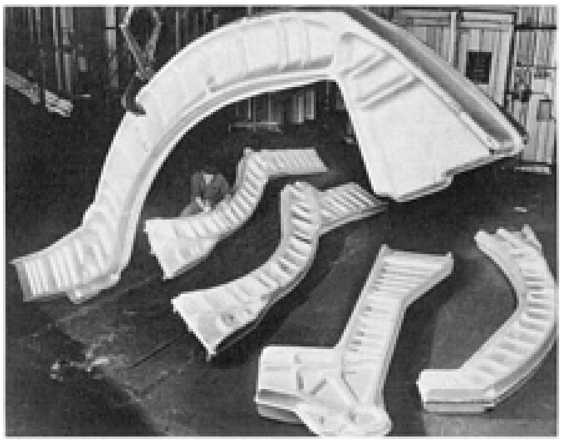

Fig. 8 Examples of very large blocker-type aluminum alloy airframe forgings.

State-of-the-art hydraulic presses, including very large machines of up to 445 MN (50,000 tonf), include speed and pressure controls and programmable modes of operation. With organization into press cells, automatic handling and lubrication, die heating and supporting equipment, such presses provide a high degree of forging process automation and forging process control to achieve process optimization and improved product uniformity. The minimum applied draft angle for high-definition hydraulic press forged aluminum alloys is 3°; for hydraulic press forged precision, net shape aluminum forgings, the minimum draft angle is 0 to 0.5° on outside contours and 0.5 to 1° on inside contours.

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Die Materials, Design, and Manufacture

For the closed-die forging of aluminum alloys, die materials selection, die design, and manufacturing are critical elements in the overall aluminum forging process, because the dies are a major element of the final cost of such forgings. Further, forging process parameters are affected by die design, and the dimensional integrity of the finished forging is in large part controlled by the die cavity. Therefore, the forging of aluminum alloys requires the use of dies specifically designed for aluminum for the following reasons:

- The deformation behavior of aluminum alloys differs from that of other materials; therefore, the intermediate and final cavity die design must optimize metal flow under given forging process conditions and provide for the fabrication of defect-free final parts
- Allowances for shrinkage in aluminum alloys are typically greater than those for steels and other materials
- Temperature control of the dies used to forge aluminum alloys is critical; therefore, the methods used for heating and maintaining die temperatures during forging must be considered in the design

Die Materials. The die materials used in the closed-die forging of aluminum alloys are identical to those employed in forging steels except that, because of the forces applied in aluminum alloy forging and the sophistication of the parts produced, such materials are typically used at lower hardness levels in order to improve their toughness. Available die materials were primarily designed for the forging of steels and are not necessarily optimized for the demands of aluminum alloy forgings. However, with advanced steelmaking technology, such as argon oxygen decarburization refining, vacuum degassing, and ladle metallurgy, the transverse ductility and fracture toughness of available standard and proprietary die steel grades have been improved dramatically. As a result, the performance of these grades in the forging of aluminum alloys has also improved dramatically.

Although die wear is less significant with aluminum alloy forgings than with steel and other high-temperature materials, high-volume aluminum alloy forgings can present die wear problems in cases in which die blocks have reduced hardness in order to provide improved toughness. Therefore, higher-hardness die inserts and/or surface treatments are often used to improve wear characteristics in order to maintain die cavity integrity. The surface treatments employed include carburizing, nitriding, carbonitriding, and surface alloying using a variety of state-of-the-art techniques.

Beyond die wear, the most common cause of die failure in aluminum forging dies is associated with die checking or die cracking, which, if left unheeded, can lead to eventual catastrophic loss of the die. Such die checking usually occurs at stress raisers inherent in the die cavity. Improved-toughness die steels, improved die-sinking techniques (see below), improved die design (see below), and lower-hardness die blocks serve to reduce the incidence of die checking in the forging of aluminum alloys. Further, forging dies for aluminum alloys are routinely repair welded using metal inert gas, tungsten inert gas, or other welding techniques.

For hot upsetting, both gripper dies and heading tools are usually made of 6G and 6F2 at a hardness of 42 to 46 HRC. Grades 6G or 6F2, or their proprietary variants, are the most widely used die materials in all closed-die forging processes for aluminum. If the quantities to be forged are large enough to justify the added cost or if the forging process and the part are particularly demanding, hot-work tool steels such as H11, H12, H13, or their proprietary variants are employed, usually at 44 to 50 HRC.

Die Design. A key element in the cost control of dies for aluminum forging and in the successful fabrication of aluminum alloy forgings is die design and die system engineering. Closed dies for aluminum forgings are manufactured either as stand-alone die blocks or as inserts into die holder systems, usually to reduce the overall cost of the dies for a given forging. Die holder systems may be universal, covering a wide range of potential die sizes, or may be constructed to handle families or parts of similar overall geometries. The design of aluminum forging dies is highly intensive in engineering skills and is based upon extensive empirical knowledge and experience. A complete compendium of aluminum forging design principles and practices is available in Ref 1.

Because aluminum alloy forging design is engineering intensive, the advent of computer-aided design (CAD) hardware and software has had an extensive impact on aluminum alloy die design. A detailed discussion of CAD technology is available in the Section "Computer-Aided Process Design for Bulk Forming" in this Volume. Computer-aided design techniques for aluminum forging parts and dies are fully institutionalized within the forging industry such that many aluminum alloy forgings, particularly high-definition and precision forgings, are designed with this technique. The CAD databases created are then used, as discussed below, with computer-aided manufacturing (CAM) to produce dies, to direct the forging process, and to assist in final part verification and quality control. Both public domain and proprietary CAD design software packages are used to design the finished forging from the machined part, including the dies, and to design the critical blocker and preform shapes needed to produce the finish shape, including the dies.

Beyond computer-aided design, heuristic techniques such as artificial intelligence are being used to commit the extensive aluminum forging design knowledge and experience into expert systems in order to enhance the design process. Complementing the expert systems is current research and development for aluminum alloys into powerful finite-element deformation-modeling techniques that, when fully developed, will further aid the designer in his task and will permit evaluation, verification, and optimization of forging part and die design on a computer before committing the design to any costly die sinking or part fabrication.

Die Manufacture. Aluminum alloy forging dies are produced by a number of machining techniques, including hand sinking, copy milling from a model, electrodischarge machining (EDM), and CNC direct sinking. With the availability of CAD data bases, CAM-driven CNC direct die sinking and EDM die sinking are at the leading edge of the state-of-the-art in aluminum alloy die sinking. These techniques serve to reduce the cost of the dies and, perhaps more important, to increase the accuracy of the dies by as much as 50% compared to the other techniques. For example, standard die-sinking tolerances are ±0.1 mm (±0.005 in.), but with CAM-driven CNC/EDM sinking, tolerances are reduced to ±0.07 mm (±0.003 in.) on complex dies. The finish on dies used for the forging of aluminum alloys is more critical than that on dies used for steel. Therefore, cavities are highly polished, frequently with automated equipment, by a variety of techniques in order to obtain an acceptable finish and to remove the disturbed surface layer resulting from such die-sinking techniques as electrodischarge machining.

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Processing of Aluminum Alloy Forgings

The common elements in the manufacture of any aluminum alloy forging include preparation of the forging stock, preheating stock, die heating, lubrication, the forging process, trimming, forming and repair, cleaning, heat treatment, and inspection. The critical aspects of each of these elements are reviewed below.

Preparation of Forging Stock. Aluminum alloy forgings are typically produced from cast or wrought stock; forged or rolled bar, extruded bar, or plate are the primary examples. Selection of forging stock type for a given forging shape is based on the required forging processes, forging shape, mechanical property requirements, and cost. Sawing and shearing are the two methods most frequently used to cut aluminum alloy forging stock into lengths for forging. Abrasive cutoff can be used, but it is slower than sawing.

Sawing with a circular or band saw having carbide-tipped blades is the fastest and generally the most satisfactory method. Sawing, however, produces sharp edges or burrs that may initiate defects when the stock is forged in closed dies.

Burrs and sharp edges are typically removed by a radiusing machine. State-of-the-art saws for cutting aluminum alloys are highly automated and frequently have automatic radiusing capability and control systems that permit very precise control of either stock length or stock volume and therefore stock weight.

Shearing is used less for aluminum than for steel, because aluminum alloy billets are softer and more likely to be mutilated in shearing and because the sheared ends may have unsatisfactory surfaces for forging without being conditioned. Shearing is successfully used for high-volume aluminum forgings made from wrought bar stock generally less than 50 mm (2 in.) in diameter. More information on the cutting of metal stock is available in the Section "Shearing, Slitting, and Cutting" in this Volume.

Preheating for Forging. As noted in the section "Effect of Temperature" in this article, metal temperature is a critical element in the aluminum forging process. Aluminum alloys form a very tenacious oxide coating upon heating. The formation of this coating is self-limiting; therefore, aluminum alloys do not scale to the same extent as steel does. However, most aluminum alloys are susceptible to hydrogen pickup during reheating operations such that reheating equipment and practices are also critical elements of forging process control.

Heating Equipment. Aluminum alloys are heated for forging with a wide variety of heating equipment, including electric furnaces, fully muffled or semimuffled gas furnaces, oil furnaces, induction heating units, fluidized-bed furnaces, and resistance heating units. Gas-fired semimuffled furnaces, either batch or continuous, are probably the most widely used. Heating equipment design and capabilities necessarily vary with the requirements of a given forging process. Both oil and natural gas furnaces must use low-sulfur fuel. Excessive hydrogen pickup in forged aluminum alloys manifests itself in two ways. The first is high-temperature oxidation, which is usually indicated by blisters on the surface of the forging. The second is bright flakes, or unhealed porosity, which is usually found during the high-resolution ultrasonic inspection of final forgings. Both types of hydrogen pickup are influenced by preheating furnace practices and/or furnace equipment in which water vapor as a product of combustion is the primary source of hydrogen. Fully muffled gas-fired furnaces or low relative humidity electric furnaces provide the least hydrogen pickup. Techniques are available for modifying the surface chemistry of aluminum alloys to reduce hydrogen pickup in heating equipment that has higher levels of relative humidity than desired. Protective-atmosphere furnaces are seldom used to preheat aluminum alloy forgings.

Induction heating, resistance heating, and fluidized-bed heating are frequently used in the forging of aluminum alloys in cases in which forging processes are highly automated. State-of-the-art gas-fired furnaces can also be linked with specially designed handling systems to provide full automation of the forging process.

Temperature Control. As noted in Fig. 1, 2, and 3 and in Table 1, aluminum alloys have a relatively narrow temperature range for forging. Therefore, careful control of the temperature in preheating is important. The heating equipment should have pyrometric controls that can maintain ± 5 °C (±10 °F). Continuous furnaces used to preheat aluminum typically have three zones: preheat, high heat, and discharge. Most furnaces are equipped with recording/controlling instruments and are frequently surveyed for temperature uniformity in a manner similar to that used for solution treatment and aging furnaces.

Heated aluminum alloy billets are usually temperature checked by using either contact or noncontact pyrometry based on dual-wavelength infrared systems. This latter technology, although sensitive to emissivity, has been successfully incorporated into the fully automated temperature verification systems used in automated high-volume aluminum forging processes to provide significantly enhanced temperature control and process repeatability. In the open-die forging of aluminum alloys, it is generally desirable to have billets near the high side of the forging temperature range when forging begins and to finish the forging as quickly as possible before the temperature drops excessively. Open-die forging and multiple-die closed-die forging of aluminum alloys are frequently conducted without reheating as long as critical metal temperatures can be maintained.

Heating time for aluminum alloys varies, depending on the section thickness of the stock and the furnace capabilities. However, in general, because of the increased thermal conductivity of aluminum alloys, the required preheating times are shorter than with other forged materials. Recording pyrometric instruments on furnaces can be used to provide an indication of when the metal has reached the desired forging temperature. Generally, 10 to 20 min per inch of section thickness is sufficient to ensure that the aluminum alloys have reached the desired temperature.

Time at temperature is not as critical for aluminum alloys as for some other forged materials; however, long soaking times offer no particular advantage, except for high-magnesium alloys such as 5083, and may in fact be detrimental in terms of hydrogen pickup. Generally, soaking times of 1 to 2 h are sufficient; if unavoidable delays are encountered such that soaking time may exceed 4 to 6 h, removal of the metal from the furnace is generally recommended.

Heating of Dies. As noted in the section "Effect of Die Temperature" in this article, die temperature is the second critical element in the aluminum forging process. Dies are always heated for the forging of aluminum alloys, with die temperature for closed-die forging being more critical. As noted in Table 2, the die temperature used for the closed-die forging of aluminum alloys varies with the type of forging equipment being employed and the alloy being forged. Both remote and on-press die heating systems are employed in the forging of aluminum alloys. Remote die heating systems are usually gas-fired die heaters capable of slowly heating the die blocks. These systems are used to preheat dies to the desired temperature prior to assembly into the forging equipment.

On-press die heating systems range from relatively rudimentary systems to highly engineered systems designed to maintain very tight die temperature tolerances. On-press die heating systems include gas-fired equipment, induction heating equipment, and/or resistance heating equipment. In addition, presses used for the precision forging of aluminum alloys frequently have bolsters that can be heated or cooled as necessary. State-of-the-art on-press aluminum die heating equipment can hold die temperature tolerances within ±15 °C (±25 °F) or better. Specific on-press die heating systems vary with the forging equipment used, the size of the dies, the forging process, and the type of forging produced.

Lubrication. Die lubrication is the third critical element in the aluminum forging process and is the subject of major engineering and developmental emphasis, both in terms of the lubricants themselves and the lubricant application systems.

The lubricants used in aluminum alloy forging are subject to severe service demands. They must be capable of modifying the surface of the die to achieve the desired reduction in friction, withstand the high die and metal temperatures and pressures employed, and yet leave the forging surfaces and forging geometry unaffected. Lubricant formulations are typically highly proprietary and are developed either by the lubricant manufacturers or by the forgers themselves. Lubricant composition varies with the demands of the forging process used and the forging type. The major active element in aluminum alloy forging lubricants is graphite; however, other organic and inorganic compounds are added to colloidal suspensions in order to achieve the desired results. Carriers for aluminum alloy forging lubricants vary from mineral spirits to mineral oils to water.

Lubricant application is typically achieved by spraying the lubricant onto the dies while the latter are assembled in the press; however, in some cases, lubricants are applied to forging stock prior to reheating or just prior to forging. Several pressurized-air or airless spraying systems are employed, and with high-volume highly automated aluminum forging processes, lubricant application is also automated by single- or multiple-axis robots. Lubricant can be applied with or without heating. State-of-the-art lubricant application systems have the capability of applying very precise patterns or amounts of lubricant under fully automated conditions such that the forging processes are optimized and repeatable.

Forging Process. The critical elements of the aluminum forging process, specifically strain rate, deformation mode, and type of forging process have been reviewed above, including state-of-the-art capabilities that have served to enhance control of the forging process and therefore the product it produces. In addition to the enhanced forging equipment employed in the manufacture of aluminum forging, mention was made of the organization of presses and supporting equipment into cells operating as systems; such systems are then integrated with advanced manufacturing and computer-aided manufacturing concepts. Aluminum alloy forging is thus entering an era properly termed integrated manufacturing, in which all aspects of the aluminum forging process from design to execution are heavily influenced by computer technology.

Trimming, forming, and repair of aluminum alloy forgings are intermediate processes that are necessary to achieve the desired finish shape and to control costs.

Trimming. The flash generated in most closed-die aluminum forging processes is removed by hot or cold trimming or sawing, punching, or machining, depending on the size, shape, and volume of the part being produced. Hot- or cold-trimming tools are ordinarily used to trim large quantities, especially on moderately sized forgings that are intricate and may contain several punchouts. The choice of hot or cold trimming is largely based on the complexity of the part and on cost. The trim presses employed are either mechanical or hydraulic. Trimming dies are usually constructed of 6G or 6F2 die block steel at a hardness of about 444 to 477 HB. Tools of these steels are less costly because they are often produced from pieces of worn or broken forging dies. Blades for trimming and the edges of trimming dies are frequently hardfaced to improve their abrasion resistance. In addition to these grades, O1 tool steel and/or high-alloy tool steel such as D2 hardened to 58 to 60 HRC have also been used to trim aluminum alloy forgings and may offer longer service lives. The hot trimming of aluminum alloys is usually accomplished immediately after forging without reheating.

Forming. Some aluminum alloy forging shapes combine hot forging with hot, warm, or cold forming to achieve the shape. An example is the forged and formed aluminum truck wheel shown in Fig. 9. Forming is accomplished on mechanical or hydraulic presses or on specialized forming equipment that is frequently integrated as a part of a forging cell with the forging press.

Fig. 9 Forged and formed aluminum alloy 6061-T6 truck wheels.

Repair. This is an intermediate operation that is conducted between forging stages in aluminum alloys. It is frequently necessary to repair the forgings to remove surface discontinuities created by the prior forging practice so that such discontinuities do not affect the integrity of the final forging product. The need for repair is usually a function of part complexity and the extent of the tooling manufactured to produce the part. There is typically a cost trade-off between increased tooling (or number of die sets) and requirements for intermediate repair that is unique to each forging configuration. Intermediate repair of aluminum alloys is usually accomplished by hand milling, grinding, machining, and/or chipping techniques.

Cleaning. Aluminum alloy forgings are usually cleaned as soon as possible after being forged. The following treatment is a standard cleaning process that removes lubricant residue and leaves a good surface with a natural aluminum color:

- Etch in a 4 to 8% (by weight) aqueous solution of caustic soda at 70 °C (160 °F) for 0.5 of 5 min
- Rinse immediately in hot water at 75 °C (170 °F) or higher for 0.5 to 5 min
- Desmut by immersion in a 10% (by volume) aqueous solution of nitric acid at 88 °C (190 °F) minimum
- Rinse in hot water

The immersion time in the first two steps varies, depending on the amount of soil to be removed and the forging configuration. The frequency of cleaning also depends on the forging configuration and the process used to produce it. Some forgings are not cleaned until just before final inspection. Additional information on the cleaning of aluminum alloys is available in the article "Surface Engineering of Aluminum and Aluminum Alloys" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Heat Treatment. All aluminum alloy forgings, except 1*xxx,* 3*xxx,* and 5*xxx* series alloys, are heat treated with solution treatment, quench, and artificial aging processes in order to achieve final mechanical properties. The furnaces used to heat treat and age aluminum alloy forgings are either continuous or batch type, fully muffled gas-fired, electric, fluidized-bed, or other specially designed equipment. Because of the shape complexity of aluminum forgings, quench-racking procedures are particularly critical in obtaining the uniform and satisfactory quench necessary to achieve the required mechanical properties and to minimize distortion. Therefore, in addition to control of solution treatment and age temperature and time, racking techniques for forgings are also the subject of necessary heat treatment control processes.

Furthermore, quenching techniques for aluminum alloy forgings are also critical because of their configuration and frequently widely variant cross-sectional thicknesses within the same forging. Depending on the specific aluminum alloy being processed, quench techniques for forgings include controlled-temperature water from 20 to 100 °C (75 to 212 °F) and synthetic quenchants, such as polyalkylene glycol and others, designed to achieve the required quench rate in order to obtain the required mechanical properties without excessive distortion. State-of-the-art aluminum forging solution treatment and age furnaces have multiple control/recording systems, microprocessor furnace control and operation systems, and quench monitoring and recording equipment, including video camera systems, that provide very precise control and repeatability of the heat treatment process. These systems can be interfaced with computer-integrated manufacturing systems.

Aluminum alloy forgings are often straightened between solution treatment and aging. Straightening is typically accomplished cold using either hand (frequently, press assisted) or die straightening techniques.

Many aluminum alloy forgings in the 2*xxx* and 7*xxx* series are compressively stress relieved between solution treatment and aging in order to reduce machining distortion. Depending on the part configuration, such compressive stress relief is accomplished by cold forging with open or closed dies, achieving a permanent set of 1 to 5%. With closed-die compressive stress relief, depending on part configuration, cold forging is accomplished either in the finish-forging dies (temper designation: T*xx*54) or in a separate set of cold-work dies (temper designation: T*xx*52). Some annular and other shapes of aluminum alloy forgings are stress relieved by cold stretching (temper designation: T*xx*51). Additional information on the heat treatment of aluminum alloys, including forgings, is available in the article "Heat Treating of Aluminum Alloys" in *Heat Treating*, Volume 4 of the *ASM Handbook* and in Ref 2.

Inspection of aluminum alloy forgings takes two forms: in-process inspection and final inspection. In-process inspection, using such techniques as statistical process control and/or statistical quality control, is used to determine that the product being manufactured meets critical characteristics and that the forging processes are under control. Final inspection, including mechanical property testing, is used to verify that the completed forging product conforms with all drawing and specification criteria. Typical final inspection procedures used for aluminum alloy forgings include dimensional checks, heat treatment verification, and nondestructive evaluation.

Dimensional Inspection. All final forgings are subjected to dimensional verification. For open-die forgings, final dimensional inspection may include verification of all required dimensions on each forging or the use of statistical sampling plans for groups or lots of forgings. For closed-die forgings, conformance of the die cavities to the drawing requirements, a critical element in dimensional control, is accomplished prior to placing the dies in service by using layout inspection of plaster or plastic casts of the cavities. With the availability of CAD databases on forgings, such layout inspections can be accomplished more expediently with CAM-driven equipment, such as coordinate-measuring machines or other automated inspection techniques. With verification of die cavity dimensions prior to use, final part dimensional inspection may be limited to verifying the critical dimensions controlled by the process (such as die closure) and monitoring the changes in the die cavity. Further, with high-definition and precision aluminum forgings, CAD databases and automated inspection equipment, such as coordinate-measuring machines and 2-D fiber optics, can be used in many cases for actual part dimensional verification.

Heat Treatment Verification. Proper heat treatment of aluminum alloy forgings is verified by hardness measurements and, in the case of 7*xxx*-T7*xxx* alloys, by eddy-current inspection. In addition to these inspections, mechanical property tests are conducted on forgings to verify conformance to specifications. Mechanical property tests vary from destruction of forgings to tests of extensions and/or prolongations forged integrally with the parts. Additional information on hardness and the electrical conductivity inspection and mechanical property testing of aluminum alloys is available in the article "Heat Treating of Aluminum Alloys" in *Heat Treating*, Volume 4 of the *ASM Handbook.*

Nondestructive Evaluation. Aluminum alloy forgings are frequently subjected to nondestructive evaluation to verify surface or internal quality. The surface finish of aluminum forgings after forging and caustic cleaning is generally good. A surface finish of 125 rms or better is considered normal for forged and etched aluminum alloys; under closely controlled production conditions, surfaces smoother than 125 rms may be obtained. Selection of nondestructive evaluation requirements depends on the final application of the forging. When required, satisfactory surface quality is verified by liquid-penetrant, eddy-current, and other techniques. Aluminum alloy forgings used in aerospace applications are frequently inspected for internal quality using ultrasonic inspection techniques.

Reference cited in this section

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Forging of Aluminum Alloys

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Forging Advanced Aluminum Materials

The preceding discussion of aluminum alloy forging technology is based on existing, commercially available wrought alloys. However, aluminum alloy development, including ingot metallurgy (I/M) and other techniques, is providing three major groups of advanced aluminum materials designed to enhance the capabilities of aluminum in critical applications, particularly aerospace. These three groups are:

- Aluminum-lithium alloys
- Alloys produced using prealloyed powder metallurgy (P/M) based on rapid solidification or other powder-making technology, such as mechanical alloying (dispersion strengthening)
- Aluminum-base discontinuous metal-matrix composites, which can be produced by either I/M or P/M techniques

None of these three groups of advanced aluminum materials is currently used commercially, but alloy and process development, including forging, is the subject of intense efforts in order to make these materials available for future commercial applications.

Aluminum-Lithium Alloys

Reduced-density aluminum alloys with mechanical properties equivalent to those of existing high-strength alloys have been identified as an important technology for effecting major reductions in weight for aerospace applications. Lithium additions to aluminum (up to about 4%) have been shown to decrease alloy density by 7 to 10% and to increase elastic modulus. Aluminum-lithium alloy development began in the mid-1950s, but more focused efforts on this technology have been underway since 1980. Table 3 lists the composition ranges or nominal compositions of several registered I/M and developmental P/M aluminum-lithium alloys under evaluation in forgings. Listed for comparison is alloy 2020, the first registered aluminum-lithium alloy, which was withdrawn from service in the early 1970s because of inadequate fracture toughness.

Table 3 Compositions of aluminum-lithium alloys

Alloy	**Type**	**Composition, %[(a)]**								Alloy density[(c)]	
		Si[(b)]	**Cu**	**Mn**	**Mg**	**Cr**	**Li**	**Zr**	**Other**	**g/cm^3**	lb/in.^{3}
2090	I/M	0.10	2.4-3.0	0.05	0.25 max	0.05	1.9-2.6	0.08-0.15	0.12Fe, 0.10Zn, 0.15Ti	2.57	**0.093**
2091	I/M	0.20	1.8-2.5	0.10	1.1-1.9	0.10	1.7-2.3	0.04-0.16	0.3Fe, 0.25Zn, 0.10Ti	2.57	**0.093**

8090	I/M	0.20	1.0-1.6	0.10	0.6-1.3	0.10	2.2-2.7	0.04-0.16	0.3Fe, 0.25Zn, 0.10Ti	2.53	**0.091**
643	P/M	. . .	0.8-1.1	. . .	0.4-0.6	. . .	3.4-3.6	0.4-0.6	. . .	2.47	**0.089**
644	P/M	. . .	0.8-1.1	. . .	0.4-0.6	. . .	3.0-3.2	0.4-0.6	. . .	2.49	**0.090**
IN905XL	P/M	. . .	. . .	. . .	4.0	. . .	1.5	. . .	0.8O, 1.1C	2.57	**0.093**
2020	**I/M**	**. . .**	**4.5**	**0.5**	**0.5**	**. . .**	**1.1**	**. . .**	**0.2Cd**	**2.71**	**0.098**

(a) All compositions contain balance of aluminum.

(b) Maximum.

(c) Densities of wrought conventional aluminum alloys range from 2.64 g/cm^3 (0.095 lb/in.^{3}) for alloy 5056 to 2.82 g/cm^3 (0.102 lb/in.^{3}) for alloys 7178 and 2011.

Aluminum-lithium alloys have been found to be readily forgeable, with flow stresses between those of alloy 2219 and 2014 (Fig. 1). Currently, recommended forging temperatures have not been fully defined; however, for the I/M alloys 2090, 2091, and 8090 and for some P/M alloys, the required metal temperatures can be expected to be similar to those of alloys 2014, 2219, or 2618 in Table 1. For other P/M alloys, the required metal temperatures may be lower. At least one of these alloys, IN905XL, may require no heat treatment; strengthening is achieved by a combination of work hardening and dispersion strengthening.

Desired mechanical properties, specifically combinations of high strength and high fracture toughness, in several recently developed aluminum-lithium alloys are highly dependent on relatively high levels (4 to 8%) of cold reduction between solution treatment and aging. Achieving these levels of cold reduction uniformly is difficult with many closed-die forgings; therefore, most current aluminum-lithium heat-treatable alloy forgings are processed to -T6*xxx* tempers, with attendant reductions in mechanical properties. Currently, mechanical property achievements in aluminum-lithium alloy forgings are similar to those obtained with 2014-T6 or -T61 and/or 2024-T6.

Aluminum-lithium alloys are considerably more costly than the current, commercially used aluminum alloys. Therefore, it is evident that high-definition and precision, net shape forgings that significantly reduce metal input in forging manufacture and reduce subsequent machining are the most cost-effective forging methods for these alloys when they are fully commercialized.

Prealloyed P/M Alloys

Rapid solidification, mechanical alloying, and other P/M technologies have been used to develop unique compositions of high-strength, elevated-temperature, and corrosion-resistant alloys that would not be achievable with standard I/M techniques. Table 4 lists the composition ranges for several registered high-strength prealloyed P/M aluminum alloys and the nominal compositions of developmental high-strength, elevated-temperature, and/or corrosion-resistant alloys under evaluation in forgings.

Table 4 Compositions of prealloyed aluminum P/M alloys

Alloy	**Type**[(a)]	**Density**	Composition, %[(b)]

		g/cm³	lb/in.³	Si	Fe	Cu	Mg	Zn	O	Other
X7064	Hi-str	2.82	0.102	0.12 max	0.15 max	1.8-2.4	1.9-2.9	6.8-8.0	0.05-0.3	**0.06-0.25Cr, 0.1-0.5Zr, 0.1-0.4Co**
7090	Hi-str	2.85	0.103	0.12 max	0.15 max	0.6-1.3	2.0-3.0	7.3-8.7	0.2-0.5	**1.0-1.9Co**
7091	Hi-str	2.82	0.102	0.12 max	0.15 max	1.1-1.8	2.0-3.0	5.8-7.1	0.2-0.5	**0.2-0.6Co**
IN9021	Hi-str	2.79	0.101	. . .	. . .	4.0	1.5	. . .	0.8	**1.1C**
CW67	Hi-str	2.87	0.104	. . .	. . .	1.5	2.2	9.0	0.35	**0.14Zr, 0.1Ni**
IN9052	Corr	2.65	0.096	. . .	. . .	. . .	4.0	. . .	0.8	**1.1C**
CU78	Elev	2.93	0.106	. . .	8.3	. . .	. . .	. . .	. . .	**4.0Ce**
CZ42	Elev	2.93	0.106	. . .	7.0	. . .	. . .	. . .	. . .	**6.0Ce**
CW63	Elev	2.74	0.099	13.0	0.1	. . .	. . .	. . .	. . .	**4.5Mn, 0.2Cr, 0.2Ti**
Al-Fe-Mo-V	Elev	2.89	0.105	. . .	8.0	. . .	. . .	. . .	. . .	**1.0V, 2.0Mo**
FVS0812	**Elev**	**2.96**	**0.107**	**1.65**	**8.0**	**. . .**	**. . .**	**. . .**	**. . .**	**1.35V**

(a) Hi-str, high-strength; Corr, corrosion-resistant; Elev, elevated-temperature.

(b) All compositions contain balance of aluminum.

High-Strength Prealloyed P/M Aluminum Alloy Forgings. The flow stresses and deformation behavior of alloys 7090, 7091, X7064, CW67, and IN9021 have been found to be similar to those of alloy 7075 (Fig. 1). These alloys have been found to be readily forgeable by all existing forging techniques and workable into all types of final forging shapes, ranging from open-die forgings to all forms of closed-die forgings. The recommended metal temperatures of these alloys for forging are the same as those for alloys 7010, 7049, 7050, and/or 7075 (Table 1), and the die temperatures are the same as those listed in Table 2.

Four of these high-strength prealloyed P/M alloys are of the 7*xxx* series, and in forgings, they are typically heat treated to -T7*xxx* tempers, with or without compressive stress relief, for optimal combinations of strength, fracture toughness, and resistance to exfoliation or stress-corrosion cracking. Prealloyed P/M aluminum alloys are far superior to I/M alloys in corrosion resistance at very high strength levels. The remaining high-strength alloy, IN9021, is typically heat treated to the -T4 temper in forgings.

Rapidly solidified or mechanically alloyed powder in these alloys is consolidated into billets ranging in size from approximately 45 kg (100 lb) to as large as 1360 kg (3000 lb) through the use of several consolidation techniques, such as vacuum hot pressing and hot isostatic pressing. In this billet form, high-strength prealloyed P/M alloys can usually be fabricated directly into forgings or, with other working techniques, such as rolling or extrusion, into bar or plate stock for forging. As with aluminum-lithium alloys, the aluminum alloys produced using prealloyed P/M techniques are considerably more expensive than the commercially used I/M 7*xxx* alloys. Therefore, high-definition and precision, net shape forgings that reduce required metal input and subsequent machining are likely to be the most cost-effective forging methods. Several of these alloys have found limited commercial application in forgings for aerospace applications that require their unique property combinations.

Corrosion-Resistant Prealloyed P/M Aluminum Alloy Forgings. Alloy IN9052 (Table 4) is an intermediate-strength prealloyed aluminum alloy with mechanical properties similar to those of I/M alloy 5083 but with superior corrosion resistance. This alloy is forged at relatively low temperatures (<370 °C, or 700 °F), and its flow stress and deformation characteristics are also similar to those of alloy 5083. As with the high-strength P/M aluminum alloys, IN9052 is consolidated into billets and then into extruded stock prior to forging. The cost of this material suggests that high-definition and/or precision forgings will be the most cost-effective forging type.

Elevated-Temperature Prealloyed P/M Aluminum Alloy Forgings. Several rapid-solidification techniques, including atomization, melt spinning, and planar casting, have been used to develop a class of prealloyed aluminum alloys with significantly improved elevated-temperature properties over those of existing wrought I/M aluminum alloys such as 2219 and 2618 and cast aluminum alloys such as A201. The nominal compositions of several of these alloys are listed in Table 4. These alloys are being developed to provide enhanced properties in forgings in the range of 205 to 345 °C (400 to 650 °F), a temperature level that exceeds the useful capability of existing aluminum alloys, and to be competitive on a density-compensated basis with some titanium alloys.

By virtue of their elevated-temperature capabilities, these alloys have been found to be difficult to fabricate into forgings, displaying flow stresses up to twice that of alloy 7075 (Fig. 1). Recommended forging temperatures for these alloys have not been completely established, but the alloys are typically forged at temperatures below 370 °C (700 °F) in order to maintain their unique microstructural features. All of these elevated-temperature aluminum alloys are not heat treatable and develop their mechanical properties through dispersion strengthening, intermetallic compounds, and/or work hardening.

The working history of these alloys has also been shown to be a critical element in their suitability for fabrication. For example, several alloys are not forgeable in the consolidated-billet form, but must be given primary working through extrusion or other techniques. However, the forging process developmental work conducted to date has demonstrated that these alloys can be successfully fabricated into sophisticated closed-die and annular forging shapes, including high-definition and precision forgings. As with all expensive advanced aluminum alloy materials, these forgings may be the most cost effective through the implementation of material and machining conservation.

Aluminum-Base Discontinuous Metal-Matrix Composites

An emerging advanced aluminum materials concept is the addition of ceramic particles, or whiskers, to aluminum-base alloys through the use of either ingot-melting or casting and/or P/M techniques, creating a new class of materials termed discontinuous metal-matrix composites. In these materials systems, the reinforcing material (for example, silicon carbide, boron carbide, or boron nitride) is not continuous, but consists of discrete particles within the aluminum alloy matrix. Unlike continuous metal-matrix composites, discontinuous metal matrix composites have been found to be workable by all existing metalworking techniques, including forging. Addition of the reinforcement to the parent aluminum alloy matrix, typically in volume percentages from 10 to 40%, modifies the properties of the alloy significantly. Typically, compared to the matrix alloy and temper, such additions significantly increase the elastic and dynamic moduli, increase strength, reduce ductility and fracture toughness, increase abrasion resistance, increase elevated-temperature properties, and do not significantly affect corrosion resistance. Table 5 lists several of the developmental discontinuous metal-matrix composite materials that are being evaluated in forgings. None of these materials yet has significant commercial application; however, alloy and forging process development continues.

Table 5 Aluminum-base discontinuous metal-matrix composite materials

Producer	**Type**	**Matrix**	**Reinforcement**[(a)]	Reinforcement loading,

		alloys		vol %
Alcoa	P/M	2*xxx*	SiC(p)	**0-30**
		7*xxx*	SiC(p)	**0-30**
Dural	I/M	2014	SiC(p)	**0-40**
		6061	SiC(p)	**0-40**
		7075	SiC(p)	**0-40**
DWA	P/M	2024	SiC(p)	**0-40**
		6061	SiC(p)	**0-40**
		7090	SiC(p)	**0-40**
		7091	SiC(p)	**0-40**
Silag	P/M	1100	SiC(w)/SiC(p)	**0-30**
		6061	SiC(w)/SiC(p)	**0-30**
		2124	SiC(w)/SiC(p)	**0-30**
		5083	SiC(w)/SiC(p)	**0-30**
		7075	SiC(w)/SiC(p)	**0-30**
		7090	SiC(w)/SiC(p)	**0-30**
		7091	SiC(w)/SiC(p)	**0-30**
Kobe	P/M-I/M	2024	SiC(w)	**0-30**
		6061	SiC(w)	**0-30**
		7075	**SiC(w)**	**0-30**

(a) SiC(p), particulate reinforcement; SiC(w), whisker reinforcement

The forging evaluation of these materials suggest that reinforcing additions to existing aluminum alloys modify the deformation behavior and increase flow stresses. The fabrication history of such materials may also be critical to their deformation behavior in forging and final mechanical property development. Although the recommended metal temperatures in forging these materials remain to be fully defined, current efforts suggest that temperatures higher than those listed in Table 1 for matfix alloys are typically necessary. Forging evaluations have demonstrated that discontinuous metal-matrix composites based on existing wrought aluminum alloys in the 2*xxx*, 6*xxx*, and 7*xxx* series can be successfully forged into all forging types, including high-definition and precision closed-die forgings. Some evidence suggests that these materials are more abusive of closed-die tooling and that die lives in forging these materials may be shorter than is typical of the parent alloys.

Forging of Aluminum Alloys

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Aluminum Alloy Precision Forgings

Precision-forged aluminum alloys are a significant commercial forging product form that has undergone major growth in use and has been the subject of significant technological development and capital investment by the forging industry. For the purposes of this article, the term precision aluminum forgings will be used to identify a product that requires no subsequent machining by the purchaser other than, in some cases, the drilling of attachment holes. Figure 10 compares precision aluminum forging design characteristics with those of a conventional aluminum closed-die forging. Precision aluminum forgings are produced with very thin ribs and webs; sharp corner and fillet radii; undercuts, backdraft, and/or contours; and, frequently, multiple parting planes that may optimize grain flow characteristics.

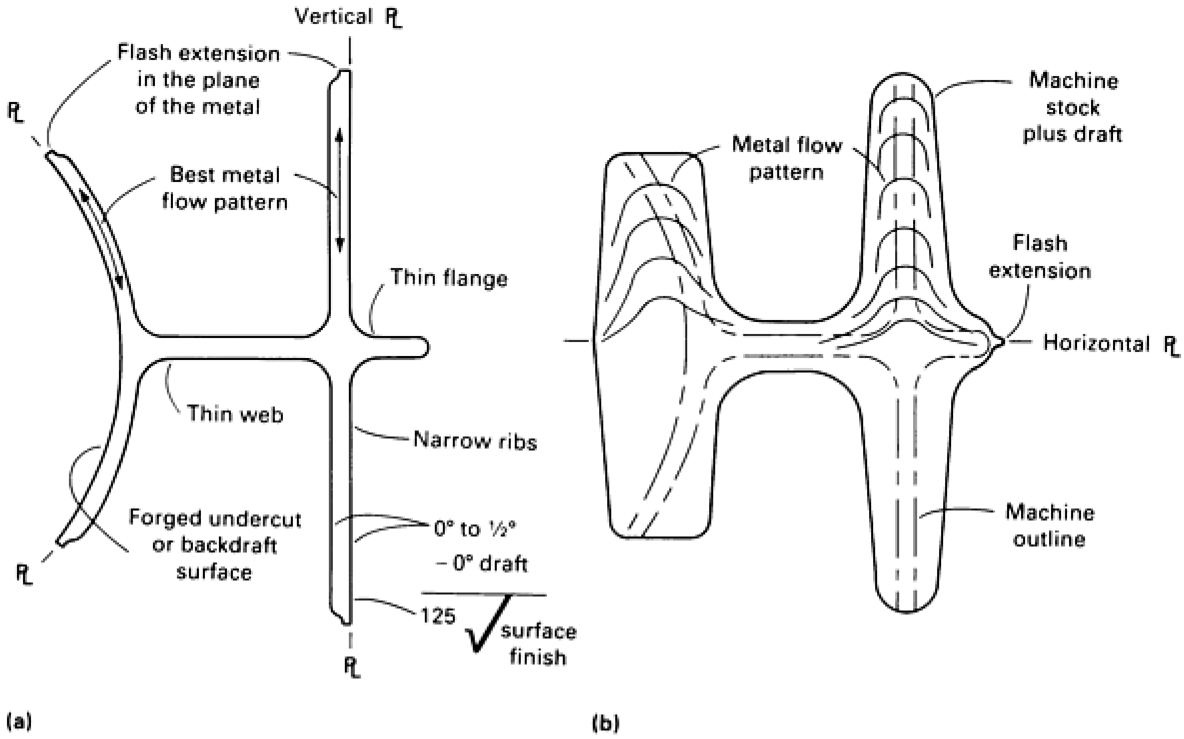


Fig. 10 Cross sections of precision (a) and conventional (b) forgings.

Design and tolerance criteria for precision aluminum forgings have been established to provide a finished product suitable for assembly or further fabrication. Precision aluminum forgings do not necessarily conform to the tolerances provided by machining of other product forms; however, as outlined in Table 6, design and tolerance criteria are highly refined in comparison to other aluminum alloy forging types and are suitable for the intended application of the product without subsequent machining by the purchaser. If the standard design and/or tolerance criteria for precision aluminum forgings are not sufficient, the forging producer frequently combines forging and machining to achieve the most cost-effective method of fabricating the necessary tolerances on the finished aluminum part.

Table 6 Design and tolerance criteria for aluminum precision forgings

Characteristic	Tolerance
Draft outside	0° +30', -0
Draft inside	1° +30', -0
Corner radii	1.5 ± 0.75 mm (0.060 ± 0.030 in.)
Fillet radii	3.3 ± 0.75 mm (0.130 ± 0.030 in.)
Contour	±0.38 mm (±0.015 in.)
Straightness	0.4 mm in 254 mm (0.016 in. in 10 in.)
Minimum web thickness[a]	2.3 mm (0.090 in.)
Minimum rib thickness	2.3 mm (0.090 in.)
Length/width tolerance	+0.5 mm, -0.25 mm (+0.020 in., -0.010 in.)
Die closure tolerance	+0.75, -0.25 mm (+0.030, -0.010 in.)
Mismatch tolerance	0.38 mm (0.015 in.)
Flash extension	0.75 mm (0.030 in.)

(a) Web thicknesses as small as 1.5 mm (0.060 in.) have been produced in certain forging designs.

Tooling and Design. Precision aluminum forging uses several tooling concepts to achieve the desired design shape; selection of the specific tooling concept is based on the design features of the precision-forged part. The three major tooling systems used are illustrated in Fig. 11. A two-piece upper and lower die system (Fig. 11a) is typically employed to precision forge shapes that can be produced with essentially horizontal parting lines. This system is very similar to the die concepts used for the fabrication of the aluminum alloy blocker, conventional, and high-definition closed-die forgings discussed above. The three-piece (or through-die) die system (Fig. 11b) consists of an upper die, a lower die (through-die), and a knockout/die insert. This system is typically employed for parts without undercuts and with vertical parting lines. The final and most complex aluminum precision-forging tooling concept is the holder (or wrap-die) system which consists of an upper die, a lower die (or holder), and multiple, movable inserts, or wraps (Fig. 11c). The multiple-insert holder/wrap-die system is used to produce the most sophisticated aluminum precision-forged shapes, including those with complex contours, undercuts, and reverse drafts.

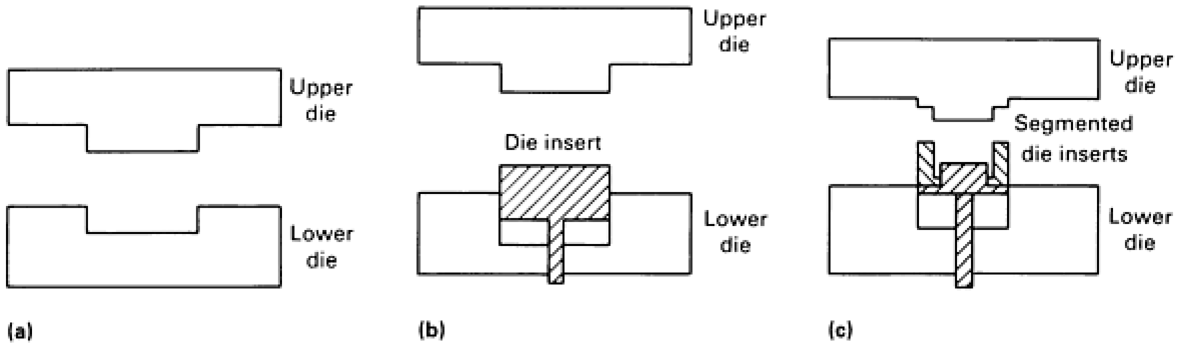
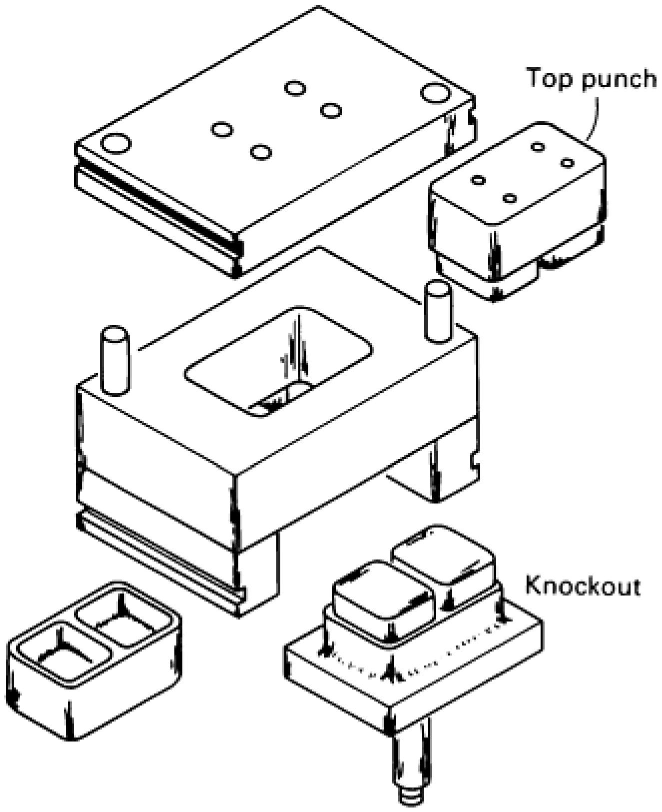
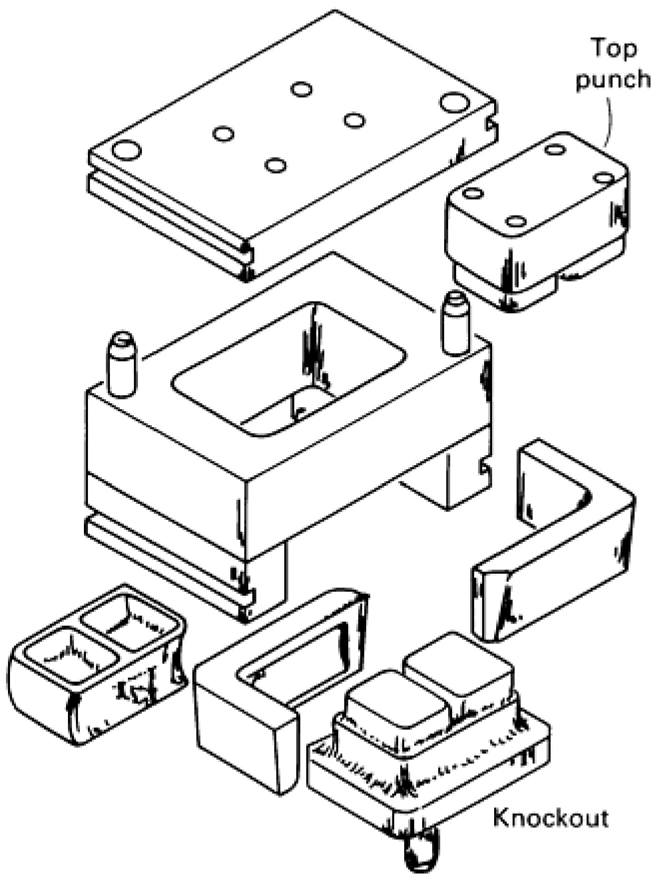


Fig. 11 Schematics of tooling concepts used in the manufacture of precision aluminum forgings. (a) Two-piece die system. (b) Three-piece die (through-die) system. (c) Multi-piece (wrap) die system. See also Fig. 12.

The through-die and the holder/wrap multiple-insert die systems for aluminum alloy precision forgings are critical elements in the sophistication of the precision-forging parts that can be produced. Figure 12 provides more insight into the components comprising these two die systems. These tooling concepts emerged in the early 1960s with the development of aluminum alloy precision-forging technology and have since been further refined and developed to provide increases in the size of precision part manufactured (see below).



(a)



(b)

Fig. 12 Components of a three-piece (through-die) system (a) and a multi-piece (wrap) die system (b) used for aluminum precision forgings. Source: Document D6-72713, Boeing Company, July 1985.

Because the through-die and holder/wrap-die systems are based on the commonality of significant portions of the tooling to a range of parts or to families of parts, the fabrication of dies for given precision forging is typically restricted to that necessary to produce the inserts. Thus, the cost of die manufacture for precision forgings is reduced when compared to that necessary to produce individual dies for each precision shape. However, aluminum precision-forging dies/inserts are usually two to four times more expensive than dies for other forging types for the same part.

The holder/wrap multiple-insert die concept is a highly engineered die system that can use two to six movable segments. Extraction of the part is achieved by lateral opening of the segments (wraps) once they have cleared the bottom die holder. Figure 13 illustrates the components of the wrap-die system first when the part has been forged (Fig. 13a) and then during extraction of the completed forging (Fig. 13b).

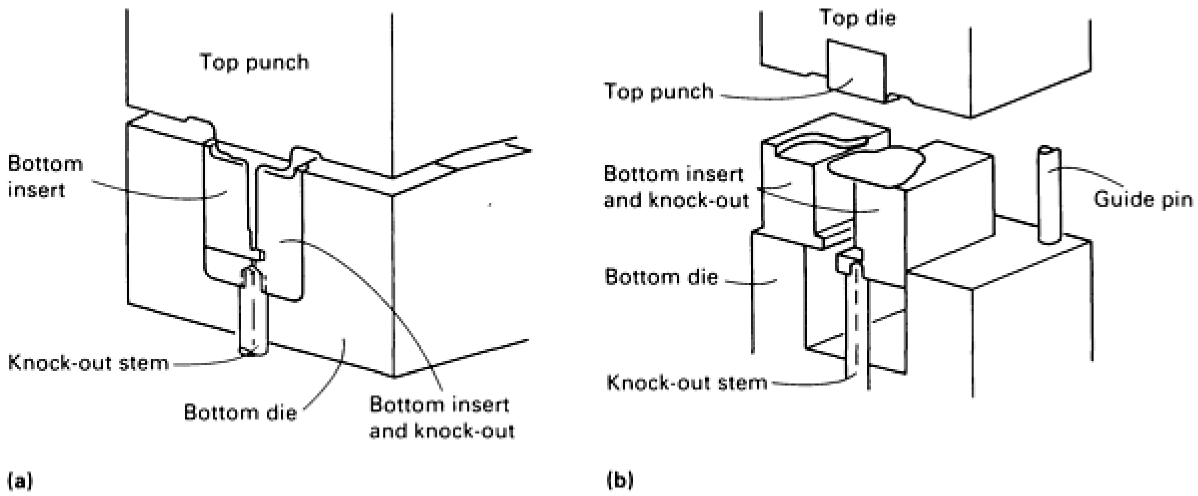


Fig. 13 Multi-piece (wrap) die system (a) during forging. (b) After forging, the die system opens to allow extraction of the completed part.

Aluminum alloy precision-forging part and tooling design are engineering-intensive activities that draw heavily on the experience of forging engineers and require interchange between producer and user to define the optimal precision-forging design for utilization, producibility, and cost control. As discussed in the section "Die Materials, Design, and Manufacture" in this article, computer-aided design, computer-aided manufacture, and computer-aided engineering (CAE) technologies have been found to be particularly effective in design and tooling manufacture activities for precision forgings to improve the design process, to assist in necessary forging process definition, and to reduce the costs of tooling manufacture.

The die materials used in dies, holders, and inserts for precision aluminum forgings are typically of the ASM 6F2 and 6G types. In some cases, inserts for high-volume precision aluminum alloy forgings are produced from hot-work grades, such as H12 and H13. Tooling for precision aluminum alloy forgings is produced by using the same techniques described above for other aluminum alloy forging types; however, CNC direct die sinking or electrical discharge machining has been found to be particularly effective for the manufacture of the close-tolerance tooling demanded by the design and tolerance criteria for precision aluminum alloy forgings.

Processing. Precision aluminum forgings can be produced from wrought stock, preformed shapes, or blocker shapes, depending on the complexity of the part, the tooling system being used, and cost considerations. Precision aluminum forgings are usually produced with multiple operations in finish dies; trimming, etching, and repair are conducted between operations.

Precision aluminum forgings are typically produced on hydraulic presses, although in some cases mechanical and/or screw presses have been effectively employed. Until recently, most precision aluminum forgings were produced on small-to-intermediate hydraulic presses with capacities in the range of 9 to 900 kN (1 to 100 tonf); however, as the size of precision parts demanded by users has increased, heavy hydraulic presses in the range of 135 to 310,000 kN (15 to 35,000 tonf) have been added or upgraded to produce this product. Forging process criteria for precision aluminum forgings are similar to those described above for other aluminum alloy forging types, although the metal and die temperatures used are usually controlled to near the upper limits of the temperature ranges outlined in Tables 1 and 2 to enhance producibility and to minimize forging pressures. The three-die systems described above are heated with state-of-the-art die heating techniques. As with other aluminum forging processes, die lubrication is a critical element in precision aluminum forging, and the die lubricants employed, although of the same genetic graphite-mineral oil formulations used for other aluminum forging processes, frequently use other organic and inorganic compounds tailored to the process demands.

Because of the design sophistication of precision aluminum forgings, this aluminum forging product is not supplied in mechanically stress-relieved tempers. However, because of the thin sections and the design complexity of this product, controlled quench rates following solution treatment, using such techniques as synthetic quenchants, are routinely employed to reduce residual stresses in the final product and/or to reduce distortion and necessary straightening to meet dimensional tolerances. In-process and final inspection for precision aluminum forgings are the same as described above for other forging products, including extensive use of automated inspection equipment, such as coordinate-measuring machines.

Precision aluminum forgings are frequently supplied as a completely finished product that is ready for assembly. In such cases, the producer may use both conventional and nonconventional machining techniques, such as chemical milling, along with forging to achieve the most cost-effective finished shape. Further, the forging producer may apply a wide variety of surface-finishing and coating processes to this product as specified by the purchaser.

Technology Development and Cost Effectiveness. Table 7 presents a summary of the history and future of the state-of-the-art in the size of aluminum precision forging producible. Within the last 5 years, the size that can be fabricated to the design and tolerance criteria listed in Table 6 has nearly doubled from 1775 cm^2 (275 in.^{2}) for H cross sections to more than 2580 cm^2 (400 in.^{2}) through enhancements of the precision aluminum forging process and equipment by forging producers. The precision forging shown in Fig. 14 illustrates the very large precision aluminum shapes being fabricated commercially. The difficult H cross-sectional forging shown in Fig. 14 has a plan view area of 2840 cm^2 (440 in.^{2}). This part incorporates some machining in its manufacture in selected regions where standard precision-forging tolerances were insufficient for assembly. Critical elements in projected changes in the state-of-the-art for aluminum precision parts are enhanced precision forging process control CAD/CAM/CAE technologies, advanced and/or integrated manufacturing technologies, and advanced die heating and die lubrication systems.

Table 7 Capabilities of the precision aluminum forging process based on part size

Forging type	Feature	Maximum size that can be processed		
		Past	Present	Future
T or U section	Plan view area	2580 cm^2 (400 in.^{2})	3870 cm^2 (600 in.^{2})	>6450 cm^2 (1000 in.^{2})
	Length	1015 mm (40 in.)	1525 mm (60 in.)	>2540 mm (100 in.)
H section	Plan view area	1775 cm^2 (275 in.^{2})	2580 cm^2 (400 in.^{2})	>3870 cm^2 (600 in.^{2})

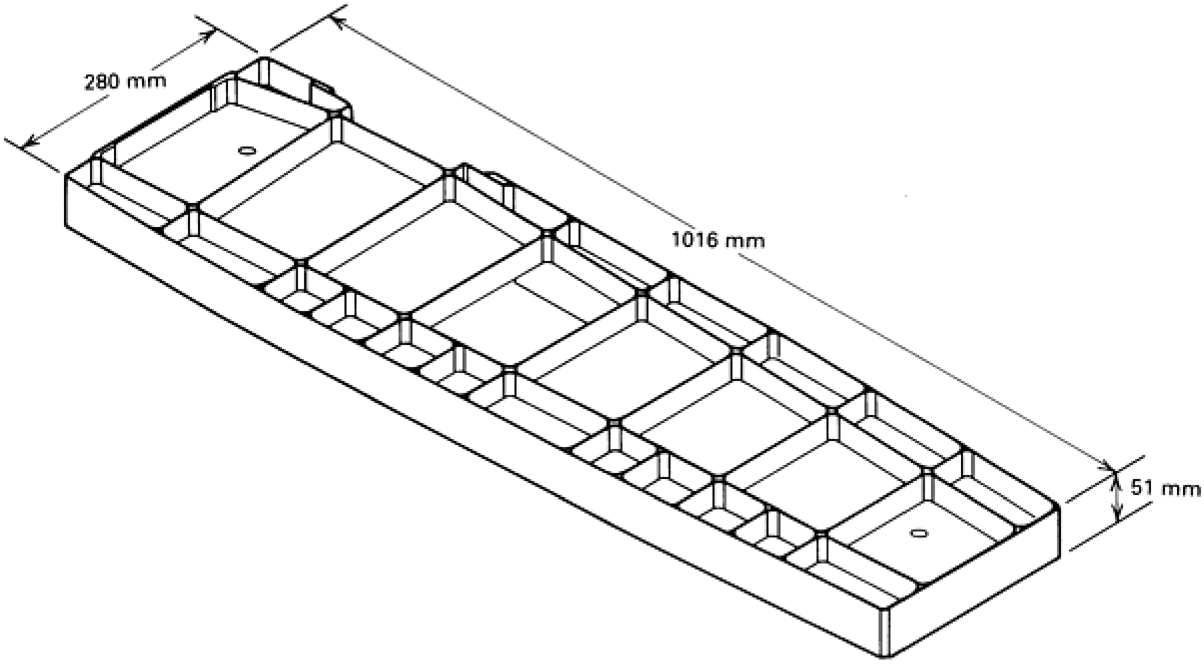


Fig. 14 Very large aluminum alloy 7075-T73 H section precision forging. Plan view area: 2840 cm^2 (440 in.^{2}); ribs 2 to 2.5 mm (0.080 to 0.100 in.) thick, 51 mm (2 in.) deep; webs typically 3 mm (0.120 in.), 2 mm (0.080 in.) in selected areas; finished weight: 5.6 kg (12.3 lb).

Selection of precision aluminum forging from the candidate methods of achieving a final aluminum alloy shape is based on value analyses for the individual shape in question. Figure 15 presents a cost comparison for a channel-type aluminum alloy part machined from plate, as machined from a conventional aluminum forging, and produced as a precision forging; costs as a function of production quantity include application of all material, tooling, setup, and fabrication costs. The breakeven point for the precision-forging method versus a conventional forging occurs with a quantity of 50 pieces, and when compared to the cost of machining the part from plate, the precision forging is always less expensive. Figure 15 also illustrates the potential cost advantages of precision aluminum alloy forgings. It has generally been found that precision aluminum forgings are highly cost effective when alternate fabrication techniques include multiple-axis machining in order to achieve the final part.

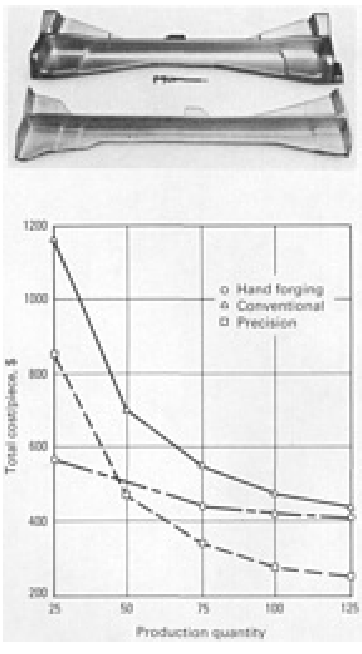


Fig. 15 Cost comparison for the manufacture of an aluminum alloy 7075-T73 component.

Recent forging industry and user evaluations have shown that precision aluminum forgings can reduce final part costs by up to 80 to 90% in comparison to machined plate and 60 to 70% in comparison to machined conventional forgings. Machining labor can be reduced by up to 90 to 95%. With such possible cost reductions in existing aluminum alloys and with the advent of more costly advanced aluminum materials, it is evident that further growth of precision aluminum forging use can be anticipated.

Forging of Aluminum Alloys

G.W. Kuhlman, Aluminum Company of America

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Forging of Copper and Copper Alloys

Robert A. Campbell, Mueller Brass Company

Introduction

COPPER AND COPPER ALLOY FORGINGS offer a number of advantages over parts produced by other processes, including high strength as a result of working, closer tolerances than competing processes such as sand casting, and modest overall cost. The most forgeable copper alloy, forging brass (alloy C37700), can be forged into a given shape with substantially less force than that required to forge the same shape from low-carbon steel. A less forgeable copper alloy, such as an aluminum bronze, can be forged with approximately the same force as that required for low-carbon steel.

Copper and copper alloy forgings, particularly brass forgings, are used in valves, fittings, refrigeration components, and other high-pressure liquid and gas handling applications. High-strength bronze forgings find application as mechanical parts such as gears, bearings, and hydraulic pumps.

Closed-Die Forging. Most copper alloy forgings are produced in closed dies. The sequence of operations is the same as that used for forging a similar shape from steel, that is, fullering, blocking, and finishing, as required (see the article "Closed-Die Forging in Hammers and Presses" in this Volume). However, it is estimated that 90% of the forgings produced from forging brass are forged completely in one or two blows in a finishing die. The starting slugs or blanks are usually cut from extruded bars or tubes to eliminate the blocking operation. Excessive flash is produced, but it is easily trimmed and remelted. In the forging of parts of mild to medium severity, in plants where remelting facilities are available, cutting slugs from bars or tubes is usually the least expensive approach. However, in plants that do not remelt their scrap, the flash must be sold as scrap, and it is sometimes more economical to use blocking. Figure 1 illustrates forged copper alloy parts in a variety of configurations.

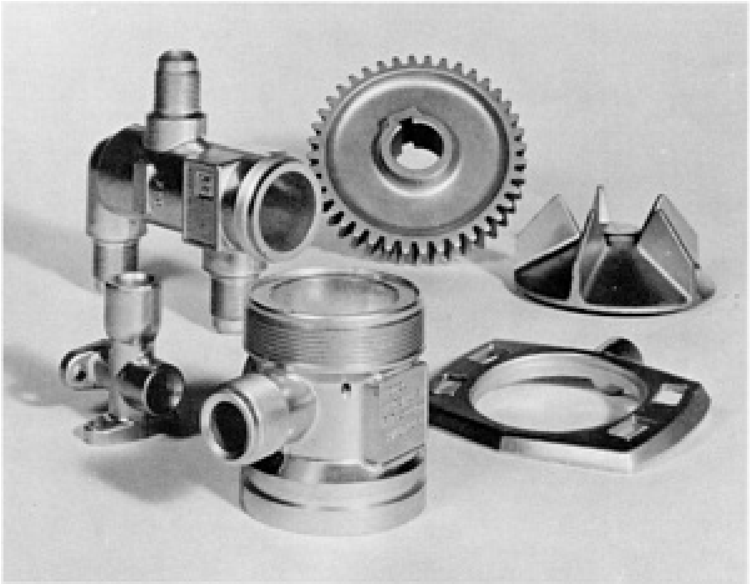


Fig. 1 Copper alloy parts made by closed-die forging. Courtesy of Mueller Brass Company

Cylindrical slugs are sometimes partially flattened before forging to promote better flow and consequently better filling of an impression. This can usually be done at room temperature between flat dies in a hammer or a press. A rectangular slug is occasionally obtained by extruding rectangular-section bar stock and sawing slugs from it.

Upset forging is used less frequently for copper alloys than for steels, primarily because copper alloys are so easily extruded. A part having a long shaftlike section and a larger-diameter head can often be made at less cost by extruding the smaller cross section from a larger one than by starting with a small cross section and upsetting to obtain the head.

In the upsetting of copper alloys, the same rule applies for maximum unsupported length as is used for steels, that is, not more than three times stock diameter. For the forging of brass, single-blow upsetting as severe as 3 to 1 (upset three times starting diameter) is considered reasonable. In practice, however, upsets of this severity are rare. The degree of allowable upset for other copper alloys is somewhat less than that for forging brass, generally in proportion to forgeability (Table 1).

Table 1 Relative forgeability ratings of commonly forged copper alloys

Ratings are in terms of the most forgeable alloy, forging brass (C37700).

Alloy	**Nominal composition**	Relative forgeability[(a)], %
C10200	99.95 min Cu	**65**
C10400	Cu-0.027 Ag	**65**
C11000	99.9 min Cu	**65**
C11300	Cu-0.027Ag + O	**65**
C14500	Cu-0.65Te-0.008P	**65**

C18200	Cu-0.10Fe-0. 90Cr-0.10 Si-0.05Pb	**80**
C37700	Cu-38Zn-2Pb	**100**
C46400	Cu-39.2Zn-0.8Sn	**90**
C48200	Cu-38Zn-0.8Sn-0.7Pb	**90**
C48500	Cu-37.5Zn-1.8Pb-0.7Sn	**90**
C62300	Cu-10Al-3Fe	**75**
C63000	Cu-10Al-5Ni-3Fe	**75**
C63200	Cu-9Al-5Ni-4Fe	**70**
C64200	Cu-7Al-1.8Si	**80**
C65500	Cu-3Si	**40**
C67500	**Cu-39Zn-1.4Fe-1Si-0.1Mn**	**80**

(a) Takes into consideration such factors as pressure, die wear, and hot plasticity

In most designs, the amount of upset can be reduced by using slugs cut from specially shaped extrusions or by using one or more blocking impressions in the forging sequence. Additional information on upset forging is available in the article "Hot Upset Forging" in this Volume.

Ring rolling is sometimes used as a means of saving material when producing ring gears or similar ringlike parts. The techniques are essentially the same as those used for steel and are described in detail in the article "Ring Rolling" in this Volume. Temperatures are the same as those for forging the same alloy in closed dies.

Cost usually governs the minimum practical size for ring rolling. Most rings up to 305 mm (12 in.) in outside diameter are more economically produced in closed dies. However, if the face width is less than about 25 mm (1 in.) it is often less expensive to produce rings no larger than 203 mm (8 in.) in outside diameter by the rolling technique. The alloy being forged is also a factor in selecting ring rolling or closed-die forging. For example, alloys such as beryllium copper that are difficult to forge are better adapted to ring rolling. For these alloys, ring rolling is sometimes used for sizes smaller than the minimum practical for the more easily forged alloys.

Robert A. Campbell, Mueller Brass Company

Forging Alloys

Copper C12200 and the copper alloys most commonly forged are listed in Table 1. They comprise at least 90% of all commercially produced copper alloy forgings. Forging brass, the least difficult alloy to forge, has been assigned an arbitrary forgeability rating of 100.

Table 2 Recommended die materials for the forging of copper alloys

Part configurations of varying severity are shown in Fig. 2.

Maximum severity	Total quantity to be forged			
	100-10,000		$\geq$10,000	
	Die material	**Hardness, HB**	**Die material**	Hardness, HB
Hammer forging				
Part 1	H11 6G, 6F2	405-433 341-375	H12	**405-448**
Part 2	6G, 6F2	341-375	6G, 6F2 H12[(a)]	**341-375** **405-448**
Part 3	6G, 6F2	269-293	6G, 6F2	**302-331**
Part 4	H11	405-433	H11	**405-433**
Part 5	6G, 6F2	302-331	6G, 6F2[(b)]	**302-331**
Press forging				
Part 1	H12 6G, 6F2	477-514 341-375	H12	**477-514**
Part 2	6G, 6F2	341-375	H12	**477-514**
Part 3	**Part normally is not press forged from copper alloys**			
Part 4	H11	405-433	6G, 6F2[(c)]	**341-375**

(a) Recommended for long runs--for example, 50,000 pieces.

(b) With either steel, use H12 insert at 405-448 HB.

(c) With either steel, use H12 insert at 429-448 HB.

Some copper alloys cannot be forged to any significant degree, because they will crack. Leaded copper-zinc alloys, such as architectural bronze, which may contain more than 2.5% Pb, are seldom recommended for hot forging. Although lead content improves metal flow, it promotes cracking in those areas of a forging, particularly deep-extruded areas, that are not completely supported by, or enclosed in, the dies. This does not mean that the lead-containing alloys cannot be forged, but rather that the design of the forging may have to be modified to avoid cracking.

The solubility of lead in β-brass at forging temperatures is about 2% maximum, but lead is insoluble in β-brass at all temperatures. Consequently, although a lead content of up to 2.5% is permissible in Cu-40Zn α-β brasses, lead in excess of 0.10% in a Cu-30Zn α-brass will contribute to catastrophic cracking.

Other copper alloys, such as the copper-nickels, can be forged only with greater difficulty and at higher cost. The copper-nickels, primarily because of their higher forging temperatures, are sometimes heated in a controlled atmosphere, thus complicating the process. The silicon bronzes, because of their high forging temperatures and their compositions, cause more rapid die deterioration than the common forging alloys.

Robert A. Campbell, Mueller Brass Company

Machines

Most copper alloy forgings are produced in crank-type mechanical presses. With these presses, the production rate is high, and less operator skill is needed and less draft is required than in forging copper alloys in hammers.

Press size is normally based on the projected (plan) area of the part, including flash. The rule of thumb is 0.5 kN of capacity per square millimeter of projected area (40 tonf/in.^{2}). Therefore, a forging with a projected area of 32.2 cm^2 (5 in.^{2}) will require a minimum of 1780 kN (200 tonf) capacity for forgings of up to medium severity. If the part is complicated (for example, with deep, thin ribs), the capacity must be increased.

Speed of the press is not critical in forging copper alloys, but minimum duration of contact between the hot forging and the die is desirable to increase die life. Detailed information on hammers and presses is available in the articles "Hammers and Presses for Forging" and "Selection of Forging Equipment" in this Volume.

Robert A. Campbell, Mueller Brass Company

Dies

Dies designed for forging copper or copper alloys usually differ from those designed for forging the same shapes from steel, as follows:

- The draft angle can be decreased for forging copper (3° max and often less than 3°)
- The die cavity is usually machined to dimensions that are 0.005 in./in. less than those for forging steels
- The die cavity is usually polished to a better surface finish for forging copper and copper alloys

Die materials and hardnesses selected for forging copper alloys depend on part configuration (forging severity) and number of parts to be produced. Figure 2 illustrates the forging severities of parts listed in Table 2.

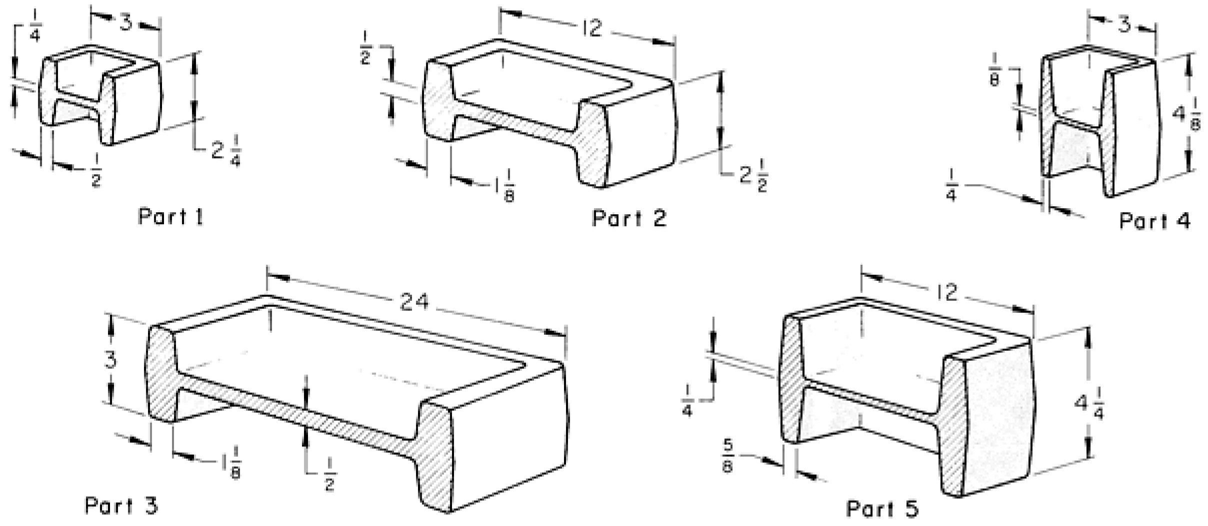


Fig. 2 Forged copper alloy parts of varying severity. See Table 2 for recommended die materials.

Whether the dies are made entirely from a hot-work steel such as H11 or H12 or whether or not inserts are used depends largely on the size of the die. Common practice is to make the inserts from a hot-work steel and to press them into rings or holders made from a low-alloy die block steel (Table 2) or L6 tool steel. Hardness of the ring or holder is seldom critical; a range of 341 to 375 HB is typical. Details on the selection of die material and data on die wear and life are available in the article "Dies and Die Materials for Hot Forging" in this Volume.

Robert A. Campbell, Mueller Brass Company

Preparation of Stock

The two methods most often used for cutting stock into slugs for forging are shearing and sawing.

Shearing is faster than other methods of cutting stock. In addition, no material is wasted in kerf. However, the ends of sheared stock are rougher than those of sawed sections. Rough or torn ends usually cannot be permitted, because forging defects are likely to nucleate from the rough ends. If shearing is used, best practice is to condition the sheared ends--for example, with a radiusing machine.

Sawing with circular saws having carbide-tipped blades is widely used as a method of preparing stock because sawed ends are usually in much better condition than sheared ends. The principal disadvantage of sawing is the loss of metal because of the kerf. In addition, if the burrs left by sawing are not removed, defects are likely to develop in the forging. Deburring of the saw sections by grinding, radiusing or barrel tumbling is always recommended.

Forging of Copper and Copper Alloys

Robert A. Campbell, Mueller Brass Company

Heating of Billets or Slugs

Optimal forging temperature ranges for ten alloys are given in Table 3. Atmosphere protection during billet heating is not required for most alloys, especially when forging temperatures are below 705 °C (1300 °F). For temperatures toward the top of the range in Table 3, a protective atmosphere is desirable and is sometimes required. An exothermic atmosphere is usually the least costly, and it is satisfactory for heating copper alloys at temperatures above 705 °C (1300 °F).

Table 3 Recommended forging temperature ranges for copper alloys

Alloy	Temperature range	
	°C	°F
C12200	730-845	**1350-1550**
C18200	650-760	**1200-1400**
C37700	650-760	**1200-1400**
C46400	595-705	**1100-1300**
C62400	705-815	**1300-1500**
C64200	730-900	**1350-1650**
C67000	595-705	**1100-1300**
C67300	595-730	**1100-1350**
C67400	595-730	**1100-1350**

Gas-fired furnaces are almost always used, and furnace design is seldom critical. Open-fired conveyor chain or belt types are those most commonly used.

Any type of pyrometric control that can maintain temperature within ±5 °C (± 10 °F) is suitable. As billets are discharged, a periodic check with a prod-type pyrometer should be made. This permits a quick comparison of billet temperature with furnace temperature.

Heating Time. The time at temperature is critical for all copper alloys, although to varying degrees among the different alloys. For forging brass (alloy C37700), the time is least critical, but for aluminum bronze, naval brass, and copper, it is most critical. Time in excess of that required to bring the billet uniformly to forging temperature is detrimental, because it causes grain growth and increases the amount of scale.

Reheating Practice. When forging in hammers, all of the impressions are usually made in one pair of dies, and reheating is rarely required. In press forging, particularly in high-production applications, blocking is often done separately, followed by trimming before the forging is completed. The operations are likely to be performed in different presses; therefore the partially completed forging is reheated to the temperature originally used.

Forging of Copper and Copper Alloys

Robert A. Campbell, Mueller Brass Company

Heating of Dies

Dies are always heated for forging copper and copper alloys, although because of the good forgeability of copper alloys, die temperature is generally less critical than for forging aluminum. Dies are seldom preheated in ovens. Heating is usually accomplished by ring burners. Optimal die temperatures vary from 150 to 315 °C (300 to 600 °F), depending on the forging temperature of the specific alloy. For alloys having low forging temperatures, a die temperature of 150 °C (300 °F) is sufficient. Die temperature is increased to as much as 315 °C (600 °F) for the alloys having the highest forging temperatures shown in Table 3.

Forging of Copper and Copper Alloys

Robert A. Campbell, Mueller Brass Company

Lubricants

Dies should be lubricated before each forging operation. A spray of colloidal graphite and water is usually adequate. Many installations include a spray that operates automatically, timed with the press stroke. However, the spray is often inadequate for deep cavities and is supplemented by swabbing with a conventional forging oil.

Forging of Copper and Copper Alloys

Robert A. Campbell, Mueller Brass Company

Trimming

Brass forgings are nearly always trimmed at room temperature. Because the forces imposed on the trimming tools are less than those for trimming steel forgings, the trimming of brass forgings seldom poses problems. Large forgings, especially in small quantities, are commonly trimmed by sawing off the flash and punching or machining the web sections. Trimming tools usually are used for trimming large quantities, especially of small forgings that are relatively intricate and require several punchouts.

Materials for trimming dies vary considerably among different plants. In some plants, it is common practice for normal trimming to make the punch from low-alloy die steel at a hardness of 46 to 50 HRC. One reason for using this steel is economy; the punches are often made from pieces of worn or broken dies. Blades for normal trimming are sometimes made by hardfacing low-carbon steels such as 1020.

In other plants both punches and blades are made from L6 steel and are heat treated to 52 to 56 HRC. Worn tools of this material can be repaired by welding with an L6 rod, remachining, and heat treating; O1 tool steel heat treated to 58 to 60 HRC has also been used for punches and blades for cold trimming. When close trimming is required, blades and punches fabricated from a high-alloy tool steel such as D2, hardened to 58 to 60 HRC, will give better results and longer life.

Hot trimming is sometimes used for one or both of the following reasons:

- For alloys such as aluminum bronzes that are brittle at room temperature
- When flash is heavy and sufficient power is not available for cold trimming

Hot trimming is usually done at 425 °C (800 °F).

Because of the lower forces involved, tools for hot trimming are simpler than those for cold trimming. Although the tool materials discussed above can also be used for hot trimming, unhardened low-carbon steel will usually suffice as a punch material. The same grade of steel with a hardfacing is commonly used as blade material.

Forging of Copper and Copper Alloys

Robert A. Campbell, Mueller Brass Company

Cleaning

Scale and excess lubricants are easily removed from copper and copper alloy forgings by chemical cleaning. Pickling in dilute sulfuric acid is the most common method for cleaning brass and most other copper alloy forgings, although hydrochloric acid can also be used. The compositions of sulfuric and hydrochloric acid solutions, the pickling procedures, and the typical uses are given in Table 4.

Table 4 Cleaning solutions and conditions for copper and copper alloy forgings

Solution	**Composition**	**Use temperature, °C (°F)**	Uses
Sulfuric acid	4-15 vol% H_2SO_4 (1.83 specific gravity); rem H_2O	Room-60 (140)	**Removal of black copper oxide scale from brass forgings; removal of oxide from copper forgings**
Hydrochloric acid	40-90 vol% HCl (35% conc); rem H_2O	Room	**Removal of scale and tarnish from brass forgings; removal of oxide from copper forgings**
"Scale" dip A	40% conc HNO_3; 30% conc H_2SO_4; 0.5% conc HCl; rem H_2O	Room	**Used with pickle and "bright" dip to give a bright, lustrous finish to copper and copper alloy forgings**
"Scale" dip B	50% conc HNO_3; rem H_2O	Room	**Used with pickle and "bright" dip to give bright, lustrous finish to copper and copper alloy forgings**
"Bright" dip	**25 vol% conc HNO_3; 60 vol% conc H_2SO_4; 0.2% conc HCl; rem H_2O**	**Room**	**Used with pickle and "scale" dip to give bright, lustrous finish to copper and copper alloy forgings**

Aluminum bronzes form a tough, adherent aluminum oxide film during forging. An effective method of cleaning aluminum bronze forgings is first to immerse them in a 10% solution (by weight) of sodium hydroxide in water at 75 °C (170 °F) for 2 to 6 min. After rinsing in water, the forgings are pickled in acid solutions in the same way as brasses.

Alloys containing substantial amounts of silicon may form oxides of silicon removable only by hydrofluoric acid or a proprietary fluorine-bearing compound. Alloys containing appreciable quantities of nickel are difficult to pickle in solutions used for brasses, because nickel oxide has a limited solubility in these solutions. For these alloys, billets should be heated in a controlled atmosphere, so that scale is kept to a minimum and can be removed by using the practice outlined above and in Table 4 for brass.

Other methods of chemical cleaning can be used, depending largely on the desired finish. Additional information is available in the article "Surface Engineering of Copper and Copper Alloys" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Appearance. When a bright, lustrous finish is desired, the metal can be pickled in the sulfuric or hydrochloric acid pickles listed in Table 4 and then given two additional dips. Pickling removes surface oxides, and the second dip, a "scale" dip, prepares the metal for the "bright" dip that follows. "Scale" dips and "bright" dips are mixtures of sulfuric and nitric acids in proportions that vary widely from plant to plant. Generally, nitric acid accelerates the action of the dip, while sulfuric acid slows it down. These solutions are used at room temperature. Parts are first dipped in the "scale" dip, rinsed in water, dipped in the "bright" solution, rinsed in cold running water, and then rinsed in hot water and dried. Compositions of "scale" and "bright" dips are listed in Table 4.

Surface Finish. In normal practice, the surface finish of cleaned forgings is expected to be 5 μm (200 μin.) or better. By more precise control, a finish of 2.5 μm (100 μin.) or better can be obtained. Die finish is the major factor affecting the surface finish of forgings. The type of alloy forged and the amount of draft have a minor influence on surface finish.

Forging of Copper and Copper Alloys

Robert A. Campbell, Mueller Brass Company

Minimum-Draft Forgings

Zero-draft forgings can be produced from copper alloys, but are usually impractical. However, the minimum-draft concept is a practical approach for producing locating and clamping surfaces for machining operations, mating surfaces in assemblies, or other functional shapes where dimensional tolerances on such surfaces are broad enough to include normal forging tolerances but too close for normal draft angles.

Forging Design. The most obvious consideration is that any shape that has a negative draft angle would be impossible to eject without damage to the die or workpiece. With zero draft, the smallest error of form or dimension can damage the die and the workpiece. Therefore, a draft angle of $\frac{1}{8}$° should be considered the absolute minimum for production forging. This very small amount of positive draft is sufficient to eliminate the possibility of negative draft while producing forgings that have essentially zero draft.

Tolerances on closed-die forgings are normally ±0.25 mm (±0.010 in.) or better for small-to-medium forgings. It can be seen from Table 5 that a small draft angle can easily be accommodated within these tolerance limits. For example, a draft of $\frac{1}{4}$° would produce a taper of only 0.083 mm (0.00327 in.) on each side of a cavity 19 mm ($\frac{3}{4}$ in.) deep. Because the total taper of 0.166 mm (0.00654 in.) (both sides of the cavity) would be less than the usual 0.51 mm (0.020 in.) total tolerance on the cavity diameter, the part would be within tolerance for a specification of parallel sides.

Table 5 Relation of draft angle to draft for minimum-draft forgings

Draft angle, degrees	**Draft, in./in.**	Total taper on diameter, in./in.
$\frac{1}{8}$	0.00219	**0.00438**
$\frac{1}{4}$	0.00436	**0.00872**
$\frac{1}{2}$	0.00873	**0.01746**
1	**0.01745**	**0.03490**

Die Design. Conventional forging practice calls for draft angles of 2° or more on press forgings and up to 5 to 7° for hammer forgings. Draft angles of 1° or less increase cost. In general, as the draft angle is decreased, more force is required to eject the forging from the die cavity or to withdraw the punch from a hole. Conventional forgings can usually be ejected by a simple knockout pin. This method is not practical for minimum-draft forgings, because pin pressure would be sufficient to damage the part.

Ejection of minimum-draft forgings is nearly always accomplished through the use of inserted dies built on die cushions to provide a secondary action within the die. This provides a stripper action to the die so that ejection pressure is distributed over an entire surface rather than concentrated on a pin. Such double-action dies are more expensive to build and to maintain than solid dies, and their use slows the production rate.

Alloy Selection. Draft angles have no effect on the relative forgeability of copper-base alloys. Any alloy that can be forged by conventional means can be forged to minimum draft angles.

Forging of Magnesium Alloys

Introduction

The forgeability of magnesium alloys depends on three factors: the solidus temperature of the alloy, the deformation rate, and the grain size. Only forging-grade billet or bar stock should be used in order to ensure good workability. This type of product has been conditioned and inspected to eliminate surface defects that could open during forging, and it has been homogenized by the supplier to ensure good forgeability. Table 1 lists the compositions of magnesium alloys that are commonly forged, along with their forging temperatures.

Table 1 Recommended forging temperature ranges for magnesium alloys

Alloy	Recommended forging temperature[a]			
	Workpiece		Forging dies	
	°C	**°F**	**°C**	°F
Commercial alloys				
ZK21A	300-370	575-700	260-315	**500-600**
AZ61A	315-370	600-700	290-345	**550-650**
AZ31B	290-345	550-650	260-315	**500-600**
High-strength alloys				
ZK60A	290-385	550-725	205-290	**400-550**
AZ80A	290-400	550-750	205-290	**400-550**
Elevated-temperature alloys				

HM21A	400-525	750-975	370-425	**700-800**
EK31A	370-480	700-900	345-400	**650-750**
Special alloys				
ZE42A	290-370	550-700	300-345	**575-650**
ZE62	300-345	575-675	300-345	**575-675**
QE22A	**345-385**	**650-725**	**315-370**	**600-700**

(a) The strain-hardening alloys must be processed on a declining temperature scale within the given range to preclude recrystallization.

Magnesium alloys are often forged within 55 °C (100 °F) of their solidus temperature. An exception is the high-zinc alloy ZK-60, which sometimes contains small amounts of the low-melting eutectic that forms during ingot solidification. Forging of this alloy above about 315 °C (600 °F)--the melting point of the eutectic--can cause severe rupturing. This problem can be minimized by holding the cast ingot for extended periods at an elevated temperature to redissolve the eutectic and to restore a higher solidus temperature.

Machines and Dies

Machines. Hydraulic presses or slow-action mechanical presses are the most commonly used machines for the open-die and closed-die forging of magnesium alloys. In these machines, magnesium alloys can be forged with small corners and fillets and with thin web or panel sections. Corner radii of 1.6 mm ($\frac{1}{16}$ in.), fillet radii of 4.8 mm ($\frac{3}{16}$ in.), and panels or webs 3.2 mm ($\frac{1}{8}$ in.) thick are not uncommon. The draft angles required for extraction of the forgings from the dies can be held to 3° or less.

Magnesium alloys are seldom hammer forged or forged in a rapid-action press, because they will crack unless exacting procedures are used. Alloys ZK60A, AZ31B, and HM21A are more easily forged by these methods than AZ80A, which is extremely difficult to forge. Cracking can occur also in moderately severe, unsupported bending.

Magnesium alloys generally flow laterally rather than longitudinally. This characteristic must be considered in the design of tools.

Dies. Because forging temperatures for magnesium alloys are relatively low (Table 1), conventional low-alloy hot-work tool steels are satisfactory materials for forging dies. Dies are finished to a smooth, highly polished surface to prevent surface roughness, scratches, or imperfections on the forging. The high polish also promotes metal flow during forging. Wet abrasive blasting and extremely fine abrasive finishing papers are used to produce a smooth finish on die-impression surfaces.

Heating for Forging

In most cases, the mechanical properties developed in magnesium forgings depend on the strain hardening induced during forging. Strain hardening is accomplished by keeping the forging temperature as low as practical; however, if temperatures are too low, cracking will occur.

In a multiple-operation process, the forging temperature should be adjusted downward for each subsequent operation to avoid recrystallization and grain growth. In addition to controlling grain growth, the reduction in temperature allows for residual strain hardening after the final operation.

Heating can be done with fuel-fired or electrically heated furnaces. Inert or reducing atmospheres are not needed at temperatures below 480 °C (900 °F).

Because forging temperatures are well below the melting points of the various alloys, no fire hazard exists when temperatures are controlled with reasonable accuracy. However, uniformity of temperature must be maintained (at least throughout the final heating zone), and large gradients and hot spots must be avoided in the preliminary heating zones. Furnaces that are equipped with fans for recirculating the air within the furnace provide the greatest uniformity of heating.

Furnaces should be loaded so that air circulates readily throughout the work load. Close stacking or "cordwood" loading should be avoided, because it will result in low temperatures at the center of the load and possibly in overheating at the edges and exposed surfaces. Too high a temperature will cause the work metal to develop cracks from hot shortness, and too low a temperature will cause shear cracking.

Die Heating

Magnesium alloys are good conductors of heat; therefore, they are readily chilled by cold dies, causing the alloys to crack. Because die contact during forging is extensive and is maintained for a prolonged period of time, dies must be heated to temperatures not much lower than those used to heat the stock (Table 1).

Die temperature is less critical for ring-rolling tools, because the area of contact is small and the duration of contact is relatively short. Furthermore, temperature buildup during rolling compensates for heat loss. Ring-rolling tools, therefore, are heated only slightly to remove chill.

Lubrication

The lubricant used in the forging of magnesium alloys is usually a dispersion of fine graphite in a light carrier oil or kerosene. This lubricant is swabbed or sprayed onto the hot dies, so that the carrier burns off and leaves a light film of graphite. Frequently, dies are lightly relubricated after billets have been partially forged. The forging billet is sometimes dipped in the lubricant before forging. Although less convenient, lampblack may be applied directly from the sooty flame of a torch. When low die temperatures can be employed, the use of aqueous colloidal graphite contributes to cleaner working conditions.

Regardless of the lubricant selected, it is important that the coating of lubricant be thin and have complete coverage. Heavy deposits of graphite adhering to a forging can present a cleaning problem, because severe pitting or galvanic corrosion can occur if cleaning with acid is attempted. This graphite film is more readily removed by sand blasting.

Forging Practice

Forging pressures for the upsetting of magnesium alloy billets between flat dies are shown in Fig. 1. At normal press-forging speeds, the forging pressure increases and then decreases slightly with increased upset reduction, probably because work metal temperature increases during forging.

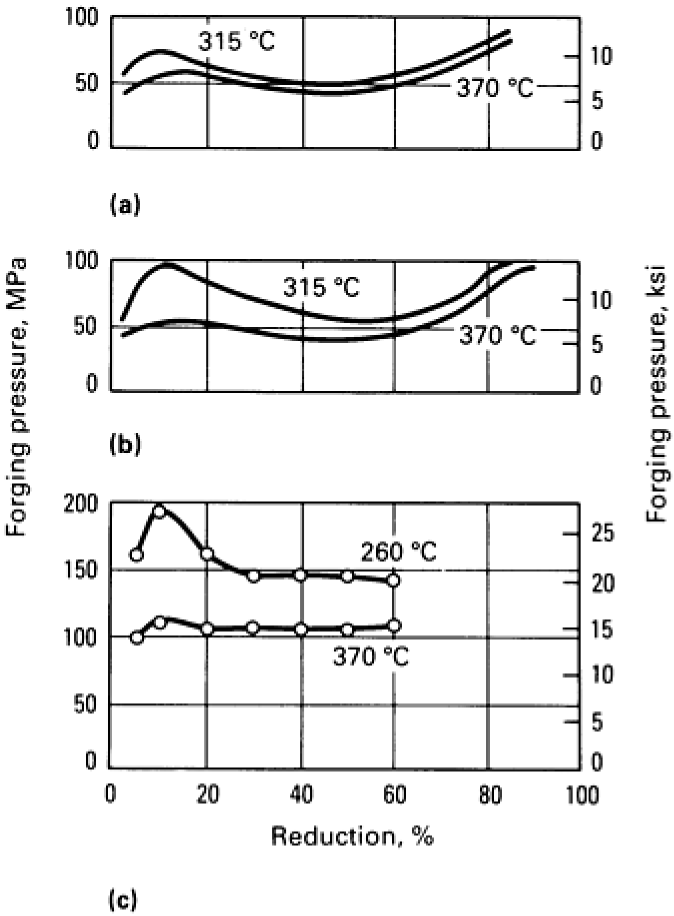
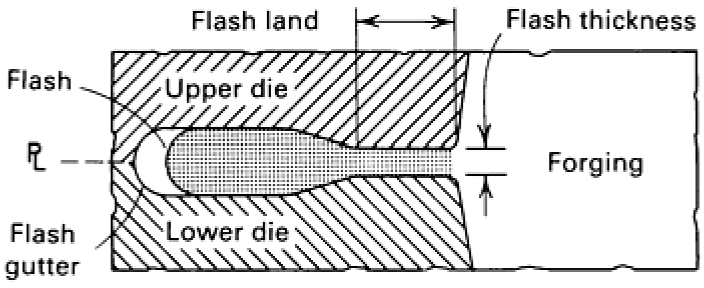


Fig. 1 Forging pressures required for the upsetting of magnesium alloy billets between flat dies. (a) Alloy AZ80A; strain rate: 0.11 s^{-1}. (b) Alloy AZ61A; strain rate: 0.11 s^{-1}. (c) Alloy AZ31B; strain rate: 0.7 s^{-1}.

Forging load and pressure in closed-die forging vary greatly with the shape being forged. Relatively small changes in flash dimensions, for example, can result in appreciable changes in the forging load:

Flash dimensions				Forging load	
Land		**Thickness**			
mm	**in.**	**mm**	**in.**	**mn**	tonf
3.8	0.15	1.2	0.046	2.7	**300**

2.5	0.1	0.64	0.025	3.5	**385**
5.0	0.2	0.64	0.025	4.9	**550**



Forging temperature has a marked effect on forging pressure requirements. Figure 2 shows the magnitude of this effect for magnesium alloy AZ31B in comparison with aluminum alloy 6061. As Table 2 shows, at normal forging temperatures, AZ31B requires greater forging pressure than carbon steel, alloy steel, or aluminum and requires less than stainless steel. Magnesium alloys flow less readily than aluminum into deep vertical die cavities. If two dies are needed for a typical aluminum structural forging, the same part in a magnesium alloy may require three dies for successful forging.

Table 2 Approximate forging pressures required for a 10% upset reduction of various materials at normal forging temperature in flat dies

Work metal	**Forging temperature**		Forging pressure	
	°C	**°F**	**MPa**	ksi
1020 steel	1260	2300	55	**8**
4340 steel	1260	2300	55	**8**
Aluminum alloy 6061	455	850	69	**10**
Magnesium alloy AZ31B	370	700	110	**16**

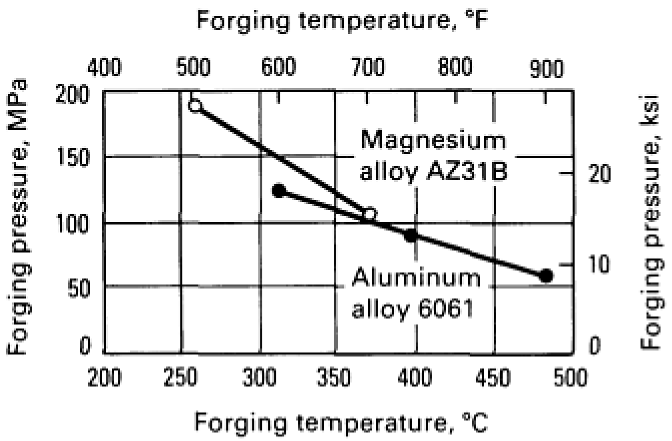


Fig. 2 Effect of forging temperature on forging pressure required for upsetting to a 10% reduction at hydraulic press speeds for a magnesium alloy and an aluminum alloy.

Grain-Size Control. An important objective in the forging of magnesium alloys is to refine the grain size. Alloys that are subject to rapid grain growth at forging temperatures (AZ31B, AZ61A, and AZ80A) are generally forged at successively lower temperatures for each operation. Common practice is to reduce the temperature about 15 to 20 °C (25 to 35 °F) after each step. For parts containing regions that receive only small reductions, all forging is often done at the lowest practical temperature to permit strain hardening. Grain growth in ZK60A and HM21A is slow at forging temperatures, and there is little risk of extensive grain growth.

Cooling Practice. Magnesium alloy forgings are water quenched directly from the forging operation to prevent further recrystallization and grain growth. With some of the age-hardening alloys, the quench retains the hardening constituents in solution so that they are available for precipitation during subsequent aging treatments.

Trimming. When only small quantities are being processed, magnesium alloy forgings are usually trimmed cold on a bandsaw. Hot trimming using a trimming press is done at 205 to 260 °C (400 to 500 °F).

Cleaning. Magnesium alloy forgings are usually cleaned in two steps. First, the workpiece is blast cleaned to remove any lubricant residue. This is followed by dipping in a solution of 8% nitric acid and 2% sulfuric acid and rinsing in warm water. The clean forgings can be dipped in a dichromate solution to inhibit corrosion if necessary.

Subsequent Heat Treatment

Forgings of some magnesium alloys, such as ZK21A, AZ31B, and AZ61A, are always used in the as-forged condition (F temper). Forgings of AZ80A, ZK60A, or HM21A can be used in either the F or T5 (artificially aged) condition. Solution treatment followed by artificial aging (T6 temper) can be used for EK31A forgings. More information on the heat treating of magnesium alloys is available in the article "Heat Treating of Magnesium Alloys" in *Heat Treating*, Volume 4 of the *ASM Handbook.*

Forging of Nickel-Base Alloys

Revised by H.H. Ruble, Inco Alloys International and S.L. Semiatin, Battelle Columbus Division

Introduction

NICKEL-BASE ALLOYS are often closed die forged into turbine blades, turbine disks, exhaust valves, chain hooks, heat exchanger headers, valve bodies, and pump bodies. Shafts and seamless rings are made by open-die forging. Seamless rings are also made by ring rolling.

Most nickel-base alloys (Table 1) are stronger and stiffer than steel. Alloy 200 (UNS N02200) and alloy 400 (UNS N04400), however, are softer than many steels. As an indication of the relative resistance to hot deformation, Table 2 lists the pressures developed in the roll gap at 20% reduction in hot rolling for five nickel-base alloys and two steels at four hot-working temperatures. Higher pressures indicate greater resistance. Sufficiently powerful equipment is of particular importance when forging alloys 800 (UNS N08800), 600 (UNS N06600), 625 (UNS N06625), and the precipitation-hardenable alloys such as 718 (UNS N07718) and X-750 (UNS N07750). These alloys were specifically developed to resist deformation at elevated temperatures.

Table 1 Nominal compositions of some nickel-base high-temperature alloys

Alloy	Composition, %[a]										
	C	**Cr**	**Mo**	**Al**	**Ti**	**Co**	**Fe**	**B**	**Mn**	**Si**	Other
200	0.08	. . .	. . .	. . .	. . .	[c]	0.4[b]	. . .	0.18	0.35[b]	**. . .**
201	0.01	. . .	. . .	. . .	. . .	[c]	0.4[b]	. . .	0.18	0.35[b]	**. . .**
301	0.15	. . .	. . .	4.38	0.63	[c]	0.30	. . .	0.25	0.5	**. . .**
400	0.15	. . .	. . .	. . .	. . .	[c]	1.25	. . .	1.0	0.25	**. . .**
K-500	0.13	. . .	. . .	3.00	0.63	. . .	1.00	. . .	0.75	0.5	**. . .**
625	0.05	21.5	9.0	0.2	0.2	1.0[b]	2.5	. . .	0.25	0.25	**3.65 Nb + Ta**
702	0.05	15.5	. . .	3.25	0.63	. . .	1.0	. . .	0.50	0.35	**. . .**
721	0.04	16.0	. . .	. . .	3.05	. . .	4.0	. . .	2.25	0.08	**. . .**
722	0.04	15.5	. . .	0.70	2.38	. . .	7.0	. . .	0.50	0.35	**. . .**
751	0.05	15.5	. . .	1.20	2.30	. . .	7.00	. . .	0.5	0.25	**0.95 Nb + Ta**
800	0.05	21.0	. . .	0.38	0.38	. . .	46.0	. . .	0.75	0.50	**. . .**
801	0.05	20.5	. . .	. . .	1.13	. . .	44.5	. . .	0.75	0.50	**. . .**
802	0.35	21.0	. . .	0.58	0.75	. . .	46.0	. . .	0.75	0.38	**. . .**
804	0.25	29.5	. . .	0.30	0.60	. . .	25.4	. . .	0.75	0.38	**. . .**

825	0.03	21.5	3.0	0.10	0.90	. . .	30.0	. . .	0.50	0.25	**. . .**
B	0.05	1.0	28.0	. . .	. . .	2.5	5.5	. . .	1.0	1.0	**0.4V**
W	0.10	5.0	25.0	. . .	. . .	1.5	5.0	. . .	0.5	0.5	**0.25V**
901	0.05	13.5	6.2	0.25	2.5	1.0	34.0	Trace	0.45	0.4	**. . .**
D-979	0.04	15.0	4.0	1.0	3.0	. . .	27.0	0.01	0.4	0.4	**4.0W**
X-750	0.04	15.0	. . .	0.6	2.4	0.4	6.5	. . .	0.5	0.2	**0.85Nb**
600	0.04	15.5	. . .	. . .	. . .	. . .	8.2	. . .	0.5	0.2	**. . .**
R-235	0.10	16.0	5.5	2.0	2.5	1.9	10.0	Trace	0.25	0.5	**. . .**
C	0.08[(b)]	16.5	16.0	. . .	. . .	. . .	6.0	. . .	1.0	1.0	**4.5W**
X	0.10	22.0	9.0	. . .	. . .	1.5	18.5	. . .	0.5	0.5	**0.6W**
718	0.04	19.0	3.0	0.6	0.8	. . .	18.0	. . .	0.2	0.2	**5.2Nb**
Nimonic 90	0.07	19.5	. . .	1.4	2.4	18.0	. . .	. . .	0.5	0.7	**. . .**
Nimonic 115	0.15	15.0	3.5	5.0	4.0	15.0	. . .	. . .	. . .	. . .	**. . .**
Unitemp 1753	0.25	16.5	1.5	2.0	3.2	7.5	9.5	0.008	. . .	. . .	**8.5W; 0.05Zr**
M252	0.11	19.0	9.5	1.0	2.5	10.0	2.5	0.005	0.20	0.30	**. . .**
René 41	0.09	19.0	9.6	1.5	3.2	11.0	. . .	0.005	0.01	0.02	**. . .**
Astroloy	0.06	15.5	5.3	4.5	3.6	15.5	0.2	0.030	0.05	0.3	**. . .**
Waspaloy	0.06	19.5	4.2	1.2	3.0	13.5	1.0	0.08	0.5	0.4	**0.09Zr**
U700	0.09	15.0	5.2	4.2	3.5	18.5	0.5	0.008	. . .	. . .	**. . .**
U500	0.09	19.0	4.0	2.8	3.0	17.0	2.0	0.008	. . .	. . .	**. . .**
Refractaloy 26	0.04	18.0	3.2	0.2	2.6	20.0	19.0	. . .	0.8	1.0	**. . .**

700	0.12	15.0	3.8	3.0	2.2	28.5	0.7	. . .	0.1	0.3	**. . .**
MAR-M 421	0.15	15.5	1.75	4.25	1.75	10.0	1.0	0.015	0.20[(b)]	0.20[(b)]	**3.5W; 1.75Nb; 0.05Zr**
Pyromet 860	0.05	12.6	6.0	1.25	3.0	4.0	. . .	0.010	0.05	0.05	**. . .**
Unitemp AF2-1DA	0.35	12.0	3.0	4.6	3.0	10.0	0.50[(b)]	0.015	0.10	0.10	**60W; 1.5Ta; 3.0Nb; 0.10Zr**
IN-100	0.15	10.0	3.0	5.5	5.0	15.0	. . .	0.015	. . .	. . .	**1.0V; 0.06Zr**
U710	0.07	18.0	3.0	2.5	5.0	15.0	0.5	0.02	0.10[(b)]	0.20[(b)]	**1.5V**
René 95	0.15	14.0	3.5	3.5	2.5	8.0	. . .	0.01	0.15[(b)]	0.20	**3.5Nb; 3.5W; 0.05Zr**
706	0.06[(b)]	16.0	. . .	0.4[(b)]	1.8	1.0[(b)]	. . .	0.006[(b)]	0.35[(b)]	0.35[(b)]	**. . .**
FA375	0.17	10.0	2.5	. . .	. . .	10.0	. . .	0.02	. . .	. . .	**4.0W**
617	**0.07**	**22.0**	**9.0**	**1.0**	**. . .**	**12.5**	**. . .**	**. . .**	**. . .**	**. . .**	**. . .**

(a) All compositions include balance nickel.

(b) Maximum.

(c) For these alloys, a balance of alloying is specified as nickel and cobalt.

Table 2 Hot-forming pressures for several nickel-base alloys

Pressures developed in the hot forming of 1020 steel and AISI type 302 stainless steel are shown for comparison.

Alloy	**UNS No.**	Pressure developed at working temperature[(a)]							
		870 °C (1800 °F)		**1040 °C (1900 °F)**		**1095 °C (2000 °F)**		1150 °C (2100 °F)	
		MPa	**ksi**	**MPa**	**ksi**	**MPa**	**ksi**	**MPa**	ksi
400	N04400	124	18	106	15.3	83	12	68	**9.8**
600	N06600	281	40.8	239	34.6	195	28.3	154	**22.3**
625	N06625	463	67.2	379	55	297	43	214	**31**

718	N07718	437	63.3	385	55.8	333	48.3	283	**41**
X-750	N07750	335	48.6	299	43.3	265	38.4	230	**33.3**
1020 steel	G10200	154	22.4	126	18.3	99	14.3	71	**10.3**
Type 302 stainless steel	**S30200**	**192**	**27.8**	**168**	**24.3**	**148**	**21.4**	**124**	**18**

(a) Pressure developed in the roll gap at 20% reduction in hot rolling

Forging of Nickel-Base Alloys

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Die Materials and Lubrication

The die materials used to forge nickel-base alloys are similar to those used for stainless steel (see the articles "Forging of Stainless Steel," and "Dies and Die Materials for Hot Forging" in this Volume). The service lives of alloy steel dies used in forging nickel alloys usually range from 3000 to 10,000 pieces.

Dies can be lubricated to facilitate removal of the workpiece after forging. Sulfur-free lubricants are necessary; those made with colloidal graphite give good results.

Lubricants can be applied by swabbing or spraying. Spraying is preferred because it produces more uniform coverage.

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Heating for Forging

Nickel-base alloy billets can be induction heated or furnace heated before hot forging. Regardless of the heating method used, the material must be cleaned of all foreign substances. Although nickel-base alloys have greater resistance to scaling at hot-working temperatures than steels, they are more susceptible to attack by sulfur during heating. Exposure of hot metal to sulfur must be avoided. Marking paints and crayons, die lubricants, pickling liquids, and slag and cinder that accumulate on furnace hearths are all possible sources of sulfur and should be removed from the metal before heating. Metal surfaces that have been attacked by sulfur at high temperatures have a distinctly burned appearance. If the attack is severe, the material is mechanically weakened and rendered useless.

If furnace heating is used, nickel-base alloy forging preforms should be supported on metal rails or by other means in order to avoid contamination. The metal should not touch the furnace bottom or sides. Protection against spalls from the roof may also be necessary.

Fuels. Many standard fuels are suitable for the furnace heating of nickel-base alloys. An important requirement is that they be of low sulfur content.

Gaseous fuels such as natural gas, manufactured gas, butane, and propane are the best fuels and should always be used if available. They must not contain more than 2 g (30 grains) of total sulfur per 2.8 m^3 (100 ft^3) of gas and preferably not more than 1 g (15 grains) of total sulfur per 2.8 m^3 (100 ft^3) of gas.

Oil is a satisfactory fuel provided it has a low sulfur content. Oil containing more than 0.5% sulfur should not be used. Coal and coke are generally unsatisfactory, because of the difficulty in providing for proper heating conditions, inflexibility in heat control, and excessive sulfur content.

The furnace atmosphere should be sulfur free and should be continuously maintained in a slightly reducing condition, with 2% or more carbon monoxide. The atmosphere should not be permitted to alternate from reducing to oxidizing. The slightly reducing condition is obtained by reducing the air supply until there is a tendency to smoke, which indicates an excess of fuel and a reducing atmosphere. The air supply should then be increased slightly to produce a hazy atmosphere or a soft flame. Excessive amounts of carbon monoxide or free carbon are not harmful; nickel-base alloys, unlike steels, will not carburize under these conditions. However, a slight excess of fuel over air is all that is required, and the closer the atmosphere is to the neutral condition, the easier it is to maintain the required temperature. The true condition of the atmosphere is determined by analyzing gas samples taken at various points near the metal surface.

It is important that combustion take place before the mixture of fuel and air contacts the work, or the metal may be embrittled. Proper combustion is ensured by providing ample space to burn the fuel completely before the hot gases enter the furnace chamber.

General Guidelines for the Breakdown of Nickel-Base Alloys (Ref 1). Because of their high alloy content and generally narrow working temperature range, nickel-base alloys must be converted from cast ingots with care. Initial breakdown operations are generally conducted well above the γ' solvus temperature, with subsequent deformation completed below it but still high enough to avoid excessive warm working and an unrecrystallized microstructure. The original cast structure must be completely refined during breakdown, that is, before final forging, particularly when substantial levels of reduction are not imposed during closed-die forging.

Good heat retention practice during ingot breakdown is an important factor in obtaining a desirable billet microstructure. Rapid transfer of the ingot from the furnace to the forging press, as well as the use of such techniques as reheating during breakdown, is necessary to promote sufficient recrystallization during each forging pass. In addition, it has been found that diffusion of precipitation-hardening elements is associated with recrystallization during ingot conversion. Mechanical factors such as cycling speed (which affects heat losses), reduction, length of pass, die design, and press capacity all influence the degree of work penetration through the billet cross section and therefore the rate of ingot conversion.

General Guidelines for the Finish Forging of Nickel-Base Alloys. Figure 1 shows the temperature ranges for the safe forging of 12 nickel-base alloys. Use of the lower part of the temperature range may be required for the development of specific mechanical properties.

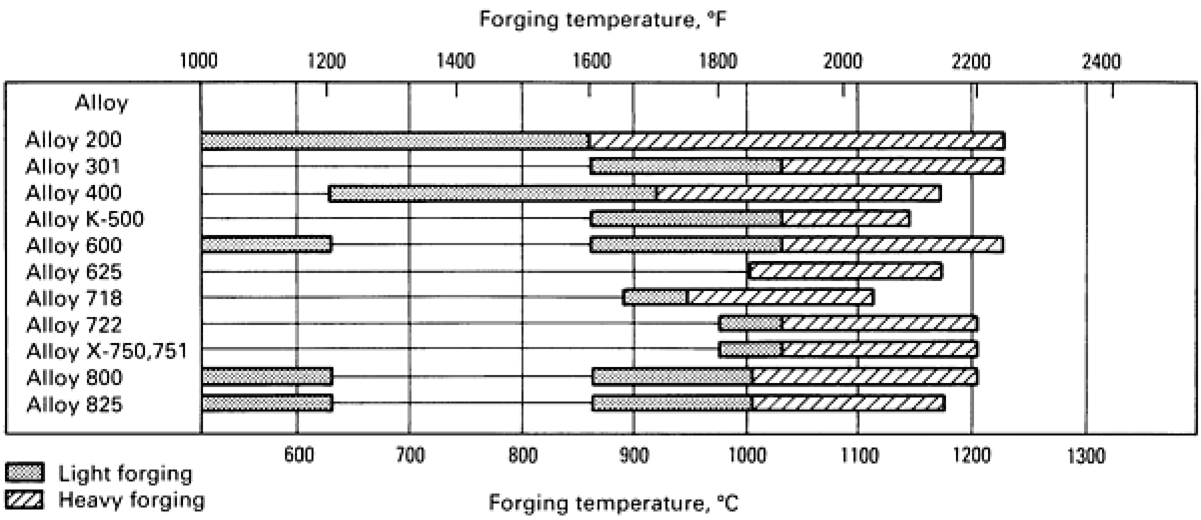

Fig. 1 Forging temperature ranges for 12 nickel-base alloys

Closed-die forging of nickel-base alloys is generally done below the γ' solvus temperature in order to avoid excessive grain growth. Approximately 80% of the reduction is scheduled in the recrystallization temperature range, with the remaining 20% done at lower temperatures to introduce a certain amount of warm work for improved mechanical properties. Preheating of all tools and dies to about 260 °C (500 °F) is recommended to avoid chilling the metal during working.

Forging Rate. A very rapid rate of forging often causes heat buildup (due to friction and deformation heating), a nonuniform recrystallized grain size, and mechanical property variations. Susceptibility to free surface ruptures also increases with forging rate (and forging temperature). Therefore, slow strain rates are typically used during the initial closed-die reductions of such alloys as Astroloy (UNS N13017) and René 95 (Ni-14Cr-8Co-3.5Mo-3.5W-3.5Nb-3.5Al-2.5Ti). With proper selection of starting stock and forging temperature, however, the forging rate is less critical. For example, some Astroloy turbine components are currently hammer forged.

Forging Reduction. A sufficient amount of recrystallization is necessary in each of a series of closed-die forging operations to achieve the desired grain size and to reduce the effects of the continuous grain-boundary or twin-boundary carbide networks that develop during heating and cooling. This condition contributes more to mechanical-property and other problems than any other single factor. Poor weldability, low-cycle fatigue, and stress rupture properties are associated with continuous grain-boundary carbide networks. Heat treatment can do very little to correct this problem without creating equally undesirable mechanical-property problems when higher solution treatment temperatures are used. All portions of a part must receive some hot work after the final heating operation in order to achieve uniform mechanical properties.

In open-die forging, a series of moderate reduction passes along the entire length of the forging is preferred. In working a square section into a round, the piece should be worked down in the square form until it approaches the final size. It should then be converted to an oversize octagon before finishing into the round. Billet corners that will be in contact with dies should be chamfered rather than left square. The work should be lifted away from the dies occasionally to permit relief of local cold areas.

Other Considerations. The precipitation-hardenable nickel alloys are subject to thermal cracking. Therefore, localized heating is not recommended. The entire part should be heated to the forging temperature.

If any ruptures appear on the surface of the metal during hot working, they must be removed at once, either by hot grinding or by cooling the work and cold overhauling. If the ruptures are not removed, they may extend into the body of the part.

For sections equal to or larger than 405 mm (16 in.) square, precautions should be taken in heating precipitation-hardenable alloys. They should be charged into a furnace at 870 °C (1600 °F) or colder and brought up to forging temperature at a controlled rate of 40 °C (100 °F) per hour.

Reference cited in this section

1. A.J. DeRidder and R. Koch, in *MiCon 78: Optimization of Processing, Properties, and Service Performance Through Microstructural Control,* H. Abram *et al.,* Ed., American Society for Testing and Materials, 1979, p 547

Cooling After Forging

The rate of cooling after forging is not critical for alloys 200, 400, and 625. Alloys K-500 (UNS N05500) and 301 (UNS N03301) should be water quenched from forging temperatures to avoid the excessive hardening and cracking that could occur if they were cooled slowly through the age-hardening range and to maintain good response to subsequent aging. Alloy 825 (UNS N08825) should be cooled at a rate equal to or faster than air cooling.

Alloys 800 and 600 are subject to carbide precipitation during heating in or slow cooling through the temperature range of 540 to 760 °C (1000 to 1400 °F). If sensitization is likely to prove disadvantageous in the end use, parts made of these alloys should be water quenched or cooled rapidly in air.

The precipitation-hardenable alloys should, in general, be cooled in air after forging. Water quenching is not recommended, because of the possibility of thermal cracking, which can occur during subsequent heating for further forging or heat treating.

Forging of Nickel-Base Alloys

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Forging Practice for Specific Alloys

The following practices are used in the forging of nickel-base alloys. Variations from these procedures may be necessary for some specialized applications (see the sections "Thermal-Mechanical Processing" and "Isothermal Forging" in this article).

Alloy 200 should be charged to a hot furnace, withdrawn as soon as the desired temperature has been reached, and worked rapidly. The recommended range of forging temperatures is 650 to 1230 °C (1200 to 2250 °F). Because the metal stiffens rapidly when cooled to about 870 °C (1650 °F), all heavy work and hot bending should be done above that temperature. High mechanical properties can be produced by working lightly below 650 °C (1200 °F). The best range for hot bending is 870 to 1230 °C (1600 to 2250 °F).

Alloy 301. The optimum temperature range for the forging of alloy 301 is 1065 to 1230 °C (1900 to 2250 °F). Light finishing work can be done down to 870 °C (1600 °F). Finer grain size is produced in forgings by using 1175 °C (2150 °F) for the final reheat temperature and by taking at least 30% reduction of area in the last forging operation.

After hot working, the alloy should be quenched from a temperature of 790 °C (1450 °F) or above. Quenching retains the strain hardening imparted by the forging operation and produces better response to subsequent age hardening. Quenching in water containing about 2 vol% alcohol results in less surface oxidation.

Material that must be cooled prior to subsequent hot working should also be quenched. Slow cooling may cause age hardening, which sets up stresses in the workpiece that can cause cracking during subsequent reheating.

Alloy 400. The maximum heating temperature for forging alloy 400 is 1175 °C (2150 °F). Prolonged soaking at the working temperature is detrimental. If a delay occurs during processing, the furnace temperature should be reduced to 1040 °C (1900 °F) and not brought to 1175 °C (2150 °F) until operations are resumed.

The recommended metal temperature for heavy reductions is 925 to 1175 °C (1700 to 2150 °F). Light reductions may be taken at temperatures down to 650 °C (1200 °F). Working at the lower temperatures produces higher mechanical properties and smaller grain size.

A controlled forging procedure is necessary to meet the requirements of some specifications for forged hot-finished parts. Both the amount of reduction and the finishing temperature must be controlled in order to develop the desired properties.

One procedure for producing forgings to such specifications consists of taking a 30 to 35% reduction after the final reheat. This is done as follows:

- Reheat
- Forge to a section having about 5% larger area than the final shape (take at least 25% reduction)
- Cool to 705 °C (1300 °F)
- Finish to size (5% reduction)

High-tensile forgings, as described in certain military specifications, also require a minimum of 30 to 35% reduction after the last reheat. This is taken in the following manner:

- Reheat
- Forge to a section having an area about 25% larger than the final shape (take about 5% reduction)
- Cool to 705 °C (1300 °F)
- Finish to size (25% reduction)

Grain refinement is achieved by using a temperature of 1095 °C (2000 °F) for the final reheat and by increasing the amount of reduction taken after the last reheat.

Alloy K-500. The maximum recommended heating temperature for the forging of alloy K-500 is 1150 °C (2100 °F). Metal should be charged into a hot furnace and withdrawn when uniformly heated. Prolonged soaking at this temperature is harmful. If a delay occurs such that the material would be subject to prolonged soaking, the temperature should be reduced to or held at 1040 °C (1900 °F) until shortly before working is to begin, then brought to 1150 °C (2100 °F). When the piece is uniformly heated, it should be withdrawn. In the event of a long delay, the work should be removed from the furnace and water quenched.

The forging temperature range is 870 to 1150 °C (1600 to 2100 °F). Heavy work is best done between 1040 and 1150 °C (1900 and 2100 °F), and working below 870 °C (1600 °F) is not recommended. To produce finer grain in forgings, 1095 °C (2000 °F) should be used for the final reheat temperature, and at least 30% reduction of area should be taken in the last forging operation.

When forging has been completed or when it is necessary to allow alloy K-500 to cool before further hot working, it should not be allowed to cool in air, but should be quenched from a temperature of 790 °C (1450 °F) or higher. If the piece is allowed to cool slowly, it will age harden to some extent, and stress will be set up that may lead to thermal splitting or tearing during subsequent reheating. In addition, quenched material has better response to age hardening because more of the age-hardening constituent is retained in solution.

Alloy 600. The normal forging temperature range for alloy 600 is 870 to 1230 °C (1600 to 2250 °F). Heavy hot work should be done in the range from 1040 to 1230 °C (1900 to 2250 °F). Light working can be continued down to 870 °C (1600 °F). Generally, forging should not be done between 650 and 870 °C (1200 and 1600 °F) because of the low ductility of the alloy in this temperature range. Judicious working at a temperature below 650 °C (1200 °F) will develop higher tensile properties.

The rate of cooling after forging is not critical with respect to thermal cracking. However, alloy 600 is subject to carbide precipitation in the range between 540 and 760 °C (1000 and 1400 °F), and if subsequent use dictates freedom from sensitization, the part should be rapidly cooled through this temperature range.

Alloy 625 should be heated in a furnace held at 1175 °C (2150 °F) but no higher. The work should be brought as close to this temperature as conditions permit. Forging is done from this temperature down to 1010 °C (1850 °F); below 1010 °C (1850 °F) the metal is stiff and hard to move, and attempts to forge it may cause hammer splits at the colder areas. The work should be returned to the furnace and reheated to 1175 °C (2150 °F) whenever its temperature drops below 1010 °C

(1850 °F). To guard against duplex grain structure, the work should be given uniform reductions. For open-die work, final reductions of a minimum of 20% are recommended.

Alloy 718 is strong and offers considerable resistance to deformation during forging. The forces required for hot deformation are somewhat higher than those employed for alloy X-750. Alloy 718 is forged in the range from 900 to 1120 °C (1650 to 2050 °F). In the last operation, the metal should be worked uniformly with a gradually decreasing temperature, finishing with some light reduction below 955 °C (1750 °F). Figure 2 shows a forged and machined alloy 718 marine propeller blade. In heating for forging, the material should be brought up to temperature, allowed to soak a short time to ensure uniformity, and withdrawn.

Fig. 2 Forged and machined alloy 718 marine propeller blade. Courtesy of Ladish Company

Alloy 718 should be given uniform reductions in order to avoid duplex grain structure. Final reductions of 20% minimum should be used for open-die work, and 10% minimum for closed-die work. Parts should generally be air cooled from the forging temperature, rather than water quenched.

Alloy 706 (UNS N09706) is similar to alloy 718, except that alloy 706 is more readily fabricated, particularly by machining. Forging should be done using the same procedures and temperatures as those for alloy 718.

Alloy X-750. The forging range for alloy X-750 is 980 to 1205 °C (1800 to 2200 °F). Below 980 °C (1800 °F), the metal is stiff and hard to move, and attempts to work it may cause splitting. All heavy forging should be done at about 1040 °C (1900 °F), and the metal should be reheated whenever it cools to below that temperature. Forgings can be finished with some light reduction in the range between 980 and 1040 °C (1800 and 1900 °F).

As a general rule, alloy X-750 should be air cooled rather than liquid quenched from the forging temperature. Liquid quenching can cause high residual stresses that may result in cracking during subsequent heating for further hot work or for heat treatment. Parts with large cross sections and pieces with variable cross sections are especially susceptible to thermal cracking during cooling. In very large cross sections, furnace cooling may be necessary to prevent thermal cracking.

Alloy 800. Hot working of alloy 800 is started at 1205 °C (2200 °F) and heavy forging is done at temperatures down to 1010 °C (1850 °F). Light working can be accomplished down to 870 °C (1600 °F). No working should be done between 870 and 650 °C (1600 and 1200 °F). As with alloy 600, thermal cracking is not a problem, and workpieces should be cooled rapidly through the range between 540 and 760 °C (1000 and 1400 °F) to ensure freedom from sensitization.

Alloy 825. The forging range for alloy 825 is 870 to 1175 °C (1600 to 2150 °F). It is imperative that some reduction be accomplished in the range between 870 and 980 °C (1600 and 1800 °F) during final forging in order to ensure maximum corrosion resistance.

Cooling after forging should be done at a rate equal to or faster than air cooling. Heavy sections may become sensitized during cooling from the forging temperature and therefore be subject to intergranular corrosion in certain media. A stabilizing anneal of 1 h at 940 °C (1725 °F) restores resistance to corrosion. If the forged piece is to be welded and used in an environment that could cause intergranular corrosion, the piece should be given a stabilizing anneal to prevent sensitization from the heat of welding, regardless of the cooling rate after forging.

Alloy 925. The hot-working characteristics of alloy 925 (UNS N09925) are similar to those of alloy 825 at temperatures to 1095 °C (2000 °F). At higher temperatures, alloy 925 has lower ductility and higher strength. The forging range is 870 to 1175 °C (1600 to 2150 °F). For maximum corrosion resistance and highest mechanical properties after direct aging, final hot working should be done in the range of 870 to 980 °C (1600 to 1800 °F).

Alloys 722 and 751 (UNS N07722 and N07751, respectively) are forged using the same procedures and temperatures as those for alloy X-750.

Alloys 903, 907, and 909 (UNS N19903, N19907, and N19909, respectively) are best forged in three stages in order to obtain the desired properties after aging. The initial breakdown of 40% minimum reduction should be performed at a temperature of 1060 to 1120 °C (1940 to 2050 °F). For intermediate forging at a minimum of 25% reduction, these alloys should be heated between 995 to 1050 °C (1825 and 1925 °F). The final heating for alloys 907 and 909 should be 980 to 1025 °C (1800 to 1875 °F) for a minimum reduction of 20% over a falling temperature range (finishing at $\leq$925 °C, or 1700 °F). The final heating for alloy 903 should be 870 °C (1600 °F) with a final forging reduction of 40% minimum. Other nickel-base heat-resistant alloys are discussed in the article "Forging of Heat-Resistant Alloys" in this Volume.

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Thermal-Mechanical Processing (TMP)

Thermal-mechanical processing refers to the control of temperature and deformation during processing to enhance specific properties. Special TMP sequences have been developed for a number of nickel-base alloys.

The design of TMP sequences relies on a knowledge of the melting and precipitation temperatures for the alloy of interest. Table 3 lists these temperatures for several nickel-base alloys. Although nickel-base (as well as iron- and cobalt-base) alloys form various carbides--for example, MC (M = titanium, niobium, etc.), M_6C (M = molybdenum and/or tungsten), or $M_{23}C_6$ (M = chromium)--the primary precipitate of concern in the processing of such materials is the γ'-strengthening precipitate. Gamma prime is an ordered face-centered cubic (fcc) compound in which aluminum and titanium combine with nickel to form Ni_3(Al, Ti). In nickel-iron alloys such as alloy 718, titanium, niobium, and to a lesser extent, aluminum combine with nickel to form ordered fcc γ' or ordered body-centered tetragonal γ". Nickel-iron base alloys are also prone to the formation of other phases, such as hexagonal close-packed Ni_3Ti (η), as in titanium-rich alloy 901, or orthorhombic Ni_3Nb (δ) in niobium-rich alloy 718.

Table 3 Critical melting and precipitation temperatures for several nickel-base alloys

Alloy	UNS No.	First melting temperature		Precipitation temperature	
		°C	°F	°C	°F

Alloy X	N06002	1260	2300	760	**1400**
Alloy 718	N07718	1260	2300	845	**1550**
Waspaloy	N07001	1230	2250	980	**1800**
Alloy 901	N09901	1200	2200	980	**1800**
Alloy X-750	N07750	1290	2350	955	**1750**
M-252	N07252	1200	2200	1010	**1850**
Alloy R-235	. . .	1260	2300	1040	**1900**
René 41	N07041	1230	2250	1065	**1950**
U500	N07500	1230	2250	1095	**2000**
U700	. . .	1230	2250	1120	**2050**
Astroloy	**N13017**	**1230**	**2250**	**1120**	**2050**

Source: Ref 2

Early forging practice of nickel- and nickel-iron base alloys consisted of forging from and solution heat treating at temperatures well in excess of the γ' solvus temperature. High-temperature solution treatment dissolved all of the γ', annealed the matrix, and promoted grain growth (typical grain size ≈ASTM 3 or coarser). This was followed by one or more aging treatments that promoted controlled precipitation of γ' and carbide phases. Optimal creep and stress rupture properties above 760 °C (1400 °F) were thus achieved. Later in the development of forging practice, it was found that using preheat furnace temperatures slightly above the recrystallization temperature led to the development of finer grain sizes (ASTM 5 to 6). Coupling this with modified heat-treating practices resulted in excellent combinations of tensile, fatigue, and creep properties.

State-of-the art forging practices for nickel-base alloys rely on the following microstructural effects (Ref 3):

- Dynamic recrystallization is the most important softening mechanism during hot working
- Grain boundaries are preferred nucleation sites for recrystallization
- The rate of recrystallization decreases with the temperature and/or the extent of deformation
- Precipitation that may occur during the recrystallization can inhibit the softening process. Recrystallization cannot be completed until the precipitate coarsens to a relatively ineffective morphology

Forging temperature is carefully controlled during the thermal-mechanical processing of nickel- and nickel-iron base alloys to make use of the structure control effects of second phases such as γ'. Above the optimal forging temperature range (Table 4), the structure control phase goes into solution and loses its effect. Below this range, extensive fine precipitates are formed, and the alloy becomes too stiff to process. Several examples of specific TMP sequences are given below.

Table 4 Structure control phases and working temperature ranges for various heat-resistant alloys

Alloy	**UNS No.**	**Phases for structure control**	Working temperature range	
			°C	°F
Nickel-base alloys				
Waspaloy	N07001	γ' (Ni^3(Al,Ti)	955-1025	**1750-1875**
Astroloy	N13017	γ' (Ni^3(Al,Ti)	1010-1120	**1850-2050**
IN-100	. . .	γ' (Ni^3(Al,Ti)	1040-1175	**1900-2150**
René 95	. . .	γ' (Ni^3(Al,Ti)	1025-1135	**1875-2075**
Nickel-iron-base alloys				
901	N09901	η(Ni^3Ti)	940-995	**1725-1825**
718	N07718	δ(Ni^3Nb)	915-995	**1675-1825**
Pyromet CTX-1	**. . .**	**η(Ni^3Ti), δ(Ni^3Nb), or both**	**855-915**	**1575-1675**

Waspaloy. A typical TMP treatment of nickel-base alloys is that used for Waspaloy (UNS N07001) to obtain good tensile and creep properties. This consists of initial forging at 1120 °C (2050 °F) and finish forging below approximately 1010 °C (1850 °F) to produce a fine, equiaxed grain size of ASTM 5 to 6. Solution treatment is then done at 1010 °C (1850 °F), and aging is conducted at 845 °C (1550 °F) for 4 h, followed by air cooling plus 760 °C (1400 °F) for 16 h and then air cooling.

René 95. Initial forging of René 95 is done at a temperature between 1095 and 1140 °C (2000 and 2080 °F). Following an in-process recrystallization anneal at 1175 °C (2150 °F), finish forging (reduction ≈40 to 50%) is then imposed below the γ' solvus, typically at temperatures between 1080 and 1105 °C (1975 and 2025 °F). The large grains formed during high-temperature recrystallization are elongated and surrounded by small recrystallized grains that form during finish forging.

Alloy 901. The thermal-mechanical processing of alloy 901 is often done to produce a fine-grain structure that enhances fatigue strength (Ref 5). This is accomplished by using the η(Ni_3Ti) phase, which is introduced in a Widmanstdätten form at the beginning of processing by a heat treatment at 900 °C (1650 °F) for 8 h. Forging is then conducted at 955 °C (1750 °F), which is below the η solvus; the forging deformation is completed below the recrystallization temperature. A fine-grain structure is generated by a subsequent recrystallization treatment below the η solvus. The needle-like η phase will become spherical during forging and will restrict grain growth. Aging is then conducted according to standard procedures.

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Isothermal Forging

Nickel-base alloys that are hard to work or are typically used in the cast condition can be readily forged when in a powder-consolidated form. The most common forging technique using powder preforms is isothermal forging; (see the article "Isothermal and Hot-Die Forging" in this Volume). In this process, powder is produced by inert gas atomization and is compacted into billet form by extrusion. The billets are fabricated below the γ' solvus temperature for alloys such as IN-100 in order to maintain a fine grain size and a fine distribution of precipitates. In this condition, the material exhibits superplastic properties that are characterized by large tensile elongations (during sheet forming) and good die-filling capacity (during forging). Multiples of the extruded bar are then isothermally forged into a variety of complex turbine engine and other high-temperature parts.

The key to successful isothermal forging of nickel-base alloys is the ability to develop a fine grain size before forging and to maintain it during forging. With regard to the latter, a high volume percentage of second phase is useful in preventing grain growth. Therefore, alloys such as IN-100, René 95, and Astroloy, which contain large amounts of γ', are readily capable of developing the superplastic properties necessary in isothermal forging. In contrast, Waspaloy, which contains less than 25 vol% γ' at isothermal forging temperatures, is only marginally superplastic. Nickel-iron base alloys such as alloys 718 and 901 have even lower volume fractions of precipitate and are therefore even less frequently used in isothermal forging.

As the term implies, isothermal forging consists of forging with the workpiece and the dies at the same temperature. Because this temperature is often of the order of 980 to 1095 °C (1800 to 2000 °F), the dies are usually made of molybdenum for elevated-temperature strength. The isothermal forging system must be operated in a vacuum or inert atmosphere in order to protect such die materials from oxidation.

Compared to conventional forging, isothermal forging deformation rates are slow; hydraulic press speeds of approximately 2.5 mm/min (0.1 in./min) are typical. However, the slower production rate is largely offset by the ability to forge complex shapes to closer tolerances, which leads to less machining and substantial material savings. In addition, a large amount of deformation is accomplished in one operation, pressures are low, and uniform microstructures are achieved. For example, the as-forged weight of a finish-machined 68 kg (150 lb) Astroloy disk is about 110 kg (245 lb) for a conventional forging versus 72 kg (160 lb) for the corresponding isothermal forging.

Forging of Nickel-Base Alloys

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Forging of Titanium Alloys

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Introduction

TITANIUM ALLOYS are forged into a variety of shapes and types of forgings, with a broad range of final part forging design criteria based on the intended application. As a class of materials, titanium alloys are among the most difficult metal alloys to forge, ranking behind only refractory metals and nickel/cobalt-base superalloys. Therefore, titanium alloy forgings, particularly closed-die forgings, are typically produced to less highly refined final forging configurations than are typical of aluminum alloys (although precision forgings in titanium alloys are produced to the same design and tolerance criteria as aluminum alloys; see the section "Titanium Alloy Precision Forgings" in this article) and to equivalent or more refined forging design sophistication than carbon or low-alloy steel forgings, because of reduced oxidation or scaling tendencies in heating. Because of the high cost of titanium alloys in comparison to other commonly forged materials, such as aluminum and alloy steels, final forging design criteria in titanium closed-die forgings are typically balanced between producibility demands and cost considerations (particularly machining costs and overall metal recovery).

In addition, the working history and forging parameters used in titanium alloy forging have a significant impact on the final microstructure (and therefore the resultant mechanical properties) of the forged alloy--perhaps to a greater extent than in any other commonly forged material. Therefore, the forging process in titanium alloys is used not only to create cost-effective forging shapes but also, in combination with thermal treatments, to create unique and/or tailored microstructures to achieve the desired final mechanical properties through thermomechanical processing (TMP) techniques. For a given titanium alloy forging shape, the pressure requirements in forging vary over a large range, depending primarily on the chemical composition of the alloy, the forging process being used, the forging strain rate, the type of forging being manufactured, lubrication conditions, and die temperature. The chemical compositions, characteristics, and typical mechanical properties of all wrought titanium alloys referred to in this article are reviewed in the article "Wrought Titanium and Titanium Alloys" in *Properties and Selection: Nonferrous Alloys and Special-Purpose Materials*, Volume 2 of the *ASM Handbook.*

Forging of Titanium Alloys

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Titanium Alloy Classes

Because of the strong relationship among the required forging process parameters, deformation behavior, and mechanical properties of the various titanium alloys, it is necessary to review the classes of titanium alloys that are forged and their typical thermomechanical processing requirements, which exert a strong influence on forging part design and forging process selection. Titanium and its alloys exist in two allotropic forms:

- The hexagonal close-packed (hcp) α phase
- The body-centered cubic (bcc) β phase

The more difficult to deform α phase is usually present at low temperatures, while the more easily deformed β phase is present at high temperatures. However, the addition of various alloying elements (including other metals and such gases as oxygen, nitrogen, and hydrogen) stabilizes either the α or β phase. The temperature at which a given titanium alloy transforms completely from α to β is termed the beta transus, β_t, and is a critical temperature in titanium alloy forging process criteria.

Titanium alloys are divided into three major classes, based on the predominant allotropic form(s) present at room temperature:

- α/near-α alloys
- α-β alloys
- β/metastable β alloys

Each of these types of titanium alloys has unique forging process criteria and deformation behavior. Further, the forging process parameters, often in combination with subsequent thermal treatments, are manipulated for each alloy type to achieve the desired final forging microstructure and mechanical properties (heat treatment serves a different purpose in titanium alloys from that in aluminum alloys or alloy steels, as discussed below). Table 1 lists most of the commonly forged titanium alloys by alloy class, along with the major alloying elements constituting each alloy.

Table 1 Recommended forging temperature ranges for commonly forged titanium alloys

Alloy	**β_t**		**Process**[a]	Forging temperature[b]	
	°C	**°F**		**°C**	°F
α/near-α alloys					
Ti-C.P.[c]	915	1675	C	815-900	**1500-1650**
Ti-5Al-2.5Sn[c]	1050	1925	C	900-1010	**1650-1850**
Ti-5Al-6Sn-2Zr-1Mo-0.1Si	1010	1850	C	900-995	**1650-1925**
Ti-6Al-2Nb-1Ta-0.8Mo	1015	1860	C B	940-1050 1040-1120	**1725-1825** **1900-2050**
Ti-6Al-2Sn-4Zr-2Mo(+0.2Si)[d]	990	1815	C B	900-975 1010-1065	**1650-1790** **1850-1950**
Ti-8Al-1Mo-1V	1040	1900	C	900-1020	**1650-1870**
IMI 685 (Ti-6Al-5Zr-0.5Mo-0.25Si)[e]	1030	1885	C/B	980-1050	**1795-1925**

IMI 829 (Ti-5.5Al-3.5Sn-3Zr-1Nb-0.25Mo-0.3Si)(e)	1015	1860	C/B	980-1050	**1795-1925**
IMI 834 (Ti-5.5Al-4.5Sn-4Zr-0.7Nb-0.5Mo-0.4Si-0.06C)(e)	1010	1850	C/B	980-1050	**1795-1925**
α-β alloys					
Ti-6Al-4V(c)	995	1825	C B	900-980 1010-1065	**1650-1800** **1850-1950**
Ti-6Al-4V ELI	975	1790	C B	870-950 990-1045	**1600-1740** **1815-1915**
Ti-6Al-6V-2Sn	945	1735	C	845-915	**1550-1675**
Ti-6Al-2Sn-4Zr-6Mo	940	1720	C B	845-915 955-1010	**1550-1675** **1750-1850**
Ti-6Al-2Sn-2Zr-2Mo-2Cr	980	1795	C	870-955	**1600-1750**
Ti-17 (Ti-5Al-2Sn-2Zr-4Cr-4Mo(f)	885	1625	C B	805-865 900-970	**1480-1590** **1650-1775**
Corona 5 (Ti-4.5Al-5Mo-1.5Cr)	925	1700	C B	845-915 955-1010	**1550-1675** **1750-1850**
IMI 550 (Ti-4Al-4Mo-2Sn)	990	1810	C	900-970	**1650-1775**
IMI 679 (Ti-2Al-11Sn-4Zr-1Mo-0.25Si)	945	1730	C	870-925	**1600-1700**
IMI 700 (Ti-6Al-5Zr-4Mo-1Cu-0.2Si)	1015	1860	C	800-900	**1470-1650**
β/near-β/metastable βalloys					
Ti-8Al-8V-2Fe-3Al	775	1425	C/B	705-980	**1300-1800**
Ti-10V-2Fe-3Al	805	1480	C B	705-785 815-870	**1300-1450** **1500-1600**
Ti-13V-11Cr-3Al	675	1250	C/B	650-955	**1200-1750**
Ti-15V-3Cr-3Al-3Sn	770	1415	C/B	705-925	**1300-1700**

Beta C (Ti-3Al-8V-6Cr-4Mo-4Zr)	795	1460	C/B	705-980	**1300-1800**
Beta III (Ti-4.5Sn-6Zr-11.5Mo)	745	1375	C/B	705-955	**1300-1750**
Transage 129 (Ti-2Al-11.5V-2Sn-11Zr)	720	1325	C/B	650-870	**1200-1600**
Transage 175 (Ti-2.7Al-13V-7Sn-2Zr)	**760**	**1410**	**C/B**	**705-925**	**1300-1700**

(a) C, conventional forging processes in which most or all of the forging work is accomplished below the β_t of the alloy for the purposes of desired mechanical property development. This forging method is also referred to as α-β forging. B, βforging processes in which some or all of the forging is conducted above the β_t of the alloy to improve hot workability or to obtain desired mechanical property combinations. C/B, either forging methodology (conventional or β) is employed in the fabrication of forgings or for alloys, such as β alloys, that are predominately forged above their β_t but may be finish forged at subtransus temperatures.

(b) These are recommended metal temperature ranges for conventional α-β, or β forging processes for alloys for which the latter techniques are reported to have been employed. The lower limit of the forging temperature range is established for open-die forging operations in which reheating is recommended.

(c) Alloys for which there are several compositional variations (primarily oxygen or other interstitial element contents) that may affect both β_t and forging temperature ranges.

(d) This alloy is forged and used both with and without the silicon addition; however, the β_t and recommended forging temperatures are essentially the same.

(e) Alloys designed to be predominately β forged.

(f) Ti-17 has been classified as an α-β and as a near-β titanium alloy. For purposes of this article, it is classified as an α-β alloy.

Alpha/Near-Alpha Alloys. Alpha titanium alloys contain elements that stabilize the hcp α phase at higher temperatures. These alloys (with the exception of commercially pure titanium, which is also an α alloy) are among the most difficult titanium alloys to forge. Typically, α/near-α titanium alloys have modest strength but excellent elevated-temperature properties. Forging and TMP processes for αalloys are typically designed to develop optimal elevated-temperature properties, such as strength and creep resistance. The β_t of α/near-α alloys typically ranges from 900 to 1065 °C (1650 to 1950 °F).

Alpha-Beta Alloys. Alpha-beta titanium alloys represent the most widely used class of titanium alloys (with Ti-6Al-4V being the most widely used of all titanium alloys) and contain sufficient β stabilizers to stabilize some of the β phase at room temperature. Alpha-beta titanium alloys are generally more readily forged than α alloys and are more difficult to forge than some β alloys. Typically, α-β alloys have intermediate-to-high strength with excellent fracture toughness and other fracture-related properties. Forging and TMP processes for α-β alloys are designed to develop optimal combinations of strength, fracture toughness, and fatigue characteristics. The β_t of α-β alloys typically ranges from 870 to 1010 °C (1600 to 1850 °F).

Beta/Metastable Beta Alloys. Beta alloys are those alloys with sufficient β stabilizers that the bcc β phase is the predominant allotropic form present at room temperature. Beta titanium alloys are usually easier to fabricate than other classes of titanium alloys, although β alloys may be equivalent to, or more difficult to forge than α-β alloys under certain forging conditions. Beta titanium alloys are characterized by very high strength with good fracture toughness and excellent fatigue characteristics; therefore, forging and TMP processes are designed to optimize these property combinations. The β_t of β titanium alloys ranges from 650 to 870 °C (1200 to 1600 °F).

Forging of Titanium Alloys

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Forgeability

Titanium alloys are considerably more difficult to forge than aluminum alloys and alloy steels, particularly with conventional forging techniques, which use nonisothermal die temperatures of 535 °C (1000 °F) or less and moderate strain rates (hot-die and isothermal forging of titanium alloys are discussed in depth in the article "Isothermal and Hot-Die Forging" in this Volume). Figure 1 compares the flow stresses of several commonly forged titanium alloys at strain rate of 10/s with the flow stress of 4340 alloy steel at a strain rate of 27/s. In Fig. 1, commercially pure titanium and Ti-8Al-1Mo-1V are α alloys, Ti-6Al-4V and Ti-6Al-6V-2Sn are α-β alloys, and Ti-13V-11Cr-3Al and Ti-10V-2Fe-3Al are β alloys.

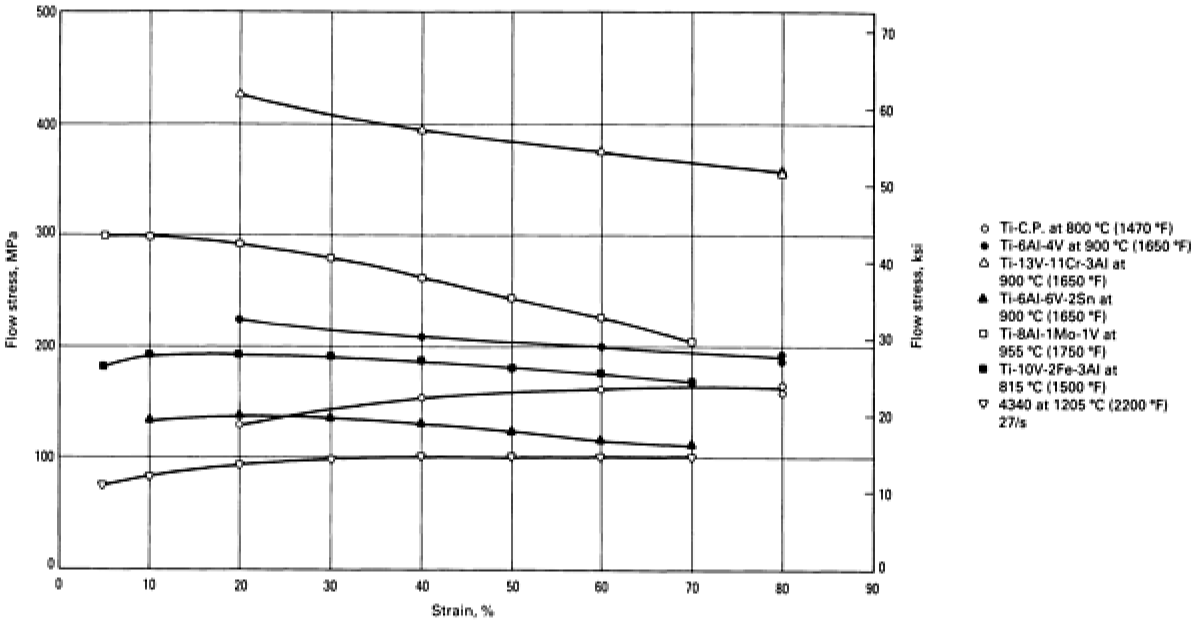


Fig. 1 Flow stress of commonly forged titanium alloys at 10/s strain rate compared to 4340 alloy steel at 27/s strain rate.

At this rapid strain rate (representative of a strain rate typical of a mechanical press or other rapid strain rate forging equipment), the β alloy Ti-13V-11Cr-3Al has the highest flow stress even at a temperature well above the β_t of the alloy; at rapid strain rates, very highly alloyed titanium alloys retard dislocation glide and other mechanisms that hasten deformation behavior. The α alloy Ti-8Al-1Mo-1V has the next highest flow stress and is typical of this class of titanium alloys. The α-β alloys Ti-6Al-4V and Ti-6Al-6V-2Sn have intermediate flow stresses at temperatures below their β_t, with the more highly βstabilized Ti-6Al-6V-2Sn having lower flow stresses than Ti-6Al-4V. Commercially pure titanium flow stress for the noted strain rate and sub-β_t temperature is similar to that for the α-β alloys. Finally, at a temperature slightly above its β_t, the metastable β alloy Ti-10V-2Fe-3Al has flow stresses lower than those of the α-β alloy Ti-6Al-4V. The flow stresses of all of the noted titanium alloys exceed that of the alloy steel 4340--in some cases by four to five times.

Effect of Temperature. The deformation characteristics of all classes of titanium alloys are very sensitive to metal temperature during deformation processes such as forging. This effect is illustrated in Fig. 2 for three alloys, each representative of one class of titanium alloy. For each of these alloys, forging pressure increases dramatically with relatively small changes in metal temperatures. For example, the forging pressure for the α alloy Ti-8Al-1Mo-1V increases nearly three times as the metal temperature decreases by approximately 95 °C (200 °F). Therefore, it is important in forging titanium alloys to minimize metal temperature losses in the transfer of heated pieces from furnace to the forging equipment and to minimize contact with the much cooler dies during conventional forging processes.

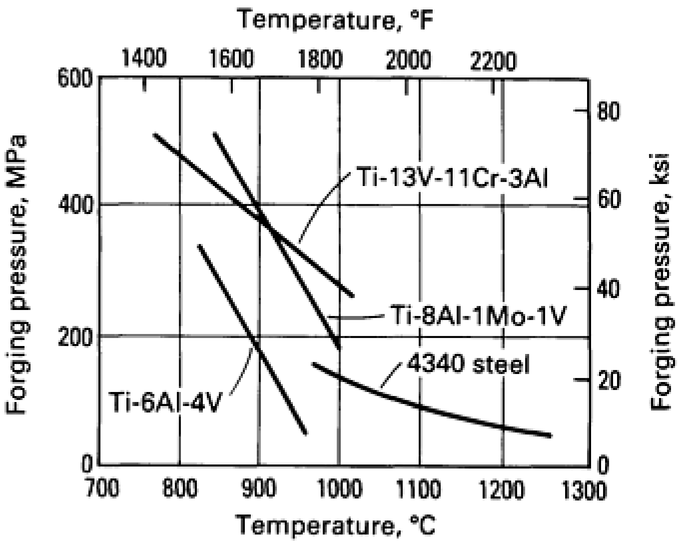


Fig. 2 Effect of forging temperature on forging pressure for three titanium alloys and 4340 alloy steel. Source: Ref 1.

The effect of temperature variations on the flow stresses of common titanium alloys does vary with alloy class. These effects are illustrated in Fig. 3(a), 3(b), and 3(c) for representative α, α-β, and β alloys, respectively. In comparing Fig. 3(a) to (c), it is evident that the more difficult-to-forge α alloys such as Ti-8Al-1Mo-1V (Fig. 3a) display the greatest sensitivity to metal temperature. For example, the flow stress at 10/s and 900 °C (1650 °F) is two to three times that of the alloy at 1010 °C (1850 °F) (the latter temperature is below the β_t of the alloy). In Fig. 3(b), the α-β alloy Ti-6Al-4V also displays sensitivity to metal temperature, but to a lesser extent than the α alloy Ti-8Al-1Mo-1V, especially at higher levels of total strain. In Fig. 3(b), at 1000 °C (1830 °F), Ti-6Al-4V is being deformed at or above the nominal β_t of the alloy, in which the structure is entirely bcc and considerably easier to deform. Finally, for the βalloy Ti-10V-2Fe-3Al less metal temperature sensitivity is displayed, also at higher levels of total strain. At 815 °C (1500 °F), Ti-10V-2Fe-3Al is being deformed above the β_t of the alloy, with an attendant reduction in flow stresses in comparison to sub β_t deformation at 760 °C (1400 °F). However, at this high strain rate, the flow stress reduction achieved by deforming βalloys above their β_t is less than the flow stress reduction achieved by deforming α-β alloys above their β_t.

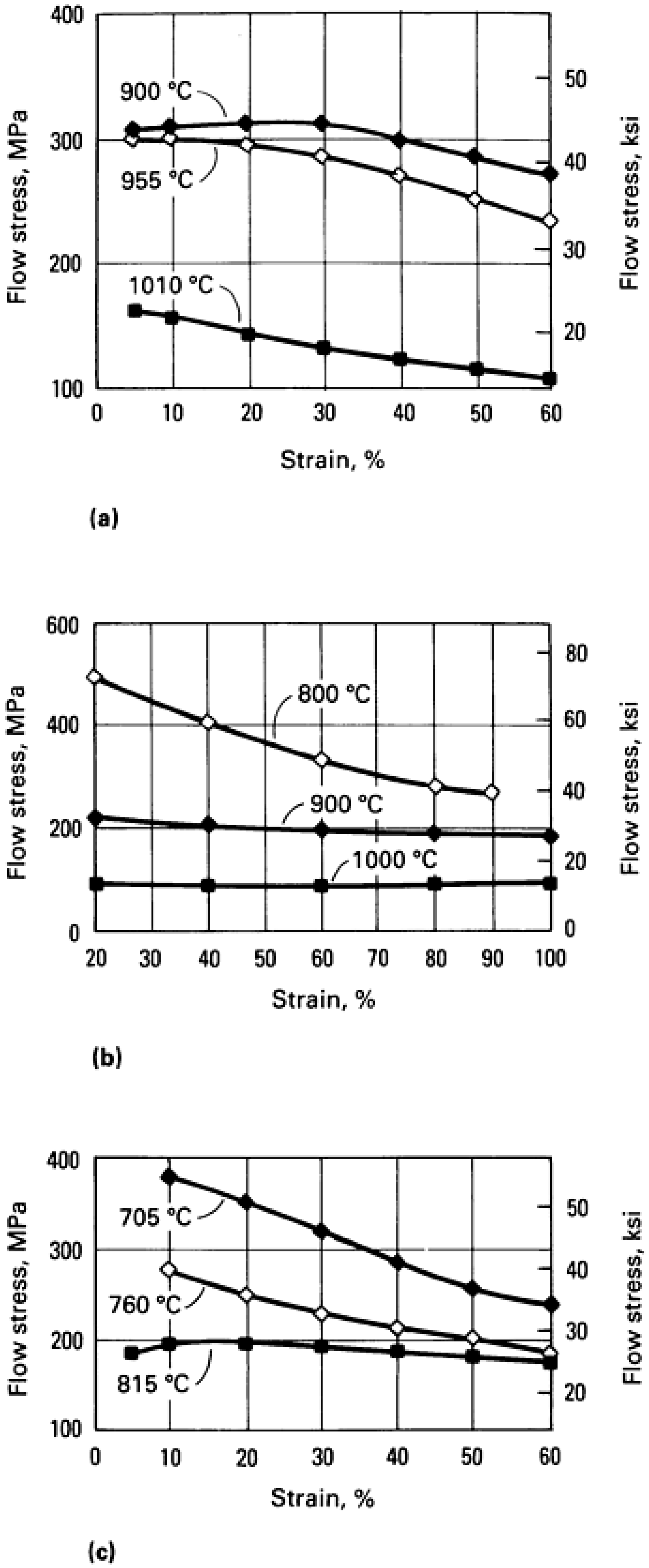


Fig. 3 Effect of forging temperature on flow stress of titanium alloys at 10/s strain rate. (a) α **alloy Ti-8Al-1Mo-1V. (b)** α-β **alloy Ti-6Al-4V. (c) Metastable** β**alloy Ti-10V-2Fe-3Al.**

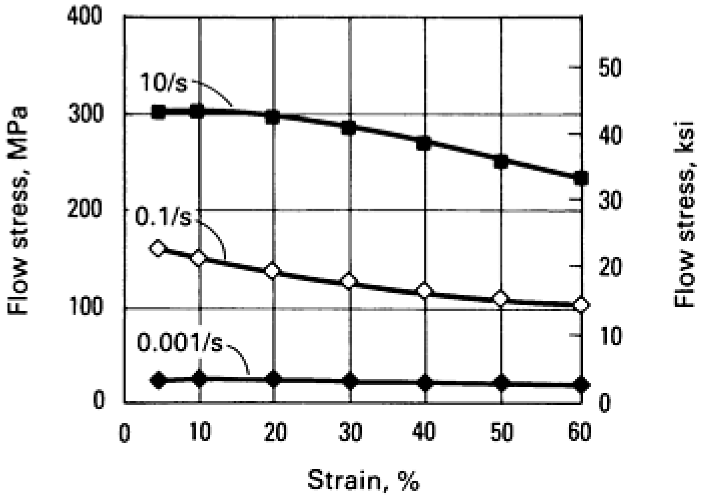
As with other forged materials, many titanium alloys display a strain-softening behavior at the strain rates typically used in conventional forging techniques. As shown in Fig. 3(a) to (c), strain softening is typically observed when such alloys are forged below their β_t and is observed to a much lesser extent when these alloys are deformed above their β_t (for example, Fig. 3b and c for Ti-6Al-4V and Ti-10V-2Fe-3Al). The differences in strain-softening behavior are a function of the differences in microstructure present during the deformation above or below the β_t of the alloy. The equiaxed α in a β matrix structure, typical of subtransus forging, has been found to redistribute strain and to promote dislocation movement more effectively than acicular α in a transformed β structure, leading to increased strain softening in the former.

Flow stresses describe the lower limit of the deformation resistance of titanium alloys as represented by ideal deformation conditions and are therefore rarely present during actual forging processes. However, flow stress information, as a function of such forging process variables as temperature and strain rate, is useful in designing titanium alloy forging processes. Because of other forging process variables, such as die temperature, lubrication, prior working history, and total strain, actual forging pressures or unit pressure requirements may significantly exceed the pure flow stress of any given alloy under similar deformation conditions.

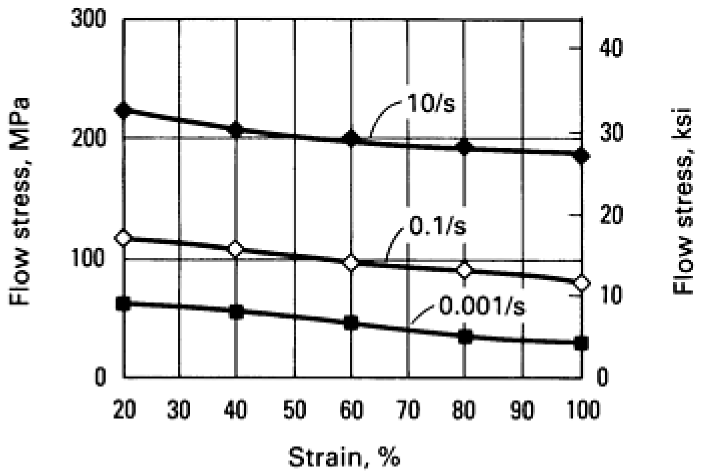
Table 1 lists recommended metal temperatures for 27 commonly forged α, α-β, and βtitanium alloys. With some exceptions, these alloys can be forged to the same degree of severity; however, the power and/or pressure requirements needed to achieve a given forging shape may vary with each individual alloy and particularly with alloy class. As a general guide, metal temperatures of β_t - 28 °C (50 °F) for α/β forging and β_t + 42 °C (75 °F) for β forging, are recommended.

Table 1 lists the recommended range of forging temperatures, with the upper limit based on prudent proximity (from furnace temperature variations and minor composition variations) to the nominal β_t of the alloy in the case of conventional, sub-β_t forging (see below) and without undue metallurgical risks in the case of super-β_t forging (see below). The lower limit of the specified ranges is the temperature at which forging should be discontinued in the case of open-die forging to avoid excessive cracking and/or other surface quality problems.

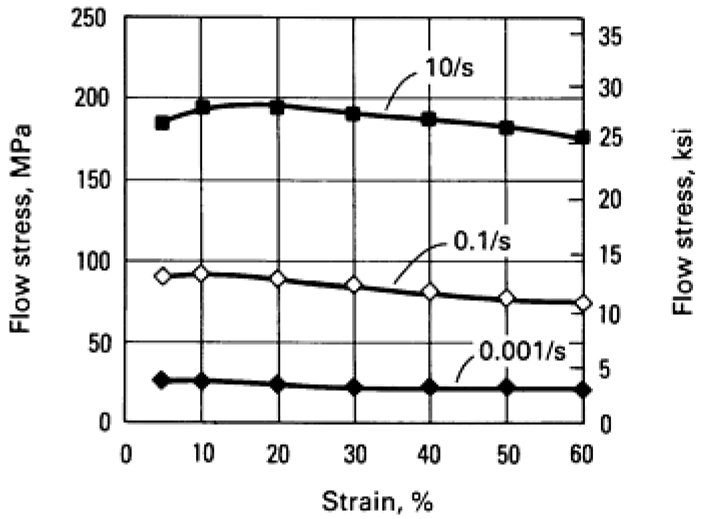
Effect of Deformation Rate. Titanium alloys are highly strain rate sensitive in deformation processes such as forging--considerably more so than aluminum alloys or alloy steels. The strain rate sensitivity for representative alloys from each of the three classes is illustrated in Fig. 4(a) for the αalloy Ti-8Al-1Mo-1V, in Fig. 4(b) for the α-β alloy Ti-6Al-4V, and in Fig. 4(c) for the β alloy Ti-10V-2Fe-3Al. For each of these alloys, as the deformation rate is reduced from 10/s to 0.001/s, the flow stress can be reduced by up to ten times. For example, the flow stress for Ti-6Al-4V at 900 °C (1650 °F), 50% strain, and 10/s is 205 MPa (30 ksi); at 0.001/s, the flow stress is 50 MPa (7 ksi), a fourfold reduction.



(a)



(b)



(c)

Fig. 4 Effect of three strain rates (0.001, 0.1, and 10/s) on flow stress of three titanium alloys forged at different temperatures. (a) α alloy Ti-8Al-1Mo-1V at 955 °C (1750 °F). (b) α-β alloy Ti-6Al-4V at 900 °C (1650

°F). (c) Metastable β alloy Ti-10V-2Fe-3Al at 815 °C (1500 °F).

From the known strain rate sensitivity of titanium alloys, it appears to be advantageous to deform these alloys at relatively slow strain rates in order to reduce the resistance to deformation in forging; however, under the nonisothermal conditions present in the conventional forging of titanium alloys, the temperature losses encountered by such techniques far outweigh the benefits of forging at slow strain rates. Therefore, in the conventional forging of titanium alloys with relatively cool dies, intermediate strain rates are typically employed as a compromise between strain rate sensitivity and metal temperature losses in order to obtain the optimal deformation possible with a given alloy. As discussed in the article "Isothermal and Hot-Die Forging" in this Volume, major reduction in resistance to deformation of titanium alloys can be achieved by slow strain rate forging techniques under conditions where metal temperature losses are minimized through dies heated to temperatures at or close to the metal temperature.

With rapid deformation rate forging techniques, such as the use of hammers and/or mechanical presses, deformation heating during the forging process becomes important. Because titanium alloys have relatively poor coefficients of thermal conductivity, temperature nonuniformity may result, giving rise to nonuniform deformation behavior and/or excursions to temperatures that are undesirable for the alloy and/or final forging mechanical properties. As a result, in the rapid strain rate forging of titanium alloys, metal temperatures are often adjusted to account for in-process heat-up, or the forging process (sequence of blows, and so on) is controlled to minimize undesirable temperature increases, or both. Therefore, within the forging temperature ranges out-lined in Table 1, metal temperatures for optimal titanium alloy forging conditions are based on the type of forging equipment to be used, the strain rate to be employed, and the design of the forging part.

Effect of Die Temperature. The dies used in the conventional forging of titanium alloys, unlike some other materials, are heated to facilitate the forging process and to reduce metal temperature losses during the forging process--particularly surface chilling, which may lead to inadequate die filling and/or excessive cracking. Table 2 lists the recommended die temperatures used for several titanium alloy forging processes employing conventional die temperatures. Dies are usually preheated to these temperature ranges using the die heating techniques discussed below. In addition, because the metal temperature of titanium alloys exceeds that of the dies, heat transfer to the dies occurs during conventional forging, frequently requiring that the dies be cooled to avoid die damage. Cooling techniques include wet steam, air blasts, and, in some cases, water.

Table 2 Die temperature ranges for the conventional forging of titanium alloys

Forging process/equipment	Die temperature	
	°C	°F
Open-die forging		
Ring rolling	150-260	300-500
	95-260	200-500
Closed-die forging		
Hammers	95-260	200-500
Upsetters	150-260	300-500
Mechanical presses	150-315	300-600

Screw presses	150-315	**300-600**
Orbital forging	150-315	**300-600**
Spin forging	95-315	**200-600**
Roll forging	95-260	**200-500**
Hydraulic presses	**315-480**	**600-900**

Reference cited in this section

1. A.M. Sabroff, F.W. Boulger, and H.J.Henning, *Forging Materials and Practices,* Reinhold, 1968

Forging of Titanium Alloys

G.W. Kuhlman, Aluminum Company of America

Forging Methods

Titanium alloy forgings are produced by all of the forging methods currently available, including open-die (or hand) forging, closed-die forging, upsetting, roll forging, orbital forging, spin forging, mandrel forging, ring rolling, and forward and backward extrusion. Selection of the optimal forging method for a given forging shape is based on the desired forging shape, the sophistication of the design of the forged shape, the cost, and the desired mechanical properties and microstructure. In many cases, two or more forging methods are combined to achieve the desired forging shape, to obtain the desired final part microstructure, and/or to minimize cost. For example, open-die forging frequently precedes closed-die forging to preshape or preform the metal to conform to the subsequent closed dies, to conserve the expensive input metal, and/or to assist in overall microstructural development.

The hot deformation processes conducted during the forging of all three classes of titanium alloys form an integral part of the overall thermomechanical processing of these alloys to achieve the desired microstructure and therefore the first- and second-tier mechanical properties. By the design of the working process history from ingot to billet to forging, and particularly the selection of metal temperatures and deformation conditions during the forging process, significant changes in the morphology of the allotropic phases of titanium alloys are achieved that in turn dictate the final mechanical properties and characteristics of the alloy. Fundamentally, there are two principal metallurgical approaches to the forging of titanium alloys:

- Forging the alloy predominantly below the β_t
- Forging the alloy predominantly above the β_t

However, within these fundamental approaches, there are several possible variations that blend these two techniques into processes that are used commercially to achieve controlled microstructures that tailor the final properties of the forging to specification requirements and/or intended service applications. The following sections in this article describe the two basic forging techniques used for titanium alloys, particularly the α/near-α, α-β, and metastable β alloys. In fully β stabilized alloys, manipulation of the α phase through forging process techniques is less prevalent; therefore, fully β stabilized alloys are typically forged above the β_t of the alloy.

Conventional (α-β) forging of titanium alloys, in addition to implying the use of die temperatures of 540 °C (1000 °F) or less, is the term used to describe a forging process in which most or all of the forging deformation is conducted at temperatures below the β_t of the alloy. For α, α-β, and metastable β alloys, this forging technique involves working the material at temperatures where both α and βphases are present, with the relative amounts of each phase being dictated by the composition of the alloy and the actual temperature used. With this forging technique, the resultant as-forged microstructure is characterized by deformed or equiaxed primary αin a transformed β matrix; the volume fraction of primary α is dictated by the alloy composition and the actual working history and temperature (Fig. 5a). Alpha-Beta forging is typically used to develop optimal strength/ductility combinations and optimal high/low-cycle fatigue properties. With conventional α-β forging, the effects of working on microstructure, particularly αmorphology changes, are cumulative; therefore, each successive α/β working operation adds to the structural changes achieved in earlier operations.

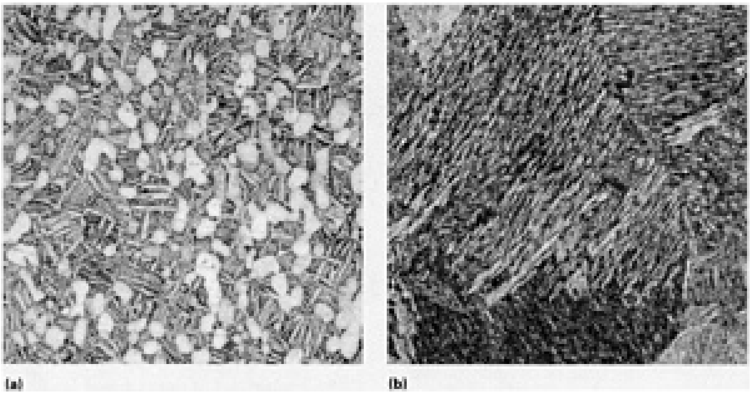


Fig. 5 Typical microstructure of forged titanium alloys. (a) α -β forging/heat treatment of alloy Ti-6Al-4V. Equiaxed primary αin transformed β. 200×. (b) β forging of alloy Ti-6Al-4V. Widmanstätten or acicular primary αin transformed β. 200×.

Example 1: Conventional α-β Forging of a Compressor Disk in Three Operations.

A 660 mm (26 in.) diam compressor disk, with a rim 44.5 mm (1.75 in.) thick and a web 19 mm (0.75 in.) thick was α-β forged from Ti-6Al-4V in three operations, as follows:

- Upset forged in a 160 kN (35,000 lbf) hammer, using a starting stock temperature of 980 °C (1800 °F) to reduce the stock height from 250 to 75 mm (10 to 3 in.)
- Blocked in a 160 kN (35,000 lbf) hammer to a rough contour, using a starting temperature of 955 °C (1750 °F), reducing rim thickness to 50 mm (2 in.) and web thickness to 25 mm (1 in.)
- Finish forged in a 160 kN (35,000 lbf) hammer to the final outline, starting at 955 °C (1750 °F), reducing rim thickness to 44.5 mm (1.75 in.) and web thickness to 19 mm (0.75 in.)

Beta forging, as the term implies, is a forging technique for α, α-β, and metastable β alloys in which most or all of the forging work is done at temperatures above the β_t of the alloy. In commercial practice, β forging techniques typically involve supertransus forging in the early and/or intermediate stages with controlled amounts of final deformation below the β_t of the alloy. Actual final subtransus working criteria are dependent on the alloy, the forging design, and the mechanical property combinations sought.

In β forging, the working influences on microstructure are not fully cumulative; with each working-cooling-reheating sequence above the β_t, the effects of the prior working operations are at least partially lost because of recrystallization from the transformation upon heating above the β_t of the alloy. Beta forging techniques are used to develop microstructures characterized by Widmanstätten or acicular primary α morphology in a transformed β matrix (Fig. 5b). This forging process is typically used to enhance fracture-related properties, such as fracture toughness and fatigue crack propagation resistance, and to enhance the creep resistance of α and α-β alloys. In fact, several recently developed α alloys (such as IMI 829 and 834) are designed to be β forged to develop the desired final mechanical properties. There is often a loss in strength and ductility with β forging as compared to α-β forging. Beta forging, particularly of α and α-β alloys, has the advantages of significant reduction in forging unit pressures and reduced cracking tendency, but it must be done under carefully controlled forging process conditions to avoid nonuniform working, excessive grain growth, and/or poorly worked structures, all of which can result in final forgings with unacceptable or widely variant mechanical properties within a given forging or from lot to lot of the same forging.

Example 2: Comparison of α-β and βForging of a Wing Spar Airframe Component in Ti-6Al-4V.

The wing spar forging shown in Fig. 6 is an example of a large titanium alloy component forged in a heavy hydraulic press. This forging weighs 262 kg (578 lb) and is produced using three press operations on a 310 or 450 MN (35,000 or 50,000 tonf) press with three sets of dies: first block, second block, and finish. For conventional α-β forging, all forging operations are conducted below the β_t of the alloy, using metal temperatures of 940 to 970 °C (1725 to 1775 °F).

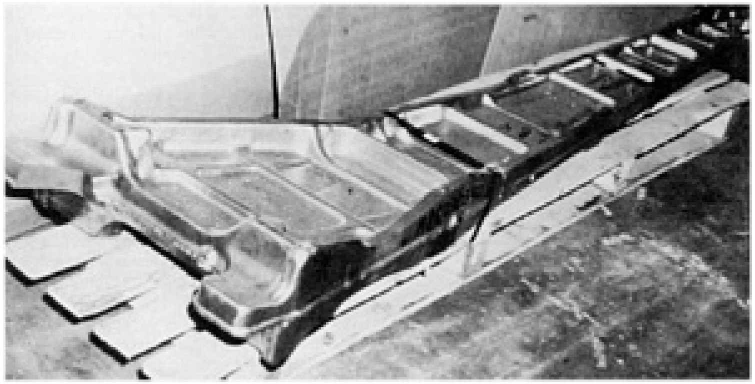


Fig. 6 Titanium alloy wing spar forged in a closed-die using α-β and β forging techniques. The part is 2.8 m (110 in.) long and weighs 262 kg (578 lb).

For β forging, two forging methods were investigated:

- Beta 1: first block only above the β_t of the alloy with second block and finish below the transus of the alloy
- Beta 2: first and second block above the transus of the alloy and finish forging only below the transus of the alloy

The metal temperature used for the β forging processes was 1040 to 1065 °C (1900 to 1950 °F). Table 3 lists the typical mechanical properties achieved in this wing spar forging with all three forging processes where the final heat treatment was an anneal at 705 to 730 °C (1300 to 1350 °F). Therefore, when β forging processes are used to produce this wing spar forging in annealed Ti-6Al-4V, the resulting yield and tensile strengths and ductilities (elongation and reduction in area) are reduced, but fracture toughness is improved over conventional α-β forging.

Table 3 Typical mechanical properties of wing spar forging obtained with three distinct forging processes

Forging process	**Direction**[(a)]	**Yield strength**		**Ultimate strength**		**Elongation, %**	**Reduction in area, %**	Plain-strain fracture toughness, K_{Ic}	
		MPa	**ksi**	**MPa**	**ksi**			**MPa**$\sqrt{\mathrm{m}}$	ksi$\sqrt{\mathrm{in.}}$
Alpha-beta	L	938	136	979	142	15	29	62	**56**

	T	938	136	958	139	14	30	57	**52**
Beta 1	L	890	129	959	139	12	25	70	**64**
	T	848	123	917	133	11	24	69	**63**
Beta 2	L	841	122	917	133	11	21	79	**72**
	T	**814**	**118**	**903**	**131**	**9**	**15**	**80**	**73**

(a) L, longitudinal; T, transverse

Open-die forging is used to produce small quantities of titanium alloy forgings for which closed-dies may not be justified (see the article "Open-Die Forging" in this Volume). The quantity of forgings that warrants the use of closed dies varies considerably, depending largely on the size and shape of the forging. The open-die forging of titanium is also used to produce prototypes or small quantities of parts that might be machined from a solid billet or plate. However, because of the high cost of titanium alloys, considerable metal and machining costs can often be saved by using open-die forgings rather than machining from a solid shape. Finally, open-die forging is frequently used to make preform shapes, ranging from pancakes or biscuits to highly contoured shapes, for subsequent closed-die forgings. As with other materials, the complexity of open-die forged shapes can be consistently reproduced with state-of-the-art flat die forging equipment (see the article "Forging of Aluminum Alloys" in this Volume).

Closed-Die Forging. By far the greatest tonnage of conventionally forged titanium alloys is produced in closed dies. Closed-die titanium alloy forgings can be classified similarly to other materials, such as aluminum, as blocker-type (achieved with single set of dies or block/finish dies), conventional (achieved with two or more sets of dies), high-definition (also requiring two or more sets of dies), and precision forgings (frequently employing hot-die or isothermal forging techniques). Precision titanium alloy forgings are discussed below. Blocker-type titanium alloy forgings are typically produced in relatively less expensive dies, with design and tolerance criteria between those of open-die and conventional forgings. Conventional closed-die titanium forgings cost more than blocker-type, but the increase in cost is usually justified because of reduced machining costs. Finally, high-definition titanium alloy forgings are also more costly than conventional forging, but may also be justified by reduced machining. Preforming using open-die, upsetting, and/or roll forging frequently precedes all types of titanium alloy closed-die forging processes (see the article "Closed-Die Forging in Hammers and Presses" in this Volume).

In comparison with aluminum alloy closed-die forgings, all types of closed-die forgings in titanium alloys are typically produced to more generous design and/or tolerance criteria, reflecting the increased difficulty in forging these alloys. Figure 7 shows a large main landing gear beam forging produced in the α-β alloy Ti-6Al-4V. This relatively high volume main landing gear beam has been fabricated with a progression of closed-die forging designs in an effort to reduce the overall cost of the final machined part. Figures 8(a), 8(b), and 8(c) illustrate one cross section from this forging and the three types of closed-die forging approaches used to manufacture this part.

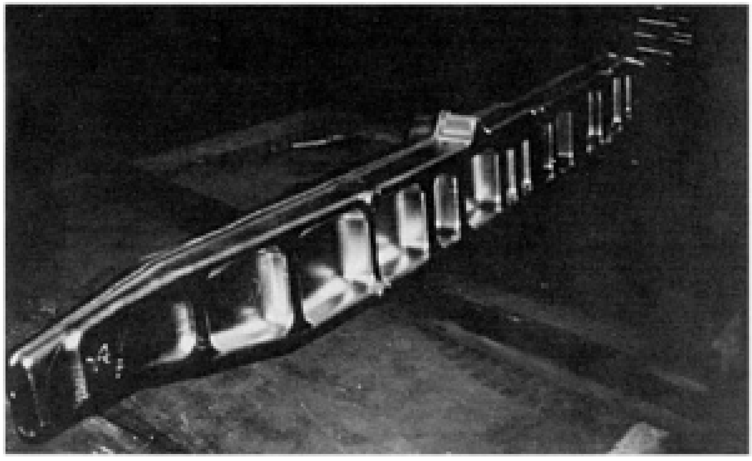
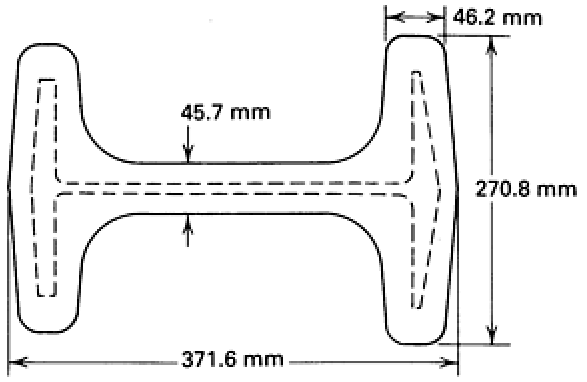
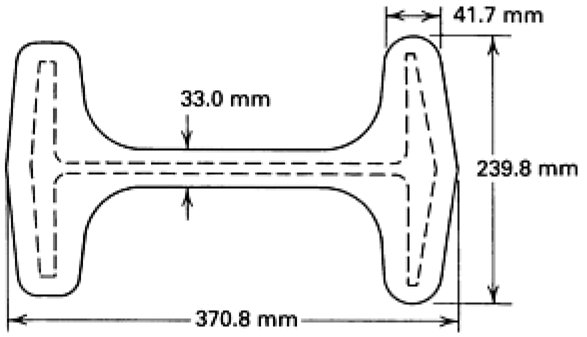


Fig. 7 Boeing 757 main landing gear beam forged of alloy Ti-6Al-4V using three available closed-die forging methods (blocker type, conventional, and high definition); see Fig. 8. The part weighs 1400 kg (3000 lb) and has 1.71 m^2 (2650 $in.^2$) plan view area (PVA); it is 498.3 mm (19.62 in.) high, 4467.1 mm (175.87 in.) long, and 339.3 mm (13.36 in.) deep.



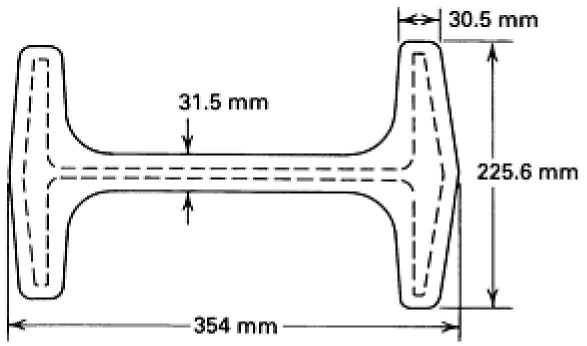
Characteristic	Tolerance
Corner radii	22.1 ± 4.6 mm (0.87 ± 0.18 in.)
Fillet radii	50.8 ± 6.4 mm (2.00 ± 0.25 in.)
Die closure	+15.7, −0.8 mm (+0.62, −0.03 in.)
Mismatch	0-6.4 mm (0-0.25 in.)
Straight within	9.7 mm (0.38 in.)
Flash extension	0-12.7 mm (0-0.50 in.)
Length and width	±1.8 mm (±0.07 in.)

(a)



Characteristic	Tolerance
Corner radii	22.1 ± 3.0 mm (0.87 ± 0.12 in.)
Fillet radii	50.8 ± 6.4 mm (2.00 ± 0.25 in.)
Die closure	+15.7, −0.8 mm (+0.62, −0.03 in.)
Mismatch	0-6.4 mm (0-0.25 in.)
Straight within	9.7 mm (0.38 in.)
Flash extension	0-12.7 mm (0-0.50 in.)
Length and width	±1.8 mm (±0.07 in.)

(b)



Characteristic	Tolerance
Corner radii	9.7 ± 3.0 mm (0.38 ± 0.12 in.)
Fillet radii	$38.1 \pm \begin{smallmatrix}6.4\\12.7\end{smallmatrix}$ mm ($1.50 \pm \begin{smallmatrix}0.25\\0.50\end{smallmatrix}$ in.)
Die closure	+15.7, −0.8 mm (+0.62, −0.03 in.)
Mismatch	0-4.8 mm (0-0.19 in.)
Straight within	6.4 mm (0.25 in.) full indicator movement
Flash extension	14.2 ± 4.6 mm (0.56 ± 0.18 in.)
Length and width	±1.5 mm (±0.06 in.)

(c)

Fig. 8 Cross sections of Boeing 757 part shown in Fig. 7 illustrating design and tolerance criteria for the 272 kg (600 lb) machined weight forging obtained from three closed-die forging methods, along with their respective forging weights. (a) Blocker type, 1364 kg (3007 lb). (b) Conventional, 1087 kg (2397 lb). (c) High definition, 879 kg (1937 lb).

Figure 8(a) shows the original blocker-type configuration (designed prior to finalization of the machined part) produced in two sets of dies. As a blocker-type part, the forging weighed 1364 kg (3007 lb) versus a machined part weight of 272 kg (600 lb) for an overall recovery from the raw forging of 20% (or a buy-to-fly ratio of 5 to 1). When the final machine part geometry had been better defined, the part was redesigned to a conventional forging (Fig. 8b) weighing 1087 kg (2397 lb), increasing the recovery from the raw forging of 25% (buy-to-fly of 4 to 1). Sufficient machining and metal cost savings were realized through this redesign to justify the costs of construction of a new set of dies. Finally, after some additional final machined part refinements, the part was redesigned to a high-definition shape (Fig. 8c), reducing the as-forged weight to 879 kg (1937 lb) and increasing the overall recovery of 31% (buy-to-fly of 3.3 to 1). Again, a cost savings was realized that justified the construction of new dies. Therefore, from blocker-type to close tolerance, the as-forged weight was reduced by nearly 500 kg (1100 lb), and the forged part/machined part recovery was increased by 11%--a significant cost savings.

Upset forging is sometimes the sole method used for forging a specific shape, such as turbine engine disks, from titanium alloys. More often, however, upsetting is used as a method of preforming to reduce the number of forging operations or to save material input, as is true for other materials (see the article "Hot Upset Forging" in this Volume). Upsetting in titanium alloys is often preferred to extrusion for creating large-headed sections adjacent to smaller cross sections. In the upset forging of titanium alloys, the unsupported length of a round section to be upset should not exceed 2.5 times the diameter; for a rectangular or square cross section, 2.5 times the diagonal. The maximum amount of upset achievable in titanium alloys without reheating depends on the alloy, but for the more readily deformable alloys, it is usually 2.5 times the diameter (or diagonal). Without several heating and upsetting operations, it is impossible to produce an upset in titanium alloys as thin or having as sharp corners as are typically produced in alloy steels.

Roll forging can be the sole forging operation used in the production of certain types of products in titanium alloys, as with other materials (see the article "Roll Forging" in this Volume); however, the roll forging of titanium alloys is much more widely used to make preform shapes, to save input material, or to reduce the number of closed-die forging operations. The roll forging of titanium alloys is frequently used for stock gathering and distribution of parts, such as blades, which have major differences in metal volume demands.

Rotary (orbital) forging is a variation of closed-die forging that is successfully used on titanium alloys for the manufacture of parts characterized by surfaces of revolution, such as turbine disks and other components with axial symmetry (see the article "Rotary Forging" in this Volume). The rotary forging of titanium alloys, because of the incremental nature of the deformation in this process, can provide enhanced final forging design sophistication and tolerances over that possible in other closed-die forging equipment, such as hammers, mechanical/screw presses, and hydraulic presses.

Spin forging can also be used in titanium alloy forging fabrication, as with aluminum and other materials. This technique combines closed-die forging and computer numerically controlled (CNC) spin forgers and achieves very close tolerance, axisymmetric, hollow shapes (see the article "Forging of Aluminum Alloys" in this Volume). Similar shape capability is possible in titanium alloys with attendant final component cost reductions from reduced material input and reduced final machining. As with aluminum, spin-forged shapes in titanium alloys can be produced to much tighter out-of-round and concentricity tolerances than competing techniques, such as forward or backward extrusion.

Ring rolling has been successfully used for producing a wide variety of rectangular and contoured annular shapes in titanium alloys and other materials. The methods used in ring rolling titanium alloys are essentially the same as those used for alloy steels (see the article "Ring Rolling" in this Volume). In addition to ring rolling, other forging methods, such as upset forging and punching, mandrel forging, and forward/backward extrusion, are sometimes used on titanium alloys to produce small or prototype quantities of annular shapes with predominant grain orientations in directions other than circumferential, as is typically achieved with ring rolling. Ring rolling is effective for a variety of titanium alloys of all types to reduce the cost of the final part through the fabrication of a near-net shape; a primary application is rotating and nonrotating turbine engine components.

Forward or backward extrusion is a variation of the closed-die forging of titanium alloys and other materials that can be used to produce hollow, axisymmetric shapes with both ends open or one end closed. Titanium alloys are among the most difficult materials to extrude because of their high resistance to deformation, temperature sensitivity, and abrasive nature. However, with properly designed and constructed tooling (usually from hot-work die steels; see the section "Die Specifications" in this article) and extrusion processes, the forward or backward extrusion of a variety of titanium alloys can be accomplished (additional information on extrusion is available in the article "Conventional Hot Extrusion" in this Volume). The extrusion of titanium alloys is usually accomplished from above the β_t of the alloy; therefore, the forward/backward extrusion applications of titanium alloys must be tolerant of the transformed microstructure and resultant properties. Forward or backward extrusion is also used to produce annular shape preforms for ring rolling or other closed-die forging operations, in which the subsequent fabrication processes may successfully modify the as-extruded microstructure. Selection of forward or backward extrusion is usually based on part geometry and press opening restrictions. Some state-of-the-art presses are equipped with openings in the upper cross-head to accommodate the fabrication of very long backward extrusions, either solid or hollow.

Forging of Titanium Alloys

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Forging Equipment

Conventional titanium alloy forgings are produced on the full spectrum of forging equipment, from hammers and presses to specialized forging machines. Selection of forging equipment for a given titanium alloy shape is based on the capabilities of the equipment, forging design sophistication, desired forging process, and cost. The types of forging equipment used are discussed in the articles "Hammers and Presses for Forging" and "Selection of Forging Equipment" in this Volume).

Hammers. Gravity and power-assisted drop hammers are extensively used for the open-die and closed-die conventional forging of titanium alloys because of the relatively low fabrication costs associated with such equipment, their ability to impart progressive deformation to these difficult-to-work alloys, and the relatively short time the workpiece is in contact with the much cooler dies. Although the power requirements for the hammer forging of titanium alloys exceed those for aluminum alloys or alloy steels, hammers have been found to be effective in the manufacture of titanium alloy forgings of almost any size, but hammers are more often used for medium-to-large forgings, including axisymmetric shapes such as turbine disks and relatively generously designed airframe components. Because hammers deform the metal with high deformation speeds, the impact/strain rate of a hammer in forging titanium alloys may cause localized temperature variations, which may adversely affect the final forging microstructure. However, with proper control of hammer-forging processes, the temperature increase can be effectively exploited to facilitate the completion of the desired forging process and to increase the total deformation time before the titanium alloy cools below the recommended forging temperature range.

Mechanical presses are extensively used for the fabrication of small-to-medium titanium alloy forgings, with forging shape sophistication ranging from relatively simple shapes to precision forgings. A prime example of a conventionally forged, precision titanium alloy part manufactured on a mechanical press is turbine engine compressor and fan blades. The relatively rapid deformation rates available in mechanical presses are effectively exploited to produce the complex contours and tight tolerances associated with such airfoil shapes. As with hammers, the rapid deformation rate typical of mechanical presses may introduce temperature variations; however, with control of input material distribution, metal temperature, and the deformation conditions, uniform final forging microstructures are readily achievable. Mechanical presses are typically used for producing titanium alloy forgings weighing less than 9.1 kg (20 lb) and are seldom used for forgings weighing over 45 kg (100 lb). Figure 9 illustrates the forging processes used to manufacture a large turbine engine fan blade. The processes used in addition to block and finish forging on a large mechanical press include upsetting and gathering in order to distribute material properly before die forging.

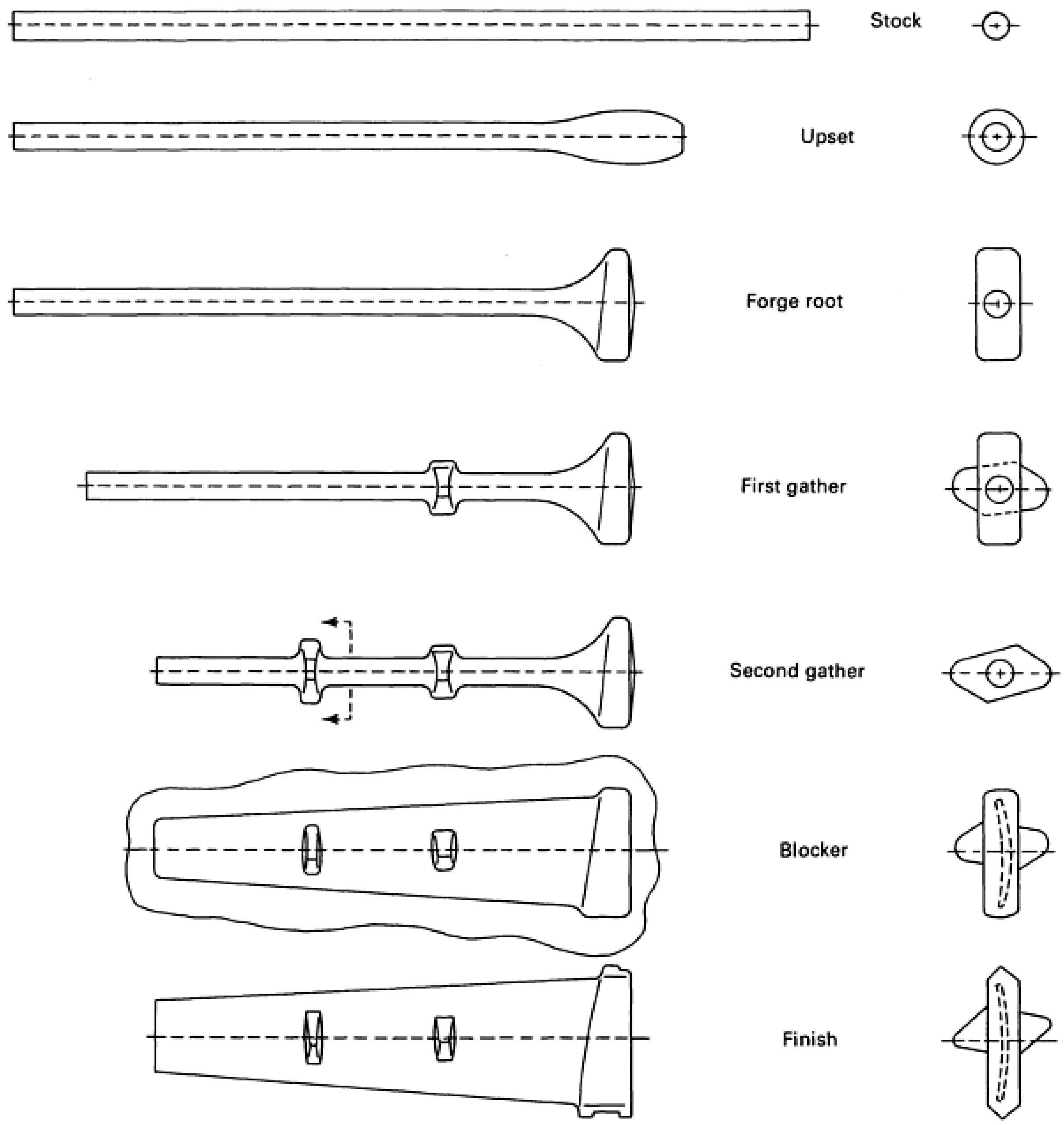

Fig. 9 Fabrication stages in the manufacture of a large alloy Ti-6Al-4V turbine engine fan blade.

Screw presses are also effective in the manufacture of titanium alloy forgings, including both simple shapes and precision forgings such as turbine engine blades and prosthetic devices. The more controlled deformation rate possible in a screw press has been exploited with titanium alloys in the manufacture of highly configured (twisted) titanium alloy blades and double-platform blades, such as those illustrated in Fig. 10.

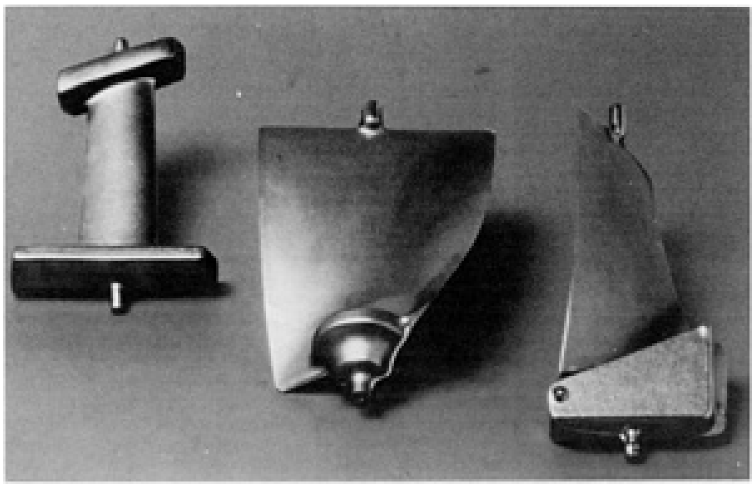

Fig. 10 Highly configured (twisted) alloy Ti-6Al-4V and alloy Ti-8Al-1Mo-1V turbine engine fan and compressor blades that were forged in screw presses.

Hydraulic presses are seldom used to manufacture small titanium alloy forgings (except for precision forgings), but are extensively used to manufacture large forgings weighing 1400 kg (3000 lb) and more. Hydraulic presses are also used to manufacture hand forgings and preforms for subsequent closed-die forging. Because the deformation achieved in a hydraulic press occurs at slower strain rates, metal temperature is usually more uniform in the forging than with rapid strain rate equipment. However, in the conventional hydraulic press forging of titanium alloys, metal temperature losses are encountered because of the time associated with the deformation and the contact with the cooler dies. Therefore, in the hydraulic press forging of titanium alloys, the metal temperatures employed are typically near the upper limits of the recommended ranges in Table 1, and insulative materials such as fiberglass are often used between the workpiece and the dies to retard heat transfer from the metal to the dies.

Figure 11 illustrates the largest closed-die titanium alloy forging ever manufactured. A 450 MN (50,000 tonf) hydraulic press was used. This is one of four main landing gear beam forgings used in the Boeing 747. This Ti-6Al-4V forging is over 6.22 in (245 in.) long and weighs over 1400 kg (3000 lb). It is manufactured using incremental forging techniques in two sets of dies in order to obtain sufficient unit pressure from the 450 MN (50,000 tonf) press.

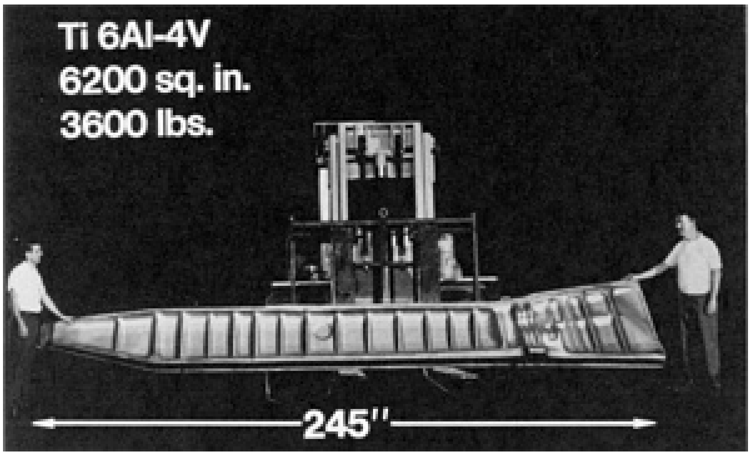

Fig. 11 Largest closed-die titanium alloy forging ever manufactured, a Boeing 747 main landing gear beam. Part was produced on a 450 MN (50,000 tonf) hydraulic press. Dimensions given in inches.

Figure 12 illustrates two other very large, highly configured Ti-6Al-4V titanium alloy airframe forgings that were also produced on a 450 MN (50,000 tonf) press--the upper and lower bulkheads for the F-15 aircraft. The smaller, upper bulkhead weighs 305 kg (670 lb), and the larger lower bulkhead weighs 725 kg (1600 lb). These three forgings (Fig. 11 and 12) illustrate not only the size of the titanium alloy forgings produced on hydraulic presses but also in conjunction with the 757 main landing gear beam shown in Fig. 7, illustrate the highly sophisticated forging design capability possible in the conventional forging of these difficult-to-fabricate alloys in the relatively slow strain rate conditions present in hydraulic presses. Such design sophistication is achieved through the optimization of forging die design and the hydraulic press forging processes used for titanium alloys.

Fig. 12 Alloy Ti-6Al-4V forgings for upper and lower bulkheads used on the F-15 that were produced on a 450 MN (50,000 tonf) hydraulic press using conventional forging methods.

Forging of Titanium Alloys

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Die Specifications

The critical elements of the closed-die forging of titanium alloys are die materials selection, die design, and die manufacture. The dies are a major part of the total cost of such forgings; however, as a percentage of total cost, the die cost for titanium alloys may be less than that for such materials as aluminum or alloy steels because of the much higher materials costs associated with titanium alloys. Further, forging process parameters and forging design capabilities are affected by die design, and the dimensional integrity of the finished titanium is in large part controlled by the die cavity. Therefore, the closed-die forging of titanium alloys requires the use of dies that are specifically designed for titanium for the following reasons:

- The shrinkage allowance in die sinking for titanium alloys is typically 0.004 mm/mm (0.004 in./in.) versus 0.006 mm/mm (0.006 in./in.) for aluminum alloys and 0.011 mm/mm (0.011 in./in.) for alloy steels
- Titanium alloys fill die contours less readily than alloy steel, stainless steel, or aluminum alloys; therefore, the die impressions for forging titanium alloys usually must have larger radii and fillets. For intricate or high-definition titanium forgings, more forging steps and therefore more die sets are typically required for titanium than for other materials, such as alloy steels or aluminum
- Dies for forging titanium alloys must be stronger than those for steel or aluminum alloys because greater unit pressures are usually needed to forge these alloys. Dies for titanium alloys may be up to 50% thicker, in terms of sidewalls and depth below the cavity, for the same depth and severity of die

impression than those used for alloy steels or aluminum. Without this increase in sidewall and/or below-cavity thickness, the risk of catastrophic die failure or excessive die distortion is significantly higher, and the number of die resinks that can be made without risk of die failure will be fewer.

- The surface finish requirements for titanium alloy dies are more stringent than those for alloy steels because of the generally poorer flow characteristics of titanium alloys

Die Materials. For the conventional forging of titanium alloys, the die materials used in closed-die forging are identical to the materials employed for aluminum alloys or alloy steels. Because of the higher temperatures associated with titanium alloy forgings, hot-work die steels such as H12 and H13 can be used more frequently with titanium alloys, especially as inserts or in small dies, than with aluminum alloys. The main body of the dies for titanium alloys is usually constructed of 6G or 6F2 die steels (see the article "Dies and Die Materials for Hot Forging" in this Volume), and/or the many proprietary grades within these composition limits offered by a number of die steel producers, at a hardness of 341 to 375 HB. A hot-work die steel at a higher hardness can then be inserted into the die cavities.

Die hardness for titanium alloys, as with other materials, depends on the severity and depth of the cavity and on the forging equipment that will be used to manufacture the forging. For hydraulic press forging, hot-work die steels are usually heat treated to 47 to 55 HRC. For dies with more severe impressions, the lower side of this range (47 to 49 HRC) is used; for dies with minimum severity, the upper side of the range (53 to 55 HRC) is used. For hammer and/or mechanical press forging, die hardness can be reduced by at least three points in order to increase toughness. Generally, the forger balances the desire for high die hardness to minimize wear with lower die hardness to increase toughness. For especially demanding or very high volume titanium forging processes (such as forward or backward extrusion, mechanical/screw press closed-die forging, and some open-die forging), hot-work die steels (H12 and H13) are used for the main body of the dies, and in some cases wrought/cast nickel-base alloys such as Alloy 718 (UNS N07718) have been successfully used where the increased cost associated with these materials is justified by improved die service life.

Even though the forging temperatures for titanium alloys are lower than those for alloy steels, die wear is generally greater in the conventional forging of titanium alloys because of the increased resistance of these alloys to deformation and the very abrasive nature of the oxide/scale coating present on these alloys during forging. Therefore, in addition to using inserts from higher-hardness hot-work die steels, other steps are frequently taken to improve the wear resistance of dies for titanium alloy forgings and to maintain the integrity of the die cavity. These steps include surface treatments/modification and modification of critical forging design parameters (with customer input) to minimize wear. Surface treatments that have been successfully used include a variety of state-of-the-art processes, such as special welding techniques, carburizing, nitriding, and surface alloying.

Example 3: Increase in the Size of Fillets That Reduced Die Wear.

The assembly rib shown in Fig. 13 was originally produced from alloy Ti-6Al-4V as a conventional closed-die forging with 4.8 mm (0.19 in.) radii at the flash land near the parting line around the forging. This fillet is shown as "Original design" in Fig. 13. Excessive die wear occurred at the fillet. The die design was revised by enlarging this fillet from 4.8 to 9.7 mm (0.19 to 0.38 in.) ("Improved design," Fig. 13). The alteration solved the problem by reducing die wear in this area to a normal level.

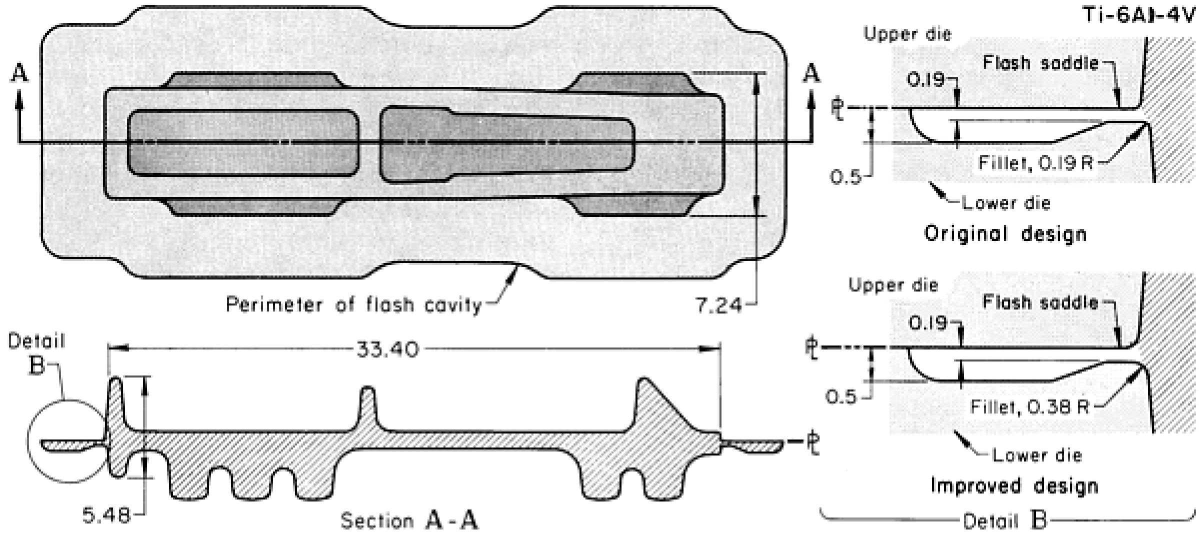

Fig. 13 Assembly rib for which forging die was redesigned to enlarge radius of fillets at flash saddle in order to increase die life. Dimensions given in inches.

Die Design. As with other materials, a key element in the cost control of dies for titanium forging and in the successful fabrication of titanium alloy forgings is die design and die system engineering. Dies for conventional closed-die titanium forgings are most frequently manufactured as stand-alone die blocks; however, in some cases, conventional closed-die, and particularly precision, titanium alloy forgings can be made from inserts in die holder systems. Die holder systems may be universal, covering a wide range of potential die sizes, or may be constructed to handle families of parts having similar overall geometries or sizes. The design of titanium alloy forging dies is highly intensive in engineering skills and is based on extensive empirical knowledge and experience. A compendium of titanium forging design principles and practices is provided in Ref 2.

As with aluminum alloys, forging design for titanium alloys is engineering intensive, and the advent of computer-aided design (CAD) hardware and software has had a significant impact on titanium alloy die design. The use of CAD technology in forging design is discussed in the article "Forging Process Design" in this Volume. As discussed in the article "Forging of Aluminum Alloys" in this Volume, CAD forging part design for titanium alloys is also institutionalized and widely used for titanium alloys. Computer-aided design databases are then used with computer-aided manufacturing (CAM) to produce dies, to direct the forging process, and to assist in final part verification and quality control. Heuristic, artificial intelligence, and deformation modeling techniques are also being applied to a variety of titanium alloys to enhance the forging design process. Further, because of the critical structural changes achieved in the forging of titanium alloys, these expert systems and finite-element models will be used to predict final part microstructures in advance of actually committing to the production forging process. Because of the flow characteristics of titanium alloys, special design features are often incorporated into the dies to restrict or to enhance metal flow in certain locations of a forging, as discussed in the following example.

Example 4: Use of Corrugations in Flash Land to Reduce Outward Flow of Flash.

A rectangular box forging (Fig. 14) was used experimentally to determine the effect of corrugations in restricting metal flow. The flash land surrounding the box was originally designed without corrugations. Because of the variation in wall thickness of the part, metal flowed more readily to the heavier walls, thus starving the sidewalls and resulting in inadequate fill. To restrain the flow of metal at the end walls, corrugations were added to the flash land at both ends (Detail A, Fig. 14). The flash land along the sidewalls was not corrugated (Detail B, Fig. 14). The restraint to flow provided by the corrugations was sufficient to fill the sidewalls completely. The corrugations also made possible a savings in the amount of metal required to complete the forging.

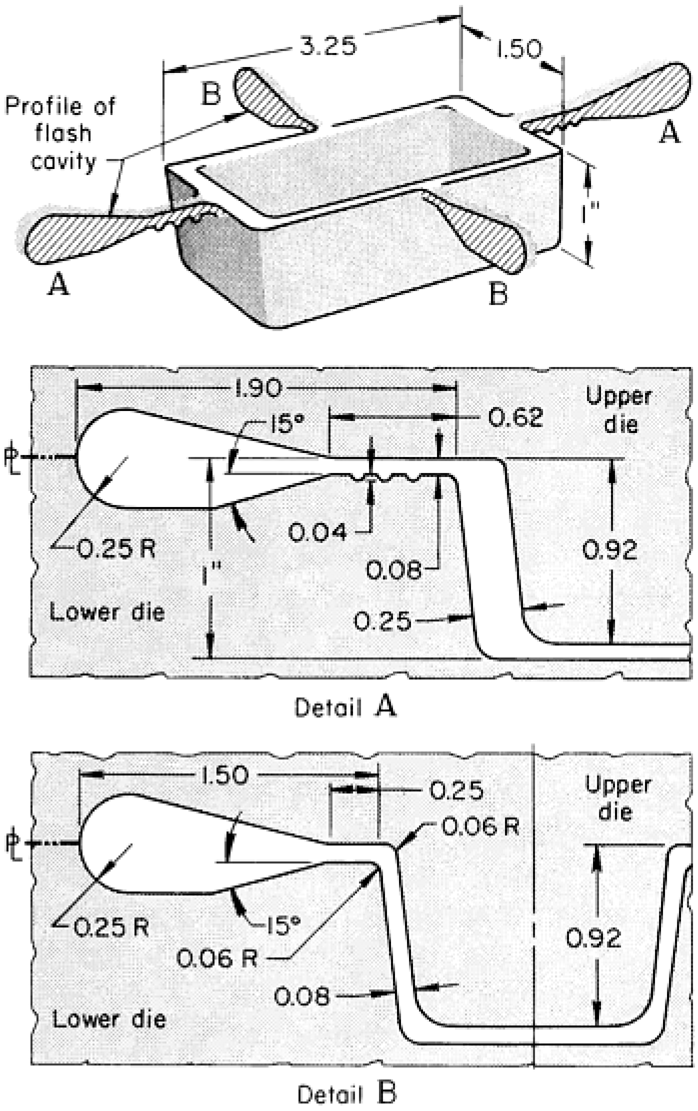

Fig. 14 Corrugations in the flash saddle at the end of a box forging that improved metal flow to the side walls. Dimensions given in inches.

Die Manufacture. Titanium alloy forging dies, which are similar to the aluminum alloy dies discussed in the article "Forging of Aluminum Alloys" in this Volume, are produced by a number of techniques, including hand sinking, copy milling from a model, electrodischarge machining (EDM), and CNC direct sinking. With CAD databases now available, CAM-driven CNC sinking of titanium alloy dies can provide the same benefits as those described for aluminum alloys.

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Titanium Alloy Forging Processing

The common elements in the manufacture of any conventional titanium alloy forging include preparation of the forging stock, preheating of the stock, die heating, lubrication, the forging process, trimming and repair, cleaning, heat treatment, and inspection. The critical aspects of each of these elements for titanium alloys are reviewed below.

Forging Stock. In the manufacture of titanium alloy forgings, the predominant forms of forging stock used are billet (round, octagonal, rectangular, or square) and bar that has been fabricated by primary hot-working processes from titanium alloy ingot. The conversion of titanium alloy ingot to forging stock is a critical part of the overall titanium alloy forging process because it affects the overall cost of the starting material used for forging and because ingot conversion plays an important role in the overall macro- and microstructural development of the final titanium alloy forgings. Only rarely is titanium alloy ingot directly forged into finished titanium alloy forging components, and even then early forging stages are used to refine the ingot structure.

Titanium alloy ingot is primarily hot worked using forging techniques; however, hot rolling can be used for bar stock. A series of working operations is carried out on titanium ingot that typically involves multiple upsetting and drawing procedures to impart primary work to the alloy, to refine the relatively coarse as-cast grain size, and to achieve the desired starting macrostructure and microstructure for forging.

Titanium ingot conversion can be accomplished by the forger or by the primary titanium metal producer. Ingot conversion working procedures, forging stock macrostructural (grain size) or microstructural requirements, nondestructive testing of the forging stock, and mechanical property testing of the forging stock for a given alloy/size/type of forging stock are usually based on the specific forging involved, the forging equipment that will be used to manufacture it, cost considerations, and final forging structural and mechanical property requirements. Requirements for the forging stock are usually the subject of specifications by the forger or are negotiated between the forger and the metal supplier. In addition, the ultimate forging customer and/or federal, military, or other governmental specifications, such as AMS 2380 (Ref 3), may impose specific requirements on the manufacture of titanium alloy ingot (for example, required melting practices and melting controls), the forging stock fabricated from such ingot, macro- and microstructural requirements for forging stock, and necessary tests and nondestructive inspections for the qualification of titanium alloy forging stock.

Surface preparation of titanium alloy billet or bar forging stock is important not only for the satisfactory performance of the stock in subsequent forging but also because detailed, stringent ultrasonic inspection is frequently performed on the forging stock (as required by customer or other specifications) as a critical part of the overall quality assurance functions on titanium alloy forgings. Ultrasonic inspection (USI) of the billet is often preferred to USI of the final forged shape because of the more regular geometric shape. Furthermore, billet conversion involves a mode of deformation that tends to enlarge critical defects making them more readily detectable. Such ultrasonic inspection is typically conducted by multiple scan and/or automated techniques on properly prepared rounds, rectangles, or squares. Therefore, titanium alloy billet or bar stock is typically ground or machined to remove all defects and to prepare the surface for the type of ultrasonic inspection that will be performed.

Preparation of Forging Stock. Properly fabricated and qualified titanium alloy forging stock is then prepared for forging using several cutting methods, including shearing, sawing, and flame cutting. As a class of materials, titanium alloys are considerably more difficult to cut than most other forged metals, except for superalloys and refractory metals. Shearing is used only on relatively small sizes of titanium alloy forging stock, typically 50 mm (2 in.) and less in diameter. Sawing techniques include cold sawing, machine hacksawing (for small-to-intermediate sizes and low volumes), machine band sawing (also for small-to-intermediate sizes and low volumes), and abrasive sawing (for intermediate-to-large rounds and squares). In all sawing operations, but particularly the abrasive sawing of titanium alloys, it is necessary to control the sawing operation through coolants, speeds, and feeds to prevent overheating during cutting; such overheating may result in cracking during subsequent forging. Flame cutting, using oxy-gas and plasma techniques, is used to cut rectangular and square forging billet in thicknesses to approximately 250 mm (10 in.). Because flame cutting leaves residual disturbed surfaces and heat-affected zones, typically $\sim$1.5 mm ($\sim$0.060 in.), it may be necessary to grind flame-cut surfaces to remove the slag and heat-affected material that may be conducive to surface cracking under severe deformation.

Preheating for Forging. Prior to preheating for forging, most titanium forging stock is coated with ceramic coatings to retard oxidation. Precoating and other titanium alloy forging lubrication issues are discussed below. The heating of titanium alloys for forging is a crucial part of the forging process, both in terms of preventing excessive contamination during heating by oxygen, nitrogen, and hydrogen and controlling the metal temperature within the narrow temperature limits necessary for the successful forging of titanium alloys.

Heating Equipment. Titanium alloys are heated for forging with various types of heating equipment, including electric furnaces, open or semimuffled gas furnaces, oil furnaces, induction heating, fluidized-bed heating, and resistance heating. Open-fired gas and electric furnaces, either continuous (for example, rotary) or batch, are the most widely used. Heating equipment design and capabilities necessarily vary with the requirements of a given forging process. Titanium alloys have an extreme affinity for all gaseous elements present during exposure to the atmospheric conditions prevalent in most heating techniques, except vacuum.

Above about 595 °C (1100 °F), titanium alloys react with both oxygen and nitrogen to form scale. Underlying the scale is an oxygen/nitrogen enriched zone called α case; both oxygen and nitrogen stabilize the α phase. This α case zone may be hard and brittle, and if deep enough, it can cause cracking and/or increased tooling wear. Therefore, titanium alloys are precoated, and heating practices and/or furnace operating conditions are controlled to minimize the development of α case. With most titanium alloys, the formation of scale and α case is a diffusion-controlled process that may be limited by precoating and/or by the furnace operating parameters. Alpha and α-β titanium alloys tend to form more scale and α case than β alloys when heated under similar temperature and furnace conditions.

In addition, titanium alloys have an extreme affinity for hydrogen. Although reducing atmospheres, as used with some ferrous alloy forging, may retard the formation of scale and α case in titanium alloys, hydrogen atmospheres dramatically increase the risk of hydrogen pickup. Therefore, in addition to precoats, which also assist in the retardation of hydrogen pickup, most titanium alloy heating systems are designed to provide oxidizing conditions (through the use of excess air in gas-fired furnaces) in order to minimize the presence of hydrogen.

Induction heating, resistance heating, and fluidized-bed heating are frequently used in forging titanium alloys where forging processes are automated. State-of-the-art gas and electric furnaces for titanium alloys also often have fully automated handling systems.

Temperature Control. As noted in Fig. 1, 2, 3, 4 and Table 1, titanium alloys have a relatively narrow temperature range for conventional forging. Further, metal temperature is critical to the microstructure of titanium alloy forgings. Therefore, temperature control in preheating for forging titanium alloys is highly critical and is usually obtained through the capabilities and control of the heating equipment. Titanium alloy heating equipment should be equipped with pyrometric controls that can maintain ±14 °C (±25 °F) or better. Titanium alloy stock heating equipment is often temperature uniformity surveyed in much the same manner as with heat-treating furnaces. Continuous rotary furnaces used for titanium alloys typically have three zones: preheat, high heat, and discharge. Most furnaces are equipped with recording/controlling instruments, and in some batch furnace operations separate load thermocouples are used to monitor furnace temperature during preheating operations.

In addition to highly controlled heating equipment and heating practices, the temperature of heated titanium alloy billets can be verified with contact pyrometry or non-contact optical pyrometers. The latter equipment must be used with care because it is emissivity sensitive and may provide different temperature indications when the metal is observed inside the hot furnace versus when the metal has been removed from the furnace. In most closed-die and open-die forging operations, it is desirable to have titanium alloy metal temperatures near the upper limit of the recommended temperature ranges. In open-die forging, the lower limit of the recommended ranges is usually the point at which forging must be discontinued to prevent excessive surface cracking.

Heating Time. It is good practice to limit the exposure of titanium alloys in preheating to times just adequate to ensure that the center of the forging stock has reached the desired temperature in order to prevent excessive formation of scale and α case. Actual heating times will vary with the section thickness of the metal being heated and with furnace capabilities. Because of the relatively low thermal conductivity of titanium alloys, necessary heating times are extended in comparison to aluminum and alloy steels of equivalent thickness. Generally, 1.2 min/mm (30 min/in.) of ruling section is sufficient to ensure that titanium alloys have reached the desired temperature.

Heating time at a specific temperature is critical in titanium alloys for the reasons outlined above. Long soaking times are not necessary and introduce the probability of excessive scale or α case. Generally, soaking times should be restricted to 1 to 2 h, and if unavoidable delays are encountered, where soaking time may exceed 2 to 4 h, removal of the metal from the furnace is recommended.

Heating of Dies. Dies are always preheated in the closed-die conventional forging of titanium alloys, as noted in Table 2, with die temperature varying with the type of forging equipment used. Dies for titanium alloy forging are usually preheated in remote die heating systems, although on-press equipment is sometimes used. Remote die heating systems are usually gas-fired die heaters, which can slowly heat the die blocks to the desired temperature range before assembly into the forging equipment.

With some conventional forging processes, particularly the hydraulic press forging of titanium alloys, the temperature of the dies may increase during forging. Die damage may occur without appropriate cooling. Therefore, titanium alloy dies are often cooled during forging using wet steam, air, or occasionally water.

For those conventional forging processes in which die temperatures tend to decrease, on-press heating systems ranging from rudimentary to highly sophisticated are used. The techniques used include gas-fired equipment, induction heating equipment, resistance heating equipment, or combinations of these methods.

Lubrication is also a critical element in the conventional forging of titanium alloys and is the subject of engineering and process development emphasis in terms of the lubricants used and the methods of application. With titanium alloy conventional forging, a lubrication system is used that includes ceramic precoats of forging stock and forgings, die lubrication, and, for certain forging processes, insulation.

Ceramic Glass Precoats. Most titanium alloy forging stock and forgings are precoated with ceramic precoats prior to heating for forging. These ceramic precoats, which are formulated from metallic and transition element oxides and other additives, provide several functions, such as:

- Protection of the reactive titanium metal from excessive contact with gaseous elements present during heating
- Insulation or retardation of heat losses during transfer from heating to forging equipment
- Lubrication during the forging process

The formulation of the ceramic precoat is varied with the demands of the forging process being used, the alloy, and the forging type. Modification of the ceramic precoat formulation usually affects the melting or softening temperature, which ranges from 595 to 980 °C (1100 to 1800 °F) for most commercially available precoats for titanium alloys. Experience has shown that ceramic precoats with a viscosity of 20 to 100 Pa · s (200 to 1000 poise) at operating temperature provide optimal lubricity and desired continuous film characteristics for protecting the metal during heating and for preventing galling and metal pickup during forging. The actual formulations of ceramic precoats are often proprietary to the forger or the precoat manufacturer. Ceramic precoats are usually colloidal suspensions of the ceramics in mineral spirits or water, with the latter being the most common. Finally, most conventional titanium forging die design techniques include allowances for ceramic precoat thickness in sinking the die cavity to ensure the dimensional integrity of the final forging.

Ceramic precoats are applied using painting, dipping, or spraying techniques; state-of-the-art dipping and/or spraying processes are fully automated. Necessary ceramic precoat thicknesses vary with the precoat and the specific forging process, but generally fall in the range of 0.01 to 0.1 mm (0.0005 to 0.005 in.). Most ceramic precoats require a curing process following application to provide sufficient green strength for handling. Curing procedures range from drying at room temperature to automated furnace curing at temperatures to approximately 150 °C (300 °F).

Die lubricants are also used in the conventional closed-die forging of titanium alloys. Such die lubricants are subject to severe demands and are formulated to modify the surface of the dies to achieve the desired reduction in friction under conditions of very high metal temperatures and die pressures and yet leave the forging surfaces and forging geometry unaffected. Die lubricant formulations for titanium alloys are usually proprietary, developed either by the forger or the lubricant manufacturer. Die lubricant composition is varied with the demands of the specific forging process; however, the major active element in titanium alloy die lubricants is graphite. In addition, other organic and inorganic compounds are added to achieve the desired results because of the very high temperatures present. Carriers for titanium alloy die lubricants vary from mineral spirits to mineral oils to water.

Titanium alloy die lubricants are typically applied by spraying the lubricant onto the dies. Several pressurized air or airless systems are employed, and with high-volume, highly automated titanium alloy forging processes, die lubricant application is also automated by single or multiaxis robots. Some state-of-the-art application systems can apply very precise patterns or amounts of lubricant under fully automated conditions.

Insulation. In the conventional forging of titanium alloys in relatively slow strain rate processes such as hydraulic press forging, insulative materials in the form of blankets are often used to reduce metal temperature losses to the much cooler dies during the initial deformation stages. The insulative blankets are usually fabricated from fiberglass that is formulated to provide the necessary insulative properties. Blanket thickness varies with specific materials of fabrication and desired insulative properties, but generally ranges from 0.25 to 1.3 mm (0.010 to 0.050 in.). If insulative blankets are used, allowance is made in die sinking tolerances for modification of die cavity dimensions to ensure the dimensional integrity of the finished forging. Insulative blankets are usually applied to the dies immediately before insertion of the hot metal for forging.

Forging Process. The critical elements of the titanium conventional forging process (including metal/die temperature, strain rate, deformation mode, and the various forging processes and state-of-the-art forging capabilities reviewed above) must be controlled to achieve the desired final forging shape. Titanium alloy forgings are produced in enhanced forging and supporting equipment organized into cells that operate as advanced manufacturing systems and are then integrated with CAM concepts and other techniques. As with other materials, titanium alloy conventional forging is entering an era that is properly termed integrated manufacturing, in which all aspects of the titanium alloy forging process from design to execution are heavily influenced by computer technology.

Trimming is an intermediate operation that is necessary for the successful fabrication of conventional titanium alloy forgings. The flash generated in most closed-die titanium alloy forging processes is removed by hot trimming, sawing, flame cutting, or machining, depending on the size, complexity, and production volume of the part being produced. Hot trimming is generally the least expensive method and is used on relatively high volume small-to-intermediate size titanium alloy forgings. Most hot trimming punches are made from 6G or 6F2 die block material with hardnesses from 388 to 429 HB. Hot trimming blades are usually made from high-alloy steel, such as AISI D2, hardened to 58 to 60 HRC. Blades can be made from other materials that are usually hardfaced with cobalt-base alloy materials offered by several suppliers. Typically, the desired minimum flash temperature for the hot trimming of titanium alloys is 540 °C (1000 °F), although fewer trimming problems will occur if the flash temperature is as high as possible. Hot trimming is best accomplished in conjunction with the hot-forging process, rather than in separate heating and trimming operations. Cold trimming is rarely used for titanium alloys because the flash is very hard and may be brittle under such conditions, leading to unsatisfactory trimming or safety hazards.

Hot trimming is sometimes facilitated by the incorporation of certain design features into the die, the forging, or both. Figure 15 shows a flap hinge forging for which flash was distributed between upper and lower dies (Details A and B, Fig. 15). The dies were designed so that the flash would always be at a point where the draft was nearly vertical; therefore, the flash could be trimmed with minimal interference with the profile of the forging.

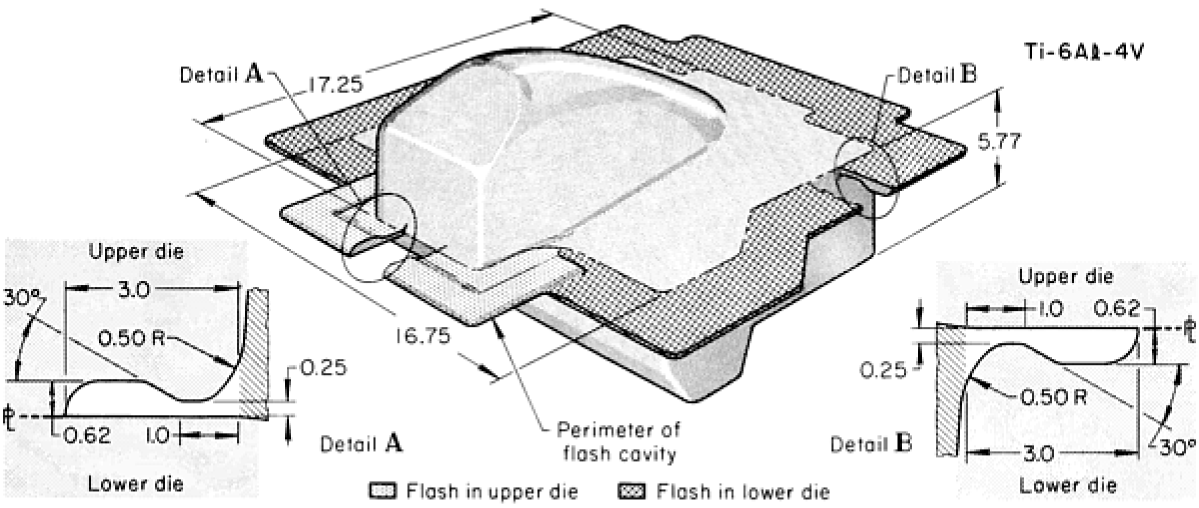


Fig. 15 Flap hinge forged in dies designed to provide uniform flash around the forging and to shift flash impression from upper to lower die. Dimensions given in inches.

The hot trimming of titanium alloy flash can be dangerous because the flash may shatter rather than trim or bend if the metal is allowed to cool below the above recommended temperature. Occasionally, a forging may jump in the impression during hammer forging and may be slightly out of position before the next blow can be stopped; unless protection is provided, flash may extrude between the dies and fly through the shop. Therefore, a flash trap should be used in the hammer forging of titanium alloys. This is usually accomplished by attaching a skirt to the top forging die. This skirt shields the striking face of the bottom die while the dies are separated. If flash breaks off, the skirt will intercept the pieces.

Machining and trimming operations are usually accomplished cold. Machine band sawing, with specially designed abrasive blades, has been shown to be an effective method of removing relatively thin titanium alloy flash where part volumes are low. Flame cutting is effective with large forgings and/or with thick flash where hot trimming is not feasible, because of either the size of the part or low part volume. Using oxy-gas, plasma, or other techniques, flash 50 mm (2 in.) or more in thickness can be successfully and economically removed. State-of-the-art flame cutting equipment used to trim titanium alloy forgings incorporates fixtures and automated systems that exploit CAD databases on titanium alloy forgings and CAM procedures. Depending on customer specifications and subsequent processing, the flame-cut flash may be repaired or left as cut. Flame cutting of flash should be accomplished prior to heat treatment so that the heat-affected zone (HAZ) is rendered machinable.

Machining, such as profile milling, can be employed on relatively low volume or intricate forgings, such as certain precision forgings, where other flash removal techniques may jeopardize the dimensional integrity of the forging.

Repair. As an intermediate operation between forging stages in most conventionally forged titanium alloys, repair of the forging is often necessary to remove surface discontinuities created by prior forging processes so that such defects do not affect the integrity of the final forging product. The necessity for intermediate repair is usually a function of the part complexity, the alloy, the forging processes, and other factors in the forging operation. For example, intermediate repair is generally required on structural shapes, but is often unnecessary on disk shapes. Compared to some other forged metals, titanium alloys are difficult to repair, requiring abrasive grinding techniques that are typically labor intensive.

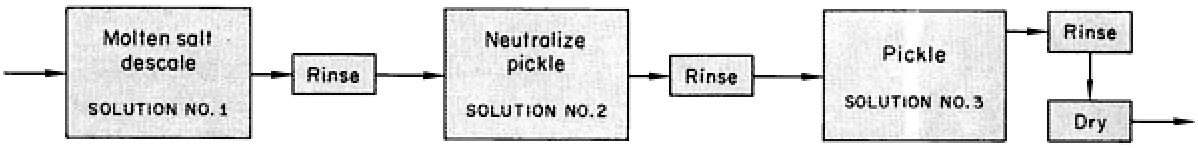
To facilitate the surface repair, titanium alloy forgings should be cleaned (discussed below) to remove the hard α case, which can cause excessive grinding tool wear. With some alloys, such as α alloys, surface repair is best accomplished after preheating the metal to about 260 to 370 °C (500 to 700 °F). Localized temperature increases may occur during abrasive grinding and, because of the poor thermal conductivity of titanium alloys, may create high thermal stresses. From the notch effect of the crack, these stresses in grinding may be high enough to propagate cracks during the repair process. Increasing the metal temperature on sensitive alloys reduces the stresses and decreases the probability of further cracking in repair. Soft silicon carbide rather than alumina grinding wheels should be used to minimize heat generation. Dye penetrant or liquid penetrant inspection techniques can be used on repaired titanium alloy forgings to ensure the removal of all surface discontinuities.

Cleaning. The oxide scale and underlying α case layers that form on all titanium alloys during heating for forging or in heat treatments are brittle and can promote cracking in subsequent forging or, in the case of finished forgings, can cause excessive machine tool wear during machining. Consequently, it may be desirable to remove the oxide and α case layers between successive forging operations, and it is mandatory to remove these layers from the finished forging before shipment to customers.

Cleaning techniques for titanium alloy forgings involve two processes--one for removing the oxide scale and the other for removing the α case layer. Scale removal can be accomplished by mechanical methods, such as gritblasting, or chemical methods, such as molten-salt descaling. Selection of the descaling method is based on part size, part complexity, and/or costs.

Gritblasting has been found to be effective in removing the scale layer, which can vary in thickness from 0.13 to 0.76 mm (0.005 to 0.030 in.). The media used in gritblasting can range from zircon sand to steel grit (typically 100 to 150 mesh) under air pressure (or equivalent) of up to 275 Pa (40 psi). Gritblasting is most frequently used on intermediate-to-large titanium alloy forgings, although it can be used for any size forging. Gritblasting equipment varies considerably, ranging from large horizontal table units to relatively small tumbling units. Gritblasting is followed by acid pickling (see below) to remove the α case.

Molten-salt descaling is another effective method of removing oxide scale and is also followed by acid pickling to remove the α case. Figure 16 shows a typical flow chart for a molten-salt descaling system followed by acid pickling. Molten-salt descaling must be closely controlled to prevent the work metal from becoming embrittled. The racks used in molten-salt descaling are usually wood, titanium, or stainless steel in order to prevent the generation of an electrical potential between the workpiece and the racks, which may result in preferential attack of the workpiece and arcing. Molten-salt descaling is most frequently used on small-to-intermediate size titanium alloy forgings, and in the case of high-volume forging parts, such systems are fully automated.



Solution No.	Type of solution	Composition of solution	Operating temperature		Cycle time, min
			°C	°F	
1	Descale	60-90% NaOH, rem $NaNO_3$ and Na_2CO_3	425-510	(800-950)	**20-50**
2	Neutralize	5-15% HNO_3 in H_2O	Room	Room	**2-5**

Fig. 16 Flow chart of operations for molten-salt descaling, neutralizing pickling, and final pickling of titanium alloys.

Acid pickling (sometimes referred to as chemical milling) is used to remove the underlying αcase, after the oxide scale has been removed, by the following procedure:

- Clean thoroughly with gritblasting or alkaline salt cleaning
- Rinse thoroughly in clean running water if alkaline cleaning has been used
- Pickle for 5 to 15 min in an aqueous nitric-hydrofluoric acid solution containing 15 to 40% HNO_3 and 1 to 5% HF and operated at 25 to 60 °C (75 to 140 °F). Usually, acid content of the pickling solution (particularly for α-β and βalloys) is near the middle of the above ranges (for example, from 30 to 35% HNO_3 and 2 to 3% HF, or an HNO_3 to HF ratio ranging 10:1 to 15:1). Alternatively, chemical solutions with approximately 2:1 ratio of HNO_3:HF have been found to remove 0.025 mm/min. (0.001 in./min) and to minimize hydrogen pickup.

The preferred bath operating temperature is 30 to 60 °C (90 to 140 °F). As the acid mixture is used, the titanium content in the bath increases and reduces the effectiveness of the bath. Titanium contents in excess of 12 g/L are usually considered to be maximum before the solution must be discarded. However, systems are available for reducing the contained titanium, including solution treatment/filtering and/or other organic chemical additions that can extend the life of pickling baths.

- Rinse parts thoroughly in clean water
- Rinse in hot water to hasten drying; allow to dry

The required metal removal and the pickling times achieved in acid pickling are dictated by several factors, including depth of α case to be removed, pickle tank operating conditions, process specification requirements, and potential for hydrogen pickup by the workpiece. Acid pickling presents the potential for excessive hydrogen pickup in titanium alloys; therefore, this process must be carefully controlled. Metal removal rates in acid pickling are usually 0.03 mm/min (0.001 in./min) or more, although the metal removal rate is heavily influenced by such factors as the alloy, acid concentrations, bath temperature, and contained titanium. Metal removal levels of 0.25 to 0.38 mm (0.010 to 0.015 in.) per surface are usually sufficient to remove the αcase; however, greater or lesser amounts of metal removal may be necessary, depending on the alloy and the specific conditions present for the forging in question.

Metal removal is monitored by witness pads on the forging (using an appropriate maskant), by test panels processed with the forgings, by actual forging measurement, or by other process control techniques. In addition, some process and/or materials specifications for titanium alloy forgings require verification of α case removal on the final forgings. The techniques used on representative samples of the lot of forgings include metallographic examination and/or microhardness measurements.

As a guide only, hydrogen pickup in acid pickling may be up to 10 ppm of hydrogen for each 0.03 mm (0.001 in.) of surface metal removal, depending upon specific pickling solution and concentration and temperature conditions. In acid pickling, αalloys tend to absorb less hydrogen than α-β alloys, which in turn tend to pick up less hydrogen than β alloys. Current process and/or material specifications for titanium alloy forgings always require measurement of final hydrogen content on each lot of forgings using either vacuum fusion or vacuum extraction techniques (typical specifications require maximum hydrogen contents in forgings of 125 to 150 ppm). Therefore, acid pickling parameters must be controlled--often to individual forging shapes and/or specific alloys--to avoid final hydrogen contents in excess of specification requirements, which can be corrected only by vacuum annealing. The potential for hydrogen pickup in acid pickling is significantly increased by decreased rates of metal removal (due to increased titanium content of the solution), higher bath temperatures (for example, bath temperatures higher than 60 °C, or 140 °F), and higher surface-area-to-volume relationships in the workpieces. Generally, the speed of metal removal through solution concentration and temperature must exceed the rate of hydrogen diffusion. With appropriate controls, acid pickling is used to remove precise amounts of material in order to remove αcase and/or to assist in obtaining the required forging dimensions (for example, in titanium precision forgings) without an undue increase in hydrogen content. Additional information on the cleaning of titanium alloys is available in the article "Surface Engineering of Titanium and Titanium Alloys" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Heat Treatment. Most titanium alloy forgings are thermally treated after forging, with heat treatment processes ranging from simple stress-relief annealing to multiple-step processes of solution treating, quenching, aging, and/or annealing designed to modify the microstructure of the alloy to meet specific mechanical property criteria. Selection of the heat treatment for titanium alloy forgings is based on the alloy, forging configuration, and mechanical property objectives. The furnaces used to thermally treat titanium alloy forgings are either continuous or batch gas-fired, electric, fluidized-bed, vacuum, or other specially designed equipment. Titanium alloy forgings that are heat treated in other than vacuum furnaces can be processed with or without ceramic precoats for protection from reaction during the thermal processes, depending on such factors as the alloy, the specific heat-treating equipment, the forging type (that is, conventional versus precision), and process/material specification requirements. The thermal treatments used for titanium alloys in forgings and other product forms are also discussed in Ref 4 and in the article "Heat Treating of Titanium and Titanium Alloys" in *Heat Treating*, Volume 4 of the *ASM Handbook.*

Annealing is used on forgings of most types of titanium alloys in order to remove the deformation and/or thermal stresses imparted as a result of forging hot-working processes and/or postforging cooling rates. Annealing is generally done in the temperature range of 595 to 925 °C (1100 to 1700 °F), depending on the specific alloy. It does not cause significant microstructural modification and is applied to conventional titanium alloy forgings primarily to facilitate the subsequent fabrication of the forgings, including machining.

Multiple-Step Heat Treatments. To modify the microstructure and resultant mechanical properties (such as strength, ductility, fatigue, creep, and fracture toughness) of many forged titanium alloys, multiple-step heat treatments (such as solution treatment plus aging/annealing, recrystallization annealing, duplex annealing, and so on) are often used. The terminology for these treatments is frequently borrowed from aluminum alloys; however, the metallurgical effects obtained are actually changes in allotropic phase relationships or phase morphology. As with the solution treatment of aluminum alloy forgings, if such multiple-step thermal treatment processes are applied to titanium alloy forgings, then racking procedures, quench rates, quench media, and so on, are the subject of forged titanium alloy heat treatment process specification and process control. Furthermore, as previously discussed, when preheating for forging, precoats, furnace atmosphere and/or furnace operating conditions in heat treatment of titanium alloy forging must be controlled to prevent excessive hydrogen pickup.

Straightening of titanium alloy forgings is often necessary in order to meet dimensional requirements. Unlike aluminum alloys, titanium alloys are not easily straightened when cold, because the high yield strength and modulus of elasticity of these alloys result in significant springback. Therefore, titanium alloy forgings are straightened primarily by creep straightening and/or hot straightening (hand or die), with the former being considerably more prevalent. Creep straightening of most alloys may be readily accomplished during annealing and/or aging processes with the temperatures prevalent during these processes; however, if the annealing/aging temperature is below about 540 to 650 °C (1000 to 1200 °F), depending on the alloy, the times needed to accomplish the desired creep straightening can be extended. Creep straightening is accomplished with rudimentary or sophisticated fixtures and loading systems, depending on part complexity and the degree of straightening required. In hot hand or die straightening, which are used most frequently on small-to-intermediate size forgings, the forgings are heated to the annealing or aging temperature, hot straightened, and then stress relieved at a temperature below that used during hot straightening.

Inspection of titanium alloy forgings takes two forms: in-process inspection and final inspection. In-process inspection techniques, such as statistical process control and/or statistical quality control, are used to determine that the product being manufactured meets critical characteristics and that the forging processes are under control. Final inspection, including mechanical property testing, is used to verify that the completed forging product conforms to all drawing and specification criteria. The final inspection procedures used on titanium alloy forgings are discussed below.

Dimensional Inspection. All final titanium alloy forgings are subjected to dimensional verification. For open-die forgings, final dimensional inspection may include verification of all required dimensions on each forging or, by using statistical sampling plans, on groups or lots of forgings. For closed-die forgings, conformance of the die cavities to drawing requirements, a critical element in dimensional control, is accomplished before placing the dies in service by using layout inspection of plaster or plastic casts of the cavities. With the availability of CAD databases on forgings, such layout inspections can be accomplished more expediently with CAM-driven coordinate-measuring machines or other automated inspection techniques. With verification of die cavity dimensions prior to use, final titanium part dimensional inspection can be limited to verification of critical dimensions controlled by the process, such as die closure, and to the monitoring of changes in the die cavity. Given the abrasive nature of titanium alloys during forging, die wear is a potential problem that can be detected by appropriate final inspection. Further, with high-definition and precision titanium forgings, CAD databases and automated inspection equipment (such as coordinate-measuring machines and 2-D fiber optics) can often be used for actual part dimensional verification.

Heat Treatment Verification. Hardness is not a good measure of the adequacy of the thermomechanical processes accomplished during the forging and heat treatment of titanium alloys, unlike most aluminum alloys and many heat-treatable ferrous alloys. Therefore, hardness measurements are not used to verify the processing of titanium alloys. Instead, mechanical property tests (for example, tensile tests and fracture toughness) and metallographic/microstructural evaluation are used to verify the thermomechanical processing of titanium alloy forgings. Mechanical property and microstructural evaluations vary, ranging from the destruction of forgings to the testing of extensions and/or prolongations forged integrally with the parts. Further discussion on testing and metallographic methodologies for titanium alloy forgings is available in *Mechanical Testing*, Volume 8, and *Metallography and Microstructures*, Volume 9 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

Nondestructive Evaluation. Titanium alloy forgings are often submitted to nondestructive evaluation to verify internal and surface quality. The surface of conventional titanium alloy forgings after forging and cleaning is relatively good--inferior to aluminum alloy forgings but generally superior to low-alloy steel forgings. A surface finish of 250 rms or better is considered normal for conventionally forged and acid pickled titanium alloy forgings, although precision forged surfaces may be smoother than 250 rms under closely controlled forging conditions and in certain types of titanium forgings.

The selection of nondestructive evaluation requirements depends on the final application of the forging. In addition to the detailed high-resolution ultrasonic inspection frequently performed on critical titanium alloy forging stock before forging (as noted above), the final titanium alloy forgings can also be submitted to ultrasonic inspection. With conventional open-die or closed-die forgings that will be machined on all surfaces, visual inspection after a good etch or chemical mill is adequate for detection of surface defects. Surface inspection techniques, such as penetrant inspection, can be performed, but are not recommended; because of the surface roughness typical of conventional titanium alloy forgings, spurious indications are frequently encountered that result in excessive inspection/repair costs for nonvalid indications. However, for precision titanium forgings, whose surfaces are typically superior to those of open-die or other closed-die titanium alloy forgings, liquid penetrant, eddy current, and other surface inspection techniques are used. Additional information on surface and internal inspection techniques and inspection criteria is available in *Failure Analysis and Prevention*, Volume 11 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

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4. E.W. Collings, Ed., *The Physical Metallurgy of Titanium Alloys,* American Society for Metals, 1984, p 181-207

Selection of Forging Method

Selection of the optimal titanium forging method (that is, open-die versus closed-die, and within closed die: blocker, conventional, high-definition, or precision forging) involves the application of value analysis techniques. Although titanium alloys are considerably more expensive than other materials, such as aluminum and ferrous alloys, specific economic results are highly part dependent. Except when mechanical properties, required grain flow, and/or specific program objectives dictate the use of a specific forging method, there are several fabrication options that are competitive candidates for the manufacture of titanium alloy shapes. The relative cost relationships between the options for titanium alloys are similar to those described for aluminum alloys in the article "Forging of Aluminum Alloys" in this Volume.

However, with titanium alloys, forging processes and methods that increase overall recovery from forged shape to finished part and reduce machining costs may have a more significant impact on total final part costs than with other materials because of the very high material costs and higher machining costs for titanium alloys as compared to ferrous or aluminum-base materials. The high material and machining costs associated with titanium alloys often result in lower break-even points (that is, lower quantities) for more expensive forging processes such as conventional, high-definition, and precision forging than for less expensive but more metal-intensive processes such as plate hog-outs, open-die forgings, or blocker-type forgings. The potential reduction in expensive material losses and machining costs through the redesign of a representative titanium alloy conventional forging is illustrated in Fig. 7 and 8(a) through (c) for a large main landing gear beam.

Selection of the most economical forging method for a given shape in titanium alloys is a process that must include consideration of all the intrinsic and extrinsic costs of manufacture, both on the part of the forger and the user. Further, as the size of the titanium alloy forging sought increases to very large parts, such as the large landing gear beams illustrated in Fig. 7 and 11, the range of possible forging methods and forging design sophistication may be restricted because of the forging process requirements for, and the difficulty in forging, titanium alloys versus the available capacity of the forging equipment.

Forging of Titanium Alloys

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Forging Advanced Titanium Materials

The above review of titanium alloy conventional forging technology is based on existing commercially available wrought titanium alloys. However, titanium alloy/materials development, using ingot metallurgy and other techniques, is providing advanced titanium materials that may present additional challenges in the manufacture of conventional forgings. Three of the major classes of titanium-base materials currently under development are:

- A new class of alloys based on intermetallic compounds
- Titanium powder metallurgy materials
- Titanium-base metal-matrix composites

Currently, none of these titanium materials developments has matured sufficiently for specific alloy formulations to be discussed; however, it is appropriate to review some of the critical demands these new materials approaches will place on forging as a cost-effective method of making advanced titanium alloy shapes.

Titanium Aluminides. A new class of elevated-temperature titanium alloys is emerging that is based on intermetallic compounds with aluminum, along with additions of other alloying elements to make these alloys workable and to achieve the desired mechanical property combinations. Titanium aluminide alloys are based on two compounds: Ti_3Al or α-2, and TiAl or γ. Titanium aluminide alloys have been found to offer elevated-temperature characteristics that are competitive with those of super-alloys at a significantly reduced density. Initial α-2 alloys have been found to be workable by forging, while initial α alloys may not be workable by deformation processes such as forging.

Preliminary α-2 titanium aluminide alloys have been found to display very high β_t values--higher than existing α titanium alloys (for example, 1040 to 1150 °C, or 1900 to 2100 °F). Further, these preliminary alloys have deformation characteristics that are considerably more difficult than those of existing α titanium alloys and similar to those of nickel/cobalt-base superalloys. However, under properly defined metal deformation conditions, some titanium aluminide α-2 alloys have been made to behave superplastically. It appears that the necessary forging processes will be similar to those used for some difficult-to-fabricate α titanium alloys and that carefully controlled conventional, hot-die, and/or isothermal forging techniques will be necessary for successful forging fabrication.

Titanium Powder Metallurgy (P/M) Materials. Several rapid-solidification, chemical reduction, and/or blending technologies are being used to produce titanium alloy P/M materials, either on a limited commercial scale or on a research scale. Most current efforts are directed toward alternate fabrication of components through powder metallurgy for existing alloys (Table 1). In many cases, the forging process has been found to contribute to the successful fabrication of final components from P/M-base titanium alloys through enhanced thermomechanical processing, microstructural modification, and/or improved component quality as a result of the deformation achieved in forging. Although most current titanium alloy P/M producing methods, particularly rapid solidification, are expensive, some evidence suggests that overall fabrication costs and the recovery of certain components can be significantly improved by combining P/M and forging processes. Future titanium alloy P/M development is expected to include alloys that are specifically formulated for P/M technology, and as with other materials (such as the nickel/cobalt-base superalloys), titanium forging can be combined with P/M consolidation (through vacuum hot pressing, hot isostatic pressing, and so on) to achieve cost-effective shapes with the desired and/or unique properties.

Titanium Metal-Matrix Composites. Using P/M-base titanium alloys and other techniques, titanium-base discontinuous metal-matrix composites are also being explored for the development of enhanced titanium materials with unique mechanical property capabilities. As discussed in the previous section, the controlled deformation typical of forging has often been successfully employed in the fabrication of experimental components from such composite titanium materials. The matrix titanium alloys used include existing and developmental alloys with a variety of ceramic whisker/particulate materials. The reactivity of titanium with many candidate ceramic compounds is of concern for the successful development of this technology. Currently, titanium-base metal-matrix alloy/materials development is an embryonic technology; however, the forging process can be expected to play a significant role in the fabrication technology for these materials.

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Titanium Alloy Precision Forgings

As with aluminum alloys (see the article "Forging of Aluminum Alloys" in this Volume), titanium alloy precision forgings can be identified by a variety of terminologies; however, in each case, this product form requires significantly reduced and/or no final machining on the part of the user (detailed information on precision forging is available in the article so titled in this volume). Precision forged titanium alloys are a significant commercial forging product that is undergoing major growth in usage and has been the subject of major forging process technology development and capital investment by the forging industry. For the purpose of this article, the term net precision titanium forging will be defined as a product that requires no subsequent machining by the user, and the term near-net precision titanium forging will be defined as a product requiring some metal removal (typically accomplished in a single machining operation) by the user. Fabrication of net or near-net titanium alloy precision forgings is determined by the alloy being forged and by value analysis for fabrication of the most cost-effective precision forged product.

The first precision forged titanium alloy products commercially produced were turbine engine compressor and fan blades (see Fig. 17); conventional forging process techniques were used. With hot-die/isothermal forging techniques (see the article "Isothermal and Hot-Die Forging" in this Volume), very complex cross-section, precision forged airframe components such as the splice angle shown in Fig. 18 are being manufactured. Titanium alloy precision forgings are produced with very thin webs and ribs; sharp corner and fillet radii; undercuts, backdraft, and/or contours; and, frequently, multiple parting planes (which may optimize grain flow characteristics) in the same manner as aluminum alloy precision forgings.

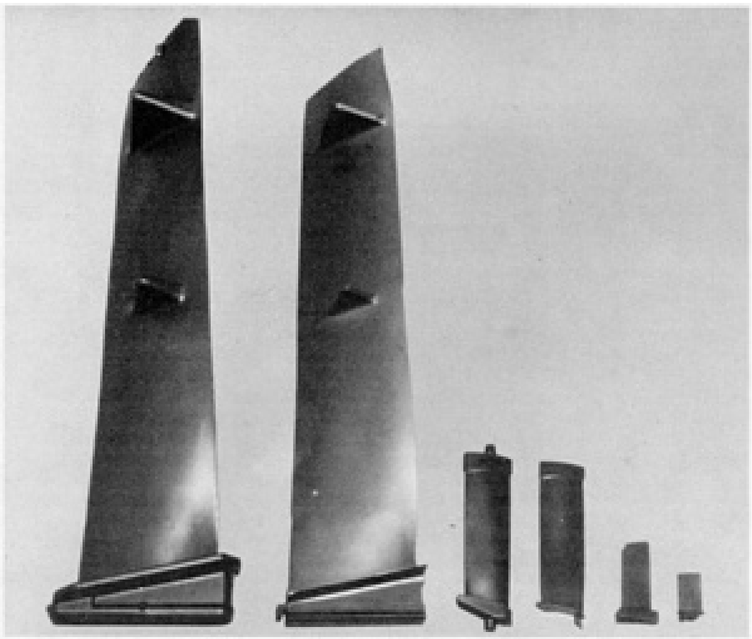

Fig. 17 Three pairs of precision forged Ti-6Al-4V airfoils. Left member of each pair is as-forged; right member, as finish machined. The largest of the three pairs of airfoils measures approximately 152 mm (6 in.) wide at base and 610 mm (24 in.) long.

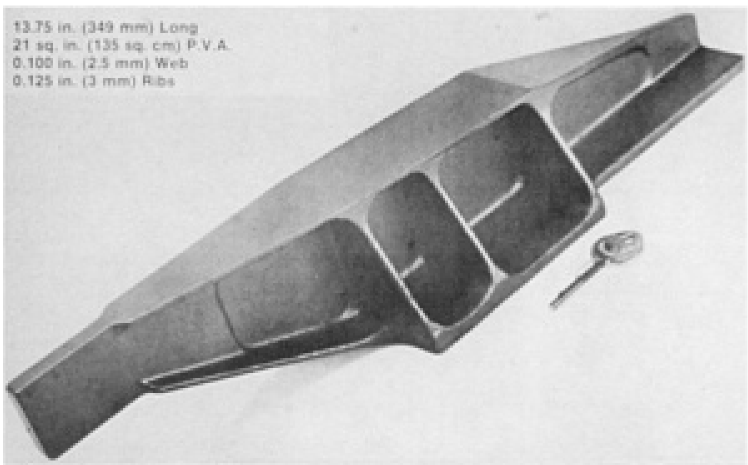

Fig. 18 Precision forged alloy Ti-6Al-6V-2Sn and alloy Ti-10V-2Fe-3Al splice fitting produced using hot-die/isothermal forging techniques to illustrate shape complexity capabilities of the process.

Design Criteria. The design and tolerance criteria for precision titanium forgings are similar to those for aluminum alloy precision forgings and have been established to provide a finished product suitable for assembly or subsequent fabrication by the user. Precision titanium alloy forgings, with the exception of airfoils, do not necessarily conform to the same tolerances provided by machining of other product forms; however, as indicated in Table 4, design and tolerance criteria for titanium precision forgings are highly refined in comparison to other titanium alloy forging types and are suitable for the intended application of the product. If the standard precision forging design and tolerance criteria are not sufficient for the final component, then the forging producer frequently combines conventional and/or hot-die/isothermal forging with machining to achieve the most cost-effective method of fabrication to the required tolerances on the finished part.

Table 4 Net titanium alloy precision forging design/tolerance criteria for selected parts and processes for metastable β and α-β alloys

Feature	**Current**	Goal
PVA, m^2 ($in.^2$)	Up to 0.193 (300)	**0.290 (450)**
Length, mm (in.)	Up to 1015 (40)	**1525 (60)**
Length/thickness tolerance, mm (in.)	+0.5, -0.25 (+0.020, -0.010)	**+0.75, -0.25 (+0.030, -0.010)**
Contour tolerance, mm (in.)	±0.38 (±0.015)	**±0.63 (±0.025)**
Draft		
Outside	0°; +30, -0°	**Same**
Inside	1°; +30, -1°	**Same**
Corner radii, mm (in.)	1.5; +0.75, -1.5 (0.060; +0.030, -0.060)	**Same**
Fillet radii, mm (in.)	3.3; +0.75, -1.5 (0.130; +0.030, -0.060)	**Same**
Straight within, mm (in.)	0.25 each 254 mm (0.010 each 10 in.)	**Same**
Minimum web thickness, mm (in.)	2.3 (0.090)[a]	**2.5 (0.100)**
Minimum rib thickness, mm (in.)	**2.3 (0.090)[a]**	**2.5 (0.100)**

(a) In some designs and under some processing conditions, minimum web thickness can be as thin as 1.5 mm (0.060 in.) and minimum rib thickness can be as thin as 2.0 mm (0.080 in.).

The titanium precision forging design and tolerance criteria achievable may vary with the alloy type because all titanium alloys are not necessarily equivalent in workability using either conventional forging techniques or hot-die/isothermal forging technology. Generally, the net titanium precision forging design parameters given in Table 4 apply to more readily workable β and metastable β alloys (such as Ti-10V-2Fe-3Al) and selected designs and forging processes for α-β alloys (such as Ti-6Al-4V and Ti-6Al-6V-2Sn). However, with more difficult-to-fabricate α titanium alloys and certain forging designs and/or forging processes for α-β alloys, the more cost-effective forging technique may be near-net titanium precision forgings with modified design criteria (for example, typically 1.5 to 2.3 mm, or 0.060 to 0.090 in., machining allowance per surface), and modified rib/web thickness, fillet radii, corner radii, and so on) but with the same dimensional tolerances outlined in Table 4. Table 4 also indicates that as the size of the net titanium precision forging is increased to 0.290 m^2 (450 in.^{2}), some modification in design and tolerance criteria is appropriate.

Tooling and Design. Precision titanium forging uses several tooling concepts to achieve the desired design shape, with the specific tooling concept based on the design features of the precision forging and the forging process used. Similar tooling design concepts outlined for aluminum alloys (see Fig. 11(a) to (c) in the article "Forging of Aluminum Alloys" in this Volume) are also used with titanium alloys. For conventional forging processes for titanium precision forgings, of which turbine airfoils are the primary example, the two-piece upper and lower die concept is the predominant approach. The other tooling concepts shown in Fig. 11(b) and in the article "Forging of Aluminum Alloys" are used in the hot-die or isothermal forging of titanium precision forgings.

For conventional titanium precision forgings, the die materials employed in tooling are either 6F2 or 6G types or hot-work die materials such as H12 and H13. Tooling for conventional titanium precision forgings is designed and produced using the same techniques as those described above for other forging types; however, CNC direct die sinking and/or EDM electrode manufacture from CAD forging and tooling databases has been found to be particularly effective for the manufacture of the close-tolerance tooling demanded by precision titanium forgings.

The die materials used for the hot-die/isothermal forging of titanium alloys are reviewed in the article "Isothermal and Hot-Die Forging" in this Volume. Selection of the die material is based on the alloy to be forged, necessary forging process conditions (for example, metal/die temperatures, die stresses, strain rate, and total deformation), forging part design, and cost considerations. Cast, wrought, and/or consolidated powder techniques are used to fabricate die blocks/inserts from superalloy materials, including Alloy 718, Waspaloy, Udimet 700, Astroloy, Alloy 713LC (Ni-12Cr-6Al-4.5Mo-2Nb-0.6Ti-0.1Zr-0.05C-0.01B), and Alloy 100 (Ni - 15.0Co - 10.0Cr - 5.5Al - 4.7Ti-3.0Mo - 1.0V - 0.6Fe - 0.15C - 0.06Zr-0.015B), with these materials listed in order of increasing temperature capability from 650 to 980 °C (1200 to 1800 °F). Most of these die materials require more expensive nonconventional machining techniques for die sinking, with electrode discharge machining being the most prevalent technique. Computer-aided design part and tooling databases have also been effectively combined with CAM-driven CNC EDM electrode manufacturing techniques to reduce the cost of die manufacture. Typically, the manufacture of a set of dies for titanium precision forging with hot-die/isothermal forging costs up to seven times that required for the dies for the manufacture of the same part in aluminum. Heated holder and insert techniques can reduce the cost factor for titanium hot-die/isothermal precision forging dies to three times the cost of the same dies for an aluminum alloy.

Forging Processing. Conventional and hot-die/isothermal forging processes for precision titanium forgings use the same steps as those outlined above for other forging types. Precision titanium forgings can be produced from wrought stock, preformed shapes, or blocker shapes, depending on the complexity of the part, the tooling system being employed, and cost considerations. For example, for the conventional forging of airfoil shapes such as blades, multiple forging processes are used (because of the high cost of raw materials) to prepare the preshape necessary for the successful fabrication of the precision part in order to conserve input material and to facilitate the precision forging process. Precision titanium forging stock fabrication and inspection criteria are similar to those described above for other titanium alloy forging types.

Unlike aluminum alloy precision forging shapes, conventionally forged titanium alloy precision forgings are usually not produced in multiple operations in finish dies, but rather by a progression of processes in multiple die sets. However, with hot-die/isothermal forging processes for precision titanium parts, multiple operations in a given die set are used. Conventionally forged titanium precision forgings are usually produced on mechanical and/or screw presses, although hammers or hydraulic presses are occasionally used for certain designs. For hot-die/isothermally fabricated precision titanium forgings, hydraulic presses are used exclusively to obtain the desired slow strain rates and controlled deformation conditions. The mechanical and/or screw presses currently used for the fabrication of conventional titanium precision forgings range up to 150 MN (17,000 tonf) (maximum press capability of up to 280 MN, or 31,000 tonf, for the largest screw press), and hydraulic presses for the hot-die/isothermal precision forging processing of titanium alloys range up to 90 MN (10,000 tonf). Other large hydraulic presses, up to 310 MN (35,000 tonf), with necessary forging process capabilities are available for the hot-die/isothermal forging of titanium (as well as aluminum alloy precision forging) as this titanium alloy forging technology is scaled-up in size.

Conventional and hot-die/isothermal forging process criteria for the precision forging of titanium alloys are similar to those described above for other titanium alloy forging types. With conventional forging, the metal and die temperatures used are usually controlled to be near the upper limits of the temperature ranges outlined in Tables 1 and 2 to enhance producibility and to minimize unit pressures. The hot-die and isothermal forging parameters employed in the precision forging of titanium alloys (see the article "Isothermal and Hot-Die Forging" in this Volume) use the metal temperatures listed in Table 1. Die temperature selection in hot-die/isothermal forging is based on the alloy, die material/die heating system, specific forging process demands (for example, the viability of near-isothermal/hot die versus isothermal conditions), sophistication of the forging design, and thermomechanical processing criteria.

Because of the stringent dimensional tolerances associated with conventionally and hot-die/isothermally forged titanium precision forgings, dies are typically heated using state-of-the-art on-press heating systems, such as resistance and/or induction heating. These heating systems maintain uniform die temperatures, typically ±14 °C (± 25 °F) or better, in order to reduce dimensional variations. As with other forging types, precoating and die lubrication are critical elements in the conventional forging of titanium precision forgings, and the precoats and die lubricants used are similar to those for other forging types, although lubricant materials are often specially formulated for an individual forging design and forging process. Insulative blankets are generally not used for the conventional forging of precision titanium forgings, because such materials may adversely affect the dimensional integrity of the forged parts.

Die heating and lubrication techniques for the hot-die/isothermal forging of titanium alloys are described in the article "Isothermal and Hot-Die Forging" in this Volume. Gas-fired, infrared, resistance, and/or induction heating systems are selected based on the die temperature to be achieved, die temperature uniformity criteria, tooling system employed, and cost considerations. These systems must heat the die stack to the required temperature and maintain the heated dies at consistent temperatures--typically ±14 to 28 °C (±25 to 50 °F). The precoats used in the hot-die/isothermal forging of titanium alloys are selected or formulated for specific metal/die temperature conditions. Under some conditions, parting agents such as boron nitride are used on the dies to facilitate part removal with minimum distortion.

Straightening is often a critical process in the manufacture of conventionally or hot-die/isothermally forged titanium precision forgings. The straightening techniques used, with airfoils as a critical example, are predominantly die straightening procedures with the metal and dies at elevated temperatures. In this process, time-temperature-pressure parameters are controlled, usually with small-to-intermediate size hydraulic presses, to achieve the desired deformation conditions and therefore the dimensional conformance. Hot-die or isothermal forming techniques (with dies at temperatures from 705 to 925 °C, or 1300 to 1700 °F) are often used to straighten conventionally or hot-die/isothermally forged titanium alloy precision forgings, particularly large airfoil shapes.

Forging stock preparation; thermal treatments; in-process cleaning, trimming, and repair; and in-process and final inspection and thermal treatment verification processes, with the exception of nondestructive evaluation, are the same as those described above for other titanium alloy forging types. Because of the highly configured nature and thin sections typical of precision titanium parts, ultrasonic inspection cannot be used on finished parts; the exception is turbine engine disks, which are usually inspected using highly sophisticated, automated ultrasonic inspection equipment. Frequently, for airframe precision titanium forgings, airfoils, and other precision titanium shapes, the detailed ultrasonic inspection performed on the forging stock before fabrication is sufficient to ensure satisfactory internal quality in the final part. Unlike other titanium alloy forging types, precision titanium forgings, which are used in service with most (if not all) of the as-forged surfaces intact, are frequently inspected by sensitive liquid penetrant inspection techniques to ensure adequate surface quality.

Precision titanium forgings are frequently supplied as a completely finished product that is ready for assembly by the user. In such cases, the forging producer can use both conventional milling and unconventional machining techniques, such as chemical milling and electrode discharge machining, along with forging, to achieve the most cost-effective finished titanium shape. Further, the forging producer can apply a wide variety of surface finish and/or coating processes to this product as specified by the purchaser. More information on surface finish and coating processes for titanium alloys is available in the article "Surface Engineering of Titanium and Titanium Alloys" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Technology Development Effectiveness. Figure 19 presents a summary of the history and future of the state-of-the-art in the size of titanium alloy precision forging that can be produced. Figure 19 differentiates between net and near-net precision titanium alloy forging technology development because not all titanium alloys are equally producible under either conventional or hot-die/isothermal forging approaches, and in order to ensure the fabrication of the most cost-effective final product, as described above, both net and near-net titanium precision forgings are used commercially.

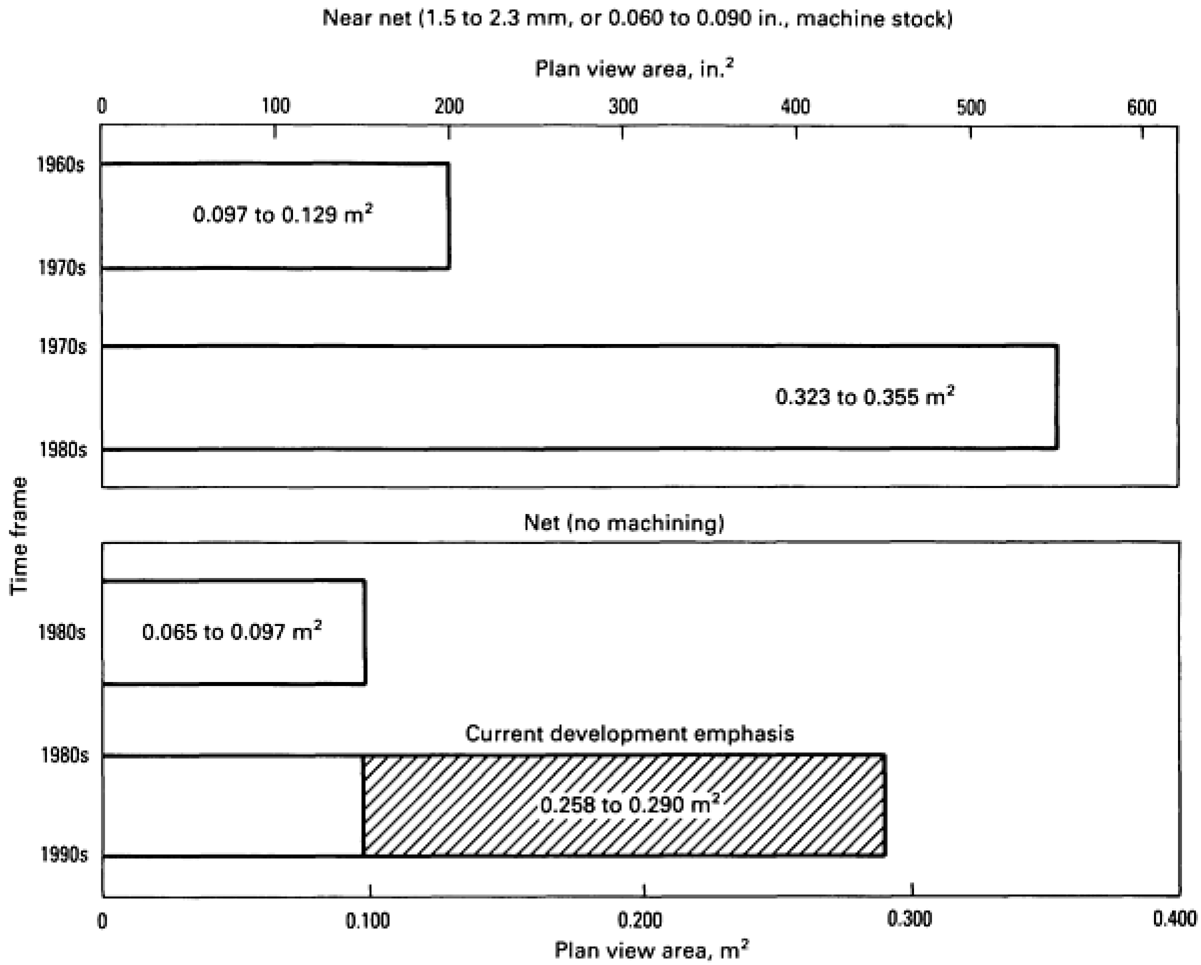

Fig. 19 Past and future near-net and net titanium alloy precision forging capabilities gaged in terms of plan view area.

As a result of both conventional and hot-die/isothermal forging technology developmental efforts, the size of the net titanium precision forging that can be fabricated to the design and tolerance criteria given in Table 4 has tripled--from 0.081 m^2 (125 in.^{2}) to over 0.194 m^2 (300 in.^{2}) PVA. The critical elements in projected changes in the state-of-the-art for titanium precision forgings, both in terms of size and cost effectiveness, are enhanced precision forging process control, CAD/CAM/CAE technologies, advanced and/or integrated manufacturing technologies, enhanced die heating systems, improved lubrication systems, and the availability of large superalloy die blocks necessary for the hot-die/isothermal forging of these alloys.

The selection of precision titanium forging from the various methods available for achieving a final titanium shape is based on the value analyses conducted for each individual shape in question. Figure 20 shows a cost comparison for an engine mount part (Fig. 20a) manufactured by machining from Ti-6Al-4V plate, by machining from a Ti-6Al-4V conventional forging, and produced as a precision forging in Ti-10V-2Fe-3Al using hot-die/isothermal forging. In the analysis shown in Fig. 20(b), the precision forging is always less costly than the machined conventional forging, and the break-even point between the precision forging and the machined plate hog-out occurs in as few as 40 pieces. The costs used in this analysis included all material, tooling, setup, and fabrication costs for each method of manufacture. Analyses of other parts have also shown that titanium precision forged shapes are highly cost effective in comparison with other fabrication approaches, particularly when the other methods require multiple-axis machining techniques to achieve the final part geometry.

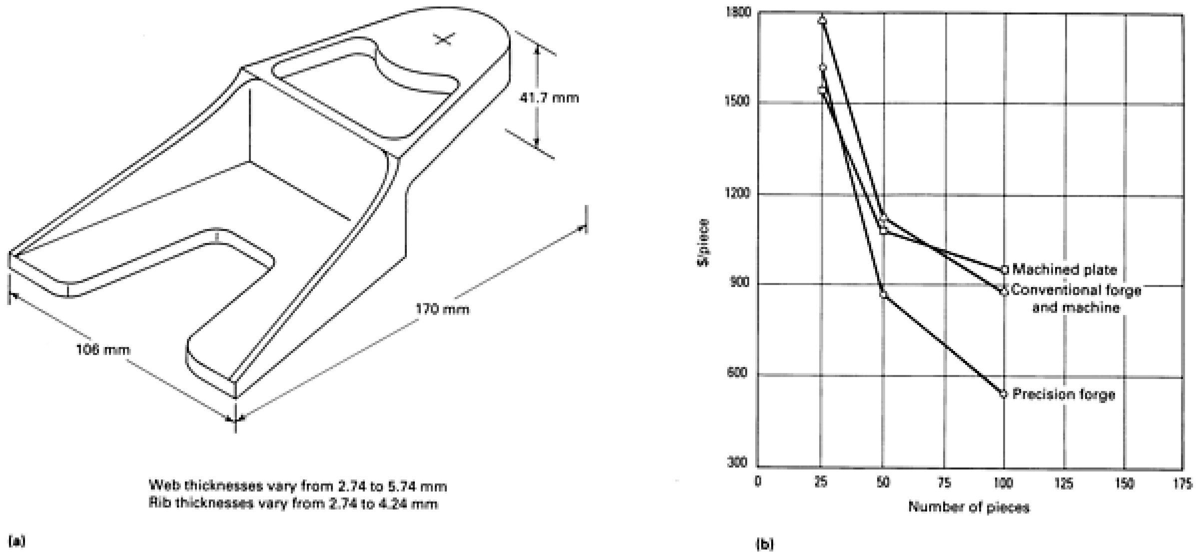

Fig. 20 Cost comparison for an engine mount part. (a) Net-shape precision forged Ti-10V-2Fe-3Al engine mount produced by hot-die/isothermal forging. (b) Cost compression of the engine mount shown to illustrate the cost-effectiveness of precision forging.

As outlined in the article "Forging of Aluminum Alloys" in this Volume, forging industry and user evaluations of precision titanium alloy forgings have indicated that final part costs can be reduced by 80 to 90% or more in comparison to machined plate, and by 60 to 70% or more in comparison to machined conventional forgings. With potential cost reductions such as these, it is evident that further growth in precision titanium forging usage can be anticipated.

Forging of Titanium Alloys

G.W. Kuhlman, Aluminum Company of America

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Introduction

COLD HEADING is a cold-forging process in which the force developed by one or more strokes (blows) of a heading tool is used to upset (displace) the metal in a portion of a wire or rod blank in order to form a section of different contour or, more commonly, of larger cross section than the original. The process is widely used to produce a variety of small- and medium-sized hardware items, such as bolts and rivets. Cold heading, however, is not limited to the cold deformation of the ends of a workpiece nor to conventional upsetting; metal displacement may be imposed at any point, or at several points, along the length of the workpiece and may incorporate extrusion in addition to upsetting. In cold heading, the cross-sectional area of the initial material is increased as the height of the workpiece is decreased. Advantages of the process over machining of the same parts from suitable bar stock include:

- Almost no waste material
- Increased tensile strength from cold working
- Controlled grain flow

Although cold heading is principally used for the production of heads on rivets or on blanks for threaded fasteners, a variety of other shapes can also be successfully and economically formed by the process. Figure 1 illustrates the cold heading of an unsupported bar or wire on a horizontal machine.

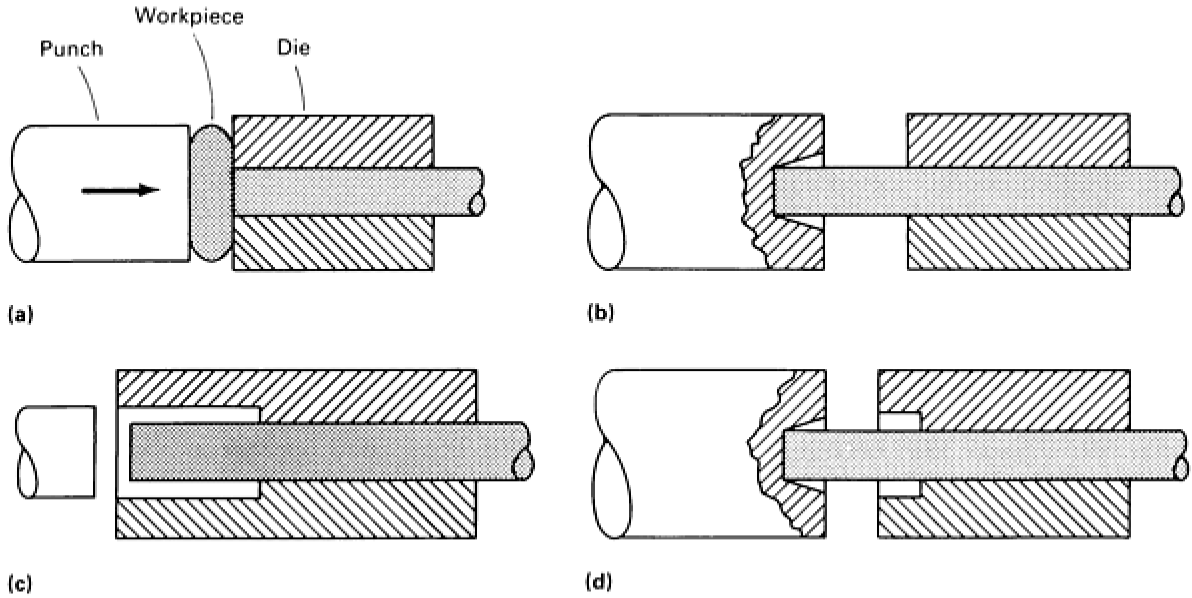


Fig. 1 Schematics of the cold heading on an unsupported bar in a horizontal machine. (a) Head formed between punch and die. (b) Head formed in punch. (c) Head formed in die. (d) Head formed in punch and die.

Materials for Cold Heading

Cold heading is most commonly performed on low-carbon steels having hardnesses of 75 to 87 HRB. Copper, aluminum, stainless steels, and some nickel alloys can also be cold headed. Other nonferrous metals and alloys, such as titanium, beryllium, magnesium, and the refractory metals and alloys, are less formable at room temperature and may crack when cold headed. These metals and alloys are sometimes warm headed (see the section "Warm Heading" in this article).

Carbon and Alloy Steels. Steels containing up to about 0.20% C are the easiest materials to cold head. Medium-carbon steels containing up to 0.40 to 0.45% C are fairly easy to cold work, but formability decreases with increasing carbon and manganese content. Alloy steels with more than 0.45% C, as well as some grades of stainless steel, are very difficult to cold head and result in shorter tool life than that obtained when heading low-carbon steels.

Microstructure also influences the upsettability of steels. The work material can sometimes be cold worked during the wire-drawing process, resulting in an increase in tensile strength and difficulty in cold heading. Large deformations or difficult-to-work materials often require process or spheroidization annealing before cold heading.

Stainless Steels. Some stainless steels, such as the austenitic types 302, 304, 305, 316, and 321 and the ferritic and martensitic types 410, 430, and 431, can be cold headed. These materials work harden more rapidly than carbon steels and are therefore more difficult to cold head. More power is required, and cracking of the upset portion of the work metal is more likely than with carbon or low-alloy steels. These problems can be alleviated by preheating the work metal (see the section "Warm Heading" in this article).

Rating Formability. Metals and alloys are rated for cold heading on the basis of the length of stock, in terms of diameter, that can be successfully upset. Equipped with flat-end punches, most cold-heading machines can upset to approximately two diameters of low-carbon steel wire per stroke. If the unsupported length is increased beyond about two diameters, the stock is likely to fold onto itself, as shown in Fig. 2. For more formable metals, such as copper and some copper alloys, the length of upset per stroke may be up to four diameters (Ref 1). Punches and dies can, however, be designed to increase the headable length of any work metal. For example, with a coning punch (Fig. 3) or a bulbing punch, it is possible to head as much as 6 diameters of low-carbon steel stock in two strokes.

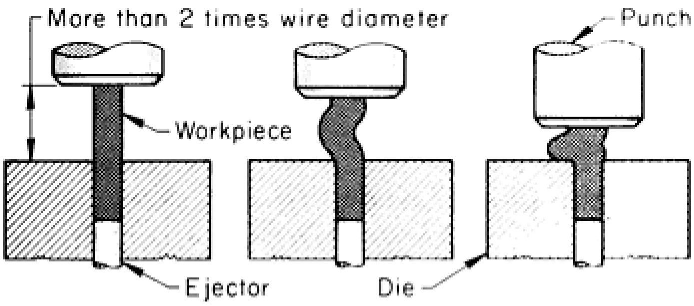


Fig. 2 Typical folding effect obtained with a flat-end punch when heading low-carbon steel having an unsupported length of more than about 2 diameters.

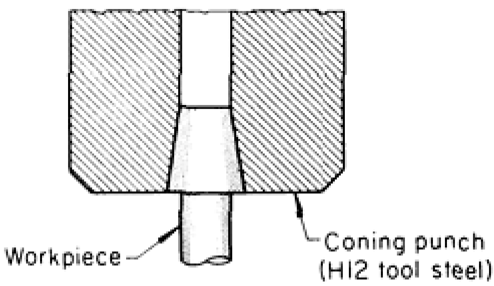


Fig. 3 Use of a coning punch in the first blow of a two-blow heading operation to enable upsetting of up to 6 diameters in two strokes.

Headability is sometimes expressed as the heading limit, which is the ratio of the diameter of the largest possible headed portion to the diameter of the stock. There is usually a direct relationship between reduction of area in a tensile test and heading limit as defined above.

Reference cited in this section

1. "Upsetting," technical brochure, National Machinery Company, 1971, p 11

Cold Heading

Equipment

Standard cold headers are classified according to two characteristics:

- Whether the dies open and close to admit the work metal or are solid
- The number of strokes (blows) the machine imparts to the workpiece during each cycle

The die in a single-stroke machine has one mating punch; in a double-stroke machine, the die has two punches. The two punches usually reciprocate so that each contacts the workpiece during a machine cycle.

Single-stroke solid-die headers are made in sizes of $\frac{1}{8}$, $\frac{3}{16}$, $\frac{1}{4}$, $\frac{5}{16}$, $\frac{3}{8}$, $\frac{1}{2}$, $\frac{5}{8}$, $\frac{3}{4}$, and 1 in. These sizes refer to the approximate diameter of stock that can be headed. Because they are single-stroke machines, product design is limited to less than two diameters of stock to form the head. Single-stroke extruding can also be done in this type of machine. These machines are used to make rivets, rollers and balls for bearings, single-extruded studs, and clevis pins.

Double-stroke solid-die headers are available in the same sizes as single-stroke solid-die headers. These machines can make short-to-medium length products (usually 8 to 16 diameters long), and they can make heads that are as large as three times the stock diameter. These machines can be equipped for relief heading, which is a process for filling out sharp corners on the shoulder of a workpiece, or a square under the head.

Some extruding can also be done in these machines. Because of their versatility over single stroke cold headers, double-stroke solid-die headers are extensively used in the production of fasteners.

Single-stroke open-die headers are made for smaller-diameter parts of medium and long lengths and are limited to heading 2 diameters of stock because of their single stroke. Extruding cannot be done in this type of machine, but small fins or a point can be produced by pinching in the die, if desired. Similar machines are used to produce nails.

Double-stroke open-die headers are made in a wider range of sizes than single-stroke open-die headers and can produce heads as large as three times stock diameter. They cannot be used for extrusion, but they can pinch fins on the workpiece, when required. They will generally pinch fins or small lines under the head of the workpiece when these are not required; if these fins or lines are objectionable, they must be removed by another operation.

Three-blow headers utilize two solid dies along with three punches and are classified as special machines. Having the same basic design as double-stroke headers, these machines provide the additional advantage of extruding or upsetting in the first die before double-blow heading or heading or trimming in the second die. Three-blow headers combine the process of trapped extrusion and upsetting in one single machine to produce special fasteners having small shanks but large heads. These headers are also ideal for making parts with stepped diameters in which the transfer of the workpiece would be accomplished with great difficulty.

Transfer and progressive headers are solid-die machines with two or more separate stations for various steps in the forming operation. The workpiece is automatically transferred from one station to the next. These machines can perform one or more extrusions, can upset and extrude in one operation, or can upset and extrude in separate operations. Maximum lengths of stock of various diameters headed in these machines range from 152 mm (6 in.) with 3.8 in. diameter to 255 mm (10 in.) with $\frac{3}{4}$ in. diameter. These machines can produce heads of five times stock diameter or more.

Boltmaking machines are solid-die headers similar to transfer and progressive headers, but they can trim, point, and roll threads. Boltmaking machines usually have a cut-off station, two heading stations, and one trimming station served by the transfer mechanism. An ejector pin drives the blank through the hollow trimming die to the pointing station. The trimming station can be used as a third heading station, or for extruding. Boltmaking machines are made for bolt diameters $\frac{3}{16}$, $\frac{1}{4}$, $\frac{5}{16}$, $\frac{3}{8}$, $\frac{1}{2}$, $\frac{5}{8}$, $\frac{3}{4}$, 1, and $1\frac{1}{4}$ in.

Rod headers are open-die headers having either single or double stroke. They are used for extremely long work (8 to 160 times stock diameter). The workpiece is cut to length in a separate operation in another machine and fed manually or automatically into the rod header.

Reheaders are used when the workpiece must be annealed before heading is completed--for example, when the amount of cold working needed would cause the work metal to fracture before heading was complete. Reheaders are made as either open-die or solid-die machines, single or double stroke, and can be fed by hand or hopper. Punch presses are also used for reheading.

Nut formers generally have four or five forming dies and a transfer mechanism that rotates the blank 180° between one or two dies or all the dies. Therefore, both ends of the blank are worked, producing workpieces with close dimensions, a fine surface finish, and improved mechanical characteristics. A small slug of metal is pierced from the center of the nut, which amounts to 5 to 15% waste, depending on the design of the nut.

Operation. Most cold-heading machines used in high production are fed by coiled wire stock. The stock is fed into the machine by feed rolls and passes through a stationary cutoff quill. In front of the quill is a shear-and-transfer mechanism. When the wire passes through the quill, the end butts against a wire stop or stock gage to determine the length of the blank to be headed. The shear actuates to cut the blank. The blank is then pushed out of the shear into the transfer, which positions the blank in front of the heading die. The heading punch moves forward and pushes the slug into the die; at the same time, the transfer mechanism releases the slug and moves back into position for another slug.

In the die, the slug is stopped by the ejector pin, which acts as a backstop and positions the slug with the correct amount protruding for heading. In a single- or double-stroke header, the heading operation is completed in this die, and the ejector pin advances to eject the finished piece. In a progressive header or a boltmaking machine, the transfer mechanism has fingers in front of each of several dies. After each stroke, the ejector pin pushes the workpiece out of the die. The transfer mechanism grips it and advances it to the next station. In boltmaking machines, the last station in the heading area is a trimming station. The trimming die (which is on the punch side) is hollow, and the die ejector pin drives the trimmed workpiece completely through the die and, by an air jet or other means, through a tube to the pointing station.

Pointers are of two types. Some have cutters that operate much like a pencil sharpener in putting a point on the workpiece (thus producing some scrap); others have a swaging or extruding device that forms the point by cold flow of the metal.

The pointed workpiece is placed in a thread roller. A boltmaking machine has a thread roller incorporated into it. The rolling dies are flat pieces of too] steel with a conjugate thread form on their faces. As the workpiece rolls between them, the thread form is impressed on its shank, and it drops out of the dies at the end, often as a finished bolt.

Tools

The tools used in cold heading consist principally of punches and dies. The dies can be made as one piece (solid dies) or as two pieces (open dies), as shown in Fig. 4.

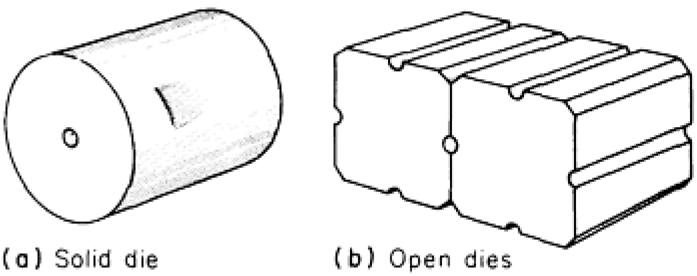


Fig. 4 Solid (one-piece) and open (two-piece) cold-heading dies.

Solid dies (also known as closed dies) consist of a cylinder of metal with a hole through the center (Fig. 4a). They are usually preferred for the heading of complex shapes. Solid dies can be made entirely from one material, or can be made with the center portion surrounding the hole as an insert of a different material. The choice of construction depends largely on the length of the production run and/or complexity of the part. For extremely long runs, it is sometimes desirable to use carbide inserts, but it may be more economical to use hardened tool steel inserts in a holder of less expensive and softer steel.

When a solid die is made in one piece, common practice is to drill and ream the hole to within 0.076 to 0.13 mm (0.003 to 0.005 in.) of finish size before heat treatment. After heat treatment, the die is ground or honed to the desired size.

Solid dies are usually quenched from the hardening temperature by forcing the quenching medium through the hole, making no particular attempt to quench the remainder of the die. By this means, maximum hardness is attained inside the hole; the outer portion of the die is softer and therefore more shock resistant.

Because the work metal is not gripped in a solid die, the stock is cut to length in one station of the header, and the cut-to-length slug is then transferred by mechanical fingers to the heading die. In the heading die, the slug butts against a backstop as it is headed. Ordinarily, the backstop also serves as an ejector.

Open Dies (also called two-piece dies) consist of two blocks with matching grooves in their faces (Fig. 4b). When the grooves in the blocks are put together, they match to form a die hole as in a solid die. The die blocks have as many as eight grooves on various faces so that as one wears, the block can be turned to make use of a new groove. Because the grooves are on the outer surface of the blocks, open-die blocks are quenched by immersion to give maximum hardness to the grooved surfaces. Open dies are usually made from solid blocks of tool steel, because of the difficulty involved in attempting to make the groove in an insert set in a holder. Open dies are made by machining the grooves before heat treating, then correcting for any distortion by grinding or lapping the grooves after heat treating.

In open-die heading, the dies can be permitted to grip the workpiece, like the gripper dies in an upsetting machine. When this is done, the backstop required in solid-die heading is not necessary. However, some provision for ejection is frequently incorporated into open-die heading.

Design. The shape of the head to be formed in the workpiece can be sunk in a cavity in either the die or the punch or sometimes partly in each. The decision on where to locate the cavity often depends on possible locations of the parting line on the head. It must be possible to extract the workpiece from both the punch and the die. It is generally useful, but not entirely necessary, to design some draft in the workpiece head for ease of ejection.

An important consideration in the design of cold-heading tools is that the part should stay in the die and not stick in the punch. Therefore, it is particularly difficult to design tooling for midshaft upsets. Where possible, the longest part of the shank is left in the die. There is less of a problem with open dies that use a special die-closing mechanism. Some punches are equipped with a special synchronized ejector mechanism to ensure that the workpiece comes free.

At best, cold heading imposes severe impact stress on both punches and dies. Minor changes in tool design often register large differences in tool life, as described in the following example.

Example 1: Improvements in Heading Tool Design That Eliminated Tool Failure.

The recessed-head screw shown in Fig. 5(a) was originally headed by the heading tool shown in Fig. 5(b). After producing only 500 pieces, the tool broke at the nib portion ("Point of failure," Fig. 5b).

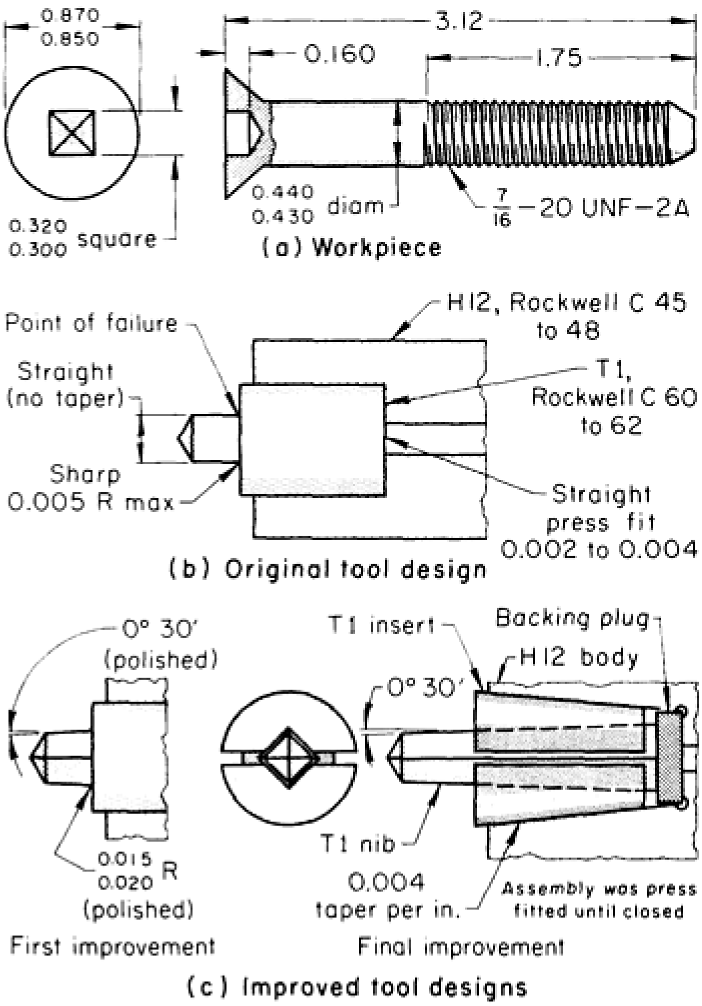


Fig. 5 Improvements in heading tool design to eliminate tool failure in the production of recessed-head screws. Dimensions given in inches.

The design of the heading tool was improved by adding a radius and a slight draft to the nib (Fig. 5c). The entire nib was then highly polished. The redesigned tools produced 12,000 to 27,000 pieces before breakage occurred, but this tool life was still unacceptable.

A final design improvement is shown at the right in Fig. 5(c). The nib was made to fit a split holder, using a slight taper to prevent the nib insert from being pulled from the split holder as the header withdrew from the workpiece. Tools of this design did not break and produced runs of more than 100,000 pieces before the nib was replaced because of wear.

Tool Materials

The shock loads imposed on cold-heading tools must be considered in the selection of tool materials. For optimal tool life, it is essential that both punches and dies have hard surfaces (preferably 60 HRC or higher). However, except for the heading of hard materials, the interior portions of the tools must be softer (40 to 50 HRC, and sometimes as low as 35 HRC for larger tools), or breakage is likely.

To meet these conditions, shallow-hardening tool steel such as W1 or W2 is extensively used for punches and open dies and for solid dies made without inserts. Inserts are commonly made from higher-alloy tool steels, such as D2 or M2, or from tungsten carbide having a relatively high percentage of cobalt (13 to 25%).

Shock-resistant tool steel such as S1 is also used for the cold heading of tools, especially for the heading of intricate shapes when a tool steel such as W1 has failed by cracking. The shock-resistant steels are generally lower in hardness than preferred for maximum resistance to wear, but it is often necessary to sacrifice some wear resistance to gain resistance to cracking.

Producing bolts that have square portions under the heads or dished heads or both can result in tool failure. Under these conditions, a change in grade of steel for the tools is sometimes mandatory.

Preparation of Work Metal

The operations required for preparing stock for cold heading may include heat treating, drawing to size, machining, descaling, cutting to length, and lubricating.

Heat Treating. The cold-heading properties of most steels are improved by process annealing, spheroidizing, or stress relieving. In general, process annealing is done at the steel mill on steels with low-to-medium carbon content. Additional heat treatment is not used unless required, for at least two reasons:

- The process could cost more than any savings realized in cold heading
- Cold-headed products often depend for their final strength on work hardening before and during the heading process, and if reannealed before cold heading, they may lose much of their potential strength

Carbon steels (1000 series) with up to about 0.25% C are usually cold headed in the mill-annealed condition as received from the steel supplier. If the heading is severe, they can be reannealed at some stage in the heading operations, but they are rarely given a full anneal before cold heading. Carbon steels (1000 series) with 0.25 to 0.44% are also mill annealed. However, because higher carbon content decreases workability, they are sometimes normalized or annealed above the upper transformation temperature; more frequently, a spheroidizing treatment is used. Carbon steels that contain more than 0.44% C, most modified carbon steels (1500 series), and all alloy steels are fully spheroidized. Heat-treating methods for steels and nonferrous metals are described in *Heat Treating*, Volume 4 of the *ASM Handbook.* In practice, experience often indicates the need for annealing or spheroidizing to prevent cracking of the work metal or to obtain acceptable tool life or both.

Drawing to size produces stock of uniform cross section that will perform as predicted in dies that have been carefully sized to fill out corners without flash or die breakage. Drawing to size also improves strength and hardness when these properties are to be developed by cold work and not by subsequent heat treatment.

Turning and Grinding. Drawn wire can have defects that carry over into the finished workpiece, exaggerated in the form of breaks and folds. Seams in the raw material that cause these defects may not be deep enough to be objectionable in the shank or body of a bolt, but can cause cracks in the head during cold heading or subsequent heat treatment. Surface seams and laps can be removed by turning, grinding, or shaving at the wire mill or by machining the headed product.

Descaling. Work metal that has been heat treated usually needs to be descaled before cold heading. Scale can cause lack of definition, defects on critical surfaces, and dimensional inaccuracy of the workpiece.

Methods of descaling include abrasive blasting, water jet blasting, pickling, wire brushing, and scraping. Selection of method depends largely on the amount of scale present and on the required quality of the surfaces on the headed workpieces. Acid pickling is usually the least expensive method for complete removal of heavy scale (see the articles on surface engineering of specific metals in *Surface Engineering*, Volume 5 of the *ASM Handbook*).

Cutting to Length. In a header that has a shear-type cutoff device as an integral part of the machine, cutting to length by shearing is a part of the sequence. In applications in which cutting to length is done separately, shearing is the method most commonly used for bars up to about 50 mm (2 in.) in diameter (see the article "Shearing of Bars and Bar Sections" in this Volume). For larger diameters, sawing is generally used. Gas cutting and abrasive-wheel cutting are used less often than shearing and sawing.

Lubrication. Although some of the more ductile metals can be successfully cold headed to moderate severity without a lubricant, most metals to be cold headed are lubricated to prevent galling of the dies, sticking in the dies, and excessive die wear. Lubricants used include lime coating, phosphate coating, stearates and oils, and plating with softer metals such as copper, tin, or cadmium.

The ultimate in lubrication for steel to be cold headed is a coating of zinc phosphate with stearate soap--the same as used for the cold extrusion of steel (see the article "Cold Extrusion" in this Volume). A similar treatment is often used for aluminum. However, for workpieces produced entirely by cold heading, this treatment is seldom necessary, except for extremely severe heading.

In the cold heading of carbon and alloy steel wire, common practice is to coat the work metal with a dry lubricant during the last draw. The lubricants most often used are calcium stearate or aluminum stearate. First, the wire is pickled to remove scale, dirt, and any previous coatings. It is then coated with lime, phosphate, or borax, which acts as a base coating. Calcium or aluminum stearate is added as a dry lubricant. The lubricant sticks to the base coating and is fused by the heat developed when the wire passes through the drawing die. For severe heading, extrusion oils are sometimes used (often in addition to the treatments given above) in the header/former, particularly when experience has proved that oil will improve results.

Stainless steel is usually electroplated with copper and then lubricated with oil or molybdenum disulfide. Oxalates are sometimes used instead of the copper plating.

In the cold heading of nonferrous metals, the need for lubrication varies from metal to metal. Nickel-base alloys, especially the high-strength alloys, require very good lubrication. These metals are usually copper plated and then given a stearate coating. The coatings are later removed with nitric acid.

The more formable nickel-base alloys are usually also copper plated. If the heading is not severe, however, they can be headed with a stearate coating only, which can be removed with hot water. Nitric acid cannot be used on Monel, because the acid will attack the base metal.

Copper-base alloys have the least need for lubrication. For normal heading operations, oil or drawing compound is added at the header. For severe heading, a stearate coating can be added during the last draw of the wire. Sulfurized oil should not be used for cold heading of copper-base alloys unless some staining can be tolerated.

Aluminum header wire is generally coated with stearate. Aluminum needs more lubrication for cold heading than copper, but much less than nickel.

In all cold heading, best practice is to use the simplest and the least lubricant that will provide acceptable results, for two reasons:

- Excessive amounts of lubricant may build up in the dies, resulting in scrapped workpieces or damaged dies
- Removal of lubricant is costly (the cost of removing lubricant usually increases in proportion to the effectiveness of the lubricant)

Complex Workpieces

Cold-headed products that have more than one upset portion need not be formed in two heading operations; many can be made in one operation of a double-stroke header. The length of stock that may be partly upset is generally limited to five times the diameter of the wire. The only other limitation is that the header must be able to accommodate the diameter and length of wire required for the workpiece.

Three pieces, each with two end upsets, that were made completely in one operation in a double-stroke open-die header are shown in Fig. 6(a). These parts were made at a rate of 80 pieces per minute. Production rate is limited only by the speed of the machine used, not by the item being produced.

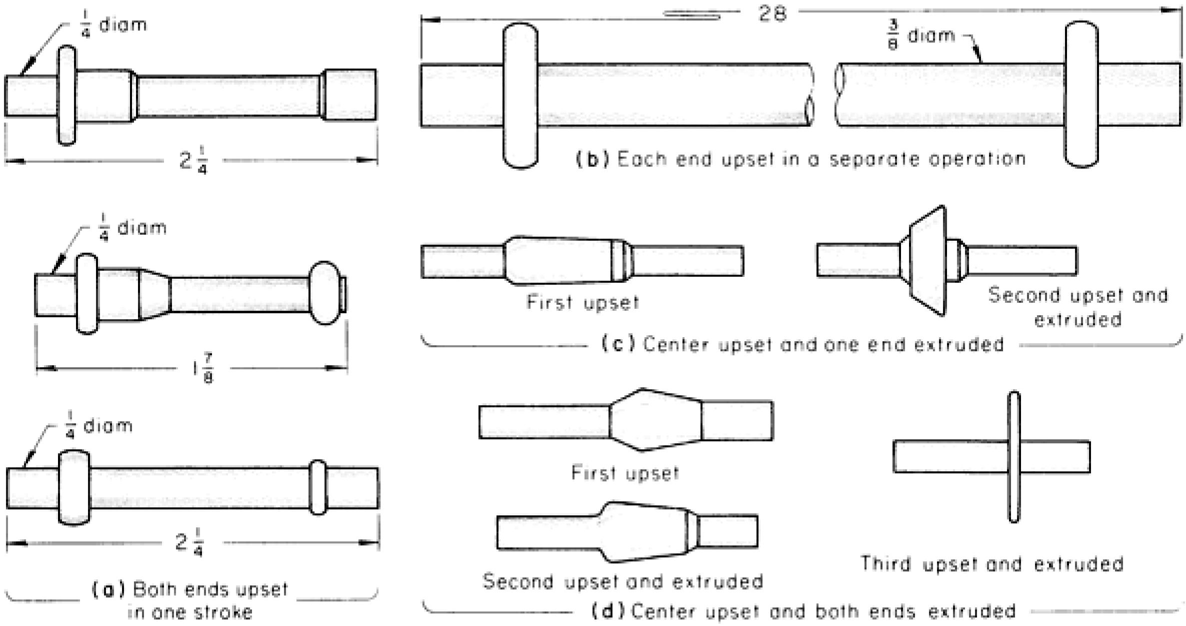


Fig. 6 Typical parts with center upsets or upsets at both ends. Dimensions given in inches.

The product becomes more expensive when the upsetting operation has to be performed twice, as in production of the 710 mm (28 in.) long axle bolt shown in Fig. 6(b). This part required two upsetting operations because the die in a standard double-stroke cold header was not long enough to form both upsets in the machine at the same time. One or more additional operations may be needed for workpieces that require pointing as well as a complex upset.

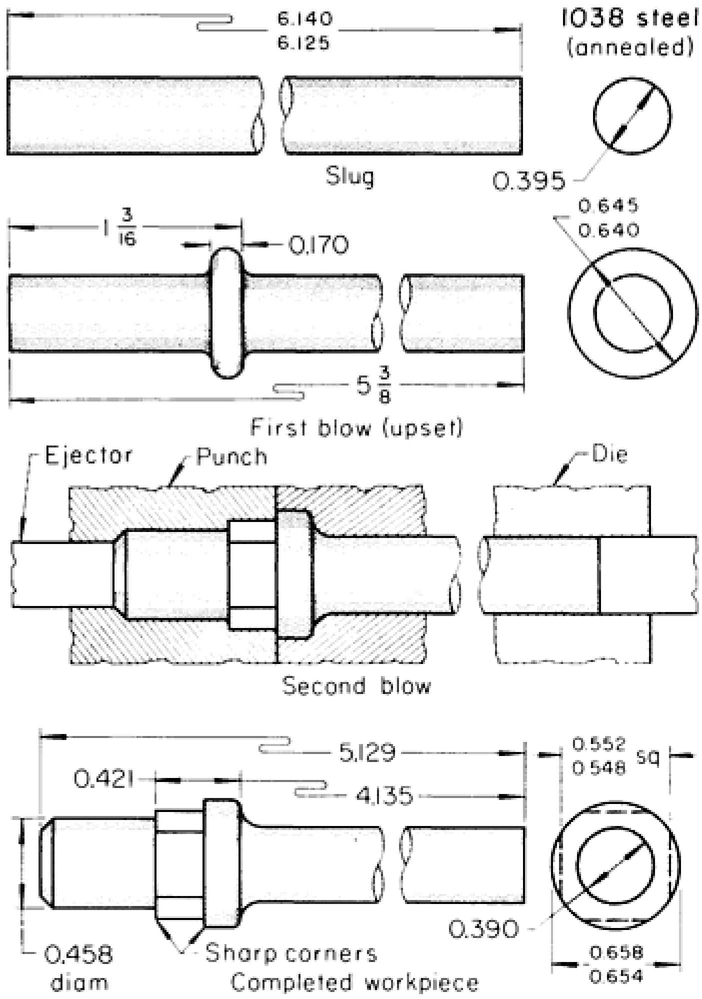
Center Upsetting. Most cold heading involves forming an upset at the end of a section of rod or wire. However, the forming of upsets at some distance from the end is common practice.

The trailer-hitch-ball stud shown in Fig. 6(c) is representative of an upset performed midway between the ends of the wire blank. This stud was upset and extruded in two strokes in a $\frac{3}{4}$ in. solid-die machine. The diameter of one end section is smaller than that of the original wire, and the round center collar is flared out to more than $2\frac{1}{2}$ times the wire diameter. The center-collar stud shown in Fig. 6(d) is another example of a center upset. Both ends of the stud were extruded below wire size, while the center collar was expanded to more than three times the original wire diameter. This stud was formed in three strokes in a progressive header.

Control of the volume of work metal to prevent formation of flash and to prevent excessive loads on the tools is important in most cold-heading operations. In center upsetting, control of metal volume is usually even more important, not only to prevent flash and tool overload but also to prevent folds. A technique used successfully in one application of center upsetting is described in the following example.

Example 2: Production of a Complex Center Upset in Two Blows.

A blank for a bicycle-pedal bolt (Fig. 7) required sharp corners on the edges and corners of the square portion and a complete absence of burrs or fins in the collar area. In heading, any excess pressure applied on the collar portion to fill the corners and edges of the square resulted in flash or overfill on the collar portion. It was necessary to upset the collar portion in one blow and to form the square in a second blow in order to fabricate this part successfully (Fig. 7). The folds generally produced by this technique were avoided by careful control of size. By forming the collar completely during the first blow and almost completely confining it during the second blow, the remainder of the metal was controlled so that it could be directed into filling the square. Therefore, the pressure needed to form and fill the square was confined to this area and not allowed to cause further upsetting in any other portion. Accurate control of the headed volume depended on the accuracy of the cut blank and of the collar formed in the first blow.



Machine	$\frac{1}{2}$ in. boltmaking machine
Tool material	M2 inserts, 62-64 HRC
Lubricant	Stearate on stock
Production rate	4200 pieces per hour[(a)]
Tool life	10,000-15,000 pieces

(a) At 100% efficiency

Fig. 7 Production of a 1038 steel blank for a bicycle-pedal bolt in two blows on a cold upsetter. Dimensions given in inches.

Economy in Cold Heading

Cold heading is an economical process because of high production rates, low labor costs, and material savings. Production rates range from about 2000 to 50,000 pieces per hour, depending on part size. Fewer machines are needed to meet production requirements than with other processes, resulting in reduced costs for equipment, maintenance, and floor space. Labor costs are minimal because most operations are performed automatically, requiring labor only for setup, supervision, and parts handling.

Material savings results from the elimination or reduction in chips produced. When cold heading is combined with other operations, such as extrusion, trimming, and thread rolling, the savings is considerable (see the section "Combined Heading and Extrusion" in this article). Subsequent machining or finishing of the cold-headed parts is usually not necessary. This can be especially beneficial when relatively expensive work materials are used. The following example describes the replacement of machining by cold heading to reduce production costs of a copper alloy nozzle component.

Example 3: Machining Replaced by Cold Heading to Save Material.

A blank for a threaded copper alloy C10200 (oxygen-free copper) nozzle component (Fig. 8) was originally produced by machining from bar stock. A material savings of more than 50% was effected by producing the component by cold heading rather than machining. The same shape and dimensional accuracy were produced by both methods. In both cases, threads were rolled in a separate operation.

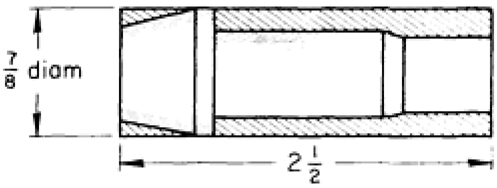


Fig. 8 Copper alloy C10200 nozzle component blank that was originally machined but was switched to cold heading to save the work metal indicated by the shaded regions. Dimensions given in inches.

Dimensional Accuracy

Work can be produced to much closer tolerances in cold headers than in hot headers. Tolerances on parts produced by single-stroke headers need to be wider than on parts given two or more blows. Rivets, often formed in single-stroke machines, have tolerances of ±0.38 mm (±0.015 in.) except where otherwise specified. Shanks for rolled threads are allowed only ±0.038 mm (±0.0015 in.). Small parts can usually have closer tolerances than large parts. Tolerances can often be maintained as close as 0.025 mm (±0.001 in.), although maintenance of a tolerance this close increases product cost; requires careful control of machines, tools, and work metal; and is unusual in practice.

The following example demonstrates tolerance capabilities and shows dimensional variations obtained in production runs of specific cold-headed products.

Example 4: Variation in Dimensions of a Valve-Spring Retainer Produced in a Nut Former.

The valve-spring retainer shown in Fig. 9 was produced from fine-grain aluminum-killed 1010 steel (No. 2 bright annealed, cold-heading quality) in a five-station progressive nut former. To determine the capabilities of the machine and tools for long-run production, several thousand pieces were made from three separate coils. Distribution charts were prepared for two critical dimensions on randomly selected parts made from each coil. Results are plotted in Fig. 9. Lots 1, 2, and 3 include parts made from the three different coils. As a further test of machine and tool capabilities, the tooling was set to a mean taper dimension for lot 1, high side for lot 2, and low side for lot 3.

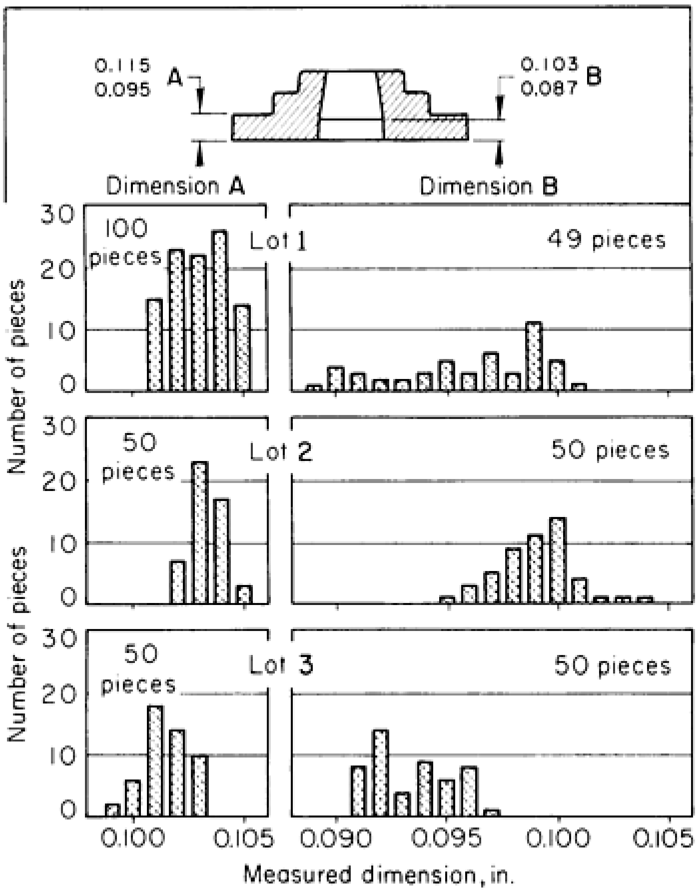


Fig. 9 Variations in dimensions of 1010 steel valve spring retainers randomly selected from three lots. Parts were produced in a five-station nut former. Dimensions given in inches.

The accuracy that could be maintained on thickness of a flat surface is demonstrated in Fig. 9. Although specifications permitted a total variation of 0.51 mm (0.020 in.) on seat thickness, actual spread did not exceed 0.13 mm (0.005 in.) for parts made from the three coils. A greater total variation was experienced for the taper-depth dimension. When the tools were set for mean, the total variation was 0.33 mm (0.013 in.) which was still within the 0.41 mm (0.016 in.) allowable (lot 1). With tools set for high side, total variation was only 0.25 mm (0.010 in.), although one part was 0.025 mm (0.001 in.) out of the allowable range (lot 2). Optimal results were obtained on the taper dimension when tools were set for the low side (lot 3); total spread was only 0.18 mm (0.007 in.).

Surface Finish

Surfaces produced by cold heading are generally smooth and seldom need secondary operations for improving the finish. Surface roughness, however, can vary considerably among different workpieces or among different areas of the same workpiece, depending on:

- Surface of the wire or bar before heading
- Amount of cold working in the particular area
- Lubricant used

- Condition of the tools

Cold drawing of the wire before cold heading will improve the final surface finish. The best finish on any given workpiece is usually where direct contact has been made with the tools, such as on the top of a bolt head or on an extruded shank portion where cold working is severe.

The lubricant is likely to have a greater effect on the appearance of a headed surface than on surface roughness as measured by instruments. For example, heavily limed or stearate-coated wire produces a dull finish, but the use of grease or oil results in a high-luster finish.

The condition of the tools is most important in controlling the workpiece finish. Rough surfaces on punches or dies are registered on the workpiece. Therefore, the best surface finish is produced only from tools that are kept polished.

The ranges of finish shown on the square-necked bolt in Fig. 10 are typical for such a part when headed from cold-drawn steel, using ground and polished tools. The best finish is on the top of the head and on the extruded shank, while the poorest finish is on the outer periphery of the round head.

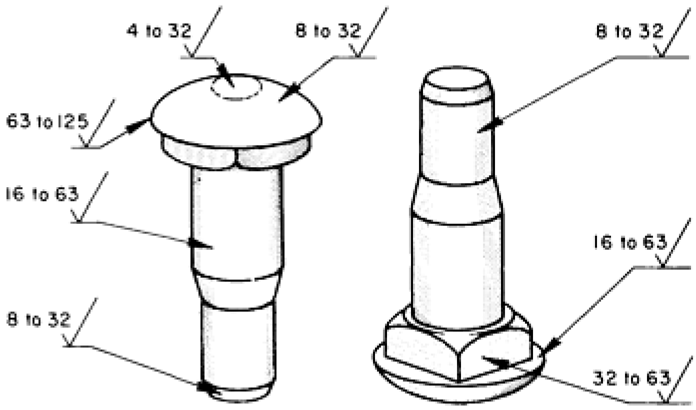


Fig. 10 Typical variations in surface roughness at various locations on a square-necked bolt headed from cold-drawn steel with ground and polished tools. Roughness given in microinches.

Combined Heading and Extrusion

It is common practice to combine cold heading with cold extrusion, and this often permits the selection of a work metal size that greatly lessens forming severity and prolongs tool life. Two parts shown in Fig. 6, a trailer-hitch-ball stud (Fig. 6c) and a center-collar stud (Fig. 6d), reflect the flexibility in design obtained by combining center upsetting and extrusion. In addition to increased tool life, other advantages can sometimes be obtained by combining cold heading and cold extrusion, as shown in the following two examples.

Example 5: Combined Heading and Extrusion That Eliminated Machining.

As shown in Fig. 11, lawnmower wheel bolts were originally produced by heading the slug and simultaneously extruding the opposite end to 13.34 mm (0.525 in.) in diameter, by coining and trimming the round head to a hexagonal shape, and by turning the bolt blank to 8.4 mm (0.331 in.) in diameter in a secondary operation prior to thread rolling.

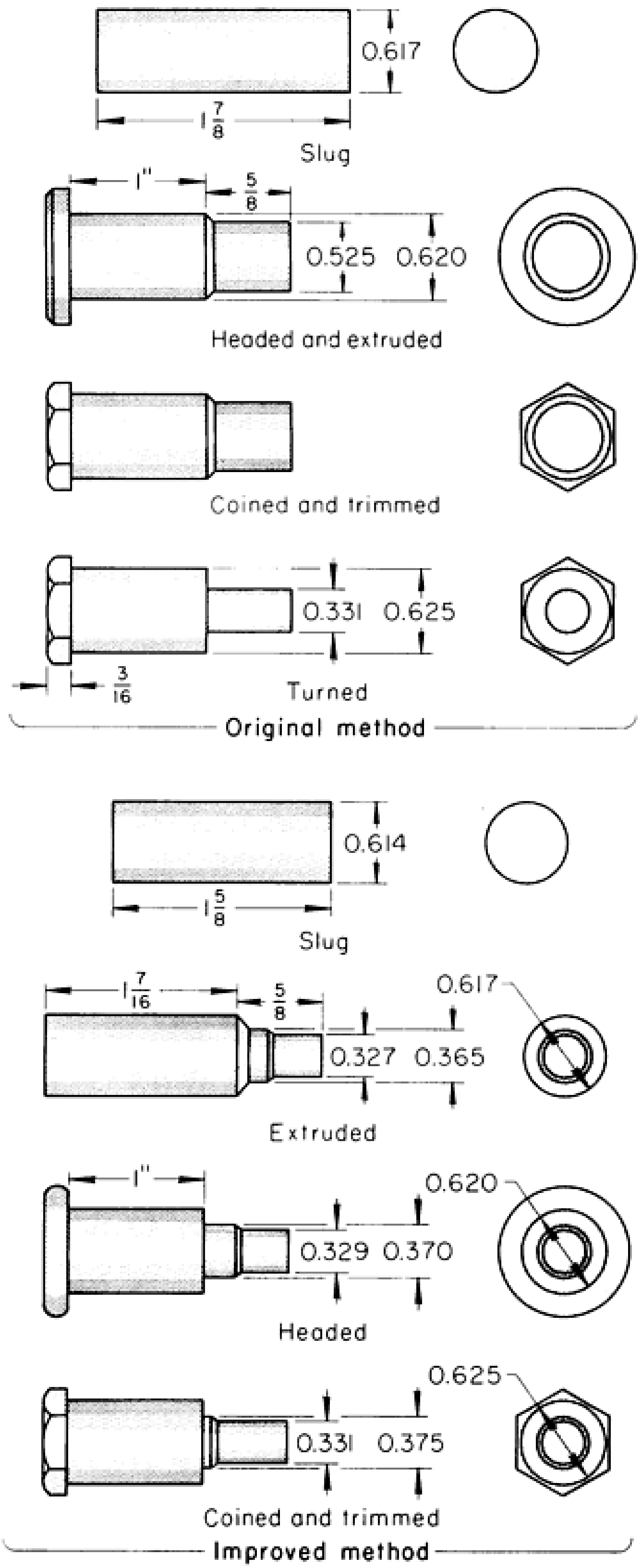


Fig. 11 Combined extrusion and cold heading used to reduce production costs for a 1018 steel lawnmower wheel. A turning operation was eliminated by cold extruding the diameter to be roll threaded. Dimensions given in inches.

By an improved method (Fig. 11), the slug was extruded to form two diameters on the shank end, then headed, coined, and trimmed. By this procedure, the minor extruded diameter was ready for thread rolling; no turning was required. The improved method not only reduced costs by eliminating the secondary turning operation but also produced a stronger part, because flow lines were not interrupted at the shoulder.

Because of the turning operation, production by the original method was only 300 pieces per hour. With the improved method, 3000 pieces could be produced per hour.

Example 6: Combining Extrusion With Heading to Decrease Heading Severity.

A socket-head cap screw was originally produced by heading 23.2 mm (0.915 in.) diam wire in four blows, using four dies. By an improved method (Fig. 12), the screw was produced by starting with a larger wire (25.1 mm, or 0.990 in., in diameter) and then combining forward extrusion with a heading operation in a first blow and completing the head by backward extrusion in a second blow. Thus, one die and two punches replaced four dies and four punches for a reduction in tool cost of about 50%. The improved method also permitted the part to be processed in a $\frac{3}{4}$ × 8 in. double-stroke header.

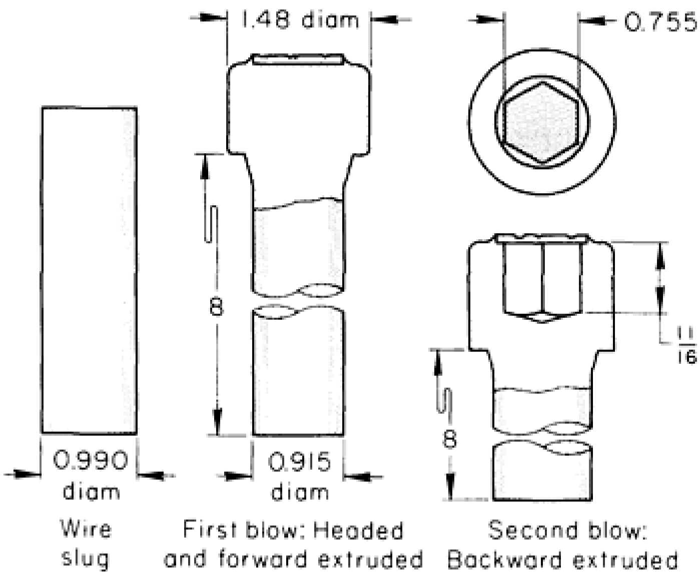


Fig. 12 Production of a large 4037 steel cap screw by extruding and heading in two blows. Dimensions given in inches.

The 25.1 mm (0.990 in.) starting diameter was cold drawn at the header from hot-rolled lime-coated 4037 steel with soap applied for a drawing lubricant. Molybdenum disulfide paste was applied as a lubricant when the cold-drawn stock entered the machine for shearing to length.

Warm Heading

In warm heading (a variation of the cold-heading process), the work metal is heated to a temperature high enough to increase its ductility. A rise in work metal temperature usually results in a marked reduction in the energy required for heading the material. Temperatures for warm heading range from 175 to 540 °C (350 to 1000 °F), depending on the characteristics of the work metal.

Applications. Warm heading is occasionally used to produce an upset that would have required a larger machine if the upsetting were done cold, but by far the most extensive use of warm heading is for the processing of difficult-to-head metals, such as austenitic stainless steels. Because they work harden rapidly, austenitic stainless steels are best headed at slow ram speeds.

The data shown in Fig. 13 suggest that the speed of the heading punch greatly affects the headability of these stainless steels. According to investigations, 80% of the loss in ductility caused by heading speed can be recovered if the metal is heated to between 175 and 290 °C (350 and 550 °F). The increase in headability with increasing temperature is indicated in Fig. 14.

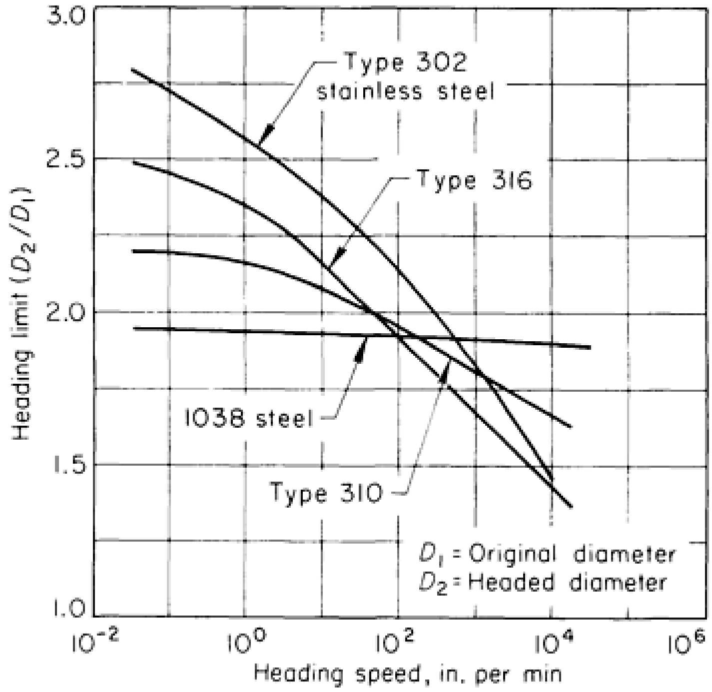


Fig. 13 Effect of heading speed on heading limits for three austenitic stainless steels and for 1038 steel.

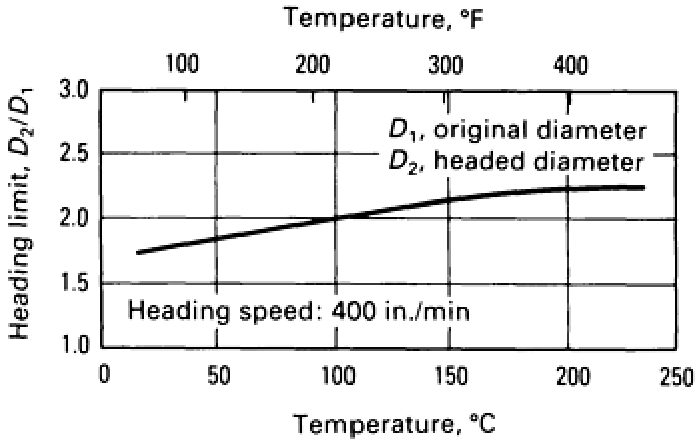


Fig. 14 Effect of work metal temperature on heading limit of austenitic stainless steel.

Machines and Heating Devices. Warm-heading machines are essentially the same as cold-heading machines except that warm-heading machines are designed to withstand the elevated temperature of the work metal. Induction heating coils or resistance heating elements can be used as auxiliary heating equipment.

Induction heating is the method most commonly used to heat work material for warm heading, although direct resistance heating is also used in some applications. The main disadvantage of induction heating is the high initial cost of the power supply. Therefore, its use is generally restricted to continuous high production.

Direct resistance heating, on the other hand, has the advantages of simplicity of equipment, accuracy of control, safety (because voltage is low), and adaptability to heating of a continuous length of work metal. The usual setup for resistance heating employs a second feeder-roll stand similar to that already on the header. The second stand is positioned about 1.5 m (5 ft) behind the first, and the wire stock (work metal) is fed through both sets of rolls. Leads from the electrical equipment are attached to the two sets of rolls, and the circuit is completed by the portion of the wire that passes between them. The wire (work metal) then becomes the resistance heater in the circuit.

Tools. Whether or not the same tools can be used for warm heading as for cold heading depends entirely on the temperature of the tools during operation. Although the tools usually operate at a temperature considerably lower than that of the work metal, it is important that the tool temperature be known. Tool temperature can be checked with sufficient accuracy by means of temperature-sensitive crayons. Under no circumstances should the tool be allowed to exceed the temperature at which it was tempered after hardening. This tempering temperature is usually 150 °C (300 °F) for carbon tool steel such as W1 or W2. Tools made from a high-alloy tool steel, such as D2, ordinarily should not be permitted to operate above 260 °C (500 °F).

When tool temperatures exceed those discussed above, the use of tools made from a hot-work tool steel, such as H12, is appropriate. However, the lower maximum hardness of such a steel somewhat limits its resistance to wear. A high-speed tool steel such as M2 will provide the high hardness and the resistance to tempering needed for long tool life.

Other Advantages of Warm Heading. As the heading temperature of a work-hardenable material increases, the resulting hardness decreases, as shown in Fig. 15. Therefore, if a material is warm headed, the hardness will remain low enough to permit such secondary operations as thread rolling, trimming, drilling, and slotting.

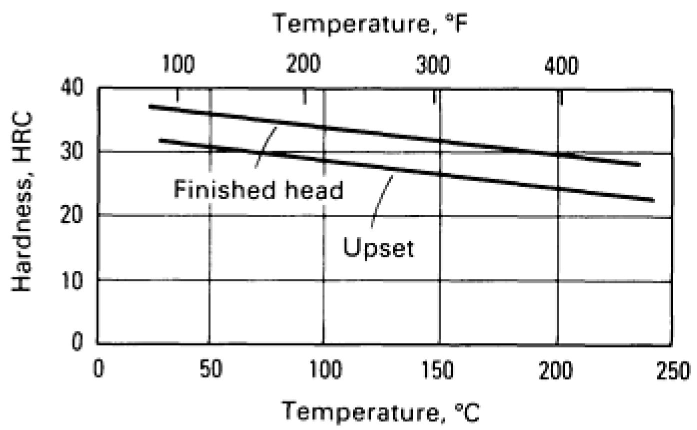


Fig. 15 Effect of heading temperature on the hardness of the upset portion and finished head of type 305 stainless steel flat-head machine screws.

In cold heading, the upset head of a work-hardening metal is very hard, a rolled thread is moderately hard, and the undeformed shoulder is relatively soft. These variations can be minimized by warm heading.

Cold Heading

Reference

1. "Upsetting," technical brochure, National Machinery Company, 1971, p 11

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

Introduction

COLD EXTRUSION is so called because the slug or preform enters the extrusion die at room temperature. Any subsequent increase in temperature, which may amount to several hundred degrees, is caused by the conversion of deformation work into heat. Cold extrusion involves backward (indirect), forward (direct), or combined backward and forward (indirect-direct) displacement of metal by plastic flow under steady, though not uniform, pressure. Backward displacement from a closed die is in the direction opposite to punch travel. Workpieces are often cup-shaped and have wall thicknesses equal to the clearance between the punch and die. In forward extrusion, the work metal is forced in the direction of the punch travel. These two basic methods of extrusion are sometimes combined so that some of the work metal flows backward and some forward. All three of these types of cold extrusion are shown in Fig. 1.

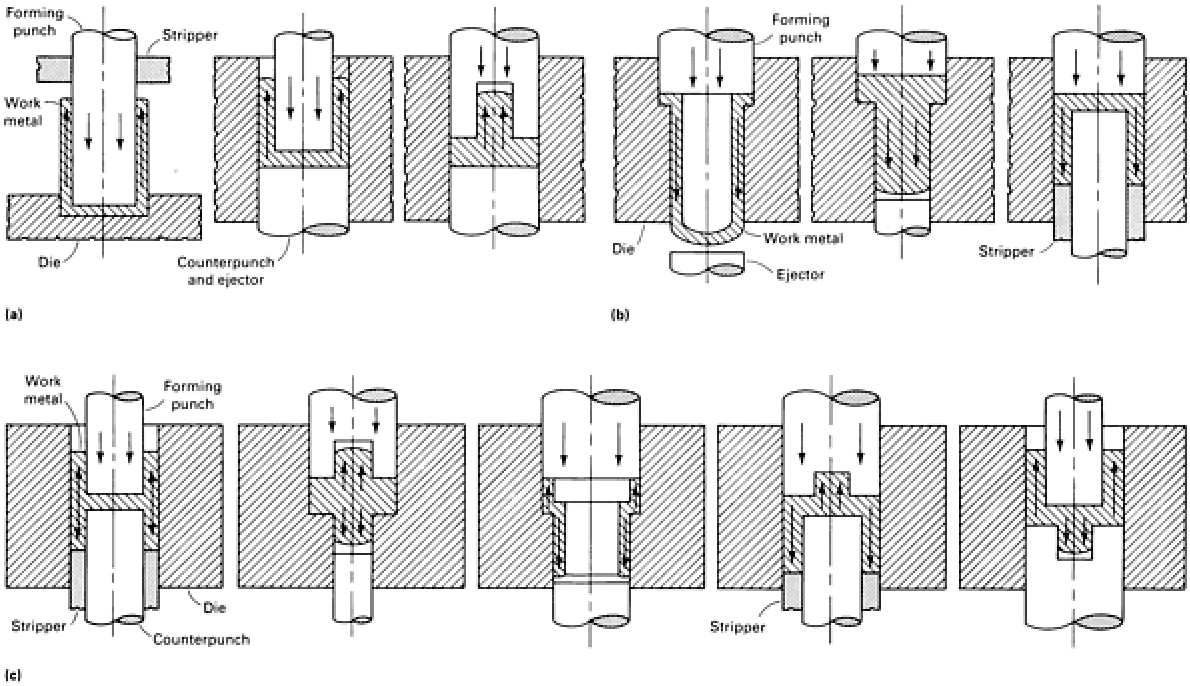


Fig. 1 Displacement of metal in cold extrusion. (a) Backward extrusion. (b) Forward extrusion. (c) Combined backward and forward extrusion

In cold extrusion, a punch applies pressure to the slug or preform, causing the work metal to flow in the required direction. The relative motion between punch and die is obtained by attaching either one (almost always the die) to the stationary bed and the other to the reciprocating ram. The axis of the machine can be vertical or horizontal. The pressure can be applied rapidly as a sharp blow, as in a crank press or header (impact extrusion), or more slowly by a squeezing action, as in a hydraulic press. The pressure exerted by the punch can be as low as 34.5 MPa (5 ksi) for soft metals or as high as 3100 MPa (450 ksi) for extrusion of alloy steel.

Work Hardening of Metals. Metals are work hardened when they are deformed at temperatures below their recrystallization temperatures. This can be an advantage if the service requirements of a part allow its use in the as-formed condition. (Under some conditions, heat treatment is not needed.) Work hardening, however, raises the ratio of yield strength to tensile strength and lowers ductility. Therefore, when several severe cold extrusion operations follow one another, ductility must be restored between operations by annealing. Any scale formed during annealing must be removed by blasting or pickling before subsequent extrusion. The effect of cold extrusion on the hardness across a section of extruded steel is described in the section "Extrusion Ratio" in this article.

In spite of the high pressure applied to it, the metal being extruded is not compressed to any measurable amount. Except for scale losses in annealing or the inadvertent formation of flash, constancy of volume throughout a sequence of operations is ensured. For all practical purposes, volumetric calculations can be based on the assumption that there is no loss of metal.

Cold-Extruded Metals. Aluminum and aluminum alloys, copper and copper alloys, low-carbon and medium-carbon steels, modified carbon steels, low-alloy steels, and stainless steels are the metals that are most commonly cold extruded. The above listing is in the order of decreasing extrudability. The equipment and tooling are basically the same regardless of the metal being extruded (see the sections "Equipment," "Tooling," and "Tool Materials" in this article).

Cold Extrusion Versus Alternative Processes. Cold extrusion competes with such alternative metal-forming processes as cold heading, hot forging, hot extrusion, machining, and sometimes casting. Cold extrusion is used when the process is economically attractive because of:

- Savings in material
- Reduction or elimination of machining and grinding operations, because of the good surface finish and dimensional accuracy of cold-extruded parts
- Elimination of heat-treating operations, because of the increase in the mechanical properties of cold-extruded parts

Cold extrusion is sometimes used to produce only a few parts of a certain type, but it is more commonly used for mass production because of the high cost of tools and equipment.

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

Extrusion Ratio

Extrusion ratio *R* is determined by dividing the original area undergoing deformation by the final deformed area of the workpiece:

$$R = \frac{A_0}{A_f}$$

Because volume remains constant during extrusion, the extrusion ratio can also be estimated by increase in length. An extrusion ratio of 4 to 1 indicates that the length has increased by approximately a factor of four.

The metal being extruded has a large effect on the maximum ratio that is practical. Some typical approximate maximum extrusion ratios are 40 for aluminum alloy 1100, 5 for 1018 steel and 3.5 for type 305 stainless steel and similar austenitic grades.

Extrusion pressure increases with extrusion ratio. Figure 2 shows that extrusion ratio has a larger effect on ram pressure in the forward extrusion of carbon steel than either carbon content or type of annealing treatment. Figure 3 illustrates the effect of tensile strength on extrudability in terms of ram pressure for both the backward and forward extrusion of low-carbon and medium-carbon steels of the 1000, 1100, and 1500 series at different extrusion ratios.

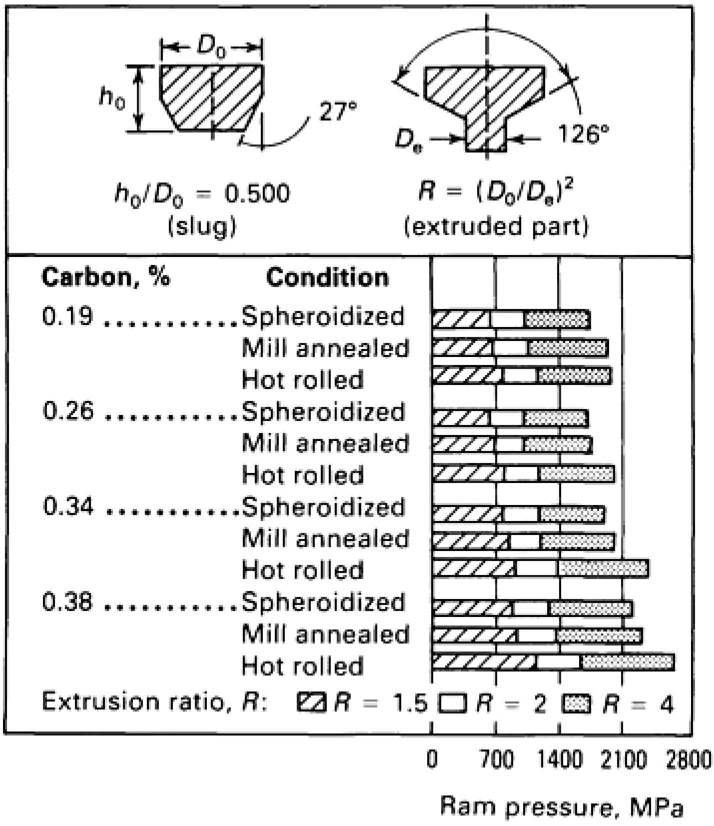


Fig. 2 Effect of carbon content, annealing treatment, and extrusion ratio on maximum ram pressure in the forward extrusion of the carbon steel part from the preformed slug

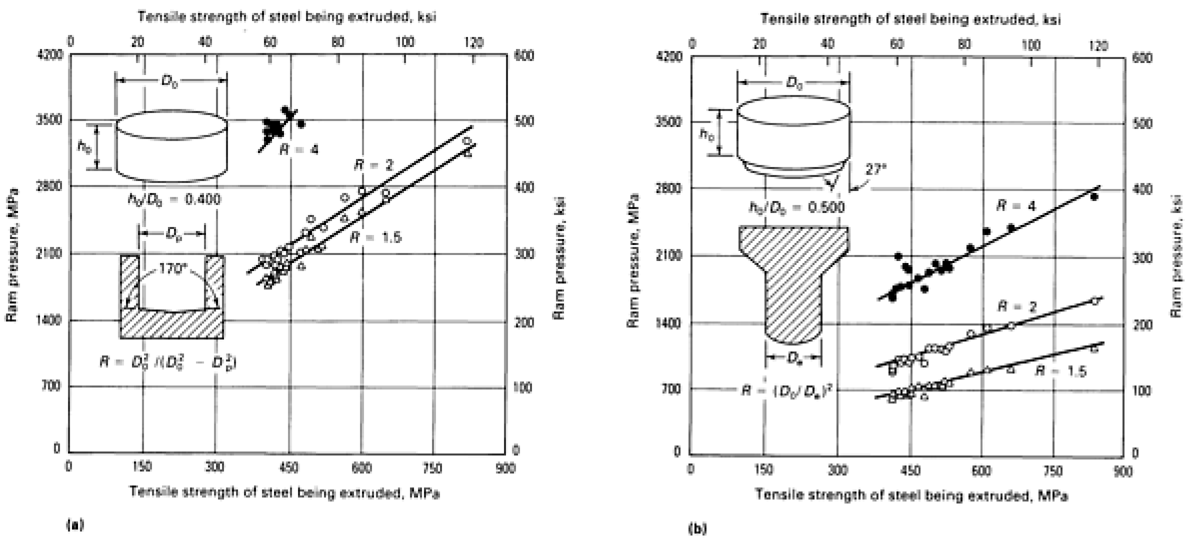


Fig. 3 Effect of tensile strength on ram pressure required for backward (a) and forward (b) extrusion of low- and medium-carbon steels at different extrusion ratios. Data are for AISI 1000, 1100, and 1500 series steels containing 0.13 to 0.44% C.

Extrusion Ratio Versus Work Hardening. Because an increase in extrusion ratio results in a corresponding increase in the amount of cold deformation, the effects of work hardening will normally vary directly with extrusion ratio. Data on the changes in tensile properties of the work metal during cold extrusion are given in Example 3.

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Because volume remains constant during extrusion, the extrusion ratio can also be estimated by increase in length. An extrusion ratio of 4 to 1 indicates that the length has increased by approximately a factor of four.

The metal being extruded has a large effect on the maximum ratio that is practical. Some typical approximate maximum extrusion ratios are 40 for aluminum alloy 1100, 5 for 1018 steel and 3.5 for type 305 stainless steel and similar austenitic grades.

Extrusion pressure increases with extrusion ratio. Figure 2 shows that extrusion ratio has a larger effect on ram pressure in the forward extrusion of carbon steel than either carbon content or type of annealing treatment. Figure 3 illustrates the effect of tensile strength on extrudability in terms of ram pressure for both the backward and forward extrusion of low-carbon and medium-carbon steels of the 1000, 1100, and 1500 series at different extrusion ratios.

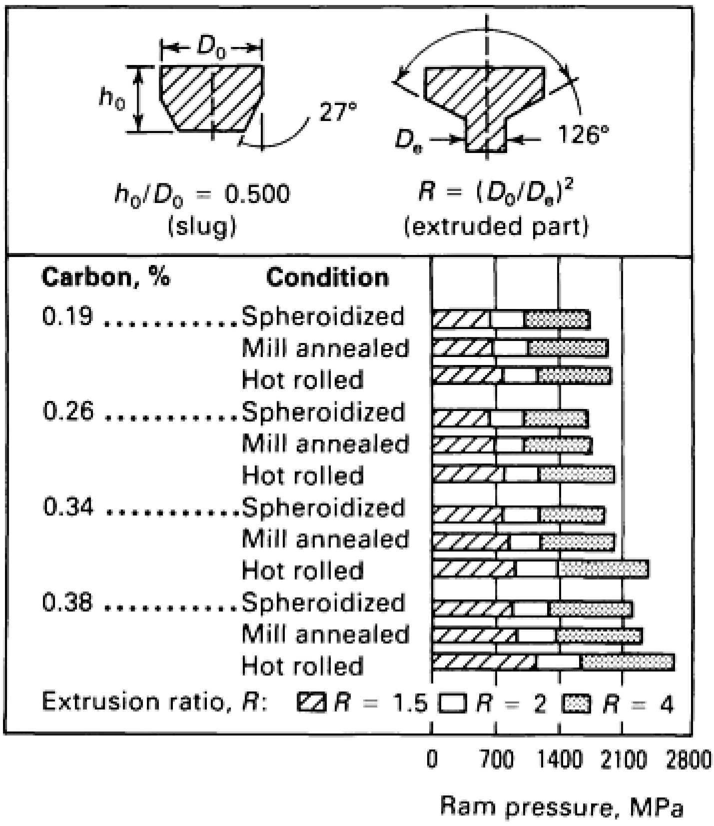

Fig. 2 Effect of carbon content, annealing treatment, and extrusion ratio on maximum ram pressure in the forward extrusion of the carbon steel part from the preformed slug

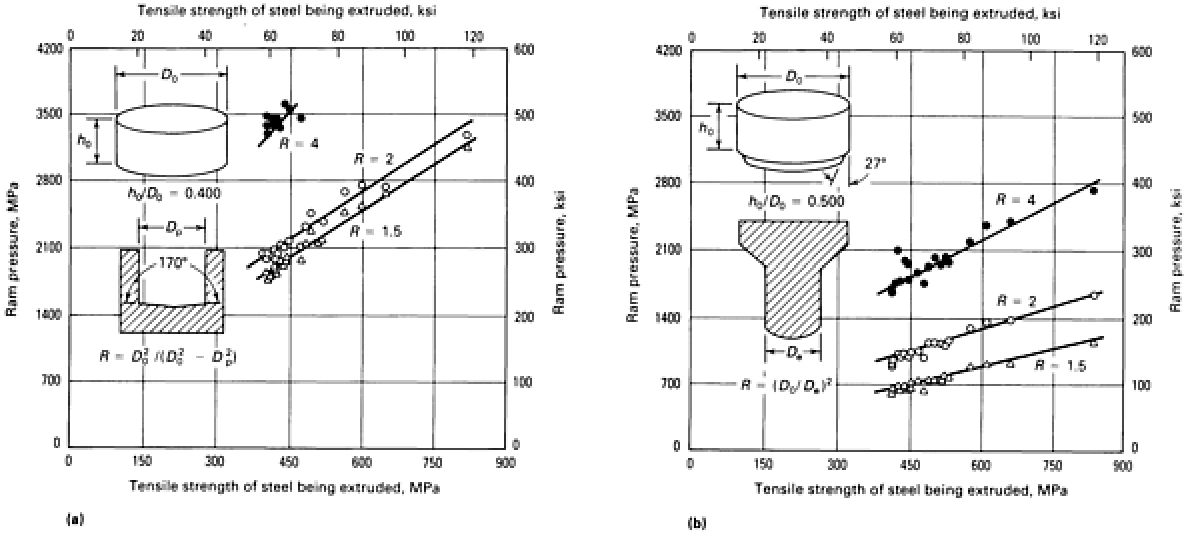

Fig. 3 Effect of tensile strength on ram pressure required for backward (a) and forward (b) extrusion of low- and medium-carbon steels at different extrusion ratios. Data are for AISI 1000, 1100, and 1500 series steels containing 0.13 to 0.44% C.

Extrusion Ratio Versus Work Hardening. Because an increase in extrusion ratio results in a corresponding increase in the amount of cold deformation, the effects of work hardening will normally vary directly with extrusion ratio. Data on the changes in tensile properties of the work metal during cold extrusion are given in Example 3.

Cold Extrusion

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

Effect of Composition and Condition on Extrudability of Steel

The extrudability of steel decreases with increasing carbon or alloy content. Extrudability is also adversely affected by greater hardness. Free-machining additives, such as sulfur or lead, are likely to impair extrudability. Nonmetallic inclusions, particularly the silicate type, are also detrimental to extrudability.

Carbon Content. The cold extrusion of steels containing up to 0.45% C is common practice, and steels with even higher carbon contents have been successfully extruded. However, it is advisable to use steels of the lowest carbon content that will meet service requirements. Most carbon and alloy steels that are extruded contain 0.10 to 0.25% C. However, in some applications, steels with more than 0.45% (especially alloy steels) are cold extruded.

Figure 2 shows the results of an investigation conducted in one plant to determine the effects of carbon content, type of annealed structure, and extrusion ratio on the ram pressure required to forward extrude a specific shape from carbon steels. These data show that ram pressures are essentially the same for steels containing 0.19 and 0.26% C, regardless of the other variables, but that ram pressure is markedly increased as carbon content reaches 0.34 and 0.38%. The steel slugs (Fig. 2) were coated with zinc stearate over zinc phosphate and were extruded under laboratory conditions at a rate of 635 mm/min (25 in./min).

Alloy Content. For a given carbon content, most alloy steels are harder than plain carbon steels and are therefore more difficult to extrude. Most alloy steels also work harden more rapidly than their carbon steel counterparts; therefore, they sometimes require intermediate annealing.

Hardness. The softer a steel, the easier it is to extrude. Steels that have been spheroidize annealed are in their softest condition and are therefore preferred for extrusion. Figure 2 shows that spheroidized steels were extruded at lower ram pressures than hot-rolled or mill-annealed steels, regardless of other variables. The data in Fig. 3 show that ram pressure must be increased as tensile strength increases for steels of low-to-medium carbon content at three extrusion ratios. However, operations that precede or follow extrusion may make it impractical to have the steel in its softest condition. Extremely soft steels of low-to-medium carbon content have poor shear-ability and machinability; therefore, some extrudability is occasionally sacrificed. Annealing techniques that produce a partly pearlitic structure are ideal for many extrusion applications in which shearability or machinability is important.

Free-machining steels, containing such additives as lead and sulfur, are not preferred for cold extrusion. Extrusions from these steels are more susceptible to defects than extrusions from their nonfree-machining counterparts. In addition, because parts produced by cold extrusion generally require only minimal machining (this is often the primary reason for using cold extrusion), there is much less need for free-machining additives than when parts are produced entirely by machining.

The successful extrusion of free-machining steels depends on the amount of upset, the flow of metal during extrusion, and the quality requirements of the extruded part. Free-machining steels can generally withstand only the mildest upset without developing defects. If it is under compression at all times during flow, a free-machining steel will probably extrude without defects. However, rupture is likely if compressive force is suddenly changed to tensile force.

Nonmetallic Inclusions. The fewer the inclusions, the more desirable the steel is for cold extrusion. Silicate inclusions have been found to be the most harmful. Therefore, some steels have been deoxidized with aluminum rather than silicon in an attempt to keep the number of silicate inclusions at a minimum. The aluminum-killed steels have better extrudability in severe applications.

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Extrusion Quality

Carbon steel bars are available at additional cost in two classes of extrusion quality: cold extrusion quality A and cold extrusion quality B. The mill preparation for cold extrusion quality A is the same as that used for special-quality bars; cold extrusion quality B is a still higher quality.

Higher quality refers primarily to fewer external and internal defects. Hot scarfing and more rigorous inspection of the billets are additional operations that are performed at the mill to prepare cold extrusion quality B material.

Alloy steel without a quality extra is used in applications similar to those of cold extrusion quality A for carbon steel. Alloy steels are also available as cold-heading quality, which parallels cold extrusion quality B for carbon steel. Boron-modified steels for heading and extrusion are also available.

The advisability of paying the additional cost for cold extrusion quality B or cold-heading quality steel depends on the severity of extrusion, the quality requirements of the extruded part, and the cost of rejected parts in comparison with the extra cost for these steels.

Severity of extrusion refers mainly to the extrusion ratio. If the ratio is low and the work metal is kept under compression during flow, it is unlikely that cold extrusion quality B steel will be beneficial. On the other hand, if the ratio is high or if the work metal is in tension at times during metal flow, cold extrusion quality B steel should be considered.

The cold extrusion of many parts involves both extrusion and upsetting. Upsetting is the more critical of the two operations, and the severity of the upset should determine the quality of steel required.

The overall quality requirements of the finished part must be considered. Minor defects are sometimes acceptable in the finished part, or they may be removable in normal machining.

More information on the workability of metals is available in the Section "Evaluation of Workability" in this Volume.

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Equipment

Hydraulic presses, mechanical presses, special knuckle-joint presses for cold extrusion, special cold-forging machines, and cold-heading machines are used in cold extrusion. Most cold extrusion operations are performed on mechanical presses or cold-heading machines. Of the two, mechanical presses are used more often, because of their adaptability to other types of operations. Mechanical presses are generally more costly and are capable of higher speeds than hydraulic presses of similar capacity. A disadvantage of a mechanical press is its limited length of stroke.

A cold-heading machine combines the essential features of a mechanical press with mechanisms that feed in bar stock, shear slugs, and transfer the slugs to the die and then to other dies if required.

Hydraulic presses represent only a small fraction of the total number of presses used for cold extrusion. However, hydraulic presses are especially well suited to the production of parts requiring long working strokes.

Proper selection of the press is important for successful cold extrusion and for the prevention of excessive maintenance charges. Mechanical presses must have:

- Sufficient flywheel energy (insufficient energy results in overloading and heating of the motor, as well as parts that are incompletely formed)
- Sufficient torque capacity in the drive mechanism to deliver the necessary force at the required point above the bottom of the stroke
- Rigid structural members to prevent excessive deflection under concentrated loading

Power Requirements. Because of work metal and tool variables, data resulting from laboratory studies of power requirements for cold extrusion are generally not applicable to shop practice. The following rules can be used as guidelines in estimating pressure, force, and horsepower requirements:

- Determine the effective contact area of the forming tool. In backward extrusion, this area is the cross-sectional area of the punch tip. For forward extrusion, the effective contact area is the annular area of the die shoulder
- Determine the extrusion ratio and ascertain that the ratio is within practical limits (see the section "Extrusion Ratio" in this article)
- Consider the tool materials used. Properly supported punches and dies made of tool steel can be operated at peak pressures as high as 2415 MPa (350 ksi). Carbide punches can be operated at peak pressures to 2760 MPa (400 ksi), and carbide dies at 3100 MPa (450 ksi)
- Peak extrusion forces can be safety estimated as the product of effective contact area (as determined in the first item in this list) and peak allowable stress (as indicated in the third item in this list). The condition of the press equipment, tools, and work material, the design of the tools, and the lubricant used, all affect the maximum extrusion ratio obtainable in a particular operation
- The energy required is calculated as the product of extrusion force and distance over which it must act to form the part. The horsepower required can be calculated from this energy and the frequency at which the energy is to be delivered
- At operating speed, flywheel energy must be four to ten times that required per stroke for extrusion; the exact multiple depends on cycle time and type of motor

Power requirements can be estimated on the basis of extrusion ratio. Other methods for determining power requirements, generally more complex, consider the influence of several interrelated variables, including the properties of the metal to be extruded, the size and shape of the part, the thickness of the wall to be produced (or reduction of area), the temperature, the effect of lubrication, the blank shape and thickness, and the grain size and orientation.

Cold Extrusion

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Tooling

Knowledge of the forces acting on tool components is not always a matter of certainty, and the design of tools is more often dictated by the dimensions of the part to be formed than by considerations of metal flow, lubrication, and other processing variables. Although many engineering components are, or can be, designed to last indefinitely, this is seldom true in the design of highly stressed, consumable tools for cold extrusion in which a tool life of 100,000 pieces is likely to be considered above average. On the other hand, conventional design criteria are applicable to the less highly stressed, nonconsumable tools for extrusion. Accordingly, it is convenient to distinguish between consumable tooling components, such as punches and dies, and nonconsumable ones, such as shrink rings and pressure pads.

Estimation of Load. Knowledge of the forces or pressures required for forward or backward extrusion is essential in design for determining tool stresses and for selecting suitable press equipment. Methods for estimating these requirements, including a method based on extrusion ratio, are discussed in the section "Power Requirements" in this article. The pressure to be applied is a function of the deformation resistance and degree of deformation. Deformation resistance, in turn, is affected by the composition, mechanical properties, and condition of the work material; the external frictional forces applied; and the size and shape of both the initial slug and the finished workpiece. Practical experience has shown that for the tool steels and carbides currently in use, the specific forming pressure at the punch should not exceed about 2370 MPa (344 ksi) and the die internal pressure should not exceed about 1895 MPa (275 ksi). If the estimated pressures exceed these limits, either the degree of deformation must be reduced or a considerably shorter tool life must be accepted.

The consumable tools (punch, die, and ejector) make direct contact with the metal to be extruded. These tools are exposed to a specific load and to wear. Their design should incorporate features that will conform to the design requirements of the workpiece while minimizing specific load and wear. It is usually possible to design tools that will satisfy both objectives by facilitating the flow of metal and reducing losses due to internal and external friction.

Tool Assembly Components. The components of a typical tool assembly used for the backward extrusion of steel parts are identified in Fig. 4. There is considerable variation in the tooling practice and design details of tool assembly components. Some of the principal factors affecting the design of punches and dies for backward and forward extrusion are discussed below and in the Selected References in this article.

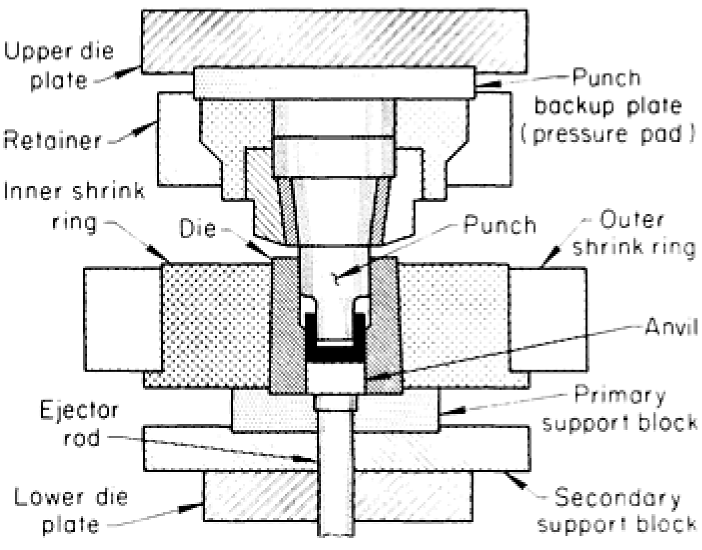

Fig. 4 Tools constituting a typical setup for the backward extrusion of steel parts

Punch Design. A major problem in punch design consists of assessing the nature and magnitude of the stresses to which the punch is subjected in service. Because the stresses are dynamic, fatigue effects will arise, and these fatigue effects, in conjunction with the inherently brittle nature of hardened tool steels, necessitate care in avoiding design features likely to produce stress concentrations. The stability problems that may arise when slender punches are used will be affected by the accuracy of alignment provided by the tool set or the press itself, or by factors in the extrusion operation, such as punch wander, initial centering, and use of distorted slugs. The ratio of punch length to punch diameter also affects stability; a ratio of about 3 to 1 is probably the maximum for cold extrusion of steel using tool steel punches.

The design of the punch nose has a significant effect on extrusion pressures and tool life. In backward extrusion acceptable results are obtained with a nose profile consisting of a truncated cone having an included angle of 170 to 180°, with an edge radius of 0.51 to 2.54 mm (0.020 to 0.100 in.), and a land length of 1.27 to 1.9 mm (0.050 to 0.075 in.) with the shank relieved 0.1 to 0.2 mm (0.004 to 0.008 in.) on the diameter. Although they reduce initial punch stresses, small cone angles or large radii are undesirable, because of rapid lubricant depletion and the risk of metal-to-metal contact. Design of the punch nose to distribute the lubricant properly during extrusion is essential for minimizing the pressures developed.

The area ratio between punch shank and head is also an important design factor. A large ratio will have the effect of spreading the punch load over a large area of pressure pad. On the other hand, it will require a wider block of metal for its fabrication with a resultant cost increase. Because pressure pads are less expensive than punches, it is generally advisable to favor the smaller ratios. The pressure pad, which transmits the load from the back of the punch to the die set, should be designed for economy, ease of replacement, and efficiency in reducing the number of punch failures.

Die Design. In forward extrusion, the die is under maximum pressure, and this pressure is not distributed uniformly. Therefore, the tool designer must calculate the hoop (tensile) stresses on the inner die wall and provide adequate reinforcement. Ordinarily, pressures of less than about half the yield strength of the die do not require reinforcement, while those in excess of this value do require reinforcement.

Extrusion dies are usually inserted in one or more shrink rings to provide reinforcement. These rings prestress the die in compression by providing interference fits between rings and die. This results in lower working stress and therefore longer fatigue life of extrusion tools. A similar technique is used to shrink radially segmented die inserts together to prevent the segments from separating under load. Permanent shrink-fit assemblies are sometimes made by heating the outer ring to facilitate assembly. Interchangeable die inserts are usually force fitted mechanically, using a tapered press fit and molybdenum disulfide as a lubricant. Of the two methods, shrinking-on by heating is generally preferred, because a cylindrical hole and shaft are easier to fabricate than a tapered hole and shaft. However, a taper fit has several advantages, such as:

- The hardness and yield strength of the various die components are not lowered (as they would be by heating) and can be measured with dependable accuracy
- The prestress value is ensured by strict control of the input measurements
- Release and exchange of the inner die bushings is quick, easy, and inexpensive
- Die parts can be standardized
- Hot-working die steels are not required

The most commonly used taper angle is $\frac{1}{2}$ to 1°. The conditions for obtaining the specified advantages of the taper force fit are careful preparation of the taper shell surfaces and exact agreement between taper angles of corresponding contact faces. If the shell surfaces do not provide uniform support over the entire die length, the prestresses will be unequal, and the reinforcement will not be fully effective.

In some setups, the first reinforcement is applied by taper force fit and the second (outer) reinforcement by shrinking-on. It is advisable to standardize on the size of reinforcing elements. In general, no further advantage is gained by making the outside diameter of a reinforcement more than four to five times the die diameter.

In forward extrusion, die angles are determined by the shape of the workpiece and by the operating sequence. In general, an angle of 2α = 24 to 70° (Fig. 5) is selected for the forward extrusion of solids, and an angle of 2α = 60 to 126° is preferred for extruding hollow parts, the angle varying inversely with wall thickness. Ejection pressure on the work increases with decreasing die angle, because greater friction must be overcome. This pressure also increases with an increase in the length of the part. Extrusion pressure causes elastic expansion of the die, which shrinks when the pressure is discontinued. Accordingly, very high wall pressures are developed, and these require correspondingly high ejection pressures.

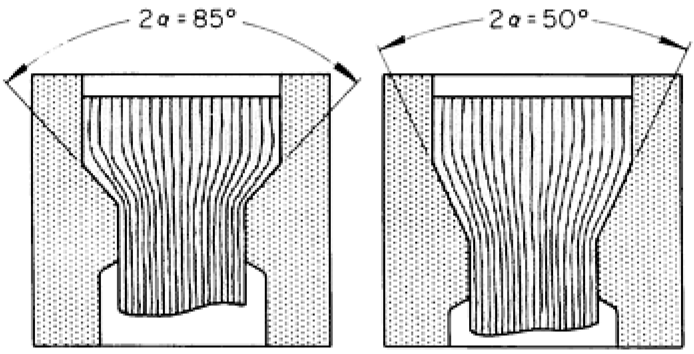

Fig. 5 Measurement of die angle in dies for forward extrusion

Tooling Setups. Metals can be cold extruded by different tooling setups, depending mainly on the size and shape of the workpiece, the composition of the work metal, and the quantity requirements. The principal types of tooling employed and examples of products formed by each type are discussed below.

Single-station tooling forms the part in one stroke of the press. Additional operations may be required for finishing. Closed-end containers, such as toothpaste tubes, are formed in this manner.

Multiple-station tooling involves a series of separate dies arranged so that the rough blank is made into a preform, which then proceeds through successive operations until the required form is produced.

Multiple-station tooling is often used for semicontinuous operations because of the need for annealing, pickling, and lubrication between operations, although it is also adaptable to continuous operations that use a transfer mechanism. This procedure has also been used in the cold forming of 75 and 155 mm shell bodies involving backward and forward extrusion.

Transfer presses are similar in concept to multiple-station tooling, that is, they can perform several operations in succession. For example, a transfer press may shear, preform, extrude, and finish draw the part in consecutive operations. Mechanical fingers transfer the workpiece from one operation to the next. Pole pieces for alternator rotors have been produced in transfer presses.

Upsetters or headers are used for continuous operation, frequently incorporating both backward and forward extrusion and cold heading. Fasteners such as hexagonal socket-head cap screws are typical examples of parts produced in upsetters.

Rotating dial or indexing can be applied for manual or automatic production. In operation, the table of the press holding the dies indexes, and the head containing the punches remains stationary except for vertical movement. Slugs can be fed automatically, and one or more parts can be formed with each stroke of the press. Instrumentation stops the operation immediately in the event of misalignment, punch breakage, or a wrong-size slug. Gear extrusions are representative examples of parts produced in this type of tooling, at the rate of two extrusions for each press stroke.

Cold Extrusion

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Tool Materials

Recommended materials for extrusion punches include M2 and M4 high-speed tool steels and tungsten carbide. Tool steel punches should be heat treated to a hardness of 62 to 66 HRC, and they must have a high compressive yield strength. Die inserts are usually fabricated from such alloy tool steels as D2, M2, and M4, and are heat treated to 58 to 64 HRC, depending on the steel.

Tungsten carbide is extensively used because it provides good die life, high production rates, and good dimensional control. Tungsten carbide often finds application as a punch material in backward extrusion. Retainer rings or housings used for tungsten carbide dies should have sufficient strength and toughness to prevent splitting and failure of the working tools. Shrink rings should be fabricated from hot-work die steels such as H11 or H13 heat treated to 46 to 48 HRC. Outer housings are often made from H13 die steel or from 4340 alloy steel. More information on die materials is available in the article "Dies and Die Materials for Hot Forging" in this Volume.

Cold Extrusion

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Preparation of Slugs

The preparation of slugs often represents a substantial fraction of the cost of producing cold-extruded parts.

Producing the Slug Shape. Despite the loss of metal, sawing and cutting off in a machine, such as an automatic bar machine, are widely used methods of producing slugs. The advantages of these methods include dimensional accuracy, freedom from distortion, and minimal work hardening. Shearing is an economical means of producing slugs. Variation in the sizes of the slugs is a major disadvantage of shearing. If slugs are allowed to vary in size, die design must allow for the escape of excess metal in the form of flash. An alternative to die adjustment in some applications is to compensate for the distortion and other discrepancies in sheared slugs by coining the slugs to desired dimensions.

Hot-rolled bar is usually the least costly form of steel for making slugs, but hot-rolled bars are likely to have deeper surface seams and greater depth of decarburized layers than cold finished bars. In addition, the variation in the outside diameter of hot-rolled bars will cause considerable variation in weight or volume of the slug, despite close control in cutting to length. Whether or not the surface seams and decarburization can be tolerated depends largely on the severity of extrusion and the quality requirements of the extruded part. In many applications, acceptable extrusions can be produced with slugs cut from hot-rolled bars.

Cold-finished bars are more expensive than hot-rolled bars. The size variation in cold-finished bars is considerably less than that in hot-finished bars. However, some seams and decarburization will also be present in cold-finished bar stock unless removed by grinding, turning, or other means. Some plants gain the advantage of cold-drawn bars by passing hot-rolled bars or rods through a cold-drawing attachment directly ahead of the slug-cutting operation.

Machined or ground bars are more costly than cold-drawn bars, but eliminate the difficulties caused by decarburization, seams, and variation in outside diameter. For some extrusions, especially those subjected to surface treatments that cannot tolerate a decarburized layer, requirements are such that previously machined bars or machined slugs must be used.

Surface Preparation of Steel Slugs. Phosphate coating for cold extrusion is almost universal practice. The primary purposes of this coating are, first, to form a nonmetallic separating layer between the tools and workpiece and, second, by reaction with or absorption of the lubricant, to prevent its migration from bearing surfaces under high unit pressures. During extrusion, the coating flows with the metal as a tightly adherent layer.

The recommended preparation of steel slugs for extrusion consist of alkaline cleaning, water rinsing, acid pickling, cold and hot water rinsing, phosphate coating, and rinsing. These are discussed below.

Alkaline cleaning is done to remove oil, grease, and soil from previous operations so that subsequent pickling will be effective. Alkaline cleaning can be accomplished by spraying the slugs with a heated (65 to 70 °C, or 150 to 160 °F) solution for 1 to 2 min or by immersing them in solution at 90 to 100 °C (190 to 212 °F) for 5 to 10 min.

Water rinsing is done to remove residual alkali and to prevent neutralization of the acid pickling solution. Slugs are usually rinsed by immersion in overflowing hot water, but they may also be sprayed with hot water.

Acid Pickling. Most commercial installations use a sulfuric acid solution (10% by volume) at 60 to 90 °C (140 to 190 °F). Pickling can be accomplished by spraying for 2 to 15 min or by immersion for 5 to 30 min, depending on surface conditions (generally, the amount of scale). Three times are usually sufficient to remove all scale and to permit a good phosphate coating. Bright annealing or mechanical scale removal, such as shot blasting, as a substitute for pickling has proved unsatisfactory for severe extrusion. However, the use of a mechanical scale-removing method prior to pickling can reduce pickling time, and for producing extrusions of mild severity, the mechanical (or bright annealing) methods have often been used without subsequent pickling.

Cold and hot water rinsing can be carried out by immersion or spraying for $\frac{1}{2}$ to 1 min for each rinse. Two rinses are used to ensure complete removal of residual pickling acid and iron salts. Cold water rinsing is usually of short duration, with heavy overflow of water to remove most of the residual acid. Hot water at about 70 °C (160 °F) increases the temperature of the workpiece and ensures complete rinsing.

Phosphate coating is performed by immersion in zinc phosphate at 70 to 80 °C (160 to 180 °F) for 3 to 5 min. Additional information is available in the article "Phosphate Coatings" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Rinsing with cold water, applied by spraying for $\frac{1}{2}$ min or by immersion for 1 min, removes the major portion of residual acids and acid salts left over from the phosphating solution. This rinse is followed by a neutralizing rinse applied by spraying or immersion for $\frac{1}{2}$ to 1 min using a well-buffered solution (such as sodium carbonate), which must be compatible with the lubricant. In the second rinse, the remaining residual acid and acid salts in the porous phosphate coating are neutralized so that absorption of, or reaction with, the lubricant is complete.

Stainless steels are not amenable to conventional phosphate coating (which is why stainless steels are more difficult to extrude than carbon steels); copper plating of stainless steel slugs is preferred. Lime coating is sometimes substituted successfully for copper plating. In extreme cases, the stainless steel can be zinc plated and then coated with zinc phosphate and a suitable soap lubricant. Methods of surface preparation for nonferrous metals are discussed in the sections "Cold Extrusion of Copper and Copper Alloy Parts" and "Cold Extrusion of Aluminum Alloy Parts" in this article.

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Lubricants for Steel

A soap lubricant provides the best results for the extrusion of steel. Slugs are immersed in a dilute (45 to 125 mL/L, or 6 to 16 oz/gal.) soap solution at 65 to 90 °C (145 to 190 °F) for 3 to 5 min. Some soaps are formulated to react chemically with the zinc phosphate coating, resulting in a layer of water-insoluble metal soap (zinc stearate) on the surfaces of the slugs. This coating has a high degree of lubricity and maintains a film between the work metal and tools at the high pressures and temperatures developed during extrusion.

Other soap lubricants, with or without filler additives, can be used effectively for the mild extrusion of steel. This type of lubricant does not react with the phosphate coating, but is absorbed by it.

Although the lubricant obtained by the reaction between soap and zinc phosphate is optimal for extruding steel, its use demands precautions. If soap accumulates in the dies, the workpieces will not completely fill out. Best practice is to vent all dies so that the soap can escape and to keep a coating of mineral seal oil (applied as an air-oil mist) on the dies to prevent adherence of the soap.

When steel extrusions are produced directly from coiled wire (similar to cold heading), the usual practice is to coat the coils with zinc phosphate, using the procedure outlined in the section "Preparation of Slugs" in this article. This practice however, has one deficiency; because only the outside diameter of the work metal is coated, the sheared ends are uncoated at the time of extrusion. This deficiency is partly compensated for by constantly flooding the work with sulfochlorinated oil. Because the major axis of a heading machine is usually horizontal, there is less danger of entrapping lubricant than when extruding in a vertical press.

Cleaning the extruded parts can be a significant item in the cost of cold extrusion. In general, the more effective the lubricant, the more difficult it is to remove. The methods used for removing pigmented drawing compounds are usually effective for removing the lubricants used for cold extrusion.

Cold Extrusion

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Selection of Procedure

The shape of the part is usually the primary factor that determines the procedure used for extrusion. For example, many cuplike parts are produced by backward extrusion, while shaftlike parts and hollow shapes can usually be produced more easily by forward extrusion. For many shapes, both forward and backward extrusion are used. Other factors that influence procedure are the composition and condition of the steel, the required dimensional accuracy, quantity, and cost.

The procedures used to extrude a given shape from highly extrudable steels are simpler than those used for more difficult-to-extrude steels. For difficult steels, it may be necessary to incorporate more passes and one or more annealing operations into the process. Some shapes may not be completely extrudable from a difficult-to-extrude steel; one or more machining operations may be required.

Normal extrusion procedures are associated with certain ranges of dimensional accuracy (see the section "Dimensional Accuracy" in this article). Special procedures and controls can provide greater-than-normal accuracy at higher cost.

Cold extrusion is ordinarily not considered unless a large quantity of identical parts must be produced. The process is seldom used for fewer than 100 parts, and more often it is used for hundreds of thousands of parts or continuous high production. Quantity requirements determine the degree of automation that can be justified and often determine whether the part will be completed by cold extrusion (assuming it can be if tooling is sufficiently elaborate) or whether, for low quantities, a combination of extruding and machining will be more economical.

Cost per part extruded usually determines:

- The degree of automation that can be justified
- Whether a combination of extruding and machining should be used for low-quantity production
- Whether it is more economical to extrude parts for which better-than-normal dimensional accuracy is specified or to attain the required accuracy with secondary operations

It is sometimes possible to extrude a given shape by two or more different procedures. Under these conditions, cost is usually the deciding factor. Several procedures for extruding specific steel parts, categorized mainly by part shape, are discussed in the following sections.

Cuplike Parts

The basic shape of a simple cup is often produced by backward extrusion, although one or more operations such as piercing or coining are frequently included in the operations sequence. For cuplike parts that are more complex in shape, a combination of backward and forward extrusion is more often used. The following example describes combined backward extrusion and coining for the fabrication of 5120 steel valve tappets.

Example 1: Backward Extrusion and Coining for Producing Valve Tappets.

The valve tappet shown in Fig. 6 was made from fine-grain, cold-heading quality 5120 steel. Slugs were prepared by sawing to a length of 25.9 to 26.0 mm (1.020 to 1.025 in.) from bar stock 22.0 to 22.1 mm (0.867 to 0.871 in.) in diameter. Slugs were tumbled to round the edges, then phosphated and lubricated with soap.

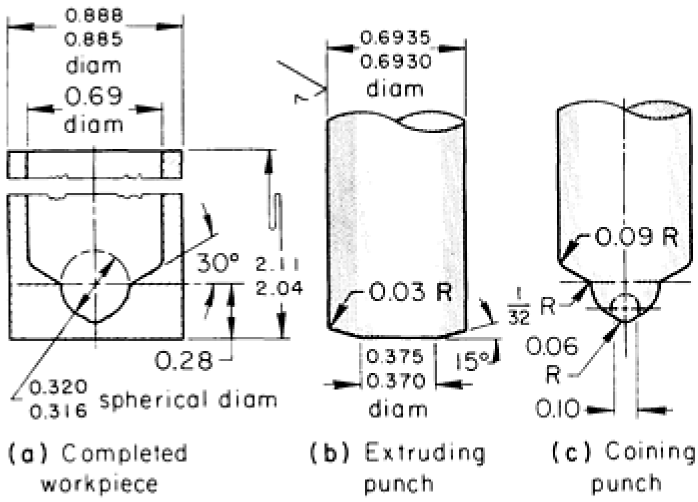


Fig. 6 5120 steel valve tappet (maximum hardness: 143 HB) produced by extrusion and coining with punches shown. Dimensions given in inches

The slugs were fed automatically into the two loading stations of the eight-station dial, then extruded, coined, and ejected. One part was produced in each set of four stations (two parts per stroke). This technique helped to keep the ram balanced, thus avoiding tilting of the press ram, prolonging punch life, and reducing eccentricity between the outside and inside diameters of the extruded part. An eccentricity of less than 0.25 mm (0.010 in.) total indicator reading (TIR) was required. The cup could not be extruded to the finished shape in one hit, because a punch of conelike shape would pierce rather than meter-out the phosphate coating. Therefore, two hits were used--the first to extrude and the second to coin. Punches are shown in Fig. 6(b) and 6(c). Axial pressure on the punch was about 2205 MPa (320 ksi).

Tubular Parts

Backward and forward extrusion, drawing, piercing, and sometimes upsetting are often combined in a sequence of operations to produce various tubular parts. The following example describes a procedure for extruding a part having a long tubular section.

Example 2: Producing Axle-Housing Spindles in Five Operations.

An axle-housing spindle was produced from a slug by backward extruding, piercing, and three forward extruding operations, as shown in Fig. 7. The 10 kg (22.5 lb) slug was prepared by sawing and then annealing in a protective atmosphere at 675 to 730 °C (1250 to 1350 °F) for 2 h, followed by air cooling. The slug was then cleaned, phosphate treated, and coated with soap. After backward extruding and piercing, and again after the first forward extruding operation, the work-piece was reannealed and recoated.

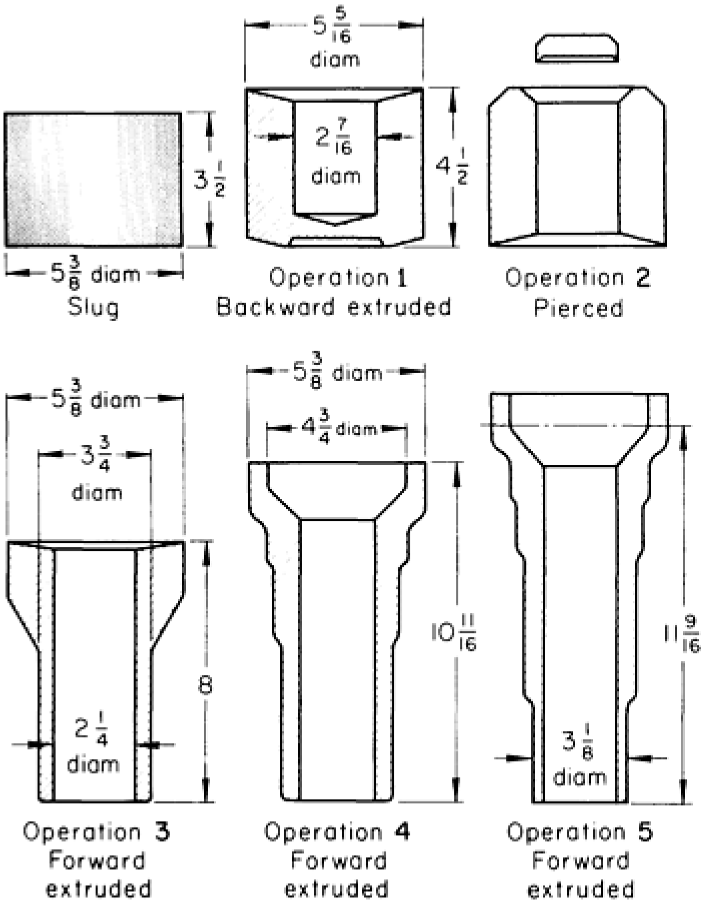


Fig. 7 1030 steel (hardness: 75 to 80 HRB) axle-housing spindle produced by extruding and piercing in five operations. Dimensions given in inches

A 49 MN (5500 tonf) crank press operated at 14 strokes per minute was used. The punches were made of D2 tool steel, and the die inserts of A2 tool steel.

Stepped Shafts

Three methods are commonly used to cold form stepped shafts. If the head of the shaft is relatively short (length little or no greater than the headed diameter), it can be produced by upsetting (heading). For a head more than about $2\frac{1}{2}$ diameters long, however, upsetting in a single operation is not advisable; buckling will result because of the excessive length-to-diameter ratio of the unsupported portion of the slug. Under these conditions, forward extrusion or multiple-operation upsetting should be considered.

Forward extrusion can be done in a closed die or an open die (Fig. 8). In a closed die, the slug is completely supported, and the cross-sectional area can be reduced by as much as 70%. Closed-die extrusion gives better dimensional accuracy and surface finish than the open-die technique. However, if the length-to-diameter ratio of the slug is more than about 4 to 1, friction along the walls of the die is so high that the closed-die method is not feasible, and an open die must be used. In an open die, reduction must be limited to about 30%, or the unsupported portion of the slug will buckle. Stepped shafts can, however, be extruded in open dies using several consecutive operations, as described in the following example.

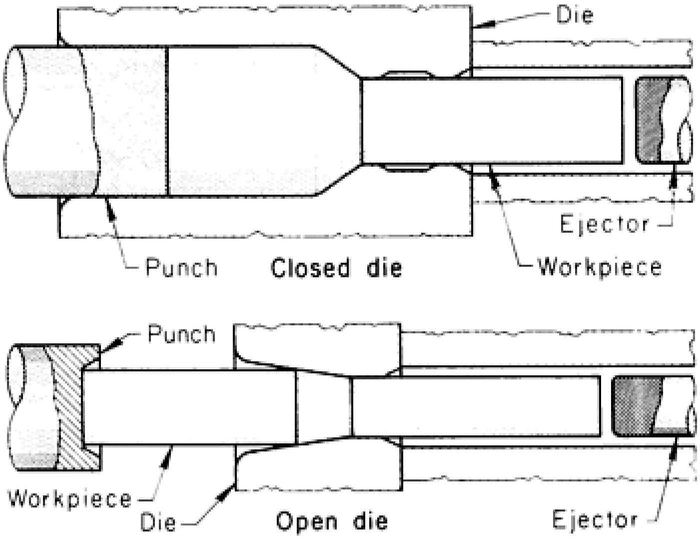


Fig. 8 End of stroke in the forward extrusion of a stepped shaft in a closed die and an open die

Example 3: Transmission Output Shaft Forward Extruded in Four Passes in an Open Die.

A transmission output shaft was forward extruded from a sheared slug in four passes through a four-station open die, as shown in Fig. 9. Extrusion took place in two directions simultaneously. Transfer from station to station was accomplished by a walking-beam mechanism.

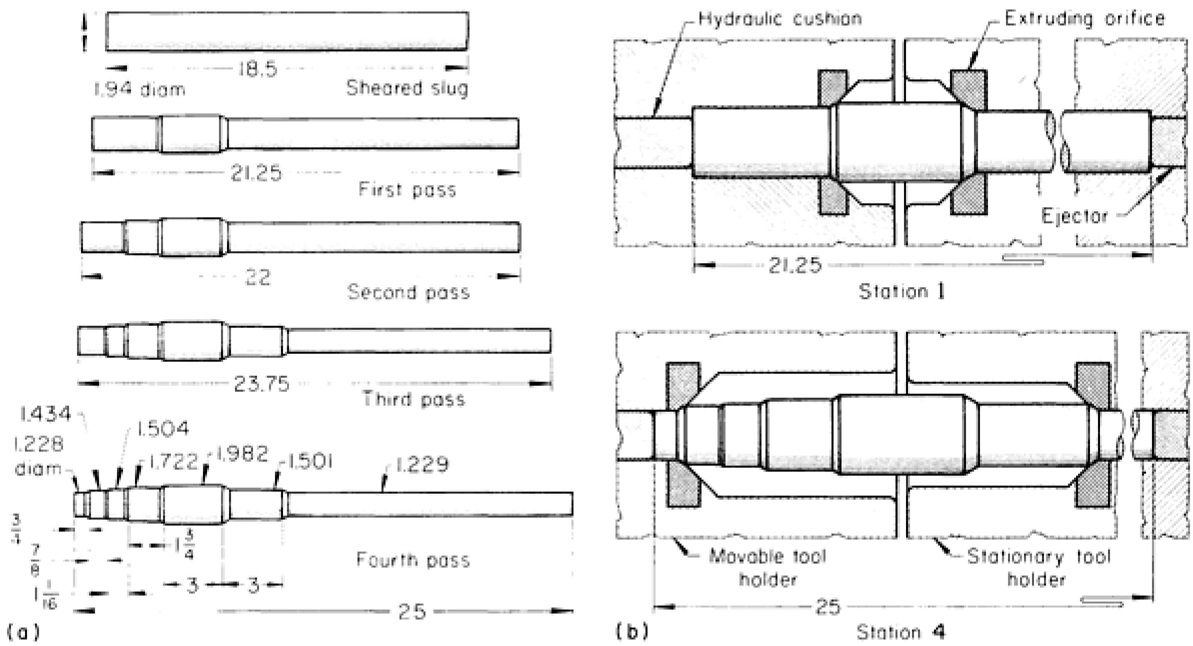


Fig. 9 4028 steel transmission shaft produced by four-pass forward extrusion in a four-station open die. (a) Shapes produced in extrusion. (b) Two of the die stations. Dimensions given in inches

Air-actuated V-blocks (not shown in Fig. 9) were used to clamp the large diameter of the shaft to prevent buckling. A hydraulic cushion (Fig. 9) contacted the slug at the start of the stroke and remained in contact with the workpiece throughout the cycle. Therefore, extrusion into the stationary tool holder took place first, ensuring that variation in finished length, caused by variation in stock diameter, was always in the movable tool holder. Each station of the die was occupied by a workpiece at all times; a finished piece was obtained with each stroke of the press. The amount of area reduction was about the same for each pass and totaled 65% for the four passes.

The cold working caused a marked change in the mechanical properties of the workpiece. Tensile strength increased from 585 to 945 MPa (85 to 137 ksi), yield strength increased from 365 to 860 MPa (53 to 125 ksi), elongation decreased from 26 to 7%, and reduction of area decreased from 57 to 25%.

Extrusion Combined with Cold Heading

The combination of cold extrusion and cold heading is often the most economical means of producing hardware items and machinery parts that require two or more diameters that are widely different (see also the article "Cold Heading" in this Volume). Such parts are commonly made in two or more passes in some type of heading machine, although presses are sometimes used for relatively small parts. Presses are required for the heading and extruding of larger parts.

Parts that have a large difference in cross-sectional area and weight distribution cannot be formed economically from material equivalent in size to the smallest or largest diameter of the completed part. The most economical procedure consists of selecting material of an intermediate size, achieving a practical amount of reduction of area during forward extrusion, and forming the large sections of the part by heading. This practice is demonstrated in the following examples.

Example 4: Adjusting Screw Blank Produced by Forward Extrusion and Severe Heading in Three Operations.

The blank for a knurled-head adjusting screw, shown in Fig. 10, was made from annealed and cold-drawn rod that was coated with lime and a soap lubricant at the mill. In this condition, the rod was fed to a heading machine, in which it was first cut to slug lengths. The slugs were then lubricated with an oil or a water-soluble lubricant containing extreme-pressure additives. As shown in Fig. 10, the slug was extruded in one die, and the workpiece was then transferred to a second die, in which it was cold headed in two operations--the first for stock-gathering, and the second for completing the head (which represents severe cold heading). Except for the extrusion die, which was made from carbide, all dies and punches were made from M2 and D2 steels hardened to 60 to 62 HRC. Tool life for the carbide components was 1 million pieces; for the tool steel components, 250,000 pieces. Production rate was 6000 pieces per hour.

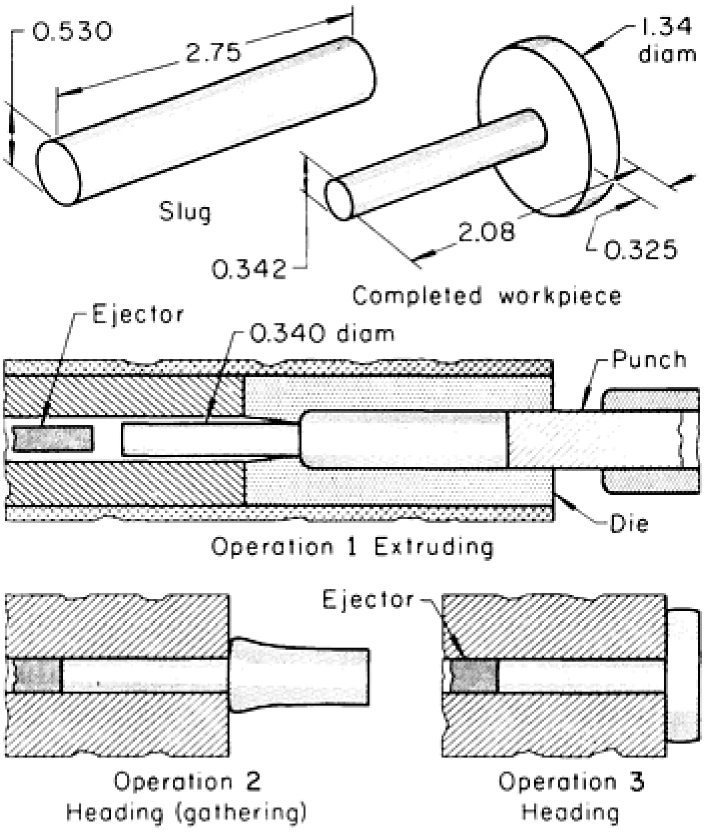


Fig. 10 1018 steel adjusting-screw blank formed by forward extruding and severe cold heading. Dimensions given in inches

Extrusion of Hot Upset Preforms

Although the use of symmetrical slugs as the starting material for extrusion is common practice, other shapes are often used as the starting slugs or blanks. One or more machining operations sometimes precede extrusion in order to produce a shape that can be more easily extruded. The use of hot upset forgings as the starting material is also common practice. Hot upsetting followed by cold extrusion is often more economical than alternative procedures for producing a specific shape. Axle shafts for cars and trucks are regularly produced by this practice; the advantages include improved grain flow as well as low cost. A typical application is described in the following example.

Example 5: Hot Forging and Cold Extrusion of Rear-Axle Drive Shafts.

The fabrication of rear-axle drive shafts (Fig. 11) for passenger cars and trucks by three-operation cold extrusion improved surfaces (and consequently fatigue resistance), maintained more uniform diameters and closer dimensional tolerances, increased strength and hardness, and simplified production. The drive shafts were hot upset forged to form the flange and to preform the shaft, and they were cold extruded to lengthen the shaft. The flange could have been upset as a final operation after the shaft had been cold extruded to length, but this would have required more passes in the extrusion press than space allowed. Hot upsetting and cold extrusion replaced a hammer forging and machining sequence after which the flange, a separate piece, had been attached.

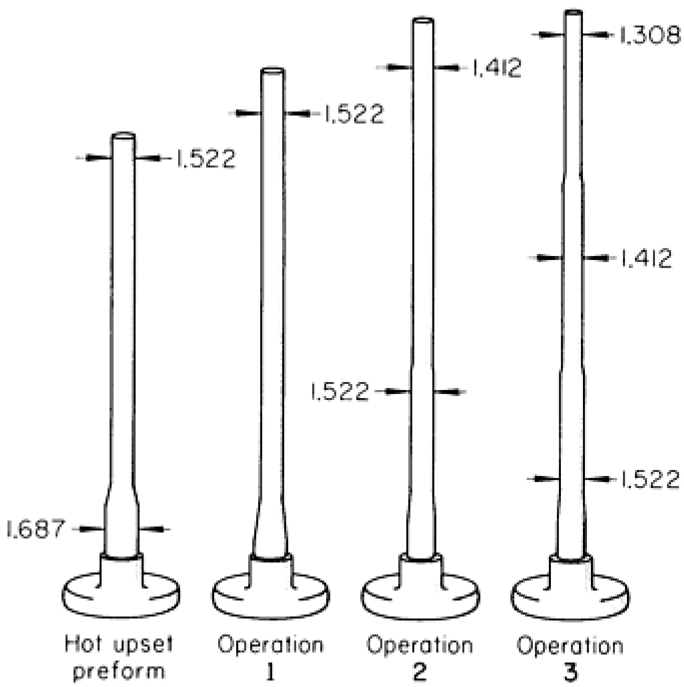


Fig. 11 1039 steel rear-axle drive shaft produced by cold extruding an upset forging in three operations. Billet weight: 36 kg (79.5 lb). Dimensions given in inches

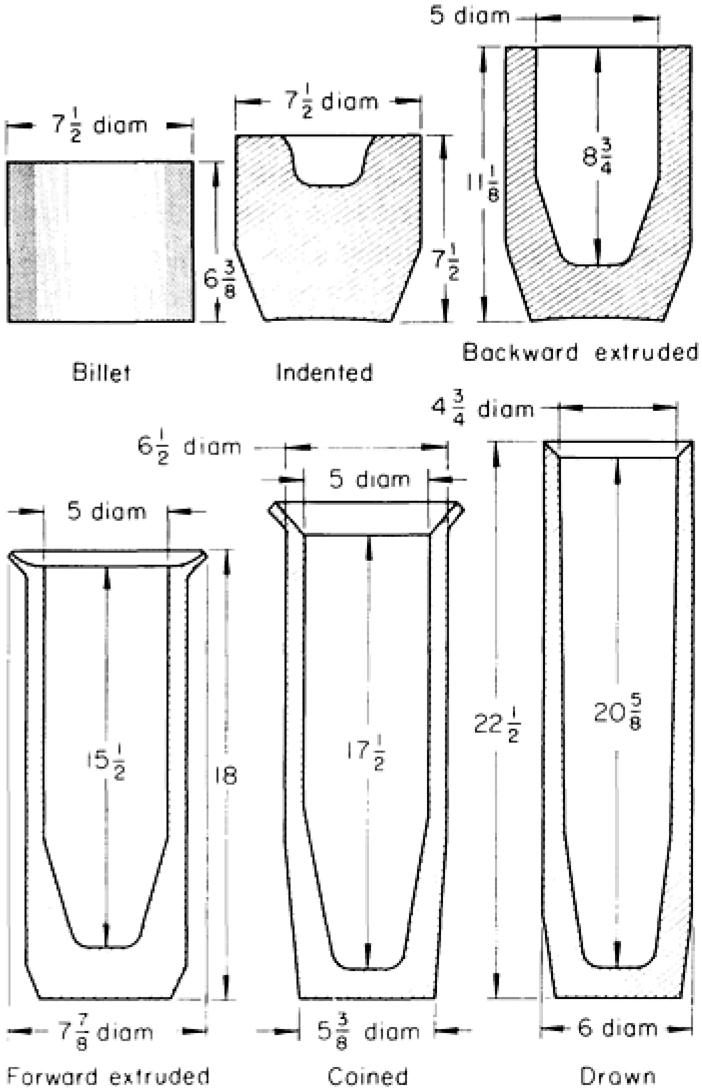
Steel was extrusion-quality 1039 in 42.9 mm ($1\frac{11}{16}$ in.) diam bars. The bars were sheared to lengths of 757 to 929 mm ($29\frac{13}{16}$ to $36\frac{9}{16}$ in.), then hot forged and shot blasted. A continuous conveyor took the hot upset preforms through a hot alkaline spray cleaner, a hot spray rinse, a zinc phosphating bath (75 °C, or 165 °F, for 5 min), a cold spray rinse, a hot spray rinse, and finally a soap tank (90 °C, or 190 °F, for 5 min). As shown in Fig. 11, cold extrusion was a three-operation process that increased the length of the shaft and reduced the smallest diameter to 33.2 mm (1.308 in.).

Extrusion of Large Parts

Although most cold extrusion of steel is confined to relatively small parts (starting slugs seldom weigh more than 11.3 kg, or 25 lb), much larger parts have been successfully cold extruded. For press operations, the practical extremes of part size are governed by the availability of machinery and tool materials, the plasticity of the work material, and economical production quantities. Bodies for large-caliber ordnance shells have been successfully produced by both hot and cold extrusion processes. The procedure used in the production of these large parts by cold extrusion is described in the following example.

Example 6: Use of Extrusion in Multiple-Method Production of Shell Bodies.

Figure 12 shows the progression of shapes resulting from extrusion, coining, and drawing in a multiple-method procedure for producing bodies for 155 mm shells from descaled 1012 steel billets 190 mm ($7\frac{1}{2}$ in.) in diameter that weighed 36 kg (79.5 lb) each. The sequence of operations is listed with Fig. 12. Production of these shell bodies was designed for semicontinuous operation that included annealing, cleaning, and application of lubricant between press operations.



Sequence of operations

Cold saw the billet.
Chamfer sawed edges.
Apply lubricant as follows:

Degrease in boiling caustic; rinse.
Pickle in sulfuric acid; rinse.

Apply zinc phosphate.
Apply zinc stearate.

Cold size indent (see illustration above).
Induction normalize (925 to 980 °C, or 1700 to 1800 °F).
Apply lubricant as in step 3.
Backward extrude (see illustration).
Induction normalize (see step 5).
Apply lubricant as in step 3.
Forward extrude in two stages to shape in illustration.
Anneal lip by localized induction heating (815 to 830 °C, or 1500 to 1525 °F).
Apply lubricant as in step 3.
Coin base and form boat tail to finish dimension and coin bottom (see illustration).
Final draw (see illustration).
Turn and recess lip.
Induction anneal nose (790 to 815 °C, or 1450 to 1500 °F).
Apply lubricant as in step 3.
Expand bourrelet in No. 6 press.
Form nose.
Anneal for relief of residual stress.

Fig. 12 1012 steel 155 mm (6 in.) shell body produced by a multiple-step procedure that included cold extrusion. Dimensions given in inches

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

Dimensional Accuracy

In cold extrusion, the shape and size of the workpiece are determined by rigid tools that change dimensionally only from wear. Because tool wear is generally low, successive parts made by cold extrusion are nearly identical. The accuracy that can be achieved in cold extrusion depends largely on the size and shape of the given section.

Tolerances for cold extrusion are commonly denoted as close, medium, loose, and open. Definitions of these tolerances, as well as applicability to specific types of extrusions, are discussed below.

Close tolerance is generally considered to be ±0.025 mm (±0.001 in.) or less. Close tolerances are usually restricted to small (<25 mm, or 1 in.) extruded diameters.

Medium tolerance denotes ±0.13 mm (±0.005 in.). Extruded diameters of larger parts (up to 102 mm, or 4 in.), headed diameters of small parts, and concentricity of outside and inside diameters in backward extruded parts are typical of dimensions on which it is practical to maintain medium tolerance.

Loose tolerance denotes ±0.38 mm (±0.015 in.). This tolerance generally applies to short lengths of extruded parts less than about 89 mm ($3\frac{1}{2}$ in.) long.

Open tolerance is generally considered to be greater than ±0.38 mm (±0.015 in.). This tolerance applies to length dimensions of large, slender parts (up to 508 mm, or 20 in., and sometimes longer).

Variation. With reasonable maintenance of tools and equipment, the amount of variation of a given dimension is usually small for a production run. Some drift can be expected as the tools wear and work metal properties vary from lot to lot.

Cold Extrusion

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

Causes of Problems

The problems most commonly encountered in cold extrusion are:

- Tool breakage
- Galling or scoring of tools
- Workpieces sticking to dies
- Workpieces splitting on outside diameter or cupping in inside diameter
- Excessive buildup of lubricant in dies

Table 1 lists the most likely causes of these problems.

Table 1 Problems in cold extrusion and some potential causes

Problem	Potential cause
Tool breakage	**Slug not properly located in die** **Slug material not completely annealed** **Slug not symmetrical or not properly shaped** **Improper selection or improper heat treatment of tool material** **Misalignment and/or excessive deflection of tools and equipment** **Incorrect preloading of dies** **Damage caused by double slugging or overweight slugs**
Galling or scoring of tools	**Improper lubrication of slugs** **Improper surface finish of tools** **Improper selection or improper heat treatment of tool material** **Improper edge or blend radii on punch or extrusion die**
Workpieces sticking to dies	**No back relief on punch or die** **Incorrect nose angle on punch and incorrect extrusion angle of die** **Galled or scored tools**
Workpieces splitting on outside diameter or forming chevrons on inside diameter	**Slug material not completely annealed** **Reduction of area either too great or too small** **Excessive surface seams or internal defects in work material** **Incorrect die angles**
Excessive buildup of lubricant on dies	**Inadequate vent holes in die** **Excessive amount of lubricant used** **Lack of a means of removal of lubricant, or failure to prevent lubricant buildup by spraying the die with an air-oil mist**

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

Cold Extrusion of Aluminum Alloy Parts

Aluminum alloys are well adapted to cold (impact) extrusion. The lower-strength, more ductile alloys, such as 1100 and 3003, are the easiest to extrude. When higher mechanical properties are required in the final product, heat-treatable grades are used.

The cold extrusion process should be considered for aluminum parts for the following reasons. High production rates--up to 4000 pieces per hour--can be achieved. However, even when parts are large or of complex shape, lower production rates may still be economical. The impact-extruded part itself has a desirable structure. It is fully wrought, achieving maximum strength and toughness. It is a near-net shape. There is no parting line, and all that may be required is a trim to tubular sections. Surface finish is good. Impacts have zero draft angles, and tolerances are tight. Once impacted, sections can be treated in the same manner as any other piece of wrought aluminum.

From a design standpoint, aluminum impacts should be considered in the following situations:

- For hollow parts with one end partially or totally closed
- When multiple-part assemblies can be replaced with a one-piece design
- When a pressure-tight container is required
- When bottoms must be thicker than the walls or the bottom design includes bosses, tubular extensions, projections, or recesses
- When a bottom flange is required
- When bottoms, sidewalls, or heads have changes in section thickness

Aluminum provides the characteristics of good strength-to-weight ratio, machinability, corrosion resistance, attractive appearance, and high thermal and electrical conductivity. It is also nonmagnetic, nonsparking, and nontoxic.

Although nearly all aluminum alloys can be cold extruded, the five alloys listed in Table 2 are most commonly used. The alloys in Table 2 are listed in the order of decreasing extrudability based on pressure requirements. The easiest alloy to extrude (1100) has been assigned an arbitrary value of 1.0 in this comparison.

Table 2 Relative pressure requirements for the cold extrusion of annealed slugs of five aluminum alloys (alloy 1100 = 1.0)

Alloy	Relative extrusion pressure
1100	**1.0**
3003	**1.2**
6061	**1.6**
2014	**1.8**

7075	**2.3**

Temper of Work Metal

The softer an alloy is, the more easily it extrudes. Many extrusions are produced directly from slugs purchased in the O (annealed, recrystallized) temper. In other applications, especially when slugs are machined from bars, the slugs are annealed after machining and before surface preparation. The raw material is often purchased in the F (as-fabricated) temper to improve machinability, and the cut or punched slugs are then annealed before extrusion.

When extruding alloys that will be heat treated, such as 6061, common practice is to extrude the slug in the O temper, solution treat the preform to the T4 temper, and then size or finish extrude. This procedure has two advantages. First, after solution treatment, the metal is reasonably soft and will permit sizing or additional working, and, second, the distortion caused by solution treatment can be corrected in final sizing. After sizing, the part can be aged to the T6 temper, if required.

Size of Extrusions

Equipment is readily available that can produce backward and forward extrusions up to 406 mm (16 in.) in diameter. Backward extrusions can be up to 1.5 in (60 in.) long. The length of forward extrusions is limited only by the cross section of the part and the capacity of the press. Irrigation tubing with a 152 mm (6 in.) outside diameter and a 1.47 mm (0.058 in.) wall thickness has been produced in lengths up to 12.2 m (40 ft). Small-outside-diameter tubing (<25 mm, or 1 in.) has been produced by cold extrusion in 4.3 in (14 ft) lengths.

Hydraulic extrusion and forging presses, suitably modified, are used for making very large extrusions. Parts up to 840 mm (33 in.) in diameter have been produced by backward extrusion from high-strength aluminum alloys in a 125 MN (14,000 tonf) extrusion press. Similar extrusions up to 1 m (40 in.) in diameter have been produced in large forging presses.

Presses

Both mechanical and hydraulic presses are used in the extrusion of aluminum. Presses for extruding aluminum alloys are not necessarily different from those used for steel. There are, however, two considerations that enter into the selection of a press for aluminum. First, because aluminum extrudes easily, the process is often applied to the forming of deep cuplike or tubular parts, and for this application, the press should have a long stroke. Again, because aluminum extrudes easily, the process is often used for mass production, which requires that the press be capable of high speeds.

The press must have a stroke that is long enough to permit removal of the longest part to be produced. Long shells are sometimes cold extruded in short-stroke knuckle-type presses, in which the punch is tilted forward or backward for removal of the workpiece.

Because of their high speeds, mechanical crank presses are generally preferred for producing parts requiring up to about 11 MN (1200 tonf) of force. Production of as many as 70 extrusions per minute (4200 per hour) is not unusual, and higher production rates are often obtained. Therefore, auxiliary press equipment is usually designed for a high degree of automation when aluminum is to be extruded.

Cold-heading machines are also used for the cold extrusion of aluminum parts. Hollow aluminum rivets are formed and extruded in cold headers in mass-production quantities. In general, the extruded parts are small and usually require an upsetting operation that can be done economically in a cold header.

Tooling

Tools designed especially for extruding aluminum may be different from those used for steel, because aluminum extrudes more easily. For example, a punch used for the backward extrusion of steel should not have a length-to-diameter ratio greater than about 3 to 1, however, this ratio, under favorable conditions, can be as high as 17 to 1 for aluminum (although a 10 to 1 ratio is usually the practical maximum).

Dies. Three basic types of dies for extruding aluminum are shown in Fig. 13. Solid dies are usually the most economical to make. Generally, a cavity is provided in each end so that the die can be reversed when one end becomes cracked or worn.

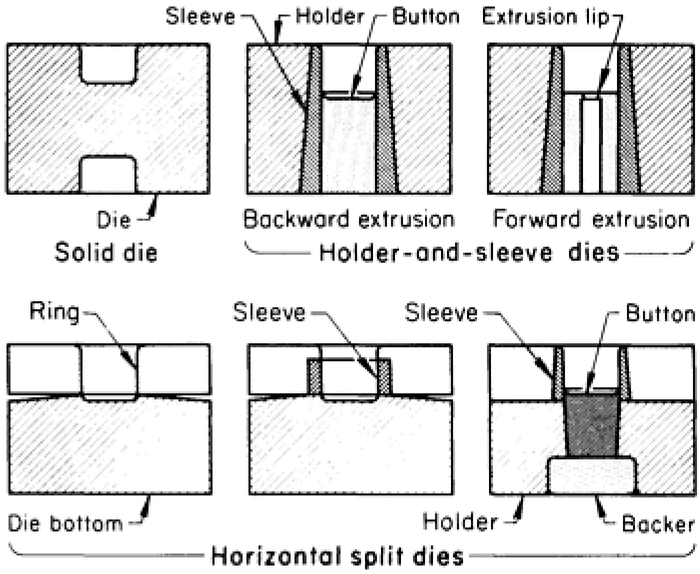


Fig. 13 Three types of dies used in the cold extrusion of aluminum alloy parts

Holder-and-sleeve dies are used when extrusion pressures are extremely high. This type of die consists of a shrink ring or rings (the holder), a sleeve, and an insert (button). The die sleeve is prestressed in compression in the shrink ring to match the tension stress expected during extrusion.

Horizontal split dies are composed of as many as four parts: a shrink ring, a sleeve (insert), and a one-piece or two-piece base. Figure 13 identifies the one-piece base as a die bottom, and the components of the two-piece base as a holder and a backer.

Compared to the die cavities used in the backward extrusion of steel, the die cavities for aluminum shown in Fig. 14 are notably shallow, reflecting a major difference in the extrusion characteristics of the two metals. Steel is more difficult to extrude, requiring higher pressures and continuous die support of the workpiece throughout the extrusion cycle. In contrast, aluminum extrudes readily, and when the punch strikes the slug in backward extrusion, the metal squirts up the sides of the punch, following the punch contours without the external restraint or support afforded by a surrounding die cavity.

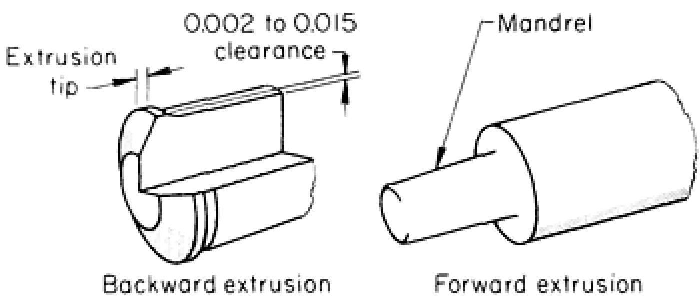


Fig. 14 Typical punches for backward and forward extrusion of aluminum alloy parts. Clearance given in inches

Punches. Typical punches for forward and backward extrusion are shown in Fig. 14. In the backward extrusion of deep cuplike parts, specially designed punches must be used to facilitate stripping.

Tool Materials. Typical tool materials and their working hardnesses for the extrusion of aluminum are given in Table 3. Additional information on tool materials is available in the section "Tool Materials" in this article.

Table 3 Typical tool steels used in extruding aluminum

Tool	**AISI steel**	Hardness, HRC
Die, solid	W1	**65-67**
Die sleeve[(a)]	D2	**60-62**
	L6	**56-62**
	H13	**48-52**
Die button[(b)]	H11	**48-50**
	H13	**48-50**
	L6	**50-52**
	H21	**47-50**
	T1	**58-60**
Ejector	D2	**55-57**
	S1	**52-54**
Punch	S1	**54-56**
	D2	**58-60**
	H13	**50-52**
Stripper	L6	**56-58**
Mandrel, forward	S1	**52-54**
	H13	**50-52**

Holder	H11	**42-48**
	H13	**42-48**
	4130	**36-44**
	4140	**36-44**

(a) Cemented carbide is sometimes used for die sleeves.

(b) Maraging steel is sometimes used for die buttons.

Stock for Slugs

Slugs for extrusions are obtained by blanking from plate; by sawing, shearing, or machining from bars; or by casting. In general, the methods for preparing aluminum slugs are similar to those for preparing slugs from other metals and are therefore subject to the same advantages and limitations (see the section "Preparation of Slugs" in this article).

Rolled aluminum alloy plate is widely used as a source of cold extrusion stock. The high speed at which slugs can be prepared is the major advantage of blanking from rolled plate. When slug thickness is greater than about 50 mm (2 in.) or when the thickness-to-diameter ratio is greater than about 1 to 1, blanking from plate is uneconomical, if not impossible. Blanking is also excessively wasteful of metal, which negates a principal advantage of the cold extrusion process.

Sawing from bars is widely used as a method of obtaining slugs. More accurate slugs are produced by sawing than by blanking; however, as in blanking, a considerable amount of metal is lost.

When "doughnut" slugs are required, they can be sawed from tubing, or they can be punched, drilled, or extruded. Machined slugs (such as those produced in an automatic bar machine) are generally more accurate but cost more than those produced by other methods.

Cast slugs can also be used; the selection of a cast slug is made on the basis of adequate quality at lower fabricating cost. Compositions that are not readily available in plate or bar stock can sometimes be successfully cast and extruded. There is often a savings in metal when a preform can be cast to shape.

Tolerance on slug volume may vary from ±2% to ±10%, depending on design and economic considerations. When extrusions are trimmed, as most are, slug tolerance in the upper part of the above range can be tolerated. When extrusions are not trimmed and dimensions are critical, the volume tolerance of the slugs must be held close to the bottom of the range. In the high-quantity production of parts such as thin-wall containers, the degree to which slug volume must be controlled is often dictated by metal cost.

Surface Preparation

Slugs of the more extrudable aluminum alloys, such as 1100 and 3003, are often given no surface preparation before a lubricant is applied prior to extrusion. For slugs of the less extrudable aluminum alloys or for maximum extrusion severity or both, surface preparation may be necessary for retention of lubricant. One method is to etch the slugs in a heated caustic solution, followed by water rinsing, nitric acid desmutting, and a final rinse in water. For the most severe extrusion, slug surfaces are given a phosphate coating before the lubricant is applied. Additional information on the alkaline etching, acid desmutting, and phosphate coating of aluminum alloys is available in the article "Surface Engineering of Aluminum and Aluminum Alloys" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Lubricants. Aluminum and aluminum alloys can be successfully extruded with such lubricants as high-viscosity oil, grease, wax, tallow, and sodium-tallow soap. Zinc stearate, applied by dry tumbling, is an excellent lubricant for extruding aluminum. In applications in which it is desirable to remove the lubricant, water-soluble lubricants are used to reduce the wash cycle.

The lubricant should be applied to metal surfaces that are free from foreign oil, grease, and dirt. Preliminary etching of the surfaces (see above) increases the effectiveness of the lubricant.

For the most difficult aluminum extrusions (less extrudable alloys or greater severity or both), the slugs should be given a phosphate treatment, followed by application of a soap that reacts with the surface to form a lubricating layer similar to that formed when extruding steel.

Impact Parts

Impact parts range from simple cuplike parts such as compressed air filter bowls, switch housings, and brake pistons to such complex parts as aerosol cylinders and ribbed cans, electrical fittings, motor housings, and home appliance parts. Numerous examples and design criteria are given in "Aluminum Impacts Design Manual and Application Guide" (see the Selected References at the end of this article).

Shallow cuplike parts can be easily extruded from most of the wrought aluminum alloys. If the wall thickness is uniform and the bottom is nearly flat, shallow cups can be produced in one hit (blow) at high production rates; if the shape is more complex, two or more hits may be needed. In the following example two hits were used to produce a part with an internal boss.

Example 7: Use of a Preform for Producing a Complex Bottom.

The aluminum alloy 1100-O housing shown in Fig. 15 required two extrusion operations on a hydraulic 3 MN (350 tonf) press because of the internal boss, which was formed by backward extrusion in a second operation, as shown in Fig. 15. The blended angle in the preform functioned as a support for the finishing punch during extrusion of the internal boss. This counteracted the side pressure that was created as the metal flowed into the cavity of the finishing punch.

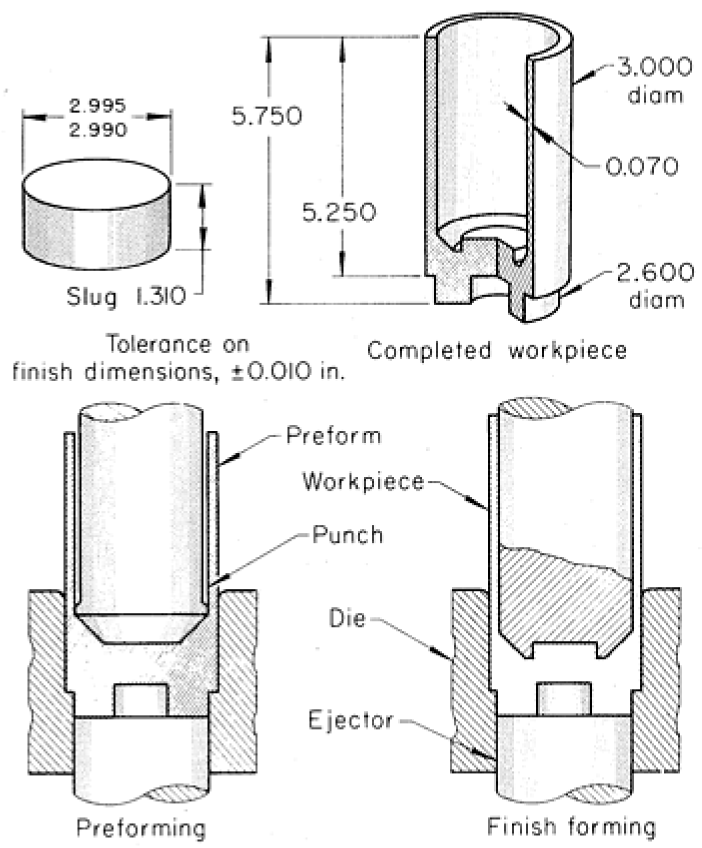


Fig. 15 Aluminum alloy 1100-O housing that was extruded in two operations because of an internal boss.

Dimensions given in inches

The slug was sawed from bar and annealed; zinc stearate lubricant was used. The production rate was 350 pieces per hour for the preforming operation and 250 pieces per hour for finish forming. Minimum tool life was 100,000 pieces.

Deep Cuplike Parts. Although cups having a length as great as 17 times the diameter have been produced, this extreme condition is seldom found in practice, because a punch this slender is likely to deflect and cause nonuniform wall thickness in the backward-extruded product. The length of the cup and the number of operations (use of preform) are not necessarily related. Whether or not a preform is required depends mainly on the finished shape, particularly of the closed end. When forming deep cups from heat-treatable alloys such as 6061, if the amount of reduction is 25% or more in the preform, the workpiece should be reannealed and relubricated between reforming and finish extruding.

Parts with Complex Shapes. Producing extrusions from aluminum and aluminum alloys in a single hit is not necessarily confined to simple shapes. The extrusion described in Example 9 was produced in a single hit despite its relatively complex shape. For extrusions with longitudinal flutes, stems, or grooves, the use of one of the most extrudable alloys, such as 1100, is helpful in minimizing difficulties. Sometimes, however, a less extrudable alloy can be used to form a complex shape in one hit.

The successful extrusion of complex shapes, especially in a single hit, depends greatly on tool design and slug design. Some developmental work is usually required for each new job before it can be put into production.

Example 8: Maximum Extrudability for a Complex Shape.

The hydraulic cylinder body shown in Fig. 16 was extruded from a solid slug in one hit. Aluminum alloy 1100, which has maximum extrudability, was required for this part because of the abrupt changes in section of the cylinder body. Surface cracks and laps resulted when more difficult-to-extrude alloys were used. The different wall thicknesses and steps in this design represent near-maximum severity for extruding in one hit, even with the most extrudable alloy. During the development of this part, it was necessary to change the face angles, shorten the steps, and blend the outside ribs more gradually to ensure complete fillout. The part was produced on a 7 MN (800 tonf) mechanical press set at 4.4 MN (500 tonf). The slug was sawed from bar, annealed, and lubricated with zinc stearate. Production rate in a single-station die was 300 pieces per hour, and minimum tool life was 70,000 pieces.

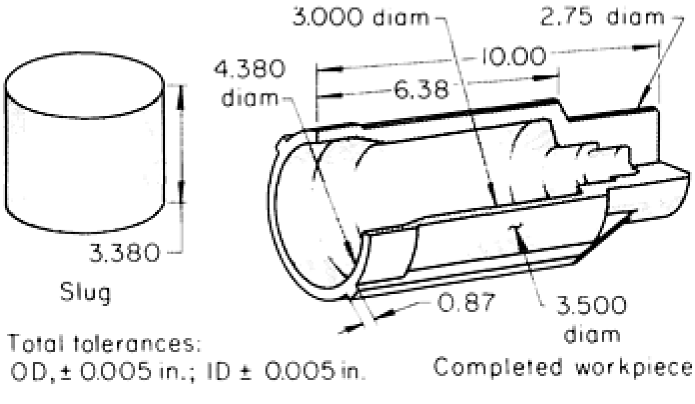


Fig. 16 Aluminum alloy 1100-O hydraulic cylinder body extruded in one hit. The complexity of this part is close to the maximum producible for one-hit extrusion of alloy 1100-O. Dimensions given in inches

Dimensional Accuracy

In general, aluminum extrusions are manufactured to close tolerances. The closeness depends on size, shape, alloy, wall thickness, type and quality of tooling, and press equipment. Lubrication and slug fit in the die are also important.

Wall thickness tolerances range from ±0.025 to ±0.13 mm (±0.001 to ±0.005 in.) for relatively thin-wall cylindrical shapes of moderate size extruded from low-strength alloys, but may be as great as ±0.25 to ±0.38 mm (±0.010 to ±0.015 in.) for large parts of high-strength alloys. Wall-thickness tolerances for rectangular shells range from ±0.13 to ±0.38 mm

(±0.005 to ±0.015 in.), depending on size, alloy, and nominal wall thickness. Diameter tolerances typically range from ±0.025 mm (±0.001 in.) for small parts to ±0.25 to ±0.38 mm (±0.010 to ±0.015 in.) for large high-strength alloy parts. Closer control of diameter can be achieved on small heavy-wall parts by centerless grinding of the extrusions (provided the alloy is one that can be ground satisfactorily). Dimensional tolerances in the forged portion of the impact are influenced by the same variables as those listed above, but a range of ±0.13 to ±0.38 mm (±0.005 to ±0.015 in.) is typical. Variations in extruded length usually necessitate a separate trimming operation.

Surface finish typically ranges from 0.5 to 1.8 μm (20 to 70 μin.). Smoother surfaces can sometimes be obtained by using extreme care in surface preparation and lubrication of the work metal and by paying close attention to the surface condition of the tools.

Cold Extrusion

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

Cold Extrusion of Copper and Copper Alloy Parts

Oxygen-free copper (Copper Development Association alloy C10200) is the most extrudable of the coppers and copper-base alloys. Other grades of copper and most of the copper-base alloys can be cold extruded, although there are wide differences in extrudability among the different compositions. For example, the harder copper alloys, such as aluminum-silicon bronze and nickel silver, are far more difficult to extrude than the softer, more ductile alloys, such as cartridge brass (alloy C26000), which can satisfactorily withstand cold reduction of up to 90% between anneals.

Alloys containing as much as 1.25% Pb can be successfully extruded if the amount of upset is mild and the workpiece is in compression at all times during metal flow. Copper alloys containing more than 1.25% Pb are likely to fracture when cold extruded.

The pressure required for extruding a given area for one of the more extrudable coppers or copper alloys (such as C10200 or C26000) is less than that required for extruding low-carbon steel. However, the pressure required for extruding copper alloys is generally two to three times that required for extruding aluminum alloys (depending on the copper or aluminum alloy being compared).

The length of a backward-extruded section is limited by the length-to-diameter ratio of the punch and varies with unit pressure. This ratio should be a maximum of 5 to 1 for copper. A ratio of 10 to 1 is common for the extrusion of aluminum, and ratios as high as 17 to 1 have been used. The total reduction of area for copper or copper alloys, under the best conditions, should not exceed 93%.

Equipment and Tooling

Copper and copper alloys can be extruded in hydraulic or mechanical presses or in cold-heading machines. Tooling procedures and tool materials for the extrusion of copper alloys are essentially the same as those for extruding steel (see the section "Tool Materials" in this article).

Preparation of Slugs

Sawing, shearing, and machining are the methods used to prepare copper and copper-alloy slugs. Each method has advantages and limitations. Sawing or shearing is generally used to produce solid slugs. Machining (as in a lathe) or cold forming in auxiliary equipment is seldom used unless a hole in the slug, or some other modification, is required.

Surface Preparation. In applications involving minimum-to-moderate severity, copper slugs are often extruded with no special surface preparation before the lubricant is applied. However, for the extrusion of harder alloys (aluminum bronze, for example) or for maximum severity or both, best practice includes the following surface preparation before the lubricant is applied:

- Cleaning in an alkaline cleaner to remove oil, grease, and soil

- Rinsing in water
- Pickling in 10 vol% sulfuric acid at 20 to 65 °C (70 to 150 °F) to remove metal oxides
- Rinsing in cold water
- Rinsing in a well-buffered solution, such as carbonate or borate, to neutralize residual acid or acid salts

Lubrication. Zinc stearate is an excellent lubricant for extruding copper alloys. Common practice is to etch the slugs as described above and then to coat them by dry tumbling in zinc stearate.

Examples of Practice

The following examples describe typical production practice for extruding parts from copper and brass. The part described in Example 10 could have been made by forging, casting, or machining; however, cold extrusion produced more accurate dimensions than forging or casting, consumed less material than machining, and was the lowest-cost method.

Example 9: Shearing, Heading, Piercing, Extruding and Upsetting in a Header.

The plumbing fitting shown in Fig. 17 was made of electrolytic tough pitch copper (alloy C11000) rod cold drawn (about 15% reduction of area) to a diameter of 26.9 mm (1.06 in.). The pipe-taper diameter and the 22.2 mm (0.875 in.) diameter of the tube socket were critical, being specified within 0.064 mm 0.0025 in.).

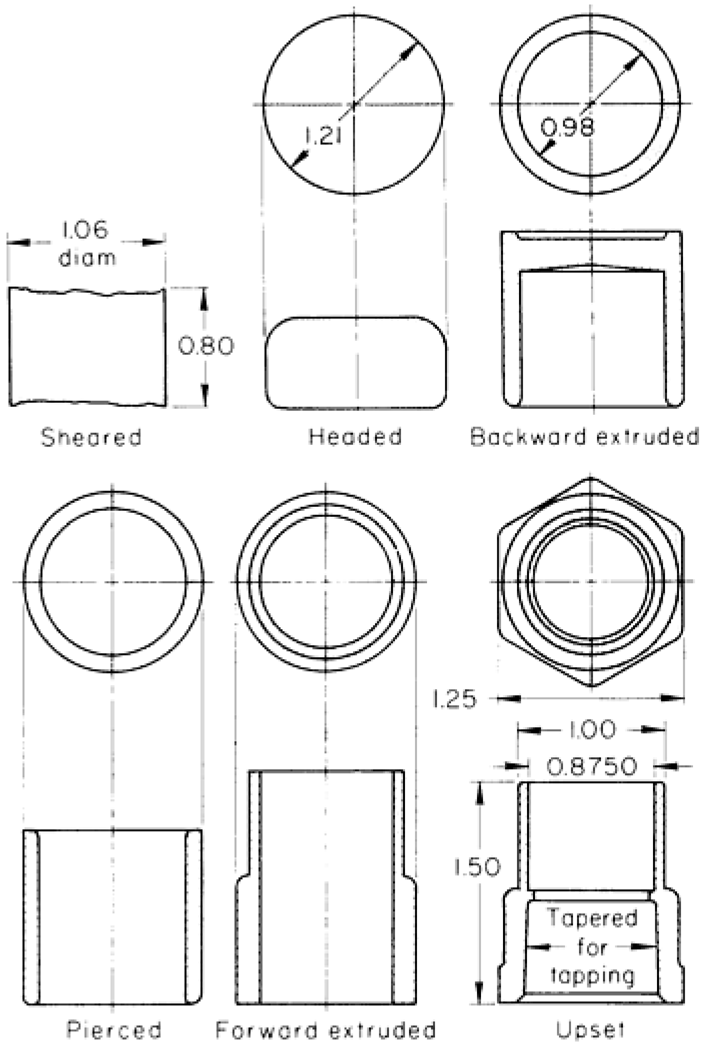


Fig. 17 Copper alloy C11000 plumbing fitting produced by the operations shown, including cold forward extrusion. Dimensions given in inches

Manufacture of the fitting consisted of feeding the rod stock into the cold-heading machine, which cut the stock into slugs 20.3 mm (0.80 in.) long and transferred the slugs progressively to dies for heading, backward extruding, piercing, forward extruding, and upsetting (Fig. 17). Only trimming on each end and tapping were required for completion. The extrusion equipment consisted of a five-die cold-heading machine.

The final cross-sectional area of the thin end after extrusion was 16.4% of the 30.7 mm (1.21 in.) diam headed preform from which the fitting was made. A reduction of this magnitude could have been made in one operation if a cylindrical rod were being extruded from the preform. The shape, however, was not suitable for production in one operation. Therefore, the fitting was made by backward and forward extrusion and mild upsetting. Production rate at 100% efficiency was 3600 pieces per hour, and minimum life of the D2 tool steel dies was 200,000 pieces.

Difficult Extrusions. The part described in the following example represents a difficult extrusion for two reasons. First, the metal (tellurium copper, alloy C14500) is one of the more difficult-to-extrude copper alloys, and second, the configuration (12 internal flutes and 12 external ribs) is difficult to extrude regardless of the metal used.

Example 10: Extrusion Versus Brazed Assembly for Lower Cost.

The rotor shown in Fig. 18 was originally produced by brazing a machined section into a drawn ribbed and fluted tubular section. By an improved method, this rotor was extruded from a sawed, annealed slug in one hit in a 1.7 MN (190 tonf) mechanical press. A lanolin-zinc stearate-trichloroethylene lubricant was used to produce 1800 pieces per hour. The extruded rotor was produced at less cost and had better dimensional accuracy than the brazed assembly, and there were fewer rejects. Minimum tool life was 50,000 pieces.

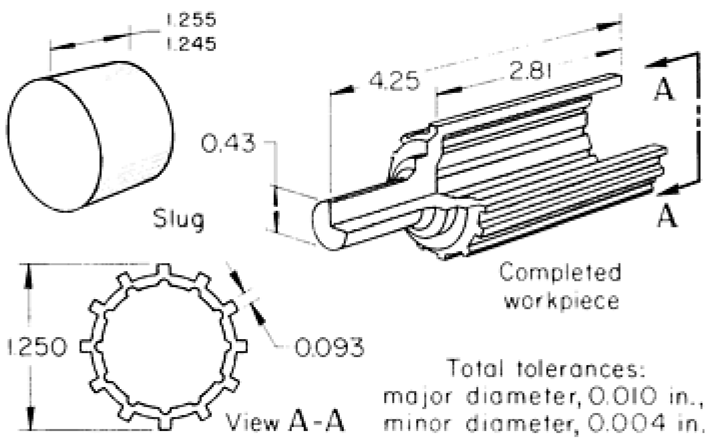


Fig. 18 Copper alloy C14500 rotor produced by combined backward and forward extrusion. Dimensions given in inches

Cold Extrusion

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

Impact Extrusion of Magnesium Alloys

Impact extrusion is used to produce symmetrical tubular magnesium alloy workpieces, especially those with thin walls or irregular profiles for which other methods are not practical. As applied to magnesium alloys, the extrusion process cannot be referred to as cold because both blanks and tooling must be preheated to not less than 175 °C (350 °F); workpiece temperatures of 260 °C (500 °F) are common.

Length-to-diameter ratios for magnesium extrusions may be as high as 15 to 1. There is no lower limit, but parts with ratios of less than about 2 to 1 can usually be press drawn at lower cost. A typical ratio is 8 to 1, and parts with higher length-to-diameter ratios are more amenable to forward extrusion than to backward extrusion. At all ratios, the mechanical properties of magnesium extrusions normally exceed those of the blanks from which they are made, because of the beneficial effects of mechanical working.

Equipment and Tooling

Mechanical presses are faster than hydraulic presses and are therefore used more often for impact extrusion, except when long strokes are needed. Presses with a capacity of 900 kN (100 tonf) and a stroke of 152 mm (6 in.) are adequate for most extrusion applications. Up to 100 extrusions per minute have been produced. Extrusion rate is limited only by press speed.

Dies for the impact extrusion of magnesium alloys differ from those used for other metals, because magnesium alloys are extruded at elevated temperature (usually 260 °C, or 500 °F). Common practice is to heat the die with tubular electric heaters. The die is insulated from the press, and an insulating shroud is built around the die. The top of the die is also covered, except for punch entry and the feeding and ejection devices. The punch is not heated, but it becomes hot during continuous operation; therefore, the punch should be insulated from the ram.

Punches and dies are usually made of a hot-work tool steel, such as H12 or H13, heat treated to 48 to 52 HRC. In one application, tools made of heat-treated H13 produced 200,000 extrusions. Carbide dies can be used and can extrude up to 10 million pieces.

The sidewalls of the die cavity should have a draft of approximately 0.002 mm per mm (0.002 in. per in.) of depth, which prevents the extrusion from sticking in the cavity. In normal operation, the part stays on the punch and is stripped from it on the upward stroke.

Procedure

Preparation of Slugs. Magnesium alloy slugs are prepared by the same methods as other metals--sawing from bar stock or blanking from plate, if rough edges can be tolerated. Slugs can also be made by casting. Slugs must be uniform in size and shape for centering in the die in order to ensure uniform wall thickness on the extrusion, which in turn depends on the clearance between die and punch. Slugs are lubricated by tumbling in a graphite suspension for 10 min until a dry coat develops.

For automatic impact extrusion of magnesium parts, the lubricated slugs are loaded into a hopper feed. The slugs are heated by an electric heater as they pass along the track between the hopper and the die.

Extrusion Practice. The heated slug is loaded onto the heated die, and the press is activated to produce the extrusion. Operating temperatures for the extrusion of magnesium alloys range from 175 to 370 °C (350 to 700 °F), depending on composition and operating speed. The operating temperature should be held constant in order to maintain tolerances.

In practice, slugs and dies are usually heated to 260 °C (500 °F) for feeding by tongs, because the rate of operation is slow. In automatic feeding, the slug and die temperature can be as low as 175 °C (350 °F), because speed is greater; dies absorb heat during operation and can increase in temperature by as much as 65 °C (150 °F). When a decrease in properties is not important, operating temperatures can be higher.

Extrusion Pressures

Pressures for the impact extrusion of magnesium alloys are about half those required for aluminum and depend mainly on alloy composition, amount of reduction, and operating temperature. Table 4 shows the pressures required to extrude several magnesium alloys to a reduction of area of 85% at temperatures ranging from 230 to 400 °C (450 to 750 °F).

Table 4 Pressures required for the impact extrusion of four magnesium alloys at various temperatures

Test pieces were extruded to a reduction in area of 85%.

Alloy	Extrusion pressure at temperature													
	230 °C MPa	**(450 °F) ksi**	**260 °C Mpa**	**(500 °F) ksi**	**290 °C MPa**	**(550 °F) ksi**	**315 °C MPa**	**(600 °F) ksi**	**345 °C MPa**	**(650 °F) ksi**	**370 °C MPa**	**(700 °F) ksi**	**400 °C MPa**	(750 °F) ksi
AZ31B	455	66	455	66	414	60	372	54	359	52	345	50	317	**46**
AZ61A	483	70	469	68	455	66	441	64	428	62	414	60	400	**58**
AZ80A	496	72	483	70	441	68	455	66	441	64	428	62	414	**60**

Thermal Expansion

Magnesium has a relatively high coefficient of thermal expansion compared to steel. Therefore, in order to ensure that the magnesium extrusion, when cooled to room temperature, will be within dimensional tolerance, it is necessary to multiply the room-temperature dimensions of steel tools by a compensatory factor for the temperature at which the magnesium alloy is to be extruded.

Tolerances

The tolerances for magnesium alloy extrusions are influenced by the size and shape of the part, the length-to-diameter ratio, and the press alignment. Table 5 gives typical tolerances for a magnesium part with a length-to-diameter ratio of 6 to 1.

Table 5 Typical tolerances for a magnesium alloy extrusion with a length-to-diameter ratio of 6 to 1

Dimension	Tolerance, mm (in.)
Diameter	**±0.05 (±0.002)**[(a)]
Bottom thickness	**±0.13 (±0.005)**[(b)]
Wall thickness, mm (in.)	
0.5-0.75 (0.020-0.029)	**±0.05 (±0.002)**
0.76-1.13 (0.030-0.044)	**±0.076 (±0.003)**
1.14-1.50 (0.045-0.059)	**±0.10 (±0.004)**
1.51-2.54 (0.060-0.100)	**±0.13 (±0.005)**

(a) Per 25 mm (1 in.) of diameter.

(b) All thicknesses

Cold Extrusion

Revised by P.S. Raghupathi, Battelle Columbus Division; W.C. Setzer, Consultant; and M. Baxi, Ullrich Copper, Inc.

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Introduction

HOT EXTRUSION is the process of forcing a heated billet to flow through a shaped die opening. The temperature at which extrusion is performed depends on the material being extruded (Table 1). Hot extrusion is used to produce long, straight metal products of constant cross section, such as bars, solid and hollow sections, tubes, wires, and strips, from materials that cannot be formed by cold extrusion (see the article "Cold Extrusion" in this Volume). The three basic types of hot extrusion are nonlubricated, lubricated, and hydrostatic (Fig. 1). This article will discuss only nonlubricated and lubricated hot extrusion; hydrostatic extrusion is covered in the article "Hydrostatic Extrusion" in this Volume.

Table 1 Typical billet temperatures for hot extrusion

Material	Billet temperature	
	°C	°F
Lead alloys	90-260	200-500
Magnesium alloys	340-430	650-800
Aluminum alloys	340-510	650-950
Copper alloys	650-1100	1200-2000
Titanium alloys	870-1040	1600-1900
Nickel alloys	1100-1260	2000-2300

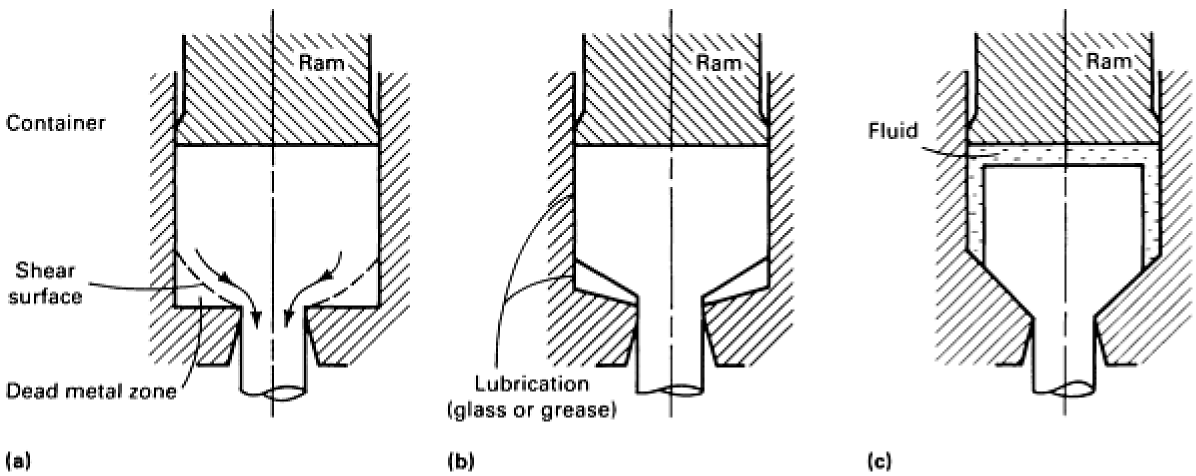


Fig. 1 Schematics of the nonlubricated (a), lubricated (b), and hydrostatic (c) extrusion processes.

In nonlubricated hot extrusion, the material flows by internal shear, and a dead-metal zone is formed in front of the extrusion die (Fig. 1a). Lubricated extrusion, as the name implies, uses a suitable lubricant (usually glass or grease) between the extruded billet and the die (Fig. 1b). In hydrostatic extrusion, a fluid film present between the billet and the die exerts pressure on the deforming billet (Fig. 1c). The hydrostatic extrusion process is primarily used when conventional lubrication is inadequate--for example, in the extrusion of special alloys, composites, or clad materials. For all practical purposes, hydrostatic extrusion can be considered an extension of the lubricated hot extrusion process.

Acknowledgements

Portions of this article were adapted from T. Allan, S.-I. Oh, and H.L. Gegel, Hot Extrusion of Rods, Tubes and Shapes, in *Metal Forming: Fundamentals and Applications,* American Society for Metals, 1983, p 189-217.

Nonlubricated Hot Extrusion

Nonlubricated hot extrusion is a relatively straightforward process once the conditions have been defined. However, a large number of metallurgical and processing factors interact and affect the mechanical properties, surface finish, and corrosion resistance of the final extruded shape. This extrusion method uses no lubrication on the billet, container, and die, and it can produce very complex sections, with mirror surface finishes and close dimensional tolerances, that are considered to be net extrusions. A flat-face (shear-face) die is often used in nonlubricated hot extrusion.

There are basically two methods of hot extruding materials without lubrication:

- Forward, or direct, extrusion
- Backward, or indirect, extrusion

In forward extrusion (Fig. 2a), the ram travels in the same direction as the extruded section, and there is relative movement between the billet and the container (Ref 1). In backward extrusion (Fig. 2b), the billet does not move relative to the container, and a die or punch is pushed against the billet to produce solid parts.

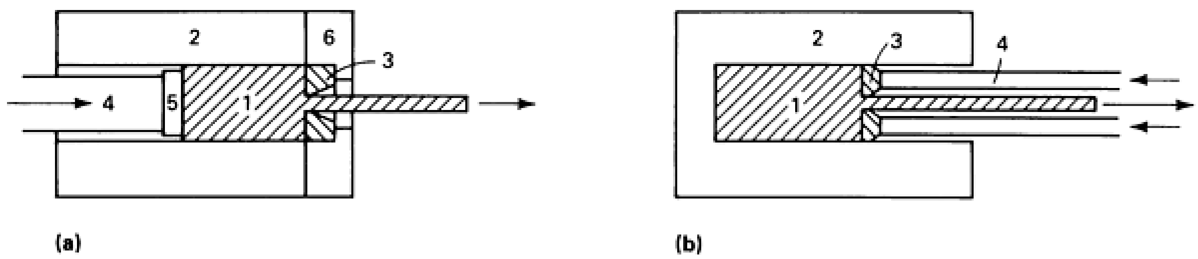


Fig. 2 Basic methods of extrusion. (a) Forward (direct). (b) Backward (indirect). 1, billet; 2, container; 3, die; 4, stem; 5, dummy block; 6, die backer.

Forward Extrusion

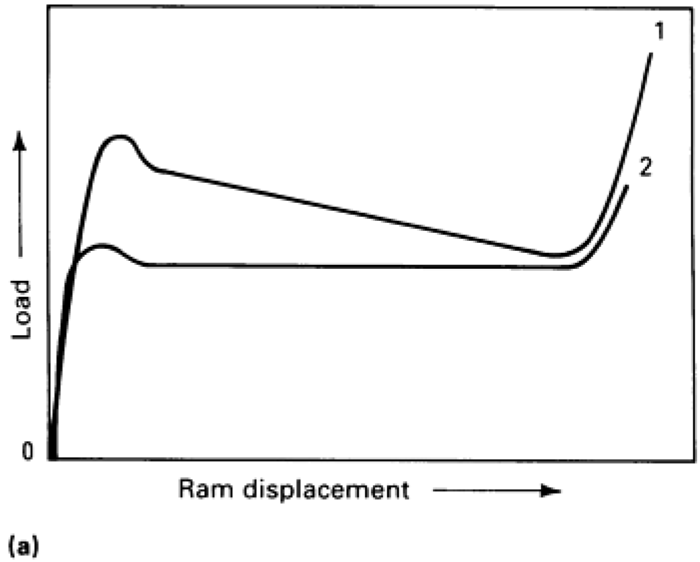
A typical sequence of operations for the forward extrusion of a solid section is as follows (Ref 1):

- The heated billet and the dummy block are loaded into the container
- The billet is extruded by the force of the ram being pushed against it. This upsets the billet, then forces

the metal to flow through the die. During extrusion, a thin shell of material may be left on the container walls. Extrusion is halted in order to leave a thin disk of material (butt) in the container

- The container is separated from the die, the extruded section with the butt, and the dummy block
- The discard (butt) is sheared off
- The shear die, the container, and the ram are returned to their initial (loading) positions

Typical load versus ram displacement curves for forward and backward extrusion are illustrated in Fig. 3, which shows that the load in forward extrusion initially increases very rapidly as the billet upsets to fill the container. This is followed by a further increase in pressure, and extrusion begins. A somewhat cone-shaped deformation zone then develops in front of the die aperture. After the maximum load has been reached, the extrusion pressure falls as the billet length decreases until a minimum is reached, then rapidly increases again. This last pressure increase occurs because only a disk of the billet remains and the metal must flow radially toward the die aperture. Resistance to deformation increases considerably with decreasing thickness.



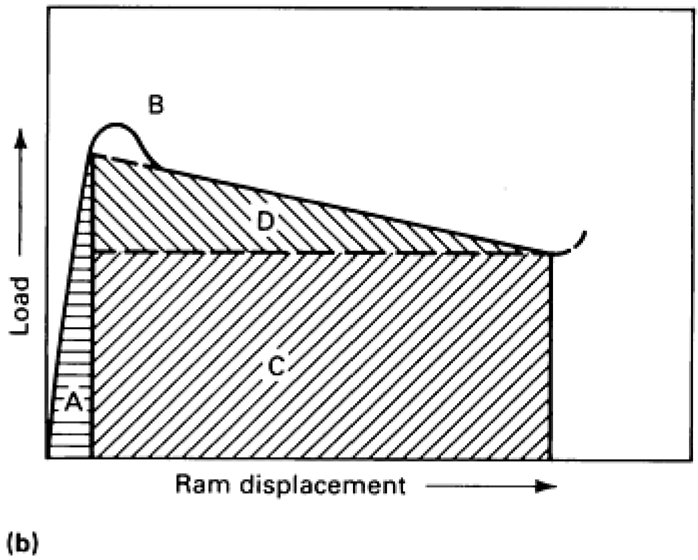


Fig. 3 Typical load versus ram displacement curves for nonlubricated extrusion processes. (a) Load versus ram displacement curves for forward (Curve 1) and backward (Curve 2) extrusion. (b) Division of the work of deformation. A, work of upsetting; B, work needed to initiate deformation; C, work of deformation; D, work

needed to overcome friction and shearing in direct extrusion

Backward Extrusion

In backward extrusion of a solid workpiece, the die is pushed by the hollow stem and moves relative to the container, but there is no relative displacement between the billet and the container (Ref 1). As a result, there is no frictional stress at the billet/container interface; therefore, the extrusion load and the temperature generated by deformation and friction are reduced, as shown in Fig. 3. The sequence of operations for the backward extrusion of a solid section is as follows:

- The die is inserted into the press
- The billet is loaded into the container
- The billet is extruded, leaving a butt
- The die and the butt are separated from the section

Backward extrusion offers a number of advantages, as follows:

- A 25 to 30% reduction in maximum load relative to direct extrusion
- Extrusion pressure is not a function of billet length, because there is no relative displacement between the billet and the container. Therefore, billet length is not limited by the load required for this displacement but only by the length and stability of the hollow stem needed for a given container length
- No heat is produced by friction between the billet and the container; consequently, no temperature increase occurs at the billet surface toward the end of extrusion, as is typical in the direct extrusion of aluminum alloys. Therefore, in backward extrusion, there is a lesser tendency toward cracking of the surfaces and edges, and extrusion speeds can be significantly higher
- The service life of the tooling is increased, especially that of the inner liner, because of reduced friction and temperatures

The disadvantage of backward extrusion is that impurities or defects on the billet surface affect the surface of the extrusion and are not automatically retained as a shell or discard in the container. As a result, machined billets are used in many cases. In addition, the cross-sectional area of the extrusion is limited by the size of the hollow stem.

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Lubricated Hot Extrusion

Generally, aluminum alloys are extruded without lubrication, but copper alloys, titanium alloys, alloy steels, stainless steels, and tool steels are extruded with a variety of graphite and glass-base lubricants. Commercial grease mixtures containing solid-film lubricants, such as graphite, often provide little or no thermal protection to the die. For this reason, die wear is significant in the conventional hot extrusion of steels and titanium alloys.

The Sejournet process is the most commonly used for the extrusion of steels and titanium alloys (Ref 2). In this process, the heated billet is rolled over a bed of ground glass or is sprinkled with glass powder to provide a layer of low-melting glass on the billet surface. Before the billet is inserted into the hot extrusion container, a suitable lubricating system is positioned immediately ahead of the die. This lubricating system can be a compacted glass pad, glass wool, or both. The prelubricated billet is quickly inserted into the container, along with the appropriate followers or a dummy block. The extrusion cycle is then started.

As a lubricant, glass exhibits unique characteristics, such as its ability to soften selectively during contact with the hot billet and, simultaneously, to insulate the hot billet material from the tooling. The tooling is usually maintained at a temperature that is considerably lower than that of the billet. In the extrusion of titanium and steel, the billet temperature is usually 1000 to 1250 °C (1830 to 2280 °F), but the maximum temperature the tooling can withstand is 500 to 550 °C (930 to 1020 °F). Therefore, compatibility can be attained only by using the appropriate lubricants, insulative die coating, and ceramic die inserts and by designing dies to minimize tool wear. Glass lubricants have performed satisfactorily on a production basis in extruding long lengths.

The choice between grease and glass lubricants is based mainly on the extrusion temperature. At low temperatures, lubrication is used only to reduce friction. At moderate temperatures, there is also some insulation between the hot billet and the tooling from the use of partially molten lubricants and vapor formation in addition to the lubrication effect. At temperatures above 1000 °C (1830 °F), the thermal insulation of the tooling from overheating is of equal importance to the lubricating effect, particularly with difficult-to-extrude alloys. The lubrication film can also impede oxidation. Lubricants can be classified into two groups, according to temperature:

- *Below 1000 °C (1830 °F):* Grease lubrication, such as grease, graphite, molybdenum disulfide, mica, talc, soap, bentonite, asphalt, and plastics (for example, high-temperature polyimides)
- *Above 1000 °C (1830 °F):* Glass lubrication, such as glass, basalt, and crystalline powder

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Metal Flow in Hot Extrusion

Metal flow varies considerably during extrusion, depending on the material, the material/tool interface friction, and the shape of the section. Figure 4 shows the four types of flow patterns that have been observed.

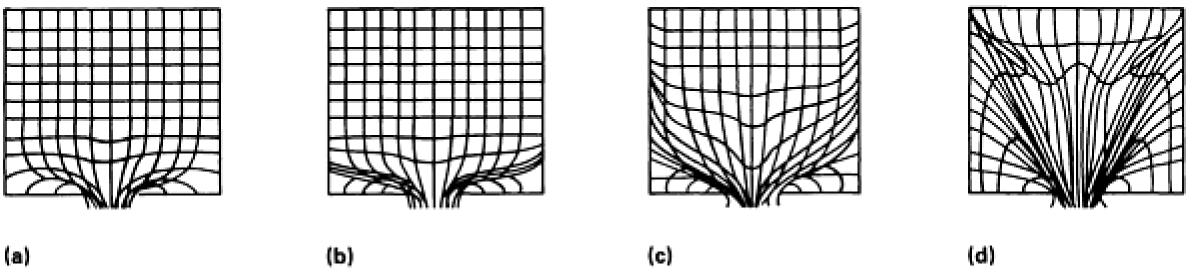

Fig. 4 Four types of flow patterns observed in the extrusion of metals. (a) Flow pattern S. (b) Flow pattern A. (c) Flow pattern B. (d) Flow pattern C. See text for details. Source: Ref 1

Flow pattern S (Fig. 4a) is characterized by the maximum possible uniformity of flow in the container. Plastic flow takes place primarily in a deformation zone directly in front of the die. The major part of the nonextruded billet, pushed as a rigid body through the die, remains undeformed; therefore, the front of the billet moves evenly into the deformation zone.

Flow pattern A (Fig. 4b) occurs in homogeneous materials when there is virtually no friction between the container and the billet but significant friction at the surface of the die and its holder. This retards the radial flow of the peripheral zones and increases the amount of shearing in this region. The result is a slightly larger dead-metal zone than that in flow type S, along with a correspondingly wider deformation zone. However, deformation in the center remains relatively uniform. Flow patterns of this type are seldom observed in nonlubricated extrusion; instead, they occur during the lubricated extrusion of soft metals and alloys, such as lead, tin, α-brasses, and tin bronzes, and during the extrusion of copper billets covered with oxide (which acts as a lubricant).

Flow pattern B (Fig. 4c) occurs in homogeneous materials if friction exists at both the container wall and at the surfaces of the die and die holder (Fig. 4c). The peripheral zones are retarded at the billet/container interface, while the lower resistance causes the material in the center to be accelerated toward the die. The shear zone between the retarded regions at the surface and the accelerated material in the center extends back into the billet to an extent that depends on the extrusion parameters and the alloy. Therefore, the dead-metal zone is large. At the start of extrusion, the shear deformation is concentrated in the peripheral regions, but as deformation continues, it extends toward the center. This increases the danger of material flowing from the billet surface--with impurities or lubricant--along the shear zone and finishing up under the surface of the extrusion. In addition, the dead-metal zone is not completely rigid and can influence, even if to a limited degree, the flow of the metal. Flow type B is found in single-phase (homogeneous) copper alloys that do not form a lubricating oxide skin and in most aluminum alloys.

Flow pattern C (Fig. 4d) occurs in the hot extrusion of materials having inhomogeneous properties when the friction is high (as in flow pattern B) and when the flow stress of the material in the cooler peripheral regions of the billet is considerably higher than that in the center. The billet surface forms a relatively stiff shell. Therefore, the conical dead-metal zone is much larger and extends from the front of the billet to the back. At the start of extrusion, only the material inside the funnel is plastic, and it is severely deformed, especially in the shear zone, as it flows toward the die. The stiff shell and the dead-metal zone are in axial compression as the billet length decreases; consequently, the displaced material of the outer regions follows the path of least resistance to the back of the billet, where it turns toward the center and flows into the funnel.

This type of flow is found in the (α + β) brasses, in which the cooling of the peripheral regions of the billet leads to an increase in flow stress, because the flow stress of the α phase is much higher than that of the β phase during hot working. As in the (α + β) brasses, flow type C will occur when there is a hard billet shell and, at the same time, the friction at the container wall is high. It can also occur without any phase change that leads to a higher flow stress if there is a large temperature difference between the billet and the container. This can take place in the extrusion of tin as well as of aluminum and its alloys.

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Extrusion Speeds and Temperatures

The temperatures developed during extrusion significantly influence the speed at which the process can be carried out. This is especially true in the extrusion of hard aluminum alloys (2*xxx* and 7*xxx*). A complex thermal situation exists as soon as the heated billet is loaded into the preheated container and extrusion begins. The temperatures are influenced by:

- Heat generation due to plastic deformation
- Heat generation due to internal shear and friction between the deforming material and the tooling
- Heat transfer within the billet
- Heat transfer between the billet and the tooling
- Heat transported with the extruded product

These phenomena occur simultaneously and result in a complex relationship among the material and process variables, that is, billet material and temperature, friction, tool material and temperature, extrusion speed, shape of the extruded section, and reduction in area.

The production rate can be increased by increasing the extrusion ratio (the ratio of the cross-sectional area of the billet to that of the extruded product) and the extrusion speed while maintaining the extrusion pressure at an acceptable level. For this purpose, the flow stress of the extruded material must be kept relatively low, for example, by increasing the billet preheating temperature. The combination of high billet temperature, large reduction in area, and high extrusion speed causes a considerable rise in temperature in the extruded material, especially near the section surface, because most of the plastic deformation and frictional energy is transformed into heat. This can cause surface defects or hot shortness, especially with difficult-to-extrude 2*xxx* and 7*xxx* aluminum alloys. With a typical extrusion ratio of 40:1, exit speeds in extruding these alloys would be of the order of 0.6 to 1.2 m/min (2 to 4 ft/min). Figure 5 shows the range of exit speeds encountered in the extrusion of various aluminum alloys. The extrusion rate depends greatly on the flow stress of the alloy under the process conditions, which in turn depends on the extrusion temperature and strain rate. Exit speeds are relatively high for soft (5*xxx* and 6*xxx*) alloys, but are quite low for harder alloys such as 7075 and 2024.

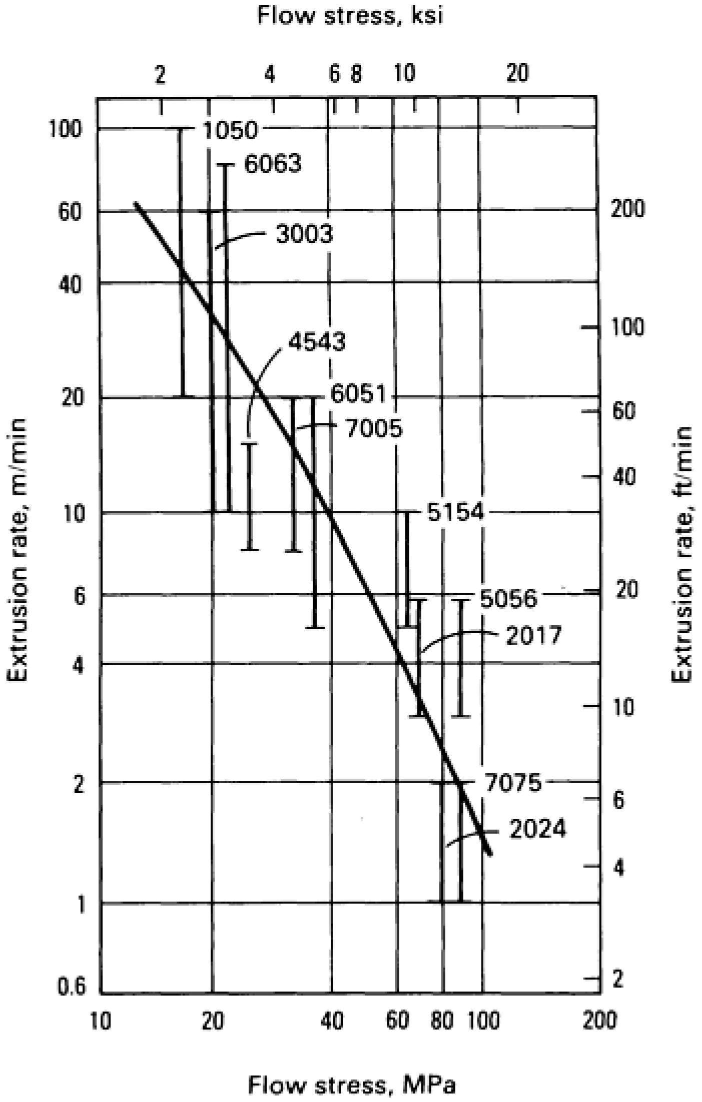

Fig. 5 Extrusion rate versus flow stress for various aluminum alloys. Source: Ref 3

Temperature increase and temperature distribution during extrusion have been investigated by many researchers (Ref 4, 5, 6, 7, 8). The emergent temperatures of aluminum, tin, and lead extruded at ram speeds from 1 to 30 m/min (3 to 100 ft/min) were measured (Ref 4). Figures 6 and 7 show the effects of extrusion ratio and ram speed on the temperature increase. A simple theoretical analysis was conducted to investigate the effect of ram speed on temperature increase (Ref 8). In this study, a billet of infinite length was assumed, container friction was neglected, and the interior of the container was assumed to be at the same temperature as the billet. The temperature of the billet varied along its length, but was assumed to be constant at any cross section. The model predicted a sigmoidal relationship between the logarithm of ram speed and the temperature rise. Based on this model, a ram speed program was devised that would give a constant emergent temperature. Experimental evaluation of this speed program resulted in maintenance of constant temperatures within ±3 K (±5 °F) for lead and ±6 K (±11 °F) for aluminum. The decrease in extrusion pressure with programmed ram speed was less than that with constant ram speed.

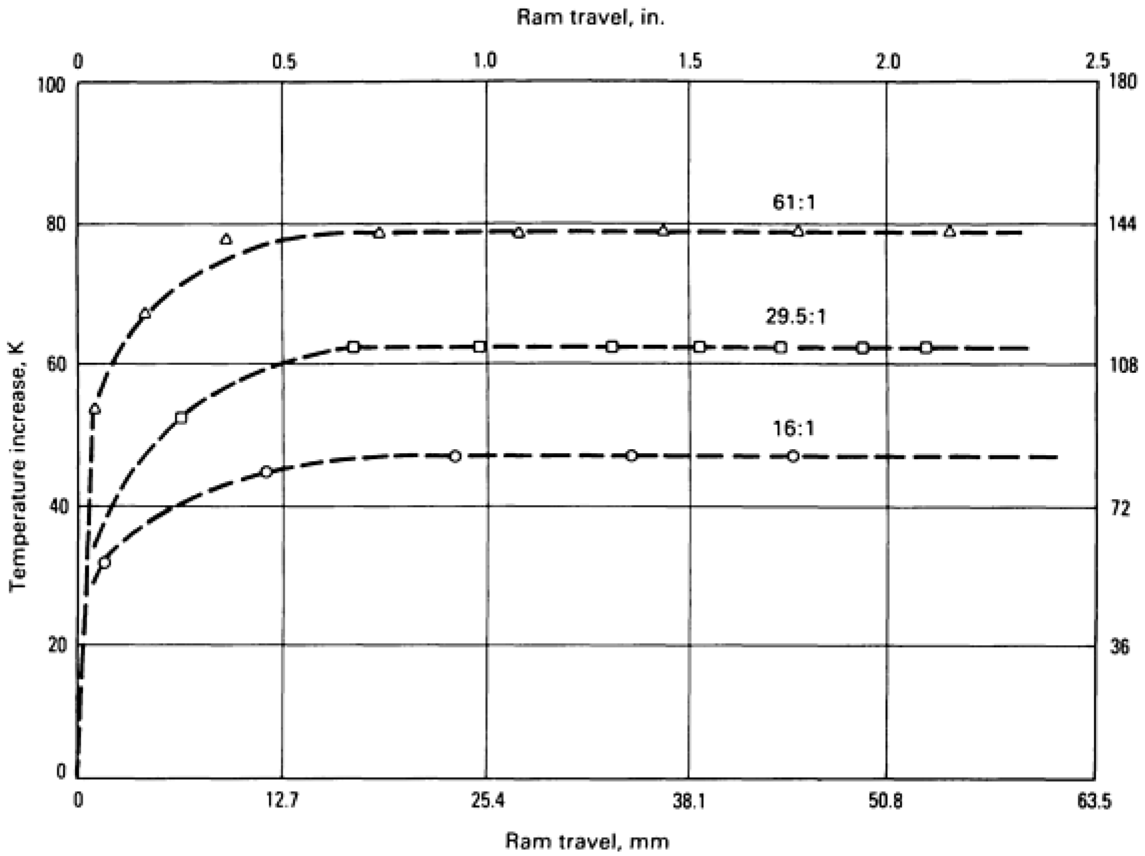

Fig. 6 Increase in emergent temperature versus extrusion ratio in the extrusion of lead. Extrusion ratios are indicated on the curves. Billet diameter: 51 mm (2 in.); billet length: 64 mm (2.5 in.); ram speed: 76 mm/min (3 in./min); starting temperature: 20 °C (70 °F). Source: Ref 4

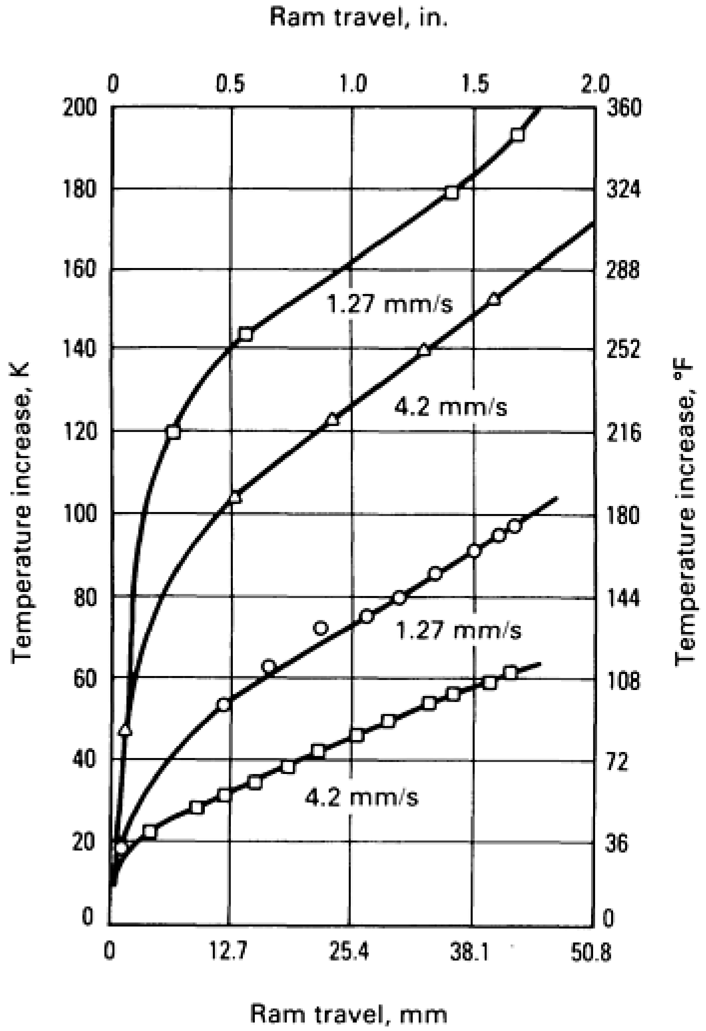

Fig. 7 Increase in emergent temperature versus ram speed in the extrusion of superpure aluminum. Ram speeds are indicated on the curves. Billet diameter: 38 mm (1.5 in.); billet length: 51 mm (2 in.); extrusion ratio: 16:1; starting temperature: 20 °C (70 °F). Source: Ref 4

Theoretical and practical studies of temperature distributions in the extrusion of aluminum alloys were conducted under conditions in which the container and tools were initially below, equal to, or above the initial billet temperature (Ref 5). For the particular experimental conditions examined, it was deduced that the increase in temperature under adiabatic conditions would be about 95 °C (205 °F). For practical purposes, it can be estimated that, in the extrusion of high-strength alloys, the maximum temperature increase likely to be encountered will not exceed 100 °C (212 °F). With the soft alloys, for which lower specific pressures are required, the temperature increase under normal production conditions is not likely to exceed 50 °C (120 °F).

Computer programs have been developed for predicting temperatures in the extrusion of rods and tubes in various materials (Ref 6, 7). As Fig. 8 shows, based on theoretical predictions and on experimental evidence, the product temperature increases as extrusion proceeds. The temperature at the product surface is higher than that at the product center. Therefore, the surface temperature of the product may approach the critical temperature at which hot shortness may occur only toward the end of the extrusion cycle. The temperature of the extruded product as it emerges from the die is one of the essential factors that influence product quality. Therefore, an ideal procedure for establishing the maximum speed of extrusion at all times would be to measure this temperature and to use it for controlling the ram speed. This procedure has been proposed by many researchers, but the problem of obtaining accurate and continuous measurement of the temperature of the extruded product remains unsolved. Methods of measuring product temperature by using various types of contact thermocouples, or by radiation pyrometry, have thus far proved to be impractical.

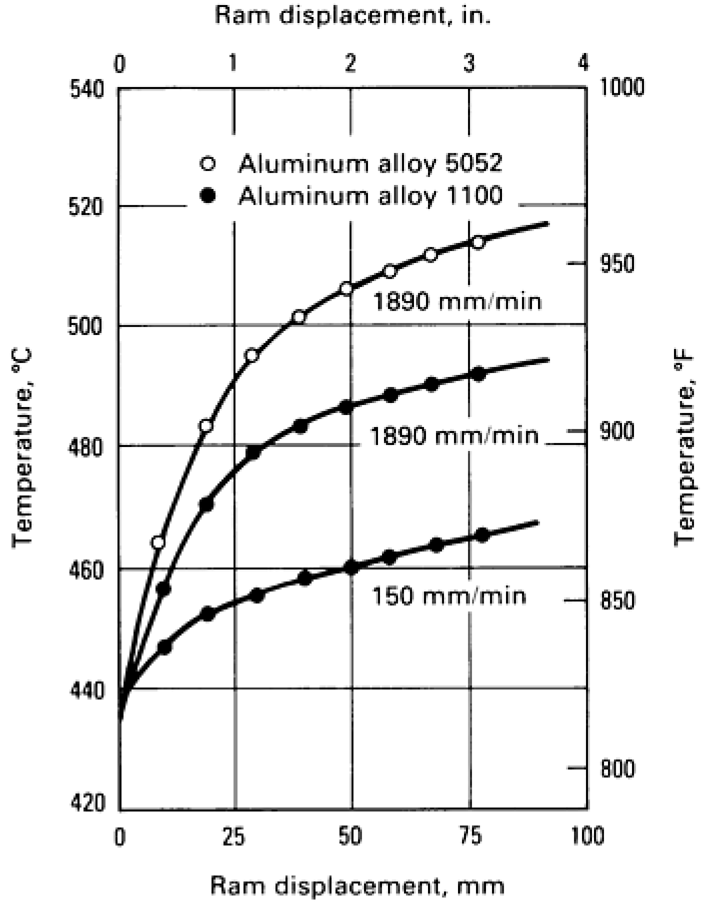

Fig. 8 Surface temperature of the extruded product versus ram displacement for two aluminum alloys. Ram velocities are indicated on the curves. Reduction ratio: 5:1; billet diameter: 71 mm (2.8 in.); billet length: 142 mm (5.6 in.); initial billet and tooling temperature: 440 °C (825 °F). Source: Ref 6

A system for isothermal extrusion was proposed in which the variation in ram speed necessary for maintaining the product temperature within the required limits was programmed (Ref 9). In presses designed to operate on this principle, the working stroke is divided into zones, each having a preset speed. In a press used for the extrusion of high-strength alloys, a time savings of 60% was claimed. Savings would be lower for more easily extrudable alloys.

In extrusion of aluminum alloys, temperature variations in the emerging product can be reduced by imposing a temperature gradient in the billet (Ref 10). The billet is inserted into the container such that the hot end is extruded first while the temperature of the cooler end increases during extrusion. This practice is not entirely satisfactory, because of the relatively high thermal conductivities of aluminum alloys; therefore, if any delays occur in the extrusion sequence, the temperatures in the billet tend to become uniform throughout the billet length. A better method consists of water quenching the feed table to the container. Another approach for increasing extrusion speed is to cool the die with water or nitrogen.

For controlling and predicting variations in ram speed during extrusion, computer simulations may be useful for predicting the temperature increase that occurs during the process (Ref 6, 7). The purpose of such computer-aided speed control is to attain maximum extrusion speeds with minimum variations in temperature in the extruded product. More information on the use of computers in die design and process simulation for hot extrusion is available in the section "Applications for Computer-Aided Design and Manufacture (CAD/CAM)" in this article.

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Presses for Hot Extrusion

Horizontal and vertical presses are used for hot extrusion. Horizontal presses are the most common. Most modern extrusion presses are driven hydraulically, but mechanical drives are used in some applications, such as the production of small tubes. Two basic types of hydraulic drives are available: direct and accumulator. In the past, accumulator presses were the most widely used type, but today direct-drive presses are used more extensively.

Accumulator-Drive Presses. The hydraulic circuit of an accumulator drive press consists of one or more air-over-water accumulators charged by high-pressure water pumps. The accumulator bottle (or bank of bottles) is designed to supply the quantity of water needed to provide the necessary pressure requirements throughout the extrusion stroke--with a pressure decrease not exceeding about 10%. This decrease in pressure is often critical in applications that involve marginal, difficult-to-extrude shapes. In addition to the high cost of high-pressure water pumps, accumulators, and valves, as well as the substantial floor space requirements, this pressure decrease characteristic of accumulator drives has resulted in the increasing popularity of direct-drive presses. However, a significant advantage of accumulator water drives is higher stem speeds (up to 380 mm/s, or 15 in./s), which make these units desirable for the extrusion of steel. Water is also a non-flammable hydraulic medium--an important consideration in the extrusion of very hot billets.

Direct-Drive Presses. Figure 9 shows a typical direct-drive oil-hydraulic press for hot extrusion. The increasing use of these presses has resulted mainly from the development of reliable, high-pressure, variable-delivery oil pumps, some of which operate at pressures over 34.5 MPa (5 ksi). Direct-drive presses are self-contained, and they require less floor space and are less expensive than accumulator-drive presses. More important, direct-drive units provide a constant force during the entire extrusion stroke, with no pressure decrease. A limitation of direct-drive presses is that the stem speeds are slower than those in accumulator drives. Stem speeds to 51 mm/s (2 in./s) are typical; however, speeds to 203 mm/s (8 in./s) can be reached by using oil accumulators with oil-hydraulic drives.

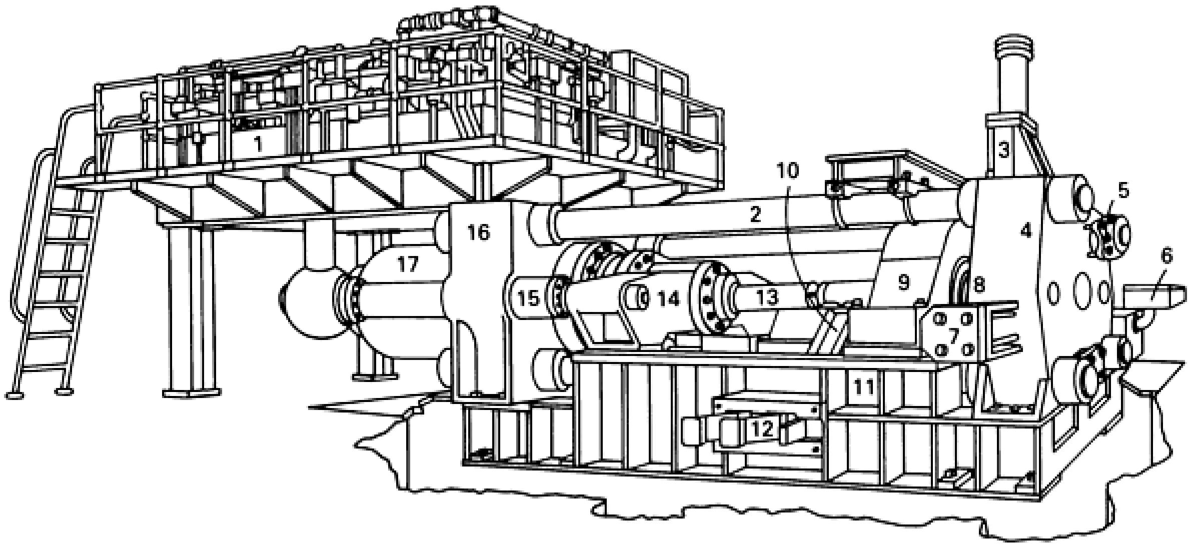

Fig. 9 Typical direct-drive hydraulic extrusion press. 1, Hydraulic power unit; 2, tie rods; 3, butt shear; 4, extrusion platen; 5, container shifting cylinders; 6, swiveling operator's console; 7, die slide; 8, container; 9, container housing; 10, billet loader; 11, press base; 12, billet loader cylinders; 13, pressing stem; 14, crosshead; 15, side cylinders; 16, cylinder platen; 17, main cylinder

Major improvements available on modern extrusion presses include simplified hydraulic circuits to facilitate troubleshooting, manifolded piping to reduce leakage and maintenance, and improved valves to minimize wear. Closed-loop constant-rate speed controls simplify the production of smooth finishes and uniform extrusion properties. In addition, the presses operate faster for increased productivity.

Solid-state programmable controllers have replaced magnetic relays on many presses for increased versatility, simplified troubleshooting, and ease of interfacing with computers. The use of computers for the presses and for the auxiliary equipment in an integrated extruding system enables the monitoring of all operations and instantaneously provides data on production, downtime, and inventory, as well as other information.

Force and Pressure Capacities. Presses for hot extrusion are usually rated in terms of force capacity, that is, the total force the press is capable of exerting upon the billet. However, press operation depends on the actual unit pressure exerted on the metal. For a press with a given force capacity, higher unit pressures can be obtained if the billet container is smaller in diameter. As the container increases in diameter, the unit pressure capability decreases, with a resultant decrease in extrusion capability.

The typical maximum unit pressure that is used on most extrusion presses is about 1035 MPa (150 ksi). This pressure is near the upper limit of the mechanical strengths of most tool steels used for extrusion. Higher pressures may result in premature tool failure.

Press Selection. The unit pressures needed for extrusion--a principal consideration in press selection--vary with the following factors:

- The metal to be extruded and its condition
- The length and temperature of the billet
- The complexity of the cross section of the product
- The speed of extrusion
- The reduction ratio

The reduction (extrusion) ratio equals the cross-sectional area of the container liner divided by the cross-sectional area of the extruded product.

Higher pressures are generally necessary at the beginning of the extrusion cycle. Pressure requirements decrease as extrusion progresses, then increase again as the butt of the billet is reduced to a thickness of about 12.7 to 25.4 mm ($\frac{1}{2}$ to 1 in.). Methods of determining press force and pressure requirements for the extrusion of various products are discussed in the section "Operating Parameters" in this article.

The advantages of using a press with sufficient capacity include the ability to use lower billet temperatures and faster speeds and the ability to obtain improved metallurgical properties in the extruded products. The use of a press having insufficient capacity can result in the inability to extrude (that is, the billets stick in the containers) or in extrusions of poor quality.

Any extrusion press requires a rigid structure as well as accurate and adjustable alignment of the stem, container, and die. Prestressed tie rod construction is used in most press structures. Modern presses permit die stack lengths that are longer than those possible with earlier models, and this provides better tool stability and improved tolerances on extruded products.

Press Accessories. Various accessories are available as standard or optional items for hot extrusion presses, including:

- Die slides or revolving die arms to facilitate changing of dies
- Indexing containers, and electrical heating elements to maintain the proper container temperature
- Piercing units and mandrel manipulators for the extrusion of tubes and hollow parts
- Internal or external billet loaders
- Cutoff shears or saws for separating the butt from the extruded product
- Mechanized butt and dummy block handling systems

Auxiliary Equipment. In addition to the press, billet heaters, stretchers, pullers, sawing equipment, and system controls are necessary for complete extrusion facilities. For many installations (especially aluminum extrusion), gas-fired log heaters have replaced gas-fired and induction billet heaters. The process of heating aluminum logs measuring 3.7 to 6.1 m (12 to 20 ft) in length and then cutting them to the required length as they emerge from the heater has eliminated the need to store billets of varying lengths. Log shears allow the press operator to tailor billet lengths to provide maximum yield from each billet with minimal scrap. Computer control ensures that the logs are sheared to the optimal billet length for the particular die being used and for the desired extrusion length.

Reliable extrusion pullers are available that reduce operator responsibilities, eliminate twisting of the extruded products, and ensure that equal-length extrusions are obtained from multiple-hole dies. These extrusion pullers also improve the efficiency of extrusion stretching operations. Fewer manipulations of the stretcher tailstock are necessary in order to accommodate unequal extrusion lengths, and the need to detwist extruded shapes prior to stretching is virtually eliminated. In many cases, stretching requires only one operator, located at the headstock; tailstock manipulation is controlled by the same operation. Several installations are equipped with completely programmed puller-stretcher combinations.

Beyond the stretcher, automatic saw tables are often provided. In many cases, cut-to-length extrusions are automatically stacked for subsequent heat treatment.

Enclosed water-filled chambers have been provided at the ends of several presses that are used to extrude copper tubing. The tubing is extruded directly into the chamber and remains submerged for the full length of the runout. A special gate prevents back-flow through the dies, and an end crimper prevents water from filling the tube. The result of this arrangement is the production of copper tubing with a refined grain structure and consistent grain orientation.

Tooling

The tooling for hot extrusion consists of such components as containers, container liners, stems (rams), dummy blocks, mandrels, spider or bridge dies for producing hollow extrusions, and flat or feeder plate dies. Flat-face and shaped dies are the two most common types (Fig. 10). Flat-face dies (also termed square dies) have one or more openings (apertures) that are similar in cross section to that of the desired extruded product. Dies for lubricated extrusion (also called shaped, converging or streamlined dies) often have a conical entry opening with a circular cross section that changes progressively to the final extruded shape required. Flat-face dies are easier to design and manufacture than shaped dies and are commonly used for the hot extrusion of aluminum alloys. Shaped dies are more difficult and costly to design and manufacture, and they are generally used for the hot extrusion of steels, titanium alloys, and other metals.

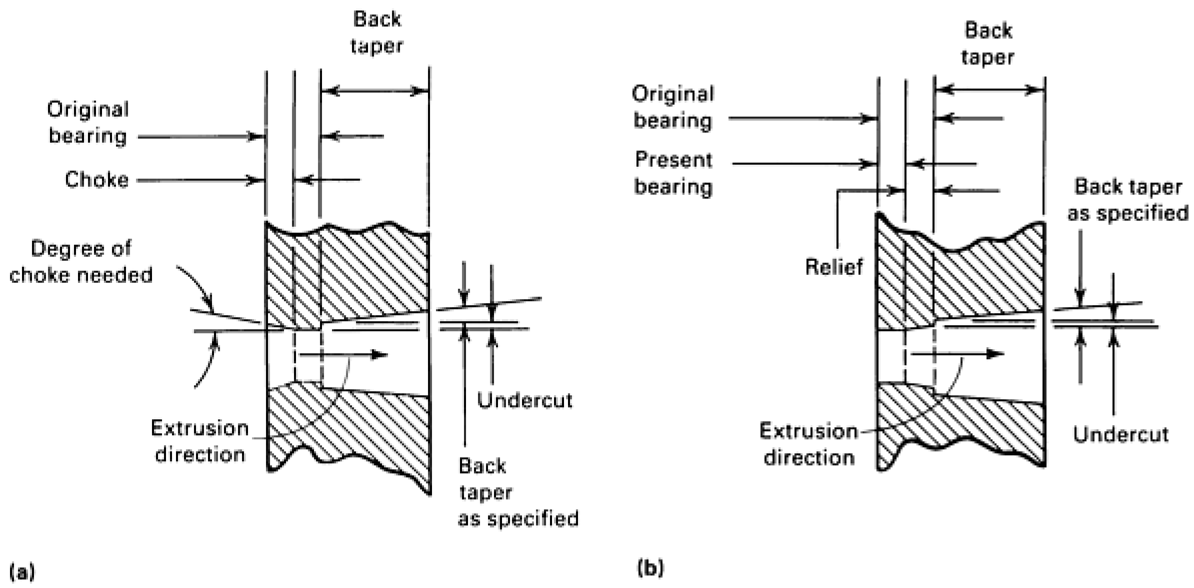

Fig. 10 Shaped die (a) and flat die (b) for hot extrusion

Die Design

Die design is a crucial aspect of the extrusion process that embodies art and science. Optimal design is influenced by such factors as the size of the shape to be produced, the maximum and minimum wall thicknesses, the press capacity, the length of the runout table, the stretcher capacity, the tool-stacking limitations, an understanding of the properties and characteristics of the metal to be extruded, and the press operating procedures and maintenance.

Computer-Aided Design and Manufacture. Computers are being used to design and manufacture dies (with computer numerical control machines) and to select process variables, such as extruding speed and billet temperatures. The software used is primarily based on an analysis of metal flow. Various design stages are displayed on a CRT screen, and the designer interacts with a computer to modify the design, based on experience. More information on the use of computers for die design is available in the section "Applications for Computer-Aided Design and Manufacture (CAD/CAM)" in this article.

Design Considerations. All metals shrink upon cooling after hot extrusion; therefore, a shrinkage allowance must be provided in the design of the dies. Deformation of the die under high pressures and expansion resulting from the high temperatures must also be considered in die design.

Another important consideration is the tendency for metal to flow faster through a larger opening than a smaller one. This must be compensated for in the design of dies to be used in extruding certain sections. For example, when a section to be extruded has a thick wall and a thin wall, various means are employed to retard metal flow through the thick section and to increase the flow rate through the thin section of the die.

The geometry of the die aperture at the front and back of the bearing surface is termed the choke and relief, respectively. A choke can be provided on certain portions of the bearing surface if the die designer anticipates difficulty in filling sharp corners or completing thin sections of the extruded product. This slows the rate of metal flow and consequently fills the die aperture. Increasing the amount of back relief at the exit side of the bearing surface increases the rate of metal flow.

For the hot extrusion of such materials as brass, bronze, and other soft metals, the dummy block is made smaller in diameter than the billet. In extruding, no lubrication is provided between the bore of the container liner and the outer surface of the billet. Consequently, friction prevents the outer surface of the billet from sliding, and the undesirable skin of the billet is left in the container as the dummy block shears the metal during its forward stroke. An additional press stroke is required to remove this retained metal before the next billet can be charged into the container.

Tool Materials

Table 2 lists typical materials and hardnesses for tools used in hot extrusion. The hot extrusion of aluminum is similar in many ways to that of magnesium; the principal difference is the pressure required. The same tool materials can often be used for the extrusion of either aluminum or magnesium.

Table 2 Typical materials and hardnesses for tools used in hot extrusion

Tooling application	**For tools used in extruding:**					
	Aluminum and magnesium		**Cooper and brass**		**Steel**	
	Tool material	**Hardness, HRC**	**Tool material**	**Hardness, HRC**	**Tool material**	**Hardness, HRC**
Dies, for both shapes and tubing	H11, H12, H13	47-51	H11, H12, H13	42-44	H13	44-48
			H14, H19, H21	34-36	Cast H21 inserts	51-54
Dummy blocks, backers, bolsters, and die rings	H11, H12, H13	46-50	H11, H12, H13	40-44	H11, H12, H13	40-44
			H14, H19	40-42	H19, H21	40-42
			Inconel 718	. . .	Inconel 718	. . .
Mandrels	H11, H13	46-50	H11, H13	46-50	H11, H13	46-50
Mandrel tips and inserts	T1, M2	55-60	Inconel 718	. . .	H11, H12, H13	40-44
					H19, H21	45-50
Liners	H11, H12, H13	42-47	A-286, V-57	. . .	H11, H12, H13	42-47
Rams	H11, H12, H13	40-44	H11, H12, H13	40-44	H11, H12, H13	40-44
Containers	4140, 4150,	35-40	4140, 4150,	35-40	H13	35-40

The dies used for the extrusion of aluminum alloys and copper alloys are generally made from AISI H11, H12, or H13 tool steels. For the extrusion of copper alloys, some companies specify tungsten hot-work steels such as H14, H19, and H21. For the extrusion of steel, H13 solid dies or H13 dies with cast H21 inserts are often used.

Dummy blocks, backers, bolsters, and die rings are routinely made from H11, H12, and H13. For the extrusion of copper, brass, and steel, H14, H19, and H21 are occasionally used. Nickel alloy 718 and other superalloys are sometimes used for dummy blocks; use of these alloys often results in extremely long tool life.

Mandrels are generally made of either H11 or H13, regardless of the material being extruded. Most mandrel tips and inserts for the extrusion of aluminum are made of T1 or M2. Nickel alloy 718 mandrel tips and inserts are commonly used in the extrusion of copper and brass, but H11, H12, H13, H19, or H21 tips and inserts can be used for the extrusion of steel.

The liners used in extruding aluminum or steel are usually made of H11, H12, or H13. Liners for the extrusion of copper and brass are normally made of a nickel- or iron-base superalloy. Rams are generally made of H11, H12, or H13.

Containers for the extrusion of aluminum or copper products are usually made of 4140, 4150, or 4340 alloy steel. Containers for the extrusion of steel can also be made from alloy steels; however, H13 is generally preferred.

Special Materials. In addition to the materials listed in Table 2, special insert materials and surface treatments have been specified (particularly for tools used in extruding complex shapes) for applications requiring better resistance to wear at higher temperatures. Special insert materials include special grades of cemented tungsten carbide, nickel-bonded titanium carbides, and alumina ceramics. Special surface treatments include nitriding, aluminide coating, and application of proprietary materials by vapor deposition or sputtering.

Materials for Hot Extrusion

The numerous uses to which extrusions are applied are constantly increasing. A large portion of metal consumption is in the form of extrusions. Depending on the material used, extrusions serve the transportation, construction, mechanical, and electrical industries. Extrusions are used for durable goods, industrial equipment, heating and air conditioning applications, petroleum production, and the production of nuclear power.

Practically all metals can be extruded, but extrudability varies with the deformation properties of the metal. Soft metals are easy to extrude; hard metals require higher billet temperatures and extruding pressures as well as sturdier presses and dies.

Lead and tin exhibit high ductility and are easy to extrude. The addition of alloying elements increases the force required, but extruding does not present a problem and is carried out with billets heated to a maximum temperature of about 300 °C (575 °F). Principal applications include pipes, wire, tubes, and sheathing for cable. Molten lead is used instead of billets for many applications. Vertical extrusion presses are sometimes used to produce protective sheathings of lead on electrical conductors.

Aluminum and aluminum alloys are probably the ideal materials for extrusion, and they are the most commonly extruded. Most commercially available aluminum alloys can be extruded. Billet temperatures generally range from about 300 to 595 °C (575 to 1100 °F), depending on the alloy. Principal applications include parts for the aircraft and aerospace industries, pipes, wire, rods, bars, tubes, hollow shapes, cable sheathing, architectural and structural sections, and automotive trim. Sections can be extruded from heat-treatable high-strength aluminum alloys.

Magnesium and Magnesium Alloys. Extruded magnesium and magnesium alloy products are used in the aircraft, aerospace, and nuclear power industries. With similar billet temperatures, the extrudability of these materials is about the same as that of aluminum, but longer heating periods are usually necessary to ensure uniform temperatures throughout the billets.

Zinc and Zinc Alloys. The extrusion of zinc and zinc alloys requires pressures that are higher than those necessary for lead, aluminum, and magnesium. Billet temperatures generally range from about 205 to 345 °C (400 to 650 °F). Applications include rods, bars, tubes, hardware components, fittings, and handrails.

Copper and copper alloy extrusions are widely used for wire, rods, bars, pipes, tubes, electrical conductors and connectors, and welding electrodes. Architectural shapes are extruded from brass, but usually in limited quantities. Billet temperatures vary from about 595 to 995 °C (1100 to 1825 °F). Depending on the alloy, extrudability ranges from easy to difficult. High pressures (690 MPa, or 100 ksi, or more) are necessary for the extrusion of many copper alloys.

Steels. For the hot extrusion of steel, it is necessary to use glass as a lubricant or some other high-temperature lubricant to prevent the excessive tooling wear that can result from the high billet temperatures required (995 to 1300 °C, or 1825 to 2375 °F). In addition, high ram speeds are required in order to minimize contact time between the billets and the tooling. The products produced include structural sections (generally required in small quantities) and tubes with small bores. For economic reasons, steel structural shapes, especially those needed in large quantities, are better suited to the rolling process. Alloy and stainless steels are usually extruded in the form of either solid shapes or tubes.

Other metals that are hot extruded include titanium and titanium alloys, nickel and its alloys, superalloys, zirconium, beryllium, uranium, and molybdenum. Some titanium alloys are more difficult to extrude than steels. Nickel alloys also can be very difficult to extrude, and billet temperatures above 995 °C (1825 °F) are used. All of these metals are extruded into tubes, rods, and bars; the bars are often used as forging stock in subsequent operations.

Metal powders are extruded into long shapes by cold and hot processes, depending on the characteristics of the powders. Aluminum, copper, nickel, stainless steels, beryllium, and uranium are some of the powders that are extruded. The powders are often compressed into billets that are heated before being placed in the extrusion press. For many applications, the powders are encapsulated in protective metallic cans, heated, and extruded with the cans. Forging of metal powders is discussed in detail in the article "Powder Forging" in this Volume; *Powder Metal Technologies and Applications*, Volume 7 of the *ASM Handbook*, is devoted to all aspects of powder metallurgy.

Characterization of Extruded Shapes

Extruded shapes in aluminum alloys are generally characterized according to geometric complexity. This characterization is also useful in classifying shapes extruded from other alloys.

The size of an extruded shape is measured by the diameter of the circle circumscribing the cross section of that shape (Fig. 11). This dimension is commonly referred to as the circumscribing circle diameter (CCD).

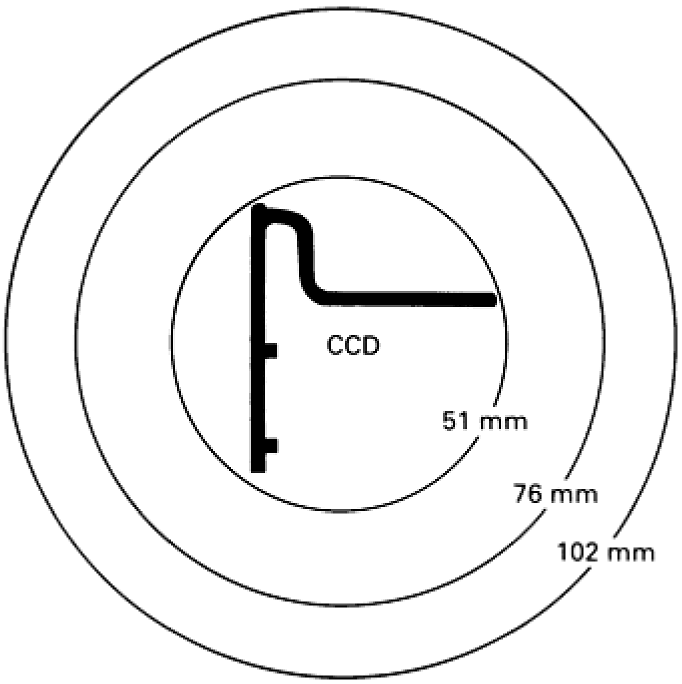

Fig. 11 Definition of size of an extruded section by circumscribing circle diameter

In extrusion, metal tends to flow more slowly at die locations that are far away from the axis of the billet. Therefore, the larger the circumscribing circle diameter, the more control required to maintain the dimensions of the extruded shape. Special care is needed in extruding large and thin shapes, especially those with thin portions near the periphery of the die. Therefore, size is one of the factors that describe the complexity of a shape.

Complexity of an Extruded Shape. Two accepted methods are available for defining the complexity of an extruded shape. One method involves the use of the shape factor, defined as follows:

$$\text{Shape factor} = \frac{\text{Perimeter}}{\text{Weight}} \quad \textbf{(Eq 1)}$$

This factor is a measure of the amount of surface generated per unit weight of metal extruded. The shape factor affects the production rate as well as the cost of manufacturing and maintaining the dies. It is used by many extruders as a basis for pricing and provides the designer with a means of comparing the relative complexities of alternate designs. The other measure of shape complexity is the classification of extruded shapes into different groups, based on the difficulty of extrusion.

Conventional Hot Extrusion

Operating Parameters

Critical parameters for successful and economical hot extrusion include the method of billet preparation and heating, the amount of pressure and rate of speed used for extruding, and the type of lubricant employed.

Billet Preparation. The more common metals that are to be extruded are generally cast in the form of cylindrical logs measuring 3.7 to 6 in (12 to 20 ft) or more in length. These logs are sawed or sheared into billets of varying length, depending on the cross-sectional area and the length of the product to be extruded.

Additional billet preparation is sometimes necessary, depending on the material to be extruded. For example, it is necessary to machine the outer surfaces of some steel billets before they are heated; the outer surfaces must then be descaled after being heated to the extrusion temperature. Best results are attained in backward extrusion by scalping the billets before extrusion to remove oxides and other impurities from the billet skin. If this is not done, these impurities would find their way onto the surfaces of the extrusion because of the inherent nature of the metal flow in backward extrusion.

Before they are extruded, aluminum billets are frequently homogenized by heat treatment. This treatment improves the extrudability of the material and the surface finish produced.

Billet temperature is important for all materials (see Table 1). A billet temperature that is too high can cause blisters or other surface defects, including cracking. A temperature that is too low increases the pressure requirements for the extrusion and shortens tool life.

Pressure Requirements. The unit pressures needed for hot extrusion are significant considerations in press selection (discussed previously). The determination of pressure requirements is difficult for the extrusion of complicated shapes and sections--especially those with thin walls. Careful judgments based on past experience must be made for estimates. Formulas have been developed for estimating pressure requirements, using shape, friction, and other parameters. However, for less complicated shapes, such as round bars and tubes, a fair approximation of pressure requirements can be calculated by:

$$P = k \ln \frac{A}{a} \quad \textbf{(Eq 2)}$$

where *P* is the extrusion pressure required (in pounds per square inch or megapascals); *k* is a numerical value representing the resistance to deformation, usually based on past experience in extruding a specific metal at a specific temperature; *A* is the cross-sectional area of the container liner or, in the case of tubes or other hollow shapes, the cross-sectional area of the liner minus the cross-sectional area of the mandrel (in square inches or square millimeters); and *a* is the total cross-sectional area of the extruded product (the shape area times the number of openings in the die) (in square inches or square millimeters).

The extrusion pressure requirements determined with Eq 2 are useful, but the values obtained are only approximations. The factor *k* varies with such factors as billet temperature, die design, type of metal extruded, amount of reduction (extrusion ratio), stem speed, and configuration of the extruded product. Billet length, nonhomogeneous metal flow, and friction also influence pressure requirements.

Unit pressures generally range from 450 to 760 MPa (65 to 110 ksi), with a maximum of about 1035 MPa (150 ksi). When practical, it is generally desirable to use a press with a capacity exceeding that actually required. This allows lower billet temperatures and faster stem speeds to be used and provides improved properties in the extruded products.

Stem Speeds. Optimal stem speeds are essential for hot extrusion. Excessive speed can cause overheating of the billet as well as tears and other surface defects. A speed that is too slow reduces productivity and increases the required extrusion pressure because of billet cooling. Slow speeds can also decrease tool life because of prolonged contact time between the tools and the hot billet. Typical stem speeds for various metals are:

Material	Stem speed	
	mm/s	in./s
Steel	152-203	**6-8**
Copper	51-76	**2-3**
Aluminum	12.7-25.4	$\frac{1}{2}$**-1**

The use of variable-delivery pumps and adjustable valves facilitates control of stem speed. Automatic control is available for maintaining constant speed throughout the extruding cycle.

Lubrication is another important operating parameter. The types of lubricants used and the effects of lubrication are discussed in the section "Lubricated Hot Extrusion" in this article.

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Brass	**25-51**	**1-2**

The use of variable-delivery pumps and adjustable valves facilitates control of stem speed. Automatic control is available for maintaining constant speed throughout the extruding cycle.

Lubrication is another important operating parameter. The types of lubricants used and the effects of lubrication are discussed in the section "Lubricated Hot Extrusion" in this article.

Applications for Computer-Aided Design and Manufacture (CAD/CAM)

In current industrial practice, the design of extrusion dies, whether of the flat or conical type, is still an art rather than a science. Die design for a new extrusion is developed from previous experience and through costly experimentation and in-plant trials. Therefore, process and die development may require relatively long periods of time and may tie up extrusion presses that should be used for actual production. Automated design systems have been developed for nonlubricated and lubricated extrusion processes to reduce the costs of designing and manufacturing extrusion dies (Ref 11, 12, 13, 14).

Many years of experience lie behind the production of extrusion dies with increasing complexity of shape, thinness of section, and quality of surface. Some of this experience is rationalized in empirical design rules, but much die design still relies on personal judgment, intuition, and experience. The dies are proven out through the production of trial extrusions. Invariably, the die orifice is corrected to achieve the required control of cross-sectional dimensions, straightness, and surface quality.

The objectives of applying CAD techniques to extrusion are:

- To provide a scientific basis and to rationalize the die design procedure as much as possible
- To improve productivity by reducing the trials and corrections needed to prove out the dies
- To optimize die design in order to achieve optimal material yield and maximum productivity
- To reduce the lead time required for designing and manufacturing the die
- To reduce die manufacturing costs by using cost-effective numerical control (NC) machining techniques whenever appropriate

Computer-Aided Design and Manufacture of Flat Dies

Design. Flat dies are primarily used for the extrusion of aluminum alloys. They consist of flat disks of tool steel containing one or more shaped orifices (Ref 15). The hot metal is forced (extruded) through these orifices to produce the desired sections (Fig. 12). The detailed design of the die involves determination of the following:

- Optimal number of shaped orifices in the die
- Location of the orifices relative to the billet axis for uniform metal flow through each orifice

- Orientation of the orifices
- Modification of the shape of the orifices to correct for thermal shrinkage and die deflection under load
- Determination of bearing lengths for balancing metal flow

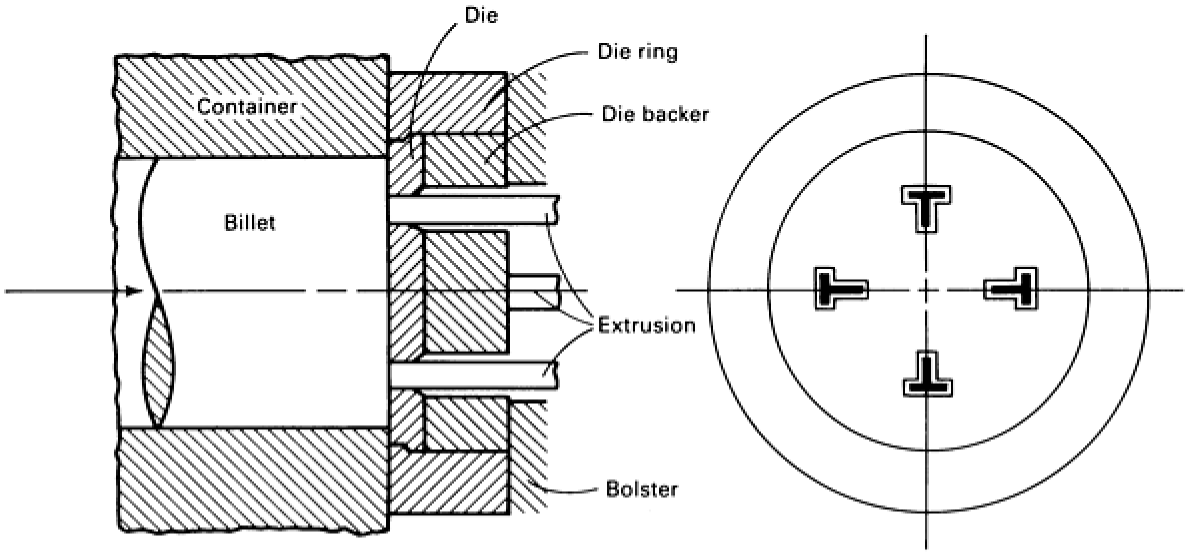

Fig. 12 Schematic of a flat die for extrusion of T-sections. Source: Ref 13

The details of a CAD technique for flat dies are given in Ref 13 and 15, in which the capabilities and application of an interactive CAD program, called ALEXTR, are described. A flow chart of the operation of this program is shown in Fig. 13. The first input into the program is the cross section of the extrusion, expressed in terms of *x*,*y* coordinates and the associated fillet or corner radii. These data are used to calculate such geometric parameters as cross-sectional area, perimeter, shape factor (perimeter/weight/length), location of centroid, and size and location of the circumscribing circle. The extrusion shape and the circumscribing circle are displayed on the graphics terminal with the calculated geometric variables.

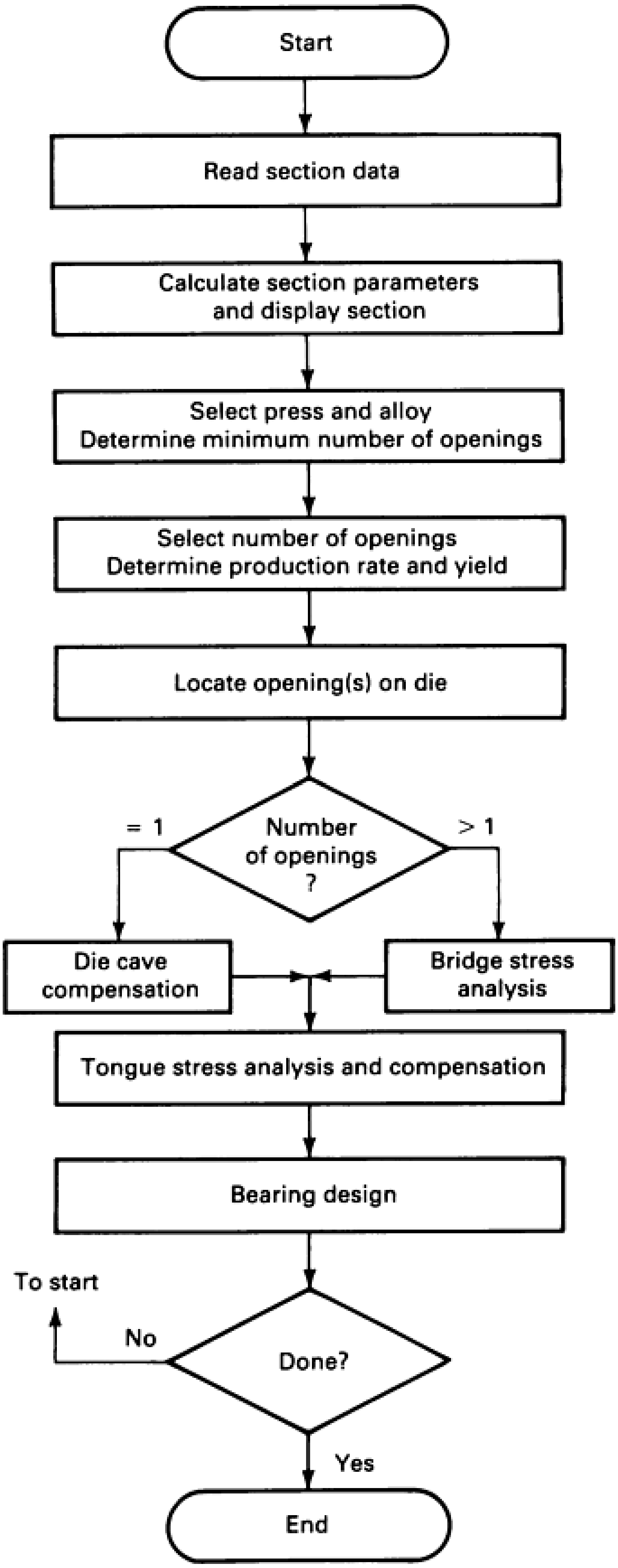

Fig. 13 Flow chart showing steps in the operation of ALEXTR. Source: Ref 13

ALEXTR then determines the number of extrusion orifices in the die (Ref 14). For this purpose, the user is asked to enter the specific press number (among several available), the alloy to be used, and the extrusion temperature. For each available press, characteristics such as press capacity, container diameter, maximum billet length, and runout length are stored in a data table. Load and yield calculations are made with the use of this information. The yield *Y*, in percent, is defined as:

$$Y = \frac{\text{Weight of usable extrusion}}{\text{Weight of billet}} \cdot 100 \qquad \textbf{(Eq 3)}$$

Because the load decreases as the number of openings increases, ALEXTR makes the first load calculation based on a single opening. If the result exceeds the press capacity, the calculation is repeated until the expected load is less than the press capacity or until the number of openings required is greater than the number allowed by the defined press characteristics. The number of openings is selected on the basis of:

- Operator specifications
- Maximum extrusion length
- Maximum material yield

The program also determines the optimal billet size for the number of openings to be used.

The next step in the design process is the layout of the extrusion orifices. Two considerations are involved. The first is that certain minimum clearances have to be maintained between the die openings (so that the die and the backup tooling have the strength to withstand the extrusion pressure) and between the die opening and the cylinder wall (to avoid flow of metal from the outside surface of the billet into the product). The second consideration is that the metal flow must be balanced by orienting the orifices such that a certain amount of symmetry is maintained, if possible.

The openings are positioned such that the center of gravity of an opening coincides with that of the billet segment feeding that opening. The opening is also rotated such that its greatest dimension is parallel to and as close as possible to the chord of the segment (Fig. 14a). If this layout is acceptable to the user, the single display is replaced with a display of the full container circle and all openings (Fig. 14b). The layout can be modified by the designer, if desired, through the use of the translate, rotate, and mirror image capabilities of the program. If unable to lay out the holes because of insufficient clearances, the designer can go back and select a different number of holes or a different press.

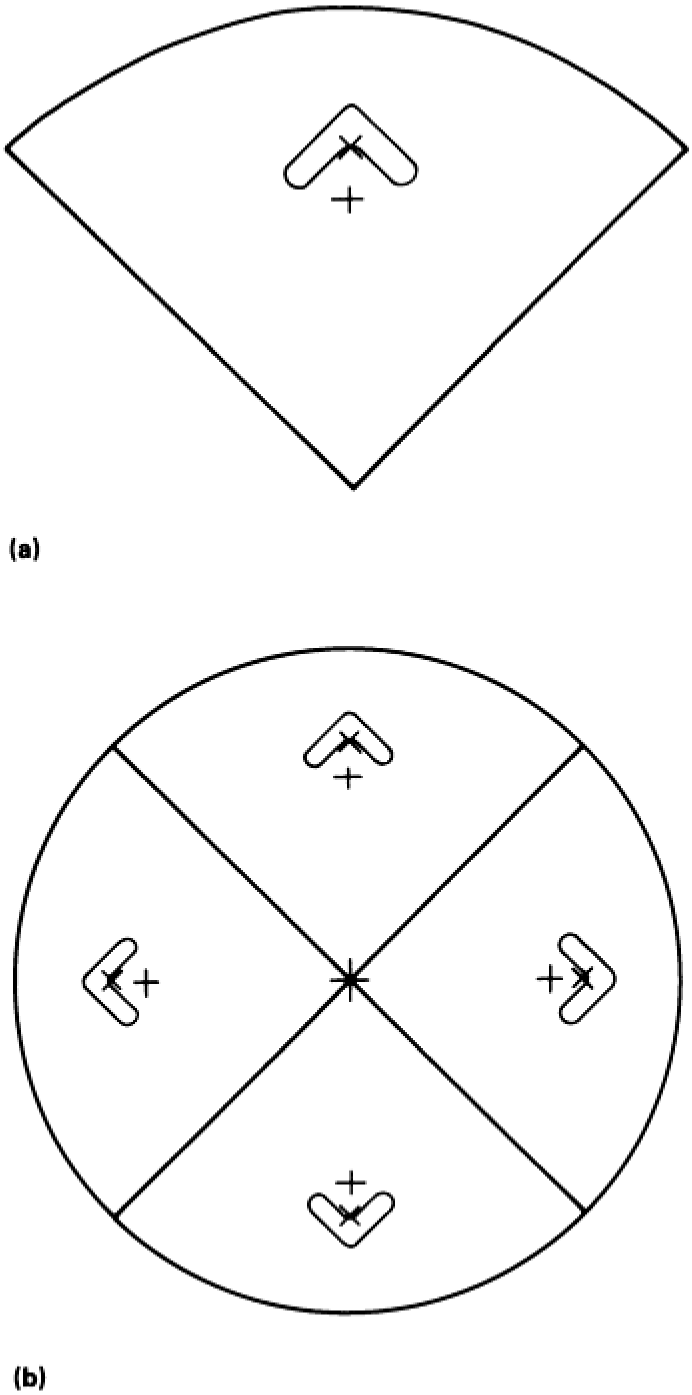

Fig. 14 Placement of openings in the die by ALEXTR. (a) Extrusion section placed on one quadrant of the die. (b) Arrangement of the entire die by repeating layout of the segment shown in (a)

After the layout is complete, ALEXTR corrects the openings for die cave and die deflection. A tool strength analysis is then performed to determine the bending and shear stresses in the die, backer, and bolster due to the extrusion pressure. The need for conforming tools to support the die is also determined by the tool strength analysis. These calculations allow the user to evaluate the need for support tooling. Die deflection analysis also includes estimation of the bending of the die at various locations. This information is helpful in predicting the dimensional changes in the die orifices under load, during extrusion. Therefore, the dimensions of the die orifices can be modified to correct for these dimensional changes to obtain the desired tolerances in the extruded shape.

The next step in the design process is the determination of die bearing lengths. The die bearing at any position is dependent on the section thickness at that position and on its distance from the die center. The designer indicates, with the light pen on the screen of the graphics terminal, the applicable thicknesses at various points on the extrusion perimeter. These dimensions are used by the computer program to calculate the die bearing lengths.

Manufacture and Evaluation. After die design has been completed, the geometric design information can be used to manufacture the dies by NC techniques. For manufacturing extrusion dies, either conventional electrical discharge machining (EDM) or wire electrical discharge machining is used. In the first case, two EDM electrodes are machined by numerical control; one electrode is used for machining the die opening(s) from the billet entry side, and the other is used for machining the die bearings from the exit side of the die. In wire electrical discharge machining, a wire electrode can be used for machining the die opening. However, the backside of the die, that is, the bearing areas, still must be machined conventionally by milling or by conventional electrical discharge machining. Numerical control programs are also used for preparing the templates for dimensional quality control of the dies and the extrusion.

In an example development study, the CAD/CAM technique was used to make flat-face dies (see Fig. 10b) and to extrude a T-shape from aluminum alloy 7075 (Ref 13). This section was extruded using a 6.2 MN (700 tonf) hydraulic press equipped with a 76 mm (3 in.) container. With an average bearing of 4.74 mm (0.187 in.), starting billet dimensions of 173 mm (2.875 in.) in diameter and 152 mm (6.0 in.) in length, and a flow stress of 52 MPa (7.5 ksi), a breakthrough load of 2.34 MN (263 tonf) was calculated. When the average bearing was specified to be 6.35 mm (0.250 in.), the expected load was 2.43 MN (273 tonf). The flow stress of 52 MPa (7.5 ksi) was estimated for the billet of aluminum alloy 7075 from data available in the literature and from preliminary extrusion tests with a round extrusion die. In these estimations, calculations were made for the average strain (4.0 for the present extrusion ratio of approximately 15 to 1) and strain rate (0.37/s for the ram speed of 406 mm/min, or 16 in./min).

The final step in the die design process was to indicate the section thicknesses and the bearing transition points. The die design data were then saved on a disk file for subsequent access by NC programs. Therefore, this geometry was used for NC machining of the EDM electrodes and the die bearings. The die opening, in H13 die steel with a hardness of 42 to 46 HRC, was machined through the die from the front. The die bearings were machined by numerical control from the back.

Computer-Aided Design and Manufacture of Dies for Lubricated Extrusion

Design. Lubrication in extrusion reduces load and energy requirements, reduces tool wear, improves surface finish, and provides a product with nearly uniform properties. This technique is commonly used in the extrusion of shapes from steels, titanium alloys, and nickel alloys. Proper die design is critical in lubricated extrusion, especially when noncircular shapes are extruded. An effective die design must ensure smooth metal flow with consistent lubrication. It is desirable to use shaped dies, which provide a smooth transition for the billet from the round or rectangular container to the shaped-die exit.

Computer-aided design techniques have been developed for the design of dies for lubricated extrusion (Ref 14, 16). For example, the design of a shaped die for extruding a T-shape from a round billet is illustrated schematically in Fig. 15. The geometry of this die should be optimized to:

- Give a defect-free extrusion requiring minimum postextrusion treatment (twisting and straightening)
- Minimize load and energy requirements
- Yield maximum throughput at minimum cost

The design procedure for determining the optimal shape of the die involves the following three steps:

- Define die geometry in a general manner
- Calculate the extrusion load as a function of the die geometry
- Optimize and determine the die shape that requires the minimum extrusion load

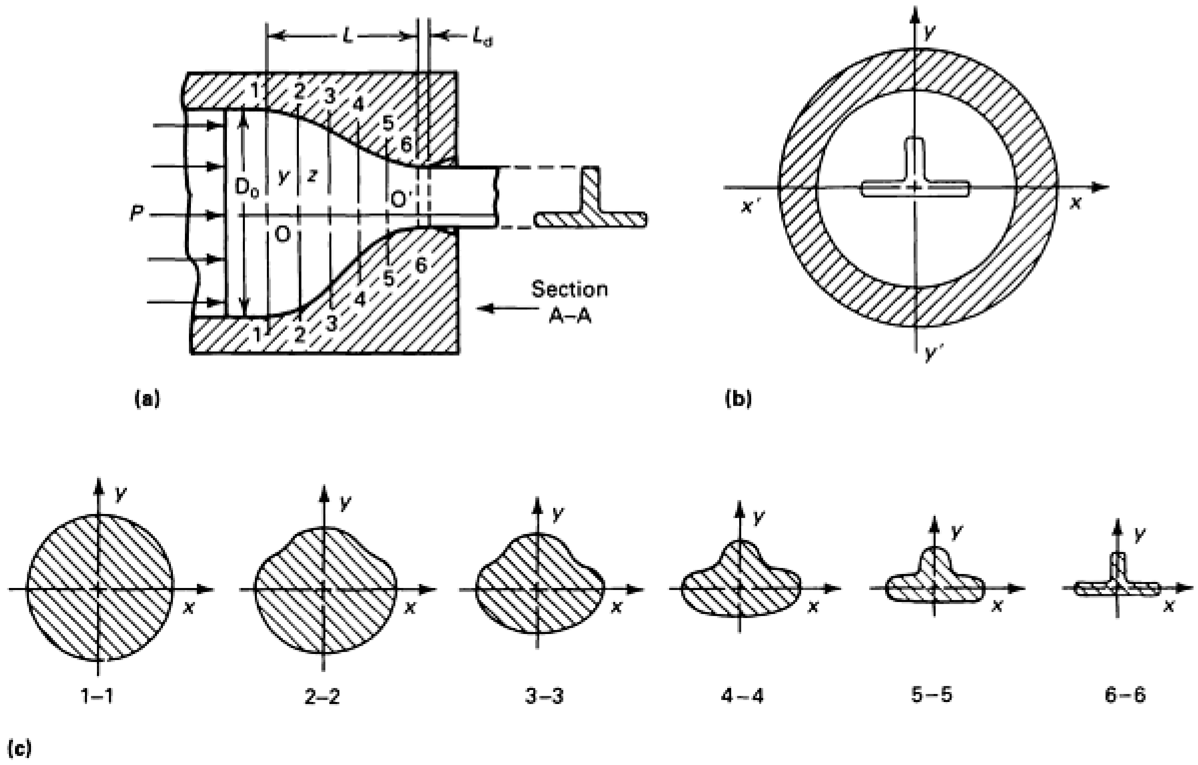

Fig. 15 Schematic of a shaped die for extrusion of a T-shape. (a) Section through *y*-*y*'. (b) Section A-A. (c) Cross sections of the billet during extrusion. Source: Ref 16

To define the die geometry for a T-extrusion, first the position of the die opening with respect to the container axis is determined. The initially circular cross section of the billet is then divided into a number of sectors. Starting from a plane of symmetry, the final cross section is divided into the same number of segments. This is done while keeping the extrusion ratios (ratio of the area of a sector in the billet to the area of the corresponding segment in the product) equal to the overall extrusion ratio. Thus, the initial and final positions of the material flow lines along the die surface are determined, and the path followed by any material point between the initial and final positions is determined by calculating and optimizing extrusion pressure.

Manufacture and Evaluation. The surface of a shaped die is defined as an array of points. The practical method of manufacturing this die is to NC machine a carbon electrode and then to electrical discharge machine the die. For this purpose, the cutter paths for machining the electrode surface must be determined. In using a ball-end mill, the position of the center of the spherical portion of the mill, with respect to any given point on the surface, can be determined by constructing a vector normal to the surface at that given point. Typically, the normal vector is calculated from the cross product of two vectors: one tangent to the surface along the material path line, and the other tangent to the cross-sectional boundary. For a tool of given radius, the coordinates of the cutter paths are determined as the tool moves, in a predetermined manner, over the array of points defining the die surface. The computer programs developed for calculating the cutter paths contain special routines to check for undercutting and gouging. The calculated cutter center points for extruding the T-section are plotted on the screen of a graphics terminal, as shown in Fig. 16.

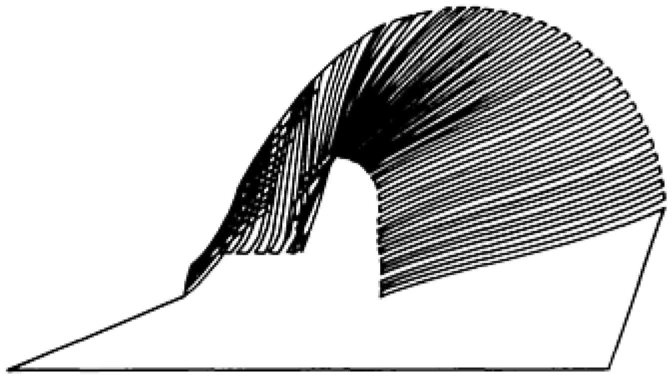

Fig. 16 Cutter path for NC machining of the EDM electrode for the streamlined die shown in Fig. 15. Source: Ref 16

The CAD/CAM technique was used to design and manufacture shaped dies for a T-shape with a 51 mm (2 in.) wide web and a 25.4 mm (1 in.) high rib. Billets 76 mm (3 in.) in diameter--from type 4340 steel and alloy Ti-6Al-4V--were extruded in a 6.2 MN (700 tonf) hydraulic extrusion press. Comparison of experimental results with computer-aided predictions showed that:

- Shaped dies for lubricated extrusion of simple structural shapes in titanium and steel can be designed and manufactured using computer-aided techniques
- Numerical control programs are adequate for machining three-dimensional streamlined surfaces (die or electrode) providing smooth transitions from round to structural sections. This was demonstrated by machining the EDM electrode for a streamlined T-section
- Straight T-sections of titanium and steel can be extruded through a streamlined die without any die modification
- Extrusion loads can be predicted with reasonable accuracy if accurate flow stress and friction data are available
- In the extrusion of simple structural shapes, the product shape does not significantly influence the extrusion load

Economic Aspects of Computer-Aided Design and Manufacture

The application of computer-aided design and manufacture in extrusion continues to increase. Several extrusion companies are using computer-aided techniques for die making and process optimization (Ref 11, 17). In addition to cost reductions, the implementation of computer-aided design and manufacture in extrusion provides the following potential benefits:

- More precise estimation, and reductions in estimation costs
- Reduction in delivery schedules
- Less dependence on skilled diemakers
- Reductions in the number of die failures and in die-design and manufacturing costs
- Improved utilization of existing press capacity by reducing die trials
- Continuous improvement of die and process technology
- Increases in material yield and press productivity

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Introduction

IN HYDROSTATIC EXTRUSION, the billet in the container is extruded through the die by the action of a liquid pressure medium rather than by direct application of the load with a ram. The process of pure hydrostatic extrusion differs from conventional extrusion processes (see the articles "Cold Extrusion" and "Conventional Hot Extrusion" in this Volume) in that the billet is completely surrounded by a fluid, which is sealed off and is pressurized sufficiently to extrude the billet through the die.

Hydrostatic extrusion can be done hot, warm, or cold and can be used to extrude brittle materials that cannot be processed by conventional extrusion. Hydrostatic extrusion also allows greater reductions in area (higher extrusion ratios) than either cold or conventional hot extrusion.

The primary advantages of simple hydrostatic extrusion over conventional hot or cold extrusion methods are:

- There is no friction between the billet and the container. Therefore, the pressure at the beginning of extrusion is much lower, and billets of any length can theoretically be extruded
- Friction at the die can be significantly reduced by a film of pressurized lubricant between the deforming metal and the die surface
- The lower extrusion pressures and the reduced die friction of hydrostatic extrusion allow the use of either higher extrusion ratios or lower extrusion temperatures
- The uniform hydrostatic pressure in the container means that billets do not have to be straight; coiled wire can also be extruded

Limitations of the hydrostatic extrusion process include:

- Containment of the fluid under high pressure (up to 2 GPa, or 290 ksi) requires reliable seals between the container bore surface and both the ram and die. The technology required to achieve dependable seals at these points is widely available, however. Also, sealing between the billet nose and the die can easily be achieved by chamfering or tapering the billet nose to match the entry angle of the die
- In addition to being tapered to match the die opening angle, the billet is also usually machined all over to remove surface defects that would otherwise reappear on the extruded product. This is especially true when cast billets are being used

Other limitations of the process arise when a relatively large volume of fluid is used compared to the billet volume to be extruded. These include:

- Increased handling for injecting and removing the fluid for each extrusion cycle
- Reduced control of billet speed and stopping due to potential stick-slip and excessive stored energy in the compressed fluid
- Reduced process efficiency in terms of billet-to-container volume ratio
- Increased complications when extruding at elevated temperatures

The problems of billet speed and stopping control can be reduced by using viscous dampers and by improving lubrication at the billet/die interface. Another approach to minimizing this and the other problems cited above is to keep the amount of pressurizing fluid to an absolute minimum, as in the Hydrafilm process (see the section "The Hydrafilm Process" in this article).

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IN HYDROSTATIC EXTRUSION, the billet in the container is extruded through the die by the action of a liquid pressure medium rather than by direct application of the load with a ram. The process of pure hydrostatic extrusion differs from conventional extrusion processes (see the articles "Cold Extrusion" and "Conventional Hot Extrusion" in this Volume) in that the billet is completely surrounded by a fluid, which is sealed off and is pressurized sufficiently to extrude the billet through the die.

Hydrostatic extrusion can be done hot, warm, or cold and can be used to extrude brittle materials that cannot be processed by conventional extrusion. Hydrostatic extrusion also allows greater reductions in area (higher extrusion ratios) than either cold or conventional hot extrusion.

The primary advantages of simple hydrostatic extrusion over conventional hot or cold extrusion methods are:

- There is no friction between the billet and the container. Therefore, the pressure at the beginning of extrusion is much lower, and billets of any length can theoretically be extruded
- Friction at the die can be significantly reduced by a film of pressurized lubricant between the deforming metal and the die surface
- The lower extrusion pressures and the reduced die friction of hydrostatic extrusion allow the use of either higher extrusion ratios or lower extrusion temperatures
- The uniform hydrostatic pressure in the container means that billets do not have to be straight; coiled wire can also be extruded

Limitations of the hydrostatic extrusion process include:

- Containment of the fluid under high pressure (up to 2 GPa, or 290 ksi) requires reliable seals between the container bore surface and both the ram and die. The technology required to achieve dependable seals at these points is widely available, however. Also, sealing between the billet nose and the die can easily be achieved by chamfering or tapering the billet nose to match the entry angle of the die
- In addition to being tapered to match the die opening angle, the billet is also usually machined all over to remove surface defects that would otherwise reappear on the extruded product. This is especially true when cast billets are being used

Other limitations of the process arise when a relatively large volume of fluid is used compared to the billet volume to be extruded. These include:

- Increased handling for injecting and removing the fluid for each extrusion cycle
- Reduced control of billet speed and stopping due to potential stick-slip and excessive stored energy in the compressed fluid
- Reduced process efficiency in terms of billet-to-container volume ratio
- Increased complications when extruding at elevated temperatures

The problems of billet speed and stopping control can be reduced by using viscous dampers and by improving lubrication at the billet/die interface. Another approach to minimizing this and the other problems cited above is to keep the amount of pressurizing fluid to an absolute minimum, as in the Hydrafilm process (see the section "The Hydrafilm Process" in this article).

Simple Hydrostatic Extrusion

In the simplest method of hydrostatic extrusion, the metal is extruded through a conical die into the atmosphere in much the same way as in conventional extrusion (Fig. 1). The container with the pressure-transmitting fluid is sealed with high-pressure seals at the ram and the die. Extrusion begins as soon as the hydrostatic pressure has reached a sufficiently high value, depending on the flow stress of the material and the extrusion ratio.

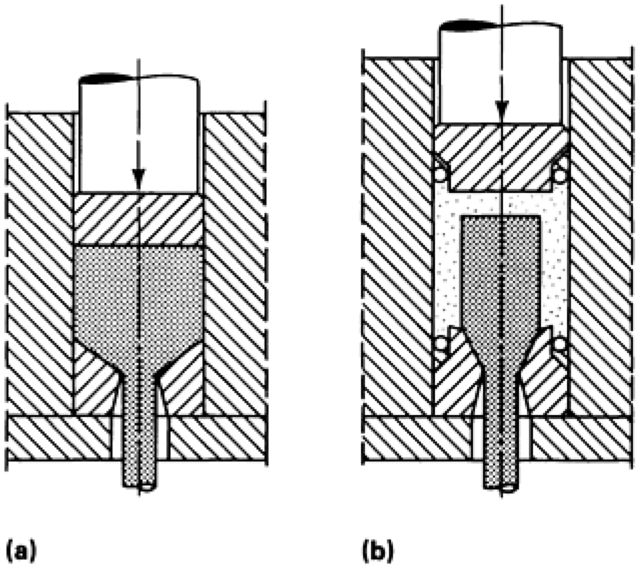

Fig. 1 Comparison of the conventional extrusion (a) and hydrostatic extrusion (b) processes

Extrusion Pressure. A pressure peak is found at the beginning of extrusion in the hydrostatic process. It is needed to initiate metal flow, which is hindered by friction at the die until a lubricating film and steady-state conditions have developed. This pressure peak can be very high, particularly if breakdown of the lubricant film occurs. The rapid decrease in pressure after extrusion begins can sometimes lead to the development of periodic oscillations in pressure; this is known as the stick-slip effect. It can be eliminated through the use of viscous dampers (Ref 1, 2) or by minimizing the amount of hydrostatic fluid used.

After the initial pressure peak, the steady-state pressure remains constant because there is no friction at the wall of the container (Fig. 2). The steady-state extrusion pressure required depends on the work material and is linearly related to the natural logarithm of the extrusion ratio R according to the empirical equation:

$$p = k_1 \ln R + k_2 \qquad \textbf{(Eq 1)}$$

where k_1 and k_2 are constants. Figure 3 illustrates this relationship for the cold extrusion of round billets to rod for several alloys. The gradient of the lines would be slightly steeper for more complex sections.

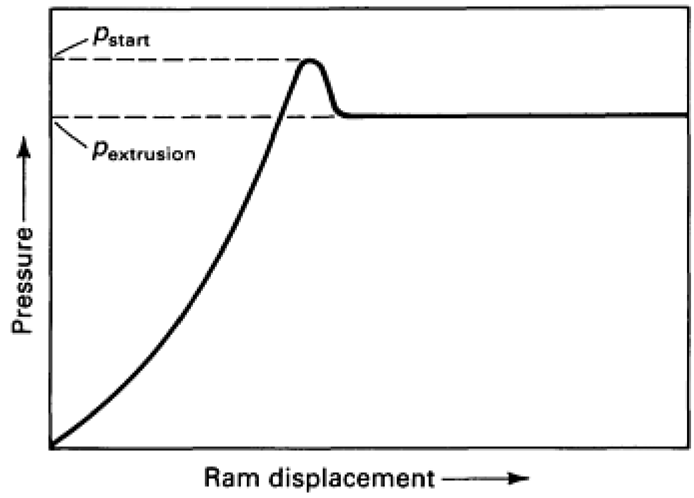

Fig. 2 **Pressure variation versus ram displacement during hydrostatic extrusion**

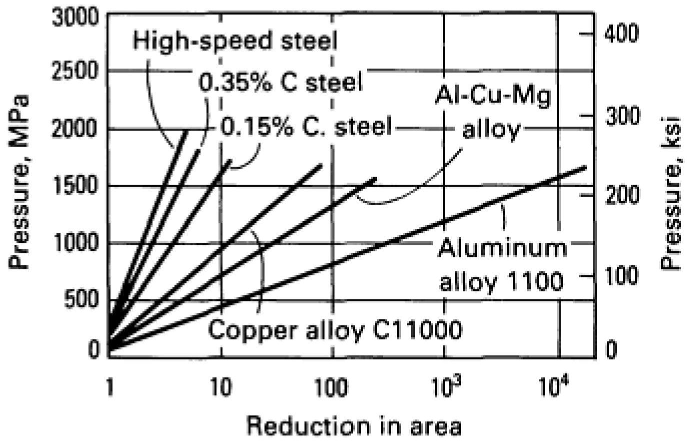

Fig. 3 **Extrusion pressure versus reduction of area in cold hydrostatic extrusion of various materials**

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1. A.H. Low and C.J.H. Donaldson, Report 289, The National Engineering Laboratory, 1967
2. H.L.D. Pugh, Hydrostatic Extrusion of Steel, *Iron Steel,* Vol 45, 1972, p 29-44, 49-51

Simple Hydrostatic Extrusion of Brittle Materials

Most brittle materials are subject to circumferential (transverse) and longitudinal surface cracking during hydrostatic extrusion. This cracking can be avoided through the use of either fluid-to-fluid extrusion or double-reduction dies. In fluid-to-fluid extrusion, the billet is hydrostatically extruded into a fluid at a lower pressure. This method has several disadvantages, including high tooling and operating costs, extrusion lengths that are limited to the length of the secondary chamber, and increased fluid pressure required for extrusion. For these reasons, the fluid-to-fluid process may not be suitable for many industrial applications.

The problem of extruding low-ductility metals was approached in a different way by researchers at Battelle Columbus Division (Ref 3), who established that the cracks or fracture first developed in the rear section of the die land, immediately before the exit plane, and that the surface cracking resulted from residual tensile stresses as the product left the die. The cracks observed were either longitudinal or transverse across the extruded product, depending on whether the predominating residual stresses were longitudinal or circumferential. This phenomenon was noted much earlier in rod and tube drawing (Ref 4). It was discovered that it is possible to reverse the residual stresses at the surface to compressive stresses by a subsequent draw with a low reduction in area (<2%).

The work of these investigators led to the development of the double-reduction die (Ref 4). Figure 4 compares a standard die with a double-reduction die. The double-reduction die used for the experiments was designed to give a 2% reduction in the second step. This method has been successfully applied to the extrusion of brittle materials, including beryllium and TZM molybdenum alloy, without any cracking. The lubricant used was polytetrafluoroethylene (PTFE), and the pressurizing fluid was castor oil. The results may be applicable to conventional cold extrusion through a lubricated conical die (Ref 3).

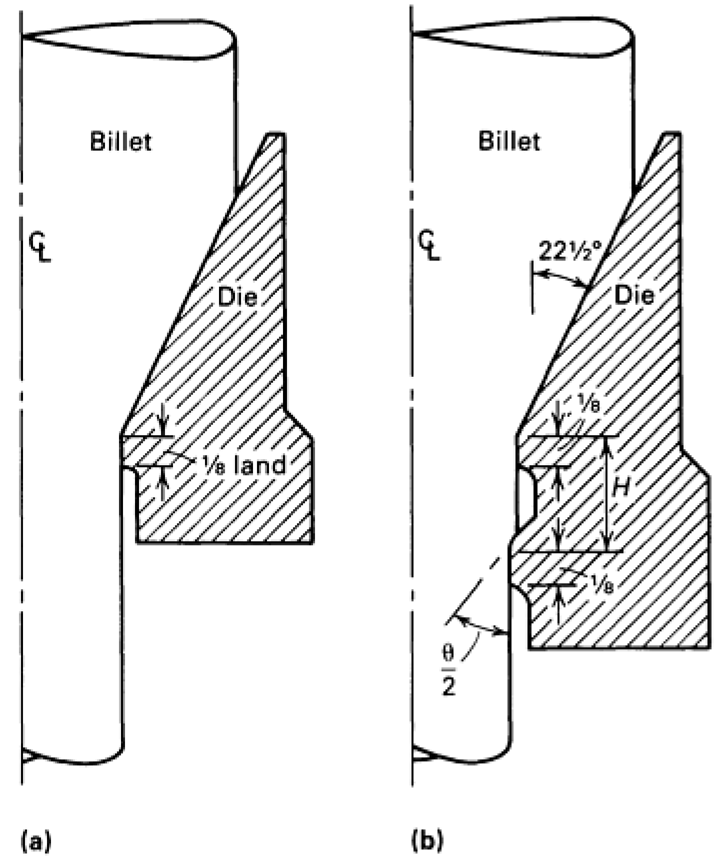

Fig. 4 Standard die (a) and double reduction die (b) for the hydrostatic extrusion of brittle materials. *H*, distance between the start of each bearing; θ, included angle at second reduction. Dimensions given in inches. Source: Ref 5

It is believed that the small second reduction prevents cracking by imposing an annular counterpressure on the extrusion as it exits the first portion of the die. This counters the axial tensile stresses arising from residual stresses, elastic bending, and friction. Prevention of circumferential cracks upon exit from the second portion of the die is believed to be associated with the favorable permanent change in residual stresses in the workpiece caused by the small second reduction (Ref 5).

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3. R.J. Fiorentino, B.D. Richardson, and A.M. Sabroff, Hydrostatic Extrusion of Brittle Materials: Role of Design and Residual Stress Formation, *Met. Form.,* 1969, p 107-110
4. H. Buhler, Austrian patent 139,790, 1934; British patent 423,868, 1935
5. R.J. Fiorentino, Selected Hydrostatic Extrusion Methods and Extruded Materials, in *Hydrostatic Extrusion:*

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Introduction

IN THE DRAWING PROCESS, the cross-sectional area and/or the shape of a rod, bar, tube, or wire is reduced by pulling through a die. One of the oldest metal-forming operations, drawing allows excellent surface finishes and closely controlled dimensions to be obtained in long products that have constant cross sections. In drawing, a previously rolled, extruded, or fabricated product with a solid or hollow cross section is pulled through a die at exit speeds as high as several thousand feet per minute (Ref 1, 2). The die geometry determines the final dimensions, the cross-sectional area of the drawn product, and the reduction in area. Drawing is usually conducted at room temperature using a number of passes or reductions through consecutively located dies. An important exception is the warm drawing of tungsten to make incandescent lamp filaments. Annealing may occasionally be necessary after a number of drawing passes before the drawing operation is continued. The deformation is accomplished by a combination of tensile and compressive stresses that are created by the pulling force at the exit from the die and by the die configuration.

In wire or rod drawing (Fig. 1 and 2), the section is usually round but could also be a shape. In the cold drawing of shapes, the basic contour of the incoming shape is established by cold-rolling passes that are usually preceded by annealing. After rolling, the section shape is refined and reduced to close tolerances by cold drawing (Ref 3). Again, a number of annealing steps may be necessary to eliminate the effects of strain hardening, that is, to reduce the flow stress and to increase the ductility.

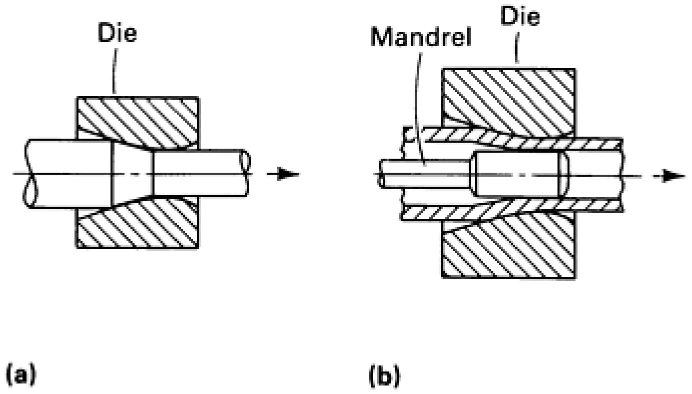

Fig. 1 Drawing of rod or wire (a) and tube (b).

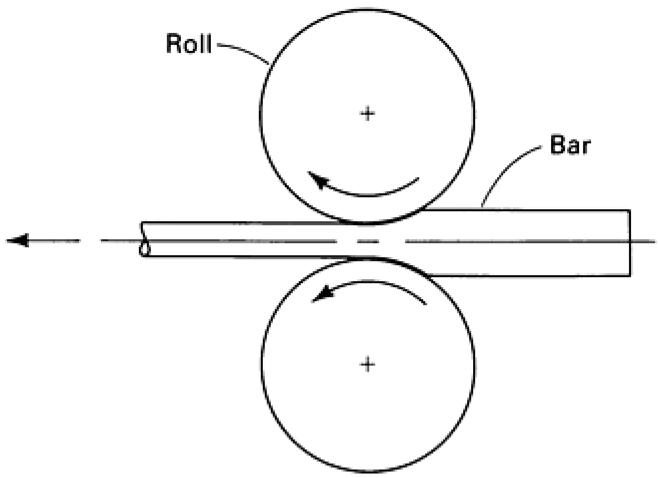

Fig. 2 Drawing of a bar through undriven rolls.

In tube drawing without a mandrel (Fig. 3), also called tube sinking, the tube is initially pointed to facilitate feeding through the die; it is then reduced in outside diameter while the wall thickness and the tube length are increased. The magnitudes of thickness increase and tube elongation depend on the flow stress of the drawn part, die geometry, and interface friction.

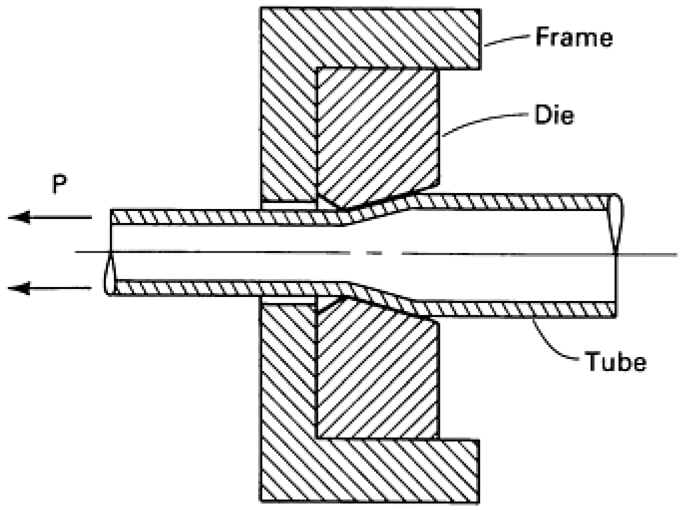

Fig. 3 Tube drawing without a mandrel (tube sinking).

Drawing with a fixed plug (Fig. 4) is widely known and used for drawing large-to-medium diameter straight tubes. The plug, when pushed into the deformation zone, is pulled forward by the frictional force created by the sliding movement of the deforming tube. Therefore, the plug must be held in the correct position with a plug bar. In drawing long and small-diameter tubes, the plug bar may stretch and even break. In such cases, it is advantageous to use a floating plug (Fig. 5). This process can be used to draw any length of tubing by coiling the drawn tube at speeds as high as 10 m/s (2000 ft/min). In drawing with a moving mandrel (Fig. 6), the mandrel travels at the speed at which the section exits the die. This process, also called ironing, is widely used for thinning the walls of drawn cups or shells in, for example, the production of beverage cans or artillery shells.

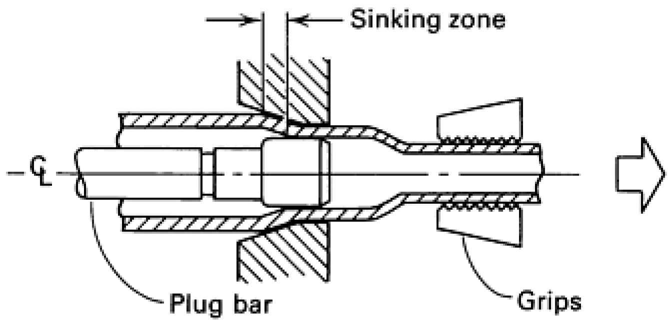

Fig. 4 Drawing with a fixed plug.

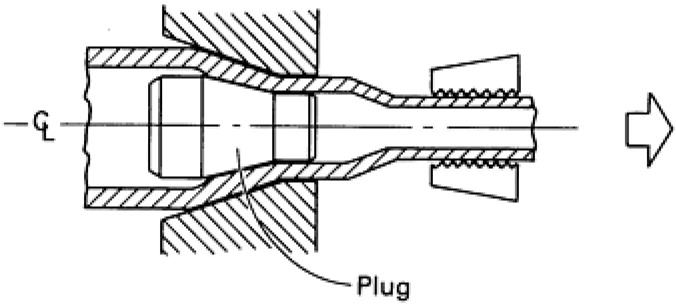

Fig. 5 Drawing with a floating plug.

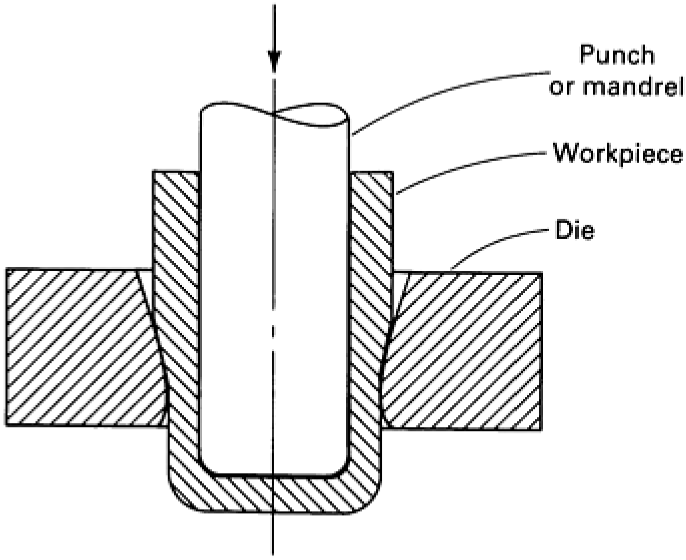

Fig. 6 Drawing with a moving mandrel.

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1. G.E. Dieter, *Mechanical Metallurgy,* 2nd ed., McGraw-Hill, 1976, p 658
2. K. Lange, Ed., Massiveforming, in *Textbook of Forming Technology,* Vol II, Springer-Verlag, 1974, p 227 (in German)
3. "Rathbone Cold-Drawn Profile Shapes and Pinion Rods," Technical Brochure, Rathbone Corporation

Wire, Rod, and Tube Drawing

Basic Mechanics of Drawing (Ref 4)

It is fundamental that the pulling force, or drawing stress, cannot exceed the strength of the wire or rod being drawn (otherwise, fracture or unstable deformation would occur). In fact, practical considerations often limit the drawing stress to about 60% of the as-drawn flow stress. Therefore, the area reduction per drawing pass is rarely greater than 30 to 35%. A particularly common reduction is that of an American Wire Gauge of 1, or about 20.7%. Thus, many reductions or drawing passes are needed to achieve a large overall reduction. Much larger reductions can be achieved in a single operation with extrusion. Alternatively, drawing can be used to generate larger quantities of small-diameter product (for example, 0.01 mm, or 0.0004 in.) with excellent dimensional control (assuming proper die maintenance).

Approach Angle. A typical carbide drawing die is illustrated in Fig. 7. The wire or rod makes contact in the drawing cone along the approach angle and is reduced to the dimensions of the drawing cone exit. The bearing region involves no further reduction and allows the die to be refinished without a change in the exit dimensions of the drawing cone. The back relief reduces the amount of abrasion that takes place if the drawing stops or if the die is out of alignment. A lubricant is introduced at the bell portion of the die and is pulled into the die/wire interface by the moving wire.

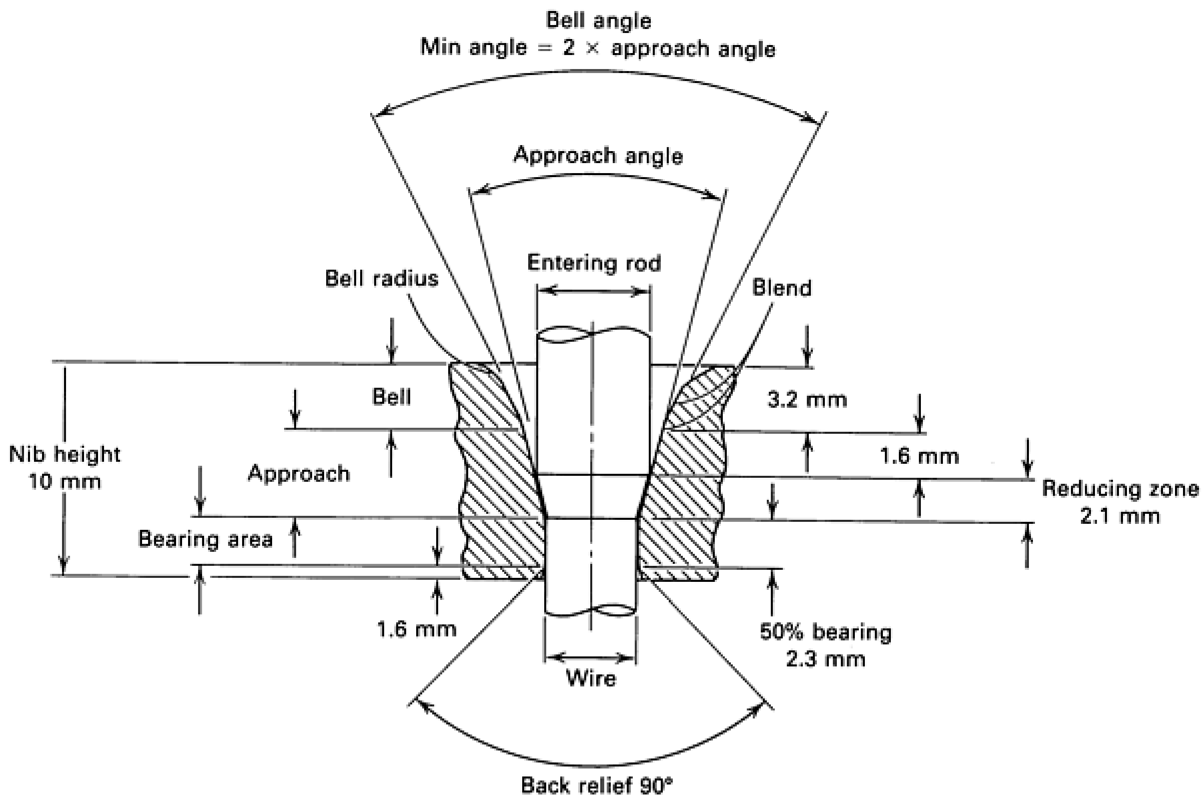

Fig. 7 Cross section of a typical wire die for drawing 5.5 mm (0.218 in.) diam rod to 4.6 mm (0.180 in.) diam wire (17% reduction per pass).

The approach angle is perhaps the most important feature of the die for most applications. The effect of the approach angle on metal flow cannot easily be considered independent of the drawing reduction, and modern drawing theory incorporates both into the Δ parameter:

$$\Delta \approx \left(\frac{\alpha}{r}\right) [1 + (1 - r)^{1/2}]^2$$

where α is the approach semiangle (one-half the included angle) in radians and r is the fractional drawing reduction, given by:

$$r = 1 - A_1/A_0$$

where A_0 and A_1 are the starting and finishing cross-sectional areas, respectively. Commercial die design often involves approach semiangles in the range of 6 to 10° and drawing reductions of about 20%. The corresponding Δ values typically range from 2 to 3, with higher values corresponding to lower reductions and higher die angles, and lower values corresponding to higher reductions and lower die angles.

Effect of Friction. Basically, low Δ values may involve excessive frictional work between the wire and the drawing cone, and high Δ values involve redundant work or plastic strain beyond that calculable from the reduction in area of the pass. Some degree of redundant work exists for Δ> 1, with redundant work increasing as Δ increases, much as frictional work can increase as Δ decreases. The net effect is that some intermediate value of Δ involves the minimum work, and therefore the minimum drawing force, because the drawing force multiplied by the drawing velocity is the work consumed per unit time. Similarly, the drawing stress equals the work per unit volume of wire drawn. The Δ for minimum drawing stress can be approximated by:

$$\Delta_{min} \simeq 4.9 \left[\frac{\mu}{\ln(1/1 - r)} \right]^{1/2}$$

where μ is the coefficient of friction between the wire and the drawing cone. The drawing stress σ_d can be usefully approximated as:

$$\sigma_d \simeq \bar{\sigma} \left(\frac{3.2}{\Delta + 0.9} \right) (\alpha + \mu)$$

where $\overline{\sigma}$ is the average strength or flow stress of the wire during the drawing pass.

Redundant Work of Deformation. Redundant work is expressed in terms of the redundant work factor or the ratio of total plastic deformation work to the work implied by dimensional change. Experimental studies suggest that the redundant work factor Φ can be estimated to be:

$$\Phi \simeq \Delta/6 + 1$$

Heat Generation During Drawing. The management of heat is of great concern in drawing; practical cold-drawing operations can involve wire temperature increases of a few hundred degrees Kelvin. Much heat is generated directly by the plastic deformation, and this heat is only partially removed by interpass cooling. The dies extract little heat under commercial conditions and become very hot. Under adiabatic conditions, the temperature increase ΔT_d associated with plastic deformation in a single pass is approximately:

$$\Delta T_d = \Phi \overline{\sigma} \ln (1/1 - r)/C\rho$$

where C and ρ are the heat capacity and density of the wire, respectively. Additional heat generation is associated with frictional work. This heat is concentrated at the die/wire interface and can lead to diminished lubrication, further heating, and catastrophic lubricant breakdown. Accompanying problems include poor wire surface quality and metallurgical changes near the wire surface. If the coefficient of friction is not influenced by Δ, frictional heating is aggravated by low Δ processing. Fortunately, there is a tendency for low approach angles (and thus low Δ) to foster hydrodynamic lubrication and a reduced coefficient of friction.

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Preparation for Drawing (Ref 5)

One or more of three basic preparation steps--heat treatment, surface preparation, and pointing--are usually required prior to successful cold drawing. These three steps are naturally dependent on the state of the part before drawing and on the desired drawing results.

Heat treatment usually involves annealing or softening so that the material is ductile enough for the intended percentage of reduction. This is particularly necessary for certain metals that are hard or brittle in the hot-worked state or for previously cold-drawn parts that have already been work hardened too much to allow further reduction.

Annealing. In the wire industry, a wide variety of in-process annealing operations are available for rendering coiled material suitable for further processing that may require formability, drawability, machinability, or a combination of these characteristics. One large wire mill reported using 42 separate and distinct annealing cycles, most of which represented compromises between practical considerations and optimal properties. For example, annealing temperatures below those that might yield optimal softness sometimes must be used in order to avoid the scaling of wire coils, which can often occur even in controlled-atmosphere furnaces. Even slight scaling can cause the coil wraps to stick together, and this can impede coil payoff in subsequent operations.

Patenting is a special form of annealing that is peculiar to the rod and wire industry. In this process, which is usually applied to medium- and higher-carbon grades of steel, the rod or wire products are uncoiled, and the strands are delivered to an austenitizing station. The strands are then cooled rapidly from above the full annealing temperature (A_3) in a molten medium (usually lead at about 540 °C, or 1000 °F) for a period of time sufficient to allow complete transformation to a fine pearlitic structure. Salt baths and fluidized beds have also been used for this purpose. This treatment increases considerably the amount of subsequent wire-drawing reduction that the product can withstand and permits the production of high-strength wire. Successive drawing and patenting steps can be used to obtain the desired size and strength level.

Surface Preparation. To prevent damage to the workpiece surface or the draw die during cold drawing, the starting stock must first be cleaned of surface contaminants, such as scale, glass, and heavy rust. This cleaning usually involves the use of various pickling or shotblasting methods. In many cases, especially when tubes are being drawn, the surface can also be coated or prelubricated by phosphatizing, plating, soaping, or liming methods. If no intermediate annealing is required, some of the prelubricating methods permit several cold-drawing passes without repeated treatment. Solid bars or rods are generally lubricated by oil during the drawing process.

To provide a wire of good surface quality, it is necessary to have clean wire rod with a smooth oxide-free surface. Conventional hot-rolled rod must be cleaned in a separate operation, but with the advent of continuous casting, which provides better surface quality, a separate cleaning operation is not required. Instead, the rod passes through a cleaning station as it exits the rolling mill.

Pointing, sometimes called chamfering, involves the preparation of a short length of one end of the starting part to a size slightly smaller than the draw die. The prepared end, called the point, is thus ready for insertion through the draw die for gripping. The actual pointing operation is usually performed at room temperature by swaging, rolling, or turning. However, it can be performed after preheating and can also be done by hammering, acid etching, or grinding.

In some cases, these pointing operations can be avoided through the use of push pointing, which involves pushing the end a short distance through the die. Pushing forces, however, are much higher than pulling forces. As a result, starting parts having small diameters and slender sections may buckle during the push-pointing process. This buckling action can be minimized by proper support, but parts having a diameter of about 9.5 mm (0.37 in.) or less generally must be pointed by one of the methods previously described.

Reference cited in this section

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Drawing of Rod and Wire (Ref 5)

An overall view of the process by which steel wire is drawn from rods is shown in Fig. 8. Methods and equipment used for the cold drawing of rod and wire, as well as small-diameter tubing, are generally designed so that the products can be uncoiled and then re-coiled after drawing. On multiple-die continuous machines, uncoiling, drawing, and re-coiling are repeated at successive stations. Rod coils, when ready for processing, are usually butt welded together for continuous drawing.

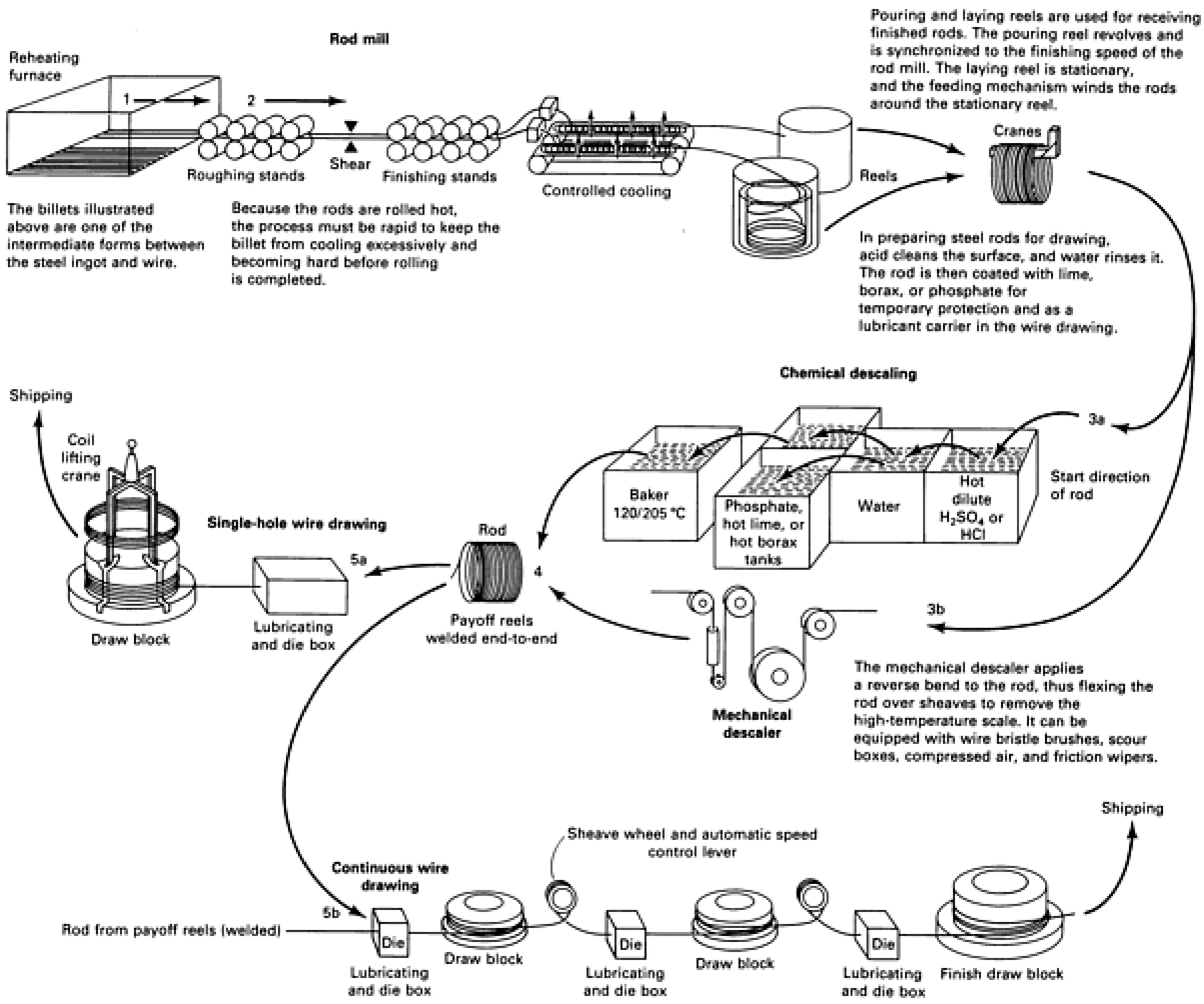

Fig. 8 Schematic diagram illustrating how steel wire is drawn from rods. Source: Ref 6.

The distinction between wire and rod (or bar) is somewhat arbitrary. The term wire generally refers to smaller-diameter products (<5 mm, or 0.2 in.) that can be rapidly drawn on multiple-die machines. Larger-diameter rod and bar stock can be drawn on single-die machines or on benches that do not require coiling of the as-drawn product.The terms rod and wire will often be defined from a marketing perspective. In both cases, the nature of the drawing process is similar (Ref 4).

In the drawing process, cleaned and coated coils of rod or wire are first placed on a payoff tray, stand, or reel; this permits free unwinding of the stock. The leading end of the rod or wire, after being pointed, is then inserted through the drawing die and seized by a gripper attached to a powered cylindrical block or capstan. On so-called dry machines, the die is mounted in an adapter within a box. This die box contains grease, dry soap, oil, or other lubricants through which the stock must pass before reaching the die.

Bull blocks are single-die drawing machines with individual drive systems. They are extensively used for breakdown, finishing, or sizing operations on large-diameter rod and wire, made from both ferrous and nonferrous metals, by firms with production requirements that do not warrant more sophisticated, continuous machines.

The spindles of these machines are generally vertical, with spindle blocks revolving in a horizontal plane. The arrangement is occasionally reversed (with the spindles horizontal and the blocks revolving in a vertical plane), particularly for applications involving large-diameter stock.

Many design variations are available with bull blocks. For example, a double-deck arrangement permits two drafts to be performed, with the second draft maintaining a fixed percentage of area reduction. Other refinements include external air cooling and internal water cooling of the block as well as riding-type block-stripping spiders for direct coiling and wire removal. These spiders, with collapsible feet, can be equipped with automatic discharging mechanisms to transfer drawn coils to wire carriers or stems.

The wire being drawn on the block is usually coiled around block pins that provide an extension to the height of the block; this is often done when large bundles are not required. A stripper, with the feet temporarily collapsed, is then inserted through the eye of the coil, with the feet fitting into stripper slots or recesses in the block flange. The feet are then locked in their extended positions, and the bundle is lifted free of the block.

Dry-Drawing Continuous Machines. For the dry drawing of ferrous metals, four types of nonslip continuous machines are in general use: accumulating-type machines, double-block accumulating-type machines, controlled-speed machines, and straight-through machines.

An accumulating-type multiblock continuous wire-drawing machine is shown in Fig. 9. This machine is equipped with electromagnetic block clutches. Photocells sense high and low wire accumulation on each block and disengage or engage appropriate block clutches. A single dc motor drives a coupled lineshaft that carries the clutches. Only the inlet block has to be stopped in the event of a payoff snarl, allowing the machine to continue production while the snarl is removed. A programmable controller enables rapid checkout and simple alteration to input and output circuits, and it serves as a continuous fault-monitoring system to simplify maintenance.

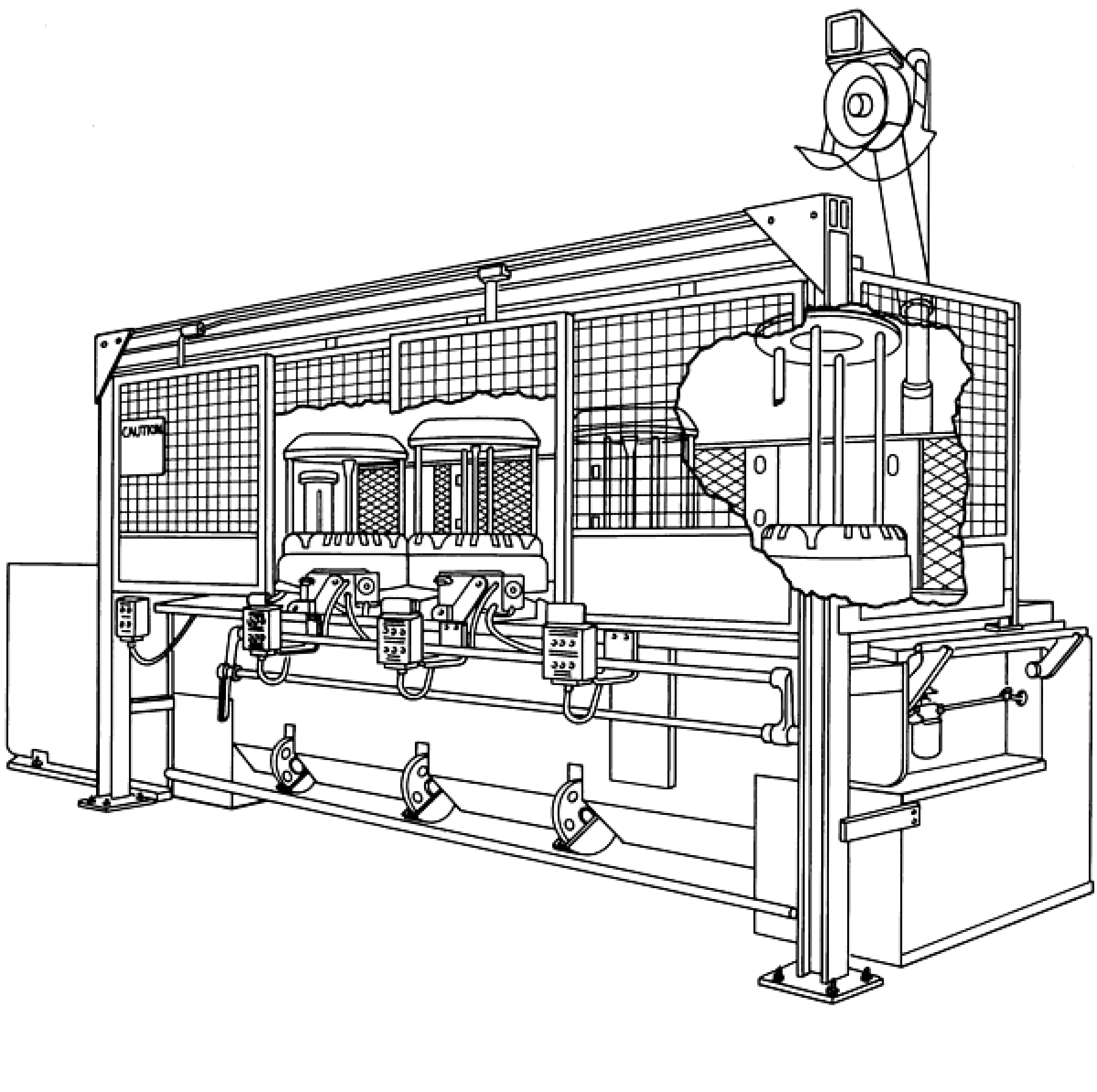

Fig. 9 Accumulating-type continuous wire-drawing machine.

Double-block accumulating machines have individually driven blocks. Wire is transferred from the first drawing block by means of an intermediate flyer sheave that reverses the direction of the wire (without twisting it) onto a coiling block mounted immediately above the first drawing block. The wire is then held temporarily in storage until demanded by the second drawing block. Fully automatic, electrical drive systems can be used to start and stop, or slowdown and speedup, the individual blocks to accumulate or deplete the wire.

On controlled-speed machines, the wire follows an essentially flat path from block to block with a constant, unvarying amount of wire storage without twisting and slipping. A tension arm between the blocks, activated by a loop of the wire being drawn, regulates the speed of the adjustable-speed dc motor on the preceding block.

Straight-through machines, without tension arms, are also available. The spindles are often canted from the vertical axis to accommodate wire buildup on the blocks and to provide unimpeded, straight entry into the succeeding die; this is usually done when large-size workpieces are required. Skillful operators are necessary because torque adjustments may need to be altered at each block when stringing up the machines in order to make the electrical system function properly.

The continuous drawing of nonferrous rod and wire, as well as some intermediate and fine sizes of ferrous wire, is generally done on wet-drawing slip-type machines. On these machines, the surface speed of the capstans, except for the final (pull-out) capstans, exceeds the speed of the wire being drawn, thus creating slip of the wire on the capstans. Brighter surface finishes are generally produced with these machines, but the machines are limited to smaller reductions per pass than with dry-drawing nonslip continuous machines.

With wet-drawing slip-type machines, the drawing operation is generally confined to an enclosed chamber, with the lubricant bathing the dies and wire as it is being drawn. These machines are less complicated electrically than nonslip machines, and only one drive system is employed. They are designed with either tandem or cone-type configurations, usually with horizontal spindles, but sometimes with a vertical spindle for the finishing capstan. Cone capstans have drawing surfaces (usually hard-faced) that are stepped outward to provide increasing peripheral speeds. This compensates for the elongation and increasing speed of the wire as it is reduced in diameter during drawing.

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6. *Designer's Handbook: Steel Wire,* American Iron and Steel Institute, 1974

Drawing of Bar (Ref 5)

Bars about 32 mm (1.25 in.) and smaller in diameter are cold drawn from coil stock by various methods. With one method, cold-drawn coils of rod and wire produced on the various machines described previously are straightened and cut into bars in a separate operation on machines designed for that purpose. Some in-line methods and equipment begin by unwinding the starting coil, then pull the stock through a draw die without recoiling, and finally straighten and cut the material into bars in a continuous operation.

The continuous machine illustrated in Fig. 10 has a fixed die box with a recirculating wet-die lubricating system. Drawing is accomplished with three moving grip slides; one slide for push pointing before the die box and two opposed-motion drawing slides after the die box. The push-pointing grip runs twice as fast as the drawing grips in order to minimize production loss when push pointing. This machine also has one set each of vertical and horizontal straightening rollers, and a set of feed-out rolls. Most cold-drawn bars are produced from hot-rolled or extruded bars up to 17 in (55 ft) long by 152 mm (6 in.) in diameter, with seldom more than one cold-drawing pass performed.

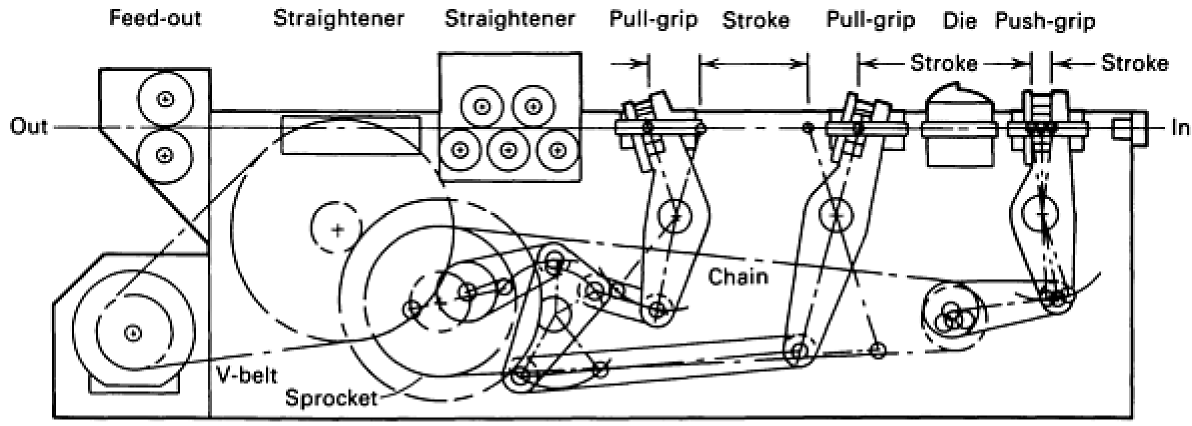

Fig. 10 In-line drawing and straightening machine for producing cold-drawn bars from hot-rolled steel coils or bars.

Drawbenches for Bars. The cold drawing of cleaned and pointed hot-rolled bars is also generally performed on a high-powered, rigidly built, long, horizontal machine called a drawbench (Fig. 11). The draw-bench consists essentially of a table of entry rollers (an elevating entry conveyor is shown), a die stand, a carriage, and an exit rack (not shown). Entry rollers support the hot-rolled bars and are usually powered to help bring the pointed ends of the bars into the draw dies. An upright head can hold as many as four dies to permit the drawing of four bars at a time. If lubrication is required, a lubricating oil system is provided on the entry side of the head.

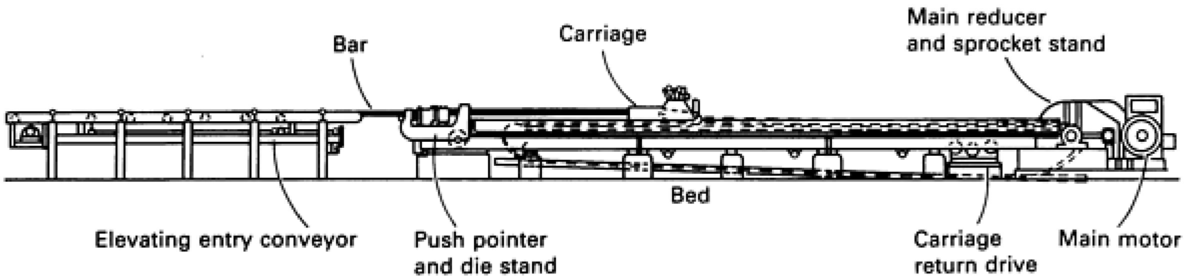

Fig. 11 Typical arrangement of a drawbench for producing cold-drawn bars from hot-rolled bars.

On most drawbenches, the entry side of the head is provided with a hydraulic pushing device, which, for a normal draft, can be used to push point the ends of the bars. Pneumatically operated grips on the carriage grasp the pointed ends of the bars protruding through the dies. The carriage is powered by a motor-driven chain(s) or hydraulic piston(s) to slide or roll along ways to pull the bar(s) through the die(s).

As soon as the bar being pulled exits the draw die, the carriage automatically releases the bar and stops. The drawn bar is then free to fall, usually onto discharge arms for removal from the drawbench. The carriage is then rapidly returned to the die stand--by a separately powered return system on chain benches or by means of a piston on hydraulic benches--for drawing the next bar. Chain-operated drawbenches are usually controlled automatically to permit low speeds at the start of the pulling action, followed by rapid acceleration to the preset pulling speed.

Reference cited in this section

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Drawing of Tube (Ref 5)

Tubes, particularly those having small diameters and requiring working only of their outer surfaces, are produced from cold-drawn coils on machines that straighten the stock and cut it to required lengths. As with bars, however, most tubes are produced from straight lengths rather than coiled stock. With four exceptions, the methods and equipment used for cold drawing tubes in straight lengths are basically identical to those used for bar drawing. The four exceptions are:

- Some tubes require more than one drawing pass
- Tubes are usually longer than bars. Drawbenches for tubes are usually correspondingly longer, some permitting drawn lengths of over 30 m (100 ft)
- Tube diameters are generally larger than bar diameters, ranging to about 305 mm (12 in.). The bigger tube drawbenches have larger components than do bar drawbenches
- Tubes require internal mandrels or bars for simultaneous working or support of the interior surface during drawing. Tube drawbenches are usually equipped with one of several available devices, usually powered, for ready assembly of the cleaned, coated, and pointed workpiece onto internal bars or rod-supported mandrels. If rod-supported mandrels are used, they are usually air-operated so that the mandrel can be placed and maintained in the plane of the draw die after pulling starts. Butt or electric-welded tubes are sometimes drawn to smooth the weld seams and tube walls

Drawing of Tubes and Cups With a Moving Mandrel. The principle of drawing with a moving mandrel is illustrated in Fig. 6 for a single-die draw. The process can be conducted hot or cold to manufacture a variety of discrete hollow cuplike components, such as artillery shells, shock absorber sleeves, beverage cans, and gas cylinders. Tube drawing with a moving mandrel, often called ironing, is carried out by using several drawing dies located in tandem (Fig. 12).

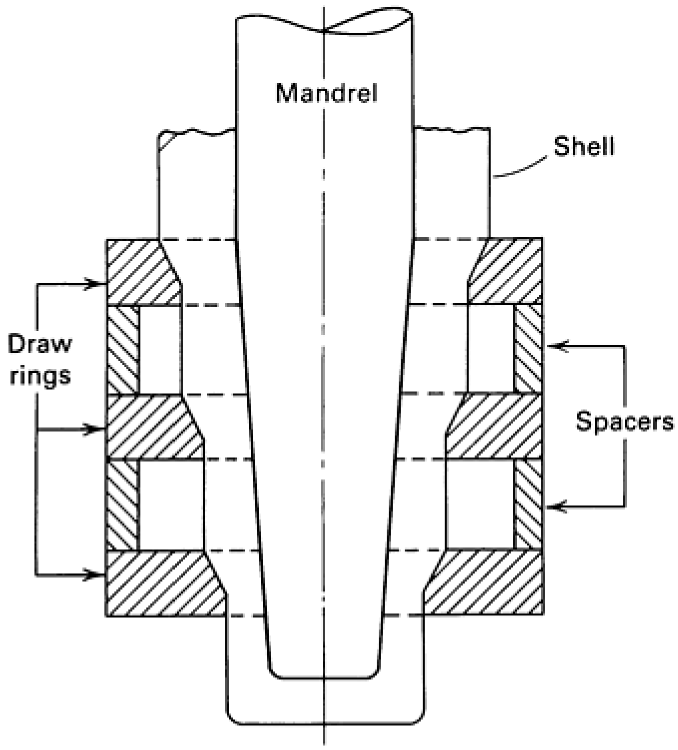

Fig. 12 Multipass ironing with tapered punch and dies in tandem.

In a typical application, a relatively thick-wall cup is first produced by extrusion or deep drawing. The wall thickness of this cup is then reduced by tandem ironing with a cylindrical punch, while the internal diameter remains unchanged. Hot and cold ironing both produce parts with good dimensional accuracy while maintaining or improving concentricity.

A very common application of tandem drawing is the production of beverage cans from steel or aluminum. The principle of a can ironing press is illustrated in Fig. 13. The press is horizontal, and the ram has a relatively long stroke and is guided by the hydrostatic bushing (A). The front seal (B) prevents mixing of the ironing lubricant with the hydrostatic bushing oil. With the ram in the retracted position, the drawn cup is automatically fed into the press, between the redraw die (D) and the redraw sleeve (C). The redraw die centers the cup for drawing and applies controlled pressure while the cup is drawn through the first die (D). As the ram proceeds, the redrawn cup is ironed by passing through the carbide dies (E), which gradually reduce the wall thickness. The ironed can is pressed against the doming punch (I), which forms the bottom shape of the can. When the ram starts its return motion, the mechanical stripper (G), assisted by the air stripper (F), removes the can from the ironing punch (H). The punch is made of carbide or cold-forging tool steel. The stripped can is automatically transported to the next machine for trimming of the top edge of the can wall to a uniform height.

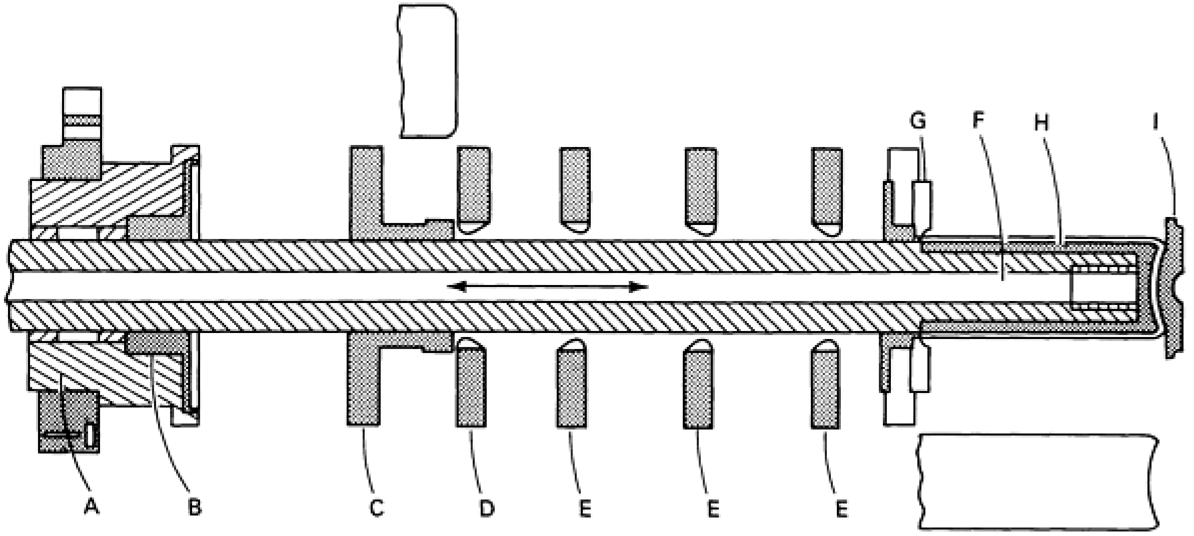

Fig. 13 Multiple-die ironing operation for the manufacture of beverage cans.

Single-Spindle Machines. With the development of floating plugs (Fig. 5), long lengths of thin-wall small-diameter nonferrous tubing can be drawn on special types of single-spindle machines. Instead of using a conventional mandrel that is attached to a rod, as is done in drawbench operations, a specially designed plug is inserted into the leading end of the tube before pointing and passing the tube through the draw die. The plug is free to ride in the throat of the die during drawing, thus controlling the inside diameter of the tube (while the die controls the outside diameter) and maintaining the desired wall thickness.

Drawing methods and machines, particularly material-handling arrangements, are generally more sophisticated for Single-spindle tube drawing than for the more conventional bull blocks used in drawing rod and wire. The machine configurations available for single-spindle tube drawing include horizontal, vertical upright, and inverted vertical designs.

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Dies and Die Materials

The selection of tool materials for cold drawing metal into continuous forms such as wire, bar, and tubing depends primarily on the size, composition, shape, stock tolerance, and quantity of the metal being drawn. The cost of the tool material is also important and may be decisive.

Dies and mandrels used for cold drawing are subjected to severe abrasion. Therefore, most of the wire, bar, and tubing produced is drawn through dies having diamond or cemented tungsten carbide inserts, and tube mandrels are usually fitted with carbide nibs. Small quantities, odd shapes, and large sizes are more economically drawn through hardened tool steel dies.

Wire-Drawing Dies. Table 1 lists recommended materials for wire-drawing dies. For round wire, dies made of diamond or cemented tungsten carbide are always recommended, without regard for the composition or quantity of the metal being drawn. For short runs or special shapes, hardened tool steel is less costly, although carbide gives superior performance in virtually any application.

Table 1 Recommended materials for wire-drawing dies

Metal to be drawn	Wire size		Recommended die material	
	mm	in.	Round wire	Special shapes
Carbon and alloy steels	<1.57	<0.062	Diamond, natural or synthetic	CPM 10V, M2, or cemented tungsten carbide
	>1.57	>0.062	Cemented tungsten carbide	
Stainless steels; titanium, tungsten, molybdenum and nickel alloys	<1.57	<0.062	Diamond, natural or synthetic	CPM 10V, M2, or cemented tungsten carbide
	>1.57	>0.062	Cemented tungsten carbide	
Copper	<2.06	<0.081	Diamond, natural or synthetic	CPM 10V, D2, or cemented tungsten carbide
	>2.06	>0.081	Cemented tungsten carbide	
Copper alloys and aluminum alloys	<2.5	<0.100	Diamond, natural or synthetic	CPM 10V, D2, or cemented tungsten carbide
	>2.5	>0.100	Cemented tungsten carbide	
Magnesium alloys	<2.06	<0.081	Diamond, natural or synthetic	. . .
	>2.06	>0.081	Cemented tungsten carbide	

Die Life. In a wire-drawing die, the approach angle and the bearing area (Fig. 7) are both subjected to severe abrasion. Normal die life is defined as the length or mass of metal drawn through a die that causes the bearing area of the die to increase from minimum to maximum size. Factors that influence die wear, both singly and collectively, are drawing speed, composition of the metal being drawn, wire temperature, reduction per pass, and hardness of the die material.

Wear often begins as an angular ring on the approach angle of the die. Die life can be increased by as much as 200% if the die is removed and repolished at the first appearance of this ring; otherwise, die wear will accelerate. Redressing should never shorten the length of the bearing area to less than 30% of the product diameter.

Diamond Dies. The use of diamond dies is restricted only by limitations on the sizes of available industrial diamonds and by cost, which is extremely high for diamonds in larger sizes. These tools can outperform cemented tungsten carbide dies by 10 to 200 times, depending on the alloy being drawn; therefore, they are cost effective despite their high unit cost.

Cemented tungsten carbide is economical for wire-drawing dies in most applications above the range of size where diamond can be used. The softer cemented carbides, which contain about 8% Co, are less brittle and can withstand greater stock reductions without breaking, but wear more rapidly than lower-cobalt grades.

If not damaged or broken, carbide dies can be progressively reworked to accommodate larger wire sizes. Diamond dies can also be reworked, but greater numbers of reworkings are expected from carbide dies.

Tool steel used for wire-drawing dies should have near-maximum hardness (62 to 64 HRC) for reductions below about 20%. For greater reductions, because of the possibility of breakage, hardness should be decreased to 58 to 60 HRC, even though the rate of wear will increase.

Die breakage is usually caused by abnormal reductions, lack of mechanical support for the insert, inadequate lubrication, or use of a tool material that is too hard and brittle for the amount of reduction and speed. Some wear resistance is always sacrificed to minimize breakage.

Drawing Bars and Tubing. Table 2 lists recommended die and mandrel materials for drawing bars and tubing. Diamond is virtually never used in larger sizes; cemented tungsten carbide is recommended for three-fourths of all applications. Tool steels are rarely used to make tools for drawing commercial-quality round bars less than 90 mm (3.5 in.) in diameter. Cemented tungsten carbide is used to draw stainless steel tubes as large as 279 mm (11 in.) in outside diameter.

Table 2 Recommended tool materials for drawing bars, tubing, and complex shapes

Metal to be drawn	Round bars and tubing[a]			
	Common commercial sizes		**Maximum commercial size[c]: dies and mandrels**	Complex shapes: dies and mandrels[a][b]
	Bar and tube dies	**Tube mandrels[b]**		
Carbon and alloy steels	Tungsten carbide	W1 or carbide	D2 or CPM 10V	**CPM 10V or carbide**
Stainless steels, titanium, tungsten, molybdenum, and nickel alloys	Diamond or carbide[d]	D2 or carbide	D2, M2, or CPM 10V[a]	**F2 or carbide[e]**
Copper, aluminum, and magnesium alloys	**W1 or carbide**	**W1 or carbide**	**D2 or CPM 10V**	**O1, CPM 10V, or carbide**

(a) Tool steels for both dies and mandrels are usually chromium plated.

(b) "Carbide" indicates use of cemented carbide nibs fastened to steel rods.

(c) 10 in. OD by $\frac{3}{4}$ in. wall.

(d) Under 1.5 mm (0.062 in.), diamond; over 1.5 mm (0.062 in.), tungsten carbide.

(e) Recommendations for large tubes or complex shapes apply to stainless steel only.

Drawing of Common Sizes. Common sizes are usually drawn in sufficient quantities to warrant the investment in carbide dies. In addition, carbide bar or tube dies can be reworked to the next larger size. Die life after reworking is substantially the same as for the first run. In drawing steel bars, it is possible to increase normal die life by properly planning the sequence of compositions to be drawn.

For example, in drawing 0.45% C steel bars 25.40 mm (1.000 in.) in diameter, a minus tolerance of 0.08 mm (0.003 in.) is allowed, but for this grade it is necessary to allow for a 0.05 mm (0.002 in.) elastic expansion of the bar after it passes through the die. When the die is worn to maximum size at the bearing area, it will still be only 25.35 mm (0.998 in.) in diameter. It is then possible to draw 0.20% C steel bars, which expand less because of their lower yield strength. After the limit of tolerance has been reached for this grade (a diameter of 25.37 mm, or 0.999 in., at the bearing area), the die can be used for drawing a still lower carbon steel, such as a low-carbon free-machining grade that expands even less, until the diameter of the bearing area reaches 25.40 mm (1.000 in.). The dies can then be reworked to the next usable size.

In many cases, the planning of drawing sequences is more complicated than described above. Bell angle, approach angle, back relief, and amount of subsequent straightening all affect as-drawn size because they influence the amount of elastic growth that occurs; therefore, these factors must be taken into account when planning drawing sequences.

Drawing of Complex Shapes. When complex shapes are to be drawn, the selection of die material is somewhat uncertain. In short runs less than 300 m (1000 ft), tool steels are generally more economical. For longer runs, carbide is usually more economical unless sharp edges, which may cause the carbide to chip, are involved. In that event, tool steel dies must be used, even though they may have to be replaced more frequently because of wear. A proprietary powder metallurgy tool steel, CPM 10V, is another alternative to cemented carbide. CPM 10V has toughness equivalent to the conventional tool steels D2 and M2, and it has substantially superior wear resistance in drawing-die service.

Die Breakage. The most frequent cause of die breakage in bar and tube drawing is a die design inappropriate for the percentage of reduction. Excessive die hardness also frequently leads to breakage, particularly of dies for drawing thin-wall tubing. Lack of lubrication, excessive drawing speeds, and other extreme conditions of operation also contribute to die breakage.

Wire, Rod, and Tube Drawing

Lubrication (Ref 7)

Proper lubrication is essential in rod, tube, and wire drawing. No friction is needed for wire drawing, tube sinking, and tube drawing on a fixed plug. However, some minimum friction is essential for drawing with a floating plug, and friction is helpful on the tube/bar interface in drawing on a bar. Therefore, if at all possible, the lubricant is chosen to give lowest friction and minimum wear. It is essential, though, that the heat generated be extracted, especially in high-speed drawing; if this is not done, the lubricant may fail, and the properties of the wire may suffer.

In dry drawing, the lubricant is chosen for its tribological attributes, and the wire is cooled while it resides on the internally cooled capstans of single-hole bull blocks and of multihole machines drawing with accumulation. In addition, external air cooling of the wire coil and water cooling of the die holder are possible. If water is applied to the wire at all, it must be totally removed before the wire enters the next die. The lubricant is usually a dry soap powder, placed in a die box and picked up by the wire surface upon its passage through the box. This technique is used for steel wire larger than 0.5 to 1 mm (0.02 to 0.04 in.) in diameter, for which the relatively rough surface produced is acceptable. For the most severe draws and for tubes, the soap is often preapplied from a solution, if necessary, over a conversion coating; the soap must be allowed to dry.

With high-strength materials such as steels, stainless steels, and high-temperature alloys, the surface of the rod or wire can be coated either with a softer metal or with a conversion coating. Copper or tin can be chemically deposited on the surface of the metal. This thin layer of softer metal acts as a solid lubricant during drawing. Conversion coatings may consist of sulfate or oxalate coatings on the rod; these are then typically coated with soap, as a lubricant. Polymers are also used as solid lubricants, such as in the drawing of titanium.

In the case of steels, the rod to be drawn is first surface treated by pickling. This removes the surface scale that could lead to surface defects and therefore increases die life.

In wet drawing, the lubricant is chosen both for its tribological attributes and for its cooling power, and it can be either oil-base or aqueous. It can be applied to the die inlet, the wire, and often also to the capstan, or the entire machine can be submerged in a bath. When the machine operates with slip, the lubricant must reduce wear of the capstan while maintaining some minimum friction. This wet-drawing practice is typical of all nonferrous metals and of steel wires less than 0.5 to 1 mm (0.02 to 0.04 in.) in diameter.

A transition between the two techniques is sometimes used, particularly in the low-speed drawing of bar and tube. A high-viscosity liquid or semisolid is applied to the workpiece and/or die. Reference 8 provides additional details on the lubrication of ferrous wire.

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The Manufacture of Commercial Superconductors*

The design requirements of commercial superconductors have challenged metal extrusion and composite metal-drawing technology such that superconductors with 10,000 to 40,000 filaments, several microns in diameter, are available in wire form. On an experimental basis, wires have been produced with 1 million filaments less than 1 μm (40 μin.) in diameter. Understanding the reasons for this challenge to metal-forming technology requires a brief introduction to engineering requirements for commercial superconductors.

The design and application of superconductors is mainly controlled by the critical temperature, T_c. Niobium-base superconductors are usually used at liquid helium temperatures (4.2 K). At these temperatures, specific heats of materials are typically in the 10^{-3} J range. Small mechanical or electromagnetic disturbances can provide sufficient heat to raise the temperature of the superconductor above T_c; this increase in temperature causes the normally high resistance to return. Commercial superconductors are designed to prevent and/or control the change to the nonsuperconducting state.

Copper and aluminum are usually used as the matrix; copper is the preferred matrix because of its mechanical compatibility with the niobium-base superconductors. If an event occurs that is sufficient to return the superconductor to the normal state, the copper temporarily conducts the current until the superconductor is cooled below the critical temperature.

The sizes of the filaments of the superconductor are chosen to be in the 100 μm (4000 μin.) range--small enough to prevent an electromagnetic instability called a flux jump. For applications requiring precise magnetic fields, as in dipole magnets, filaments must be in the 1 μm (40 μin.) range. Power frequency applications require filaments of less than 1 μm (40 μin.).

Commercial superconductors for power applications are manufactured by a coextrusion and composite-drawing process. The resulting wire consists of one to tens of thousands of filaments of the superconductor, each individually surrounded by a normal metal matrix. The superconductor itself is usually a ductile alloy of niobium and titanium (Fig. 14) or a brittle intermetallic of niobium and tin (Nb_3Sn) (Fig. 15 and 16).

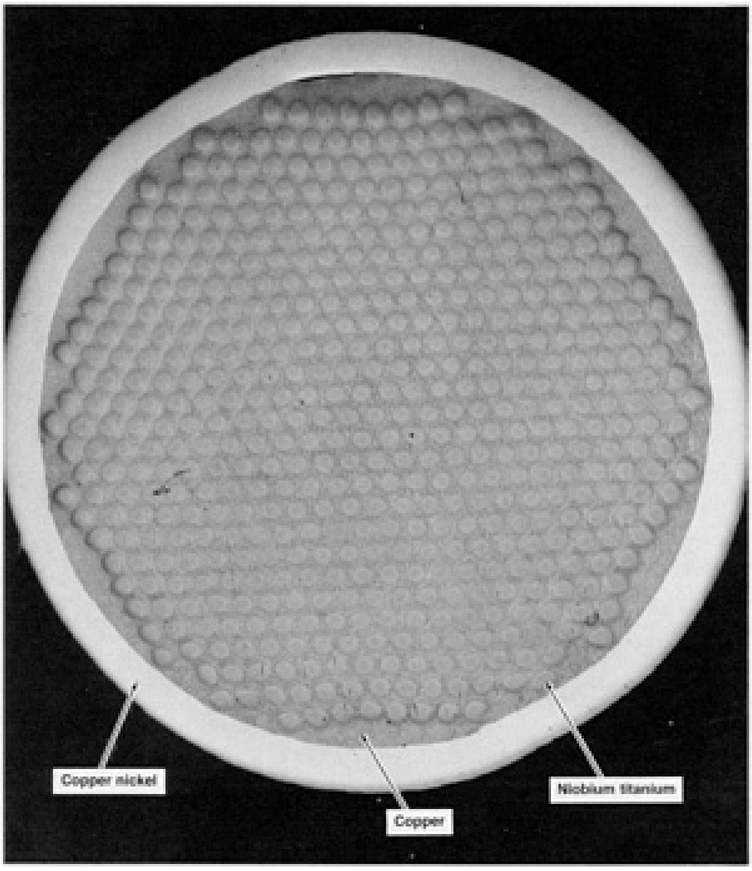

Fig. 14 Cross section of 500 niobium-titanium filaments separated by a copper substrate and enclosed within a copper-nickel tube. Courtesy of Oxford Superconducting Technology.

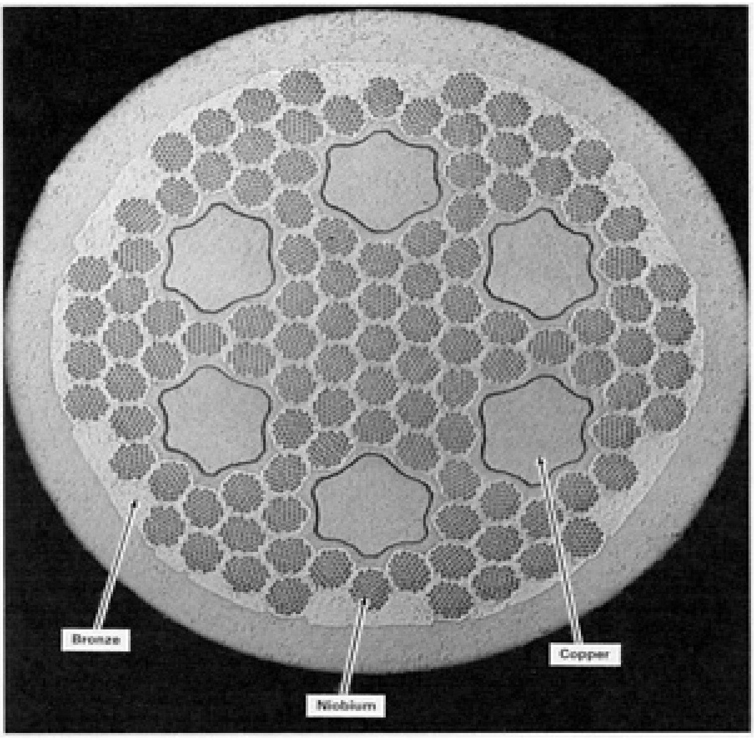

Fig. 15 Cross section of niobium filaments reacted with tin in the bronze substrate to form Nb_3Sn. Courtesy of Oxford Superconducting Technology.

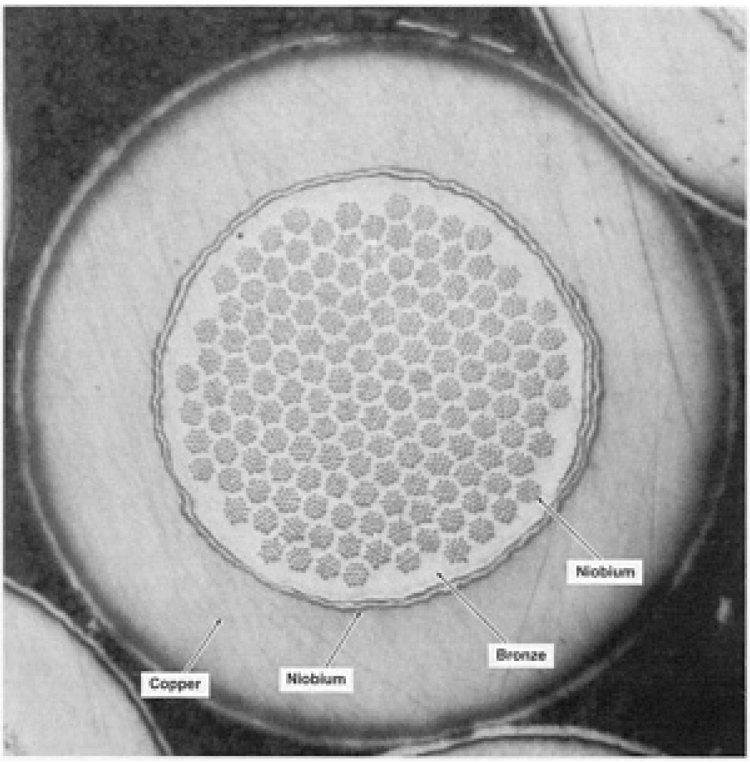

Fig. 16 Cross section of 3000 filaments of Nb_3Sn in final conductor. Courtesy of Oxford Superconducting Technology.

Superconducting multifilamentary conductors are manufactured using a combination of extrusion and wire-drawing techniques (up to 40 to 50 such separate sequences may be necessary) to make up to 1 million individual wire filaments of microscopic size enclosed within a wire having outside diameter of fractions of an inch. The two primary techniques used are billet stacking and the modified jelly-roll method.

Billet Stacking Method. The manufacture of a typical niobium-titanium superconductor with filaments in the 10 to 100 μm (400 to 4000 μin.) range begins with the assembly of a billet (Fig. 17). The billet is assembled by inserting rods of the superconductor into an array of tubes of CDA 101 copper with a hexagonal outer shape and a round inner diameter. The array approximates a circle having a diameter slightly less than the copper extrusion can placed over it. A typical billet is 305 mm (12 in.) in diameter and 762 mm 30 in. long.

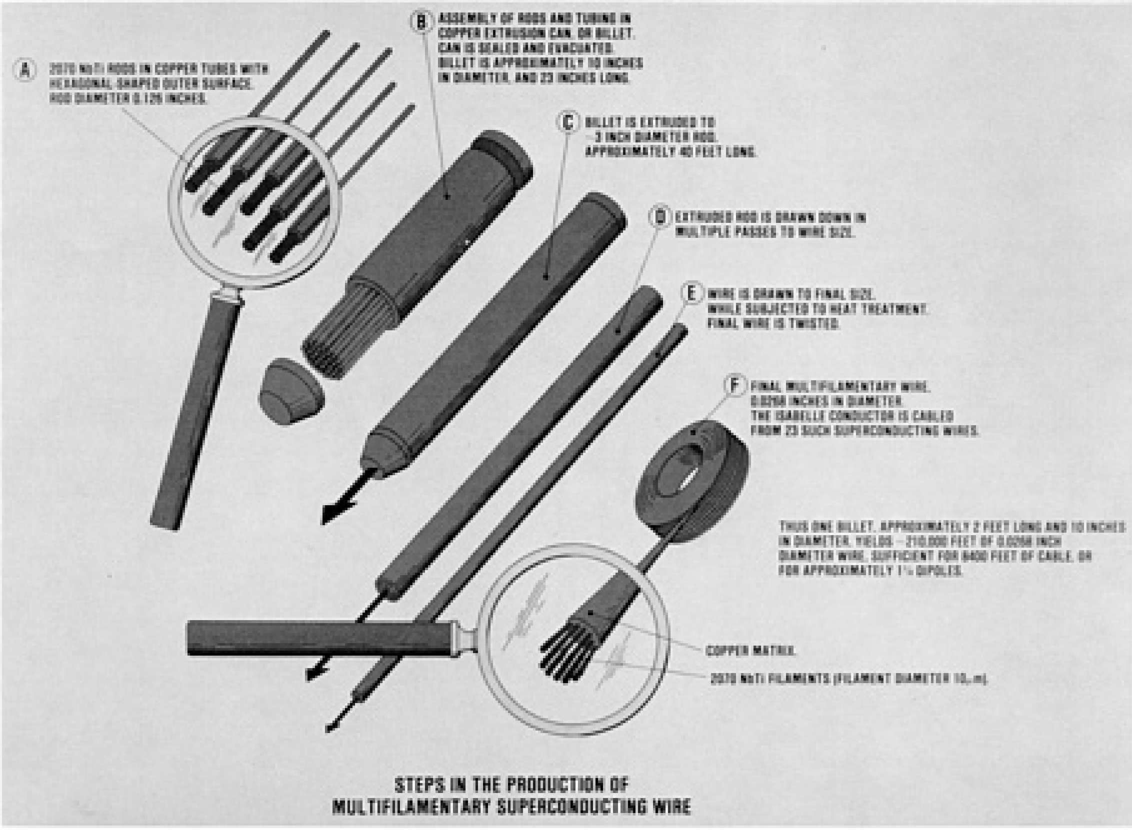

Fig. 17 Fabrication steps and process parameters required to manufacture multicore niobium-titanium filament conductors. Courtesy of Intermagnetics General Corporation.

The billet is evacuated to remove the air and electron beam welded to form a vacuum-tight seal. The elements in the billet are metallurgically bonded and uniformly reduced in area by a hot direct extrusion. Reduction ratios of 16 to 1 are generally used, requiring extrusion forces typically in the 31 to 44 MN (3500 to 5000 tonf) range. The resulting extrudate is normally 10 m (33 ft) long by 85 mm (3.3 in.) in diameter. This rod is then cold drawn using a proprietary die and reduction schedules designed to ensure uniform coreduction of the superconducting filaments. The initial draw process requires benches as long as 60 m (200 ft), with 590 kN (60,000 kgf) of draw force to be used to ensure that the rod does not have to be cut before it is coiled for further drawing. The remaining process involves performing heat treatment and draw cycles to develop the current capacity of the superconductor, annealing the matrix, and uniformly coreducing the filaments. Specially modified wire-drawing machinery is generally used. The final step is an anneal to restore the ductility and resistivity of the copper matrix. Preceding this step is a twisting operation, which twists the wire upon itself. This twists the filaments inside the composite, ensuring that the filaments act individually under electromagnetic fields.

Superconductors requiring more than 5000 or 6000 filaments or, correspondingly, filaments of sizes less than 10 μm (400 μin.) are made by coextruding a single filament, which, in the case of niobium-titanium, usually has a diffusion barrier. This ensures that no damaging intermetallics form during the extrusion or heat treatment process. This extrudate is drawn to the appropriate size and assembled into a second extrusion billet; the process is then repeated.

The extrusion and draw process can be repeated a number of times to produce wires with 20,000 to 1 million or more filaments. As shown in Fig. 18, a first-generation billet can yield 19 individual filaments in the wire configuration. Sixty one of these 19-filament wires were then stacked, extruded, and drawn to yield a 19 × 61 array of 1159 filaments in the second-generation billet. The third-generation billet, consisting of 61 of the 1159 filament wires obtained in the second-generation billet, was stacked, extruded, and drawn to yield a 70,699 filament superconducting wire. However, the problem of "sausaging," or filament nonuniformity, especially in the outer diameter of the conductor, becomes more evident as more extrusions and drawings of the wire are attempted.

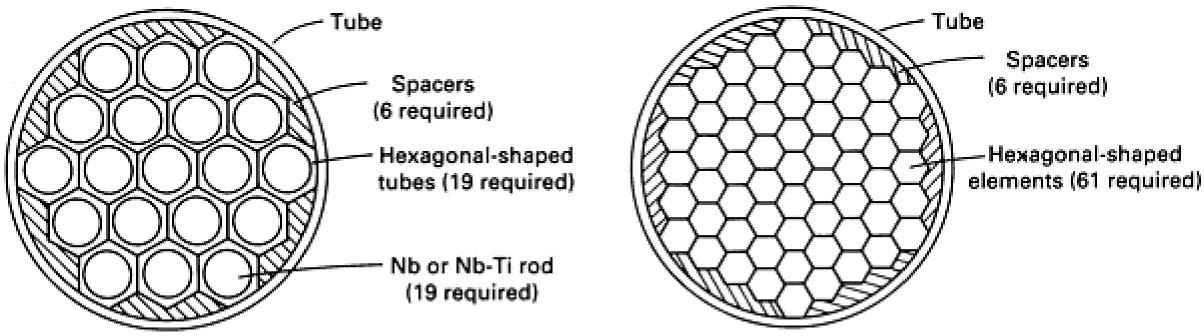

Fig. 18 Cross section of billet assembly used to produce a 70 699-filament wire using up to third-generation billets. (a) First-generation billet. (b) Second- and third-generation billets.

For these developmental conductors, the matrix is often composed of several metals or alloys of metals, such as copper nickel, or is alloyed slightly with magnetic impurities, such as manganese. This ensures that the electromagnetic and physical sizes of the micron-size filaments are the same.

The manufacture of Nb_3Sn in filamentary form has presented some unusual problems because of the brittle nature of the superconductor. The accepted processes are based on forming the brittle phase at the final stage. Early technology carried the tin in a 13 wt% bronze matrix. The manufacturing process was a multiple coextrusion and codrawing process similar to that for niobium-titanium. The work-hardening rates of bronze require many anneals, and this can lead to premature formation of the brittle phase. More recently, a process based on maintaining the tin in its pure phase has gained acceptance. This is called the internal tin process.

The standard process begins with a billet of copper and niobium filaments assembled into a tubular array (Fig. 19). The billet follows the usual procedures for niobium-titanium, but is extruded with a mandrel to maintain the hole. The resulting extrudate typically has several hundred filaments of niobium in a copper matrix with a hole of about 10% of the diameter running throughout the length at the center. Tin is inserted into the hole, and the resulting composite is drawn to a size suitable for assembly into the stabilizer tube. This tube is also formed from a hollow extrusion but is composed of copper and a diffusion barrier such as niobium or vanadium. The diffusion barrier keeps the tin from alloying with the copper in the stabilizer. Quantities of 19 to 37 composite elements are assembled and inserted into the stabilizer tube. The resulting rod is repeatedly drawn without any annealing to yield a wire having a diameter ranging from 1 mm to fractions of a millimeter. The resulting conductor frequently has several to tens of thousands of filaments in the several micron size range with multiple cores of tin. The wire is then heat treated to diffuse and then react the tin with the niobium to form the Nb_3Sn.

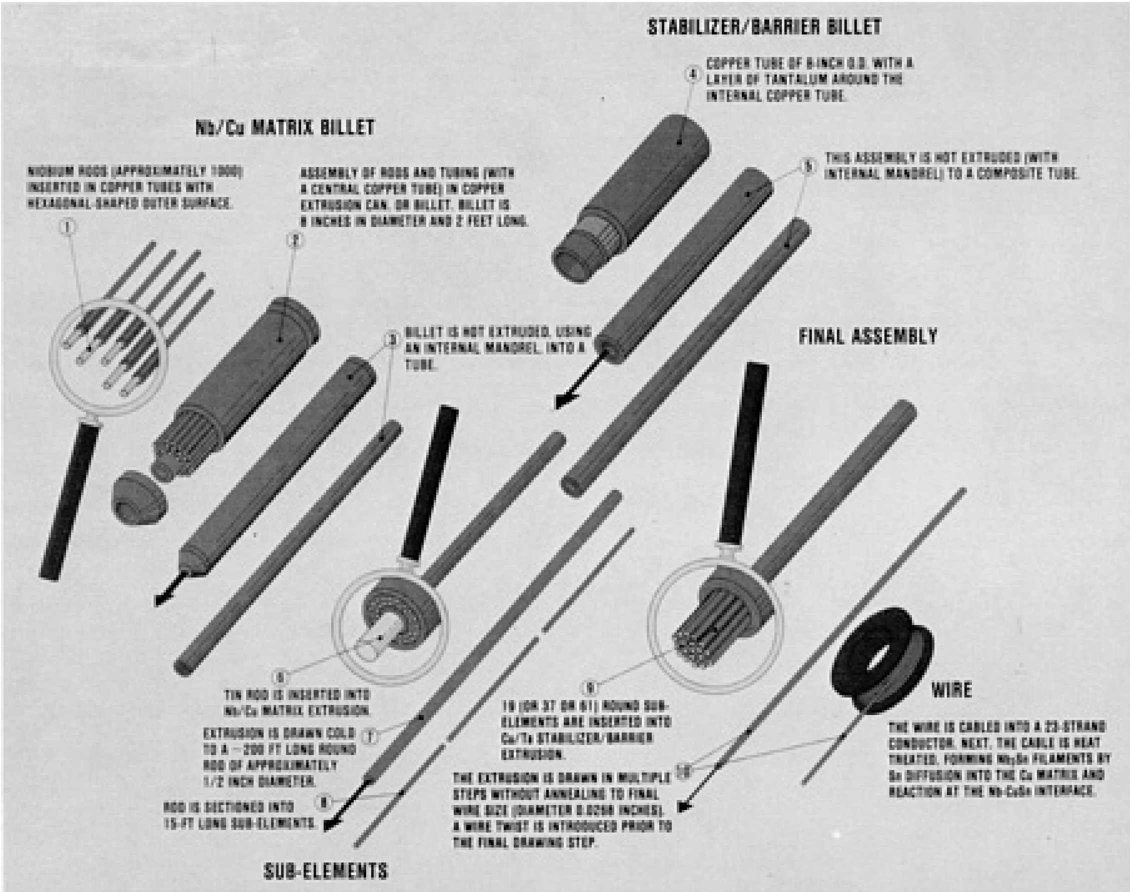

Fig. 19 Sequence of manufacturing operations involved in the formation of Nb_3Sn multifilamentary wire using the internal tin process. Courtesy of Intermagnetics General Corporation.

Modified Jelly-Roll Method. Instead of using rods, the modified jelly-roll method (Ref 9) utilizes thin (0.05 to 0.50 mm, or 0.0020 to 0.020 in., thick) niobium foil sheets that are slit with a number of discontinuous and staggered parallel slits having controlled interconnection distances, *d*, of 5 to 150 mm (0.197 to 5.9 in.), as shown in Fig. 20. This niobium sheet is subsequently stretched at right angles to the original slits to produce a diamond-shaped array of continuously connected filaments--a hexagonal matrix that is 45 to 85% open. The expanded niobium sheet is then rolled up with the designated matrix material (bronze, copper, and aluminum), inserted into a copper container, sealed, extruded, and processed as conventional wire. As area reduction of the cross section progresses, the horizontal dimension ***d*** is stretched and elongated, and the vertical dimension ***w*** is compressed by a factor of 100,000 to 1,000,000 to form the individual filaments (Fig. 21(a) and 21(b)).

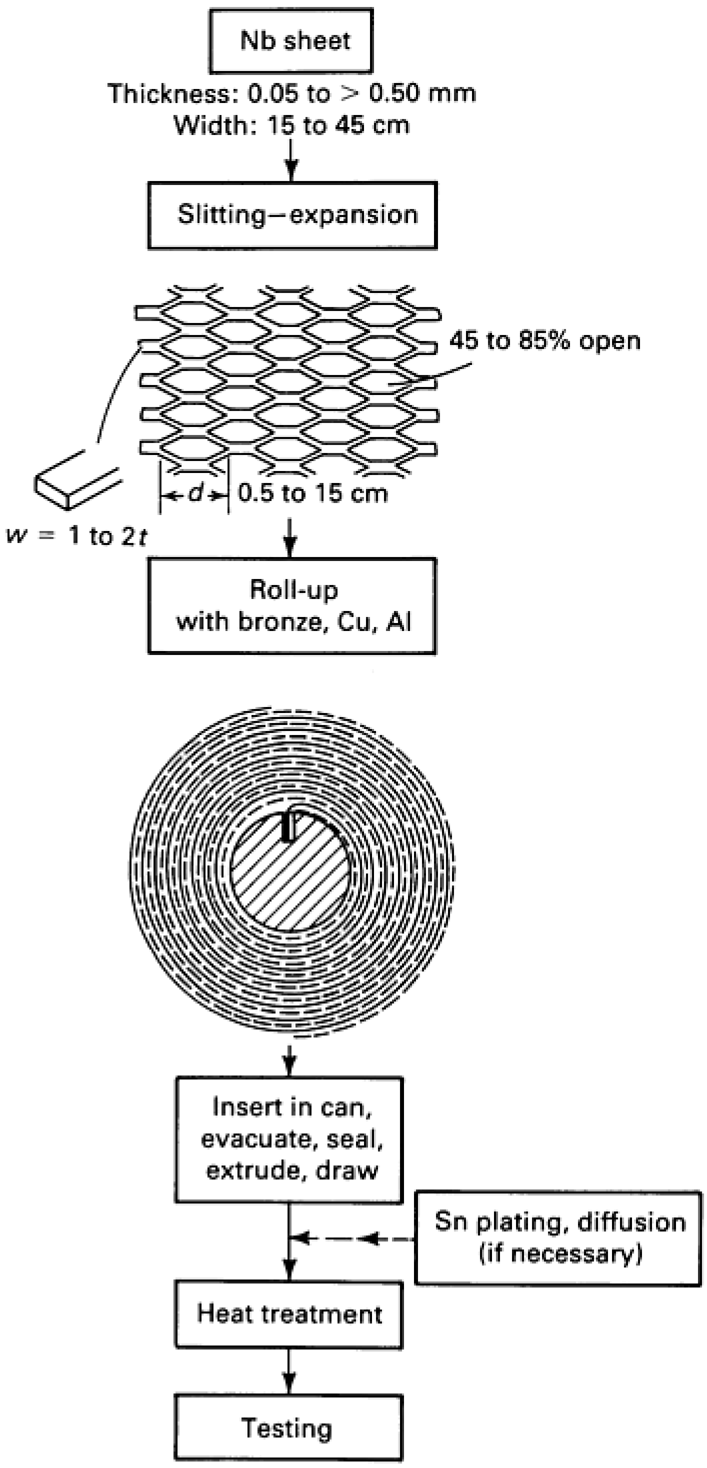

Fig. 20 Schematic of the modified jelly-roll process for producing superconducting multi-filamentary wire.

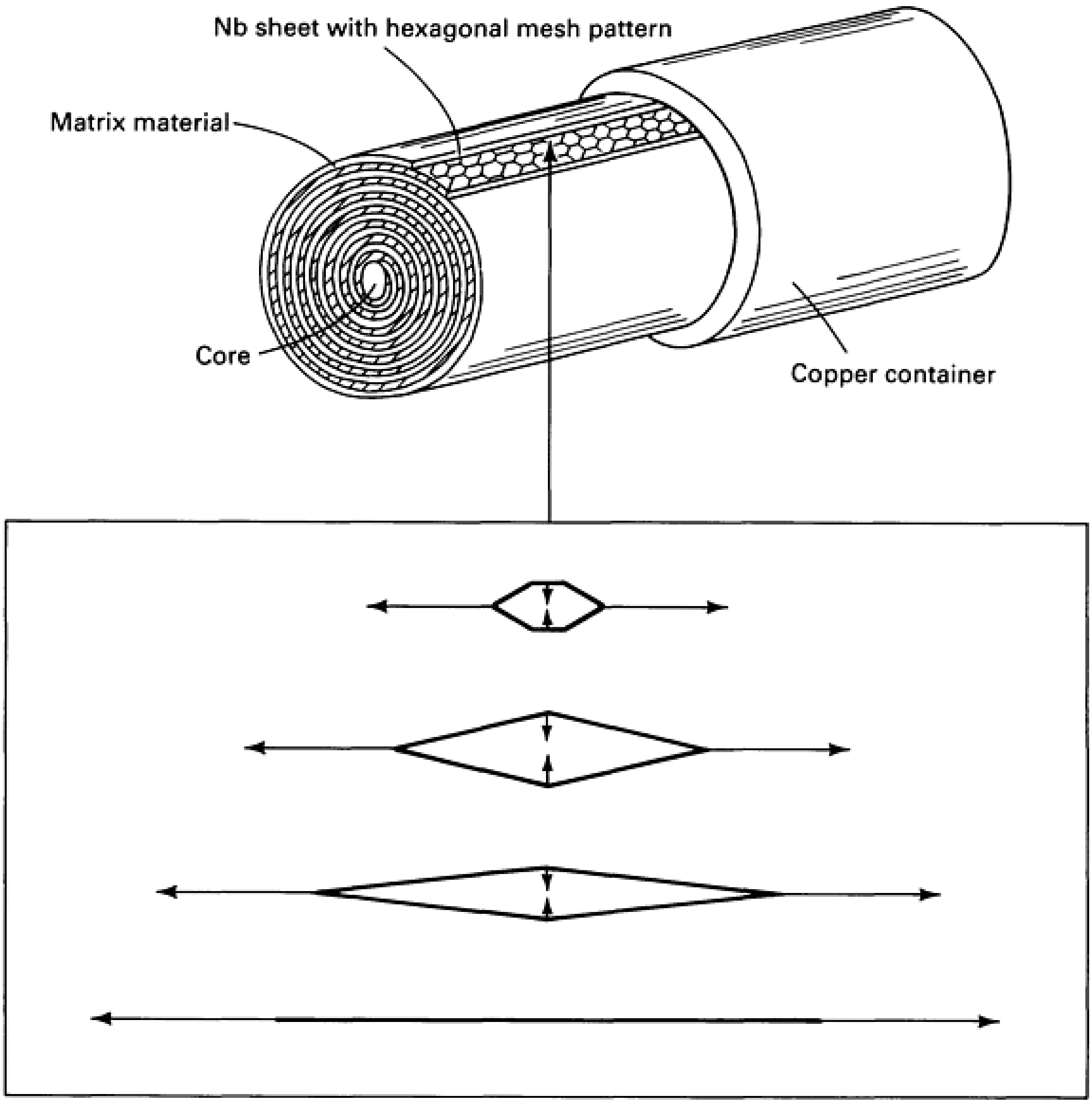

Fig. 21(a) Detail of niobium sheet mesh rolled into a jelly roll using a matrix material to show how the hexagonal mesh is transformed into an individual filament through elongation in the horizontal direction and compression in the vertical direction when subjected to extrusion and drawing.

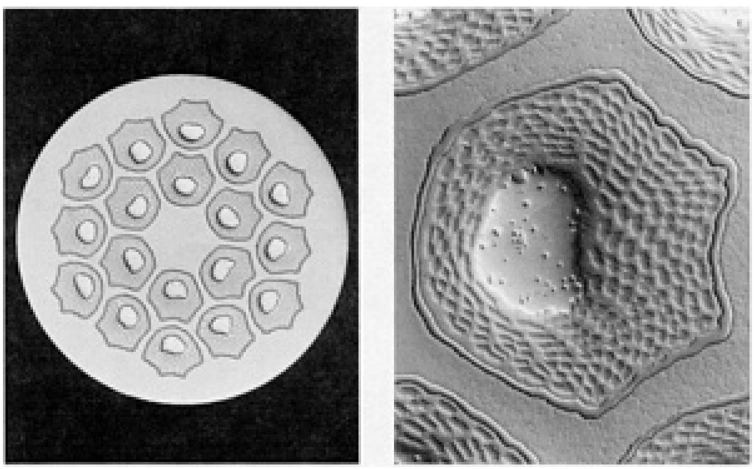

Fig. 21(b) Cross section of a 0.78-mm (0.0307-in.) diam unreacted niobium-tin multifilamentary composite

wire consisting of 18 subelements that were produced using the modified jelly-roll method. The wire was cold worked to a 160,000 to 1 reduction in area. Left: 65×. Right: Close-up of one of the 18 subelements showing the individual niobium filaments surrounding a tin/copper alloy core and a vanadium barrier. 465× (differential interference contrast). Courtesy of P. E. Danielson, Teledyne Wah Chang Albany.

Numerous billets 75 mm (3 in.) in diameter have been produced with a final niobium filament size of 1 to 2 μm (40 to 80 μin.) in diameter. It should be possible to achieve submicron filaments. At the commercial level, the process can be used for Va_3Ga, Nb_3Al, Nb_3Sn, NbTi, and other composites.

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Wire, Rod, and Tube Drawing

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Flat, Bar, and Shape Rolling

G. D. Lahoti, The Timken Company; S.L. Semiatin, Battelle Columbus Division

Introduction

ROLLING OF METALS is perhaps the most important metalworking process. More than 90% of all the steel, aluminum, and copper produced--in 1985, some 800 million tons of material worldwide--go through the rolling process at least one time. Thus, rolled products represent a significant portion of the manufacturing economy and can be found in many sectors. Beams and columns used in buildings are rolled from steel. Railroad tracks and cars are made from rolled steel, and airplane bodies are made from rolled aluminum and titanium alloys. The wire used in fences, elevator ropes, electrical conductors, and cables are drawn from rolled rods. Many consumer items, including automobiles, home appliances, kitchen utensils, and beverage cans, use rolled sheet materials.

In rolling, a squeezing type of deformation is accomplished by using two work rolls (Fig. 1) rotating in opposite directions. The principal advantage of rolling lies in its ability to produce desired shapes from relatively large pieces of metals at very high speeds in a somewhat continuous manner. Because other methods of metalworking, such as forging, are relatively slow, most ingots and large blooms are rolled into billets, bars, structural shapes, rods (for drawing into wire), and rounds for making seamless tubing. Steel slabs are rolled into plate and sheet.

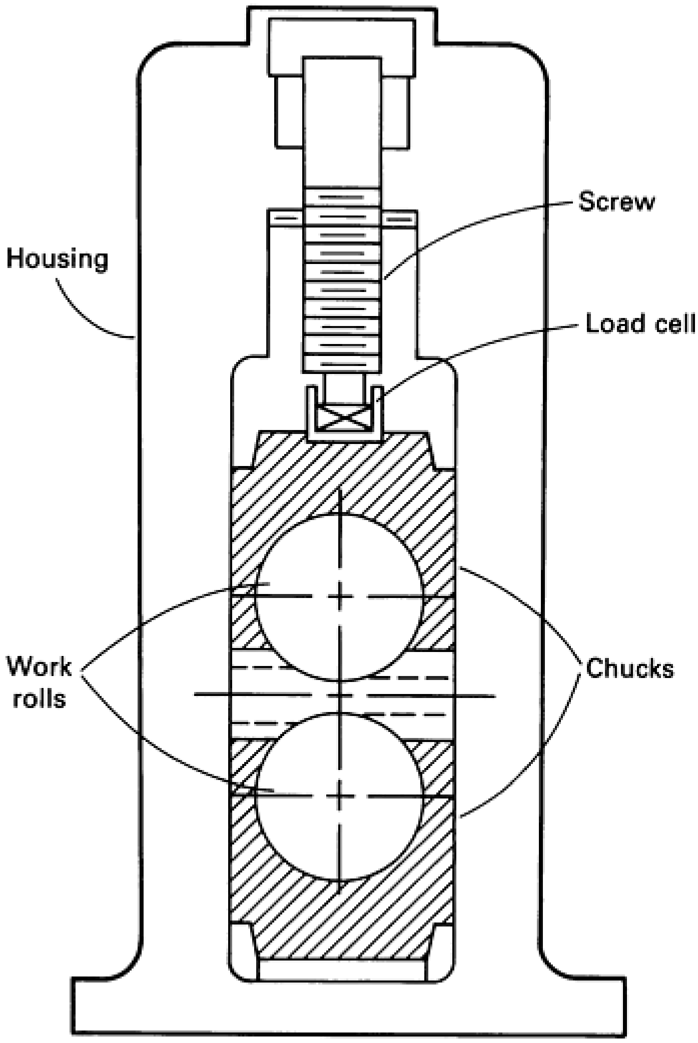

Fig. 1 Typical rolling mill stand

Although the rolling of metals has been done for some time and has been a very productive means of working large quantities of metals to a variety of shapes and sizes, the state of the technology had been somewhat stagnant until recently, when major innovations started to appear. With the advent of computer-assisted controls, highly automated, very high-speed rolling mills were installed beginning in the 1970s. One rod mill commissioned in 1980, for example, is reported to roll steel wire rod at the rate of 335 kph (210 mph). This mill has a rated output of 545,000 Mg (600,000 tons) per year, and the entire mill is operated from three climate-controlled pulpits equipped with computerized controls and closed-circuit video monitors. Another modern mill came on stream in the early 1980s. It is a 200 cm (80 in.) hot strip mill capable of producing steel coils up to 188 cm (74 in.) wide and weighing up to 33.6 Mg (37 tons). The mill features computer controls that automatically adjust water flow rates, roll speeds, and strip temperatures to meet metallurgical requirements. In addition to these developments, computer-aided modeling of the rolling process is now routinely used at several locations for design of rolls and optimization of the process parameters (see the section "Mechanics of Plate Rolling" in this article). Understanding of the materials also has improved considerably, thereby permitting development of new products such as high-strength low-alloy (HSLA) steels, which require controlled rolling. In short, significant developments are happening in this field, which was largely neglected for decades.

Acknowledgements

The sections "Basic Rolling Processes," "Strip Rolling Theory," "Mechanics of Plate Rolling," and "Shape Rolling," were adapted from Rolling of Strip, Plate and Shapes, Chapter 16, in *Metal Forming: Fundamentals and Applications,* by T. Altan, S.-I. Oh, and H.L. Gegel, American Society for Metals, 1983, p 249-276.

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Basic Rolling Processes

Many engineering metals, such as aluminum alloys, copper alloys, and steels, are often cast into ingots and are then further processed by hot rolling into blooms, slabs, and billets, which are subsequently rolled into other products such as plate, sheet, tube, rod, bar, and structural shapes (Fig. 2). The definitions of these terms are rather loose and are based on the traditional terminology used in the primary metal industry. For example, a bloom has a nearly square cross section with an area larger than 205 cm^2 (32 $in.^2$); the minimum cross section of a billet is about 38 × 38 mm (1.5 × 1.5 in.), and a slab is a hot-rolled ingot with a cross-sectional area greater than 103 cm^2 (16 $in.^2$) and a section width of at least twice the section thickness. Plates are generally thicker than 6.4 mm (0.25 in.), whereas sheets are thinner-gage materials with very large width-to-thickness ratios. Sheet material with a thickness of a few thousandths of an inch is referred to as foil. Rolling of blooms, slabs, billets, plates, and structural shapes is usually done at temperatures above the recrystallization temperature, that is, in the hot-forming range, where large reductions in height or thickness are possible with moderate forming pressures. Sheet and strip often are rolled cold in order to maintain close thickness tolerances.

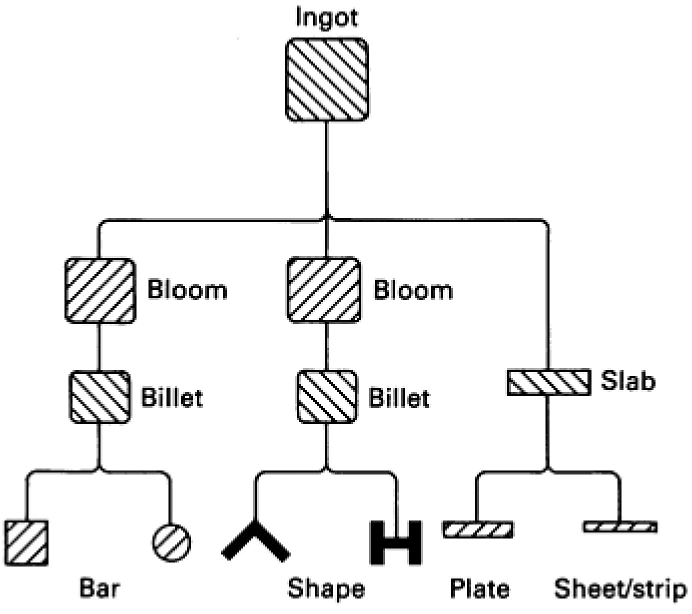

Fig. 2 Rolling sequence for fabrication of bars, shapes, and flat products from blooms, billets, and slabs

The primary objectives of the rolling process are to reduce the cross section of the incoming material while improving its properties and to obtain the desired section at the exit from the rolls. The process can be carried out hot, warm, or cold, depending on the application and the material involved. The technical literature on rolling technology, equipment, and theory is extensive because of the significance of the process (Ref 1, 2, 3, 4, and 5). Many industrial investigators prefer to divide rolling into cold and hot rolling processes. From a fundamental point of view, however, it is more appropriate to classify rolling processes on the bases of the complexity of metal flow during the process and the geometry of the rolled product. Thus, the rolling of solid sections can be divided into the categories below.

Uniform Reduction in Thickness with No Change in Width. This is the case with strip, sheet, or foil rolling where the deformation is in plane strain, that is, in the directions of rolling and sheet thickness. This type of metal flow exists when the width of the deformation zone is at least 20 times the length of that zone.

Uniform Reduction in Thickness with an Increase in Width. This type of deformation occurs in the rolling of blooms, slabs, and thick plates. The material is elongated in the rolling (longitudinal) direction, is spread in the width (transverse) direction, and is compressed uniformly in the thickness direction.

Moderately Nonuniform Reduction in Cross Section. In this case, the reduction in the thickness direction is not uniform. The metal is elongated in the rolling direction, is spread in the width direction, and is reduced nonuniformly in the thickness direction. Along the width, metal flow occurs only toward the edges of the section. The rolling of an oval section in rod rolling or of an airfoil section would be considered to be in this category.

Highly Nonuniform Reduction in Cross Section. In this type of deformation, the reduction in the thickness direction is highly nonuniform. A portion of the rolled section is reduced in thickness while other portions may be extruded or increased in thickness. As a result, in the width (lateral) direction metal flow may be toward the center. Of course, in addition, the metal flows in the thickness direction as well as in the rolling (longitudinal) direction.

The above discussion illustrates that, except in strip rolling, metal flow in rolling is in three dimensions (in the thickness, width and rolling directions). Determinations of metal flow and rolling stresses in shape rolling are very important in designing rolling mills and in setting up efficient production operations. However, the theoretical prediction of metal flow in such complex cases is nearly impossible at this time. Numerical techniques are being developed in an attempt to simulate metal flow in such complex rolling operations.

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Strip Rolling Theory

The most rigorous analysis was performed by Orowan (Ref 6) and has been applied and computerized by various investigators (Ref 7, 8, 9, 10, 11, 12). More recent studies consider elastic flattening of the rolls and temperature conditions that exist in rolling (Ref 9, 13). The roll-separating force and the roll torque can be estimated with various levels of approximations by such mathematical techniques as the slab method, the upper bound method (Ref 10), or the slip line method of analysis (Ref 2, 4). Most recently, computerized numerical techniques are being used to estimate metal flow, stresses, roll-separating force, temperatures, and elastic deflection of the rolls (Ref 9, 13).

Simplified Method for Estimating Roll-Separating Force. The strip-rolling process is illustrated in Fig. 3. Because of volume constancy, the following relations hold:

$$W \cdot H_0 \cdot V_0 = W \cdot H \cdot V = W \cdot H_1 \cdot V_1 \quad \textbf{(Eq 1)}$$

where W is the width of the strip; H_0, H, and H_1 are the thicknesses at the entrance, in the deformation zone, and at the exit, respectively; and V_0, V, and V_1 are the velocities at the entrance, in the deformation zone, and at the exit, respectively. In order to satisfy Eq 1. the exit velocity V_1 must be larger than the entrance velocity V_0. Therefore, the velocity of the deforming material in the *x* or rolling direction must steadily increase from entrance to exit. At only one point along the roll-strip interface is the surface velocity of the roll, V_R, equal to the velocity of the strip. This point is called the neutral point, or neutral plane, indicated by *N* in Fig. 3.

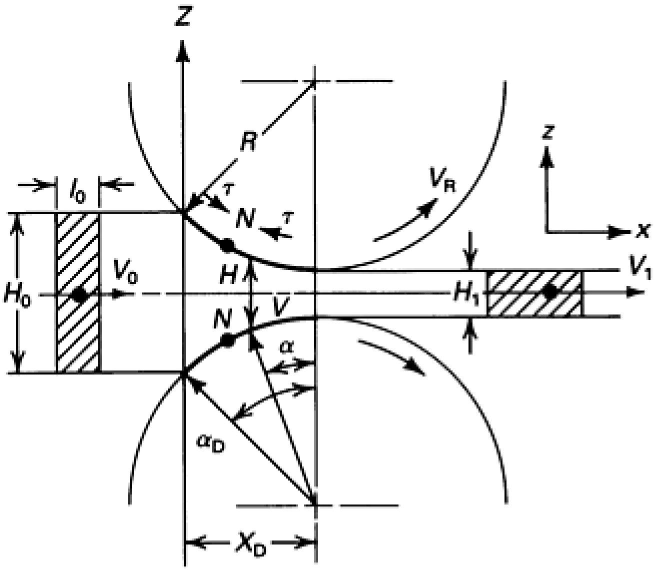

Fig. 3 Representation of strip rolling. The strip width *w* is constant in the *y* (width) direction.

The interface frictional stresses are directed from the entrance and exit planes toward the neutral plane because the relative velocity between the roll surface and the strip changes its direction at the neutral plane. This will be considered later in estimating rolling stresses.

An approximate value for the roll-separating force can be obtained by approximating the deformation zone, shown in Fig. 3, with the homogeneous plane-strain upsetting process. With this assumption, Eq 2 is valid, that is, the load per unit width of the strip is given by:

$$L = \frac{2\bar{\sigma}}{\sqrt{3}}\left(1 + \frac{ml}{4h}\right)l \qquad \textbf{(Eq 2)}$$

However, in this case the following approximations must be made:

- Average strip height $h = 0.5(H_0 + H_1)$
- Average length of the deforming strip $l = R\alpha_D$, with $\cos \alpha_D = 1 - (H_0 - H_1)2R$. In the literature, it is often recommended that the value of the projection of strip length X_D (Fig. 3) be used for *l;* however, considering the effect of friction on the roll-strip interface length, $R\alpha_D$, it is more appropriate to use $l = R\alpha_D$

To estimate average flow stress $\bar{\sigma}(\bar{\varepsilon}\ \dot{\bar{\varepsilon}}, \theta)$ at a given rolling temperature θ, the average strain $\bar{\varepsilon}$ is obtained from the thickness reduction, that is, $\bar{\varepsilon} = \ln (H_0/H_1)$. The strain rate $\dot{\bar{\varepsilon}}_\alpha$ is given by:

$$\dot{\bar{\varepsilon}}_\alpha = V_z/H = 2V_R \sin \alpha/H = [2V_R \sin \alpha]/[H_1 + 2R(1 - \cos \alpha)] \qquad \textbf{(Eq 3)}$$

where V_z is the velocity at a given plane in the z direction (see Fig. 3), H is the thickness at a given plane (roll angle α) in the deformation zone, and V_R is the roll surface velocity. At the entrance plane:

$$V_z = 2V_R \sin \alpha_D; H = H_0$$

At the exit plane:

$$V_z = 0; H = H_1$$

By taking a simple average of these two limiting values, an approximate value of strain rate is obtained:

$$\dot{\bar{\varepsilon}} = [2V_R \sin \alpha_D/H_0 + 0]/2 \qquad \textbf{(Eq 4)}$$

A more accurate value can be obtained by calculating an integrated average of $\dot{\bar{\varepsilon}}\alpha$ (Eq 3) throughout the deformation zone. Then, an average approximate value is (Ref 1):

$$\dot{\bar{\epsilon}} = \frac{V_R}{H_0}\left[\frac{2(H_0 - H_1)}{R}\right]^{1/2} \qquad \textbf{(Eq 5)}$$

The stress (roll pressure) distribution in strip rolling is illustrated in Fig. 4. The maximum stress is at the neutral plane N. These stresses increase with increasing friction and length of the deformation zone X_D. Tensile stresses applied to the strip at entrance or exit have the effect of reducing the maximum stress (by an amount approximately equal to $\Delta\sigma_z$ in Fig. 4b) and shifting the position of the neutral plane. The analogy to plane-strain upsetting is illustrated in Fig. 4(a).

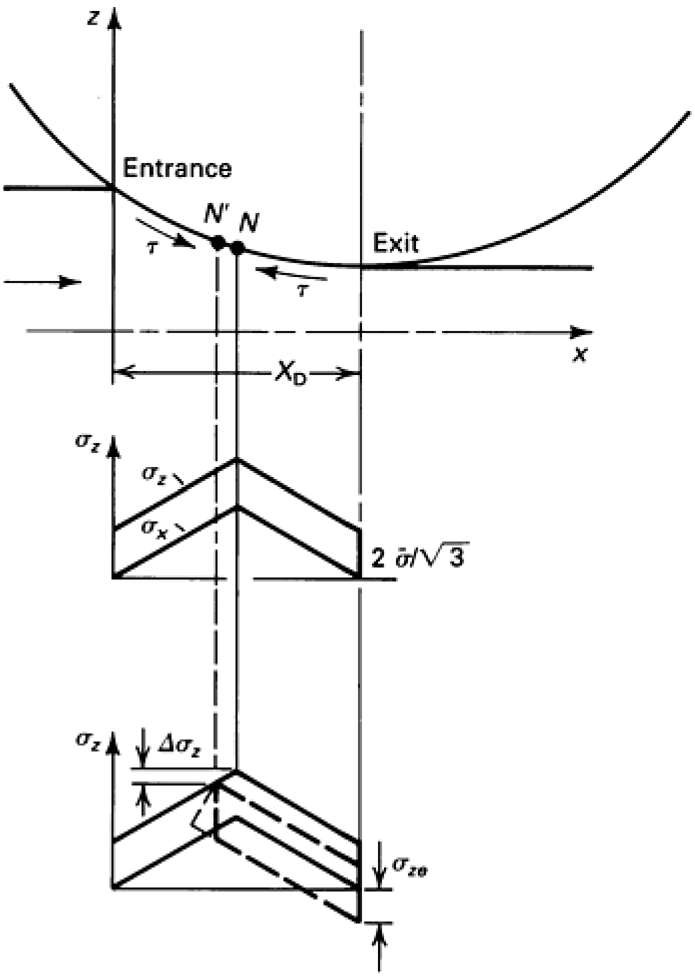

Fig. 4 Stress distribution in rolling. (a) With no tensile stresses at entry or exit. (b) With tensile stress σ_{ze} at exit.

The stress distribution can be calculated by using the equations derived in most textbooks (Ref 1, 2, 3, 4, 5) or by following the theory presented by Orowan (Ref 6). However, these calculations are quite complex and require numerical techniques in order to avoid an excessive number of simplifying assumptions. A computerized solution, with all necessary details and the listing of the FORTRAN computer program, is also given by Alexander (Ref 8).

For a numerical/computerized calculation of rolling stresses, the deformation zone can be divided into an arbitrary number of elements with flat, inclined surfaces (Fig. 5). The element, illustrated in this figure, is located between the neutral and exit planes because the frictional stress τ is acting against the direction of metal flow. When this element is located between the entrance and neutral planes, τ acts in the direction of metal flow. The stress distribution within this element can be obtained by use of the slab method, as applied to plane-strain upsetting (Ref 14):

$$\sigma_z = \frac{K_2}{K_1} \ln \left(\frac{h_1}{h_0 + K_1 X} \right) + \sigma_{z1} \quad \textbf{(Eq 6)}$$

where

$$K_1 = -2 \tan \alpha \quad \textbf{(Eq 7)}$$

$$K_2 = -\frac{2\bar{\sigma}K_1}{\sqrt{3}} + 2\tau(1 + \tan^2 \alpha) \quad \textbf{(Eq 8)}$$

$$\tau = m\bar{\sigma}/\sqrt{3} \quad \textbf{(Eq 9)}$$

Following Fig. 5, for $x = \Delta x$, $h_0 + K_1x = h_1$, and therefore Eq 6 gives $\sigma_z = \sigma_{z1}$, the boundary condition at $x = \Delta x$, which is known. For $x = 0$:

$$\sigma_z = \sigma_{z0} = \frac{K_2}{K_1} \ln\left(\frac{h_1}{h_0}\right) + \sigma_{z1}$$

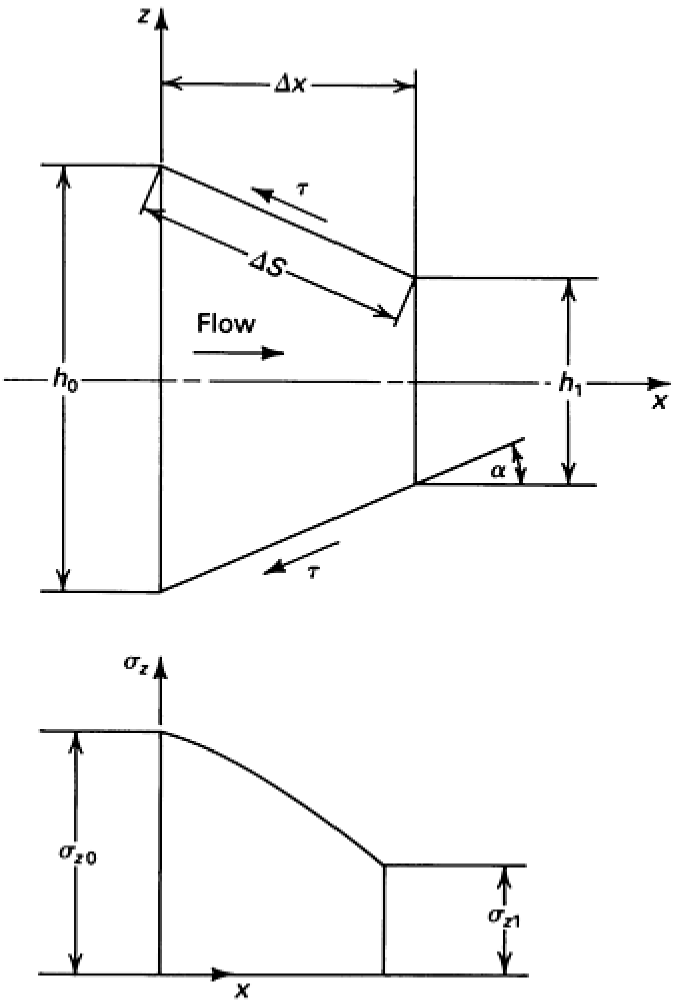

Fig. 5 Stresses in a deformation element used in computerized calculation of rolling stresses

If the element shown in Fig. 5 is located between the entrance and neutral planes, then the sign for the frictional shear stress τ must be reversed. Thus, Eq 6 and 7 are still valid, but:

$$K_2 = -\frac{2\bar{\sigma}}{\sqrt{3}} K_1 - 2\tau(1 + \tan^2 \alpha) \quad \textbf{(Eq 10)}$$

In this case, the value of the boundary condition at $x = 0$, that is, σ_{z0}, is known, and σ_{z1}, can be determined from Eq 6:

$$\sigma_{z1} = \sigma_{z0} - \frac{K_2}{K_1} \ln\left(\frac{h_1}{h_0 + K_1\Delta x}\right) \quad \textbf{(Eq 11)}$$

The stress boundary conditions at exit and entrance are known. Thus, to calculate the complete stress (roll pressure) distribution and to determine the location of the neutral plane, the length of the deformation zone X_D (see Fig. 3 and 4) is divided into n deformation elements (Fig. 6). Each element is approximated by flat top and bottom surfaces (Fig. 5). Starting from both ends of the deformation zone, that is, entrance and exit planes, the stresses are calculated for each element successively from one element to the next. The calculations are carried out simultaneously for both sides of the neutral plane. The location of the neutral plane is the location at which the stresses, calculated progressively from both exit and entrance sides, are equal. This procedure has been computerized and extensively used in cold and hot rolling of sheet, plane-strain forging of turbine blades (Ref 15) and in rolling of plates and airfoil shapes (Ref 16, 17).

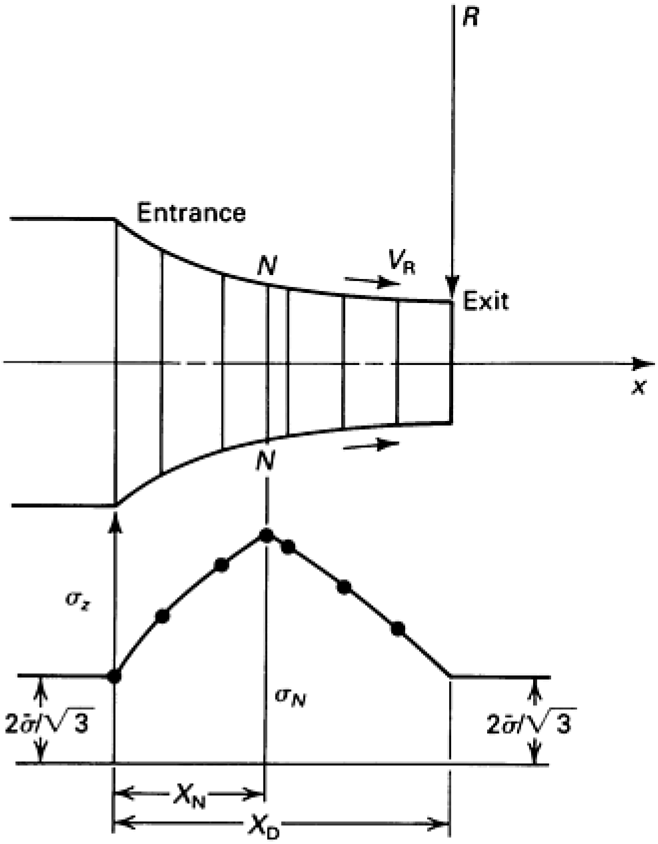

Fig. 6 Calculation of stress distribution by dividing the deformation zone into a number of tapered elements. In this case, tensile stresses in the strip are zero at both entrance and exit.

Roll-Separating Force and Torque. The integration of the stress distribution over the length of the deformation zone gives the total roll-separating force per unit width in strip rolling. In addition, the total torque is given by:

$$T = \int_0^{X_D} R\, dF \qquad \textbf{(Eq 12)}$$

where X_D is the length of the deformation zone (Fig. 6), R is roll radius, and F is the tangential force acting on the roll. Assuming that all energy is transmitted from the roll to the workpiece by frictional force:

$$dF = \tau\, dS \qquad \textbf{(Eq 13)}$$

In Fig. 5, it can be seen that:

$$dS = dx/\cos\alpha = \sqrt{1+\tan^2\alpha}\, dx \qquad \textbf{(Eq 14)}$$

In the deformation zone, the frictional force is in the rolling direction between entry and neutral planes. It changes direction between the neutral and exit planes. Thus, the total roll torque per unit width is:

$$T = R\tau \left[\int_0^{X_N} (1 + \tan^2 \alpha) dx - \int_{X_N}^{X_D} (1 + \tan^2 \alpha) dx \right] \quad \textbf{(Eq 15)}$$

where τ equals $m\bar{\sigma}/\sqrt{3}$; *R* is roll radius; α is roll angle (Fig. 3); X_N is the *x* distance of the neutral plane from the entrance (Fig. 6); and X_D is the length of the deformation zone (Fig. 6).

Elastic Deflection of Rolls. During rolling of strip, especially at room temperature, a considerable amount of roll deflection and flattening may take place. In the width direction, the rolls are bent between the roll bearings, and a certain amount of crowning, or thickening of the strip, occurs at the center. This can be corrected by either grinding the rolls to a larger diameter at the center or by using backup rolls.

In the thickness direction, roll flattening causes the roll radius to "enlarge," increasing the contact length. There are several numerical methods for calculating the elastic deformation of the rolls (Ref 9). A method for approximate correction of the force and torque calculations for roll flattening entails replacement of the original roll radius *R* with a larger value *R*'. A value of *R*' is suggested by Hitchcock (Ref 18) and is referred to extensively in the literature (Ref 2, 4). This is given as:

$$R' = R \left[1 + \frac{16(1 - \nu^2)p}{\pi E(H_0 - H_1)} \right] \quad \textbf{(Eq 16)}$$

where ν is Poisson's ratio of the roll material, *p* is the average roll pressure, and *E* is the elastic modulus of the roll material.

It is obvious that *R*' and *p* influence each other. Therefore, a computerized iteration procedure is necessary for consideration of roll flattening in calculating rolling force or pressure. Thus, the value of *p* is calculated for the nominal roll radius *R*. Then *R*' is calculated from Eq 16. If $R'/R \neq 1$, the calculation of *p* is repeated with the new *R*' value, and so on, until *R*'/*R* has approximately the value of 1.

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Mechanics of Plate Rolling

In rolling of thick plates, metal flow occurs in three dimensions. The rolled material is elongated in the rolling direction as well as spread in the lateral or width direction. Spread in rolling is usually defined as the increase in width of a plate or slab expressed as a percentage of its original width. The spread increases with increasing reduction and interface friction, decreasing plate width-to-thickness ratio, and increasing roll-diameter-to-plate thickness ratio. In addition, the free edges tend to bulge with increasing reduction and interface friction. The three-dimensional metal flow that occurs in plate rolling is difficult to analyze. Therefore, most studies of this process have been experimental in nature, and several empirical formulas have been established for estimating spread (Ref 19, 20, 21). Recently, attempts were also made to predict elongation or spread theoretically (Ref 22, 23, 24). Once the spread has been estimated, the elongation can be determined from the volume constancy, or vice versa.

An Empirical Method for Estimating Spread. Among the various formulas available for predicting spread, Wusatowski's formula (Ref 20) is used most extensively and is given as:

$$W_1/W_0 = abcd(H_0/H_1)^P \quad \textbf{(Eq 17)}$$

where W_1 and W_0 are the final and initial widths of the plate, respectively; H_1 and H_0 are the final and initial thicknesses of the plate, respectively; P equals $10^{(-1.269)}\ (W_0/H_0)(H_0/D)^{0.556}$; D is the effective roll diameter; and $a, b, c,$ and d are constants that allow for variations in steel composition, rolling temperature, rolling speed and roll material, respectively. These constants vary slightly from unity, and their values can be obtained from the literature (Ref 16, 20, 24).

An empirical formula for predicting spread such as Eq 17 gives reasonable results within the range of conditions for the experiments from which the formula was developed. There is no formula that will make accurate predictions for all the conditions that exist in rolling. Thus, it is often necessary to attempt to estimate spread or elongation by theoretical means.

The theoretical prediction of spread involves a rather complex analysis and requires the use of computerized techniques (Ref 16, 22, 23). A modular upper-bound method has been used to predict metal flow, spread, elongation and roll torque (Ref 16). The principles of this method are described below. Figure 7 illustrates the coordinate system, the division of the deformation zone into elements, and the notations used. The spread profile is defined in terms of a third-order polynomial $w(x)$ with two unknown coefficients a_1 and a_2. The location of the neutral plane x_n is another unknown quantity. The following kinematically admissible velocity field, initially suggested by Hill (Ref 25), is used:

$$V_x = 1/[w(x)h(x)] \quad \textbf{(Eq 18)}$$

$$V_y = \frac{1}{h(x)} \frac{d}{dx}\left[\frac{1}{w(x)}\right] \quad \textbf{(Eq 19)}$$

$$V_z = \frac{1}{w(x)} \frac{d}{dx}\left[\frac{1}{h(x)}\right] \quad \textbf{(Eq 20)}$$

Using Eq 18, 19, and 20, the upper-bound method can be applied to predict spread. A computer program, SHPROL, can be used for some steps in the analysis. More information on SHPROL is available in the section "Shape Rolling" in this article.

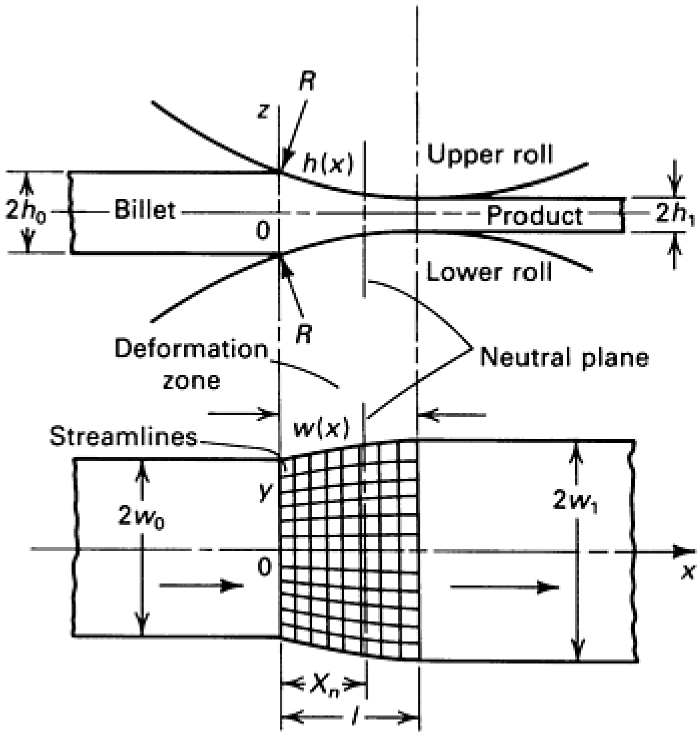

Fig. 7 Configuration of deformation and the grid system used in the analysis of the rolling of thick plates. Source: Ref 16

Prediction of Stresses and Roll-Separating Force. Once the spread (the boundaries of the deformation zone) has been calculated, this information can be used to predict the stresses and the roll-separating force. The computerized procedure used here is in principle the same as the method discussed earlier for predicting the stresses in strip rolling (Ref 16).

The deformation zone under the rolls is divided into trapezoidal slabs by planes normal to the rolling direction and along the stream tubes, as illustrated in Fig. 5 and 8. The stresses acting on strips in the rolling and transverse directions are illustrated in Fig. 8(b) and 8(c), respectively. As expected from the slab analysis, the stress distributions are very similar to those illustrated for strip rolling in Fig. 4, 5, and 6. By use of a numerical approach similar to that discussed for strip rolling, detailed predictions of stresses, in both the longitudinal and lateral directions, can be made. The stresses are calculated by assuming the frictional shear stress τ to be constant, as in the case of upper-bound analysis. Thus, the stress distribution at various planes along the width, or y, direction (Fig. 8) is linear on both sides of the plane of symmetry. The stress distribution in the rolling, or x, direction is calculated along the streamlines of metal flow (Fig. 7). At each node of the mesh, the lower of the σ_z values is accepted as the actual stress. Thus, a tentlike stress distribution is obtained (Fig. 9). Integration of the stresses acting on the plane of contact gives the roll-separating force.

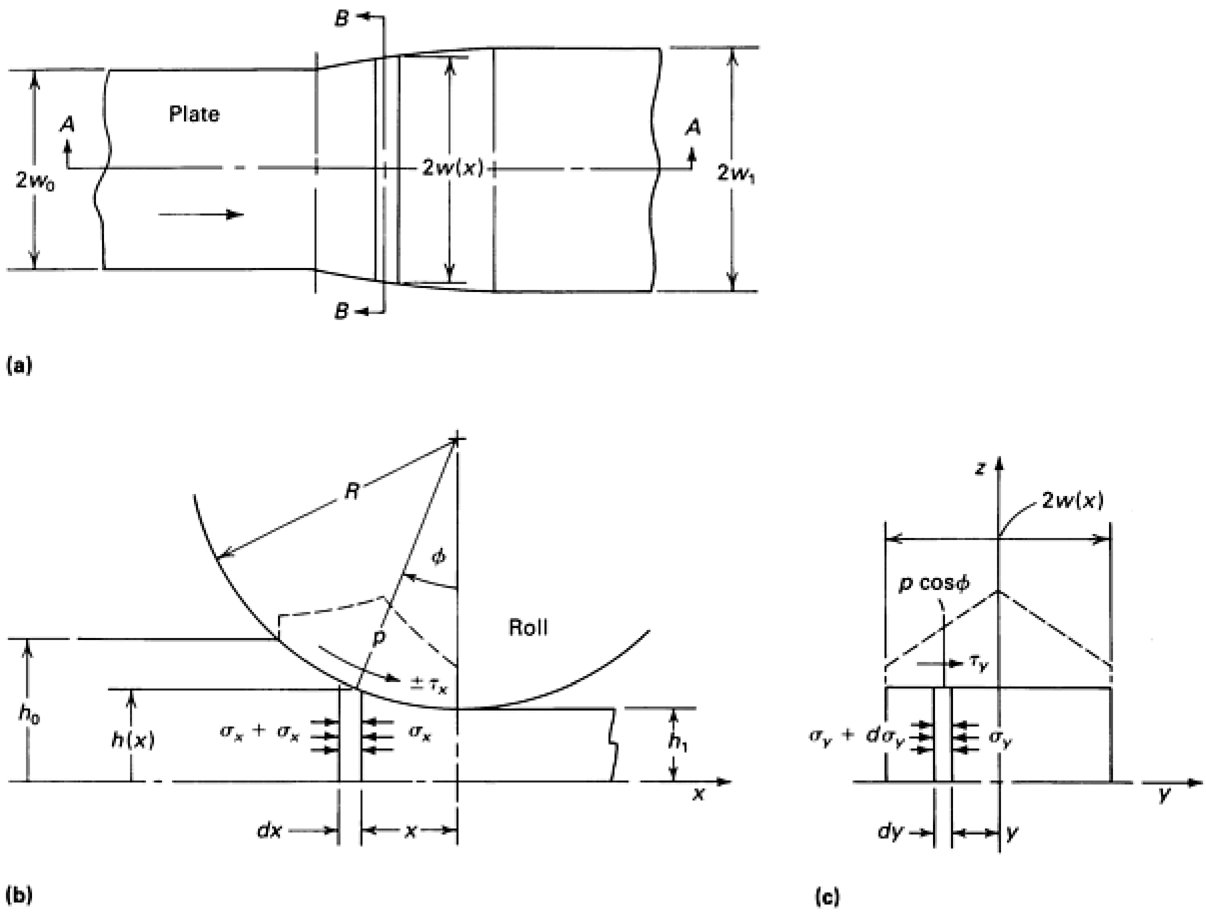

Fig. 8 Stress analysis of the rolling of plates. (a) Top view of the rolled plate. (b) Stresses in the rolling direction. (c) Stresses in the transverse direction. Source: Ref 16

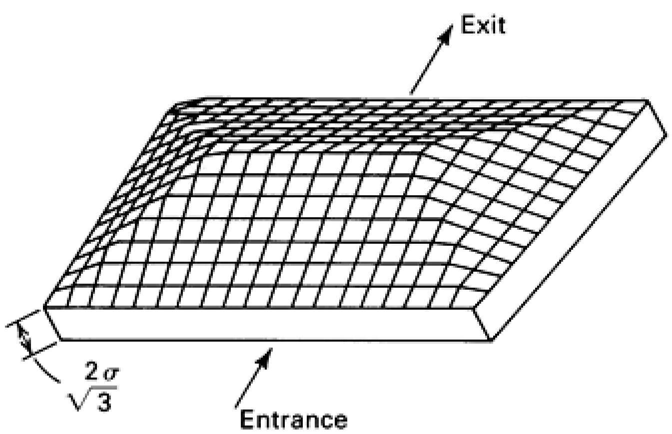

Fig. 9 The calculated stress (σ_z) distribution in plate rolling shown three-dimensionally. Source: Ref 16.

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Flat, Bar, and Shape Rolling

G. D. Lahoti, The Timken Company; S.L. Semiatin, Battelle Columbus Division

Shape Rolling

Rolling of shapes, also called caliber rolling, is one of the most complex deformation processes. A round or round-cornered square bar, billet, or slab is rolled in several passes into relatively simple sections such as rounds, squares, or rectangles; or complex sections such as L, U, T, H, or other irregular shapes (Ref 26). For this purpose certain intermediate shapes or passes are used, as shown in Fig. 10 for the rolling of angle sections (Ref 27). The design of these intermediate shapes, that is, roll pass design, is based on experience and differs from one company to another, even for the same final rolled section geometry. Relatively few quantitative data on roll pass design are available in the literature. Good summaries of references on this subject are given in several books (Ref 24, 26, 28, 29, 30, 31) and in a few recent articles (Ref 32, 33).

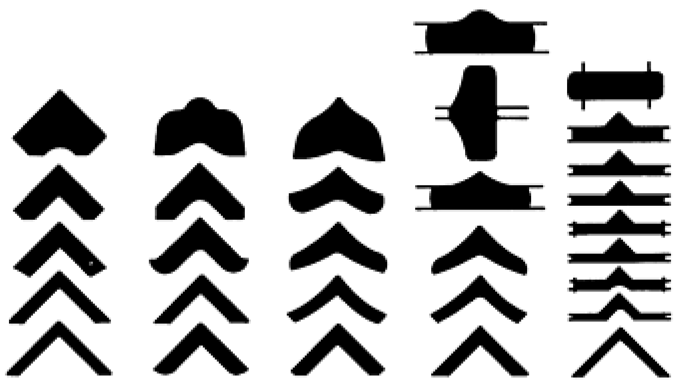


Fig. 10 Five possible roll pass designs for the rolling of a steel angle section. Source: Ref 27.

Basically, there are two methods for rolling shapes or sections. The first method is universal rolling (Fig. 11). The second method is caliber rolling (Fig. 10, 12). In universal rolling, the mill and stand constructions are more complex. However, in the rolling of I-beams or other similar sections, this method allows more flexibility than does caliber rolling and requires fewer passes. This is achieved because this method provides appropriate amounts of reductions, separately in webs and flanges.

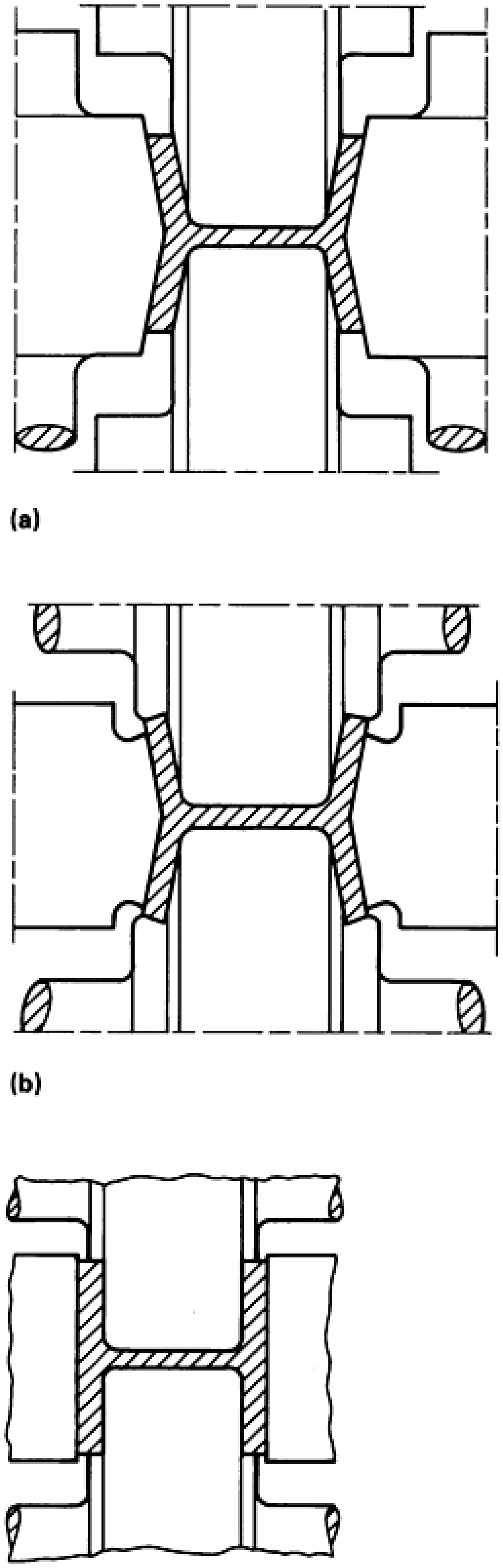


Fig. 11 Universal rolling of flanged beams. (a) Universal roll stand. (b) Edging stand. (c) Finishing stand. Source: Ref 34

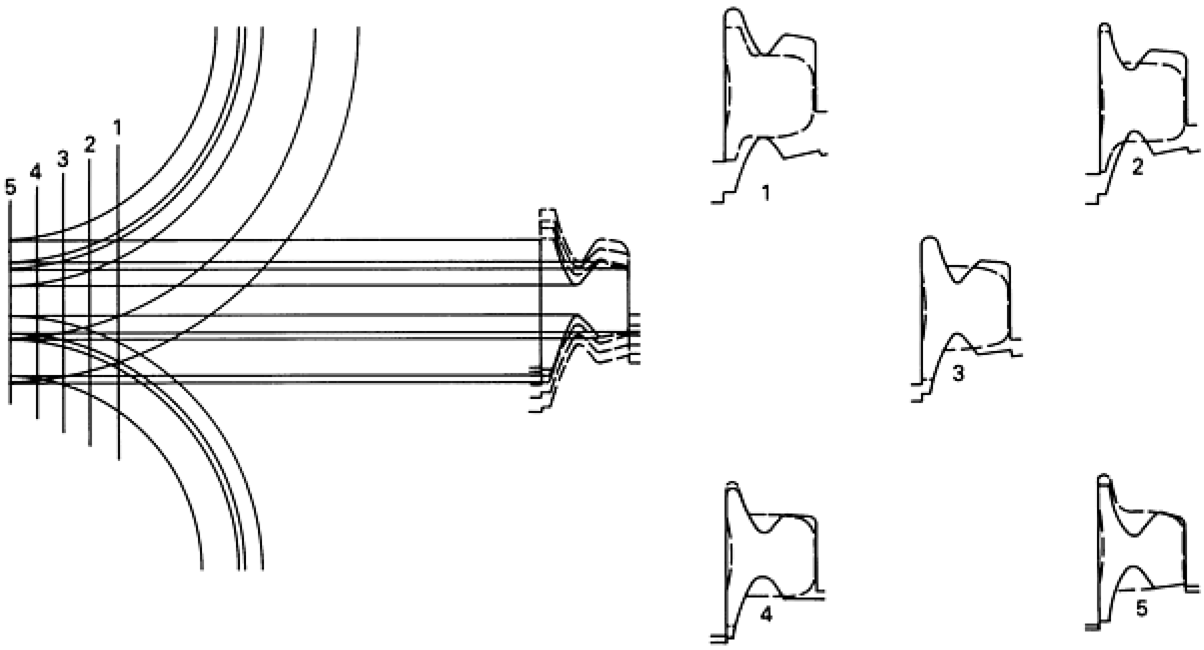
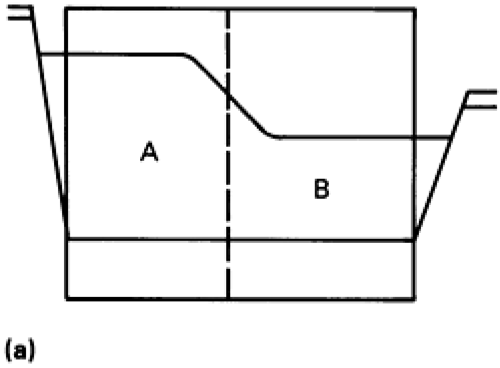
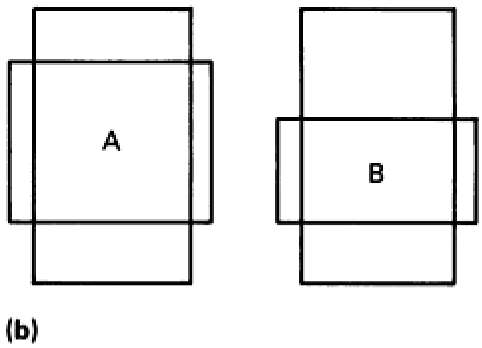


Fig. 12 Analysis of a roll stand used in rolling of rails. Sketches 1 through 5 illustrate the stock in broken lines and the roll in solid lines at various positions in the deformation zone. Source: Ref 35

For successful rolling of shapes, it is necessary to estimate for each stand: the roll separating force and torque, the spread and elongation, and the appropriate geometry of the roll cavity or caliber. The force and torque can be estimated either by using empirical formulas or by approximating the deformation in shape rolling with that occurring in an "equivalent" plate rolling operation. In this case, the "equivalent" plate has initial and final thicknesses that correspond to the average initial and final thicknesses of the rolled section. The load and torque calculations can be performed for the "equivalent" plate, as discussed earlier in this chapter for plate rolling. The results are approximately valid for the rolled shape being considered.

Estimation of Elongation. During the rolling of a given shape or section, the cross section is not deformed uniformly, as can easily be seen in Fig. 12. This is illustrated further in Fig. 13 for a relatively simple shape. The reductions in height for zones A and B are not equal (Fig. 13a). Consequently, if these two zones, A and B, were completely independent of each other (Fig. 13b), zone B would be much more elongated than zone A. However, the two zones are connected and, as part of the rolled shape, must have equal elongation at the exit from the rolls. Therefore, during rolling, metal must flow from zone B into zone A so that a uniform elongation of the overall cross section is obtained (Fig. 13c). This lateral flow is influenced by the temperature differences that exist in the cross section because of variations in material thickness and heat flow.





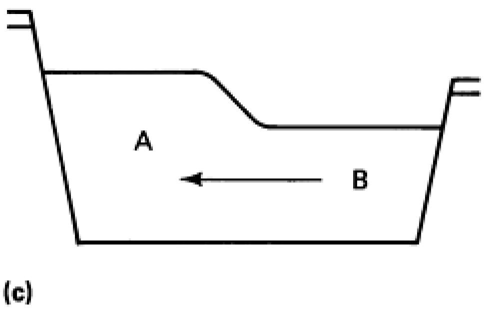


Fig. 13 Nonuniform deformation in the rolling of a shape. (a) Initial and final sections. (b) Two zones of the section considered as separate plates. (c) Direction of lateral metal flow. Source: Ref 24

To estimate the overall elongation, it is necessary to divide the initial section into a number of "equivalent" plates (A, B, C, and so forth), as shown in Fig. 13. The elongation for an individual section, without the combined influence of other portions of the section, can be estimated by using both the plate-rolling analogy and the techniques discussed in this article. The combined effect can be calculated by taking a "weighted average" of the individual elongations. For example, if the original section is to be divided into an equivalent system consisting or two plate sections (A and B in Fig. 13), with individual cross-sectional areas A_a and A_b, then the following weighted-average formula can be used:

$$\lambda_m = \frac{A_0}{A_1} = \frac{A_{a0} + A_{b0}}{A_{a1} + A_{b1}} = \frac{A_{a1}\lambda_a + A_{b1}\lambda_b}{A_{a1} + A_{b1}} \quad \textbf{(Eq 21)}$$

where λ is the elongation coefficient (that is, the cross-sectional area at the entrance divided by the cross-sectional area at the exit); *A* is the cross-sectional area; m is a subscript denoting average; a and b are subscripts denoting section portions A and B; and 0 and 1 are subscripts denoting entrance and exit values, respectively.

Computer-Aided Roll Pass Design. Estimation of the number of passes and of the roll geometry for each pass is the most difficult aspect of shape rolling. Ideally, to accomplish this, certain factors, discussed below, must be considered.

The Characteristics of the Available Installation. These include diameters and lengths of the rolls, bar dimensions, distance between roll stands, distance from the last stand to the shear, and tolerances that are required and that can be maintained.

The reduction per pass must be adjusted so that the installation is used at a maximum capacity, the roll stands are not overloaded, and roll wear is minimized. The maximum value of the reduction per pass is limited by the excessive lateral metal flow, which results in edge cracking; the power and load capacity of the roll stand; the requirement for the rolls to bite in the incoming bar; roll wear; and tolerance requirements.

At the present stage of technology, the above factors are considered in roll pass design by using a combination of empirical knowledge, some calculations, and some educated guesses. A methodical way of designing roll passes requires not only an estimate of the average elongation, as discussed earlier, but also the variation of this elongation within the deformation zone. The deformation zone is limited by the entrance, where a prerolled shape enters the rolls, and by the exit, where the rolled shape leaves the rolls. This is illustrated in Fig. 12. The deformation zone is cross sectioned with several planes (for example, planes 1 to 5 in Fig. 12; 1 is at the entrance, 5 is at the exit). The roll position and the deformation of the incoming billet are investigated at each of these planes. Thus, a more detailed analysis of metal flow and an improved method for designing the configuration of the rolls are possible. It is evident that this process can be drastically improved and made extremely efficient by the use of computer-aided techniques.

In recent years, most companies that produce shapes have computerized their roll pass design procedures for rolling rounds (Ref 33, 36, 37, 38, 39, 40) or structural shapes (Ref 36, 40, 41, 42, 43). In most of these applications, the elongation per pass and the distribution of the elongation within the deformation zone for each pass are predicted by using an empirical formula. If the elongation per pass is known, it is then possible, by use of computer graphics, to calculate the cross-sectional area of a section for a given pass, that is, the reduction and the roll geometry. The roll geometry can be expressed parametrically (in terms of angles, radii, and so forth). These geometric parameters can then be varied to optimize the area reduction per pass and obtain an acceptable degree of fill of the roll caliber used for that pass.

Computer-Aided Roll Pass Design of Airfoil Sections. To analyze metal flow and predict force and torque in the rolling of airfoils, two computer programs have been developed in a recent study (Ref 17). The first of these programs, SHPROL, uses upper bound analysis in a numerical form to predict spread and roll torque. SHPROL is based on the following simplifying assumptions:

- The initial contact between the rolls and the entrance section can be approximated as a straight line. (This is only correct if the upper and lower surfaces of the initial section already have the shape of the rolls.)
- An airfoil shape can be considered as an aggregate of slabs, as shown in Fig. 14.
- Plane sections perpendicular to the rolling direction remain plane during rolling. Thus, the axial velocity (velocity in the rolling, or *x*, direction) at any section perpendicular to the rolling direction is uniform over the entire cross section.
- The velocity components in the transverse, or *y*, direction and in the thickness, or *z*, direction are functions of *x* and linear in the *y* and *z* coordinates, respectively.

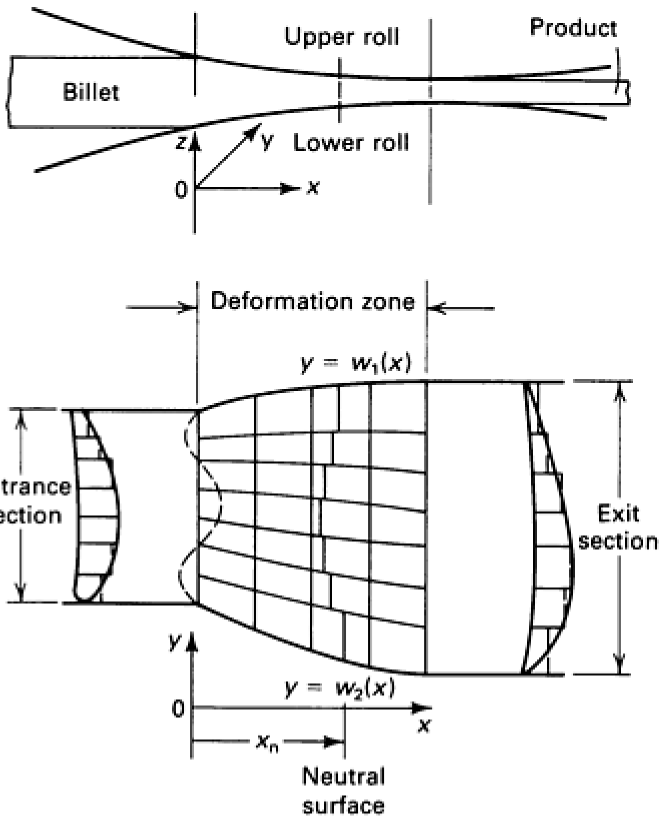


Fig. 14 Configuration of deformation zone in the application of numerical upper-bound analysis to the rolling of airfoil shapes. Source: Ref 17

In Fig. 14 each element is considered to be a plate for which it is possible to derive a kinematically admissible velocity field. The total energy dissipation rate of the process $\dot{E}_T$ is:

$$\dot{E}_T = \dot{E}_P + \dot{E}_D + \dot{E}_F \quad \textbf{(Eq 22)}$$

where $\dot{E}_P$ is the energy rate of plastic deformation, and is calculated for each element by integrating the product of flow stress and the strain rate over the element volume; $\dot{E}_D$ represents the energy rates associated with velocity discontinuities and is due to internal shear between the elements; and $\dot{E}_F$ is the energy rate due to friction between the rolls and the deforming material.

The total energy dissipation rate $\dot{E}_T$ is a function of unknown spread profiles w_1 and w_2 (Fig. 14) and the location of the neutral plane x_n. As in the analysis discussed earlier for plate rolling, the unknown coefficients of w_1, w_2, and x_n are determined by minimizing the total energy rate.

The computer program SHPROL uses as input data: roll and incoming-shape geometry, friction, flow stress, and roll speed. SHPROL can predict the energy dissipation rates, the roll torque, and, most important, the amounts of elongation and spread within one deformation zone, in the rolling of any airfoil shape.

The second program, called ROLPAS, uses interactive graphics and is capable of simulating the metal flow in the rolling of relatively simple shapes, such as rounds, plates, ovals, and airfoils (Fig. 15). ROLPAS uses as input: the geometry of the initial section, the geometry of the final section, the flow stress of the rolled material and the friction factor, and the variations in elongation and spread in the rolling direction, as calculated by the SHPROL program.

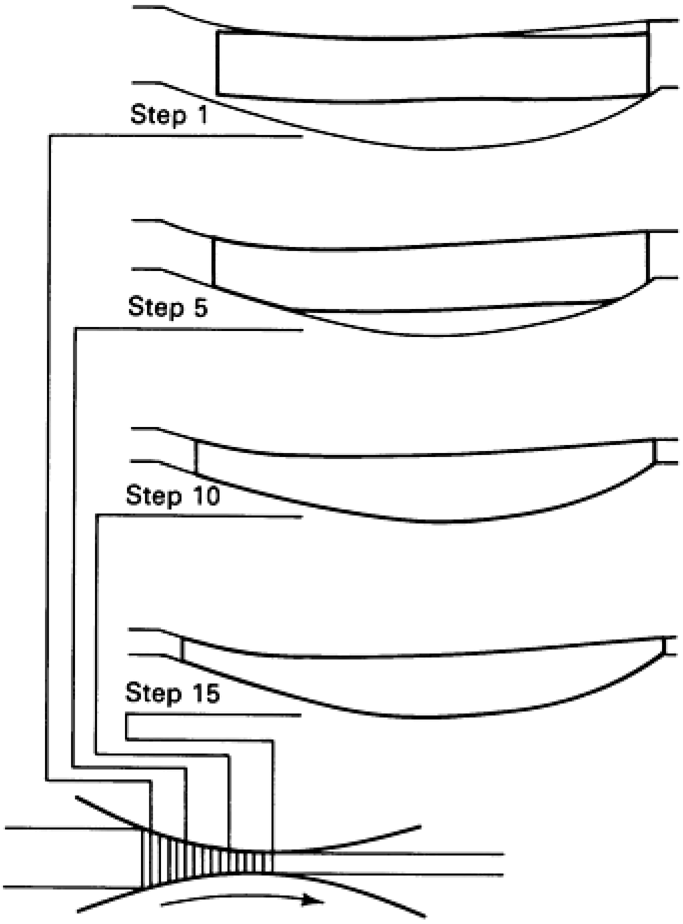


Fig. 15 Selected simulation steps as displayed by ROLPAS for a test airfoil shape cold rolled from rectangular steel stock

To simulate the rolling process, ROLPAS divides the deformation zone into a number of cross sections parallel to the roll axis (Fig. 7, 15). The simulation is initiated by considering the cross-sectional area, stresses present, and the roll-separating force and torque for the first section. These same analyses can then be performed on any succeeding section.

Computer-Aided Roll Pass Design for Round Sections. Several computer-aided methods for designing caliber rolls for rod rolling have been discussed in the literature (Ref 33, 36, 37, 38, 39, 40). One of these methods is a computer program called RPDROD for establishing roll cross sections and pass schedules interacting with a graphics terminal (Ref 33). RPDROD uses an empirical formula for estimating the variation of the spread in the roll bite and parametrically described alternative roll caliber designs. When using this program, the designer obtains an optimum roll pass schedule by evaluating a number of alternatives in which individual pass designs are selected from a variety of caliber shapes commonly used in rod rolling.

The computer program RPDROD consists of four modules, called STOCK, SCHEDULE, GROOVE, and METAL FLOW. The STOCK design module allows the user to design/specify the entry cross section for the first pass in the schedule. A square, rectangular, or round stock cross section can be defined. The SCHEDULE design module allows the user to design the roll pass schedule by providing specific functions:

- Add a new pass to the roll pass schedule, by estimating alternative roll cross section dimensions from design data provided by spread/elongation calculations
- Delete pass design data from the schedule in order to investigate alternative pass designs
- Review and/or provide hard copy of existing pass design data

The SCHEDULE design module allows the user to design an optimum roll pass schedule by investigating various alternative pass design and/or shape combinations. In principle, any roll cross section shape considered by the program could be used for a given pass in the schedule. However, RPDROD has facilities for checking input data and thus for preventing the selection of an illogical pass design or the inappropriate selection of roll cross-sectional shape combinations.

The GROOVE design module can be used to change the initially suggested roll cross section dimensions, as the user deems appropriate. As in the SCHEDULE module, input checking facilities ensure that specified roll cross-sectional dimensions are consistent with the chosen roll cross-sectional shape and bar entry cross section.

The METAL FLOW design module provides the user with details of metal flow simulation, including the calculated cross sections of the deforming bar in the roll bite, stresses in the deforming material, roll separating load, and roll torque. For this purpose, this module uses the ROLPAS program discussed earlier for the rolling of airfoil shapes.

As an example, a pass schedule calculated with RPDROD is given in Table 1. Comparison of these results with laboratory experiments indicated that these predictions were reasonably accurate, and RPDROD can be used for practical roll pass design for rolling of round sections (Ref 33).

Table 1 Summary of pass schedule information for the laboratory experiments as simulated by RPDROD

Pass number	**Groove shape**	**Rotation angle, degrees**	**Exit area, in.^{2}**	**Exit speed, ft/min**	**Reduction in area, %**	**Area fill, %**	**Roll force, tonf**	**Horse-power**	Roll speed, rpm
Stock	Square	0.0	1.559	60.0					
1	Square	45.0	1.394	55.6	10.6	93.2	5.3	1.3	**30.0**
2	Square	90.0	1.171	57.5	16.0	96.1	16.3	6.3	**30.0**
3	Square	90.0	1.042	57.7	11.0	95.7	8.7	2.5	**30.0**
4	Oval	45.0	0.873	59.4	16.2	98.8	14.1	6.2	**30.0**
5	Round	90.0	0.780	58.1	10.6	99.3	5.9	1.9	**30.0**
6	Oval	90.0	0.675	58.3	13.5	101.1	9.1	2.9	**30.0**
7	**Round**	**90.0**	**0.595**	**59.1**	**11.8**	**98.9**	**4.8**	**1.4**	**30.0**

Computer-Aided Roll Pass Design of Structural and Irregular Sections. Computer graphics is being used by many companies for the design and manufacture of the caliber shapes for the rolling of structural sections (Ref 36, 40, 41, 42, 43). A publication on this subject gives an excellent summary of the practical use of computer graphics for roll caliber and roll pass design (Ref 43). In this case, the cross section of a rolled shape is described in general form as a polygon. Each corner or fillet point of the polygon is identified with the *x* and *y* coordinates and with the value of the corresponding radius (Fig. 16). Thus, any rolled section can be represented by a sequence of lines and circles. This method of describing a rolled section is very general and can define a large number of sections with a single computer program. Lines or circles that are irrelevant in a specific case can be set equal to zero. Thus, a simpler section, with a smaller number of corner and fillet points, can be obtained. For example, in the rolling of the symmetric-angle section shown in Fig. 10, several intermediate section passes are required. Such an intermediate section is shown in parametric representation in Fig. 17. In this figure, all the geometric variables can be modified to change the cross-sectional area and/or the amount of reduction per pass. These variables, which fully describe this section, are SELA = length (of one leg) at centerline, BETAG = angle at top corner, RK = radius at top corner, AL = length of straight portion at top, RD = radius of leg at top, PRST = projection of draft angle, RRU = radius at lower tip of leg, and RH = radius at bottom corner.

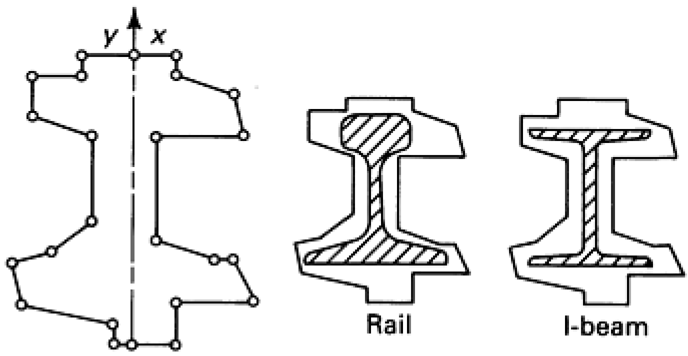


Fig. 16 Geometric representation of a rolled section as a polygon. Source: Ref 43

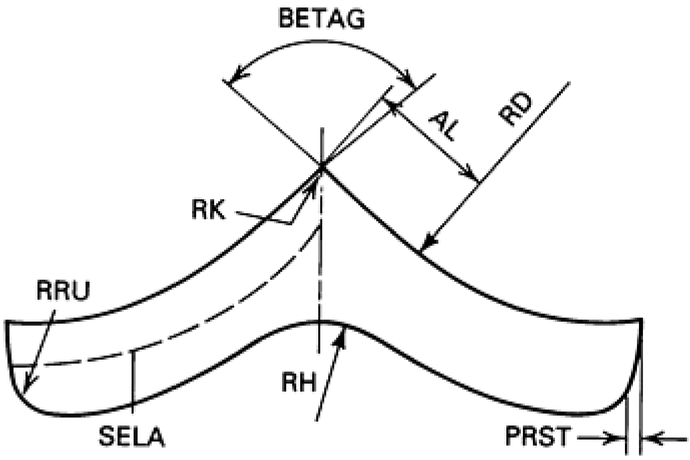


Fig. 17 Parametric representation of an intermediate-shape rolling pass for a symmetric angle section. See also Fig. 18. Source: Ref 43

In establishing the final section geometry, the designer assigns desired values to the variables listed above and, in addition, inputs the desired cross-sectional area and the degree of caliber fill, for example, the desired ratio of rolled section area versus section area on the caliber rolls. Thus, there is only one geometric variable that is calculated by the computer program, and that is the thickness of the leg of the angle section. In the example shown in Fig. 18(a), the leg thickness is calculated to be 18.2 mm (0.72 in.). The designer compares this section geometry (Fig. 18a) with the caliber geometry of the next pass that has been generated in a similar way. Let us assume that the section shown in Fig. 18(a) appears to be too long, that is, SELA is 67.5 mm (2.66 in.) and should be reduced to 65 mm (2.56 in.) without modifying the other variables. The interactive program is rerun with the new value for SELA. The modified section, shown in Fig. 18(b), is slightly thicker than the original section in order to maintain the same cross-sectional area.

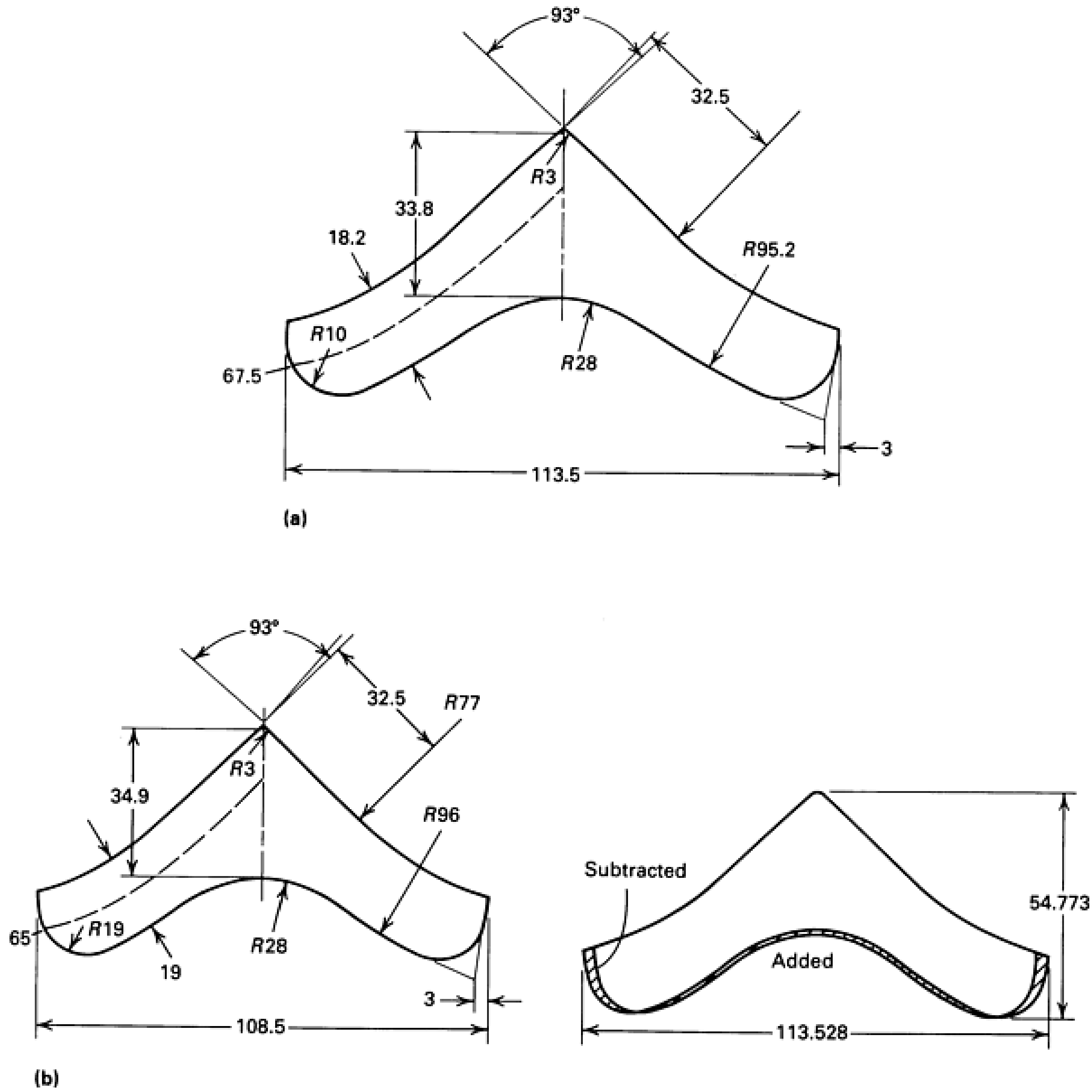


Fig. 18 (a) Alternative design for an intermediate-angle pass obtained by assigning values to the parameters given in Fig. 17. (b) Modified design of the intermediate-angle pass shown in (a), with new dimensions (left) and added and subtracted areas (right). Source: Ref 43

This interactive graphics program does not involve any analysis of metal flow or stresses. Nevertheless, it is extremely useful to the designer for modifying section geometries quickly and accurately, calculating cross-sectional areas, and cataloging all this geometrical information systematically. The program also automatically prepares engineering drawings of the sections and the templates for quality control as well as tapes for numerically controlled milling of the templates and the graphite EDM electrodes used in manufacturing the necessary cutting tools for roll machining (Ref 43).

Finite-element modeling has been used in the analysis of three-dimensional metal flow and of stress and strain distributions in shape rolling (Fig. 19, 20). By this so-called FEM method, the complex shape under the roll gap is divided into cells or elements with simple three-dimensional shapes. Through the analysis of these elements one at a time, the deformation pattern in a complex shape can be determined. Such calculations take hours on most computers; however, as new generations of computer hardware that perform calculations at greater speed become available, the laborious task of designing rolls for complex shapes will be greatly simplified.

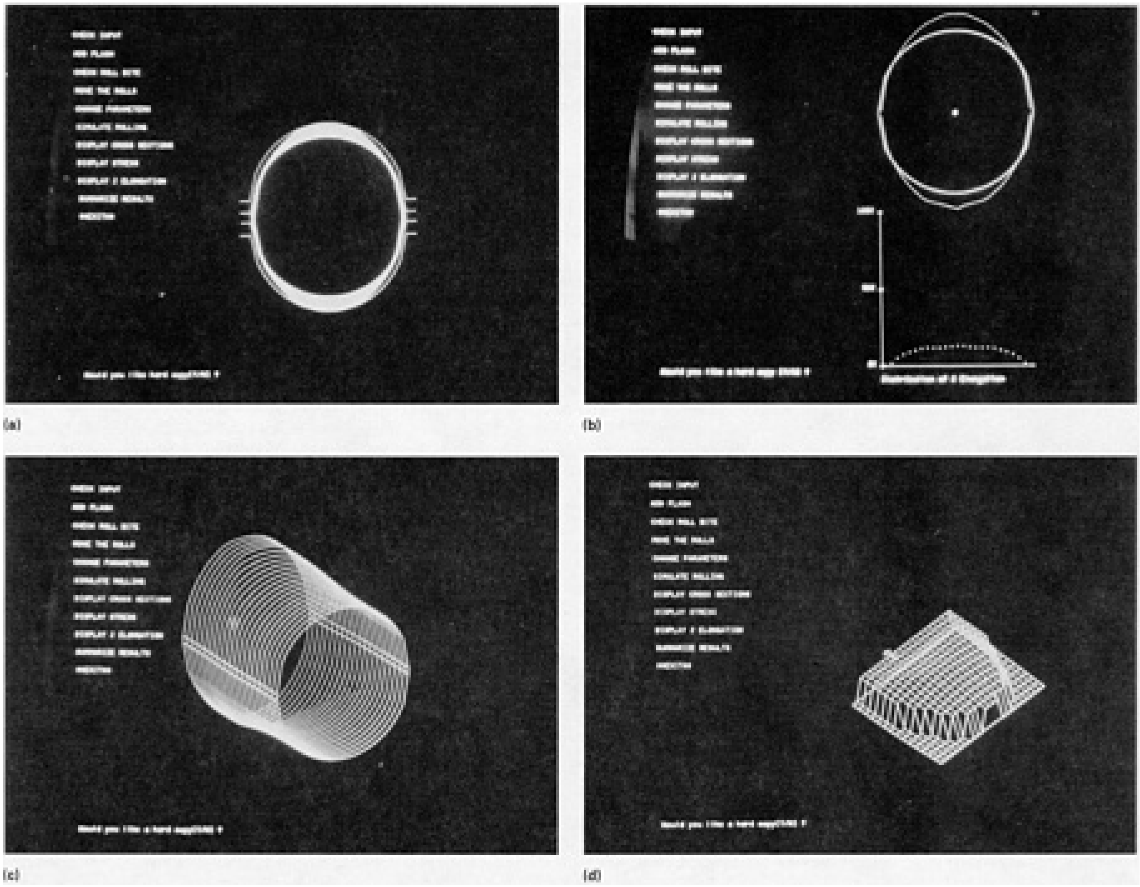


Fig. 19 Computer analysis of a bar-rolling pass. (a) Cross sections of the bar at various stages through the roll bite. (b) Distribution of elongation at the roll exit. (c) Three-dimensional view of the roll bite. (d) Pressure distribution along the arc of contact. Courtesy of K.F. Kennedy

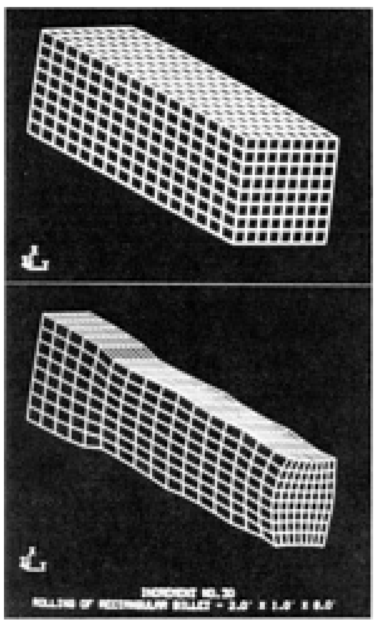


Fig. 20 Finite element modeling of metal flow in rolling. The square bloom is divided into numerous elements (top), and an analysis of a pass through a rolling stand is carried out. The bulging of the sides of the workpiece after a given reduction in height is shown at bottom. Courtesy of B. Kiefer, Bethlehem Steel Corporation

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Flat, Bar, and Shape Rolling

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Rolling Mills (Ref 44)

Mills are classified by descriptive dimensions that indicate the size of the mill, by the arrangement of roll stands, and by the type of product that is rolled. The dimensions used to indicate size vary depending on the type of mill and the product. (More information on classification and other aspects of rolling mills is available in Ref 44.)

However, there are three principal types of rolling mills, referred to as two-high, three-high, and four-high mills (Fig. 21). This classification, as the names indicate, is based on the way the rolls are arranged in the housings. A two-high stand consists of two rolls, one positioned directly above the other; a three-high mill has three rolls, and a four-high mill has four rolls, also arranged one on top of the other.

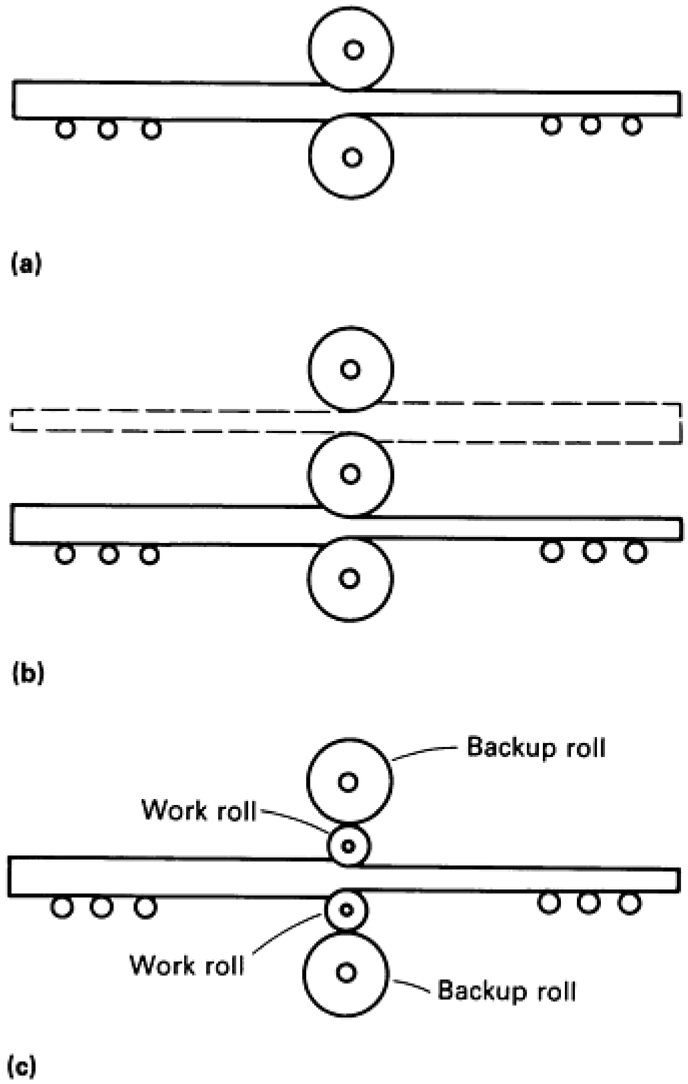

Fig. 21 The most common types of rolling mills. (a) Two-high. (b) Three-high. (c) Four-high

Two-high mills (Fig. 21a) may be either pull-over (drag-over) mills or reversing mills. In pull-over-type mills, the rolls run in only one direction. The workpiece must be returned over the top of the mill for further rolling, hence the name pull-over. Reversing mills employ rolls on which the direction of rotation can be reversed. Rolling then takes place alternately in two opposite directions. Reversing mills are among the most widely used in industry, and can be used to produce slabs, blooms, plates, billets, rounds, and partially formed sections suitable for rolling into finished shapes on other mills.

In three-high mills (Fig. 21b), the top and bottom rolls rotate in the same direction, while the middle roll rotates in the opposite direction. This allows the workpiece to be passed back and forth alternately through the top and middle rolls and then through the bottom and middle rolls without reversing the direction of roll rotation.

Four-high mills (Fig. 21c) are used for rolling flat material such as sheet and plate. This type of mill uses large backup rolls to reinforce smaller work rolls, thus obtaining fairly large reductions without excessive amounts of roll deflection. Four-high mills are used to produce wide plates and hot-rolled or cold-rolled sheet, as well as strip of uniform thickness.

Specialty Mills. Two other types of mills that are used are cluster mills and planetary mills. The most common type of cluster mill is the Sendzimir mill. In a typical Sendzimir mill design (Fig. 22a), each work roll is supported through its entire length by two rolls, which in turn are supported by three rolls. These rolls transfer roll-separating forces through four large backup rolls to a rigid, cast steel housing. Sendzimir mills are used for the cold rolling of sheet and foil to precise thicknesses.

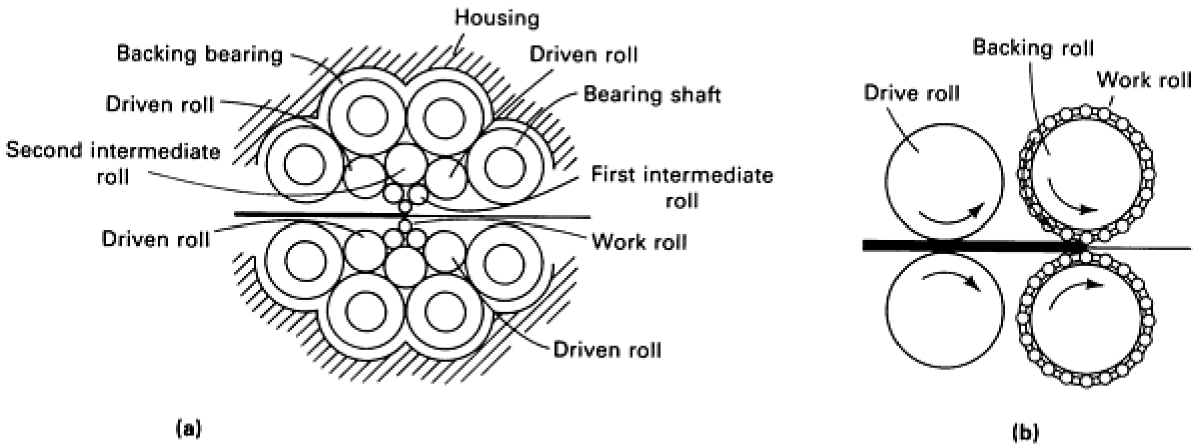

Fig. 22 Two types of specialty mills. (a) Sendzimir mill, used for precision cold rolling of thin sheet and foil. (b) Planetary mill, used to accomplish large reductions in a single pass

Planetary mills were developed in Germany to reduce slabs to hot-rolled strip in a single pass. This is accomplished by the use of two backup rolls surrounded by a number of small work rolls (Fig. 22b). Planetary mills are capable of reductions of up to 98% in a single pass, and have been designed up to 2030 mm (80 in.) in width.

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Flat, Bar, and Shape Rolling

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Rolls and Roll Materials

Of all the components of a rolling mill, the rolls are probably of primary interest, because they control the reduction and shaping of the work metal. There are three main parts of a roll: the body (the part on which the actual rolling takes place), the necks (which support the body and take the rolling pressure), and the driving ends, commonly known as wobblers (where the driving force is applied). These parts are shown in Fig. 23.

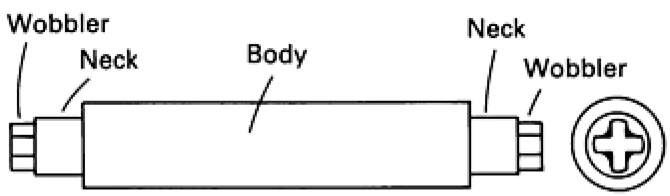

Fig. 23 Principal parts of a rolling mill roll

Rolls must have good wear resistance; sufficient strength to withstand the bending, torsional, and shearing stresses to which they are subjected; and, for hot rolling, ability to withstand elevated temperatures without heat checking (thermal fatigue) and oxidation.

Roll Design

Rolls are designed by engineering companies and builders of rolling mills, except for pass and groove designs on grooved rolls, which generally are engineered in the user's roll shop. The proportions of rolls are based on application and mill design. The width of the metal to be rolled, or the length of the billet where cross rolling is required, determines the width of the body face. Body diameter is selected to provide the required bite and pass angle to accomplish reduction and to provide sufficient mass to resist roll deflection and breakage. Rolls of smaller diameter result in less spread of the work metal and require less rolling pressure, separating force, and power for a given reduction. In designing rolls for shape rolling, deep grooves should be placed as far as possible from the center, in a location where the bending moment is at a minimum.

The size of a roll is generally designated by body diameter and body length, in that order; for example, a 600 × 1200 mm (24 × 48 in.) roll would have a body diameter of 600 mm and a body length of 1200 mm. For rolls used in processing shapes, the body diameter given is the nominal, or pitch, diameter.

Journal, or neck, dimensions are determined by imposed bending loads and by bearing design. The abrupt change in diameter from roll body to roll neck intensifies bending and torsional stresses at this location. To prevent breakage, the neck diameter should be as large a proportion of the body diameter as is feasible. Safe ratios of neck diameter to body diameter vary with type of bearing, type of mill, and conditions of service. In any event, neck diameter should never be smaller than 50% of body diameter. More information on roll design and manufacture is available in Ref 44.

Roll Materials

Cast iron rolls are used in the as-cast condition or after stress relief. Some high-alloy iron rolls are heat treated by holding at high temperature, then subjected to several lower-temperature treatments. Cast irons used for rolls are metastable and may be white or gray depending on composition, inoculation (if any), cooling rate, and other factors. Because of the number of elements present, determination of transformation diagrams is complicated.

Development of proper roll specifications to meet widely varying rolling requirements is an extremely complicated, technical undertaking; for example, when specifying radial hardness penetration, roll manufacturers must consider the requirements dictated by the design of each particular mill. Because of these factors, each roll must be more or less tailored for its intended use, and close cooperation between manufacturer and user is necessary to obtain maximum roll life and performance.

In American practice, cast iron rolls are classified as chilled iron rolls, grain rolls, sand iron rolls, ductile iron rolls, or composite rolls.

Chilled iron rolls (hardness 50 to 90 HSc) have a definitely formed, clear, homogeneous, chilled white iron body surface and a fairly sharp line of demarcation between the chilled surface and the gray iron interior portion of the body. Clear, chilled iron rolls can be made in unalloyed or alloyed grades, as shown in Table 2. The depth of chill is measured visually as the distance between the finished surface of the body and the depth at which the first graphitic specks appear. Below this, there is an area consisting of a mixture of white and gray iron known as mottle, which gradually becomes more gray and more graphitic, until it merges with the main gray iron structure of the roll interior.

Table 2 Applications of cast iron rolls

Type of roll	Applications
Chilled iron rolls	
Unalloyed	

(50-72 HSc)	**Hot and cold rolls for sheet mills, tin mills, two-high and three-high plate mills, and jobbing mills; wet and dry work rolls for four-high hot strip mills and for intermediate and finishing stands in rod, merchant, sheet, bar, and skelp mills**
Alloy iron (60-90 HSc)	**Hot rolls for sheet and strip mills in ferrous, nonferrous, rubber, plastic, and paper industries, two-high and three-high plate mills, and universal mills; work rolls for four-high hot strip mills and for finishing stands in sheet, bar, skelp, strip, and merchant mills; cold rolls for finishing ferrous and nonferrous sheet and strip**
Grain rolls (40-90 HSc)	
Mild hard	**Light-duty roughing rolls for small merchant and bar mills**
Medium hard	**Intermediate rolls for merchant and bar mills and for large structural mills**
Hard	**Finishing rolls for merchant, bar, and structural mills; flat finishing rolls for sheet, bar, and skelp mills; sizing, high-mill, reeler, and welding rolls for tube mills**
Sand iron rolls (35-45 HSc)	**Mild-duty rolls for roughing stands in small mills and finishing stands in large structural mills**
Ductile iron rolls (50-80 HSc)	**Roughing and intermediate rolls for bar and merchant mills and for tube mills and various other uses**

Alloy chilled iron rolls have hardnesses ranging from 60 to 90 HSc that are controlled by carbon and alloy contents. Customary maximum percentages of alloying elements are 1.25 Mo, 1.00 Cr, and 5.5 Ni. Many different combinations are used to produce desired properties. Rolls of this type, particularly in the harder grades, are used chiefly for rolling flat work, both hot and cold. The softer, machinable grades are used for rolling rod and small shapes.

Grain rolls are "indefinite chill" iron rolls (hardness 40 to 90 HSc) that have an outer chilled face on the body. There is finely divided graphite at the surface, which gradually increases in amount and in flake size, with a corresponding decrease in hardness, as distance from the surface increases. These rolls have high resistance to wear and good finishing qualities, to considerable depths. The harder grades are used for hot and cold finishing of flat rolled products, and the softer grades are for deep sections (even with small rolls). Alloying elements such as chromium, nickel, and molybdenum are usually added, either singly or in combination, to develop specific levels of hardness and toughness similar to those of chilled iron rolls.

Sand iron rolls (no chill; hardness 35 to 45 HSc) are cast in sand molds, in contrast to chilled iron rolls and grain rolls, the bodies of which are cast directly against chills. In a sand iron roll, the metal in the grooves of the body may be mildly hardened by use of cast iron ring inserts set in the sand mold. Sand iron rolls are used chiefly for intermediate and finishing stands on mills that roll large shapes. They are also used for roughing operations in primary mills.

Ductile iron rolls (hardness 50 to 65 HSc) are made of iron of restricted composition to which magnesium or rare-earth metals are added under controlled conditions to cause the graphite to form, during solidification, as nodules instead of the flakes common to gray iron. The resulting iron has strength and ductility properties between those of gray iron and steel.

Composite rolls, sometimes called double-pour rolls (hardness: bodies, 70 to 90 HSc; necks, 40 to 50 HSc) are rolls in which the body surface is made of a richly alloyed, hard, wear-resistant cast iron, and the necks, wobblers, and central areas of the body are of a tougher and softer material. The metals are firmly bonded together during casting to form an integral structure that produces a wearing surface of high hardness, along with a tougher body and neck. Composite rolls are thus better able to withstand impact and thermal stresses. The outer rolling surface may be of either chilled or grain iron. The chief application of composite rolls in the rolling of steel has been for work rolls in four-high hot and cold strip mills and in plate mills; in the rolling of nonferrous metals, the chief application has been for rolls for hot breakdown and cold reduction of sheet and strip.

Cast Steel Rolls. Differentiation between cast iron rolls and cast steel rolls cannot be made strictly on the basis of carbon content. Iron rolls are usually of compositions that produce free graphite in unchilled portions; steel rolls do not exhibit free graphite.

The harder cast alloy steel rolls have hardnesses equivalent to those of the softer cast iron rolls, and the superior toughness of cast steel rolls often makes them preferable to cast iron rolls.

Composition. Alloy steel rolls have almost entirely superseded carbon steel rolls in use. Compositions of most alloy steel rolls are within the following limits: 0.40 to 2.0 C; less than 0.012 S, usually 0.06 max; less than 0.012 P, usually 0.06 max; up to 1.25 Mn; up to 1.50 Cr; up to 1.50 Ni; and up to 0.60 Mo. Higher carbon contents increase hardness and wear resistance. Some rolls have higher alloy contents, but these are usually employed for special purposes.

Applications. Cast steel rolls are graded according to carbon content. The general applications of these rolls are listed in Table 3. This table does not constitute a rigid classification, because conditions vary widely from mill to mill. Adjustments in carbon and alloy content are commonly made to suit individual conditions.

Table 3 Applications of cast steel rolls

Carbon, %	Applications
0.50-0.65	**Applications in which strength is the only requirement**
0.70-0.85	**Blooming mills; roughing stands in jobbing, plate, and sheet mills; muck mills**
0.90-1.05	**Blooming mills; slab mills; roughing stands in continuous bar mills; backing rolls**
1.10-1.25	**Blooming and slab mills where breakage is not great; piercing mills; roughing stands in billet, bar, rail, and structural mills**
1.35-1.55	**Intermediate stands for rail mills; structural, continuous-billet, and continuous-bar mills**
1.60-1.80	**Intermediate stands for continuous-bar and billet mills; middle rolls for three-high mills**
1.85-2.05	**Middle rolls for rail and structural mills; finishing mills where housing design is too limited for iron rolls**
2.10-2.60	**Finishing rolls for unusual conditions**
2.65 and up	**Special applications**

Hardened forged steel rolls are principally used for cold rolling various metals in the form of coiled sheet and strip. Extremely high pressures are used in cold rolling, and forged rolls have sufficient strength, surface quality, and wear resistance for cold-rolling operations. Forged rolls are sometimes employed in nonferrous hot mills in preference to iron rolls because of their higher bending strength and resistance to metal pickup.

Type and Design. Forged steel rolls are generally flat-bodied (or plain-bodied) rolls designed to close dimensional tolerances and concentricity. They vary widely in size from a few kilograms to as much as 45 Mg (50 tons). During manufacture, holes are bored through the centers of larger rolls for heat treatment and inspection purposes. New design developments include tapered journals with drilled holes to accommodate a special type of roller bearing, and somewhat greater use of fully hardened bearing journals for direct roller-bearing contact. Forged rolls have been specified for work rolls, backup rolls, auxiliary rolls, and special rolls.

Composition. The most commonly used composition for forged steel rolls, sometimes known as regular roll steel, averages 0.85 C, 0.30 Mn, 0.30 Si, 1.75 Cr, and 0.10 V. About 0.25% Mo is sometimes added to this basic composition, and the chromium content may be varied to obtain specific characteristics. For rolling nonferrous metals, a forged steel containing 0.40 C and 3.00 Cr is preferred. In Sendzimir mills, the work rolls and first and second intermediate supporting and drive rolls usually are made from high-carbon high-chromium tool steel with 1.50 or 2.25% C and 12.00% Cr (AISI D1 or D4). For more severe service, work rolls of M1 are used. The powder metallurgy (P/M) alloy CPM 10V has wear resistance approaching that of cemented carbide, which makes it attractive for some special forged steel rolls. The composition of CPM 10V is 2.45 C, 5.25 Cr, 10.0 V, and 1.30 Mo.

Hardness. Selection of the proper hardness for the body of the roll is essential for successful service performance. The hardness range varies with the specific application and is developed with the cooperation of mill operators. Most forged rolls are heat treated to high hardness, but they may be processed to lower values for specific purposes. Because of their high hardness, hardened steel rolls require careful handling in shipping, storage, mill service, and grinding.

Hardness of work rolls for rolling thin strip averages about 95 HSc; lower hardnesses are employed for rolling thicker strip. In temper and finishing mills, work roll hardness is sometimes higher than 95 HSc, and for special applications such as foil rolls, it is up to 100 HSc. In nonferrous rolling, especially in aluminum plate mills, work roll hardness generally ranges from 60 to 80 HSc. Hardness of backing rolls varies from 55 to 95 HSc; values on the high side of this range are specified for rolls in small mills and foil mills.

For Sendzimir mills, customary hardness is 61 to 64 HRC for D1 and D4 steel work rolls and 64 to 66 HRC for high-speed steel work rolls. Customary hardness of intermediate rolls is 58 to 62 HRC.

Only the body section of a forged roll is hardened. Journals are usually not hardened, except those for direct-contact roller-bearing designs, for which a minimum hardness of 80 HSc is specified. In normal practice, the journals of forged rolls range in hardness from 30 to 50 HSc.

Sleeve Rolls. Use of forged and hardened sleeve-type rolls in certain hot strip and cold reduction mills has become common because such rolls are more economical. Sleeves are forged from high-quality alloy steel. Chromium-molybdenum-vanadium and nickel-chromium-molybdenum-vanadium compositions are generally used. Sleeves are heat treated by liquid quenching in either oil or water and are tempered to hardnesses of 50 to 85 HSc, depending on application.

The mandrel over which the sleeve is slipped may be made from a cast roll that has been worn below its minimum usable diameter, from a new casting made specifically for use as a mandrel, or from an alloy steel forging.

The outside diameter of the mandrel and the inside diameter of the sleeve are accurately machined or ground for a shrink fit. Mounting is accomplished by heating the sleeve to obtain the required expansion and then either slipping the sleeve over the mandrel or inserting the mandrel in the sleeve. This operation is performed with the mandrel in a vertical position. A locking device prevents lateral movement of the sleeve. Final machining is done after the sleeve is mounted.

Forged sleeves provide the hard, dense, spall-resistant surface required for the severe service encountered in hot and cold reduction mills. Another economical advantage of this type of roll is that the mandrel may be resleeved four or five times.

Reference cited in this section

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G. D. Lahoti, The Timken Company; S.L. Semiatin, Battelle Columbus Division

Instruments and Controls

Early rolling mills used few, if any, sensing and monitoring devices and were manually controlled by the operators. However, in modern high-speed mills, instrumentation and process controls are essential to ensure a correct set, the proper operation of mills, and an acceptable product quality. At a rolling speed of 1500 m/min (4900 ft/min), for example, 1200 mm (48 in.) wide sheet that is rolled 0.01 mm (0.0004 in.) too thick can result in a loss of one ton of steel every 5 min. Therefore, at each mill stand, instruments are used to measure roll force, drive motor current, roll speed, and roll gap. In addition, other devices measure workpiece temperature, size, and shape. Furthermore, microprocessors and computers are being used to program the mill operation, based on mathematical models. In slower mills, such as blooming, billet, and slab mills, the operator often acts as the controller, adjusting the mill operation based on feedback from instrumentation. However, for high-speed mills (bar, sheet, and strip mills), this is best accomplished by computer control.

The principal components of a computer-controlled system are:

- Mathematical models that adequately describe the process
- Instrumentation to measure the required variables of the system
- Control equipment, including a digital computer, to perform the required functions for control of the system

Process Models. A computer-controlled system can only follow orders; it is necessary to tell the computer what to do. This instruction is provided by programming the computer in accordance with mathematical formulations or process models that describe the relationships between the process variables. The mathematical form of these relationships depends on the specific application and might include differential equations derived from theoretical considerations, empirical equations developed from experimental data, statistical analysis, logical decisions, or some combination of these. The chosen treatment of the processing data must provide the processing parameters to be controlled and the desired degree of control. In addition to inputting the computational instructions, the computer must be programmed for the logic to be used, the time sequence of required events, priorities of control actions under certain circumstances, and other decisions necessary for proper process control.

Instrumentation. A computer-controlled system accepts the quantitative values of the many processing variables and executes its control function based on these values. A prime requisite of such a system is adequate, reliable instrumentation for translating a process variable from its physical or chemical units to a form suitable for use by the computer. Many instruments are presently available to provide rapid on-line measurements of such variables as width, thickness, position, force, temperature, and flow. Instruments that measure other physical and chemical properties of both raw materials and finished products are available. However, this kind of measurement generally involves the taking of a sample and subsequent analysis in an off-line laboratory.

Control Equipment. The final component of a computer control system is the digital computer system, including hardware and software, and process regulating devices. The computer hardware includes a central processing unit that has the arithmetical and logical capability needed to run the mathematical models, a storage (memory) unit for accumulation of process measurement data and other information, and a computer interface to allow the central processing unit and memory to communicate with the instrumentation, with process regulators such as screw position regulators and speed regulators, with operators, and with other computers, including a business computer system.

Software consists of all the computer programs needed to accomplish the desired functions of the computer controlled system.

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Materials for Rolling

A large number of metals are rolled using the methods and equipment described above, or slight variations of them. By far the largest amount of rolled material falls under the general category of ferrous metals, or materials whose major constituent is iron. Included in this group are carbon and alloy steels, stainless steels, and specialty steels. Nonferrous metals, including aluminum alloys, copper alloys, titanium alloys, and nickel-base alloys also are processed by rolling.

Steels

Conventional primary and secondary rolling of steels is usually conducted at elevated (hot-rolling) temperatures. In a typical hot-rolling operation involving multiple passes through a reversing or multistand mill, the temperature of the work metal drops considerably. For carbon steels, the initial rolling temperature may be about 1200 °C (2190 °F); it may drop to 900 °C (1650 °F) or lower by the final pass. Because the size of recrystallized grains decreases with temperature, hot rolling results in a fine grain size.

The control of grain size and other microstructural features during rolling is especially important in low-carbon and low-alloy steels. Higher-carbon and high-alloy steels produced in the form of plates, bars, or shapes often undergo subsequent mechanical (for example, forging or extrusion) or thermal (such as hardening or tempering) processing in which the final properties are tailored to the end-use. Two products in which rolling is used almost exclusively to control structure and properties are low-carbon steel (used in automotive and appliance applications) and high-strength low-alloy steels (used in various structural applications).

Low-carbon steel is produced in the form of sheet by a combination of hot and cold rolling. The starting steel is either rimmed or killed. In these steels, it is not possible to obtain very fine grain sizes by means of controlling the rolling process (Fig. 24). This is because of the absence of alloying elements, which could retard the rapid grain growth that occurs between passes. Nevertheless, grain size and strength are of secondary importance in this product. The most important property is cold formability, because the sheet metal is often subsequently stamped into complex shapes at room temperature.

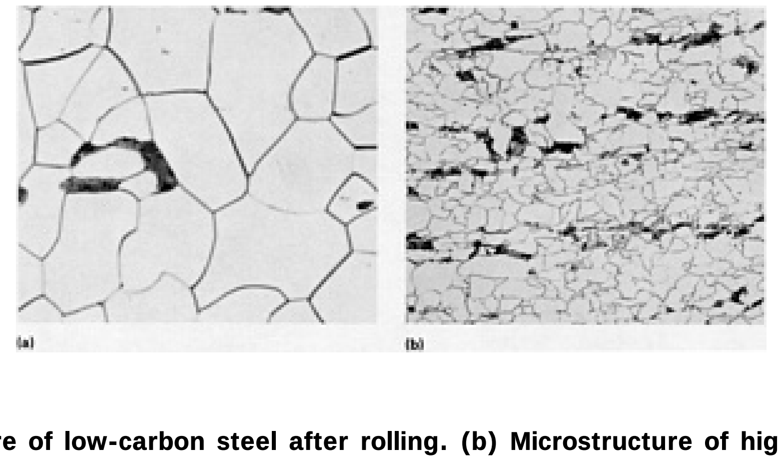

Fig. 24 (a) Microstructure of low-carbon steel after rolling. (b) Microstructure of high-strength low-alloy steel after rolling. See text for details. Courtesy of L. Cuddy, Pennsylvania State University

High-strength low-alloy steels, in contrast to the low-carbon steels discussed above, are designed to have high strength and a relatively modest amount of cold formability. These grades are typically produced as hot-rolled sheet, bar, and plate with 0.05 to 0.10% C and small amounts of niobium, vanadium, and titanium.

The correct thermomechanical treatment is extremely important in determining the final properties of high-strength low-alloy steels. For these materials, controlled rolling (Fig. 25) is used to refine the relatively coarse austenite structure by a series of high-temperature rolling and recrystallization steps. A moderate to heavy reduction is imposed on the material below the recrystallization temperature (T_r in Fig. 25) to achieve the desired fine grain size and the associated properties. More information on controlled rolling of high-strength low-alloy steels is available in Ref 45.

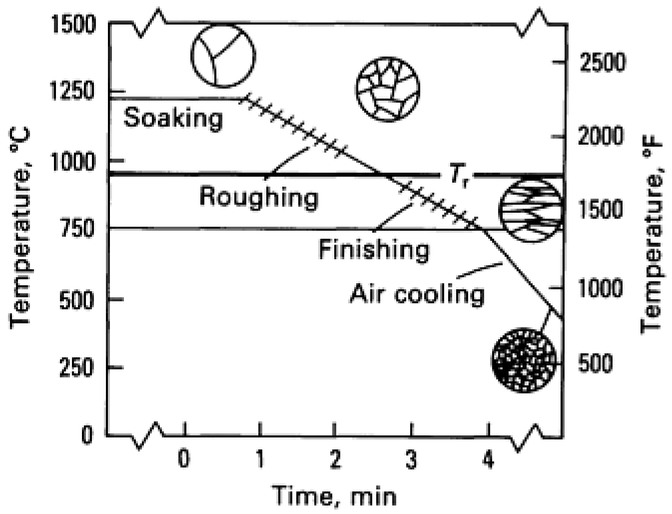

Fig. 25 Temperature/time profile for controlled rolling of high-strength low-alloy steel plate. Ar_3, austenite-to-ferrite transformation temperature. Source: Ref 45

Stainless steels are available in the same product forms as carbon and low-alloy steels. Mills of more rugged construction are required for the rolling of stainless steels than are needed for plain carbon and alloy steels because of the higher strengths of the stainless alloys; otherwise, rolling practice is similar to that used for carbon and alloy steels.

Stainless steel mill products are normally obtained in the annealed condition, but strength or hardness higher than that in the annealed condition can be attained by controlled cold rolling.

Nonferrous Materials

A number of nonferrous metals are also rolled into a variety of product forms using methods similar to those described above for steels. These include aluminum, copper, titanium, and nickel-base alloys.

Aluminum Alloys. Aluminum alloy sheet and plate are also hot rolled from slabs. As for steels, the slabs are frequently fabricated from cast ingots. In many cases, it is first necessary to remove surface defects from the ingots by means of a machining operation known as scalping. Also, the ingots are given a preliminary high-temperature homogenization heat treatment to eliminate chemical nonuniformities inherent in aluminum alloy castings. Such a treatment expands the temperature regime over which rolling can be successfully conducted without fracture. Aluminum slabs are finished by hot rolling alone on continuous mills (thicker gages) or by a combination of hot and cold rolling (thinner gages). Aluminum foil is one of the most common of rolled aluminum products, and is produced by cold rolling to thicknesses as small as 6 μm (0.00024 in.) (Ref 46). Aluminum alloy blooms are hot rolled from square ingots on two-high reversing mills, with scalping and homogenization heat treatments being used as for the slabbing operation. Bars, shapes, and wire are subsequently hot rolled from reheated blooms.

Copper Alloys (Ref 47). Production of copper alloy strip and sheet begins with semicontinuous cast slabs or continuous cast strip. Initial breakdown of these products is usually by hot rolling on two-high reversing mills equipped with vertical edging rolls. After rolling, the strip is scalped (milled) to remove any oxides remaining from the casting and rolling operations. After milling, the strip has a thickness of about 7.6 to 10.2 mm (0.300 to 0.400 in.).

Copper alloy sheet is cold rolled to final thickness using either four-high or Sendzimir mills to obtain the necessary reduction while maintaining flatness. The finished thickness of the sheet can be as low as 0.1 mm (0.004 in.).

Titanium and titanium alloys are considerably more difficult to roll than are either steels or copper and aluminum alloys. Most titanium alloys have very narrow working temperature ranges. To overcome this problem, titanium alloys are often rolled in packs, or layers of sheets, that are sometimes encased in a steel envelope (Fig. 26). The envelope, called a can, is evacuated to minimize oxidation of the work metal, and also serves to minimize heat loss to the relatively cold rolls upon deformation. The narrow working temperature range of titanium alloys makes the rolling of these materials labor intensive. Rolling passes are done by hand on relatively small pieces, and many intermediate reheating steps are required. Some β titanium alloys, however, are being continuously rolled on hot and cold strip mills.

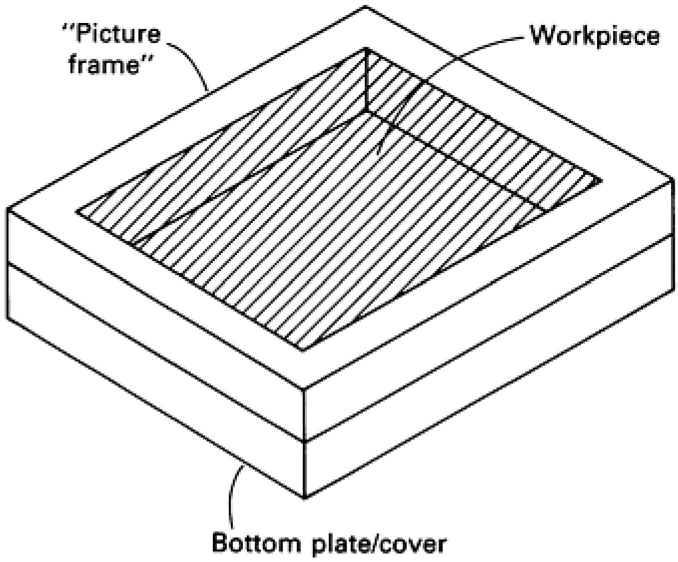

Fig. 26 Pack ("can") used for rolling titanium and nickel-base alloys. The top cover, which would be welded in place before rolling, is not shown.

Nickel-base superalloys are the most difficult materials to roll. Primary rolling of these materials is usually done at temperatures near the melting point on rugged, powerful mills built to withstand the high stresses encountered in the working of these alloys. Like titanium alloys, nickel-base alloys have narrow working temperature ranges, are often rolled in packs or cans, and must be reheated frequently between passes. Mill products include sheets, bars, and shapes.

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Flat, Bar, and Shape Rolling

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Heated-Roll Rolling

Heated-roll rolling is a process that was developed at Battelle Columbus Laboratories to roll difficult-to-work materials. Heated-roll rolling is an isothermal or near-isothermal process in which the work rolls are heated to the same or nearly the same temperature as the work metal. Heated-roll rolling is analogous to isothermal and hot-die forging (see the article "Isothermal and Hot-Die Forging" in this Volume).

Heated-Roll Rolling of Sheet and Strip

In conventional hot rolling of high temperature materials, such as titanium and nickel alloys, the hot metal is deformed between cold or warm rolls. This causes chilling on the surface of the rolled metal, resulting in higher working loads and stresses than would be required if chilling were avoided. Further, chilling limits the maximum possible reduction per pass and the minimum thickness attainable in conventional hot sheet rolling. From a materials viewpoint, conventional hot rolling often requires rolling temperatures that are higher than optimal, which cause more workpiece contamination and can result in microstructure and property variations in rolled products. To overcome some of the problems associated with conventional hot rolling, the techniques of isothermal and near-isothermal hot rolling have been developed.

The technique has been demonstrated for sheet rolling on modified conventional rolling mills with either two-high or four-high arrangements (Ref 48, 49, 50). In the two-high arrangement, where the rolls are relatively large, a composite roll design has often been employed (Fig. 27). This consists of rolls with outer sleeves made of a high-temperature superalloy and with cores of hot-work tool steels (such as AISI H13). Such a design satisfies the need for a roll with good hot hardness at temperatures in the 815 °C (1500 °F) range at a cost less than a solid roll made from an expensive superalloy. This setup is improved by induction heating the roll surfaces and by internal water cooling of the core of the rolls and roll bearings. The viability of heated-roll rolling also has been demonstrated on a four-high mill (Ref 49). In this case, the work rolls and the backup rolls were heated by banks of radiant heaters and had a design maximum operating surface temperature of 815 °C (1500 °F) for the work rolls.

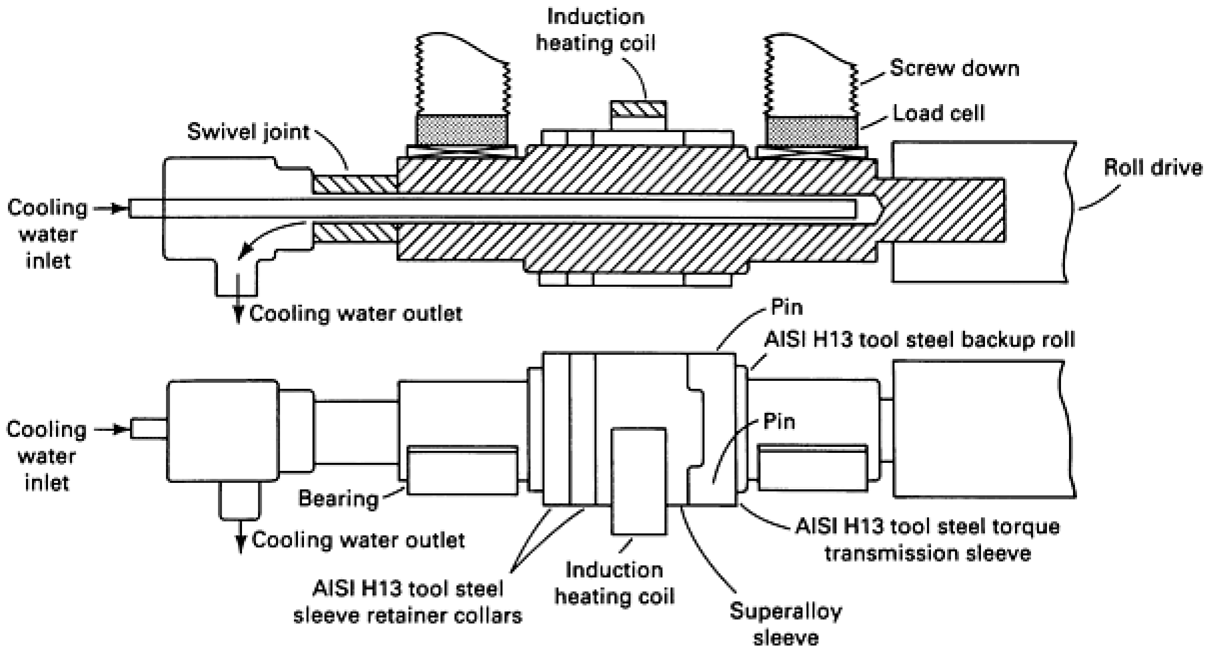

Fig. 27 Schematic showing composite roll construction and auxiliary equipment for heated-roll rolling. Source: Ref 48, 50

In studies conducted at Battelle Columbus Division, rolling was not performed at low deformation rates, and therefore did not rely on the superplastic characteristics of the workpiece materials, which are often used in isothermal and hot-die forging. Rather, these studies concentrated on determination of the workability of and uniformity obtainable in difficult-to-work and temperature sensitive alloys in the absence of chilling. Thus, attention was focused on a much wider range of alloys than those used in isothermal forging, including tungsten, beryllium, Ti-6Al-4V, alloy 718, and several alloy steels and oxide-dispersion strengthened alloys. For powder metallurgy (P/M) consolidated and stress relieved tungsten, for instance, workability was shown to be greatly improved with heated-roll rolling (Ref 48). This material is difficult to hot work conventionally because of its ductile-to-brittle transition at temperature of 230 to 250 °C (445 to 480 °F). With rolls at a temperature of 150 °C (300 °F) or lower, strip preheated to 260 °C (500 °F) and rolled at a speed of 6 m/min (20 ft/min) developed severe laminations and edge cracking because of chilling. In contrast, when the roll temperature was raised to the preheat temperature of the strip (260 °C, or 500 °F), none of these problems was encountered.

The effect of heated rolls on temperature uniformity in rolling of strip from high-temperature alloys was quantified through a series of heat transfer simulations performed on a digital computer (Ref 48, 49, 50). An example result is shown in Fig. 28 for thin beryllium strip preheated to 540 °C (1000 °F) and rolled to 20% reduction in thickness at a rolling speed of 30 m/min (100 ft/min). In this case, when the rolls are at room temperature, the temperature of the hot strip decreases significantly during rolling, and large temperature gradients are present. Both of these factors may cause workability problems. On the other hand, temperature decreases and thermal gradients through the strip thickness are calculated to be comparatively small when the rolls are heated to a surface temperature of 540 °C (1000 °F).

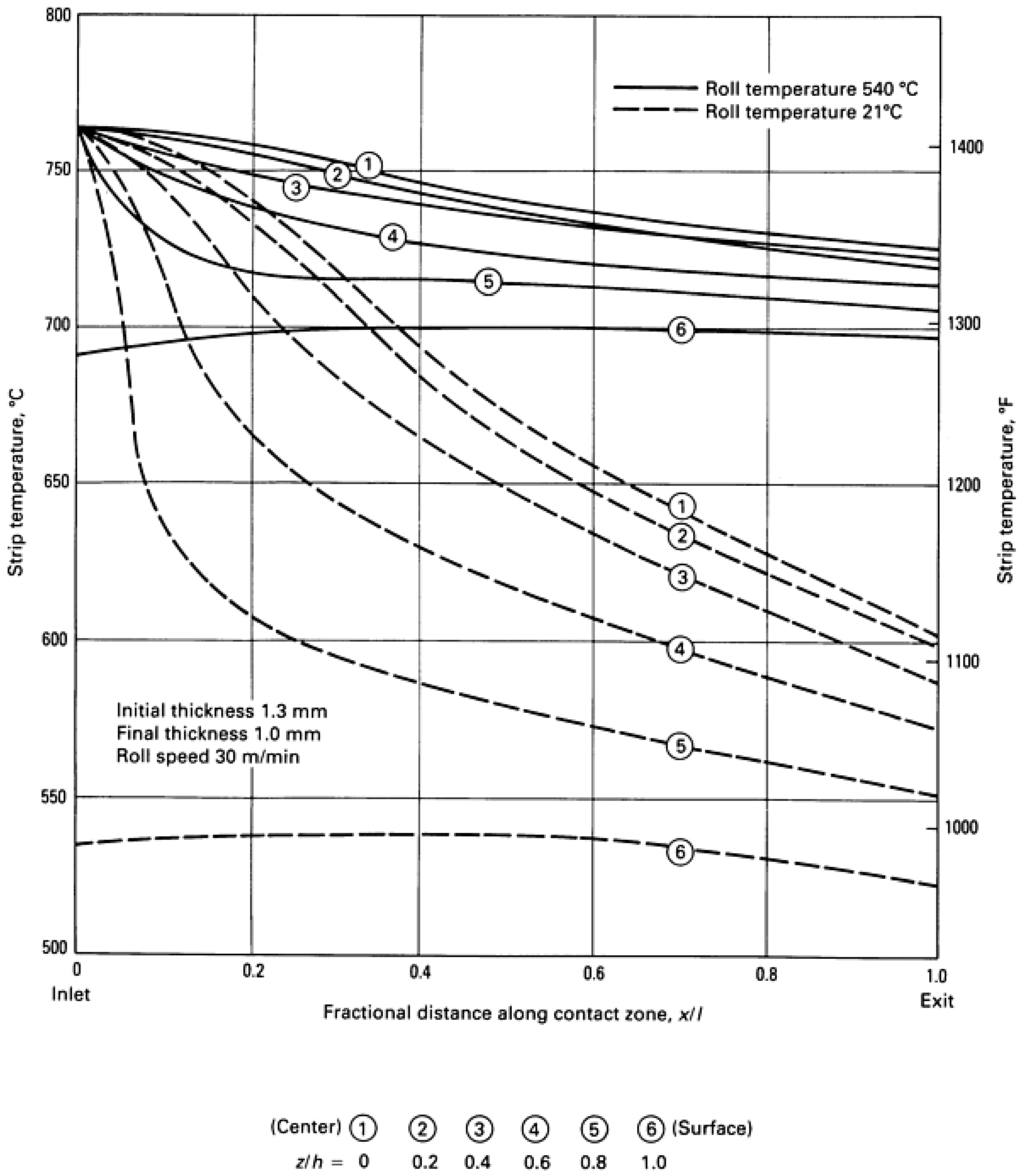

Fig. 28 Predicted temperature profiles for beryllium strip preheated to 760 °C (1400 °F). Source: Ref 48, 50

Experiments at Battelle have also shown that heated-roll rolling lowers roll separating forces and enables larger reductions per pass by eliminating or minimizing chilling effects. This is illustrated by the data on the rolling of Ti-6Al-4V shown in Table 4. For example, an 80% reduction at a strip preheat temperature of 995 °C (1825 °F) required a roll separating force of 28.9 kN (6500 lbf) at a roll temperature of 540 °C (1000 °F). With the rolls at 27 °C (80 °F) and the strip at the same preheat temperature, a 62% reduction required more than twice the roll separating force measured in the first case (Table 4).

Table 4 Rolling loads required for Ti-6Al-4V

Initial sheet thickness was 3.1 mm (0.12 in.); rolling speed was 32 m/min (105 ft/min).

Reduction, %	**Workpiece preheat temperature**		**Roll surface temperature**		Rolling load per inch of sheet width	
	°C	**°F**	**°C**	**°F**	**kN**	lbf
43	650	1200	27	80	95.7	**21,500**
53	840	1550	27	80	66.8	**15,000**
62	995	1825	27	80	75.7	**17,000**
51	650	1200	540	1000	77.9	**17,500**
68	840	1550	540	1000	64.5	**14,500**
80	**995**	**1825**	**540**	**1000**	**28.9**	**6,500**

Source: Ref 49

Like the microstructures obtained in isothermal forging, microstructures in isothermally rolled sheet have been found to be very uniform. Thus, it has been suggested that in some cases one or more post-rolling heat treating steps may be eliminated. This was demonstrated in the processing of 8670 alloy steel strips that were preheated to 840 °C (1550 °F), rolled, quenched upon exit from the mill, and tempered for 2 h at 175 °C (350 °F) (Ref 51). At 840 °C (1550 °F), this steel is totally austenitic. When the rolls were unheated, a coarse martensitic structure at the strip center, microstructural non-uniformity near the surface, and rolling directionality were evident. However, when the rolls were heated to 840 °C (1550 °F) to produce isothermal metalworking conditions, a fine, uniform martensitic microstructure with no directionality was obtained. Therefore, heated-roll rolling of steel sheet and shapes which are to be subsequently hardened may offer the advantage of eliminating the austenitizing and quenching stages.

Heated-Roll Rolling of Shapes

The application of the isothermal and heated-roll rolling concept to shapes also has met with success, although the commercialization of the process, like that for flat rolling, has yet to be fully realized and accepted. One company, in cooperation with Battelle Columbus Division, modified an existing two-high production rolling mill with 250-mm (10-in.) diameter rolls into a heated-roll rolling setup. This setup was used to produce a structural L-shape to close tolerances from Ti-6Al-4V and a high-temperature superalloy (Ref 49). Similar to designs used in Battelle's work, the company used a composite roll design consisting of an AISI A9 tool steel core and a superalloy sleeve. Further, the rolls were heated by banks of quartz-tube radiant heaters which Battelle had also shown to be feasible. With this tooling, 150 m (500 ft)--a production-size quantity, of the structural shape--were successfully rolled for each of the two alloys. Compared to conventional rolling practice, the heated-roll rolling of the superalloy required fewer rolling and other major operations.

A different isothermal rolling concept has been developed and applied for the rolling of structural shapes (channels, Z sections, T sections, I sections, etc.) of various titanium and nickel-base alloys (Ref 52). In this method, molybdenum alloy rolls are heated only locally by an electric current which is passed from one roll to another through the workpiece. Thus, the workpiece can be heated by resistance heating during or just prior to rolling. It may also be preheated to a temperature lower than the actual rolling temperature prior to being fed into the mill. Thus, oxidation and heating times are reduced. In this scheme, local resistance heating aids in producing very thin sections found in many structural parts and allows very large reductions (up to 90%) to be taken in a single pass. However, to obtain large reductions, it is necessary to apply a "feed force," in addition to the "squeeze force" supplied by the rolls.

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Flat, Bar, and Shape Rolling

G. D. Lahoti, The Timken Company; S.L. Semiatin, Battelle Columbus Division

Defects In Rolling

A number of defects or undesirable conditions can develop in the rolling of flat, bar, or shaped products. Very broadly, these problems can be attributed to one of four sources: melting and casting practice, metallurgical sources, heating practice, and rolling practice.

Melting and Casting Practice. The major problems associated with melting and casting practice are the development of porosity and a condition known as scabs. Porosity is developed in cast ingots when they solidify and is of two types: pipe and blow holes. Pipe is a concave cavity formed at the top of the ingot due to nonuniform cooling and shrinkage. If not cropped off, pipe can be rolled into the final product to form an internal lamination (Fig. 29). These laminations may not be immediately evident following rolling, but may become apparent during a subsequent forming operation. The occurrence of laminations is most prevalent in flat-rolled sheet products. Blow holes are usually a less serious defect. They are the result of gas bubbles entrapped in the metal as the ingot solidifies. If the surfaces of holes are not oxidized, they may be welded closed during the rolling operation.

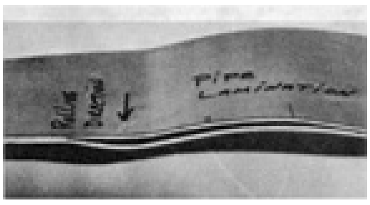

Fig. 29 Laminations in rolled steel sheet resulting from insufficient cropping of the pipe from the top of a

conventionally cast ingot. Courtesy of V. Demski, Teledyne Rodney Metals

Scabs are caused by improper ingot pouring, in which metal is splashed against the side of the mold wall. The splashed material, or scab, tends to stick to the wall and become oxidized. Scabs usually show up only after rolling and, as can be expected, give poor surface finish.

Metallurgical Sources. Defects such as poor surface finish may also result from a metallurgical source, nonmetallic inclusions. In steels, inclusions are of two types, refractory and plastic. Refractory inclusions are often metallic oxides such as alumina in aluminum-killed steels or complex oxides of manganese and iron in rimmed steels. When near the surface, such inclusions give rise to defects known as seams and slivers (Fig. 30).

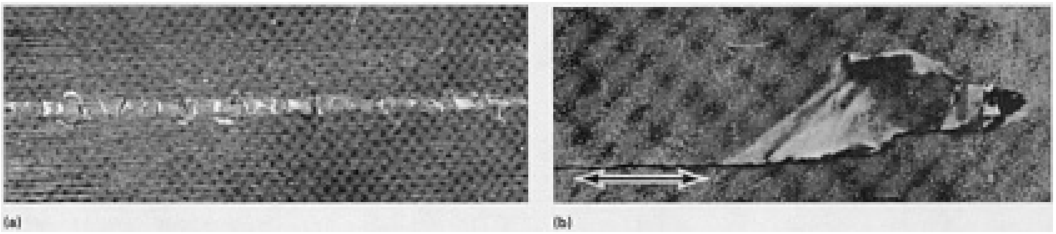

Fig. 30 (a) Seams and (b) slivers caused in rolled material by the presence of surface inclusions. Courtesy of V. Demski, Teledyne Rodney Metals

Plastic inclusions, such as manganese sulfides, elongate in the rolling direction during hot forming. The presence of these elongated inclusions (stringers) produces fibering, which causes directional properties. For example, ductility transverse to the fiber is frequently lower than that parallel to it. In high-strength low-alloy steels, sulfide shape control elements, such as cerium, are often added to prevent the development of such fibering, which is especially undesirable from the viewpoint of subsequent forming operations or service behavior.

Another rolling defect whose source is metallurgical is alligatoring (Fig. 31). This defect, found most frequently in rolled slabs of aluminum-magnesium, zinc, and copper-base alloys, is manifested by a gross midplane fracture at the leading edges of the rolled metal.

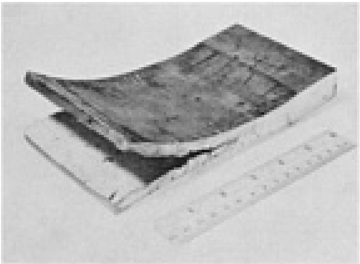

Fig. 31 Alligatoring in a rolled slab. This defect is thought to be caused by nonhomogeneous deformation and nonuniform recrystallization during primary rolling of such metals as zinc alloys, aluminum-magnesium alloys, and copper-base alloys. Courtesy of J. Schey, University of Waterloo

Heating Practice. Two rolling defects that stem from heating practice are rolled-in scale and blisters. The development of scale during preheating of ingots, slabs, or blooms is almost inevitable, particularly for steels. Sometimes descaling operations involving hydraulic sprays or preliminary light rolling passes are not totally successful; scale may get rolled into the metal surface and become elongated into streaks during subsequent rolling. The other defect, blistering, is a raised spot on the surface caused by expansion of subsurface gas during heating. Blisters may break open during rolling and produce a defect that looks like a gouge or surface lamination.

Rolling practice can cause defects also. In bar and shape rolling, for example, excessive reduction in the finishing pass may cause metal to extrude laterally in the roll gap, leading to a defect known as finning. Finning in an intermediate pass causes folds or laps in subsequent passes. Excessive reduction in the leader pass (the pass prior to the finishing pass) also may wrinkle open the sides of a bar, which after turning 90° in the finishing pass can result in a series of hairline cracks. In the rolling of slabs and plates, two defects that affect yield are fishtail and overlaps, both of which need to be trimmed off (Fig. 32). The former results from nonuniform reduction in the width, and the latter results from nonuniform reduction in thickness. These defects can be reduced by proper design of the blooming and rolling sequence. Several defects, such as wavy edges, center buckle, herringbone, and quarter buckle can be created in cold rolling of sheets and strips. These defects are primarily due to localized overrolling, which can occur because of improper roll profiles or variations in the properties or shape of the incoming strip.

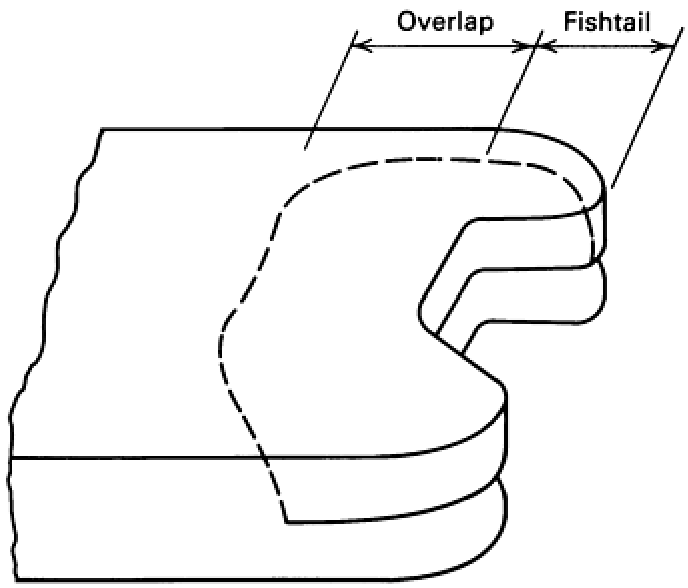

Fig. 32 Overlap and fishtail formed during rolling of slabs and plates. Overlap is the result of nonuniform deformation in thickness, while fishtail is caused by nonuniform deformation in width.

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Introduction to Workability

George E. Dieter, University of Maryland

Introduction

WORKABILITY refers to the relative ease with which a metal can be shaped through plastic deformation. The term workability is often used interchangeably with the term formability, which is preferred when referring to the shaping of sheet metal parts (see the Section "Evaluation of Formability for Secondary (Sheet) Forming" in this Volume). However, workability is usually used to refer to the shaping of materials by such bulk deformation processes as forging, extrusion, and rolling.

The characterization of the mechanical behavior of a material by tension testing measures two different types of mechanical properties: strength properties (such as yield strength and ultimate strength) and ductility properties (such as percentage of elongation and reduction in area). Similarly, the evaluation of workability involves both measurement of the resistance to deformation (strength) and determination of the extent of possible plastic deformation before fracture (ductility). Therefore, a complete description of the workability of a material is specified by its flow stress dependence on processing variables (for example, strain, strain rate, preheat temperature, and die temperature), its failure behavior, and the metallurgical transformations that characterize the alloy system to which it belongs.

However, the major emphasis in workability is on measurement and prediction of the limits of deformation before fracture.Therefore, the emphasis in this article is on methods for determining the extent of deformation a metal can withstand before cracking or fracture occurs. It is important, however, to allow for a more general definition in which workability is defined as the degree of deformation that can be achieved in a particular metalworking process without creating an undesirable condition. Generally, the undesirable condition is cracking or fracture, but it may be another condition, such as poor surface finish, buckling, or the formation of laps, which are defects created when metal folds over itself during forging.In addition, in the most general definition of workability, the creation by deformation of a metallurgical structure that results in unsatisfactory mechanical properties, such as poor fracture toughness or fatigue resistance, can be considered to be a limit on workability.

In the restricted context of the forging process, the relative ability of a material to deform without fracture is termed forgeability.Because the ability of the material to flow readily and to fill the die recesses completely is important, this is another aspect of forgeability. This aspect of forgeability is measured by the flow stress of the material. Methods of measuring flow stress are briefly discussed in this article and are treated in more detail in the Section "Computer-Aided Process Design for Bulk Forming".

Generally, workability depends on the local conditions of stress, strain, strain rate, and temperature in combination with material factors, such as the resistance of a metal to ductile fracture. In addition to a review of the many process variables that influence the degree of workability, the mathematical relationships that describe the occurrence of room-temperature ductile fracture under workability conditions are summarized in this article. The most common testing techniques for workability prediction are discussed in the article "Workability Tests" in this Section.

Introduction to Workability

George E. Dieter, University of Maryland

Material Factors Affecting Workability

Fracture Mechanisms. Fracture in bulk deformation processing usually occurs as ductile fracture, rarely as brittle fracture.However, depending on temperature and strain rate, the details of the ductile fracture mechanism will vary. Figure 1 illustrates the different modes of ductile fracture obtained in a tension test over a wide range of strain rates and temperatures. At temperatures below about one-half the melting point of a given material (below the hot-working region), a typical dimpled rupture type of ductile fracture usually occurs. A more commonly found representation of possible fracture mechanisms is the fracture mechanism (Ashby) map (Fig. 2). Such a map shows the area of dominance of each fracture mechanism. The maps are constructed chiefly by using the best mechanistic models of each fracture process.

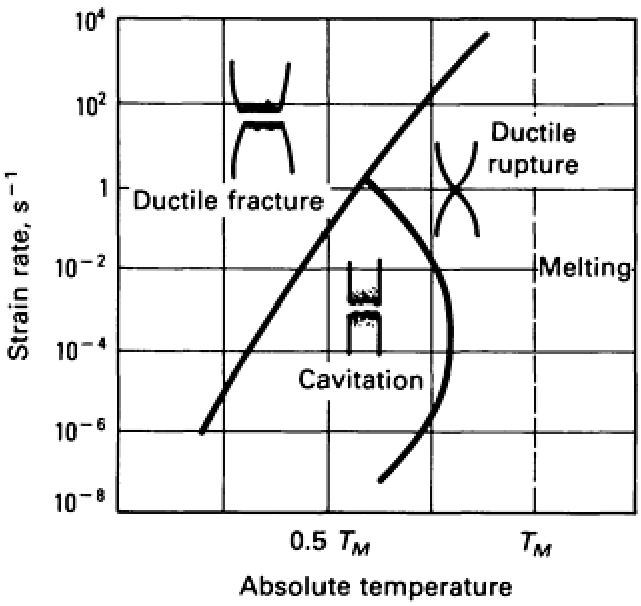

Fig. 1 Tensile fracture modes as a function of temperature (measured on the absolute temperature scale) and strain rate.

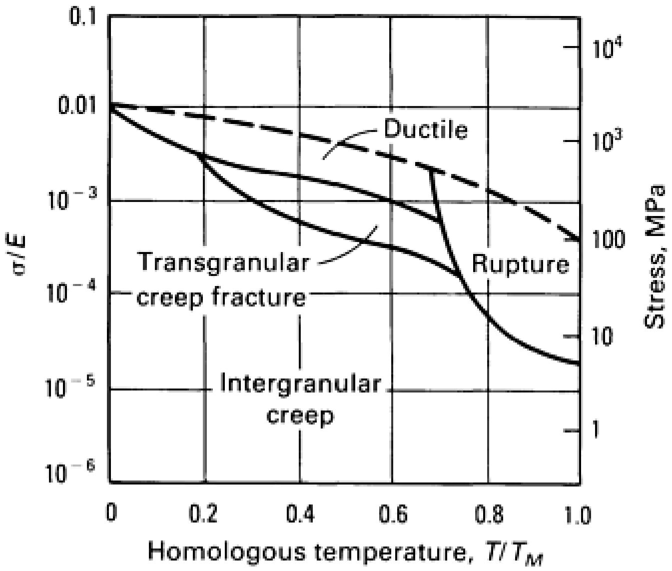

Fig. 2 Fracture mechanism map for nickel.

The three stages of ductile fracture are shown schematically in Fig. 3. The first stage is void initiation, which usually occurs at second-phase particles or inclusions.Voids are initiated because particles do not deform, and this forces the ductile matrix around the particle to deform more than normal. This in turn produces more strain hardening, thus creating a higher stress in the matrix near the particles. When the stress becomes sufficiently large, the interface may separate, or the particle may crack. As a result, ductility is strongly dependent on the size and density of the second-phase particles, as shown in Fig. 4.

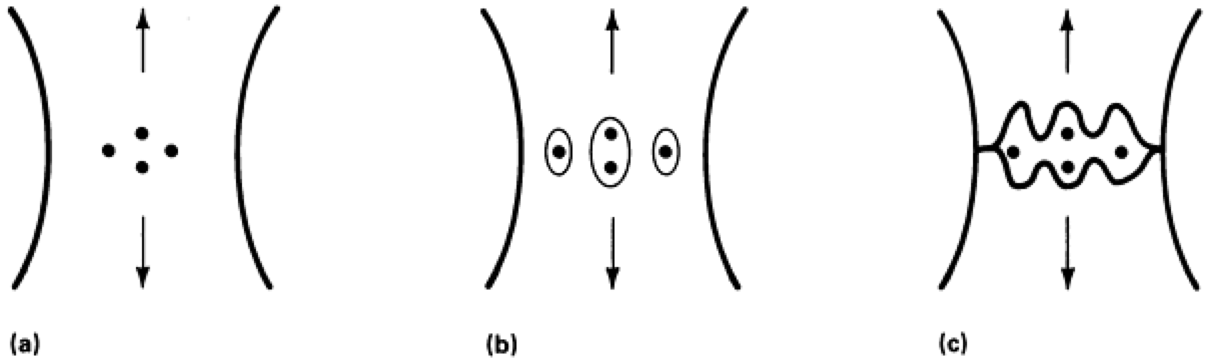

Fig. 3 Stages in the dimple rupture mode of ductile fracture. (a) Void initiation. (b) Void growth. (c) Void linking.

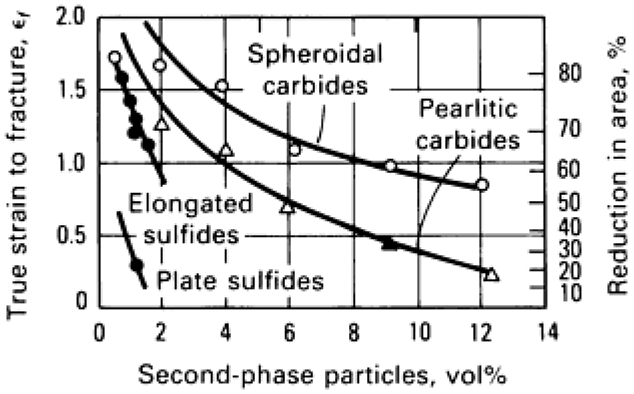

Fig. 4 Effect of volume fraction of second-phase particles on the tensile ductility of steel. Source: Ref 1.

The second stage of ductile fracture is void growth, which is a strain-controlled process. Voids elongate as they grow, and the ligaments of matrix material between the voids become thin. Therefore, the final stage of ductile fracture is hole coalescence through the separation of the ligaments, which links the growing voids.

Ductile fracture by void growth and coalescence can occur by two modes. Fibrous tearing (Mode I) occurs by void growth in the crack plane that is essentially normal to the tensile axis. In Mode II void growth, voids grow in sheets at an oblique angle to the crack plane under the influence of shear strains. This type of shear band tearing is found on the surface of the cone in a ductile cup-and-cone tensile fracture. It commonly occurs in deformation processing, in which friction and/or geometric conditions produce inhomogeneous deformation, leading to local shear bands. Localization of deformation in these shear bands leads to adiabatic temperature increases that produce local softening.

Increasing the temperature of deformation leads to significant changes in deformation behavior and fracture mode. At temperatures above one-half the melting point, particularly at low strain rates, grain-boundary sliding becomes prominent. This leads to wedge-shaped cracks that propagate along grain boundaries and result in low ductility (Fig. 5).

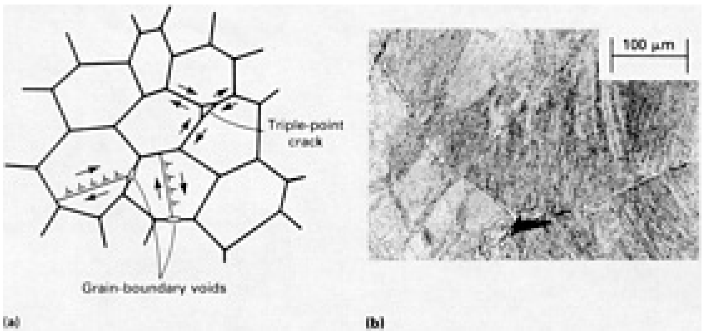

Fig. 5 Formation of grain-boundary voids (cavitation) and triple-point cracks at warm- and hot-working temperatures. (a) Schematic showing how grain-boundary voids are formed under the action of matrix deformation and how grain-boundary sliding in the absence of grain-boundary migration and recrystallization may cause cracks to open at triple points. (b) Examples of grain-boundary voids and triple-point cracking at the prior β grain boundaries in hot-forged Ti-6Al-2Sn-4Zr-2Mo-0.1Si with a Widmanstätten α starting microstructure. Source: Ref 2, 3.

The probability of wedge cracking varies with the applied strain rate. If the strain rate is so high that the matrix deforms at a faster rate than the boundaries can slide, then grain-boundary sliding effects will be negligible, and wedge cracking will not occur. If the strain rate is very low, sufficient time is available to relax the high stresses at the triple points, and a different fracture mechanism comes into play.

Round or elliptical cavities form in grain boundaries at high temperatures, but form at lower stresses than those that produce wedge-shaped cracks. Nevertheless, like wedge cracks, these cavities are formed by grain-boundary shearing. With low strain rates (<1 s^{-1}), the initiation of voids by grain-boundary sliding and the reduced rate of dynamic recrystallization can result in extensive internal void formation, or cavitation. Cavitation is germane with respect to ductility in creep, but it is generally not a factor in fracture in hot working.

For high-temperature fracture initiated by grain-boundary sliding, the processes of void growth and coalescence, rather than void initiation, are the primary factors that control ductility. When voids initiated at the original grain boundaries have difficulty in linking because boundary migration is high as a result of dynamic recrystallization, hot ductility is high. In extreme cases, this can lead to highly ductile rupture, as shown in Fig. 1.

Compressive stresses superimposed on tensile or shear stresses by the deformation process can have a significant influence on closing small cavities or limiting their growth and thus enhancing workability. Because of this important role of the stress state, it is not possible to express workability in absolute terms. Workability depends not only on material characteristics but also on process variables, such as strain, strain rate, temperature, and stress state.

A processing map can be developed considering all of the failure mechanisms that can operate in a material over a range of strain rates and temperatures. The processing map is a useful concept that should gain wider acceptance in the study of material response in deformation processing.

Figure 6 illustrates a processing map for aluminum that is based on theoretical models; however, it agrees well with experimental work. A safe region is indicated in which the material should be free from cavitation damage or flow localization. The processing map predicts that, at constant temperature, there should be a maximum in ductility with respect to strain rate. For example, at 500 K (227 °C, or 440 °F), ductility should be at a maximum at a strain rate of 10^{-3} to 1 s^{-1}. Below the lower value, wedge cracking will occur; above this level, ductile fracture would reduce ductility.

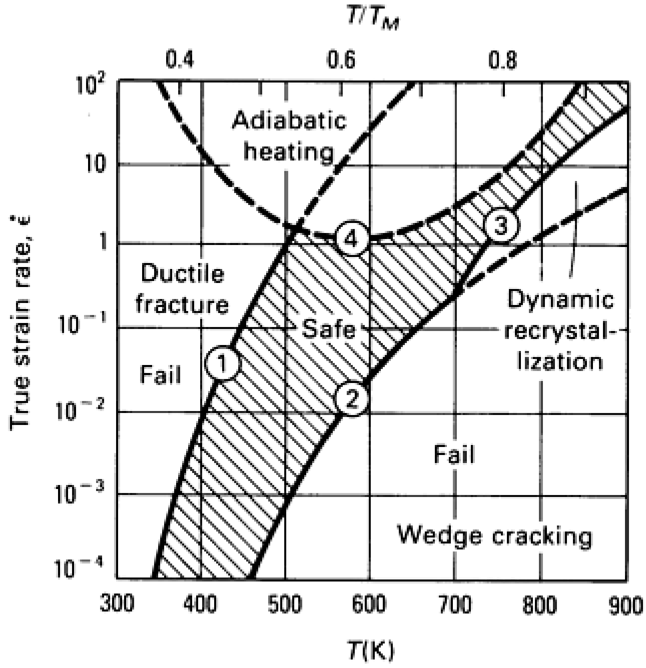

Fig. 6 Composite processing map for aluminum showing the safe region for forming. Boundaries shift with microstructure. Instabilities due to purely continuum effects, such as shear localization in sheet metal forming, are not considered. Source: Ref 4.

The safe region shown in Fig. 6 is sensitive to the microstructure of the metal. Decreasing the size and volume fraction of hard particles would move boundary 1 to the left. Increasing the size or fraction of hard particles in the grain boundary would make sliding more difficult and would move boundary 2 to the right. More detailed information on fracture mechanisms can be found in the article "Modes of Fracture" in *Fractography*, Volume 12 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

Flow Localization. Workability problems can arise when metal deformation is localized to a narrow zone. This results in a region of different structures and properties that can be the site of failure in service. Localization of deformation can also be so severe that it leads to failure in the deformation process. In either mode, the presence of flow localization needs to be recognized.

Flow localization is commonly caused by the formation of a dead-metal zone between the workpiece and the tooling. This can arise from poor lubrication at the workpiece/tool interface. Figure 7 illustrates the upsetting of a cylinder with poorly lubricated platens. When the workpiece is constrained from sliding at the interface, it barrels, and the friction-hill pressure distribution is created over the interface. The inhomogeneity of deformation throughout the cross section leads to a dead zone at the tool interface and a region of intense shear deformation.

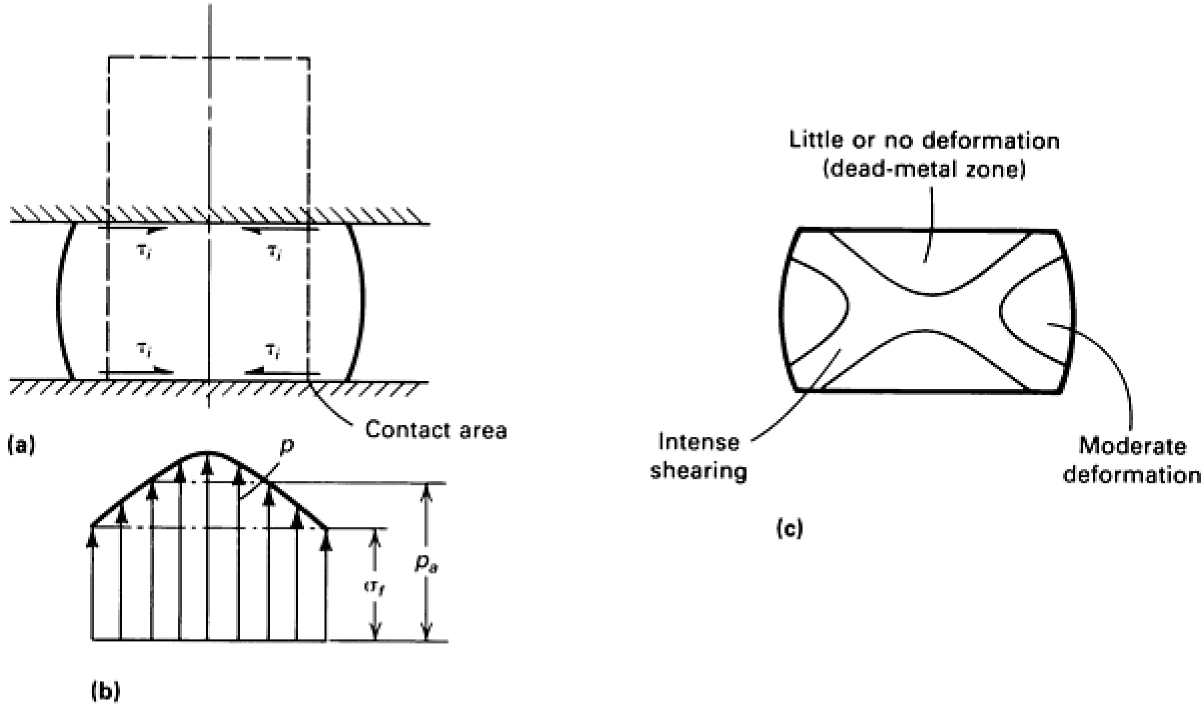

Fig. 7 Consequences of friction illustrated in the upsetting of a cylinder. (a) Direction of shear stresses. (b) Consequent rise in interface pressure. (c) Inhomogeneity of deformation. τ_i, average frictional shear stress; *p*, normal pressure; σ_f, flow stress; p_a, average die pressure.

A similar situation can arise when the processing tools are cooler than the workpiece; in this case, heat is extracted at the tools. Consequently, the flow stress of the metal near the interface is higher because of the lower temperature.

However, flow localization may occur during hot working in the absence of frictional or chilling effects. In this case, localization results from flow softening (negative strain hardening). Flow softening arises during hot working as a result of structural instabilities, such as adiabatic heating, generation of a softer texture during deformation, grain coarsening, or spheroidization. Flow softening has been correlated with materials properties (Ref 5) by the parameter:

$$\alpha = -\frac{(\gamma - 1)}{m} \quad \textbf{(Eq 1)}$$

for upset compression and

$$\alpha = -\frac{\gamma}{m} \quad \textbf{(Eq 2)}$$

for plane-strain compression where $\gamma = (1/\sigma)d\sigma/d\varepsilon$ is the normalized flow-softening rate, and $m = d \log \sigma / d \log \dot{\epsilon}$ is the strain rate sensitivity. In $\alpha + \beta$ titanium alloys and other materials that exhibit a strong tendency toward flow localization, this phenomenon is likely to occur when the parameter α is greater than 5. Figure 8 shows a crack that initiated in a shear band during the high-energy-rate forging of a complex austenitic stainless steel.

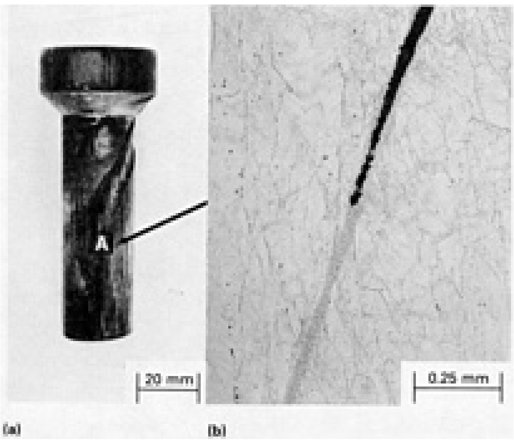

Fig. 8 Austenitic stainless steel high-energy-rate-forged extrusion. Forging temperature: 815 °C (1500 °F); 65% reduction in area; $\dot{\epsilon}$ = 1.4 × 10^3 s^{-1}. (a) View of extrusion showing spiral cracks. (b) Optical micrograph showing the microstructure at the tip of one of the cracks in the extrusion (area A). Note that the crack initiated in a macroscopic shear band that formed first at the lead end of the extrusion. Etchant: Oxalic acid. Source: Ref 6.

Metallurgical Considerations. The common failure modes occurring in deformation processing are summarized in Table 1. Three temperature regions are common: cold working, warm working, and hot working. Two types of ductile fracture are distinguished in cold working, depending on the stress state. Free surface fracture occurs at a free surface when there is no stress normal to the surface, while centerburst-type failure occurs at the center of the bar or forging because of high hydrostatic tension.

Table 1 Common failure modes in deformation processing

Temperature regime	Metallurgical defects in:	
	Cast grain structure	Wrought (recrystallized) grain structure
Cold working	(a)	Free surface fracture, dead metal zones (shear bands, shear cracks), centerbursts, galling
Warm working	(b)	Triple-point cracks/fractures, grain-boundary cavitation/fracture
Hot working	Hot shortness, centerbursts, triple-point cracks/fractures,	Shear bands/fractures, triple-point cracks/fractures,

(a) Cold working of cast structures is typically performed only for very ductile metals (such as dental alloys) and usually involves many stages of working with intermediate recrystallization anneals.

(b) Warm working of cast structures is rare.

Workability problems depend greatly on grain size and grain structure. When the grain size is large relative to the overall size of the workpiece, as in conventionally cast ingot structures, workability is lower, because cracks may initiate and propagate easily along the grain boundaries. Moreover, with cast structures, impurities are frequently segregated to the center and top or to the surface of the ingot, creating regions of low workability. Because chemical elements are not distributed uniformly on either a micro- or a macroscopic scale, the temperature range over which an ingot structure can be worked is rather limited.

Typically, cast structures must be hot worked. The melting point of an alloy in the as-cast condition is usually lower than that of the same alloy in the fine-grain, recrystallized condition because of chemical inhomogeneities and the presence of low melting point compounds that frequently occur at grain boundaries. Deformation at temperatures too close to the melting point of these compounds may lead to grain-boundary cracking when the heat developed by deformation increases the workpiece temperature and produces local melting.

This fracture mode is called hot shortness. This type of fracture can be prevented by using a sufficiently low deformation rate that allows the heat developed by deformation to be dissipated by the tooling, by using lower working temperatures, or by subjecting the workpiece to a homogenization heat treatment prior to hot working. The relationship between the workability of cast and wrought structures and temperatures is shown in Fig. 9.

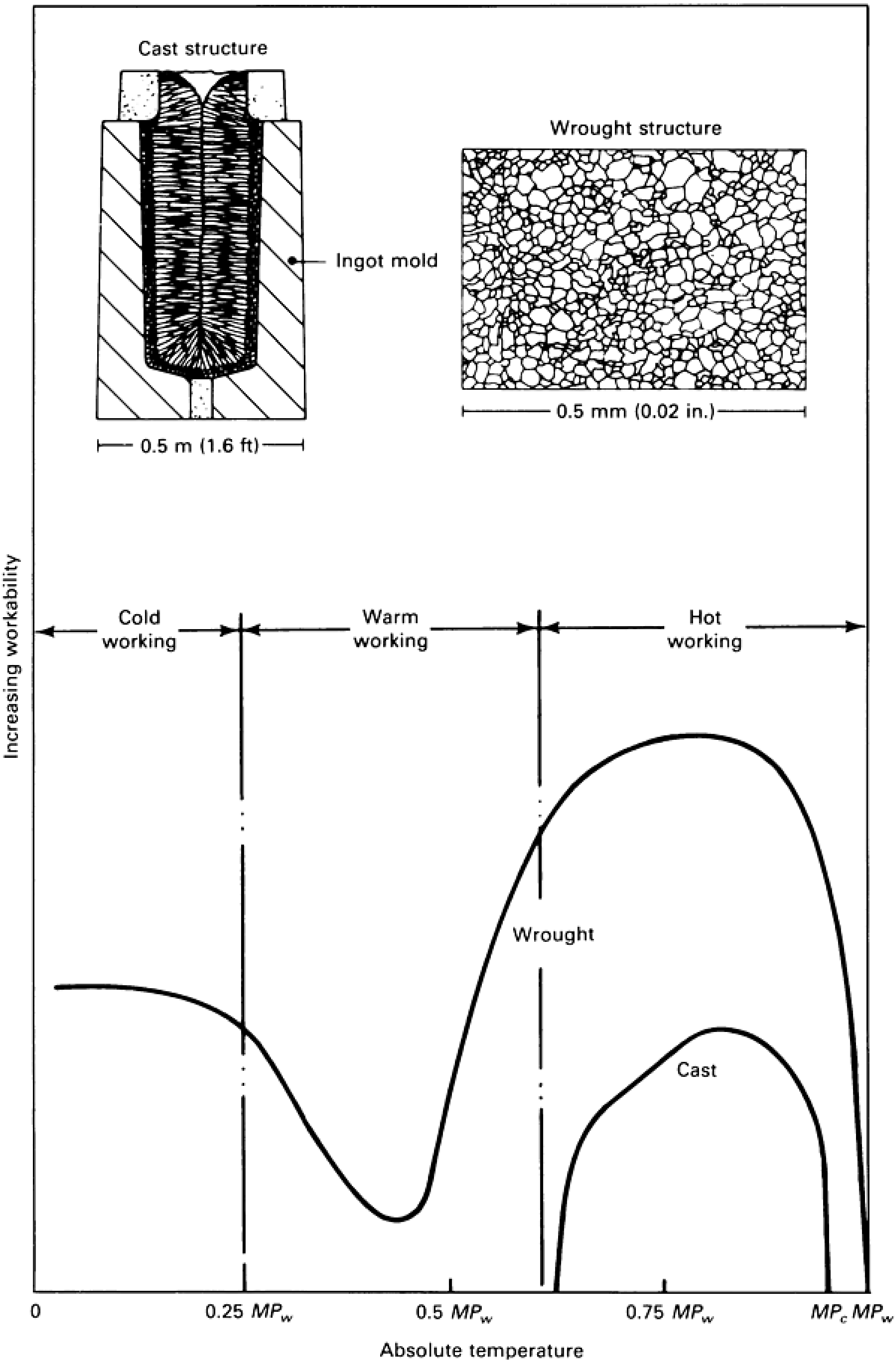

Fig. 9 Relative workabilities of cast metals and wrought recrystallized metals at cold-, warm-, and hot-working temperatures. The melting point (or solidus temperature) is denoted as MP_c (cast metals) or MP_w (wrought and

recrystallized metals).

The intermediate temperature region of low ductility shown in Fig. 9 is found in many metallurgical systems (Ref 7). This occurs at a temperature that is sufficiently high for grain-boundary sliding to initiate grain-boundary cracking, but not so high that the cracks are sealed off from propagation by a dynamic recrystallization process.

The relationship between workability and temperature for various metallurgical systems is summarized in Fig. 10. Generally, pure metals and single-phase alloys exhibit the best workability, except when grain growth occurs at high temperatures. Alloys that contain low melting point phases (such as γ'-strengthened nickel-base superalloys) tend to be difficult to deform and have a limited range of working temperature. In general, as the solute content of the alloy increases, the possibility of forming low melting point phases increases, while the temperature for precipitation of second phases increases. The net result is a decreased region for good forgeability (Fig. 11).

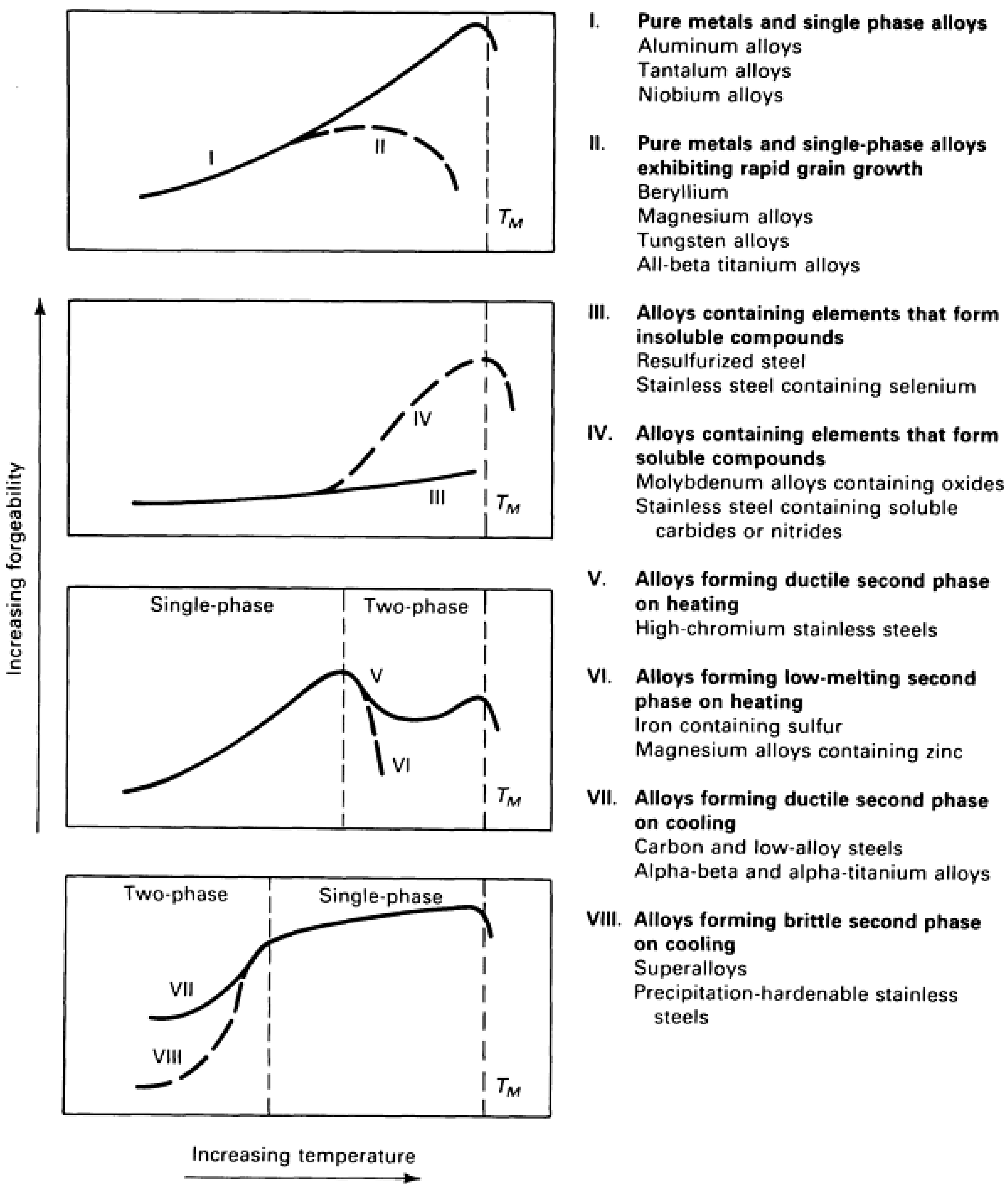

Fig. 10 Typical workability behavior exhibited by different alloy systems. T_M: absolute melting temperature. Source: Ref 8.

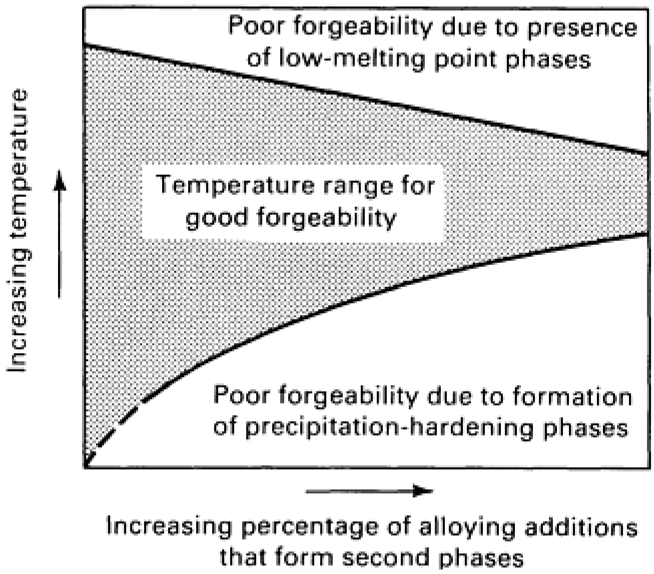

Fig. 11 Influence of solute content on melting and solution temperatures and therefore on forgeability.

During the breakdown of cast ingots and the subsequent working by forging nonuniformities in alloy chemistry, second-phase particles, inclusions, and the crystalline grains themselves are aligned in the direction of greatest metal flow. This directional pattern of crystals and second-phase particles is known as the grain flow pattern. This pattern is responsible for the familiar fiber structure of forgings (Fig. 12). It also produces directional variation in such properties as strength, ductility, fracture toughness, and resistance to fatigue. This anisotropy in properties is greatest between the working (longitudinal) direction and the transverse direction (Fig. 13). In a properly designed forging, the largest stress should be in the direction of the forging fiber, and the parting line of the dies should be located so as to minimize disruption to the grain flow lines.

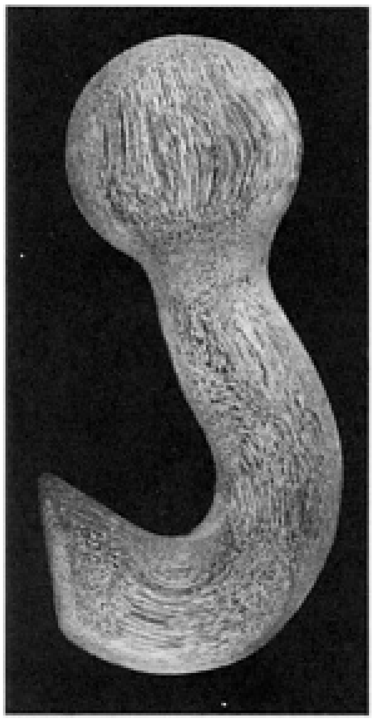

Fig. 12 Flow lines in a forged 4140 steel hook. Specimen was etched using 50% HCl. 0.5×.

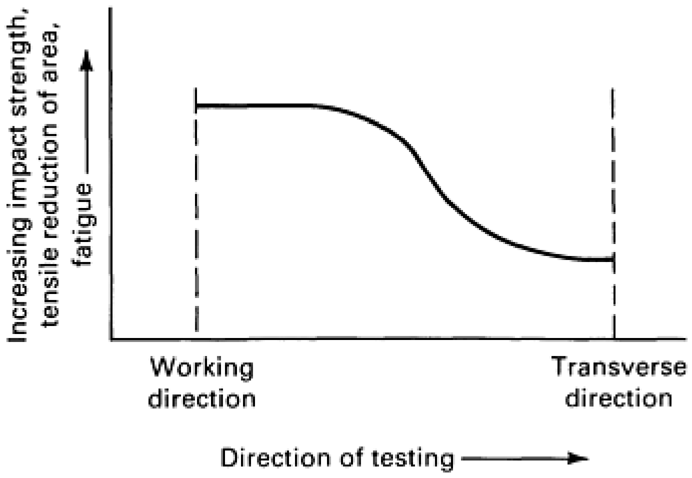

Fig. 13 Anisotropy in wrought alloys.

In closed-die forging, an important consideration is for the metal to flow during forging to fill the cavities of the impression die. The forgeability of the metal is a way of expressing this capability. Poor forgeability can be caused by rupture before the die is filled or by a high flow strength, which causes the metal to flow past recesses without filling them or causes underfilling with the maximum loads available. A shop floor evaluation of forgeability is provided in Table 2. Different alloys were hammer forged into a connecting rod for an automobile engine in a set of dies ordinarily used for 1030 steel rods. The forgeability of the materials is indicated by the number of blows needed to complete the forging and the remarks about die filling.

Table 2 Forging data on connecting rods forged from eight different materials representing increasing forging difficulty

Material	**Forging temperature**		**Number of times heated**	**Total number of blows to complete forging**	Remarks
	°C	**°F**			
1030 steel	1260	2300	1	<20	**Completely filled**
Type 304 stainless steel	1200	2200	1	40	**Completely filled**
Type 347 stainless steel	1200	2200	1	50	**Completely filled**
Monel	1120	2050	2	51	**Completely filled**
16-25-6	1100	2000	5	76	**Incomplete fill on shaft end**
N-155	1100	2000	5	89	**Incomplete fill on both ends and ribs**

Alloy B (UNS N10001)	1200	2200	3	. . .	**Extreme difficulty in drawing and fullering shape; experiment discontinued**
Alloy C (UNS N10002)	**1180**	**2150**	**4**	**. . .**	**Same as for Alloy B**

Source: Ref 8

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Introduction to Workability

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Process Variables Controlling Workability

Strain. The principal objective in plastic deformation processes is to change the shape of the deformed product. A secondary objective is to improve or control the properties of the deformed product. In dense metals, unlike porous powder compacts, the volume of the workpiece remains constant as it increases in cross-sectional area, *A*, and decreases in length, *L*:

$$A_0L_0 = A_1L_1 \qquad \textbf{(Eq 3)}$$

The strain produced in a deformation process is described by the engineering strain:

$$e = \frac{L_1 - L_0}{L_0} = \frac{A_0 - A_1}{A_1} \qquad \textbf{(Eq 4)}$$

or alternatively by the true strain:

$$\epsilon = \ln \frac{L_1}{L_0} = \ln \frac{A_0}{A_1} = \ln (e + 1) \quad \textbf{(Eq 5)}$$

Deformation in metalworking is often expressed by the cross-sectional area reduction, *R*:

$$R = \frac{A_0 - A_1}{A_0} \quad \textbf{(Eq 6)}$$

but from constancy of volume (Eq 3), $A_0L_0 = A_1L_1$, and:

$$\epsilon = \ln \frac{L_1}{L_0} = \ln \frac{A_0}{A_1} = \ln \frac{1}{1 - R} \quad \textbf{(Eq 7)}$$

The relationship between area reduction and the various measures of strain is given in Table 3.

Table 3 Relationships among area reduction (*R*), engineering strain (*e*), and true strain (ϵ)

	Area reduction (*R*), %											
	5	**10**	**15**	**20**	**25**	**30**	**40**	**50**	**60**	**70**	**80**	90
e	0.052	0.111	0.176	0.250	0.333	0.428	0.667	1.000	1.500	2.333	4.000	**9.000**
ε	**0.051**	**0.105**	**0.162**	**0.223**	**0.287**	**0.356**	**0.511**	**0.693**	**0.916**	**1.204**	**1.609**	**2.303**

Strain rate is the time rate of change of strain, that is, the rate at which deformation proceeds. It can be an important variable in workability experiments and may be difficult to control. The true strain rate for a cylinder of height *h* upset in compression at a deformation velocity *v* at time *t* is:

$$\dot{\epsilon} = \frac{d\epsilon}{dt} = \frac{1}{h}\frac{dh}{dt} = \frac{v}{h} \quad \textbf{(Eq 8)}$$

Temperature. Metalworking processes are commonly classified as hot-working or cold-working operations. Hot working refers to deformation under conditions of temperature and deformation velocity such that recovery processes occur simultaneously with deformation. Cold working refers to deformation carried out under conditions for which recovery processes are not effective during the process. In most hot working, the strain hardening and the distorted grain structure produced by deformation are eliminated rapidly by the formation of new strain-free grains as a result of recrystallization during or immediately after deformation.

Very large deformations are possible in hot working, because the recovery processes keep pace with the deformation. Hot working occurs at essentially constant flow stress. Flow stress decreases with the increasing temperature of deformation. In cold working, strain hardening is not relieved, and the flow stress increases continuously with deformation. Therefore, the total deformation possible before fracture is less for cold working than for hot working, unless the effects of strain hardening are relieved by annealing.

About 95% of the mechanical work expended in deformation is converted into heat. Some of this heat is conducted away by the tools or lost to the environment. However, a portion remains to increase the temperature of the workpiece. The faster the deformation process, the greater the percentage of heat energy that goes to increase the temperature of the workpiece.

Friction. An important concern in all practical metalworking processes is the friction between the deforming workpiece and the tools and/or dies that apply the force and constrain the shape change. Friction occurs because metal surfaces, at least on a microscale, are never perfectly smooth and flat. Relative motion between such surfaces is impeded by contact under pressure.

The existence of friction increases the value of the deformation force and makes deformation more inhomogenous (Fig. 7c), which in turn increases the propensity for fracture. If friction is high, seizing and galling of the workpiece surfaces occur, and surface damage results.

The mechanics of friction at the tool/workpiece interface are very complex; therefore, simplifying assumptions are usually used. One such assumption is that friction can be described by Coulomb's law of friction:

$$\mu = \frac{\tau_i}{p} \qquad \textbf{(Eq 9)}$$

where μ is the Coulomb coefficient of friction, τ_i is the shear stress at the interface, and p is the stress (pressure) normal to the interface.

Another simplification of friction is to assume that the shear stress at the interface is directly proportional to the flow stress σ_0 of the material:

$$\tau_i = m \frac{\sigma_0}{\sqrt{3}} \qquad \textbf{(Eq 10)}$$

where m, the constant of proportionality, is the interface friction factor. For given conditions of lubrication and temperature and for given die and workpiece materials, m is usually considered to have a constant value independent of the pressure at the interface. Values of m vary from 0 (perfect sliding) to 1 (no sliding). In the Coulomb model of friction, τ_i increases with p up to a limit at which interface shear stress equals the yield stress of the workpiece material.

Control of friction through lubrication is an important aspect of metalworking. High friction leads to various defects that limit workability. However, for most workability tests, conditions are selected under which friction is either absent or easily controlled. Most workability tests make no provision for reproducing the frictional conditions that exist in the production process; consequently, serious problems can result in the correlation of test results with actual production conditions.

Stress State. Because of the different geometries of the tools and workpiece and the different manners in which the forces of deformation are applied, different metalworking processes produce different stress states. A common system of classifying the stress state found in metalworking processes is:

Tensile-compressive systems

- Biaxial tension-uniaxial compression, such as under the roll of a two-roll rotary piercer
- Uniaxial tension-uniaxial compression, such as in the flange of a cup in deep drawing
- Uniaxial tension-biaxial compression, such as in the deformation zone in wire and rod drawing

Compressive stress systems

- Uniaxial stress, such as in forging and upsetting in closed dies
- Biaxial stress, such as between the rolls of a rolling mill when operating without front or back tension
- Triaxial stress, such as in extrusion and in certain parts of a forging deformed in a die

Tensile stress systems

- Biaxial stress, such as stretch forming

Two states that occur frequently in structural or metalworking applications--plane stress and plane strain--deserve special mention. The use of the adjective plane implies that the condition is confined to a two-dimensional situation. Plane stress occurs when the stress state lies in the plane of the member. This typically occurs when one dimension of the member is very small compared to the other two, and the member is loaded by a force lying in the plane of symmetry of the body. Examples are thin, plate-type structures, such as thin-wall pressure vessels.

Plane strain occurs when the strain in one of the three principal directions is zero (for example, $e_3 = 0$), as in an extremely long member subjected to lateral loading or a thick plate with a notch loaded in tension. A common plane-strain condition in metalworking is the rolling of a wide sheet. In this case, there is no strain in the width direction. Although $e_3 = 0$ for plane strain, there is a stress acting in that direction. For plastic deformation, the stress in the principal direction for which strain is zero is the average of the other two principal stresses, that is, $\sigma_3 = (\sigma_1 + \sigma_2)/2$.

Stress systems in metalworking are usually complex. To describe a complex stress system rather briefly, it is necessary to calculate an effective stress, σ:

$$\bar{\sigma} = \frac{1}{\sqrt{2}}[(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2]^{1/2} \qquad \textbf{(Eq 11)}$$

where $\sigma_1 > \sigma_2 > \sigma_3$ are the three principal stresses. Tensile stresses are positive, and compressive stresses are negative. Another important stress term is the mean or hydrostatic component of stress, σ_m:

$$\sigma_m = \frac{1}{3}(\sigma_1 + \sigma_2 + \sigma_3) \qquad \textbf{(Eq 12)}$$

In general, the greater the level of tensile stress, the more severe the stress system is with regard to workability. For a given material, temperature, and strain rate of deformation, the workability is much improved if the stress state is highly compressive. A general workability parameter has been proposed that allows for the stress state (Ref 9):

$$\beta = \frac{3\sigma_m}{\bar{\sigma}} \qquad \textbf{(Eq 13)}$$

where σ_m is the hydrostatic stress component, and $\bar{\sigma}$ is the effective stress. Figure 14 shows the β parameter plotted for various mechanical tests and metalworking processes. The curve is evaluated by three basic tests: tension, torsion, and compression. Table 4 gives the pertinent values for each test. Figure 14 shows that workability is enhanced when β is predominately compressive.

Table 4 Evaluation of β parameter

Test	**Principal stresses**	**Effective stress**	**Mean stress**	**β**	Strain-to-fracture measurement
Tension	σ_1; $\sigma_2 = \sigma_3 = 0$	σ_1	$\frac{\sigma_1}{3}$	1.0	$\epsilon_f = \ln \frac{A_0}{A_n}$ at necking
Torsion	$\sigma_1 = -\sigma_2$; $\sigma_3 = 0$	$\sqrt{3}\sigma_1$	0	0	$\epsilon_f = \frac{r\theta}{\sqrt{3}L}$

Compression	**$-\sigma_1$; $\sigma_2 = \sigma_3 = 0$**	**$-\sigma_1$**	$-\frac{\sigma_1}{3}$	**-1.0**	$\epsilon_f = \ln \frac{A_f}{A_0}$

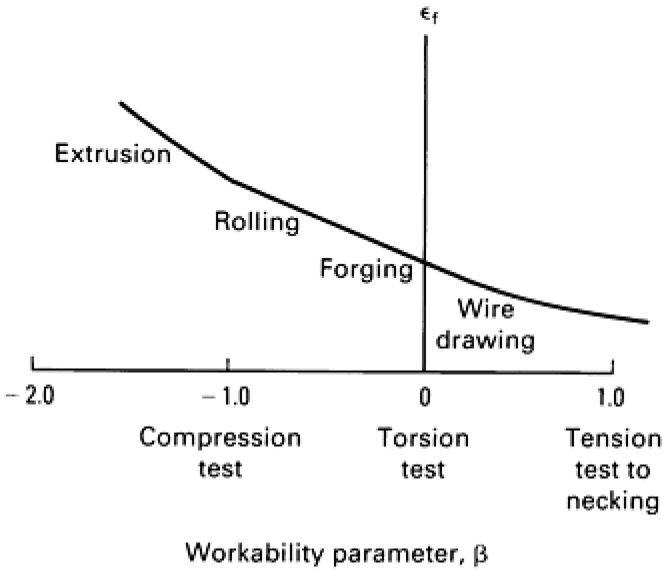

Fig. 14 Influence of stress state on strain to fracture.

Yielding Criteria. The ease with which a metal yields plastically or flows is an important factor in workability. If a metal can be deformed at low stress, as in superplastic deformation, then the stress levels throughout the deforming workpiece are low, and fracture is less likely. The dominant metallurgical conditions and temperature are important variables, as is the stress state. Plastic flow is produced by slip within the individual grains, and slip is induced by a high resolved shear stress. Therefore, the beginning of plastic flow can be predicted by a maximum shear stress, or Tresca, criterion:

$$\tau_{max} = \frac{1}{2}(\sigma_1 - \sigma_3) = \frac{\sigma_0}{2} \quad \textbf{(Eq 14)}$$

where τ_{max} is the maximum shear stress and σ_0 is the yield (flow) stress measured in either a uniaxial tension or uniaxial compression test. Although adequate, this yield criterion neglects the intermediate principal stress σ_2. A more complete and generally applicable yielding criterion is that proposed by von Mises:

$$2\sigma_0^2 = (\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 \quad \textbf{(Eq 15)}$$

where $\sigma_1 > \sigma_2 > \sigma_3$ are the three principal stresses, and σ_0 is the uniaxial flow stress of the material. This is the same equation given earlier for effective stress.

The significance of yield criteria is best illustrated by examining a simplified stress state, in which $\sigma_3 = 0$ (plane stress). The Tresca yield criterion then defines a hexagon, and the von Mises criterion an ellipse (Fig. 15).

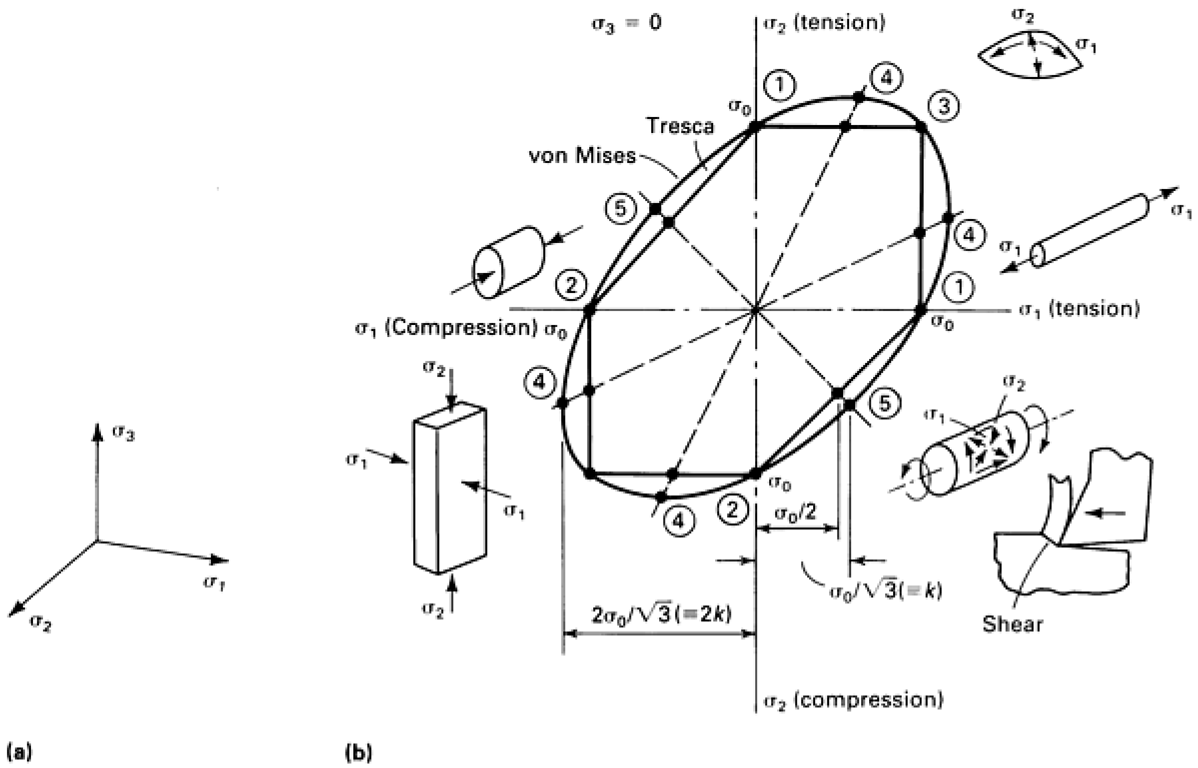

Fig. 15 Directions of principal stresses (a) and yield criteria (b) with some typical stress states.

Yielding (plastic flow) can be initiated in several modes. In pure tension, flow occurs at the flow stress σ_0 (points 1 in Fig. 15 corresponding to two directions in the plane of a sheet). In pure compression, the material yields at the compressive flow stress, which, in ductile materials, is usually equal to the tensile flow stress σ_0 (points 2, Fig. 15). When a sheet is bulged by a punch or a pressurized medium, the two principal stresses in the surface of the sheet are equal (balanced biaxial tension) and must reach σ_0 (point 3, Fig. 15).

A technically important condition is reached when deformation of the workpiece is prevented in one of the principal directions (plane strain). This occurs because a die element keeps one dimension constant; or only one part of the workpiece is deformed, and adjacent nondeforming portions exert a restraining influence. In either case, the restraint creates a stress in that principal direction; the stress is the average of the two other principal stresses (corresponding to points 4, Fig. 15). The stress required for deformation is still σ_0 according to Tresca, but is $1.15\sigma_0$ according to von Mises. The latter is usually regarded as the plane-strain flow stress of the materials. It is sometimes called the constrained flow stress.

Another important stress state is pure shear, in which the two principal stresses are of equal magnitude but of the opposite sign (points 5, Fig. 15). Flow now occurs at the shear flow stress τ_0, which is equal to $0.5\sigma_0$ according to Tresca and $0.577\sigma_0$ according to von Mises. The shear flow stress according to von Mises is often denoted as *k.* Figure 15 illustrates how the stress required to produce plastic deformation varies significantly with the stress state and how it can be related to the basic uniaxial flow stress of the material through a yield criterion.

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Workability Fracture Criteria

Workability is not a unique property of a given material. It depends on such process variables as strain, strain rate, temperature, friction conditions, and the stress system imposed by the process. For example, metals can be deformed to a greater extent by extrusion than by drawing because of the compressive nature of the stresses in the extrusion process that makes fracture more difficult. Therefore, workability can be expressed as:

$$\text{Workability} = f_1 \text{ (material)} \cdot f_2 \text{ (process)} \quad \textbf{(Eq 16)}$$

where f_1 is a function of the basic ductility of the material, and f_2 is a function of the stress and strain imposed by the process. Because f_1 depends on the material condition and the fracture mechanism, it is a function of temperature and strain rate. Similarly, f_2 depends on such process conditions as lubrication (friction) and die geometry.

Therefore, to describe workability in a fundamental sense, a fracture criterion must be established that defines the limit of strain as a function of strain rate and temperature. Also required is a description of stress, strain, strain rate, and temperature history at potential fracture sites. The application of computer-aided finite-element analysis of plastic deformation and heat flow has facilitated this goal (see the Section "Computer-Aided Process Design for Bulk Forming" in this Volume). The forming limit diagrams illustrated and discussed in the article "Workability Theory and Application in Bulk Forming Processes" in this Volume are a direct outgrowth of the above concept of workability.

The simplest and most widely used fracture criterion is that discussed in Ref 10. This fracture criterion is not based on a micromechanical model of fracture, but simply recognizes the joint roles of tensile stress and plastic strain in producing fracture:

$$\int_0^{\epsilon_f} \bar{\sigma}\left(\frac{\sigma^*}{\bar{\sigma}}\right) d\bar{\epsilon} = C \quad \textbf{(Eq 17)}$$

where $\overline{\sigma}$ is the effective stress; σ^* is the maximum tensile stress; $d\bar{\epsilon}$ is the effective strain, which is equal to $(\sqrt{2}/3)[(d\varepsilon_1 - d\varepsilon_2)^2 + (d\varepsilon_2 - d\varepsilon_3)^2 + (d\varepsilon_3 - d\varepsilon_1)^2]^{1/2}$; and C is a material constant evaluated from the compression test. This fracture criterion indicates that fracture occurs when the tensile strain energy per unit volume reaches a critical value.

Use of this fracture criterion is shown in Fig. 16. The values of the reduction ratio at which centerburst fracture occurs in the cold extrusion of two aluminum alloys are illustrated. The energy conditions for different die angles are given by the three curves that reach a maximum. The fracture curves for the two materials slope down to the right. Centerburst occurs in the regions of reduction, for which the process energies exceed the material fracture curve. No centerburst occurs at small or large reduction ratios.

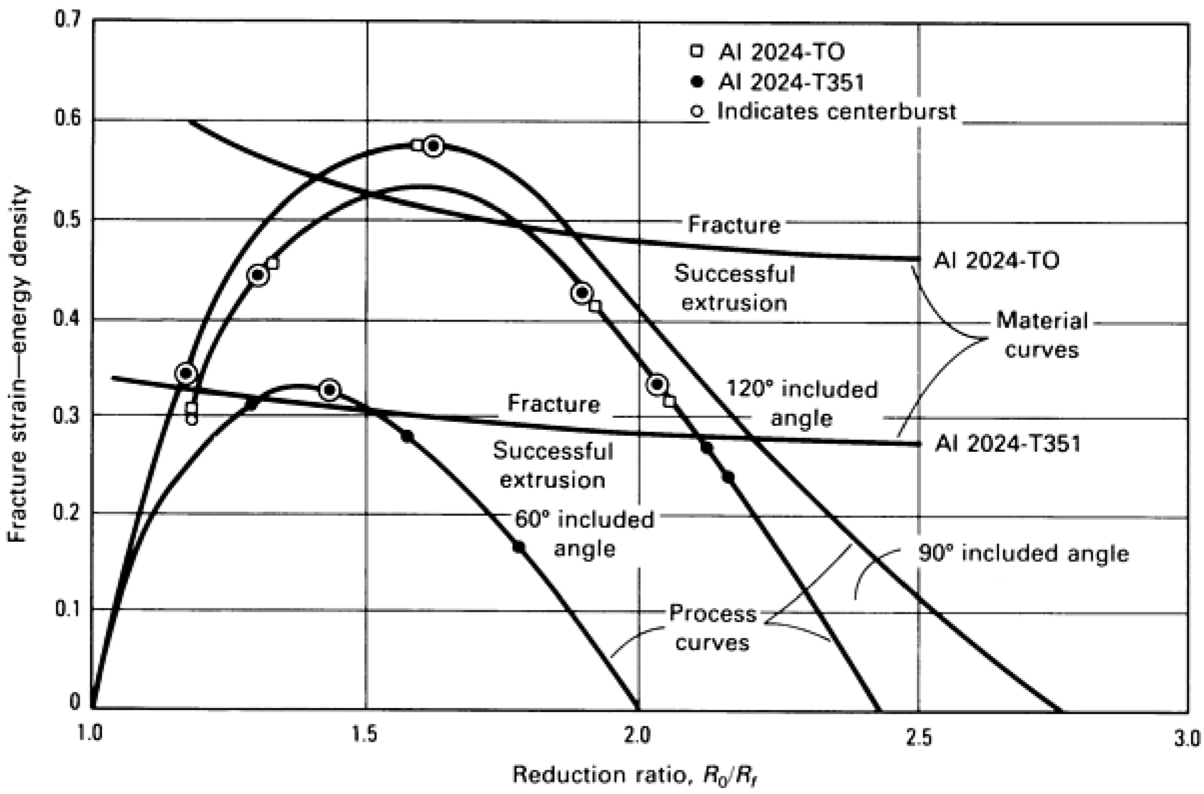

Fig. 16 Workability criteria for centerbursting in aluminum alloy 2024. Based on a maximum tensile stress-strain energy criterion. Source: Ref 11.

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Dynamic Material Modeling

A processing map, such as that shown in Fig. 6, is a very useful guide for the selection of deformation processing conditions. However, processing maps that are constructed from mechanistic models of fracture modes are limited in practical application as a design tool. The analyses are valid only for pure metals and simple alloys, not for complicated engineering materials in which strain-rate sensitivity is a function of temperature and strain rate. Moreover, the numerous material parameters, such as diffusivity, that must be introduced into the models, are difficult to obtain for complicated engineering alloys. Perhaps most important, the location of the boundaries in the processing map is very sensitive to microstructure and to prior thermomechanical history. It is difficult to account for these factors in the mechanistic models.

A top-down approach has been developed that begins with macroscopic determination of flow stress as a function of temperature, strain rate, and strain and ends with a microscopic evaluation of the microstructure and the final properties of the forging (Ref 12). The technique, called dynamic material modeling (DMM), maps the power efficiency of the deformation of the material in a strain rate/temperature space (Fig. 17). At a hot-working temperature, the power per unit volume P absorbed by the workpiece during plastic flow is:

$$P = \bar{\sigma}\,\dot{\bar{\epsilon}} = \int_0^{\dot{\bar{\epsilon}}} \bar{\sigma}\, d\dot{\bar{\epsilon}} + \int_0^{\bar{\sigma}} d\bar{\sigma} \qquad \textbf{(Eq 18)}$$

or

$$P = G + J \qquad \textbf{(Eq 19)}$$

where *G* is the power dissipated by plastic work (most of it converted into heat), and *J* is the dissipator power co-content, which is related to the metallurgical mechanisms that occur dynamically to dissipate power. A strong theoretical basis for this position has been developed from continuum mechanics and irreversible thermodynamics (Ref 13). Figure 18 illustrates the definitions of *G* and *J*. At a given deformation temperature and strain:

$$J = \int_0^{\bar{\sigma}} \dot{\bar{\epsilon}}\, d\,\bar{\sigma} = \frac{\bar{\sigma}\,\dot{\bar{\epsilon}}\,m}{m+1} \qquad \textbf{(Eq 20)}$$

where *m* is the strain rate sensitivity of the material. The value of *J* reaches its maximum when *m* = 1:

$$J_{max} = \frac{\bar{\sigma}\,\dot{\bar{\epsilon}}}{2} \qquad \textbf{(Eq 21)}$$

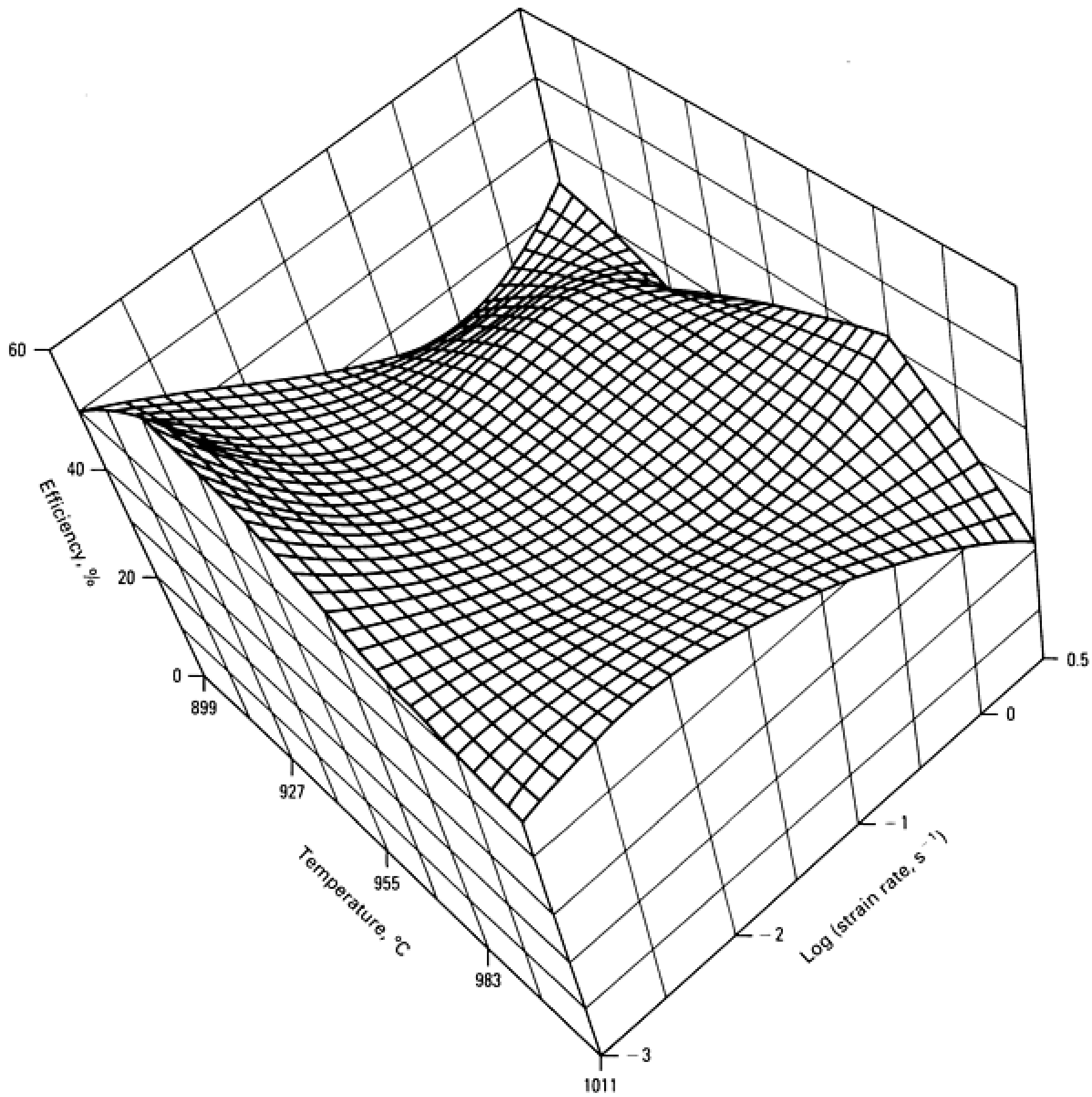

Fig. 17 Three-dimensional plot showing variation of efficiency of power dissipation with temperature and strain rate for Ti-6242 $\alpha + \beta$ preform at 0.6 strain. Source: Ref 13.

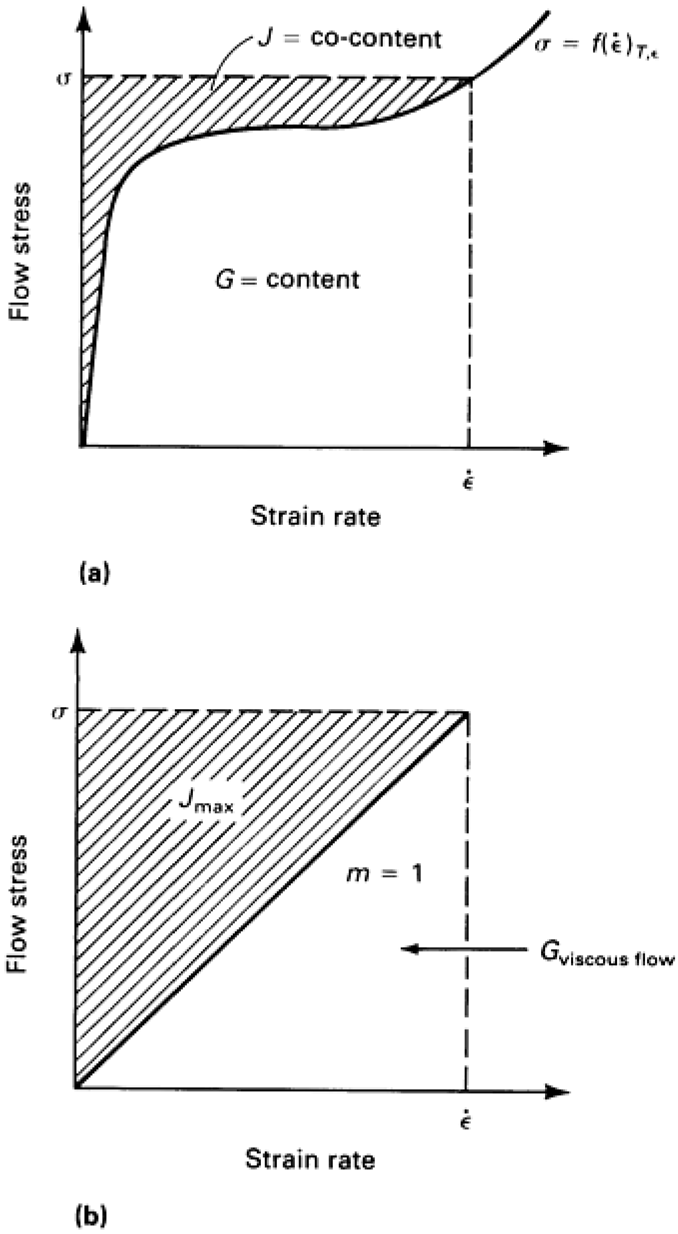

Fig. 18 Schematic of constitutive relation of material system as energy converter (dissipator). (a) Material system as nonlinear (general case) energy dissipater. (b) Material system as linear (special case) dissipator. Source: Ref 13.

A convenient measure of the power dissipation capacity of the materials is the efficiency of dissipation, η:

$$\eta = \frac{J}{J_{max}} = \frac{2m}{m+1} \quad \textbf{(Eq 22)}$$

where $m = (d \ln \overline{\sigma})/(d \ln \dot{\overline{\varepsilon}})$.

By measuring flow stress as a function of strain, strain rate, and temperature and by calculating strain rate sensitivity at each T and $\dot{\epsilon}$(Ref 14, 15) it is possible to determine η from Eq 22. By applying stability theory (Ref 13), it is then possible to establish the loci of bifurcation points as boundaries between safe and unsafe regions on a processing map (Fig. 19). The boundaries, which are determined by Liapunov stability analysis, correspond to a narrow region in which the energy dispersal processes of the material are in a steady state. Processing conditions should be designed to operate in the regions identified as stable. The DMM methodology describes the dynamic path a material element takes in response to an instantaneous change in $\dot{\epsilon}$ at a given T and $\bar{\epsilon}$. As such, it is a map that graphically describes power dissipation by the material in stable and unstable ways. These boundaries correspond to safe and unsafe regions on a processing map. The use of the DMM methodology is in its infancy, but it appears to be a powerful tool for evaluating workability and controlling microstructure by thermomechanical processing in complex alloy systems. More detailed information can be found in the Section "Computer-Aided Process Design for Bulk Forming" in this Volume.

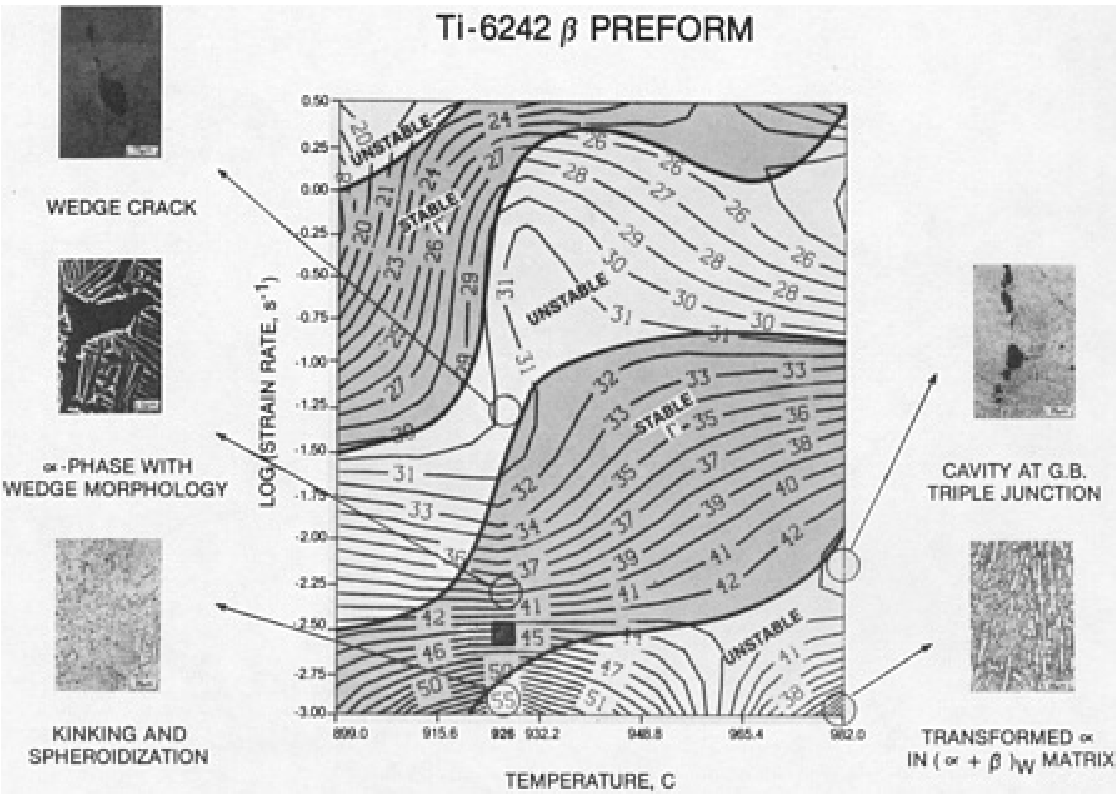

Fig. 19 Processing map for Ti-6242 β microstructure with stable regions identified. Source: Ref 13.

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Introduction to Workability

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Workability Tests

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Introduction

WORKABILITY is a complex property of a material, as indicated in the article "Introduction to Workability" in this Section. It is difficult to isolate the intrinsic workability because this property is strongly influenced by stress state, which is in turn affected by friction and by the geometry of the tools and the workpiece. It has also been shown that the workability of a material is strongly influenced by metallurgical structure and that workability can be a complex function of temperature and strain rate. At the current state of development, the ability to model a forging process by calculating stress, strain, strain rate, and temperature throughout a deforming workpiece with a computer-based finite-element technique exceeds the ability to predict the workability of the material.

A large number of tests are currently used to evaluate the workability of a material. The primary tests--tension, torsion, compression, and bend--will be discussed in this article. These are tests for which the state of stress is well defined and controlled. Of these four tests, the compression test has been the most highly developed as a workability test. The cold upset (compression) test will be described in detail in the article "Workability Theory and Application in Bulk Forming Processes" in this Section.

Specialized workability tests that have been developed from the four primary tests will also be covered. Each of these tests provides information that is not readily available from the primary tests.

A number of workability tests will be discussed that are especially applicable to the forging process. These forgeability tests are used because they lend themselves to the form of material or the particular forging process. Although most workability tests seek to determine the extent of large-scale deformation that is possible before fracture, one class of test is concerned with the propensity for localized deformation and fracture.

Finally, this article will conclude with a discussion of typical forging defects. Although not strictly related to workability tests, forging defects certainly represent a limitation to workability.

Workability Tests

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Primary Tests

The primary tests for workability are those for which the stress state is well-known and controlled. Generally, these are small-scale laboratory simulation tests.

The tension test is widely used to determine the mechanical properties of a material (Ref 1). Uniform elongation, total elongation, and reduction in area at fracture are frequently used as indices of ductility. However, the extent of deformation possible in a tension test is limited by the formation of a necked region in the tension specimen. This introduces a triaxial tensile stress state and leads to fracture.

For most metals, the uniform strain that precedes necking rarely exceeds a true strain of 0.5. For hot-working temperatures, this uniform strain is frequently less than 0.1. Although tension tests are easily performed, necking makes control of strain rate difficult and leads to uncertainties about the value of strain at fracture because of the complex stresses that result from necking. Therefore, the utility of the tension test is limited in workability testing. This test is primarily used under special high strain rate, hot tension test conditions to establish the range of hot-working temperatures. A description of this test method can be found later in this article.

In the torsion test, deformation is caused by pure shear, and large strains can be achieved without the limitations imposed by necking (Ref 2, 3). Because the strain rate is proportional to rotational speed, high strain rates are readily obtained (Table 1). Moreover, friction has no effect on the test, as it does in compression testing. The stress state in torsion may represent the typical stress in metalworking processes, but deformation in the torsion test is not an accurate simulation of metalworking processes, because of excessive material reorientation at large strains.

Table 1 Torsional rotation rates corresponding to various metalworking operations

Operation	**von Mises effective strain rate ($\dot{\bar{\epsilon}}$)[a], s^{-1}**	**Corresponding surface shear strain rate in torsion ($\dot{\Gamma}$), s^{-1}**	Rotation rate[b], rpm
Isothermal forging	10^{-3}	1.73×10^{-3}	**0.02**
Hydraulic press forging	1	1.73	**16.5**
Extrusion	20	34.6	**330.4**
Mechanical press forging	50	86.6	**827.0**
Sheet rolling	200	346.4	**3307.9**

Wire drawing	**500**	**866.0**	**8269.7**

(a) $\dot{\epsilon} = \dot{\Gamma}\sqrt{3}$

(b) Assuming specimen geometry with $r/L = 1.0$

Because of the above advantages, the torsion test is frequently used to measure the flow stress and the stress-strain curve (flow curve) under hot-working conditions. Figure 1 shows typical flow curves as a function of temperature and strain rate. In the torsion test, measurements are made of the torque, *M*, to deform the specimen and the angle of twist θor number of turns ($\theta = 2\pi$ rad per turn). The shear stress τ on the outer surface of the specimen is given by:

$$\tau = \frac{M(3 + m + n)}{2\pi r^3}$$

where *r* is the specimen radius, *m* is the strain rate sensitivity found from plots of log *M* versus log $\dot{\theta}$at fixed values of θ, and *n* is the strain-hardening exponent obtained from the instantaneous slope of log *M* versus log θ.

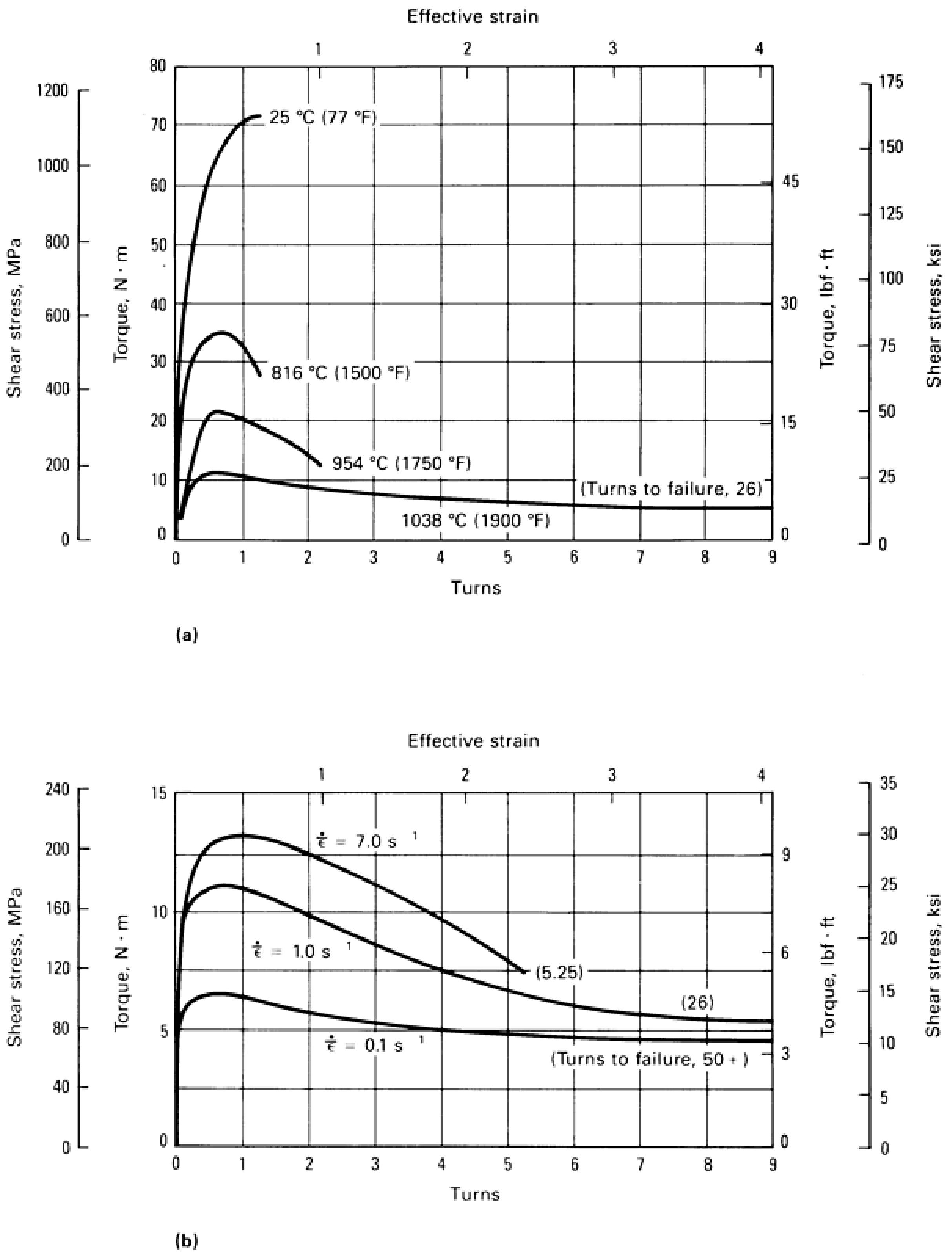

Fig. 1 Flow curves for Waspaloy. (a) Effect of temperature at a fixed effective strain rate of 1 s^{-1}. (b) Effect of strain rate at a fixed test temperature of 1038 °C (1900 °F). Flow softening at the higher temperature is a result of dynamic recrystallization. Source: Ref 4.

The engineering shear strain Γ and shear strain rate $\dot{\Gamma}$ are given by:

$$\Gamma = \frac{r\theta}{L}$$

and

$$\dot{\Gamma} = \frac{r\dot{\theta}}{L}$$

where *r* is the radius of the specimen and *L* is the gage length. These values of shear stress and shear strain are typically converted to effective stress $\overline{\sigma}$ and effective strain $\overline{\epsilon}$ by means of the von Mises yielding criterion (see the article "Introduction to Workability" in this Section):

$$\overline{\sigma} = \sqrt{3}\tau$$

and

$$\bar{\epsilon} = \frac{\Gamma}{\sqrt{3}}$$

Figure 2 shows agreement in plots of $\overline{\sigma}$ versus $\overline{\epsilon}$ for stress-strain data determined in torsion, tension, and compression. The agreement becomes much better at hot-working temperatures.

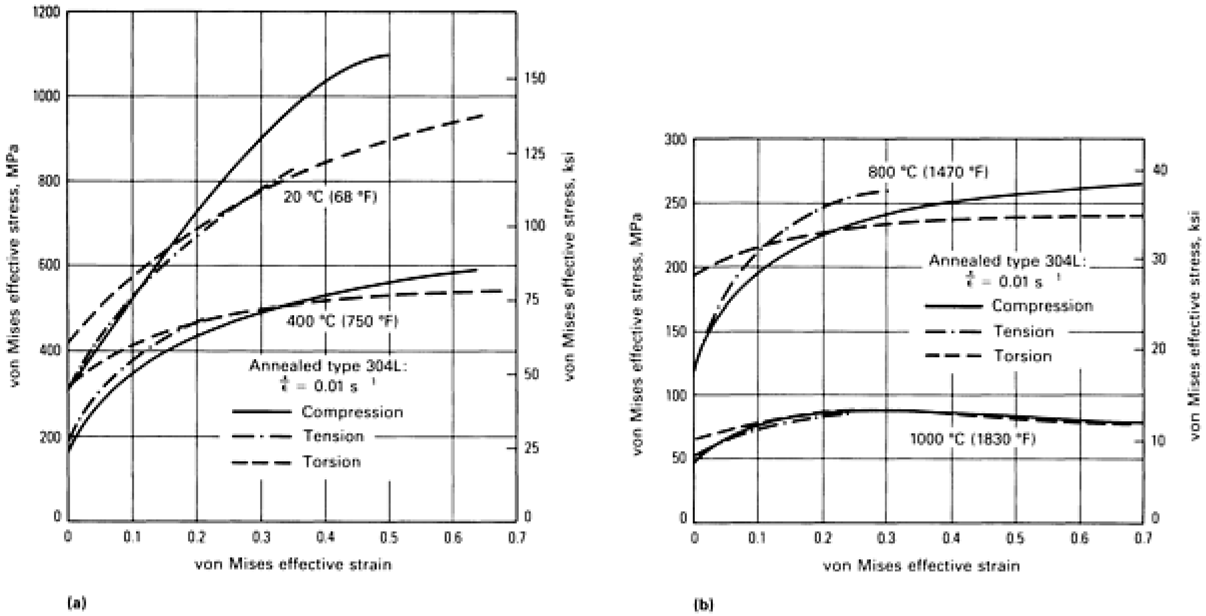

Fig. 2 Comparison of effective stress-strain curves determined for type 304L stainless steel in compression, tension, and torsion. (a) Cold- and warm-working temperatures. (b) Hot-working temperatures. Source: Ref 2.

Fracture data from torsion tests are usually reported in terms of the number of twists to failure or the surface fracture strain to failure. Figure 3 shows the relative hot workability of a number of steels and nickel-base superalloys, as indicated by the torsion test. The test identifies the optimal hot-working temperature.

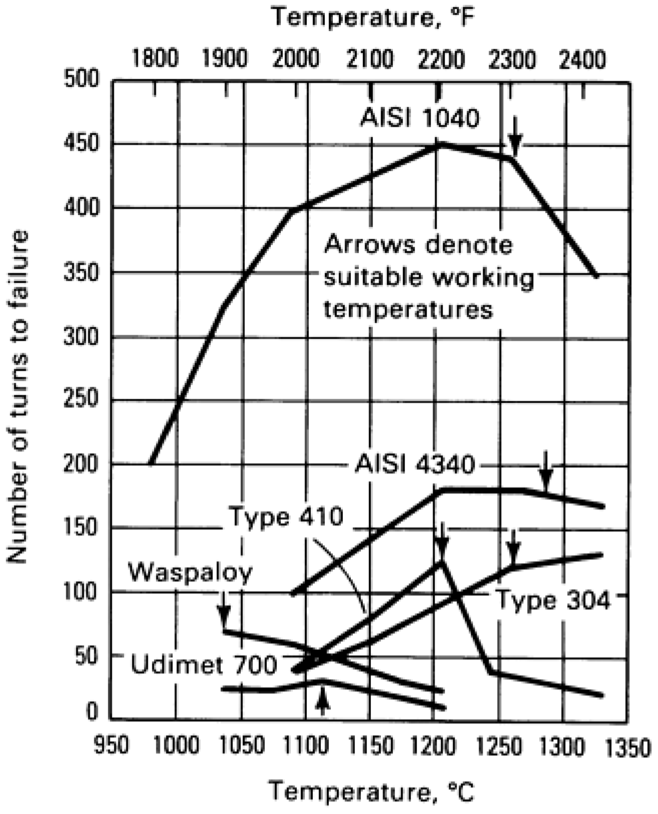

Fig. 3 Ductility determined in hot torsion tests. Source: Ref 2.

The compression test, in which a cylindrical specimen is upset into a flat pancake, is usually considered to be a standard bulk workability test. The average stress state during testing is similar to that in many bulk deformation processes, without introducing the problems of necking (in tension) or material reorientation (in torsion). Therefore, a large amount of deformation can be achieved before fracture occurs. The stress state can be varied over wide limits by controlling the barreling of the specimen through variations in geometry and by reducing friction between the specimen ends and the anvil with lubricants.

Compression testing has developed into a highly sophisticated test for workability in cold upset forging, and it is a common quality control test in hot-forging operations. Compression forging is a useful method of assessing the frictional conditions in hot working. The principal disadvantage of the compression test is that tests at a constant, true strain rate require special equipment.

Compression Test Conditions. Unless the lubrication at the ends of the specimen is very good, frictional restraint will retard the outward motion of the end face, and part of the end face will be formed by a folding over of the sides of the original cylinder onto the end face in contact with the platens. The barreling that results introduces a complex stress state, which is beneficial in fracture testing but detrimental when the compression test is used to measure flow stress. The frictional restraint also causes internal inhomogeneity of plastic deformation. Slightly deforming zones develop adjacent to the platens, while severe deformation is concentrated in zones that occupy roughly diagonal positions between opposing edges of the specimen (see Fig. 7 in the article "Introduction to Workability" in this Section).

Figure 4 shows the hot upsetting of a cylinder under conditions of poor lubrication in which the platens are cooler than the specimen. The cooling at the ends restricts the flow so that the deformation is concentrated in a central zone, with dead-metal zones forming adjacent to the platen surfaces (Fig. 4a).

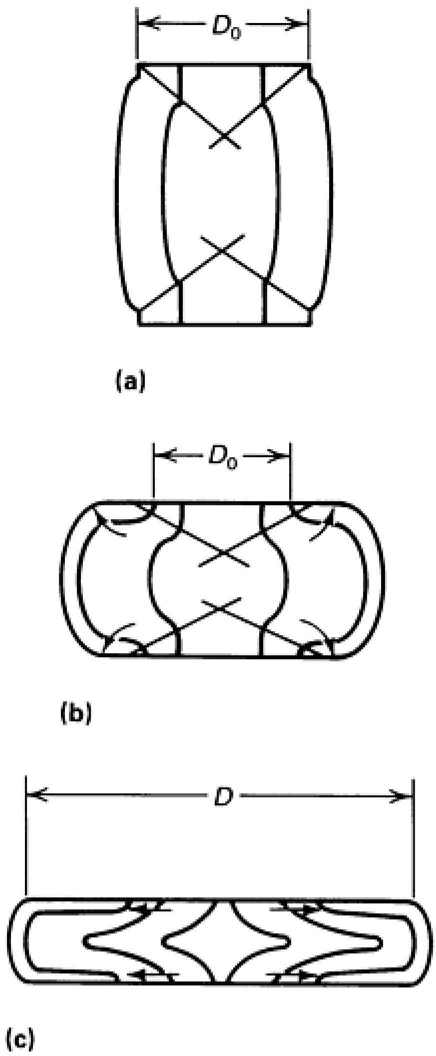

Fig. 4 Deformation patterns in nonlubricated, nonisothermal hot forging. (a) Initial barreling. (b) Barreling and folding over. (c) Beginning of end face expansion. Source: Ref 5.

As deformation proceeds, severe inhomogeneity develops, and the growth of the end faces is attributed entirely to the folding over of the sides (Fig. 4b). When the diameter-to-height ratio, D/h, exceeds about 3, expansion of the end faces occurs (Fig. 4c).

The conditions described above are extreme and should not be allowed to occur in hot compression testing unless the objective is to simulate cracking under forging conditions. Adequate lubrication cannot improve the situation so that homogeneous deformation occurs; however, with glass lubricants and isothermal conditions, it is possible to conduct hot compression testing without appreciable barreling (Ref 6). Isothermal test conditions can be achieved by using a heated subassembly, such as that shown in Fig. 5, or heated dies that provide isothermal conditions (Ref 8).

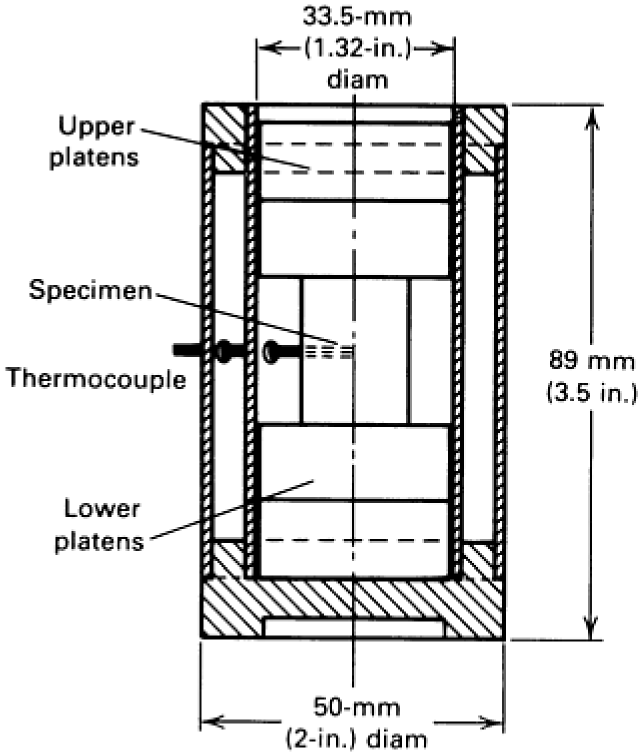

Fig. 5 Heated subassembly with specimen in position used to achieve isothermal test conditions. Thermocouple is removed prior to compression. Source: Ref 7.

The true strain rate in a compression test is:

$$\dot{\epsilon} = \frac{d\epsilon}{dt} = \frac{-dh/h}{dt} = -\frac{1}{h}\frac{dh}{dt} = -\frac{v}{h}$$

where *v* is the velocity of the platen and *h* is the height of the specimen at time *t*. Because *h* decreases continuously with time, the velocity must decrease in proportion to (-*h*) if $\dot{\epsilon}$is to be held constant. In a normal test, if *v* is held constant, the engineering strain rate $\dot{e}$will remain constant:

$$\dot{e} = \frac{de}{dt} = \frac{-dh/h_0}{dt} = -\frac{1}{h_0}\frac{dh}{dt} = \frac{-v}{h_0}$$

The true strain rate, however, will not be constant. A machine called a cam plastometer can be used to cause the bottom platen to compress the specimen through cam action at a constant true strain rate to a strain limit of ϵ= 0.7 (Ref 9). The use of cam plastometers is limited; there probably are not more than ten in existence. However, an essentially constant true strain rate can be achieved on a standard closed-loop servo-controlled testing machine. Strain rates up to 20 s^{-1} have been achieved (Ref 6, 10). The history of the cam plastometer, the basic principles involved in the technique, and the equipment used are discussed in the article "High Strain Rate Compression Testing" in *Mechanical Testing*, Volume 8 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

When a constant true strain rate cannot be obtained, the mean strain rate may be adequate. The mean true strain rate, $\langle\dot{\bar{\epsilon}}\rangle$, for constant velocity v_o, when the specimen is reduced in height from h_0 to *h*, is given by:

$$\langle\dot{\epsilon}\rangle = \frac{v_0}{2}\frac{\ln(h_0/h)}{(h_0 - h)}$$

Flow Stress in Compression. Ideally, the determination of flow stress in compression should be carried out under isothermal conditions (no die chilling) at a constant strain rate and with a minimum of friction in order to minimize barreling. These conditions can be met with conventional servohydraulic testing machines. For an essentially homogeneous upsetting test, a cylinder of diameter D_0 and initial height h_0, will be compressed to height h and spread out to diameter D_1 according to the law of constancy of volume:

$$D_0^2 h_0 = D^2 h$$

If friction can be neglected, the uniaxial compressive stress (flow stress) corresponding to a deformation force P is:

$$\sigma_0 = \frac{P}{A} = \frac{4P}{\pi D^2} = \frac{4Ph}{\pi D_0^2 h_0}$$

If substantial friction is present, the average pressure, $\overline{p}$, required to deform the cylinder is greater than the flow stress of the material, σ_0:

$$\frac{\overline{p}}{\sigma_0} = \left(\frac{h}{4\mu a}\right)^2 \left(e^{2\mu a/h} - \frac{2\mu a}{h} - 1\right)$$

where a is the radius of the cylinder, and μ is the Coulomb coefficient of friction. The true compressive strain is given by:

$$\epsilon = \ln\left(\frac{h_0}{h}\right)$$

The effects of friction and die chilling can be minimized through the use of a long, thin specimen. Therefore, most of the specimen volume is unaffected by the dead-metal zones at the platens. However, this approach is limited, because buckling of the specimen will occur if h/D exceeds about 2.

An extrapolation method involves testing cylinders of equal diameters but varying heights so that the D_0/h_0 ratio ranges from about 0.5 to 3.0 (Ref 11). A specific load is applied to the specimen, the load is removed, and the new height is determined in order to calculate a true strain. Upon relubrication, the specimen is subjected to an increased load, unloaded, and measured. The cycle is then repeated.

The same test procedure is followed with each specimen so that the particular load levels are duplicated. The results are illustrated in Fig. 6. For the same load, the actual strain (due to height reduction) is plotted against the D_0/h_0 ratio for each test cylinder. A line drawn through the points is extrapolated to a value of $D_0/h_0 = 0$. This would be the anticipated ratio for a specimen of infinite initial height for which the end effects would be restricted to a small region of the full test height. The true stress corresponding to each of these true strains is given by:

$$\sigma_0 = \frac{4Ph}{\pi D_0^2 h_0}$$

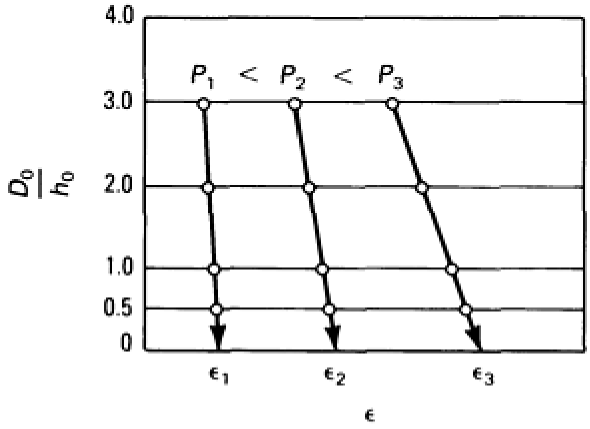

Fig. 6 Extrapolation method to correct for end effects in compressive loading. Source: Ref 11.

Ductility Testing. The basic hot ductility test consists of compressing a series of cylindrical or square specimens to various thicknesses, or to the same thickness with varying specimen length-to-diameter (length-to-width) ratios. The limit for compression without failure by radial or peripheral cracking is considered to be a measure of workability. This type of test has been widely used in the forging industry. Longitudinal notches are sometimes machined into the specimens before compression, because the notches apparently cause more severe stress concentrations, thus providing a more reliable index of the workability to be expected in a complex forging operation.

Plastic Instability in Compression. Several types of plastic instabilities can be developed in the compression test. The first type is associated with a maximum in the true stress-strain curve. The second type concerns inhomogeneous deformation and shear band formation. Figure 7 shows the type of plastic instability that occurs in some materials in hot compression testing. At certain temperatures and strain rates, some of the typical strengthening mechanisms become unstable. Because the rate of flow softening exceeds the rate of area increase as the specimen is compressed, a maximum results in the flow stress curve.

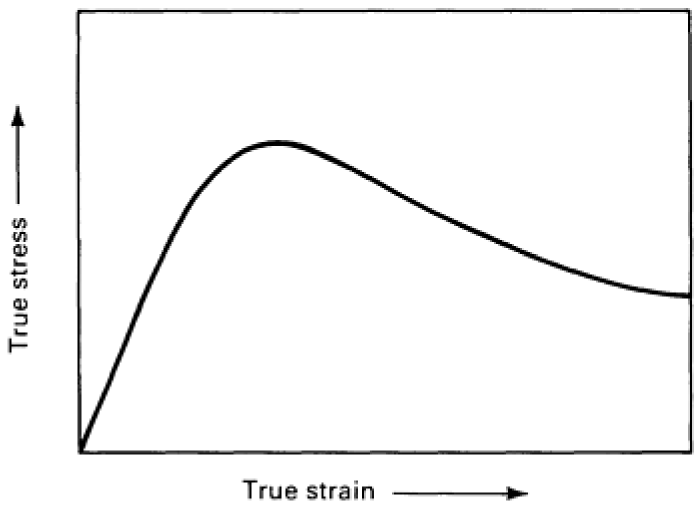

Fig. 7 Example of compressive flow stress curve showing strain softening.

Analysis of the compression process indicates that the plastic deformation is stable (no maximum in the flow curve) as long as $(\gamma + m) \leq 1$, where γ is the dimensionless work-hardening coefficient, and m is strain rate sensitivity. Both of these material parameters are defined below (Ref 12, 13). A material with a high strain rate sensitivity is more resistant to flow localization in the tension test (necking), but in compression testing, a higher rate sensitivity leads to earlier flow localization.

Flow softening or negative strain hardening can also produce flow localization effects in compression independently of the effects of die chilling or high friction. The constant strain rate, isothermal hot compression test is useful for detecting and predicting flow localization. Nonuniform flow in compression is likely if a flow parameter α_c exceeds a certain value:

$$\alpha_c = -\frac{(\gamma - 1)}{m} \geq 5$$

where

$$\gamma = \left(\frac{1}{\sigma}\right)\left(\frac{d\sigma}{d\epsilon}\right)$$

and

$$m = \frac{\partial \ln \sigma}{\partial \ln \dot{\epsilon}}\bigg|_{T,\epsilon} \simeq \frac{\Delta \log \sigma}{\Delta \ln \dot{\epsilon}}\bigg|_{T,\epsilon}$$

Figure 8 illustrates the differences in deformation of titanium alloy samples. The specimens in Fig. 8(a) to (c) were deformed at a temperature at which α_c was high. In Fig. 8(a), $\dot{\epsilon} = 10^{-3}$ s^{-1} and $\alpha_c = 2$. In Fig. 8(b), $\dot{\epsilon} = 10^{-1}$ s^{-1} and $\alpha_c = 5$. In Fig. 8(c), $\dot{\epsilon} = 10$ s^{-1} and $\alpha_c = 5$. However, the specimens in Fig. 8(d) to (f) were deformed at a temperature at which α_c was less than 0.

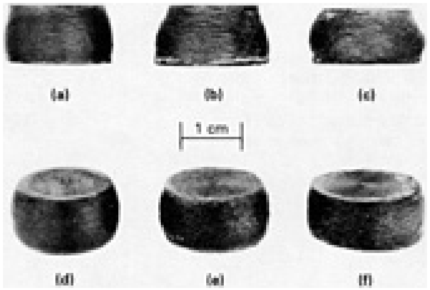

Fig. 8 Specimens of Ti-10V-2Fe-3Al from isothermal hot compression tests. (a) to (c) Tested at 704 °C (1300 °F). (d) to (f) Tested at 816 °C (1500 °F). Strain rates were 10^{-3} s^{-1} (a, d), 10^{-1} s^{-1} (b, e), and 10 s^{-1} (c, f). Before testing, the alloy had been β annealed to yield an equiaxed β starting microstructure. Source: Ref 14.

The bend test is useful for assessing the workability of thick sheet and plate. Generally, this test is most applicable to cold-working operations. Figure 9 shows a plate deformed in three-point bending. The principal stress and strains developed during bending are defined in Fig. 10. The critical parameter is width-to-thickness ratio (w/t). If $w/t > 8$, bending occurs under plane-strain conditions ($\epsilon_2 = 0$) and $\sigma_2/\sigma_1 = 0.5$. If $w/t > 8$, the bend ductility is independent of the exact w/t ratio. If $w/t < 8$, then stress state and bend ductility depend strongly on the width-to-thickness ratio.

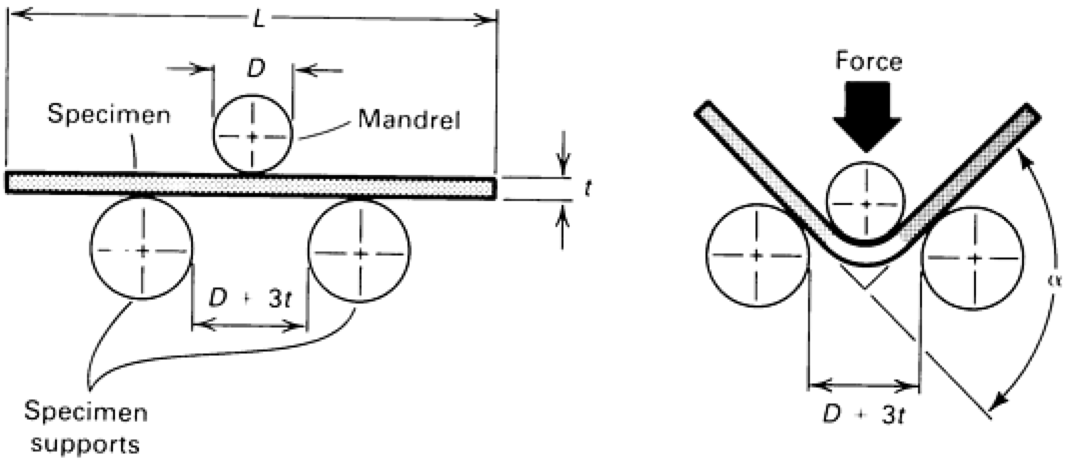

Fig. 9 Three-point bend test.

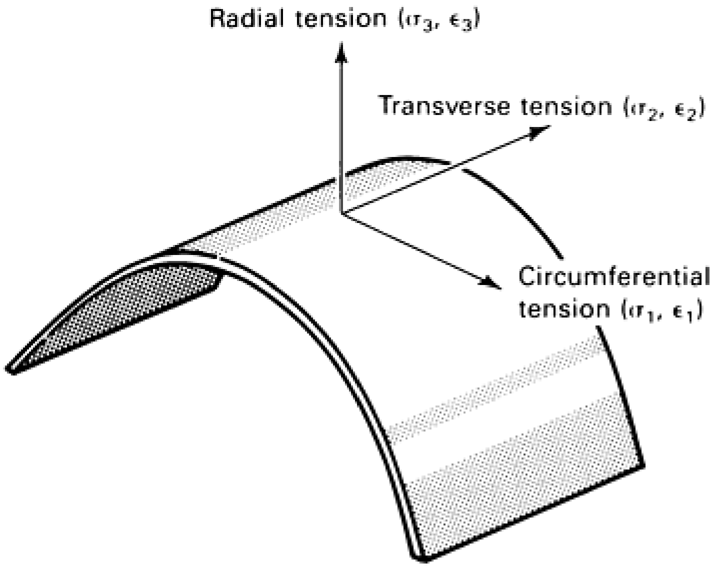

Fig. 10 Schematic of the bend region defining direction of principal stresses and strains.

For pure plastic bending, in which elastic deformation can be ignored, the maximum tensile fiber strain is (Ref 15):

$$\epsilon_0 = \ln \sqrt{\frac{R_o}{R_i}}$$

where R_o is the radius of curvature on the outer (tensile) surface and R_i is the radius of curvature on the inner (compressive) surface. When this strain is entered into the stress-strain equation or curve for the material, it gives the flow stress for the material $\overline{\sigma}$. Because of the plane-strain condition, the maximum fiber stress is $2\overline{\sigma}/\sqrt{3}$.

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Workability Tests

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Specialized Tests

In the plane-strain compression test, the difficulties encountered with bulging and high friction at the platens in the compression of cylinders can be minimized (Ref 11). As shown in Fig. 11, the specimen is a thin plate or sheet that is compressed across the width of the strip by narrow platens that are wider than the strip. The elastic constraints of the undeformed shoulders of material on each side of the platens prevent extension of the strip in the width dimension; hence the term plane strain.

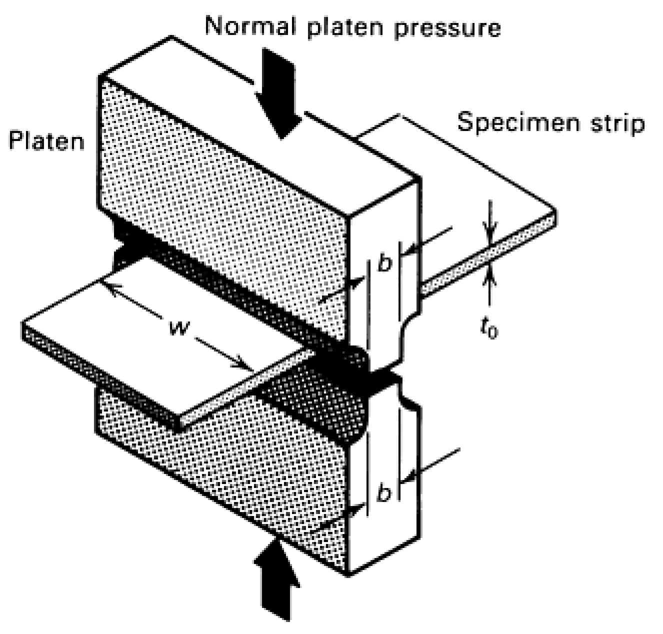

Fig. 11 Plane-strain compression test.

Deformation occurs in the direction of platen motion and in the direction normal to the length of the platen. To ensure that lateral spread is negligible, the width of the strip should be at least six to ten times the breadth of the platens. To ensure that deformation beneath the platens is essentially homogeneous, the ratio of platen breadth to strip thickness (*b*/*t*) should be between 2 and 4 at all times. It may be necessary to change the platens during testing to maintain this condition. True strains of 2 can be achieved by carrying out the test in increments in order to provide good lubrication and to maintain the proper *b*/*t* ratio. Although the plane-strain compression test is primarily used to measure flow properties at room temperature, it can also be used for elevated-temperature tests (Ref 16, 17).However, because of its geometry, this test is more applicable to rolling operations than to forging.

The true stress and true strain determined from the plane-strain compression test can be expressed as:

$$p = \frac{P}{wb}$$

$$\epsilon_{pc} = \ln \frac{t_0}{t}$$

Because of the stress state associated with plane-strain deformation, the mean pressure on the platens is 15.5% higher in the plane-strain compression test than in uniaxial compression testing. The true stress-strain curve in uniaxial compression (σ_0 versus ε) can be obtained from the corresponding plane-strain compression curve (p versus ϵ_{pc}) by:

$$\sigma_0 = \frac{\sqrt{3}}{2} p = \frac{p}{1.155}$$

and

$$\epsilon = \frac{2}{\sqrt{3}} \epsilon_{pc} = 1.155\ \epsilon_{pc}$$

The partial-width indentation test is a new test for evaluating the workability of metals. It is similar to the plane-strain compression test, but it does not subject the test specimen to true plane-strain conditions (Ref 18). In this test, a simple slab-shaped specimen is deformed over part of its width by two opposing rectangular anvils having widths smaller than that of the specimen.Upon penetrating the workpiece, the anvils longitudinally displace metal from the center, creating overhangs (ribs) that are subjected to secondary, nearly uniaxial tensile straining. The material ductility under these conditions is indicated by the reduction in the rib height at fracture. The test geometry has been standardized (Fig. 12).

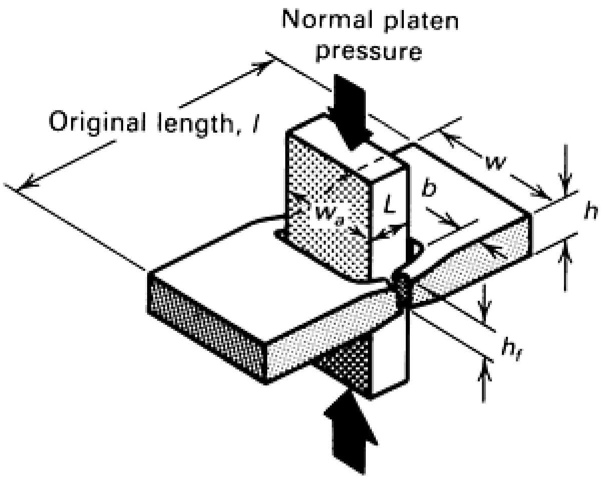

Fig. 12 Partial-width indentation test. $L \simeq h$; $b = h/2$ $w_a = 2L$; $l = 4L$.

One advantage of this test is that it uses a specimen of simple shape. In addition, ascast materials can be readily tested. One edge of the specimen can contain original surface defects. The test can be conducted hot or cold. Therefore, the partial-width indentation test is suitable not only for determining the intrinsic ductilities of materials but also for evaluating the inhomogeneous aspects of workability. This test has been used to establish the fracture-limit loci for ductile metals (Ref 19).

The secondary-tension test, a modification of the partial-width indentation test, imposes more severe strain in the rib for testing highly ductile materials. In this test, a hole or a slot is machined in the slab-type specimen adjacent to where the anvils indent the specimen. Preferred dimensions of the hole and slot are given in Fig. 13. With this design, the ribs are sufficiently stretched to ensure fracture in even the most ductile materials. The fracture strain is based on reduction in area where the rib is cut out, so that the fracture area can be photographed or traced on an optical comparator.

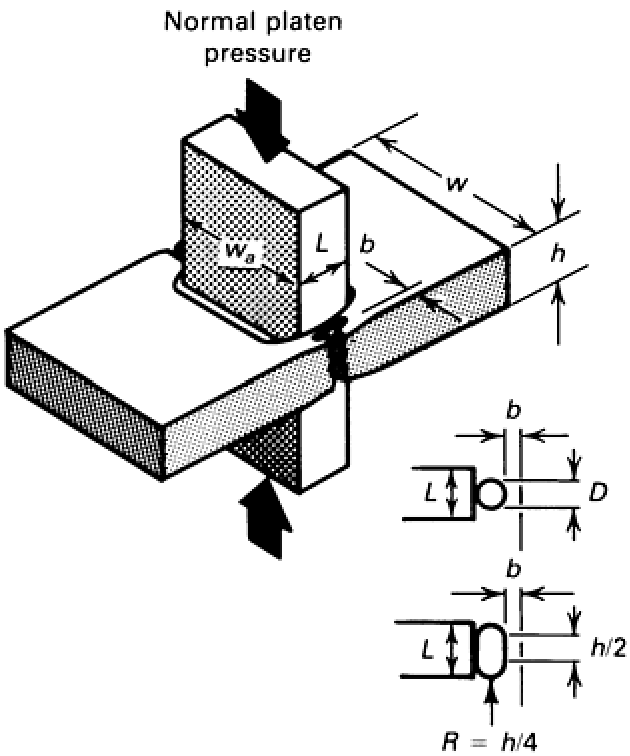

Fig. 13 Secondary-tension test showing the geometries of holes and slots $L \simeq h$; $w_a \geq 2h$; $b = h/4$; $D = h/2$.

Ring Compression Test. When a flat ring-shaped specimen is upset in the axial direction, the resulting change in shape depends only on the amount of compression in the thickness direction and the frictional conditions at the die/ring interfaces. If the interfacial friction were zero, the ring would deform in the same manner as a solid disk, with each element flowing outward radially at a rate proportional to its distance from the center.

In the case of small, but finite, interfacial friction, the outside diameter is smaller than in the zero-friction case. If the friction exceeds a critical value, frictional resistance to outward flow becomes so high that some of the ring material flows inward to the center. Measurements of the inside diameters of compressed rings provide a particularly sensitive means of studying interfacial friction, because the inside diameter increases if the friction is low and decreases if the friction is higher (Fig. 14).

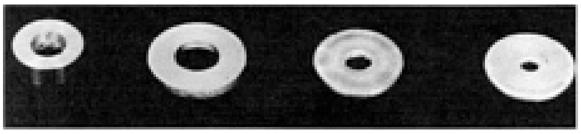

Fig. 14 Variation in shape of ring test specimens deformed the same amount under different frictional conditions. Left to right: undeformed specimen; deformed 50%, low friction; deformed 50%, medium friction; deformed 50%, high friction.

The ring test, then, is a compression test with a built-in frictional measurement. Therefore, it is possible to measure the ring dimensions and compute both the friction value and the basic flow stress of the ring material at the strain under the given deformation conditions.

Analysis of Ring Compression. The mechanics of the compression of flat ring-shaped specimens between flat dies have been analyzed using an upper bound plasticity technique (Ref 20, 21). Values of p/σ_0 (where p is the average forging pressure on the ring, and σ_0 is the flow stress of the ring material) can be calculated in terms of ring geometry and the interfacial shear factor, m. In these calculations, neither σ_0 nor the interfacial shear stress, τ, appears in terms of independent absolute values, but only as the ratio m (see the article "Introduction to Workability" in this Section).

The analysis assumes that this ratio remains constant for a given material and deformation conditions. If the analysis is carried out for a small increment of deformation, σ_0 and τ can be assumed to be approximately constant for this increment, and the solution is valid. Therefore, if the shear factor m is constant for the entire operation, the mathematical analysis can be continued in a series of small deformation increments, using the final ring geometry from one increment as the initial geometry for the subsequent increment. As long as the ratio of the interfacial shear stress, τ, to the material flow stress, σ_0, remains constant, strain hardening of the ring material during deformation has no effect if the increase in work hardening in any single deformation increment can be neglected.

The progressive increase in interfacial shear stress accompanying strain hardening is also immaterial if it can be assumed to be constant over the entire die/ring interface during any one deformation increment. Therefore, the analysis can be justifiably applied to real materials even though it was initially assumed that the material would behave according to the von Mises stress-strain rate laws, provided the assumption of a constant interfacial shear factor, m, is correct. However, it has been shown that a highly strain rate sensitive material requires a different analysis (Ref 22).

Based on these assumptions, the plasticity equations have been solved for several ring geometries over a complete range of m values from 0 to unity (Ref 23), as shown in Fig. 15. The friction factor can be determined by measuring the change in internal diameter of the ring.

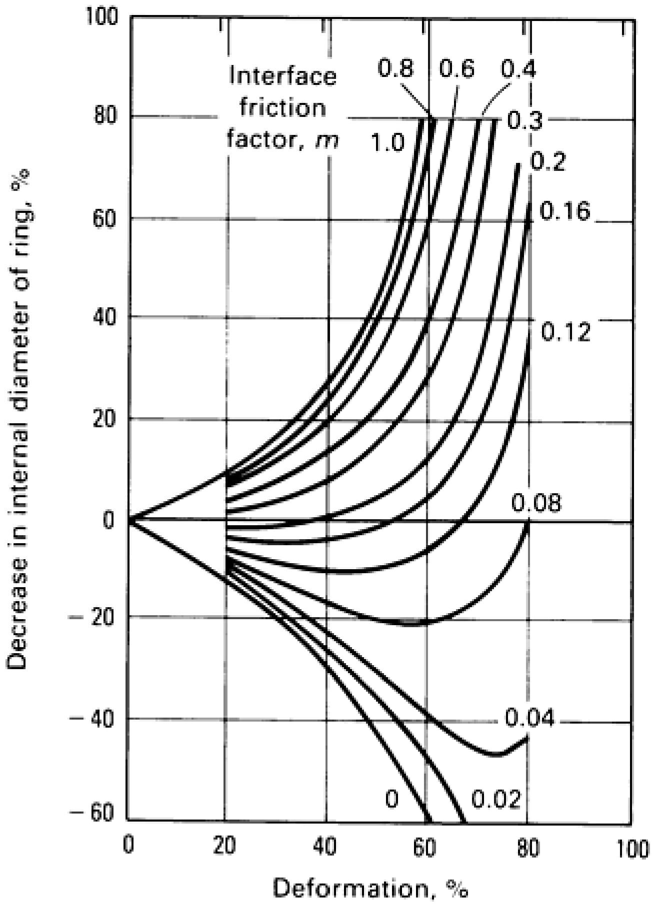

Fig. 15 Theoretical calibration curve for standard ring with an OD:ID:thickness ratio of 6:3:2.

The ring thickness is usually expressed in relation to the inside and outside diameters. The maximum thickness that can be used while still satisfying the mathematical assumption of thin-specimen conditions varies, depending on the actual friction conditions. Under conditions of maximum friction, the largest usable specimen height is obtained with rings of dimensions in the OD:ID:thickness ratio of 6:3:1. Under conditions of low friction, thicker specimens can be used while still satisfying the above assumption. For normal lubricated conditions, a geometry of 6:3:2 can be used to obtain results of sufficient accuracy for most applications.

For experimental conditions in which specimen thicknesses are greater than those permitted by a geometry of 6:3:1 and/or the interface friction is relatively high, the resulting side barreling or bulging must be considered. Analytical treatment of this more complex situation is available in Ref 24.

The ring compression test can be used to measure the flow stress under high-strain practical forming conditions. The only instrumentation required is that for measuring the force needed to produce the reduction in height. The change in diameter of the 6:3:1 ring is measured to obtain a value of the ratio p/σ_0 by solving the analytical expression for the deformation of the ring or by using computer solutions for the ring (Ref 25). Measurement of the area of the ring surface formerly in contact with the die and knowledge of the deformation load facilitate calculation of p and therefore the value of the material flow stress, σ_o, for a given amount of deformation. Repetition of this process with other ring specimens over a range of deformation allows the generation of a complete flow stress-strain curve for a given material under particular temperature and strain rate deformation conditions.

Hot Tension Testing. Although necking is a fundamental limitation in tension testing, the tension test is nevertheless useful for establishing the temperature limits for hot working. The principal advantage of this test for industrial applications is that it clearly establishes maximum and minimum hot-working temperatures (Ref 26).

Most commercial hot tensile testing is done with a Gleeble unit, which is a high strain rate, high-temperature testing machine (Ref 27). A solid buttonhead specimen that has a reduced diameter of 6.4 mm (0.250 in.) and an overall length of 89 mm (3.5 in.) is held horizontally by water-cooled copper jaws (grips), through which electric power is introduced to resistance heat the test specimen (Fig. 16). Specimen temperature is monitored by a thermocouple welded to the specimen surface at its midlength. The thermocouple, with a function generator, controls the heat fed into the specimen according to a programmed cycle. Therefore, a specimen can be tested under time-temperature conditions that simulate hot-working sequences.

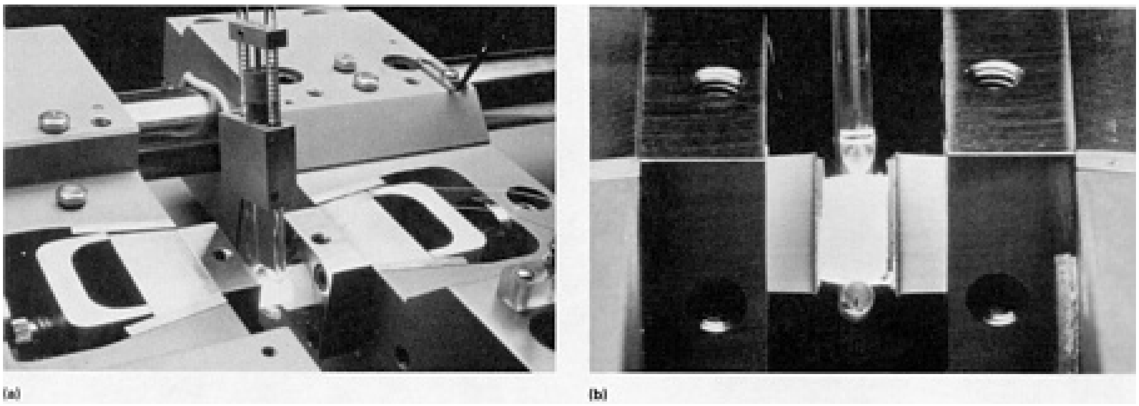

Fig. 16 The Gleeble test unit used for hot tension and compression testing. (a) Specimen in grips showing attached thermocouple wires and linear variable differential transformer for measuring strain. (b) Close-up of a compression test specimen. Courtesy of Duffers Scientific, Inc.

The specimen is loaded by a pneumatic-hydraulic system. The load can be applied at any desired time in the thermal cycle. Temperature, load, and crosshead displacement are measured as a function of time. In the Gleeble test, the crosshead speed can be maintained constant throughout the test, but the true strain rate decreases until necking occurs, according to the relationship:

$$\dot{\epsilon} = \frac{d\epsilon}{dt} = \frac{1}{L}\frac{dL}{dt}$$

When the specimen necks, the strain rate increases suddenly in the deforming region, because deformation is concentrated in a narrow zone. Although this variable strain rate history introduces some uncertainty into the determination of strength and ductility values, it does not negate the utility of the hot tension test. Moreover, a procedure has been developed that corrects for the change in strain rate with strain so that stress-strain curves can be constructed (Ref 28).

The percent reduction in area is the primary result obtained from the hot tension test. This measure of ductility is used to assess the ability of the material to withstand crack propagation. Reduction in area adequately detects small ductility variations in materials caused by composition or processing when the material is of low-to-moderate ductility. It does not reveal small ductility variations in materials of very high ductility.

A general qualitative rating scale between reduction in area and workability is given in Table 2. This correlation was originally based on superalloys. In addition to ductility measurement, the ultimate tensile strength can be determined with the Gleeble test. This gives a measure of the force required to deform the material.

Table 2 Qualitative hot-workability ratings for specialty steels and superalloys

Hot tensile reduction in area[a], %	**Expected alloy behavior under normal hot reductions in open-die forging or rolling**	Remarks regarding alloy hot-working practice
<30	Poor hot workability, abundant cracks	**Preferably not rolled or open-die forged; extrusion may be feasible; rolling or forging should be attempted only with light reductions, low strain rates, and an insulating coating.**
30-40	Marginal hot workability, numerous cracks	**This ductility range usually signals the minimum hot-working temperature; rolled or press forged with light reductions and lower-than-usual strain rates**

40-50	Acceptable hot workability, few cracks	**Rolled or press forged with moderate reductions and strain rates**
50-60	Good hot workability, very few cracks	**Rolled or press forged with normal reductions and strain rates**
60-70	Excellent hot workability, occasional cracks	**Rolled or press forged with heavier reductions and higher strain rates than normal if desired**
>70	**Superior hot workability, rare cracks. Ductile ruptures can occur if strength is too low.**	**Rolled or press forged with heavier reductions and higher strain rates than normal if alloy strength is sufficiently high to prevent ductile ruptures**

Source: Ref 26

(a) Ratings apply for Gleeble tension testing of 6.4-mm (0.250 in.) diam specimens with 25-mm (1 in.) head separation.

Hot Tension Test Procedure Variations. Two variations of the hot tension test can be used to establish the temperature limits of hot working: on-heating tests and on-cooling tests. The on-heating test method is used for a material for which little or no hot-working information is available. The specimens are resistance heated to the test temperature, held for 1 to 10 min, and pulled to fracture at a crosshead rate approximating the strain rate of plant practice.

The reduction in area versus test temperature obtained by the on-heating testing of a heat-resistant alloy is shown in Fig. 17. The optimal reheat temperature for working lies between the peak ductility temperature and the zero-ductility temperature. The test clearly distinguishes between ingots prepared by electroslag remelting (ESR) and vacuum arc remelting (VAR) practices.

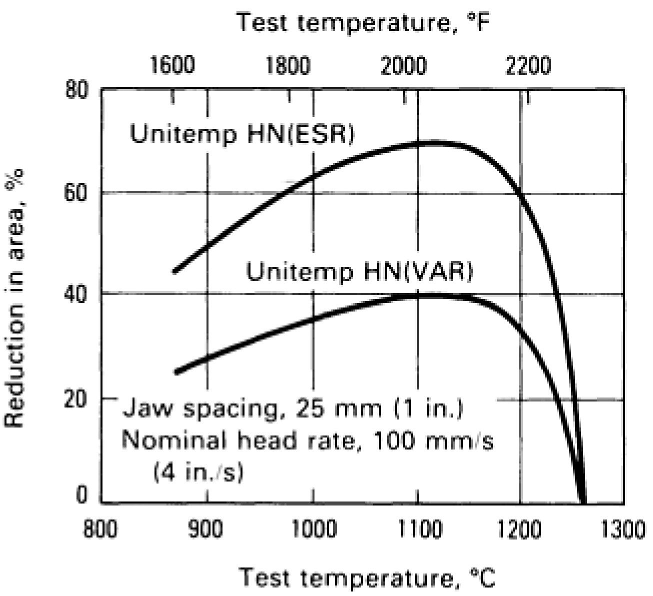

Fig. 17 Reduction in area versus test temperature obtained by hot tension testing on heating. Specimens were heated to the test temperature, held 5 min, and pulled to fracture.

The on-cooling test procedure is used to establish the optimal preheat temperature in this range. The objective is to determine which hot-working temperature provides the highest ductility over the broadest temperature range without risking permanent damage to the material from overheating. Unmachined specimen blanks are heat treated in a furnace at a given preheat temperature and duration to duplicate a furnace soak commensurate with the workpiece size and the hot-working operation. Samples are water quenched from the soak temperature to retain the high-temperature structure.

After machining, tensile specimens are heated to the preheat temperature in the Gleeble unit and held for 1 to 10 min to dissolve any phases that may have precipitated during cooling. Specimens are then cooled to a series of temperatures below the preheat temperatures at 28 to 55 °C (50 to 100 °F) intervals, held 5 s at the test temperature, and pulled to fracture at the appropriate head speed.

Data obtained from on-cooling tests conducted on three test specimens that were subjected to varying preheat temperatures are shown in Fig. 18. A preheat temperature of 1205 °C (2200 °F) was selected as optimal in this example, because it produced a slightly higher and rather broad band of high ductility. The minimum hot-working temperature was established as the temperature at which the reduction in area decreases to the 50% level for typical workpiece reductions.

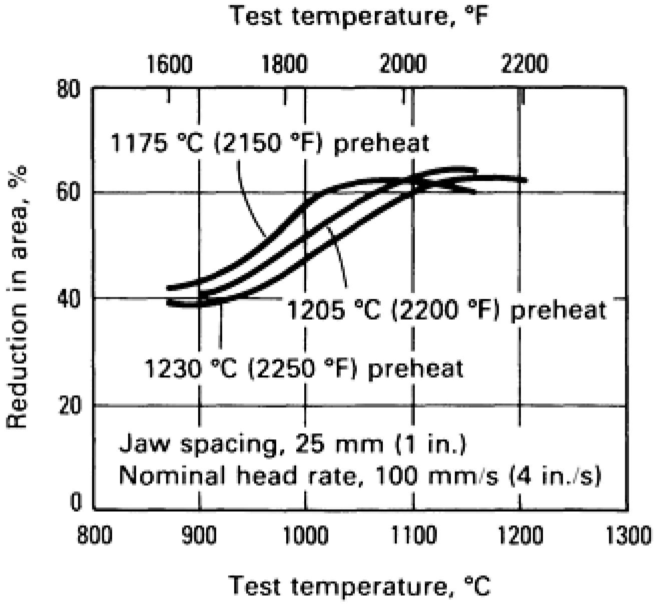

Fig. 18 Reduction in area versus testing temperature for Unitemp HN (ESR) generated by testing on cooling. Specimen blanks were furnace soaked 2 h at the preheat temperatures. The specimens were then heated to the preheat temperatures in the Gleeble unit, held 5 min, cooled to the test temperature, held 5 s, and pulled to fracture.

On-cooling hot tension testing is useful, because the brief hold times for on-heating tests may not develop a grain size representative of that temperature, or they may be insufficient to dissolve or precipitate a phase that will occur during an actual furnace soak prior to hot working. In addition, most industrial hot-working operations are performed while the workpiece temperature cools slowly. On-cooling tests also indicate how closely the zero-ductility temperature can be approached before hot ductility is severely reduced.

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Workability Tests

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Forgeability Tests

Basically, all forging processes consist of the compressive deformation of a metal workpiece between a pair of dies (Ref 14). The two broad categories of forging processes are open-die and closed-die modes. The simplest open-die forging operation is the upsetting of a cylindrical billet between two flat dies. The compression test is a small-scale prototype of this process. As the metal flows laterally between the advancing die surfaces, there is less deformation at the die interfaces (because of the friction forces) than at the midheight plane. Therefore, barreling occurs on the sides of the upset cylinder. Generally, metal flows most easily toward the nearest free surface because this path presents the least friction.

Closed-die forging is done in closed or impression dies that impart a well-defined shape to the workpiece. The degree of lateral constraint varies with the shape of the dies and the design of the peripheral areas where flash is formed, as well as with the same factors that influence metal flow in open-die forging (amount of reduction, frictional boundary conditions, and heat transfer between the dies and the workpiece).

Because forging is a complex process, a single workability test cannot be relied on to determine forgeability. However, several testing techniques have been developed for predicting forgeability, depending on alloy type, microstructure, die geometry, and process variables. This section will summarize some of the common tests for determining workability in open-die and closed-die forging.

Wedge-Forging Test. In this test, a wedge-shaped piece of metal is machined from a cast ingot or wrought billet and forged between flat, parallel dies (Fig. 19). The dimensions of the wedge must be selected so that a representative structure of the ingot is tested. Coarse-grain materials require larger specimens than fine-grain materials. The wedge-forging test is a gradient test in which the degree of deformation varies from a large amount at the thick end (h_2) to a small amount or no deformation at the thin end (h_1). The specimen should be used on the actual forging equipment in which production will occur to allow for the effects of deformation velocity and die chill on workability.

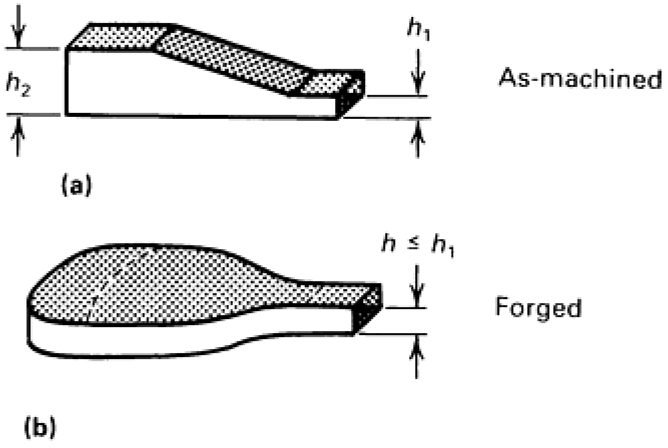

Fig. 19 Specimens for the wedge test. (a) As-machined specimen. (b) Specimen after forging.

Tests can be made at a series of preheat temperatures, beginning at about nine-tenths of the solidus temperature or the incipient melting temperature. After testing at each temperature, the deformation that causes cracking can be established. In addition, the extent of recrystallization as a function of strain and temperature can be determined by performing metallographic examination in the direction of the strain gradient.

The sidepressing test consists of compressing a cylindrical bar between flat, parallel dies where the axis of the cylinder is parallel to the surfaces of the dies. Because the cylinder is compressed on its side, this testing procedure is termed sidepressing. This test is sensitive to surface-related cracking and to the general unsoundness of the bar, because high tensile stresses are created at the center of the cylinder (Fig. 20).

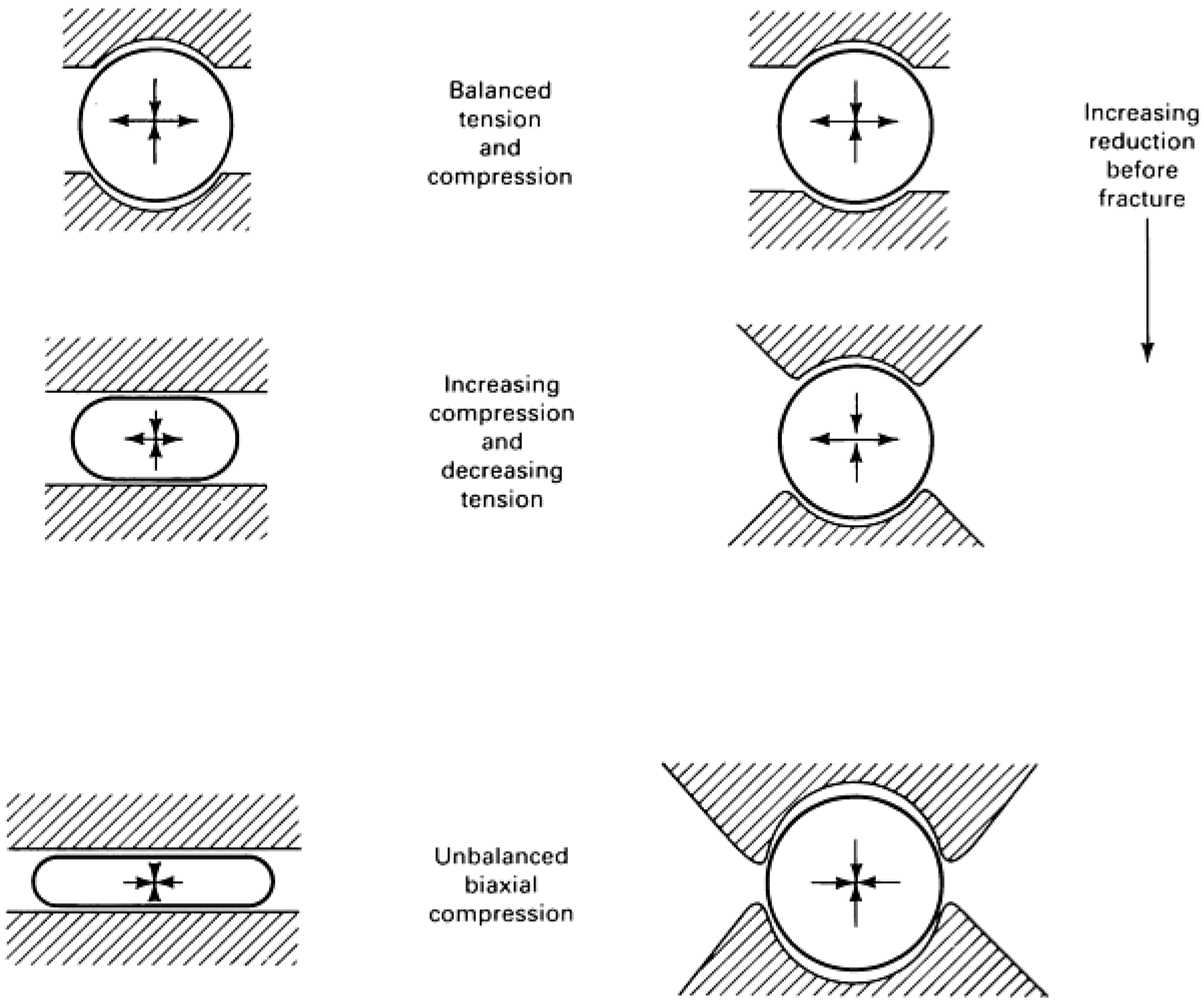

Fig. 20 Effects of billet shape and degree of enclosure on stress state in forging with good lubrication and no chilling. Source: Ref 29.

For a cylindrical bar deformed against flat dies, the tensile stress is greatest at the start of deformation and decreases as the bar assumes more of a rectangular cross section. As shown in Fig. 20, the degree of tensile stress can be reduced at the outset of the tests by changing from flat dies to curved dies that support the bar around part of its circumference.

The typical sidepressing test is conducted with unconstrained ends. In this case, failure occurs by ductile fracture on the expanding end faces. If the bar is constrained to deform in plane strain by preventing the ends from expanding, deformation will be in pure shear, and cracking will be less likely. Plane-strain conditions can be achieved if the ends are blocked from longitudinal expansion by machining a channel or cavity into the lower die block.

The notched-bar upset test is similar to the conventional upset test, except that axial notches are machined into the test specimens (Ref 30). The notched-bar test is used with materials of marginal forgeability for which the standard upset test may indicate an erroneously high degree of workability. The introduction of notches produces high local stresses that induce fracture. The high levels of tensile stress in the test are believed to be more typical of those occurring in actual forging operations.

Test specimens are prepared by longitudinally quartering a forging billet, thus exposing center material along one corner of each test specimen (Fig. 21). Notches with 1.0 or 0.25 mm (0.04 or 0.01 in.) radii are machined into the faces as shown. A weld button is frequently placed on one corner to identify the center and surface material of alloys that are difficult to forge because of segregation.

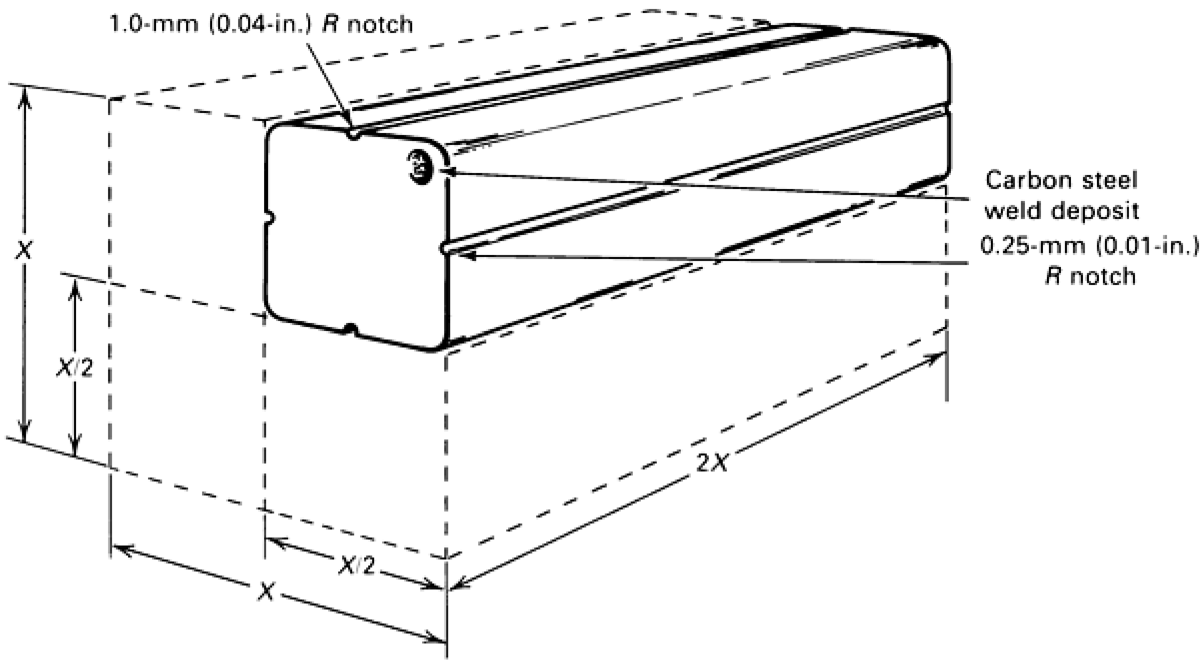

Fig. 21 Method of preparing specimens for notched-bar upset forgeability test. Source: Ref 30.

Specimens are heated to predetermined temperatures and upset about 75%. The specimen is oriented with the grooves (notches) in the vertical direction. Because of the stress concentration effect, ruptures are most likely to occur in the notched areas. These ruptures can be classified according to the rating system shown in Fig. 22. A rating of 0 indicates that no ruptures are observed, and higher numbers indicate an increasing frequency and depth of rupture.

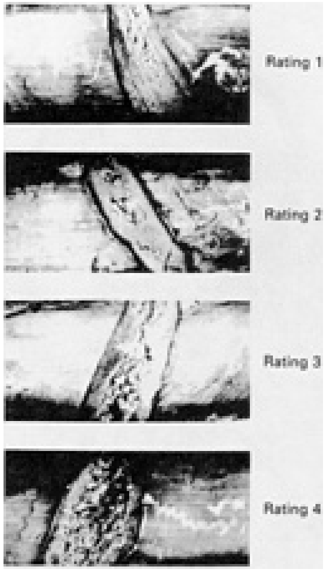

Fig. 22 Suggested rating system for notched-bar upset test specimens that exhibit progressively poorer forgeability. A rating of 0 indicates freedom from ruptures in the notched area. Source: Ref 30.

Figure 23 shows roll-forged rings made from two heats of type 403 stainless steel. The ring shown in Fig. 23(a) came from a billet with a notched-bar forgeability rating of 0. The billet shown in Fig. 23(b) had a forgeability rating of 4.

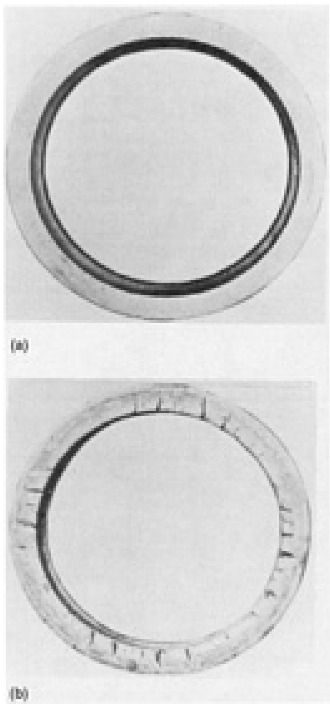

Fig. 23 Rolled rings made from two heats of type 403 stainless steel exhibiting different forgeability ratings in notched-bar upsetting tests. (a) Forgeability rating is 0. (b) Forgeability rating is 4. Courtesy of Ladish Company.

Truncated Cone Indentation Test. This test involves the indentation of a cylindrical specimen by a conical tool (Fig. 24). As a result of the indentation, cracking is made to occur beneath the surface of the testpiece at the tool/material interface. The reduction (measured at the specimen axis) at which cracking occurs can be used to compare the workability of different materials. Alternatively, the reduction (stroke) at which a fixed crack width is produced or the width of the crack at a given reduction can be used as a measure of workability.

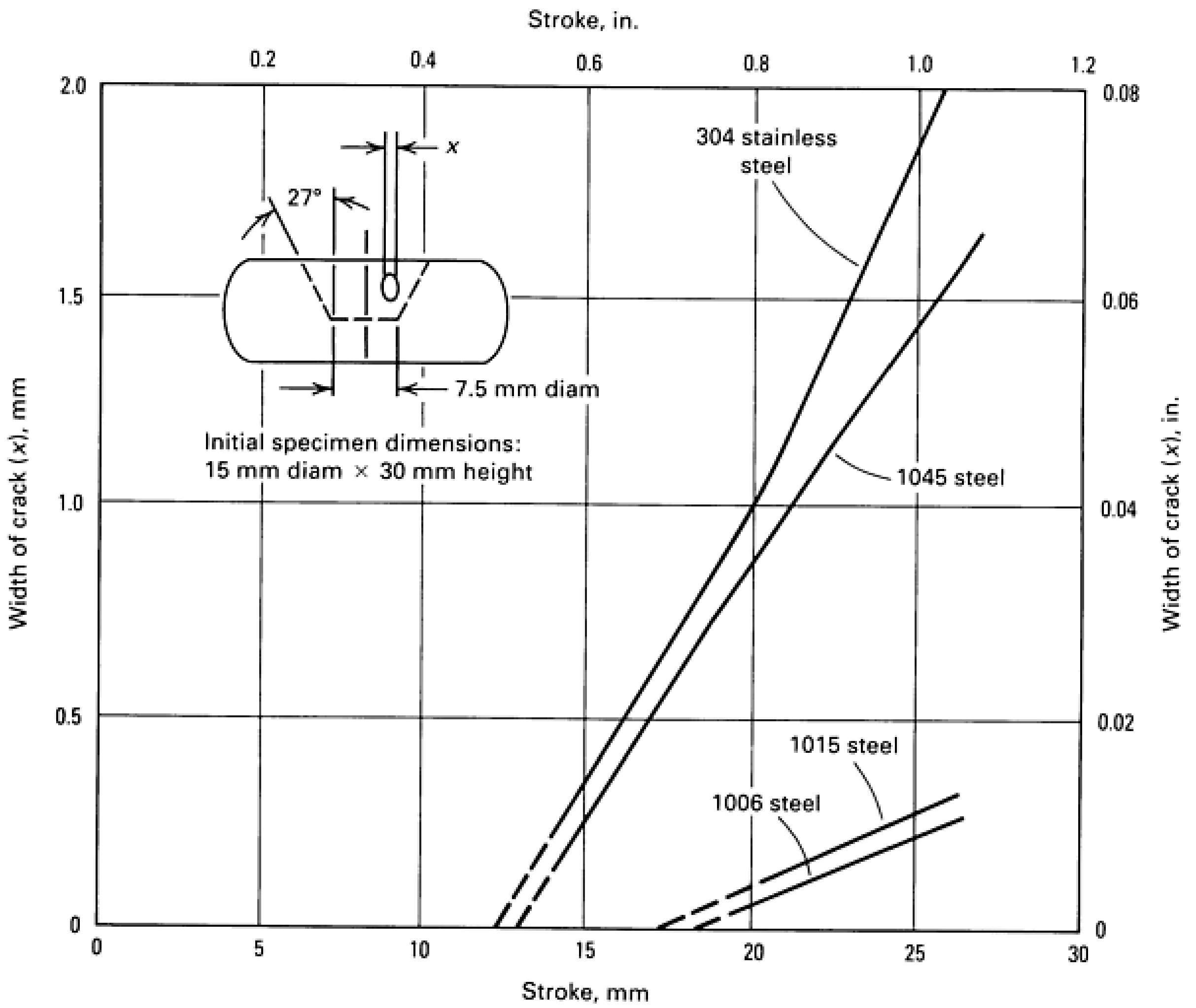

Fig. 24 Relationship between crack width and stroke in truncated cone indentation test for workability of various steels at cold-forging temperatures.

The truncated cone was developed as a test that minimizes the effects of surface flaws and the variability they produce in workability (Ref 31). This test has been primarily used in cold forging.

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Tests for Flow Localization

Complex forgings frequently develop regions of highly localized deformation. Shear bands may span the entire cross section of a forging and, in extreme cases, produce shear cracking. Flow localization can arise from constrained deformation due to die chill or high friction. However, flow localization can also occur in the absence of these effects if the metal undergoes flow softening or negative strain hardening.

The simplest workability test for detecting the influence of heat transfer (die chilling) on flow localization is the nonisothermal upset test, in which the dies are much colder than the workpiece. Figure 25 illustrates zones of flow localization made visible by sectioning and metallographic preparation.

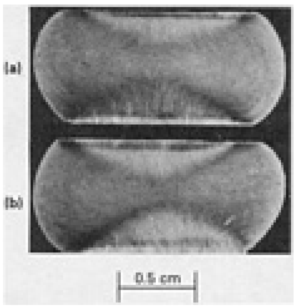

Fig. 25 Axial cross sections of specimens of Ti-6Al-2Sn-4Zr-2Mo-0.1Si with an equiaxed α starting microstructure. Specimens were nonisothermally upset at 954 °C (1749 °F) to 50% reduction in a mechanical press ($\dot{\epsilon} \approx$30 s^{-1}) between dies at 191 °C (376 °F). Dwell times on the dies prior to deformation were (a) 0 s and (b) 5 s. Source: Ref 32.

The sidepressing test conducted in a nonisothermal manner can also be used to detect flow localization. Several test specimens are sidepressed between flat dies at several workpiece temperatures, die temperatures, and working speeds. The formation of shear bands is determined by metallography (Fig. 26). Flow localization by shear band formation is more likely in the sidepressing test than in the upset test. This is due to the absence of a well-defined axisymmetric chill zone. In the sidepressing of round bars, the contact area starts out at zero and builds up slowly with deformation. In addition, because the deformation is basically plane strain, surfaces of zero extension are present, along which block shearing can initiate and propagate. These are natural surfaces along which shear strain can concentrate into shear bands.

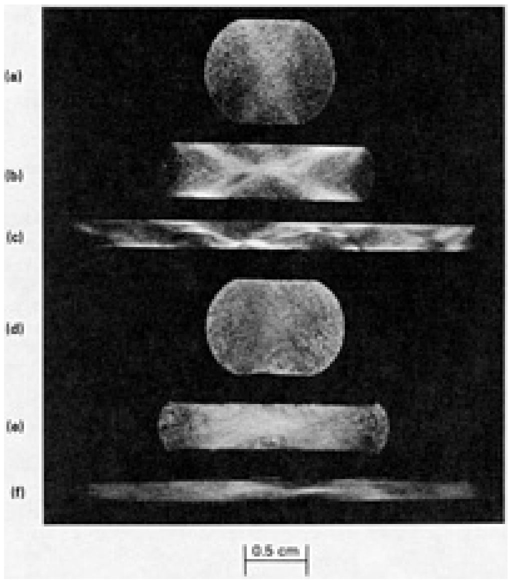

Fig. 26 Transverse metallographic sections of specimens of Ti-6Al-2Sn-4Zr-2Mo-0.1Si with an equiaxed α structure. Specimens were nonisothermally sidepressed with zero dwell time in a mechanical press ($\dot{\epsilon} \approx 30$ s^{-1}) between dies heated to 191 °C (376 °F). Specimen preheat temperatures (T_s) and percent reductions (*R*), relative to the initial specimen diameter, were as follows: (a) T_s: 913 °C (1675 °F); *R*: 14%. (b) T_s: 913 °C (1675 °F); *R*: 54%. (c) T_s: 913 °C (1675 °F); *R*: 77%. (d) T_s: 982 °C (1800 °F); *R*: 21 %. (e) T_s: 982 °C (1800 °F); *R*: 57%. (f) T_s: 982 °C (1800 °F); *R*: 79%. Source: Ref 32.

Testing to evaluate material susceptibility to localized deformation can also involve the use of a cylindrical upset specimen with a reduced gage section (Ref 33), as shown in Fig. 27. The ability of the material to distribute deformation (Fig. 28) is measured by an empirical parameter--percent distributed gage volume (DGV). The larger the DGV percentage, the greater the penetration of the deformation into the heavy ends of the specimen and the greater the ability of the material to distribute deformation. Figure 29 shows the metallographic appearance of the condition with distributed flow (Fig. 29a) and concentrated deformation (Fig. 29b). Figure 30 illustrates that the DGV percentage is a sensitive parameter for detecting flow localization.

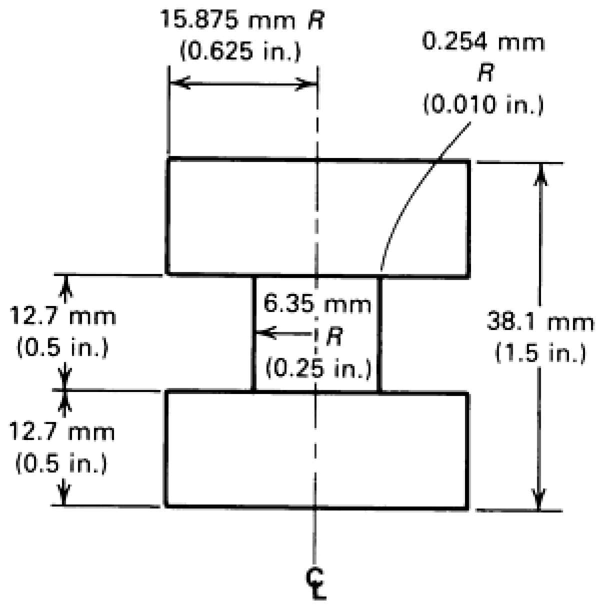

Fig. 27 Shape and dimensions of cylindrical compression specimen with a reduced gage section.

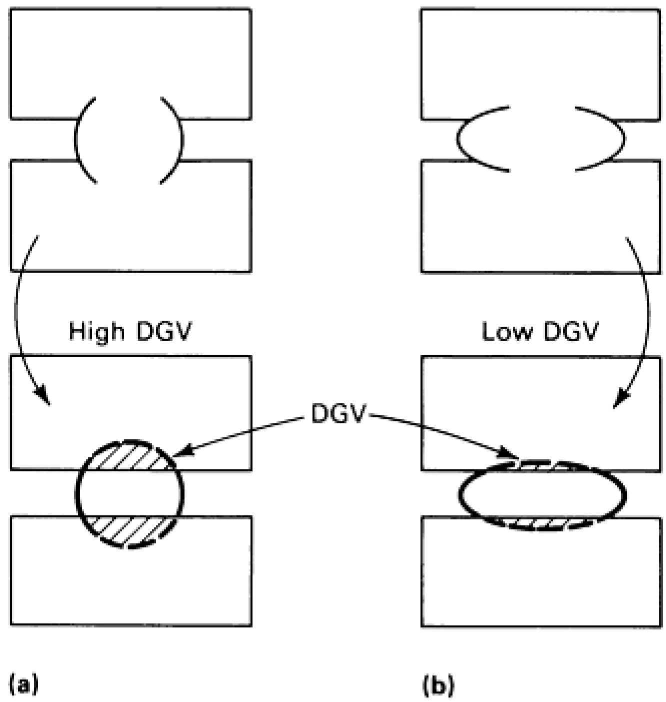

Fig. 28 Schematic of specimen cross sections showing the relative amount of gage volume penetration (DGV) into the specimen ends for two different deformation behaviors. (a) Distributed deformation. (b) Concentrated deformation. Source: Ref 33

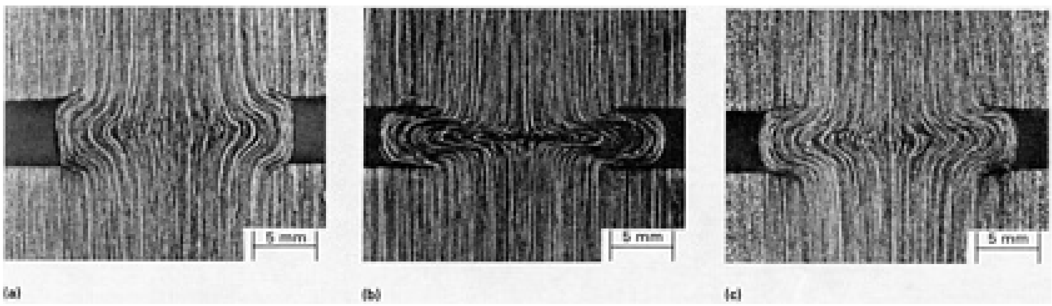

Fig. 29 Light micrographs showing variations in flow line contours and gage penetration into the specimen ends. After press forging at 650 °C (1200 °F) (a), 815 °C (1500 °F) (b), and 870 °C (1600 °F) (c). Etched in oxalic acid

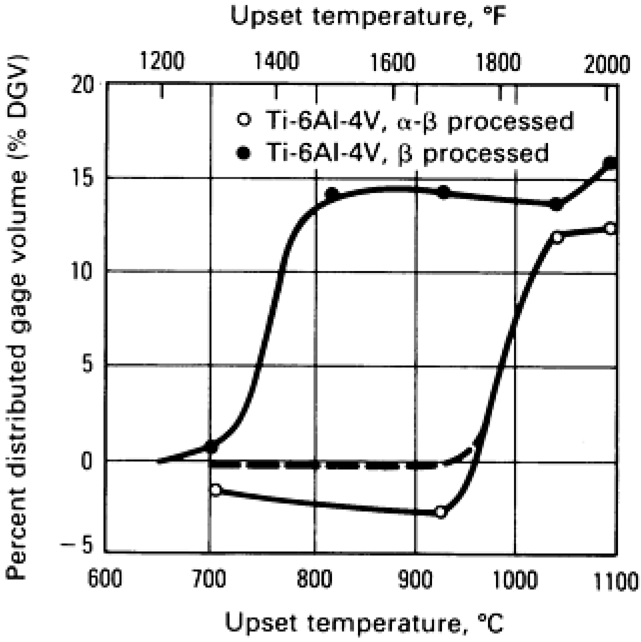

Fig. 30 Variation in DGV percentage for pressed specimens of Ti-6Al-4V as a function of upset temperature. Closed circles indicate starting microstructures of globular α. Open circles indicate starting microstructure of acicular α. Source: Ref 33

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Workability Tests

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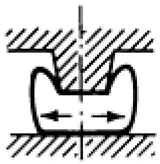
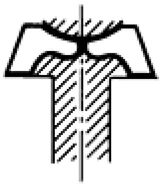
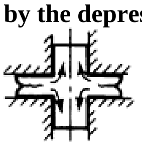
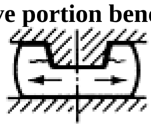
Forging Defects

Cracking in Cold Forging. The types of cracks that develop in cold forging by upsetting-type processes are discussed in Ref 31. The various geometric forms of cold forging are shown in Fig. 31. The classification of cracks is given in Fig. 32. Table 3 provides a detailed description of each type of crack.

Table 3 Characteristics of cracks and crack growth mechanism

Cracking type	**No. of working method in the chart (see Fig. 31)**	Characteristics of cracking and estimation of crack growth mechanism
α	00, 01, 11, 02, 21, 22, 08, 87, 07, 77, 09, 03, 33, 04, 44,	**External cracking that appears at midheight of side surface of the specimen in the upsetting; two types of cracks, longitudinal and oblique, occur according to the degree of end constraint**

	43	**of specimen; they are all shear cracks**
β	08, 88, 87, 89, 04, 44, 43	**Longitudinal cracking that occurs at the bottom of the concave part of the specimen (in upsetting by a circular truncated cone punch); it is a shear crack based on the section of the specimen and is caused by the expansion of material under the cone punch due to circumferential flow; prevention requires selection of a suitable punch shape** 
γ	03, 33, 05, 55, 07, 77, 87, 43, 06, 66, 65	**Shear cracking that appears at the corner of extruded material in free extrusion (coining); cracking occurs at the boundary between dead-metal and plastic zones; surface cracking is caused by cracks that occur at the corners; prevention requires selection of a suitable diameter for the die** 
δ	33	**Cracking that occurs at midheight of material in flange in two direction free extrusion without side constraint; the material is extruded forward and backward; therefore, cracking is caused by the depression of material in flange; prevention requires selection of a suitable diameter die** 
ε	08, 87, 88, 89	**Cracking that occurs at midheight on inside surface of the concave part and is advanced in a circumferential direction in upsetting by the circular truncated cone punch; crack starts at the point where the material around the concave portion bends toward the inside; prevention requires selection of a suitable punch shape** 
α+ β	08, 87, 88, 89	**Cracking in which α and β cracks coexist**
ζ	55	**Cracking that occurs at the center of material with the sides constrained and forward and backward extrusion; the cavity occurs at the center of material, as material is extruded forward and backward** 
η	08, 87, 88, 89	**Cracking that appears to advance from upper and side surface of concave portion to the top of specimen (in upsetting by a circular truncated cone punch); in cross section, the cracks are distributed radially; in the case of a large tapered punch, cracks are caused by the expansion of upper part of specimen by the punch; prevention requires selection of a suitable punch shape**
κ	05, 55	**Cracking that occurs at the center of specimen when excessive reduction is imposed on the specimen in extrusion and drawing**

μ	66	**Peripheral cracking that occurs at midheight on the side surface of the specimen in forward and backward piercing and 45° shearing crack with respect to longitudinal axis**
λ	06, 65, 66	**Cracking that occurs at the bottom of the concave portion in piercing**
θ	**07, 99**	**Microscopic cracks that occur at the boundary between top and bottom dead metal and at the point of inflection of the metal flow in case of excessive upsetting of a bolt head in bolt forging; in practical use, the splitting off of the head is caused by these microscopic cracks**

Source: Ref 31

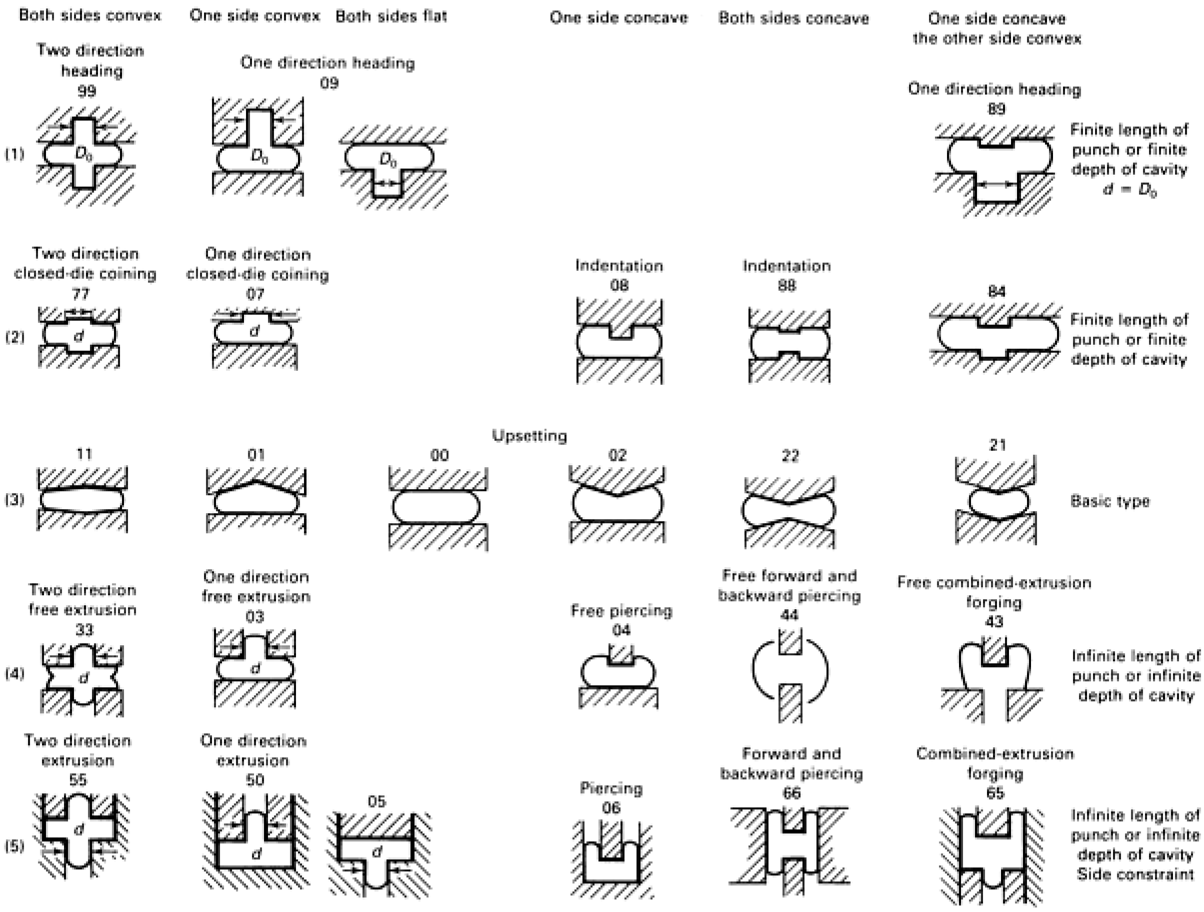


Fig. 31 Working methods in cold working. Cross-hatching indicates tool shape. See Table 3 for a description of the working method numbers. Source: Ref 31

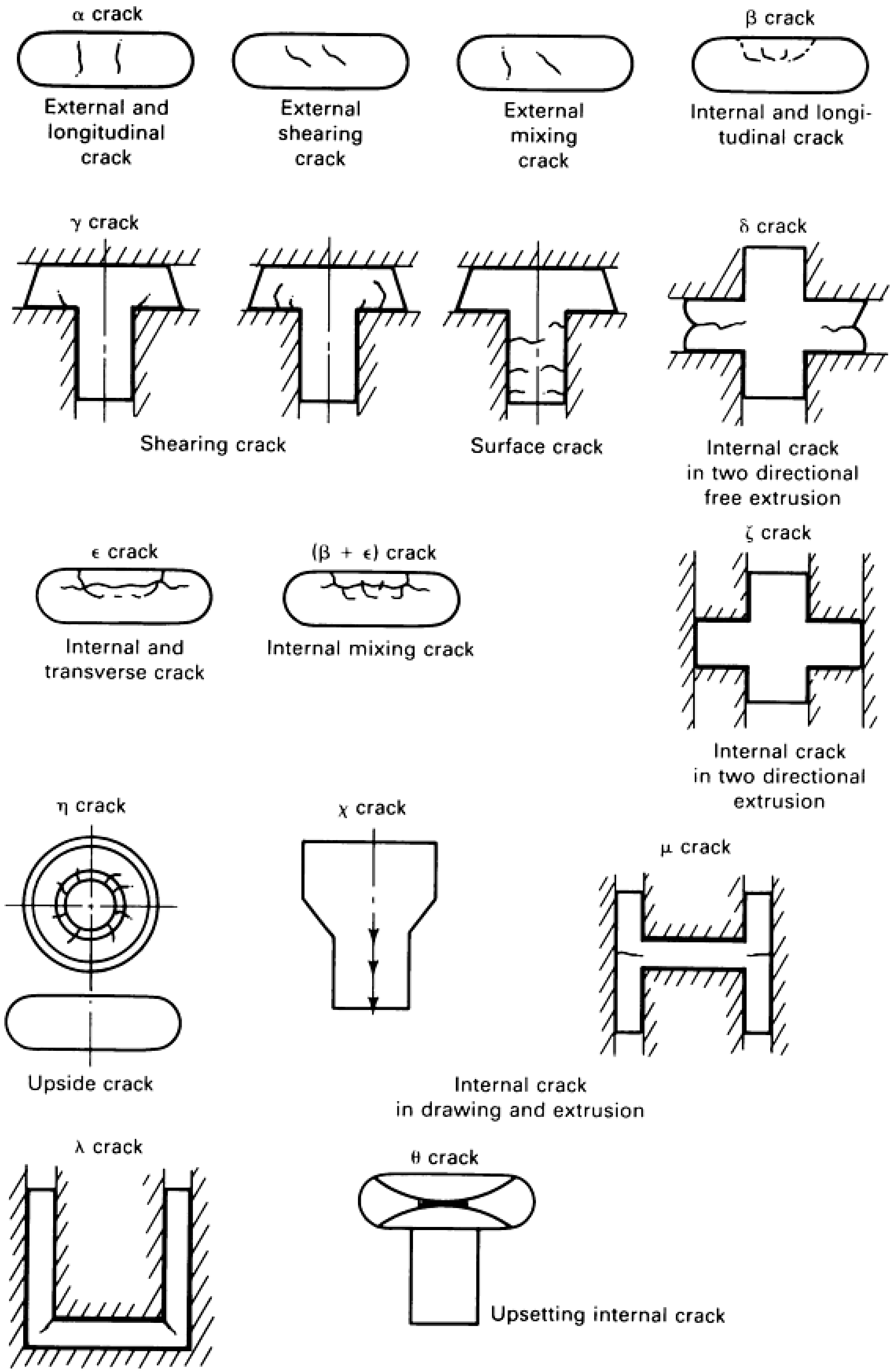
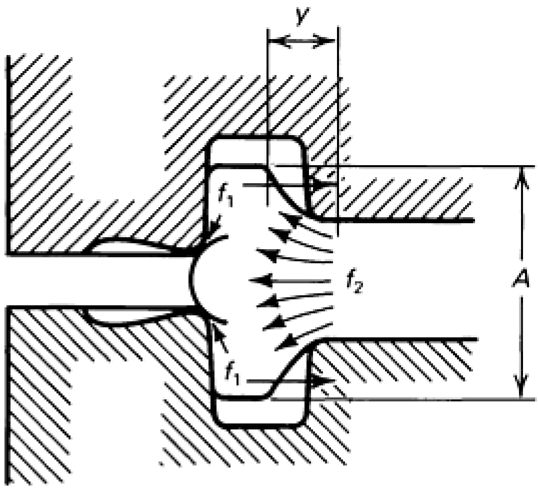


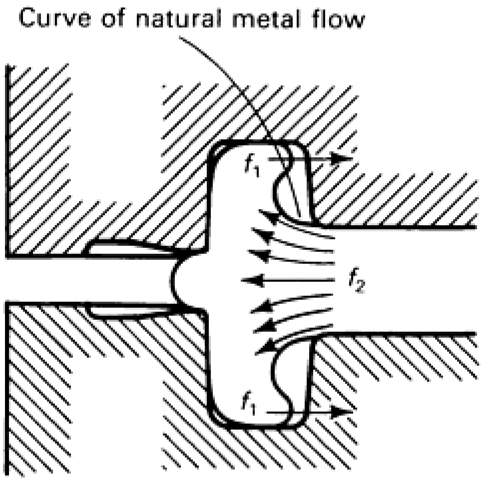
Fig. 32 Classification of cracks. Greek letters indicate type of cracking. See also Table 3. Source: Ref 31

Defects in Closed-Die Forging. The defects discussed above can also occur in closed-die forging. However, other defects in addition to fracture and localized flow occur in closed-die forging. These defects often result from such factors as improper selection of the starting or preform shape of the workpiece, poor die design, improper selection of lubricant, temperature, or working speed.

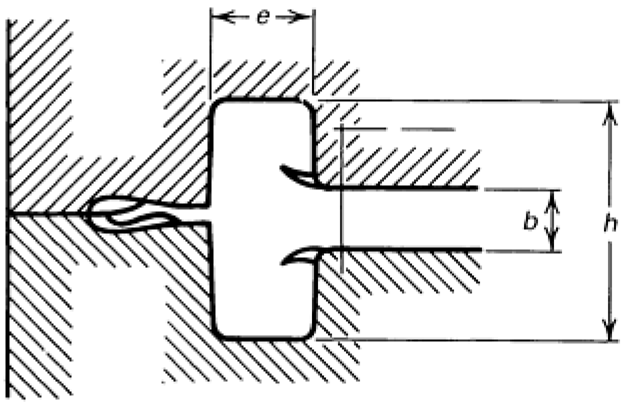
The principal defects in closed-die forging are laps, flow-through defects, extrusion defects, and cold shuts. Laps are defects that form when metal folds back over itself during forging. For example, in the finish forging of a webbed forging in which the preform web is too thin, the web may buckle and fold back onto itself. In addition, in the forging of a web, the metal may flow nonuniformly and cause a lap (Fig. 33). Frequently, a lap results from an excessively sharp radius in the forging die. Figure 34 shows a forging lap at a sharp radius.



Formation of flash



Reverse flow forming a fold



Formed forging defect

Fig. 33 Lap formation in the rib of a rib-web part due to improper preform geometry. Source: Ref 34

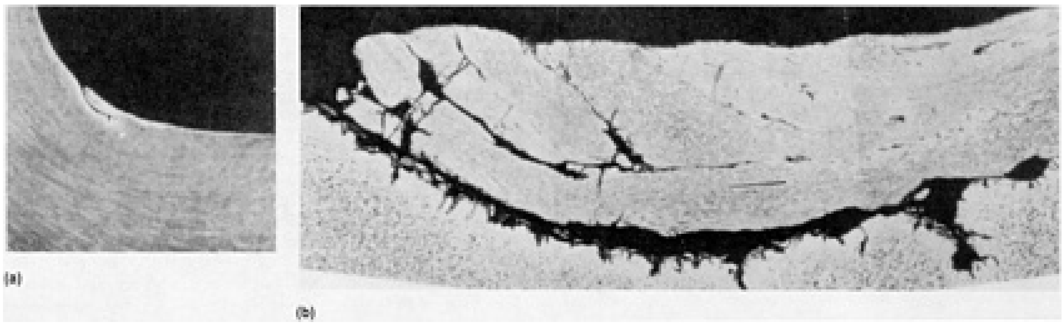


Fig. 34 Lap defect in Ti-6Al-4V bulkhead forging. (a) $3\frac{1}{3}\times$. (b) $50\times$. Courtesy of F. Lake and D. Moracz, TRW, Inc.

Flow-through defects are flaws that form when metal is forced to flow past a recess after the recess has filled or when material in the recess has ceased to deform because of chilling (Fig. 35). Similar to laps in appearance, flow-through defects can be shallow, but they are indicative of an undesirable grain flow pattern or shear band that extends much deeper into the forging. Flow-through defects can also occur when trapped lubricant forces metal to flow past an impression.

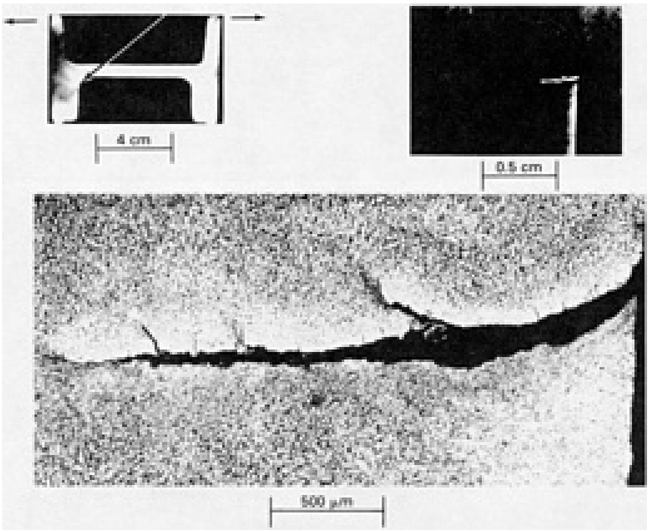


Fig. 35 Flow-through defect in Ti-6Al-4V rib-web structural part. Source: Ref 35.

Extrusion-type defects are formed when centrally located ribs formed by extrusion-type flow draw too much metal from the main body or web of the forging. A defect similar to a pipe cavity is thus formed (Fig. 36). Methods of minimizing the occurrence of these defects include increasing the thickness of the web or designing the forging with a small rib opposite the larger rib, as shown in Fig. 36.

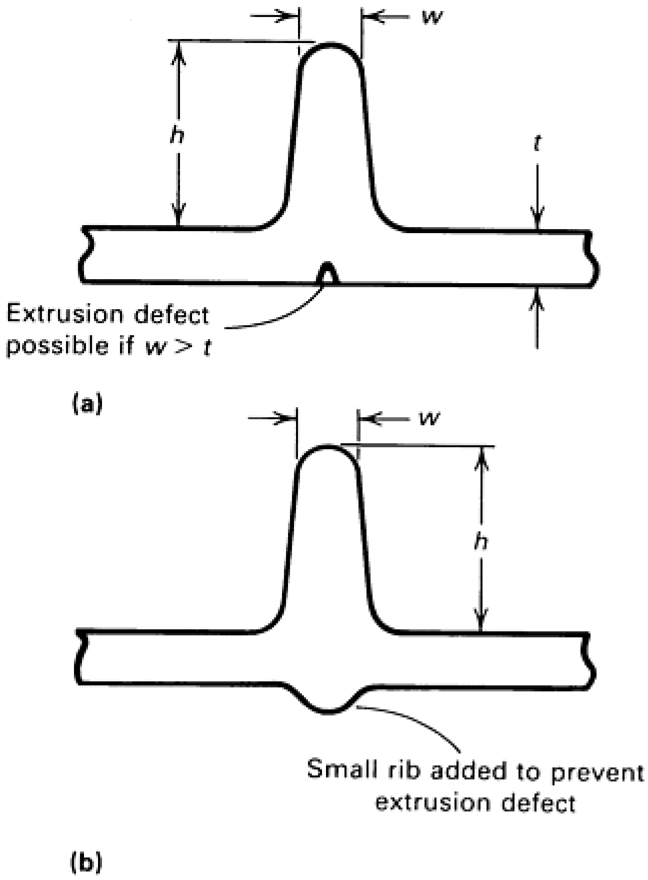


Fig. 36 Extrusion-type defect in centrally located rib (a) and die-design modification (b) used to avoid defect. Source: Ref 34.

Most of the defects summarized above occur in hot forging, which is most common for impression-die forging. Therefore, defect formation may also involve entrapment of oxides and lubricant. When this occurs, the metal is incapable of rewelding under the high forging pressures; the term cold shut is frequently applied in conjunction with laps, flow-through defects, and so on, to describe the flaws generated.

These defects are all related to the velocity field distribution of the deforming metal. They can be avoided by proper die design, preform design, and choice of lubrication system. Strictly speaking, these defects are not fundamental to the workability of the material. However, knowledge of these common forging defects is necessary for a practical understanding of workability. These are the defects that commonly limit deformation in the forging process.

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Workability Tests

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Workability Theory and Application in Bulk Forming Processes

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Introduction

WORKABILITY, as described in the two previous articles in this Section, is not merely a property of a material but a characteristic of the material/process system. The role of the material is measured by a simple test or tests and should be expressed in a quantitative form that is universally applicable. This measure is taken to be a basic property of the material composition and structure, and it reflects the macroscopic outcome of the micromechanisms of plastic flow perturbed by such inhomogeneities as voids, inclusions, and grain boundaries (see, for example, Fig. 3 to 5 in the article "Introduction to Workability" in this Section). Such phenomena are dependent not only on the material structure but also on the process parameters (strain rate and temperature), which define the role of the process in determining workability.

The micromechanisms of ductile fracture in bulk forming processes are strongly influenced by the stress and strain environment imposed by the process. It was shown in Fig. 14 in the article "Introduction to Workability" in this Section that an overall measure of strain to fracture could be related to an overall value of hydrostatic stress in the material during processing. In the spectrum of processes (extrusion, rolling, forging, and wire drawing), the average hydrostatic stress becomes increasingly tensile, and the strain to fracture progressively decreases. Within each of these processes, however, the stress and strain states can be considered on a localized basis in the specific regions in which fractures initiate. These localized conditions are controlled by the geometry of the workpiece, die design, and friction at the die/workpiece interface. These three factors, in addition to the strain rate and temperature parameters mentioned above, embody the role of the process in determining workability.

This article will focus on the effects of mechanical plasticity on workability, that is, process control of localized stress and strain conditions to enhance workability. First, the nature of local stress and strain states in bulk forming processes will be described, leading to a classification scheme that facilitates the application of workability concepts. This defines testing procedures and specific process measurements for an experimental approach to workability evaluation. Theoretical models and fracture criteria will then be described and compared with experimental results. Finally, the application of workability concepts to forging, rolling, and extrusion processes will be discussed.

Workability Theory and Application in Bulk Forming Processes

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Stress and Strain States

Forging, extrusion, and rolling processes are generally considered to involve the application of compressive force to material to impart a change in shape and dimensions. Upon close examination, however, it is clear that deformation resulting from the applied load causes secondary stress and strain states that vary from point to point throughout the deforming workpiece. These stress and strain states may include tension, so fracture can occur at certain locations in a material even though the primary (applied) load is compressive.

To explore this possibility further, it is useful to review plasticity theory briefly. In the article "Introduction to Workability" in this Section, the von Mises yield criterion was given; this yield criterion is the foundation of plasticity theory for isotropic materials. It gives the relationships between normal and shear stress components at yielding. From this, relationships between stress and strain components are derived:

$$\begin{aligned}
\epsilon_1 &= \lambda \left[\sigma_1 - \frac{1}{2}\sigma_2 - \frac{1}{2}\sigma_3\right] \\
\epsilon_2 &= \lambda \left[\sigma_2 - \frac{1}{2}\sigma_3 - \frac{1}{2}\sigma_1\right] \\
\epsilon_3 &= \lambda \left[\sigma_3 - \frac{1}{2}\sigma_1 - \frac{1}{2}\sigma_2\right]
\end{aligned} \qquad \textbf{(Eq 1)}$$

where ε denotes strain, σ denotes stress, and the subscripts 1, 2, and 3 designate the three directions in an orthogonal coordinate system. Here, λ is a proportionality factor dependent on the deformation history and flow stress curve of the material. The resulting strain in a given direction is affected by the stress in all three coordinate directions. In addition, these relationships satisfy the volume constancy condition, $\varepsilon_1 + \varepsilon_2 + \varepsilon_3 = 0$.

To illustrate these relationships, it is useful to consider a two-dimensional case of plane stress, $\sigma_3 = 0$:

$$\epsilon_1 = \lambda \left[\sigma_1 - \frac{1}{2}\sigma_2\right]$$
$$\epsilon_2 = \lambda \left[\sigma_2 - \frac{1}{2}\sigma_1\right] \quad \text{(Eq 2)}$$

Referring to Fig. 1 and Eq 2, a reduction in thickness (compressive strain in the 2-direction) can be accomplished either by a compressive stress, P_2 (or $-\sigma_2$), or by a tensile stress, σ_1. Although the nature of the deformation is the same in both cases, accomplishing that deformation in the latter case could risk fracture because it involves a tensile stress.

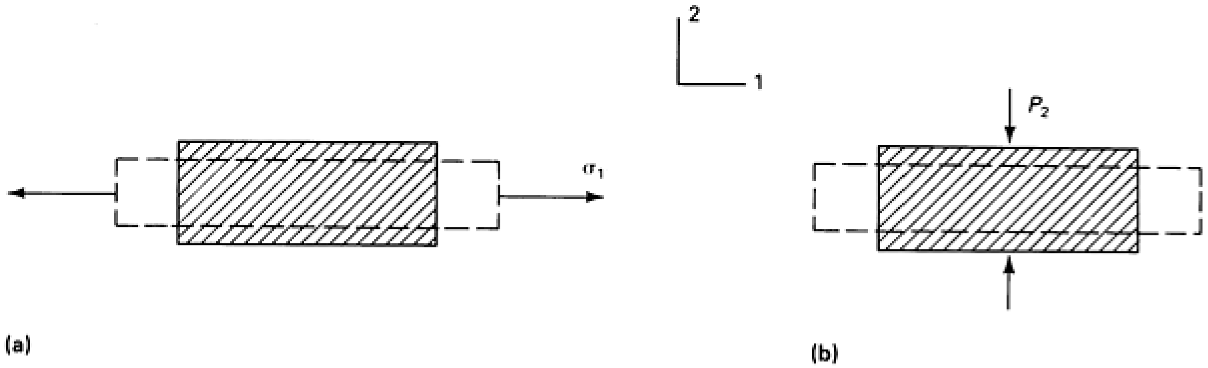

Fig. 1 Similarity of deformation under horizontal tension (a) and vertical compression (b).

For more general cases, Eq 2 can be rearranged to determine the following stresses:

$$\sigma_1 = \frac{4}{3\lambda}\left[\epsilon_1 + \frac{1}{2}\epsilon_2\right] = \frac{4}{3\lambda}\epsilon_1\left[1 + \frac{1}{2}\frac{\epsilon_2}{\epsilon_1}\right]$$
$$\sigma_2 = \frac{4}{3\lambda}\left[\epsilon_2 + \frac{1}{2}\epsilon_1\right] = \frac{4}{3\lambda}\epsilon_1\left[\frac{1}{2} + \frac{\epsilon_2}{\epsilon_1}\right] \quad \text{(Eq 3)}$$
$$\sigma_3 = 0$$

Therefore, the stresses depend on the localized strains ε_1 and ε_2 that result from metal flow. Equation 3 is a more convenient representation of plasticity relationships for workability study.

For example, the cylindrical surface of a compression test undergoes various combinations of axial and circumferential strains, depending on the aspect ratio and the friction at the die contact surfaces (Fig. 2). When no friction exists, the ratio of circumferential strain to axial strain is $\varepsilon_1/\varepsilon_2 = -\frac{1}{2}$. According to the first part of Eq 3, $\sigma_1 = 0$ for this case. The deformation in this case is referred to as homogeneous compression, because the only stress acting is σ_2 and it is uniform throughout the specimen. Therefore, the homogeneous compression test is suitable for measuring flow stress.

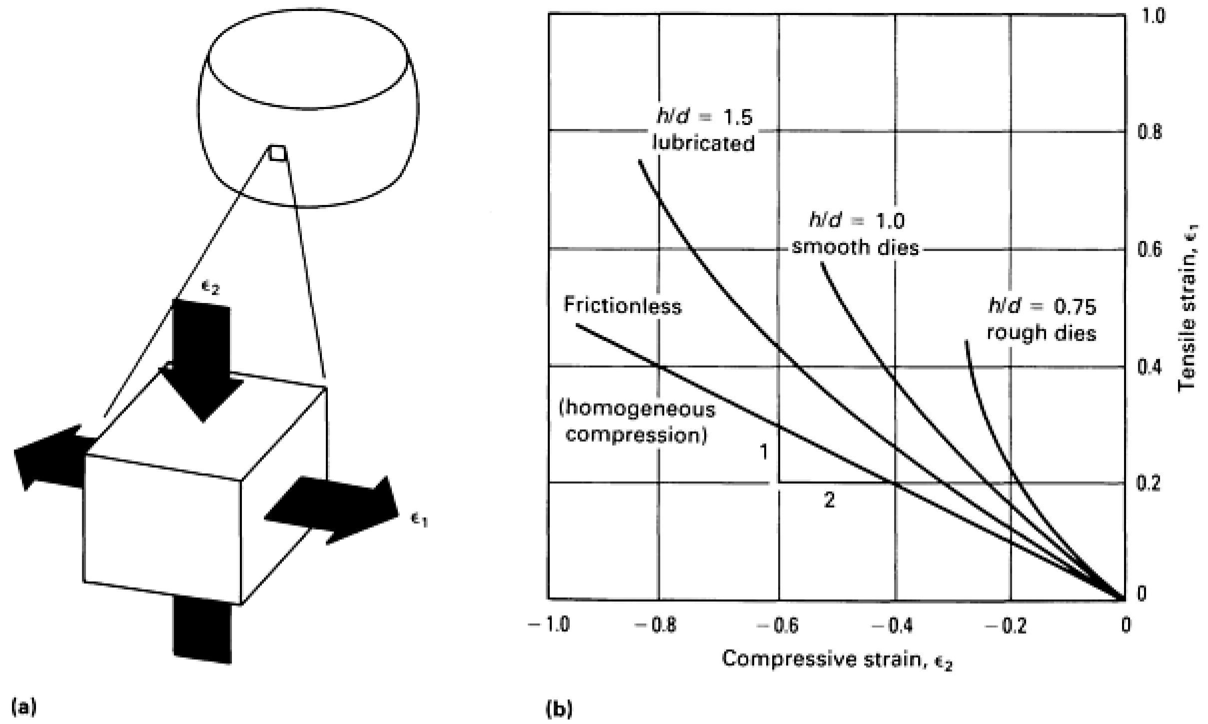

Fig. 2 Localized strains on the bulging cylindrical surface of an upset test (a) and their variation with aspect ratio and friction conditions (b). Source: Ref 1.

When friction exists at the die contact surfaces, material at these surfaces is constrained from moving outward, while material at the midplane is not constrained. As a result, bulging occurs, as shown in Fig. 2(a). Under these conditions, the circumferential strain ε_1 (>0) increases, and the localized axial compressive strain ε_2 (<0) decreases. From Eq 3 as $\varepsilon_1/\varepsilon_2$ becomes more negative ($< -\frac{1}{2}$), σ_1 becomes more positive. Therefore, increasing bulging due to friction during the compression of a cylinder increases the secondary tensile stress σ_1 and enhances the likelihood of fracture.

Similarly, at the edges of bars during rolling (Fig. 3), the elongation strain ϵ_1 is determined by the overall reduction in area. The localized compressive vertical strain ε_2, however, depends on the shape of the edge. Greater convexity and sharpness of the edge decrease the compressive vertical strain for a given reduction, which, from the first part of Eq 3, increases the secondary tensile stress σ_1 at the edge. Therefore, edge cracking during rolling is also due to secondary stress states.

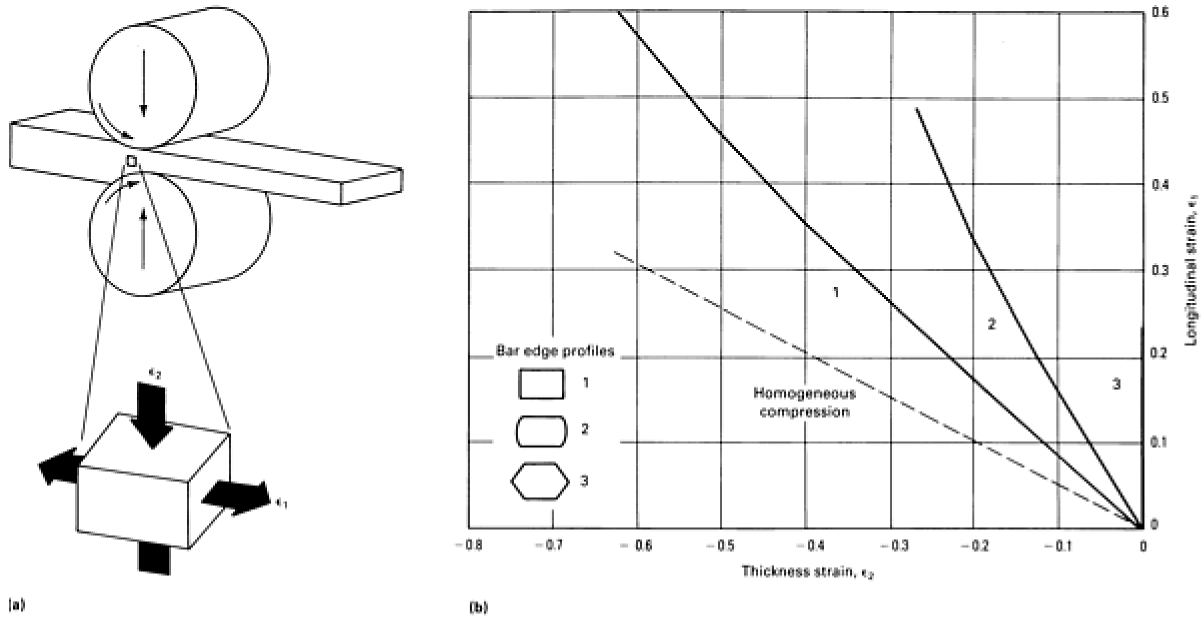

Fig. 3 Localized strains at the edges of bars during rolling (a) and their variation with edge profile (b). Source: Ref 2.

More complex cases of the same type of secondary tensile stress states occur in forging (Fig. 4). During the forging of a hub shape, for example, the top surface of the hub is subjected to biaxial tension because of friction during flow around the die radius (Fig. 4a). This is identical to the conditions present at the nose of a billet that is being extruded or rolled. Similarly, during forging, the top surface of a rib undergoes tensile strain in the direction of curvature, and essentially no strain occurs along the length direction of the rib (Fig. 4b). In both cases, localized secondary tensile stresses are generated that may cause fracture. These stresses can be calculated from measured strain values using Eq 3.

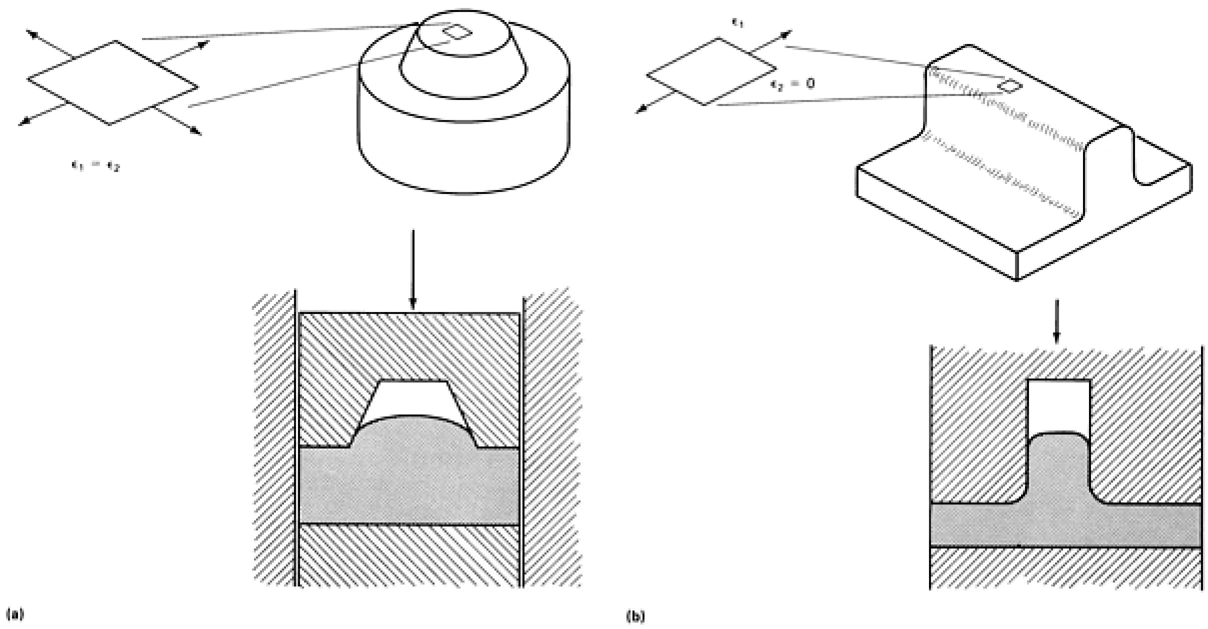

Fig. 4 Strains at the free surfaces of forgings. (a) Axisymmetric hub. (b) Rib-web forging.

Figures 2, 3, and 4 show examples of plane stress, with the stress normal to the free surface being zero. Other regions of workpieces in bulk deformation processes, however, are subjected to three-dimensional stress states. For example, material at the die contact surfaces in forging, rolling, and extrusion (Fig. 5) is subjected to strains ϵ_1 and ϵ_2 in the plane of the surface, as in Fig. 2, 3, and 4. In Fig. 5, however, this surface is also acted upon by pressure P_3 normal to the plane. Similarly, at internal locations of the workpieces in such processes as forging or wire drawing (Fig. 6), a material element of the central longitudinal plane is subjected to strains ϵ_1 and ϵ_2 and stress normal to the plane.

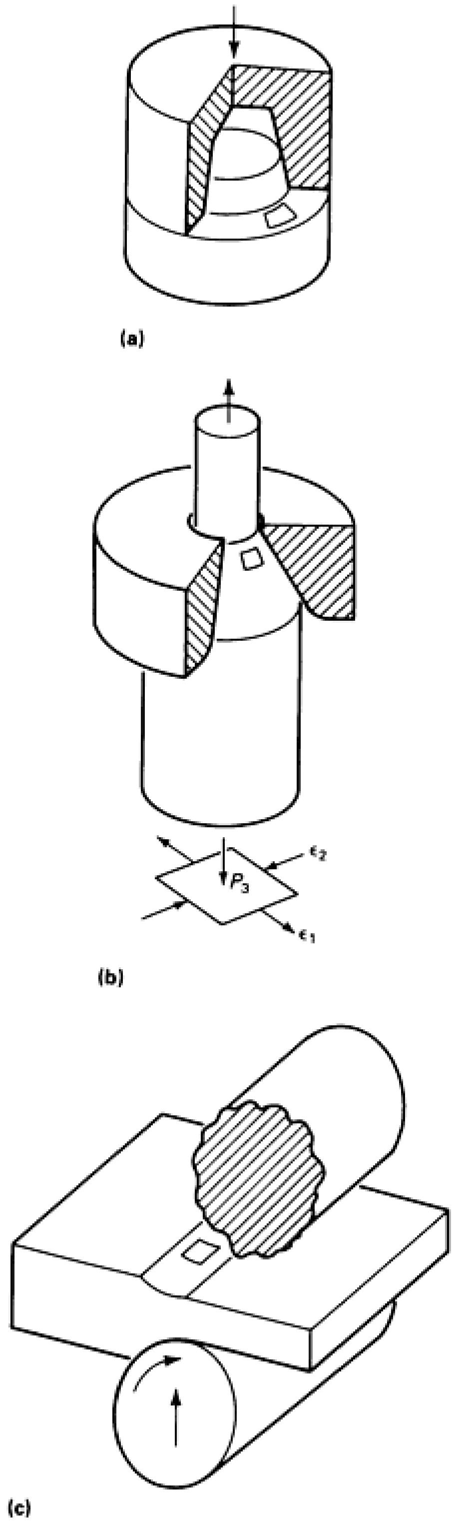

Fig. 5 An element of material at the die contact surfaces during forging (a), drawing or extrusion (b), and rolling (c).

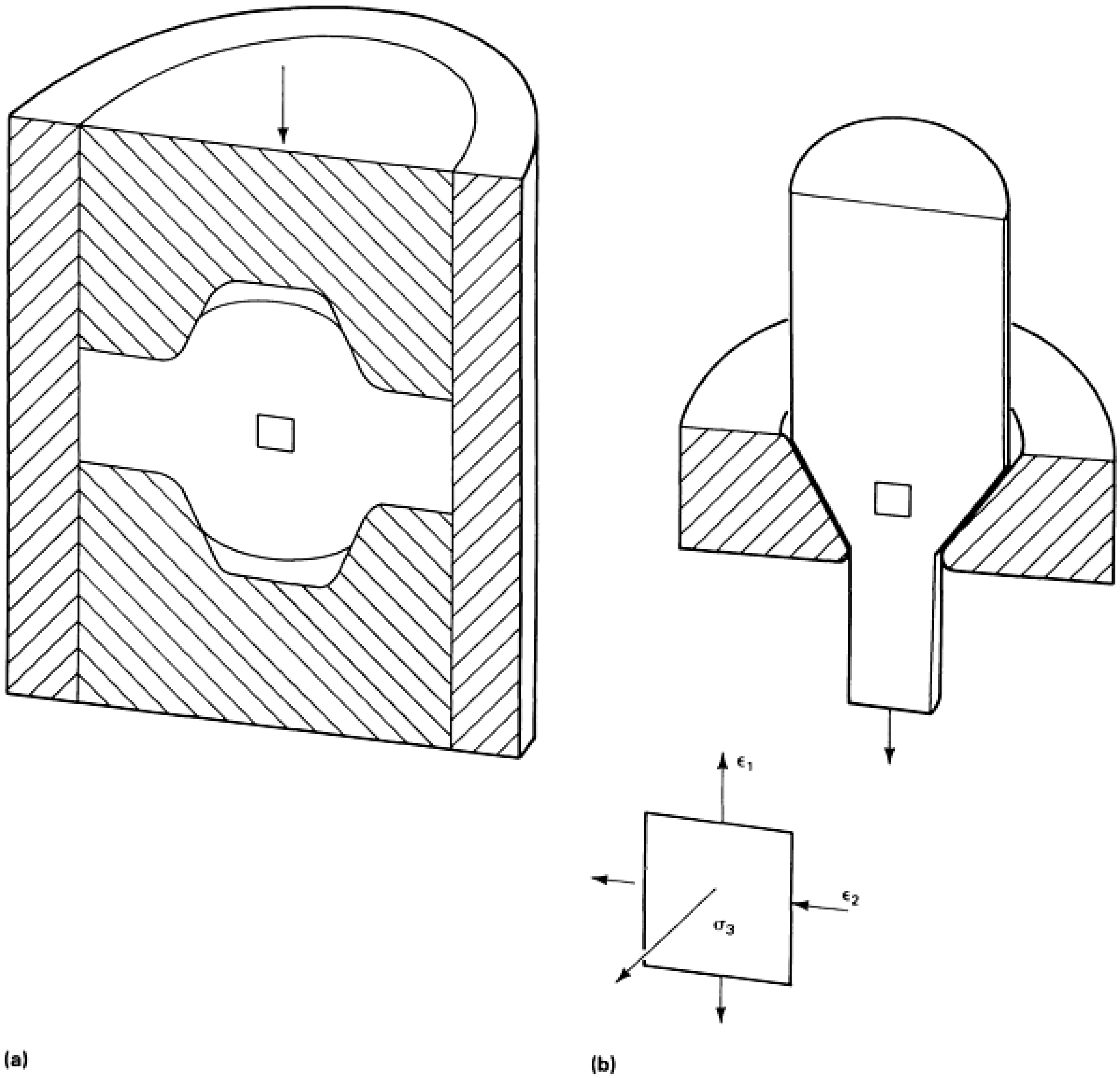

Fig. 6 An element of material at the center of a forging undergoing double extrusion (a) and at the center of wire being drawn (b). The element undergoes strains ε_1 and ε_2, as in Fig. 2, 3, and 4, with stress σ_3 normal to the 1-2 plane.

In Fig. 5 and 6, the material element can be thought of as the plane-stress elements in Fig. 2, 3, and 4 with σ_3 acting normal to the plane. If ε_1 and ε_2 are taken to be the strains in this plane, then Eq 3 becomes:

$$\sigma_1 = \frac{4}{3\lambda}\left[\epsilon_1 + \frac{1}{2}\epsilon_2\right] + \sigma_3$$

$$\sigma_2 = \frac{4}{3\lambda}\left[\epsilon_2 + \frac{1}{2}\epsilon_1\right] + \sigma_3$$

$$\sigma_3 = \sigma_3$$

In other words, for the same deformation (that is, the same values of ε_1 and ε_2 as in Fig. 2, 3, 4), the stresses σ_1 and σ_2 in Eq 3 are biased by σ_3. The stress normal to the surface, then, increases the hydrostatic stress component of the stress state by σ_3. This reflects the basic concept that hydrostatic stress does not affect yielding or plastic deformation. As shown in Fig. 14 in the article "Introduction to Workability" in this Section, however, the hydrostatic stress has a significant effect on fracture. In Fig. 5, the compressive stress (die pressure) P_3 would increase the strains at fracture, but in Fig. 6 the internal stress σ_3 may be tensile and decrease the strains at fracture.

The above discussion of stress and strain states in various regions of workpieces suggests a convenient method of classifying cracking defects, including those shown in Fig. 32 in the article "Workability Tests" in this Volume. Figures 2, 3, and 4 in this article illustrate the locations at which free surface cracks occur in forging and rolling, as well as the nose ends of billets in extrusion or rolling. They are represented by α cracks in Fig. 32 in the article "Workability Tests." Figure 5 in this article shows examples of circumstances in which contact surface cracking occurs--for example, the fir tree defect in extrusion or the longitudinal surface cracks in rolled plates. Other examples in forging include γ, η, and λ cracks as shown in Fig. 32 in the article "Workability Tests."

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Empirical Criterion of Fracture

The stress and strain environments described in the previous section in this article suggest that a workability test should be capable of subjecting the material to a variety of surface strain combinations. A capability for testing under superimposed normal stress would also be desirable.

When considering workability tests, it is important to recognize that fractures initiate in localized regions where interaction between the stress and strain states and the material structure reaches a critical level. Orientation, shape, and volume fraction of inclusions and other inhomogeneities have a dominant effect on the fracture process. Therefore, it is critically important that workability test specimens contain material having the same microstructural features as the material in the localized, potential fracture regions of the actual process.

Specifically, when evaluating a workpiece for surface fractures, specimen surfaces must contain the as-received surface of the workpiece under consideration because it may contain laps, seams, a decarburized layer, and so on, which affect fracture initiation. By the same argument, evaluation of material for internal fractures, such as central burst, must involve test specimens taken from the middle of the workpiece, where, for example, segregation of second phases may have occurred. Also, because of possible anisotropy effects, orientation of the critical stresses with respect to any inclusion alignment must be the same in the test specimens as it is in the actual process and material of interest.

The compression test has become a standard for workability evaluation. As shown in Fig. 2, a range of strain combinations can be developed at the cylindrical free surface simply by altering friction and geometry conditions. The influence of friction and consequent bulging on circumferential tensile stress development is clearly shown in Fig. 7. Compression with friction produces circumferential tension that leads to fracture, while frictionless compression avoids barreling, tension, and cracking as described in Fig. 2 and Eq 3.

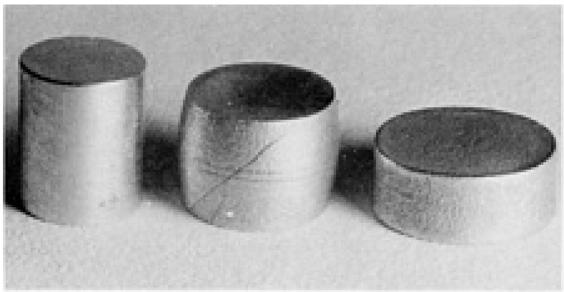

Fig. 7 Compression tests on 2024-T35 aluminum alloy. Left to right: undeformed specimen, compression with friction (cracked), compression without friction (no cracks).

Alterations of the compression test geometry have been devised to extend the range of surface strains available toward the vertical, tensile strain axis, ε_1 (Ref 3). Test specimens are artificially pre-bulged by machining a taper or a flange on the cylinders (Fig. 8). Compression then causes lateral spread of the interior material, which expands the rim circumferentially while applying little axial compression to the rim. Therefore, the tapered and flanged upset test specimens provide strain states consisting of small compressive strain components. Each combination of height, *h,* and thickness, *t,* gives a different ratio of tensile to compressive strain. The strain states developed at the surfaces of straight, tapered, and flanged compression test specimens are summarized in Fig. 9.

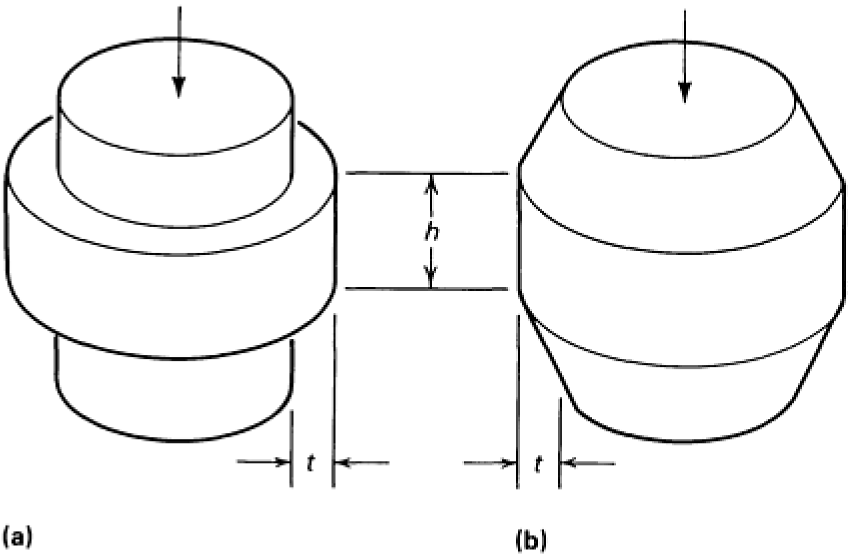

Fig. 8 Flanged (a) and tapered (b) prebulged compression test specimens. Lateral spread of interior material under compression expands the rim circumferentially while little axial compression is applied. See Fig. 9.

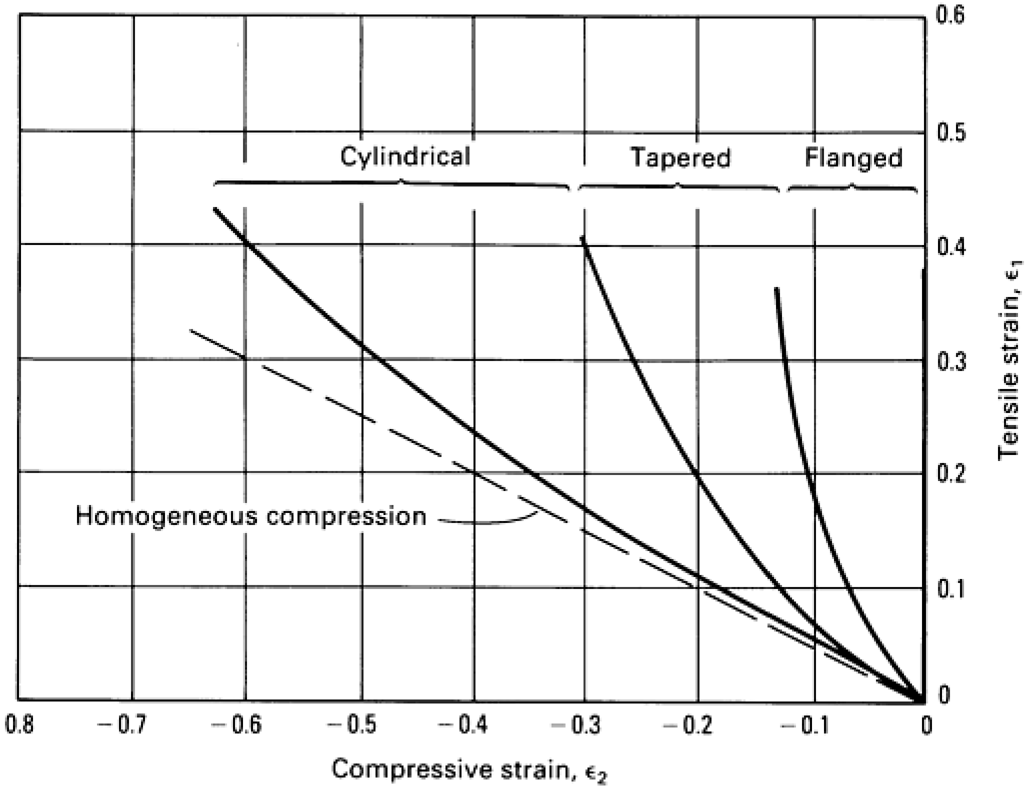

Fig. 9 Range of free surface strain combinations for compression tests having cylindrical (Fig. 2), tapered and flanged (Fig. 8) edge profiles. The ranges shown are approximate and they may overlap a small amount.

The variety of strain combinations available in compression tests offers the possibility for material testing over most of the strain combinations that occur in actual metalworking processes. A number of samples of the same material and condition are tested, each one under different friction and geometry parameters. Tests are carried out until fracture is observed, and the local axial and circumferential strains are measured at fracture. Figures 10, 11, and 12 give some examples of results for AISI 1045 carbon steel, 2024-T351 aluminum alloy at room temperature, and 2024-T4 alloy at a hot-working temperature. In some cases, the fracture strains fit a straight line of slope -$\frac{1}{2}$; in others, the data fit a dual-slope line with slope -$\frac{1}{2}$ over most of the range and slope -1 near the tensile strain axis. Similar data have been obtained for a wide variety of materials. In each case, the straight line behavior (single or dual slope) appears to be characteristic of all materials, but the height of the line varies with the material, its microstructure, test temperature, and strain rate.

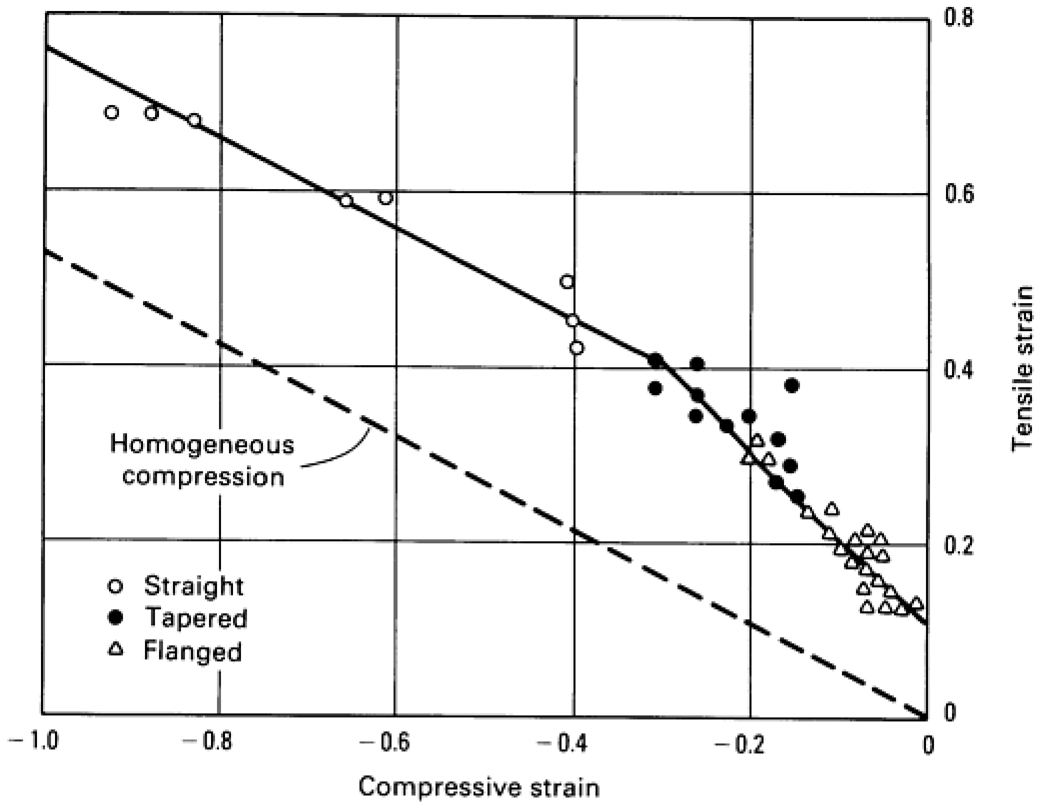

Fig. 10 Fracture locus for AISI 1045 cold-drawn steel.

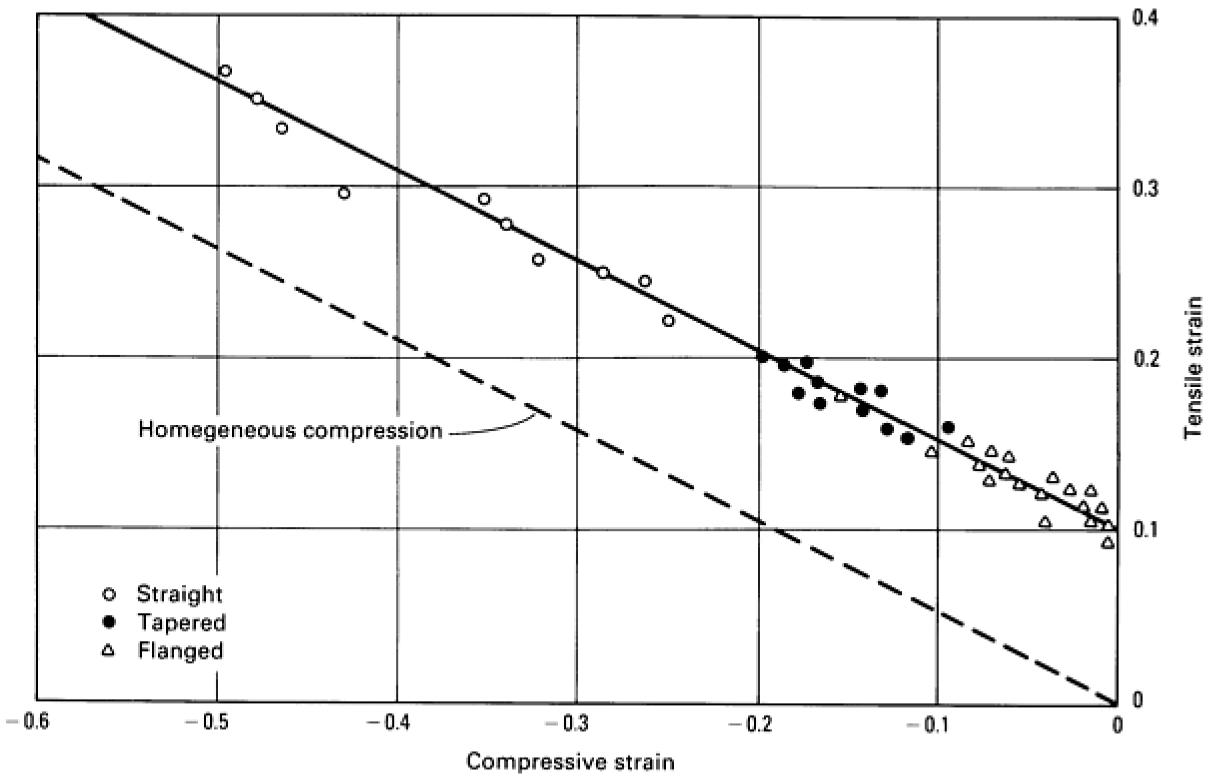

Fig. 11 Fracture locus for aluminum alloy 2024-T351 at room temperature.

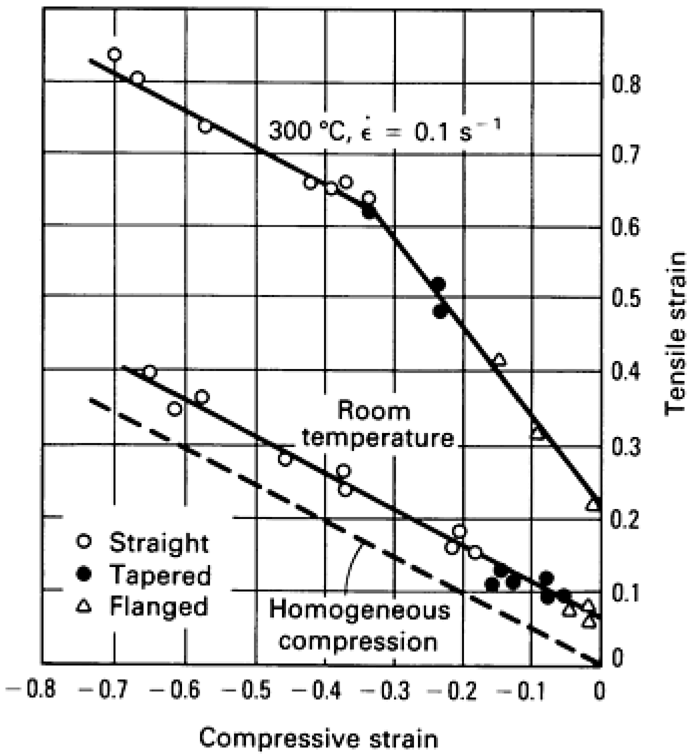

Fig. 12 Fracture locus for aluminum alloy 2024-T4 at room temperature and at 300 °C (570 °F). $\dot{\epsilon}$ **= 0.1 s**$^{-1}$**.**

The nature of the fracture loci shown in Fig. 10, 11, and 12 suggests an empirical fracture criterion representing the material aspect of workability. The strain paths at potential fracture sites in material undergoing deformation processing (determined by measurement or mathematical analysis) can then be compared to the fracture strain loci. Such strains can be altered by process parameter adjustment, and they represent the process input to workability. If the process strains exceed the fracture limit lines of the material of interest, fracture is likely. Other approaches to establishing fracture criteria, as well as applications of the criteria, are given in the following two sections of this article.

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Theoretical Fracture Models and Criteria

Fracture criteria for metalworking processes have been developed from a number of viewpoints. The most obvious approach involves modeling of the void coalescence phenomenon normally associated with ductile fracture (see Fig. 3 in the article "Introduction to Workability" in this Section). Another approach involves a model of localized thinning of sheet metal that has been adapted to bulk forming processes. In addition to models of fracture, criteria have been developed from macroscopic concepts of fracture. The Cockcroft criterion is based on the observation that both tensile stress and plastic deformation are necessary ingredients, which lead to a tensile deformation energy condition for fracture.

The upper bound method has been used to predict fracture in extrusion and drawing. Other approaches are based on the calculation of tensile stress by slip-line fields.

Void Growth Model. Microscopic observations of void growth and coalescence along planes of maximum shear leading to fracture have led to the development of a model of hole growth (Ref 4). Plasticity mechanics is applied to the analysis of deformation of holes within a shear band. When the elongated holes come into contact, fracture is considered to have occurred (Fig. 13). When the McClintock model is evaluated for a range of applied stress combinations, a fracture strain line can be constructed.

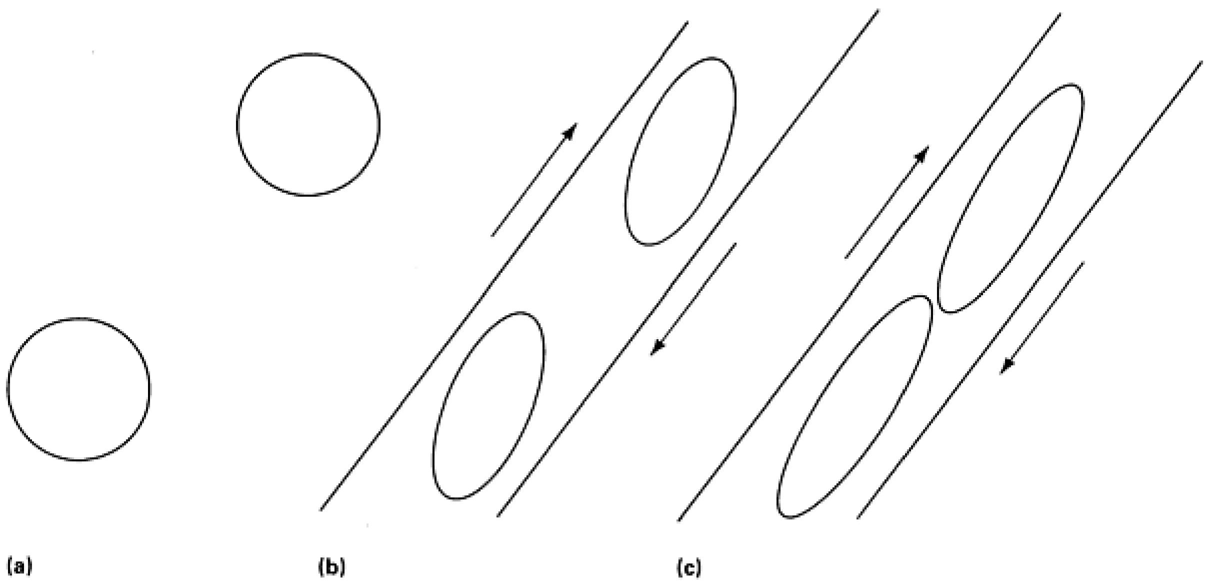

Fig. 13 McClintock model of void coalescence by shear from initial circular voids (a), through growth (b), and void contact (c).

Figure 14 shows the calculated results from the McClintock model in comparison with the experimental fracture line. The predicted fracture strain line has a slope of $-\frac{1}{2}$ over most of its length, matching that of the experimental fracture line. Near the tensile strain axis, the slope of the predicted line is -1, matching that of actual material results shown in Fig. 10 and 12.

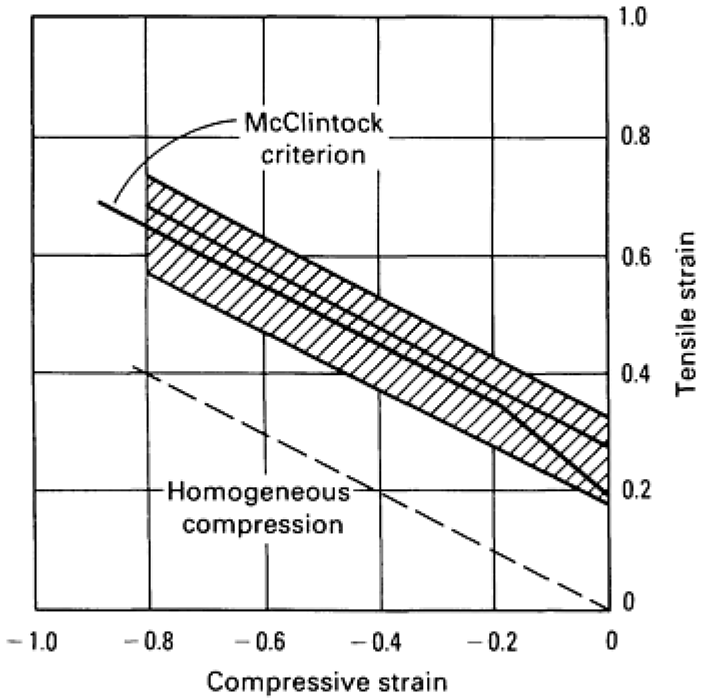

Fig. 14 Fracture strain locus predicted by the McClintock model of void growth. The shaded area represents typical experimental fracture loci, such as Fig. 10, 11, and 12.

Localized Thinning Model. In sheet forming, the observation that a neck forms before fracture, even under biaxial stress conditions in which localized instability cannot occur, has prompted consideration of the effects of inhomogeneities in the material. For example, a model of localized thinning due to a small inhomogeneity has been devised (Ref 5). Beginning with the model depicted in Fig. 15, plasticity mechanics is applied to determine the rate of thinning of the constricted region t_B in relation to that of the thicker surrounding material t_A. When the rate of thinning reaches a critical value, the limiting strains are considered to have been reached, and a forming limit diagram can be constructed. The analysis includes the effects of crystallographic anisotropy, work-hardening rate, and inhomogeneity size.

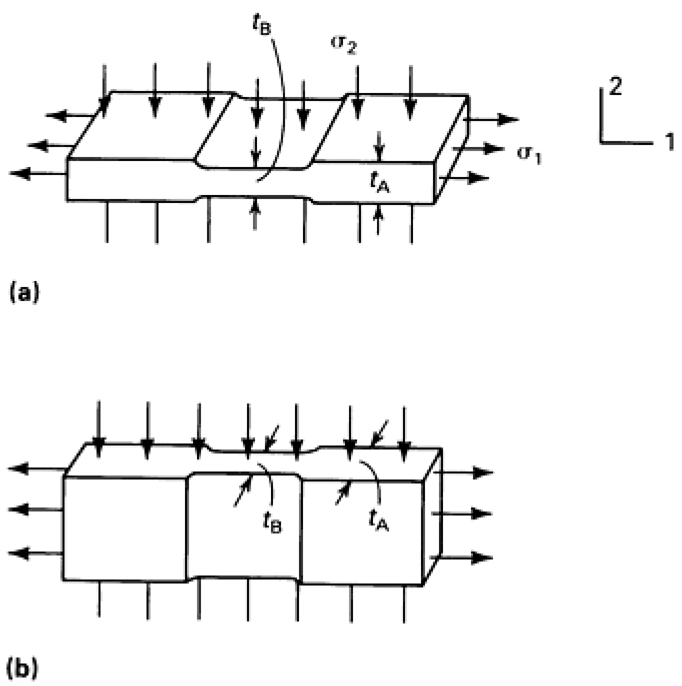

Fig. 15 Models for the analysis of localized thinning and fracture at a free surface. (a) R-model, (b) Z-model. Source: Ref 6.

This model was applied to free surface fracture in bulk forming processes because of evidence that localized instability and thinning also precede this type of ductile fracture (Ref 6). Two model geometries were considered, one having a groove in the axial direction (Z-model) and the other having a groove in the radial direction (R-model) as shown in Fig. 15. Applying plasticity mechanics to each model, fracture is considered to have occurred when the thin region, t_B, reduces to zero thickness. When these fracture strains are plotted for different applied stress ratios, a fracture strain line can be constructed. As shown in Fig. 16, the predicted fracture line matches the essential features of the experimental fracture lines. The slope is -½ over most of the strain range and approximately -1 near the tensile strain axis. Again, this model is in general agreement with the dual-slope fracture loci shown in Fig. 10 and 12.

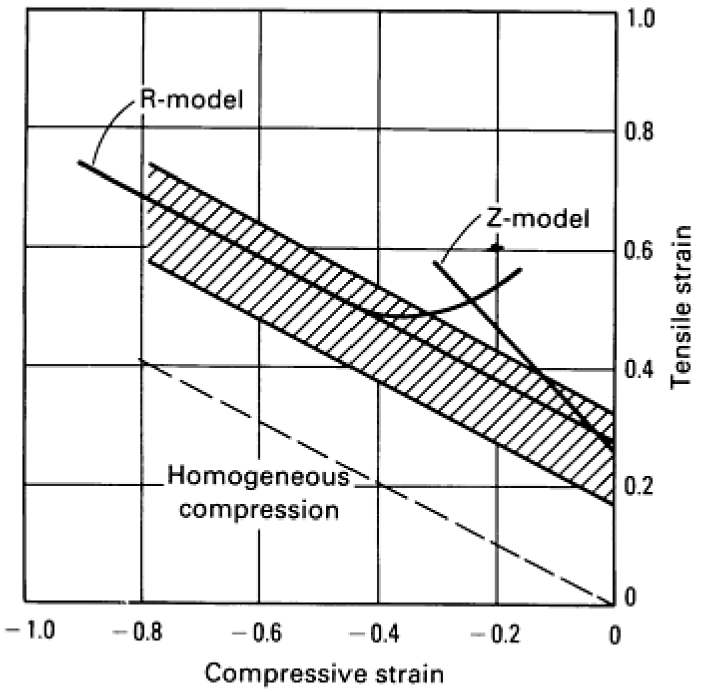

Fig. 16 Fracture strain locus predicted by the model of localized thinning. The shaded area represents typical experimental fracture loci.

Cockcroft Model. The Cockcroft criterion of fracture is not based on a micromechanical model of fracture, but simply recognizes the macroscopic roles of tensile stress and plastic deformation (Ref 7). It is suggested that fracture occurs when the tensile strain energy reaches a critical value:

$$\int_0^{\bar{\epsilon}_f} \sigma^* d\bar{\epsilon} = C$$

where σ^* is the maximum tensile stress; $\bar{\epsilon}$ is the equivalent strain; and C is a constant determined experimentally for a given material, temperature, and strain rate. The criterion has been successfully applied to cold-working processes. It has also been reformulated to provide a predicted fracture line for comparison with the experimental fracture strain line. Figure 17 shows that the fracture strain line predicted by the Cockcroft criterion is also in reasonable agreement with experimental results. This criterion does not show the dual-slope behavior of the previous models and some actual materials. The question of why some materials exhibit the dual-slope fracture locus and others only a single slope remains the subject of speculation, discussion, and further study.

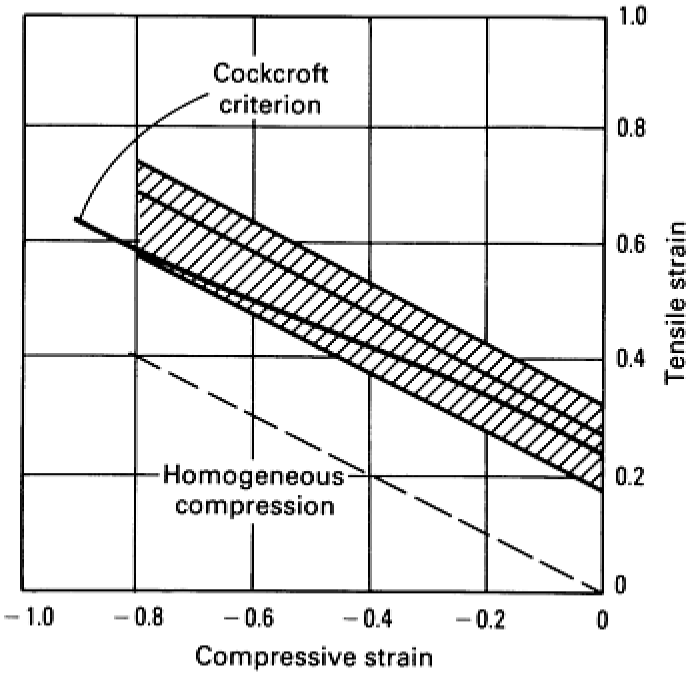

Fig. 17 Fracture strain locus predicted by the Cockcroft criterion.

The upper bound method of plasticity analysis requires the input of a flow field in mathematical function form. The external work required to produce this flow field is determined through extensive calculation. This value for external work is an upper bound on the actual work required. Through optimization procedures, the flow field can be found that minimizes the calculated external work done, and this flow field is closest to the actual metal flow in the process under analysis. The upper bound method has been applied to a number of metalworking processes (Ref 8).

In consideration of metal flow fields, perturbations can be incorporated that simulate defect formation. In some situations, the external work required to create the flow field with defects is less than the work required to create the flow field without defects (sound flow). According to the upper bound concept, defects would occur in these situations.

The upper bound method has been applied to the prediction of conditions for central burst formation in extrusion and wire drawing. As shown in Fig. 18, for die angles less than α_1, sound flow requires less drawing force than flow with central burst formation. Above α_2, extrusion with a dead-metal zone near the die requires lower drawing force. In the range of die angles between α_1 and α_2, central burst is energetically more favorable (Ref 9).

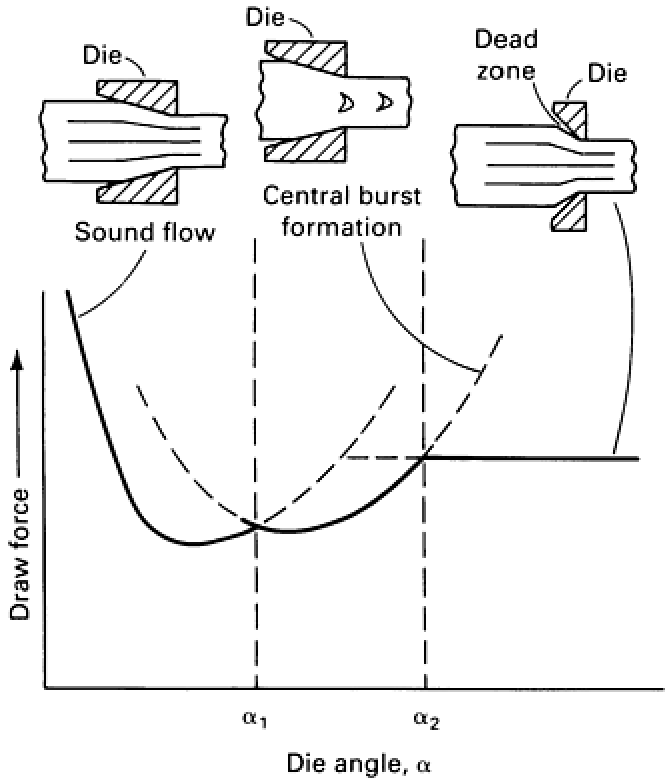

Fig. 18 Variation in mode of flow with die angle in wire drawing. The mode requiring the smallest force at any die angle is the active mode. This is a schematic for one value of reduction. Source: Ref 9.

Repeated calculations using the upper bound method provide the combinations of die angle and reduction that cause central burst (Fig. 19). Friction at the die contact surface affects the results. If operating conditions are in the central burst region, defects can be avoided by decreasing the die angle and/or increasing the reduction so that the operating conditions are in the safe zone. This example is a clear illustration of the role of process parameters (in this case, geometric conditions) in workability. An application of this method is given in the section "Applications" in this article.

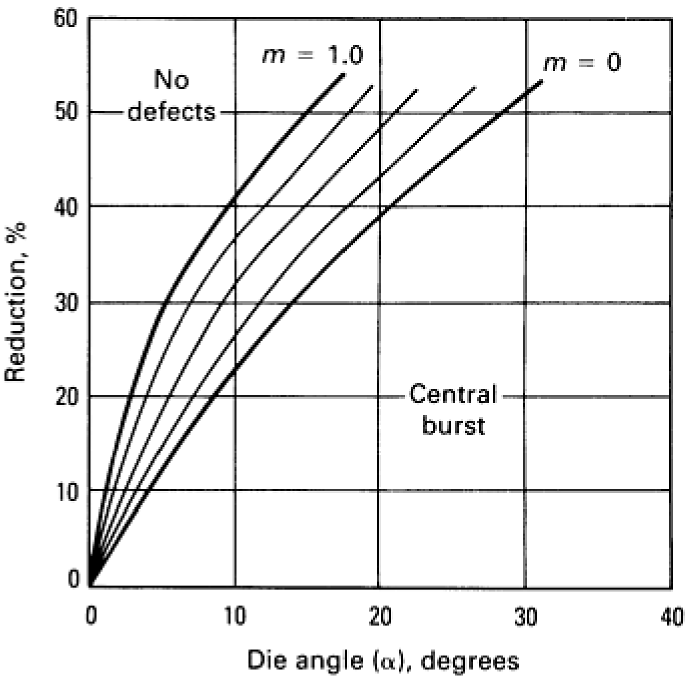

Fig. 19 Upper bound prediction of central burst in wire drawing. Increasing friction, expressed by the friction factor *m*, increases the defect region of the map. Source: Ref 10.

It should be pointed out that the upper bound method for defect prediction gives only a necessary condition. The strain-hardening and strain rate hardening characteristics of the material have been included in the analysis (Ref 10), but the microstructural characteristics have been omitted. Therefore, when operating in the central burst range illustrated in Fig. 19, fracture can occur; whether or not it will depends on the material structure (voids, inclusions, segregation, and so on). When operating in the safe area shown in Fig. 19, central burst will not occur, regardless of the material structure.

Tensile Stress Criterion. The role of tensile stress in fracture is implicit yet overwhelmingly clear throughout the discussion of fracture and fracture criteria. The calculation of tensile stresses in localized regions, however, requires the use of advanced plasticity analysis methods, such as slip-line fields or finite-element analysis. One result of slip-line field analysis that has wide application in workability studies is discussed below.

Double indentation by flat punches is a classical problem in slip-line field analysis (Fig. 20). The boundaries of the deformation zone change as the aspect ratio *h*/*b* (workpiece thickness-to-punch width) increases. For $h/b > 1$, the slip-line field meets the centerline at a point, and for $h/b < 1$ the field is spread over an area nearly as large as the punch width.

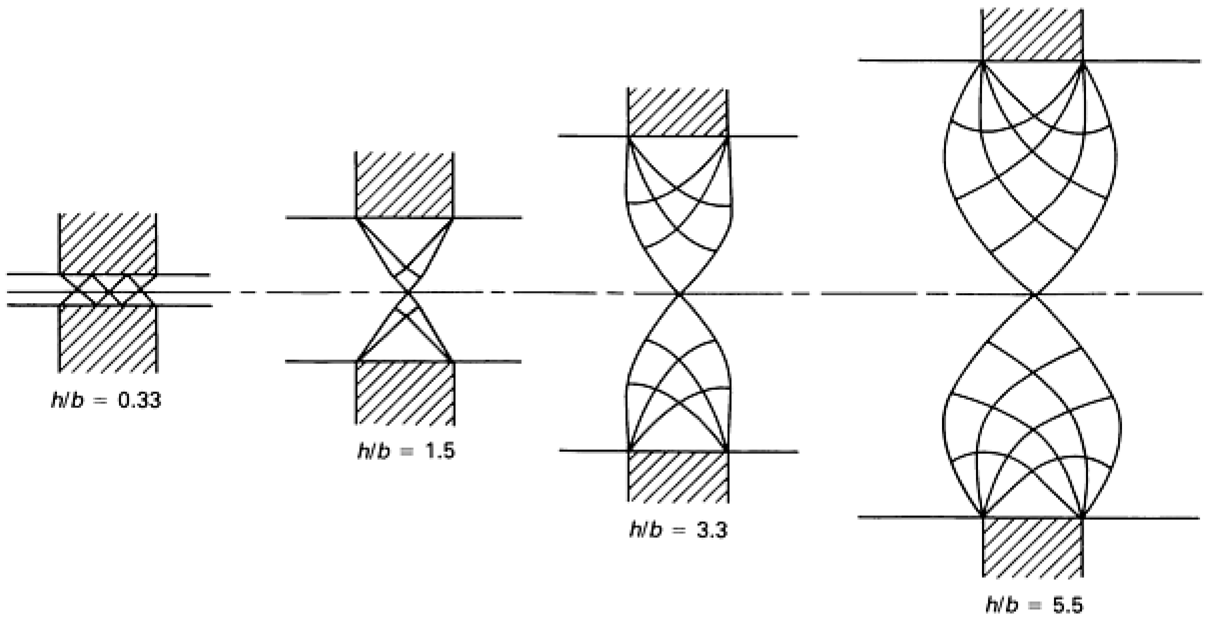

Fig. 20 Slip-line fields for double indentation at different *h*/*b* ratios. Source: Ref 11.

This tooling arrangement and deformation geometry approximates several other metalworking processes, as shown in Fig. 21. For similar *h*/*b* ratios in these processes, then, the stresses throughout the deformation zone of the process can be approximated by those calculated from slip-line field analysis for double indentation.

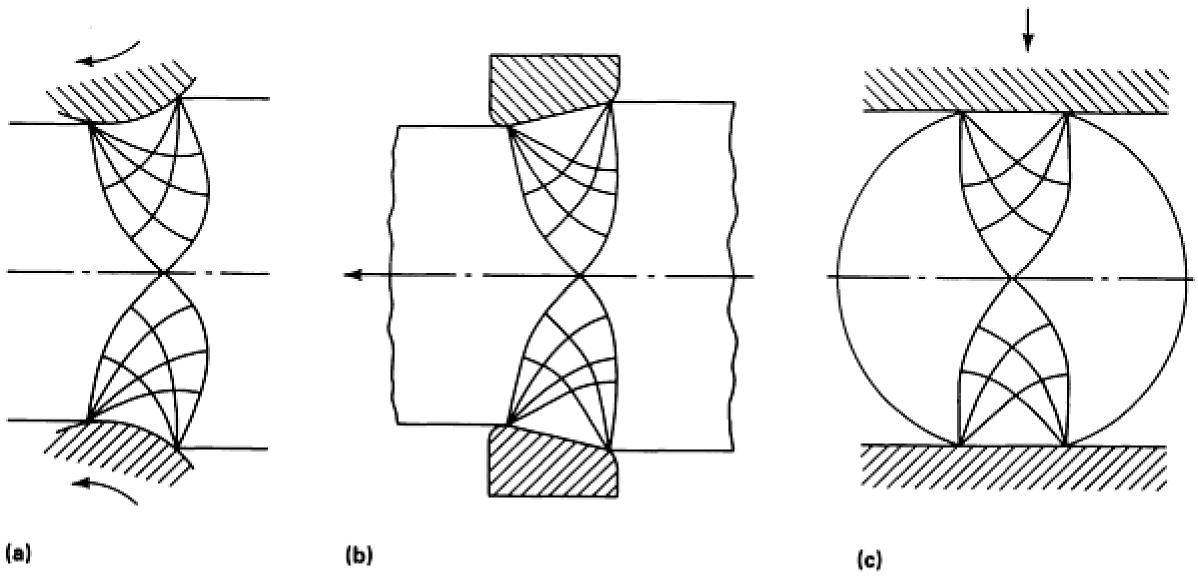

Fig. 21 Slip-line fields for rolling (a), drawing (b), and side pressing (c). These fields are similar to those for double indentation shown in Fig. 20.

Most of the work on double indentation has focused on the calculation of punch pressure and the extrapolation of these results to other processes--for example, those in Fig. 21. In a very detailed study of double indentation, remarkable agreement was found between experimental results and slip-line field results (Ref 12).

For workability studies, however, it is necessary to locate and to calculate the critical tensile stresses. It can be shown that the hydrostatic stress is always greatest algebraically at the centerline of the material and that this stress is tensile for $h/b > 1.8$. The calculated results for punch pressure and centerline hydrostatic stress are given in Fig. 22. Therefore, it is necessary to specify die and workpiece geometric parameters such that $h/b < 1.8$ in order to avoid tensile stress and potential fracture at the centerline of the processes shown in Fig. 21.

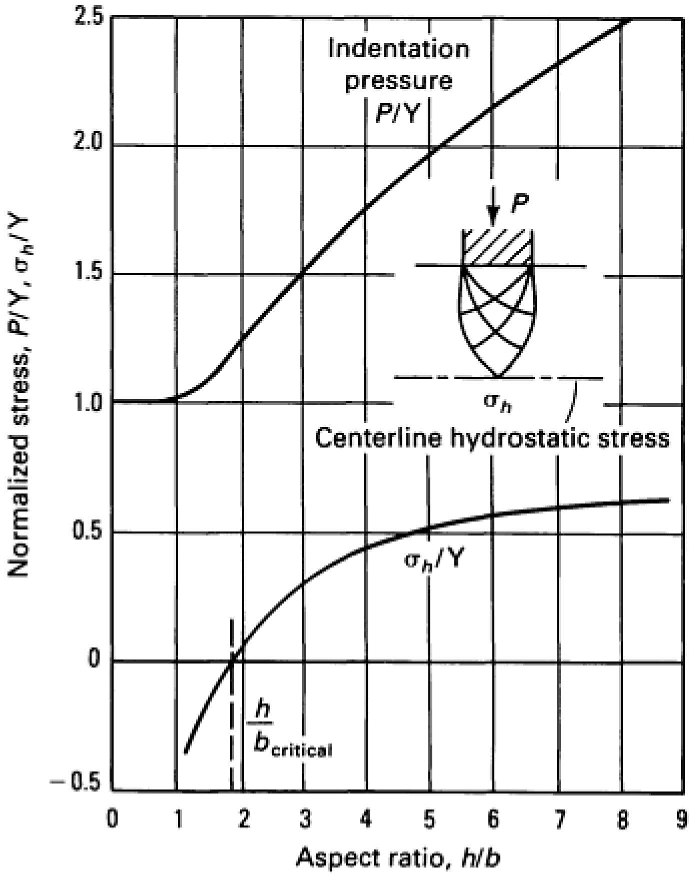

Fig. 22 Variation of the normalized indentation pressure (*P*/Y where Y is the yield strength) and the normalized centerline hydrostatic stress (σ_h/Y) with *h*/*b* ratio as calculated from slip-line field analysis.

For example, in extrusion, *h*/*b* is approximated by:

$$h/b = \frac{\alpha[1 + (1 - R)^{1/2}]^2}{R}$$

where α is the die half-angle and *R* is area reduction. Taking *h*/*b* = 1.8, the relationship between α and *R* that produces tensile hydrostatic stress at the centerline can be calculated. The result is given in Fig. 23, which is shown to be similar to the relationship predicted by upper bound analysis (Fig. 19). The correlation is remarkable in view of the dissimilarity in die shape between extrusion and double indentation. Furthermore, the similarity emphasizes that the flow mode for defect formation in the upper bound method is physically equivalent to the development of tensile hydrostatic stress.

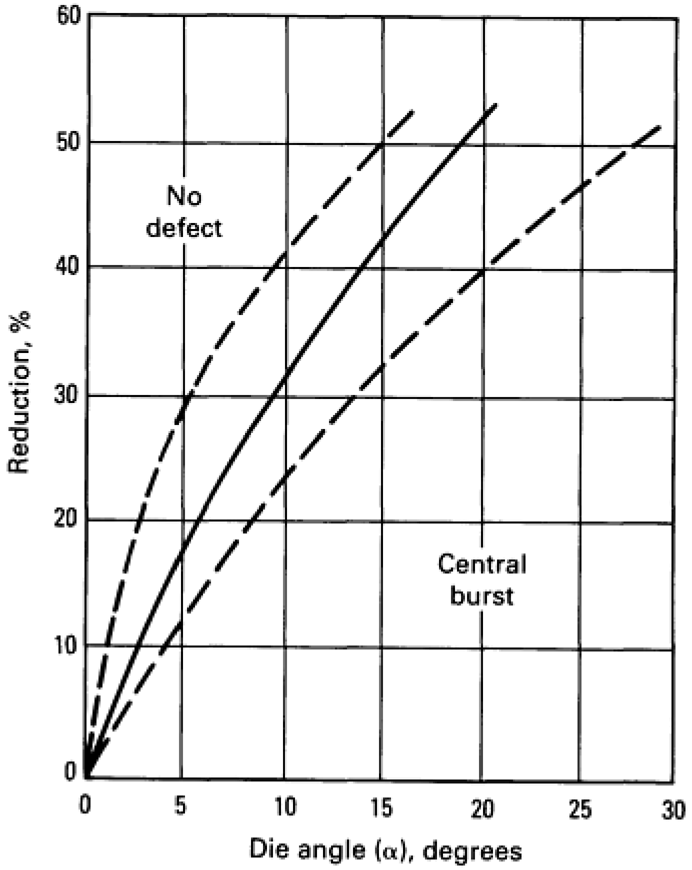

Fig. 23 Prediction of central burst in wire drawing by the tensile stress criterion and slip-line field analysis of double indentation. The range of predictions by upper bound analysis (Fig. 19) is shown by dashed lines.

As in the case of the upper bound method, the existence of a tensile hydrostatic stress does not ensure fracture, but it is a necessary ingredient. The material structure must be considered in conjunction with the tensile stress. In other words, the upper bound and tensile stress criteria are useful for defining approximate deformation limits and successful process parameters, but more detailed criteria or models are required to provide more exact values. An experimental-analytical approach, then, would be most useful in which an experimental value reflecting the inherent material ductility is determined first (this value is used to define a point on the fracture strain locus), followed by development of the rest of the fracture limit line, as in Fig. 10, 11, 12, 14, 16, and 17.

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Workability Theory and Application in Bulk Forming Processes

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Applications

The fracture criteria discussed previously in this article can be used as tools for troubleshooting fracture problems in existing processes or for designing/modifying processes for new products. In either case, graphical representation of the criteria permits independent consideration of the process and material parameters in quantitative or qualitative form.

An example is the bolt-heading process shown in Fig. 24(a). If it is required to form a bolt head diameter *D* from the rod of diameter *d,* the required circumferential strain is ln (*D*/*d*), indicated by the horizontal dashed line in Fig. 24(b). The strain paths that reach this level, however, depend on process parameters, as shown previously in Fig. 2(b), and the fracture strain loci vary with material, as shown in Fig. 10, 11, and 12. Referring to Fig. 24(b), if strain path ***a*** describes the strain state at the expanding free surface for one set of processing conditions and the material used has a forming limit line labeled A, then, in order to reach the required circumferential strain, the strain path must cross the fracture line, and fracture is likely to occur. As shown, one option for avoiding defects is to use material B, which has a higher fracture limit. Another option is to alter the process so that strain path ***b*** is followed by the material. The latter option represents a process change, which in this case involves improved lubrication, as shown in Fig. 2. This procedure has been quantified and implemented in a computerized tool for upsetting process design (Ref 13).

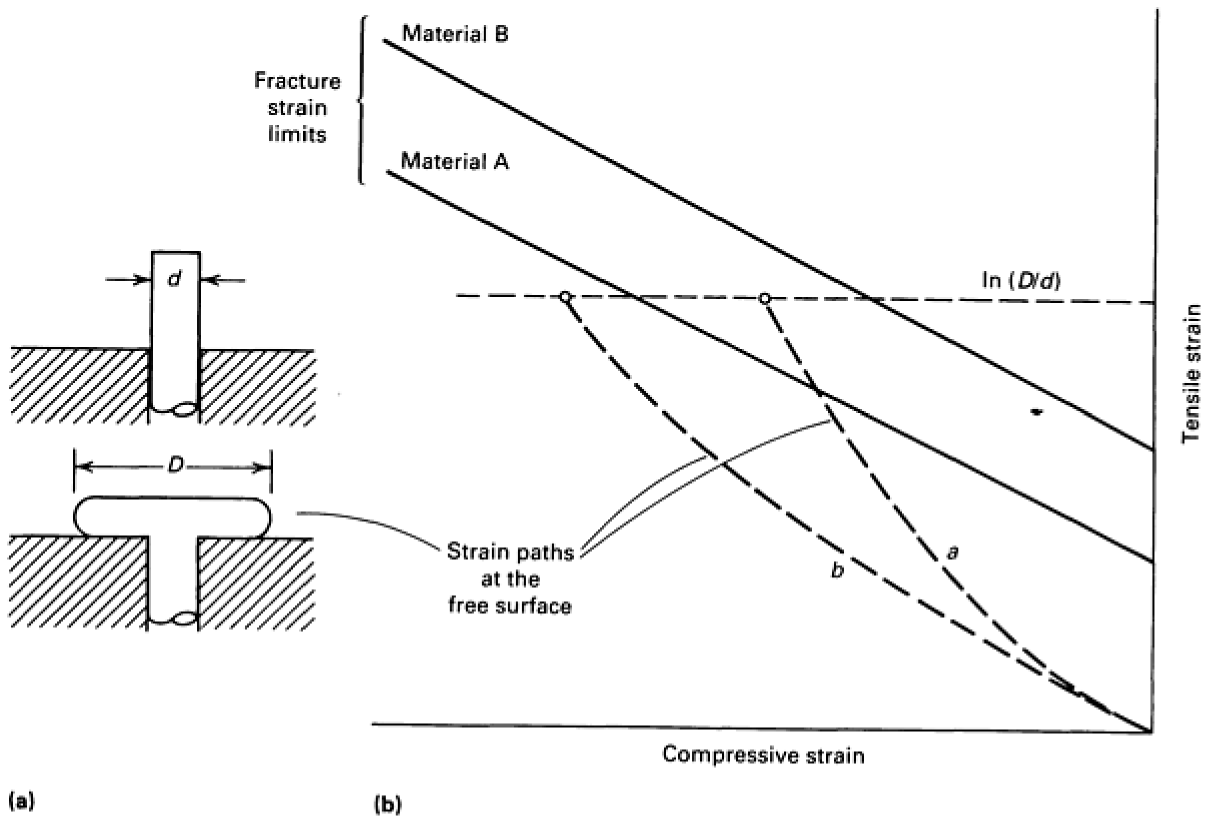

Fig. 24 Upsetting (a) of bar diameter *d* to head diameter *D.* (b) Material fracture strain limits are superimposed on strain paths reaching the final required strain. Strain path *b* (low friction) prevents fracture for both

materials. Material B avoids fracture for either strain path.

In more complex cases, other means are available for altering the strain path, such as modification of die design, workpiece (preform) design, and redistribution of lubricant. Examples of application to powder forging preform design and other metalworking processes are given in Ref 14 and 15.

The workability concept presented above provides a useful supplement to the experience and intuition of the die designer because it presents a graphical and quantitative description of the relationship between material and process parameters. Below are some examples of the application of the workability analysis procedures described previously.

Bar Rolling. As shown in Fig. 3, the strains at the edges of bars during rolling are similar to those at the bulging free surface of a cylinder during compression. It should be possible, then, to predict fracture in bar rolling from compression tests on the alloy of interest. This is pertinent in current attempts to roll ingots of high alloy content into bar form. The complete workability study of bar rolling includes physical modeling of bar rolling to obtain the strain states at the edges of the bar, compression tests to obtain the material fracture limits, and comparison of the two sets of results to establish roll pass reduction limits.

Such a study is illustrated by the analysis of cracking during the rolling of 2024-T351 aluminum alloy bars. The intent was to roll square bars into round wire without resolutioning. Rolling was done on a two-high reversible bar mill with 230 mm (9 in.) diam rolls at 30 rpm (approximate strain rate: 4 s^{-1}). The roll groove geometry is shown in Fig. 25. Defects occurred primarily in the square-to-diamond passes (1-2 and 3-4), but the two diamond-to-square passes (2-3 and 4-5), the square-to-oval pass (5-6), and the oval-to-round pass (6-7) were also examined for completeness.

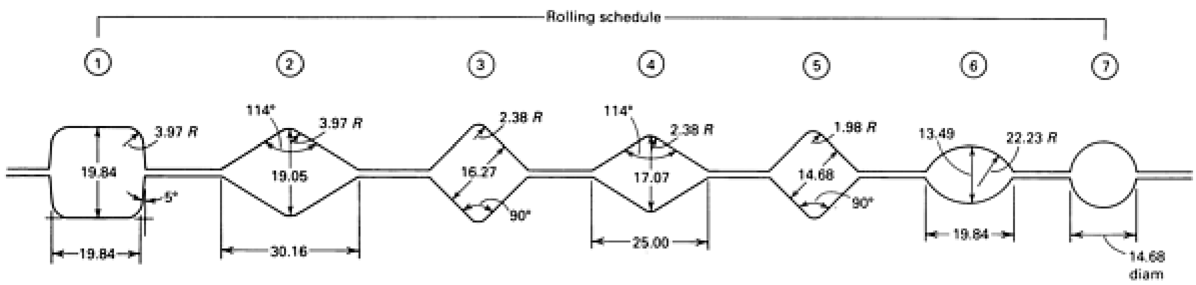

Fig. 25 Roll groove geometry for rolling square bars into round wire. Dimensions given in millimeters.

Lead was used as the simulation material for the physical modeling of bar rolling. Pure (99.99%) lead was cast and extruded into 25 mm (1 in.) round bars and then squared in the box pass (step 1, Fig. 25). Grids were placed on the lateral edges of the bars by an impression tool, and the grid spacing was measured before and after each pass for calculation of the longitudinal, ε_1, and vertical, ε_2, strains. Different reductions in area were achieved by feeding various bar sizes and by changing roll separation distances. A transverse slice was cut from the bars after each pass for measurement of the cross-sectional area and calculation of the reduction.

Results of the strain measurements are summarized in Fig. 26, in which tensile strain is plotted simultaneously with the compressive strain and reduction. As expected, the square-to-diamond passes involve the least compressive vertical strain, and the square-to-oval pass has the greatest compressive strain. The tensile strain versus reduction plot is the same for all cases, reflecting volume constancy.

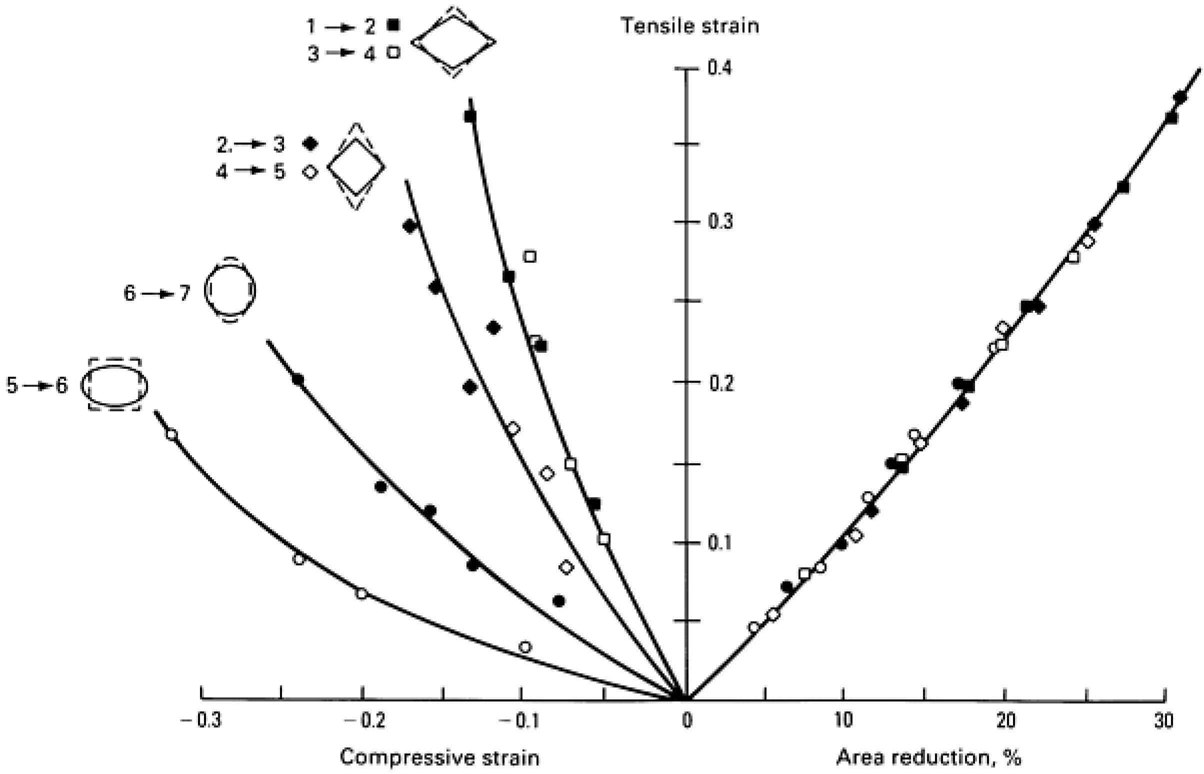

Fig. 26 Measured localized strains during the rolling of lead bars. Left side shows longitudinal tensile strain versus vertical compressive strain. Right side shows longitudinal strain versus cross-sectional area reduction at room temperature.

Compression tests were performed on the 2024 aluminum alloy at room temperature and at 250 °C (480 °F) at a strain rate of 4 s^{-1} to determine fracture limit lines. Straight, tapered, and flanged specimen profiles were used. Results are given in Fig. 27. Superposition of Fig. 26 onto Fig. 27 gives the rolling deformation limits.

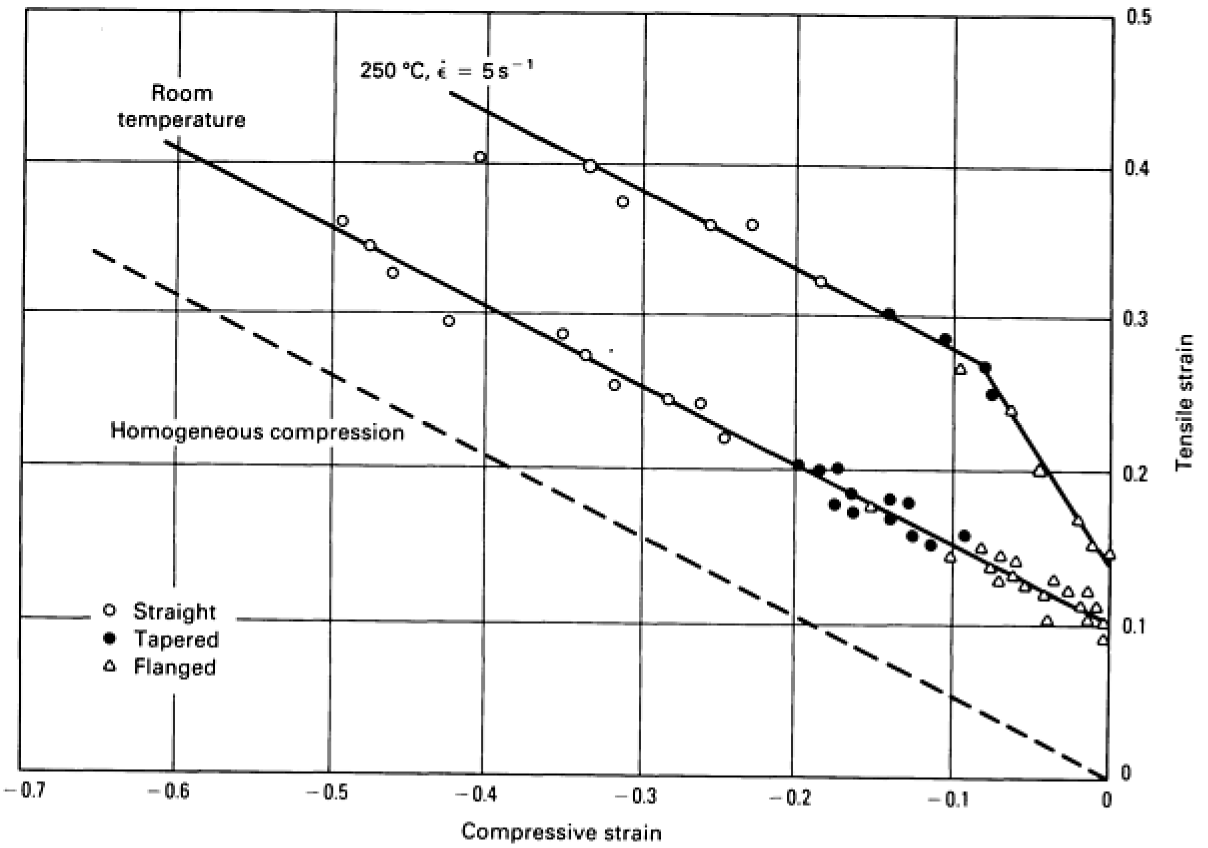

Fig. 27 Fracture strain lines for 2024 aluminum alloy in the T351 temper, measured by compression tests at room temperature and at 250 °C (480 °F).

To test the workability predictions, aluminum alloy bars were rolled at room temperature and at 250 °C (480 °F). Grid and area reduction measurements were made for the square-to-diamond passes. Figure 28 shows the measured strains at room temperature, which agree with those measured in lead bars for the same pass (Fig. 26). Open circles indicate fracture, and closed circles indicate no fracture. The fracture line for the aluminum alloy at room temperature is superimposed as the dashed line. It is clear that edge cracking in bar rolling conformed with the material fracture line, and the limiting reduction is approximately 13% for this combination of material and pass geometry. Similarly, at 250 °C (480 °F), there was conformance between fracture in bar rolling (Fig. 29) and the fracture line of the alloy (Fig. 28). In this case, the limiting reduction is approximately 25%.

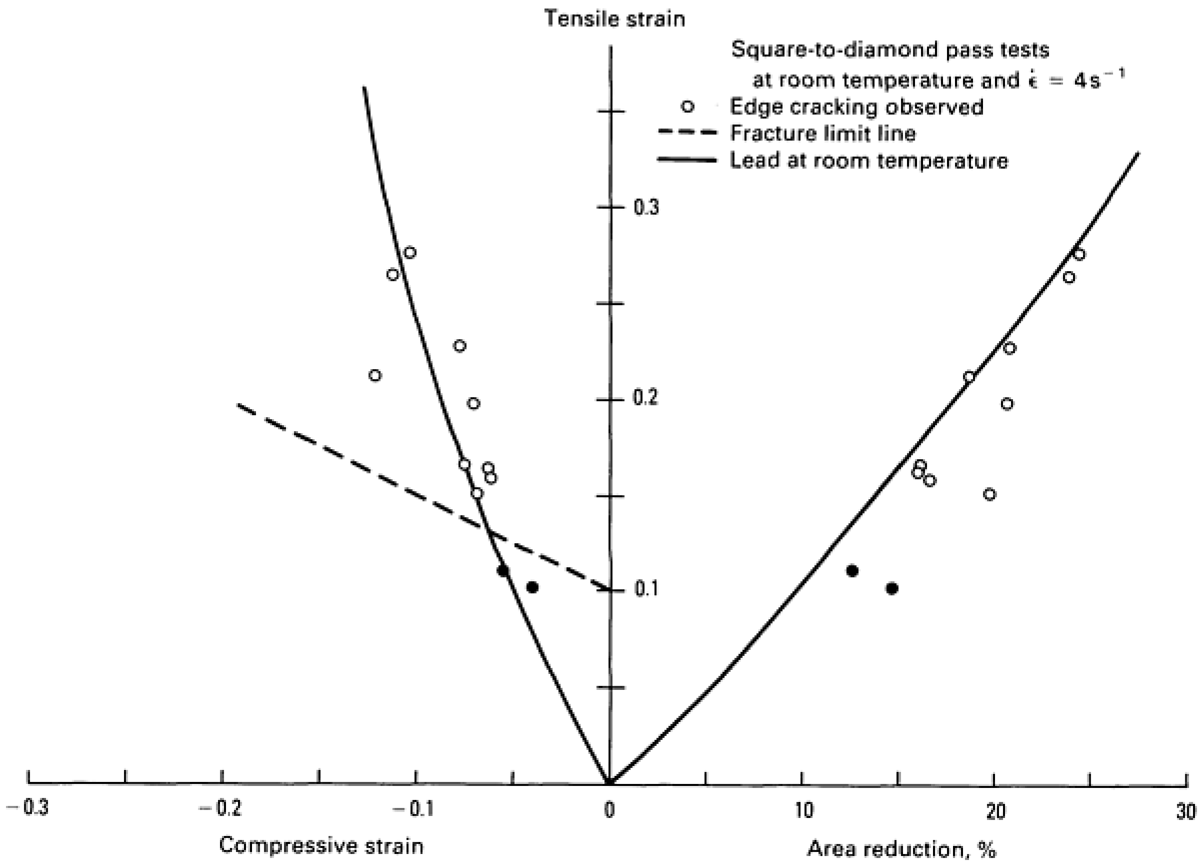

Fig. 28 Superposition of fracture line (dashed) on measured strains during rolling of 2024-T351 aluminum alloy bars at room temperature. Solid line represents the strain path measured during rolling of the lead model material shown in Fig. 26.

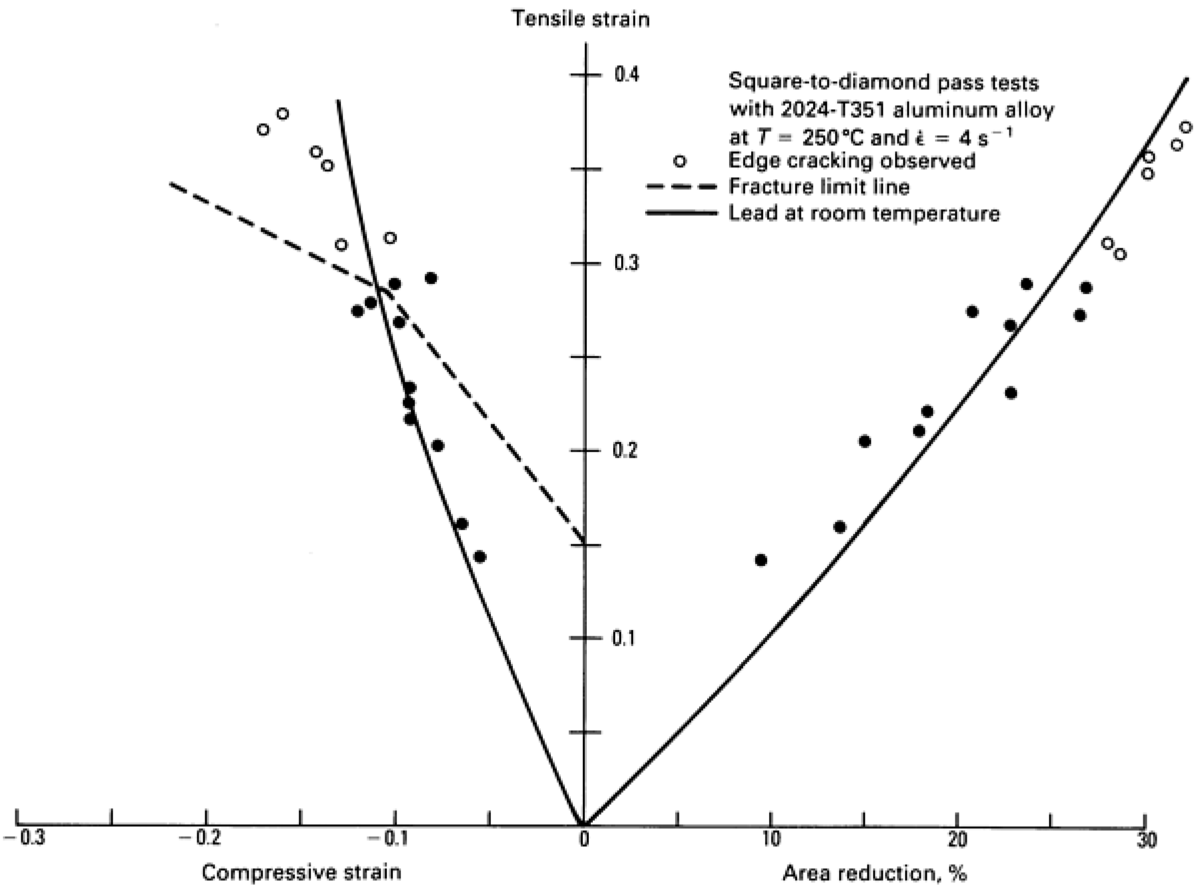

Fig. 29 Superposition of fracture line (dashed) on measured strains during the rolling of 2024-T351 aluminum alloy bars at 250 °C (480 °F). Solid line represents the strain path measured during rolling of the lead model material shown in Fig. 26.

Extrapolating the above results for cold rolling, the limiting reduction for diamond-to-square passes would be approximately 15%; for the oval-to-round pass, approximately 20%; and for the square-to-oval pass, approximately 25%. Similarly, in the hot rolling of this aluminum alloy, the reduction limit for diamond-to-square passes would be approximately 27%; for oval-to-round passes, approximately 30%; and for square-to-oval passes, approximately 40%. The latter two are beyond the reduction normally used because of fin formation, so cracking rarely occurs in such passes.

Example 1: Preform Design for a Ball Bearing Race.

A low-load high-torque ball bearing outer race was cold forged from a low-alloy steel powder preform (Fig. 30). The preforms were compacted from 4600 grade powder with carbon added to give 0.20% C in the sintered material. The sintered preforms were 80% of theoretical density.

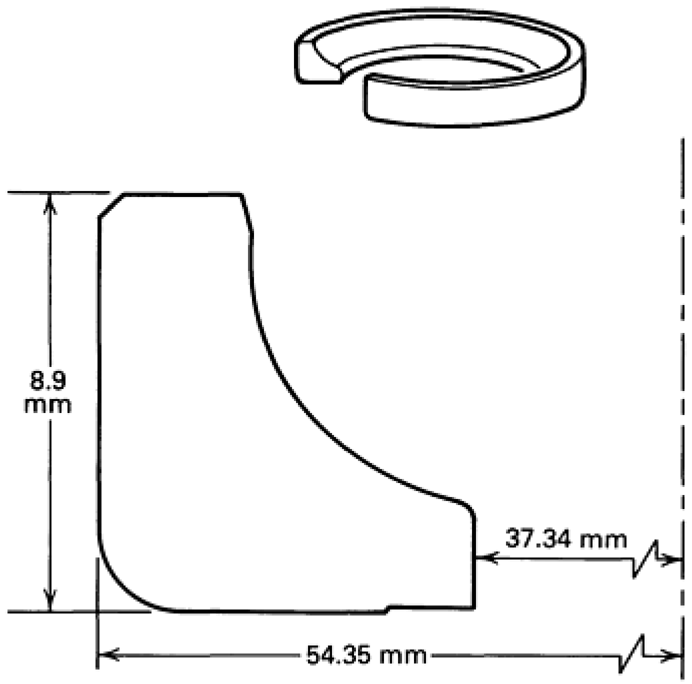

Fig. 30 Ball bearing outer race that was cold forged from sintered powder preform of 4620 low-alloy steel.

Initial efforts led to cracking through the preform along a diagonal beginning at the point of contact between the punch and preform (Fig. 31). The large shear stress developed by the contact was beyond the fracture limit of the porous preform, and two solutions were considered that would avoid such stresses (Fig. 32). The first solution was the use of a flat preform that involves back extrusion flow into the outer rim, and the second was the use of a tall, thin-wall preform that involves radial inward flow into the inner flange. The first option was rejected because it would generate circumferential tension that would most likely cause fracture. The second option is desirable because compression is applied at the top face; this option was pursued through physical modeling.

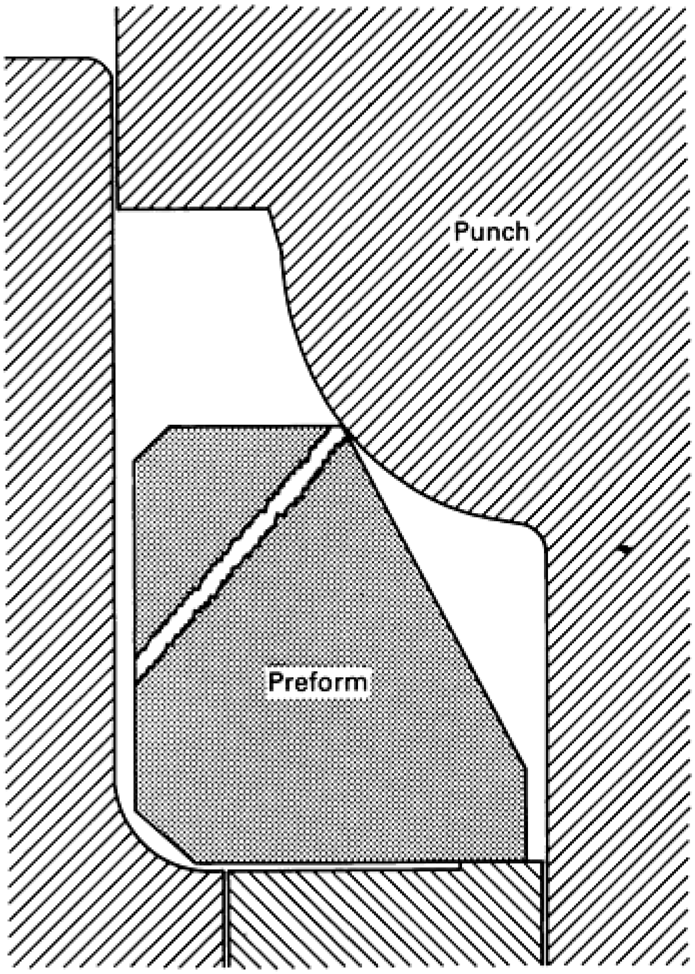

Fig. 31 Cracks initiated at the point of contact between the punch and preform in the original preform design. The preform had a taper of 30° on the inside diameter.

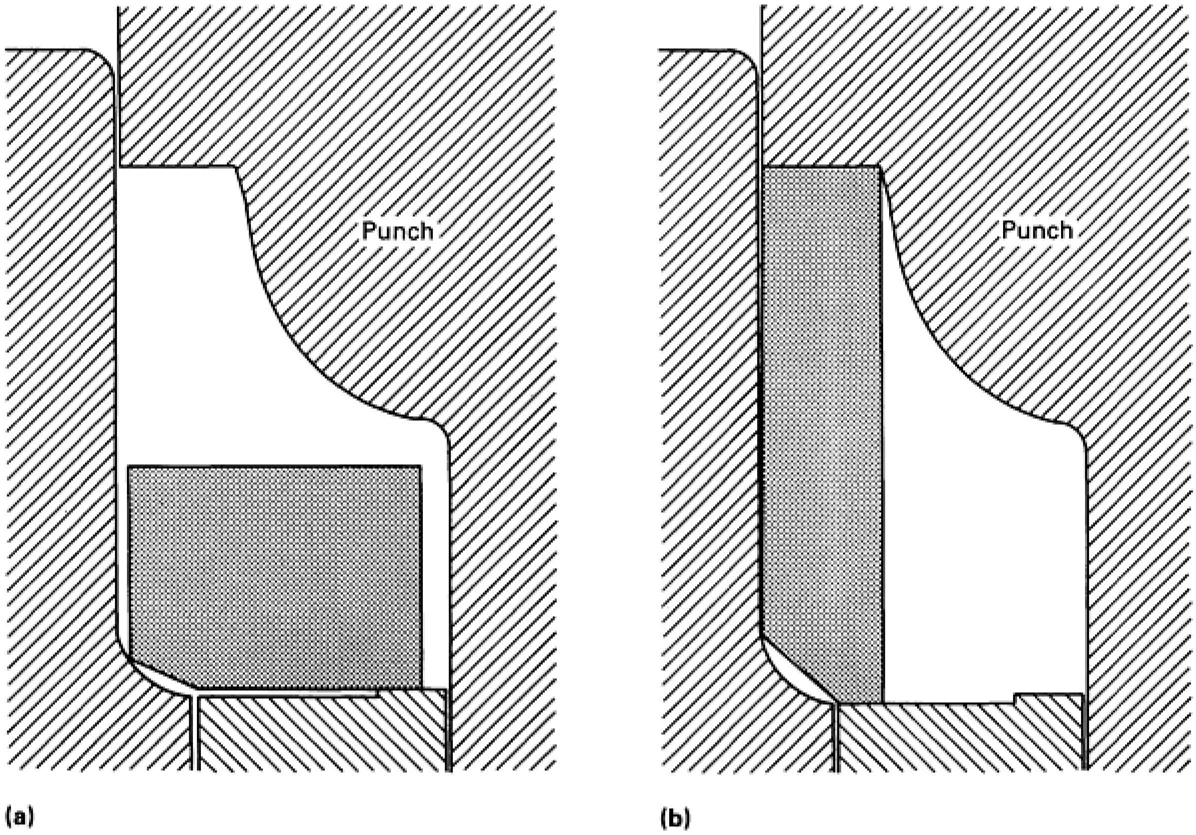

Fig. 32 Preform alternatives for forging the ball bearing outer race shown in Fig. 30. (a) Back extrusion. (b) Compression and radial inward flow.

The primary concern with the second option (Fig. 32b) was the large amount of radially inward deformation required to form the inner flange. As a result, this option was examined by physical modeling. Model preforms were produced from sintered 601AB aluminum alloy powder and gridded on the inside surface (Fig. 33a). Grid displacements were measured after each of several increments of deformation, and the calculated strains were plotted along with the fracture line of the material (Fig. 33b). It is clear that both the axial and circumferential strains are compressive throughout the process and do not exceed the fracture line. Some wrinkling of the inside surface occurred, but this was smoothed out when the surface contacted the mandrel under pressure.

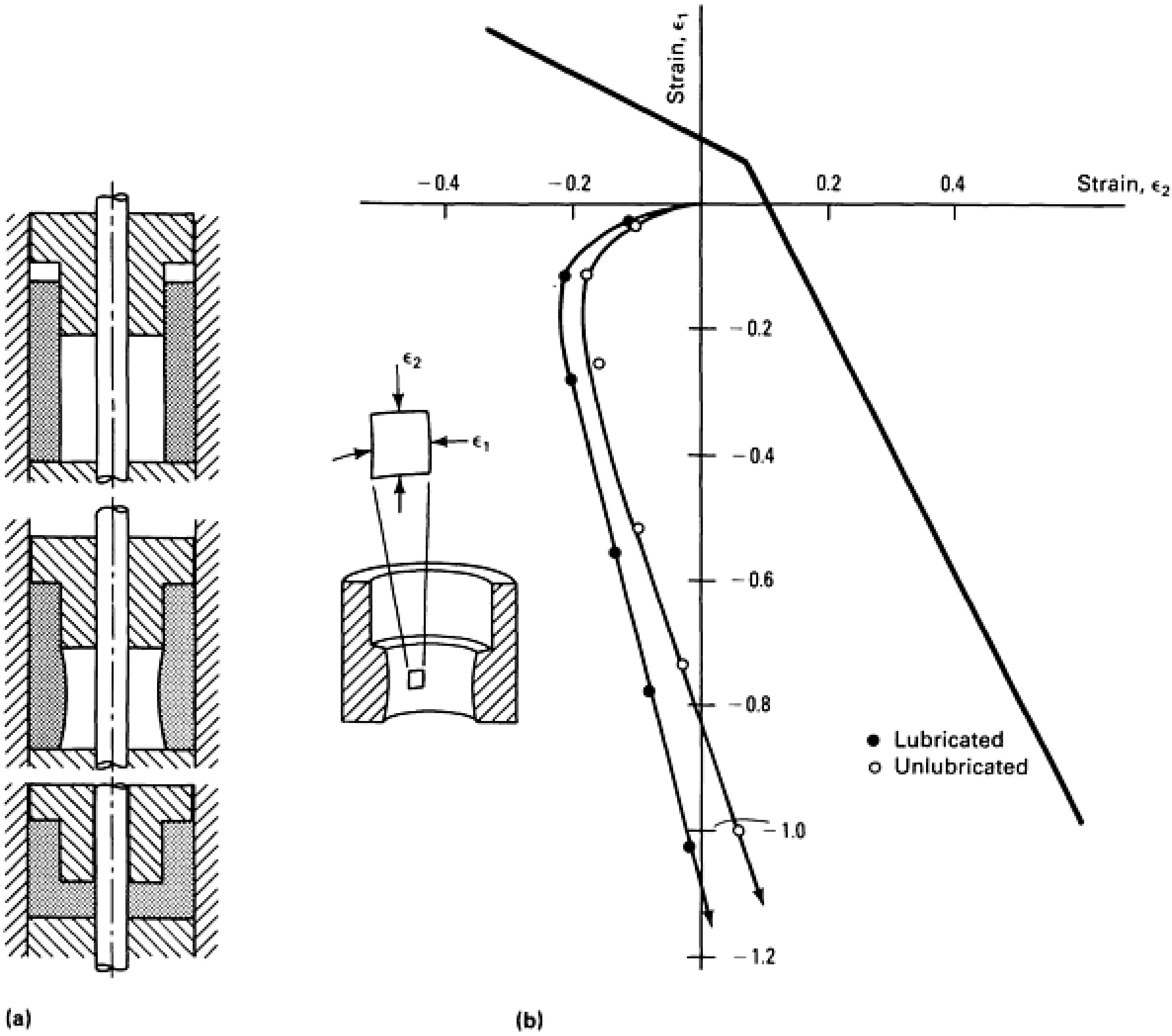

Fig. 33 Physical model (a) for the second option (Fig. 32b) and the measured strains (b) during forging of the preform. The heavy line is the material fracture line. It is clear that the strain path never crosses the fracture line and that defects are prevented.

Actual production of straight-wall preforms, as in Fig. 33(a), was not feasible, because the height-to-thickness ratio is too large for compaction. A compromise was developed in which the preform angle was 17° instead of 30°, as used in the original preform, or 0°, as used in the physical model (Fig. 33b). This ensured initial punch contact at the top face of the preform and generated compressive strains on the inside surface, as in Fig. 33(b). Cold-forging trials on these preforms produced no cracks, developed the desired full density in the ball path region, and showed the added benefit of a smooth ball path surface that did not require grinding.

Example 2: Back Extrusion of Copper Alloy.

A low-ductility dispersion-strengthened copper alloy was back extruded into a cup shape, as shown in Fig. 34. The deformation was carried out at room temperature on a mechanical press. Crack formation on the rim caused high rejection rates. The original slugs for this process were smaller in diameter (16 mm, or 0.625 in.) than the die inside diameter (19 mm, or 0.75 in.), as shown in Fig. 35(a). Deformation of such preforms involves circumferential expansion strain (equal to ln (D/d), where D is the die bore diameter and d is the slug diameter), along with very little compressive strain at the rim (Fig. 35b). For this case, the circumferential strain is ln (0.75/0.625) = 0.18. Workability analysis would then require only measurement of the material fracture line and comparison with the required circumferential strain 0.18.

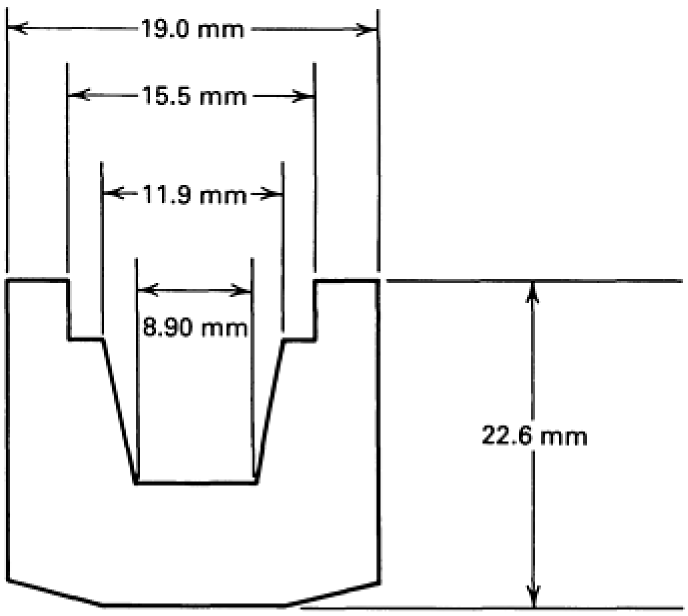

Fig. 34 Part that was back extruded from copper alloy.

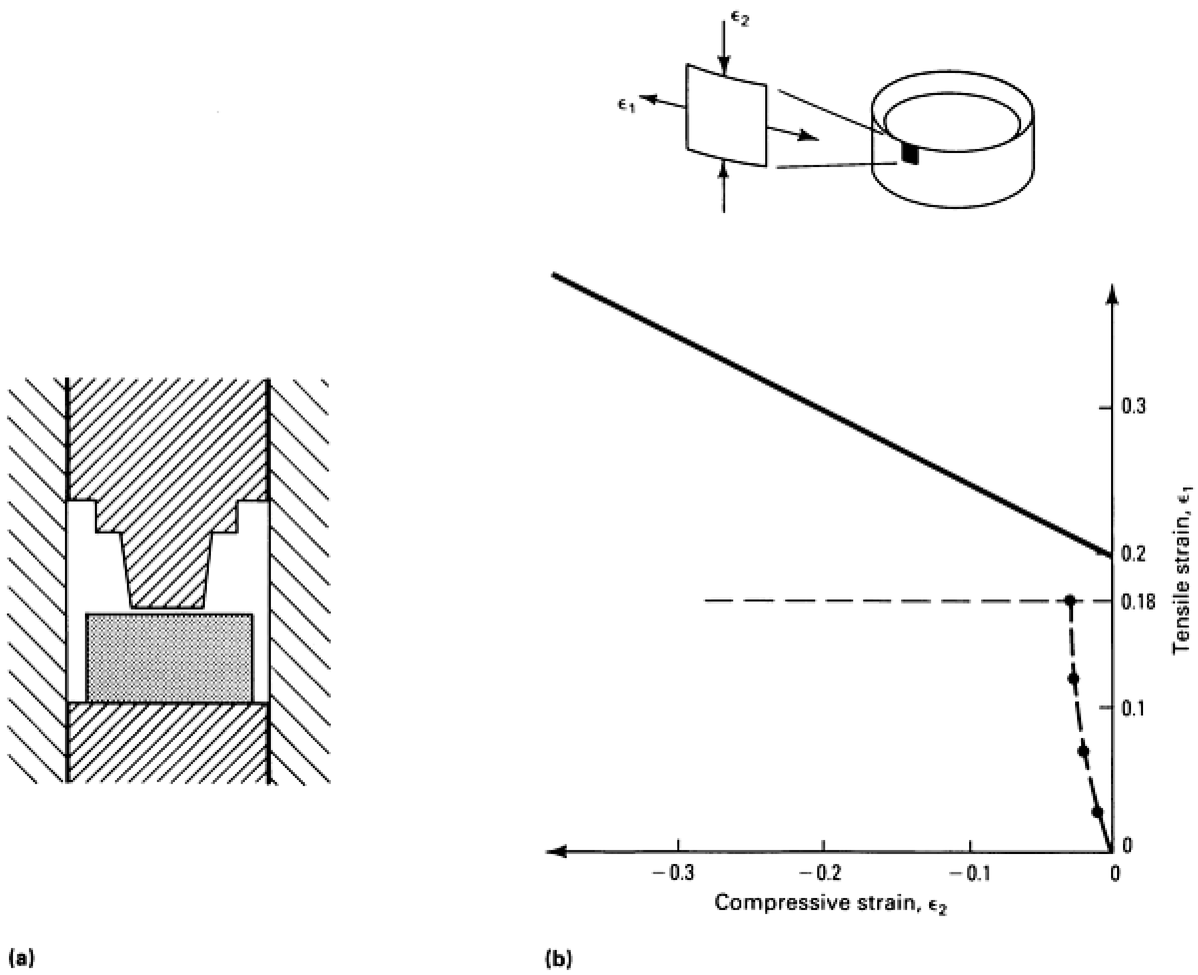

Fig. 35 Back extrusion of the cup shape shown in Fig. 34. (a) The preform slug was 16 mm (0.625 in.) in diameter; the die was 19 mm (0.75 in.) in diameter. (b) Strains at the cup rim where fracture occurred consist of circumferential tension to a value of 0.18, and very little compressive strain. The heavy line is the material

fracture strain line.

Fracture strains were measured in flange compression tests, as shown in Fig. 36, giving a minimum circumferential strain of 0.2, which is sufficiently above the required strain for avoidance of fracture. A hydraulic press was used (giving a strain rate of approximately 0.5 s^{-1}), on the assumption that there is no strain rate effect at room temperature. Because the workability analysis showed that fracture should not be a problem, the effect of strain rate was explored further.

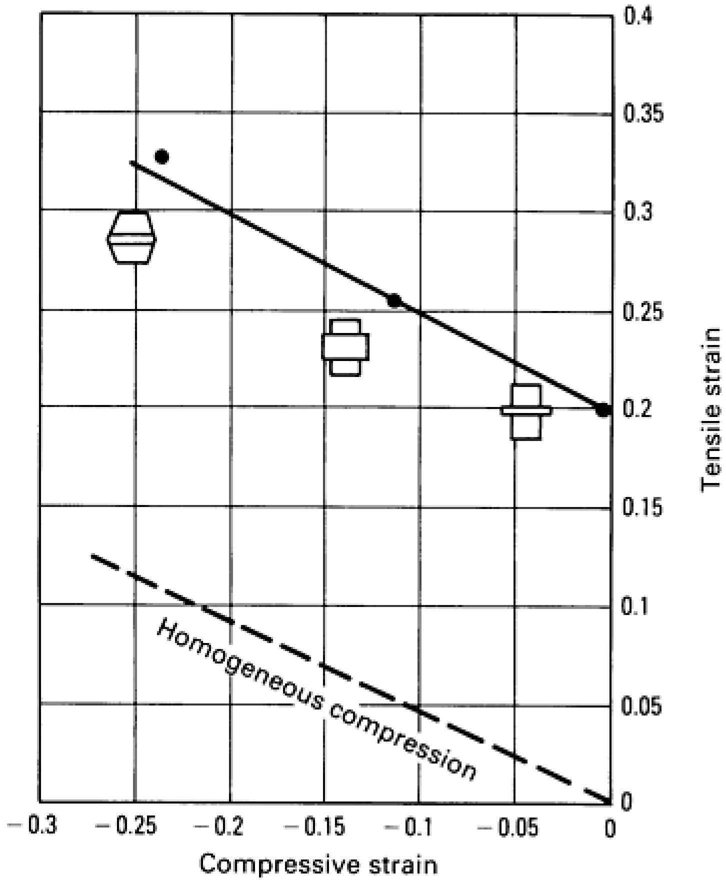

Fig. 36 Fracture strain line for copper alloy, determined by flanged and tapered compression tests. Specimen geometries used for each test are also shown.

Tests were performed on the same alloy using controlled strain rate servohydraulic test equipment at strain rates of 5, 10, and 15 s^{-1}; the third strain rate given is close to that in the production mechanical press. Figure 37 shows the surprising result that the fracture limit line decreases with increasing strain rate. In particular, the minimum circumferential strain falls below the required value of 0.18 for successful forming of the rim; this explains the occurrence of fracture on the production press. The problem was corrected by using slugs of larger diameter to decrease the circumferential tension and by preforming a taper on the top face (Fig. 38), which produced some axial compression in the material at the rim. The strain path then avoided crossing the fracture line (Fig. 39), and the rejection rate during production on the mechanical press was nil.

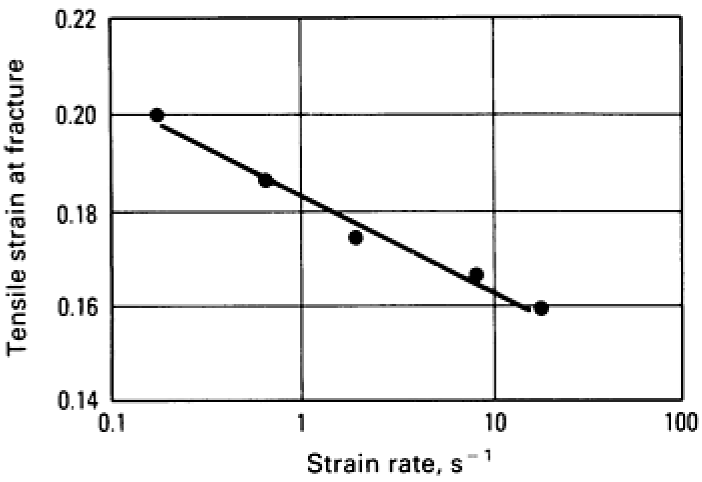

Fig. 37 Decrease in circumferential tensile strain at fracture with increasing strain rate for the copper alloy tested in Fig. 36. Results are for thin-flanged compression specimens, which have the lowest fracture strain.

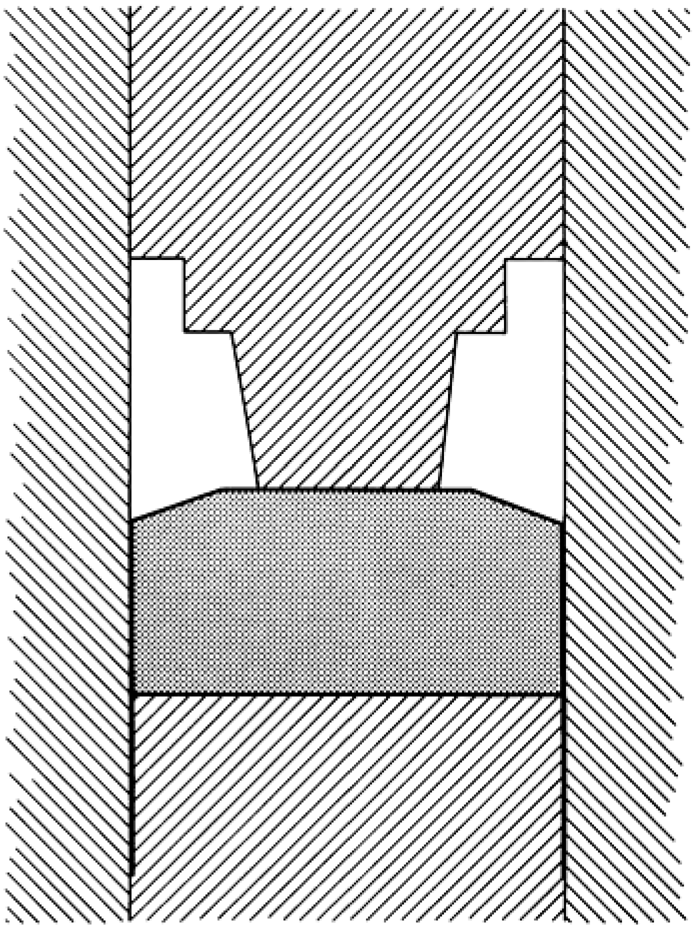

Fig. 38 Modified preform slug for the back extrusion of the cup shape shown in Fig. 34. The slug diameter is 18.8 mm (0.74 in.) and has a 5° taper on the top surface.

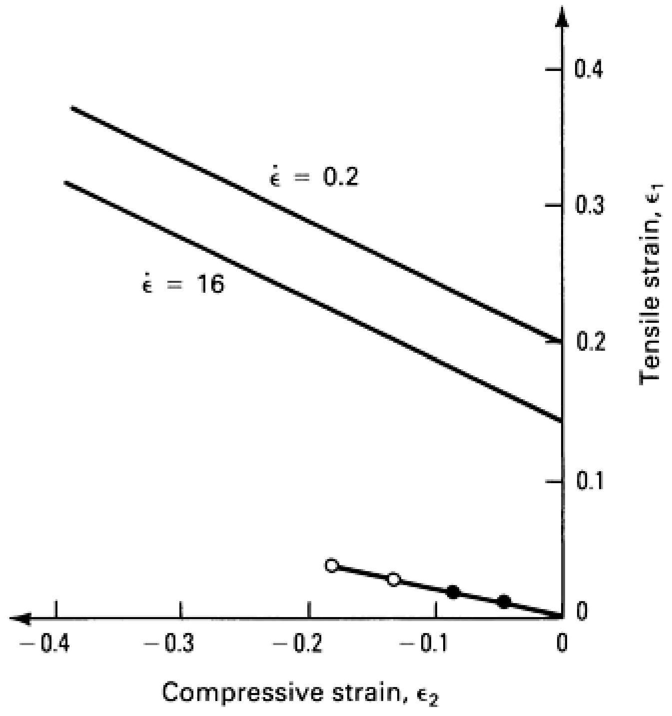

Fig. 39 Comparison of measured strains at the cup rim during back extrusion of the modified preform slug shown in Fig. 38. The strains do not exceed the material fracture strain line for low or high strain rate forming.

Contact Surface Fracture and Internal Fracture. All of the previous applications and examples involved free surface fractures and could be treated directly by the fracture line. Consideration of contact surface fractures (Fig. 5) and internal fracture (Fig. 6), however, requires modification of this approach or use of a new approach. In the following, an example is given of the application of the upper bound and tensile stress criteria to central burst in extrusion. The empirical workability concept described previously is then modified for application to contact surface fracture as well as central burst.

Example 3: Central Burst During Extrusion.

Central burst can occur in extrusion when light reductions and large die angles are used (Fig. 19) and it is encountered in the production of shafts for transmissions and suspension systems. A test of the central burst criterion was carried out by processing shafts from hot-rolled 1024 steel bars 22 mm ($\frac{7}{8}$ in.) in diameter (Ref 16). The processing sequence consisted of initial drawing followed by three extrusion steps in a boltmaker:

Process	**Reduction, %**	Die half-angle, degrees
Drawing	8	**9**
Extrusion	22	**22.5**
Extrusion	23	**22.5**

Extrusion	**16**	**22.5 or 5**

All passes are in the central burst area of Fig. 40, except for the last pass with a 5° die angle.

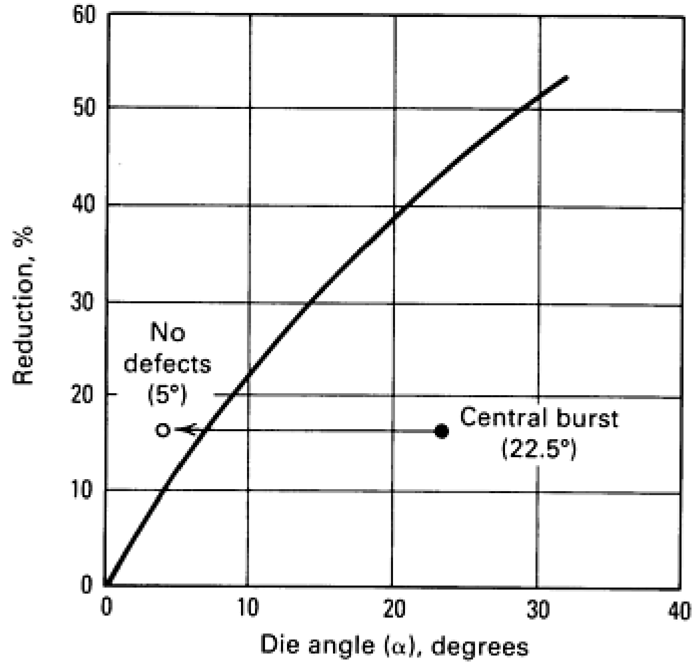

Fig. 40 Location of process conditions on a theoretical central burst map. For an angle of 22.5°, central burst occurred in 4.5% of the extruded shafts. For a die angle of 5° no central burst occurred.

A total of 1000 shafts were processed with the 22.5° die, and 500 shafts with the 5° die. All shafts were tested ultrasonically for internal defects. Central bursting was detected in 4.5% of the shafts extruded with the 22.5° die, and no defects were detected in the shafts extruded with the 5° die. These results show that the upper bound central burst criterion is a necessary condition. It was further shown in Ref 16 that central burst was avoided in other heats with slightly different compositions because their strain-hardening coefficients were larger than the original heat. This confirmed the predicted results in Ref 10.

Modified Empirical Criterion. It was shown previously in this article that measured free surface strains at fracture fit a linear or bilinear line that constitutes a fracture locus for the material tested (Fig. 10, 11, 12). This is a convenient representation of the complexities of ductile fracture, which are controlled by stress and deformation. The experimental fracture locus is also reproduced by several theoretical fracture criteria (Fig. 14, 16, and 17).

For contact surface and internal fractures, however, the surface on which the strains can be monitored is subjected to stress normal to that surface. It was shown in Eq 3 that stress states leading to a given set of surface strains differ only by a hydrostatic stress component, and this component is equal to the applied stress normal to the surface on which the strains are monitored. Experience shows that this hydrostatic stress affects fracture, and it should also affect the fracture strain locus. It should be possible, then, to use the theoretical fracture criteria to predict the effects of hydrostatic stress on the fracture strain locus.

The simplest criterion described previously in this article is that due to Cockcroft; therefore, it was modified to predict the effects of stress normal to the plane (Fig. 5 and 6) on the fracture strains $_1$, and $_2$. The result (Fig. 41) shows that superimposed pressure (P > 0) increases the height of the fracture strain line and also increases its slope slightly. Superimposed tension (P < 0) decreases the height of the fracture line, decreases its average slope, and gives it a slight downward curvature. It is clear that the increase in strains to fracture due to additional pressure is unlimited as pressure increases, but the strains to fracture due to additional tension are limited by zero as tension increases. This result is discussed below with regard to internal fracture and die contact surface fracture.

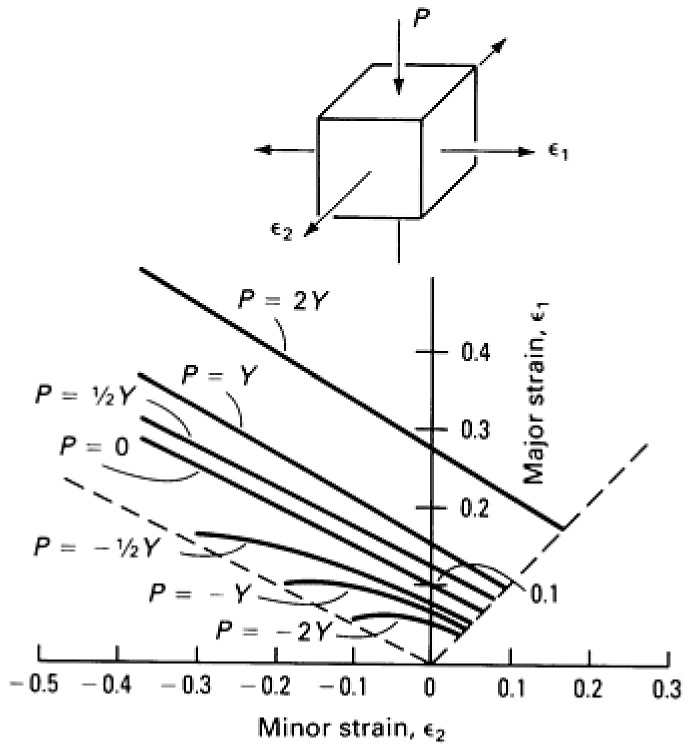

Fig. 41 Movement of the fracture strain line due to superimposed hydrostatic stress. Applied stress is represented in terms of multiples of the yield strength, *Y.* Negative values of *P* indicate hydrostatic tension. Calculations are based on a modification of the Cockcroft criterion.

Central Burst in Forgings. Internal fractures along the centerline of extruded or drawn bars were discussed above (Fig. 18, 19, 23, and 40). Similar fractures are observed served in forged shapes, such as that shown in Fig. 42 for heat-treated 6061 aluminum alloy. Here, as the outer region is compressed between dies, material flows radially inward and then vertically into the opposed hubs. This develops a hydrostatic tensile stress state at the center (Fig. 6a), and fracture is a strong possibility.

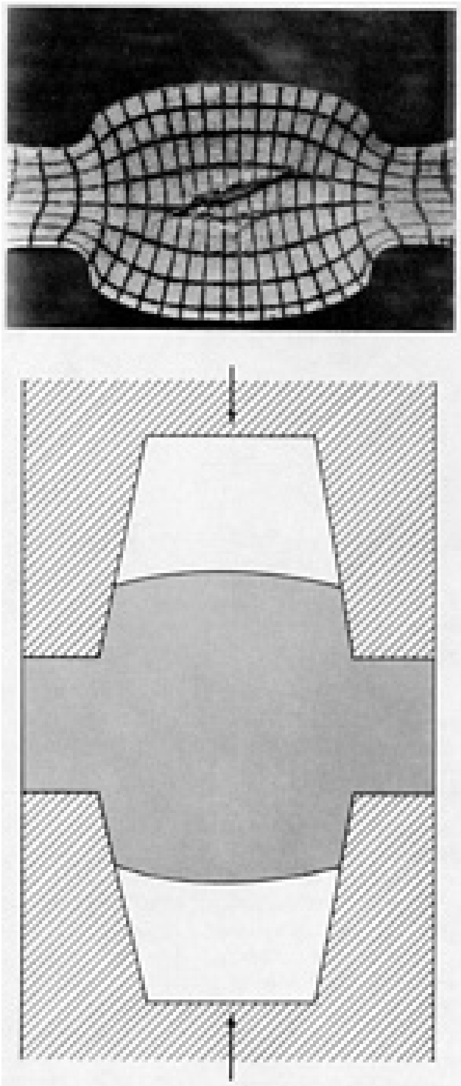

Fig. 42 Internal fracture during the double-extrusion forging of aluminum alloy 6061. Grid deformations on the middle longitudinal plane are shown. Stress and strain states are defined by Fig. 6(a).

Through visioplasticity analysis on split, gridded specimens (Fig. 42), the strain and stresses at the center of the workpiece were calculated for several increments of deformation. The hydrostatic stress state at the center of the specimen is not always tensile; initially, it is compressive, and then it reverses, becoming tensile as the flange thickness is reduced and flow into the hub occurs. Meanwhile, the strains at the center are increasing monotonically as deformation progresses. This is illustrated in Fig. 43 by the steps 0-1-2-3. As deformation proceeds, the strains at the center increase; but the hydrostatic pressure is also increasing, so the fracture line moves upward. Then, as the flange thickness approaches one-half of the hub base diameter (die orifice diameter), the hydrostatic stress becomes tensile, so the fracture line decreases in height. The strains at the center continue to rise, however, and cross the fracture line, leading to the central burst. The calculated hydrostatic tension at fracture was $0.3Y$. This approach could be used for predicting central burst in drawing and extrusion to provide a material-dependent criterion, as opposed to the more simplistic upper bound and tensile stress criteria described previously.

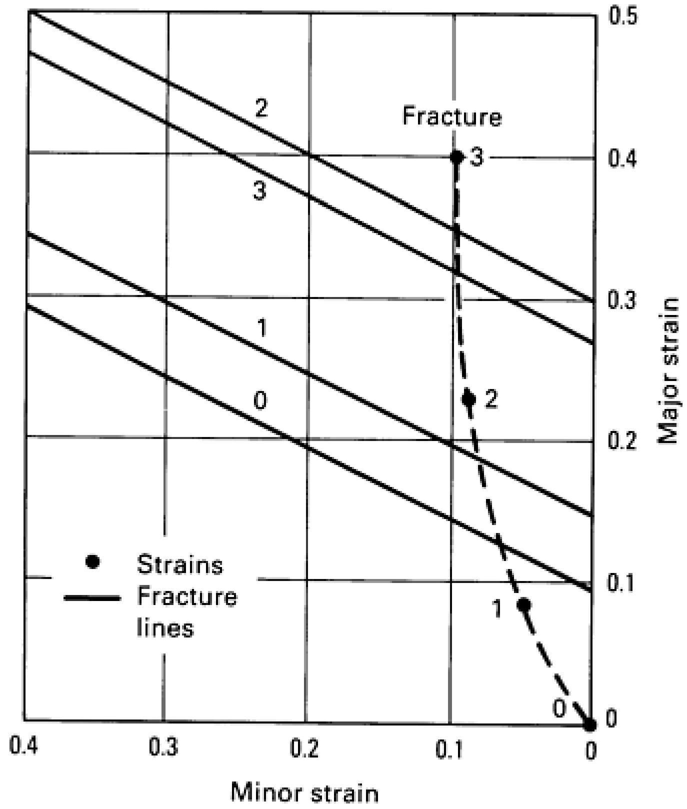

Fig. 43 Progression of surface strains and fracture line at the central internal location of the double-extrusion forging shown in Fig. 42. The fracture line rises from 0 to 1 to 2 as internal pressure increases and then falls to point 3 as the internal stress becomes tensile.

Die Contact Surface Fracture. Frequently, cracks occur during forging on surfaces that are in contact with the dies (Fig. 5). One common location of such defects is the vicinity of a die or punch corner. From the observation of a variety of such defects, it appears that a common characteristic is an abrupt change in frictional shear traction distribution in the region of the crack. High friction to retard metal flow in advance of the crack location is one method for preventing such defects.

A technique for studying die contact surface cracks was developed by means of a disk compression test and dies having a rough surface in the central region and a smooth surface in the outer region. Figure 44 shows the top view of a 6061 aluminum alloy disk compressed between such dies.In the transition region between the rough central die surface and the smooth outer region, radial cracks initiate and propagate outward. Such cracks occurred at approximately 30% reduction when the smooth outer region was lubricated with Teflon.The cracks occurred at approximately 45% reduction when grease lubrication was used in the outer smooth region. No cracks occurred even for very large reductions when the smooth outer region was not lubricated.

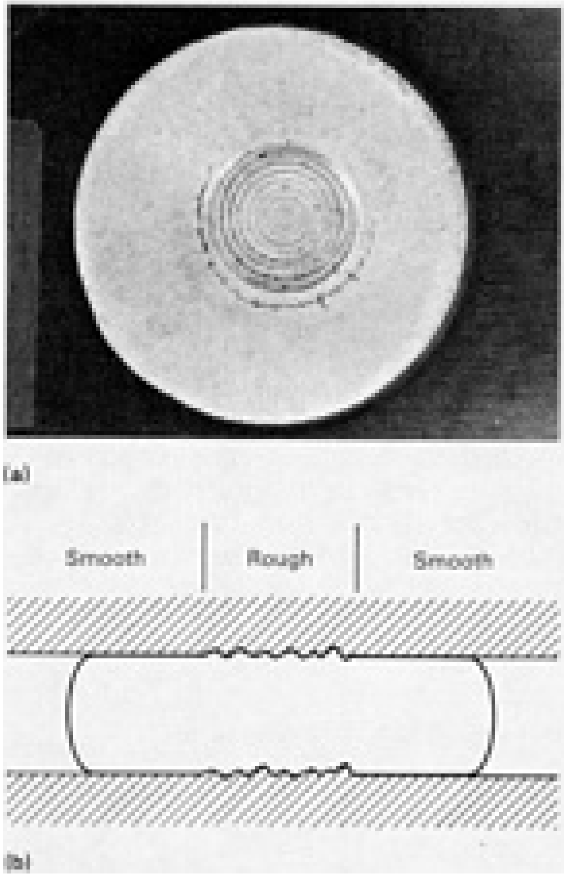

Fig. 44 Top view of aluminum alloy 6061 disk (a) compressed between dies (b). Cracks form at the transition region between rough and smooth areas of the die.

Grid marks placed on the die contact surface of the disks were used to measure the distribution of surface strains in the radial direction. Figure 45 gives an example of such measurements. In the rough central region, the strains are zero, while in the smooth outer region the strains are equal and constant. In the transition, however, the circumferential strain, θ, jumps abruptly from zero to its constant value in the outer region, and the radial strain, $_r$, overshoots to a very high value before returning to its constant value in the smooth outer region.

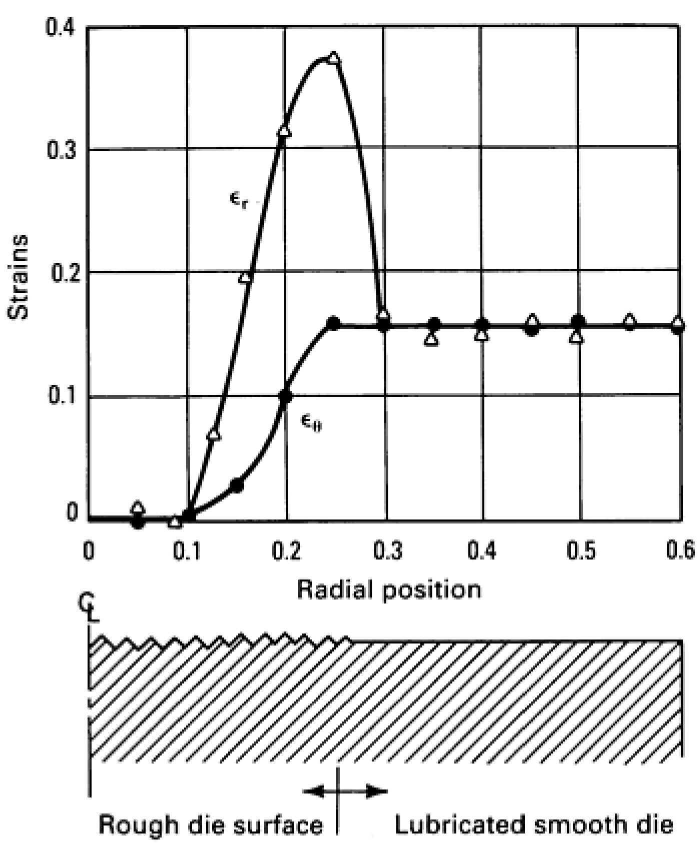

Fig. 45 Radial variation of contact surface strains after 30% compression of the disk shown in Fig. 44.

The strains shown in Fig. 45 were the same regardless of the friction condition in the smooth outer region. Therefore, fractures in the transition region occur because of the combination of large tensile surface strains and low hydrostatic stress state.This explains the occurrence of cracks at low reduction when Teflon is used, and no occurrence of fracture when no lubricant is used. The Teflon, having a near-zero friction coefficient, results in very low radial back pressure on the transition region, while grease and no lubricant provide progressively larger back pressures.

By means of visioplasticity analysis, the stresses were determined at the contact surface in the vicinity of the transition region. The resulting normal die pressure plus the surface radial and circumferential strains define the stress and strain states in the transition region and can be illustrated on a forming limit diagram. Figure 46 shows the change in surface strains and the increase in the fracture line due to increasing normal pressure during compression of a disk with grease lubricant in the outer region (indicated by the increments of reduction to 45%). The fracture line increases at a slower rate than the strains increase with increasing pressure, and at 45% reduction the strain path exceeds the fracture line and cracks are observed. For Teflon lubricant, the crossover occurs at about 30% reduction, while in the case of no lubricant the fracture line moves progressively away from the strain path.

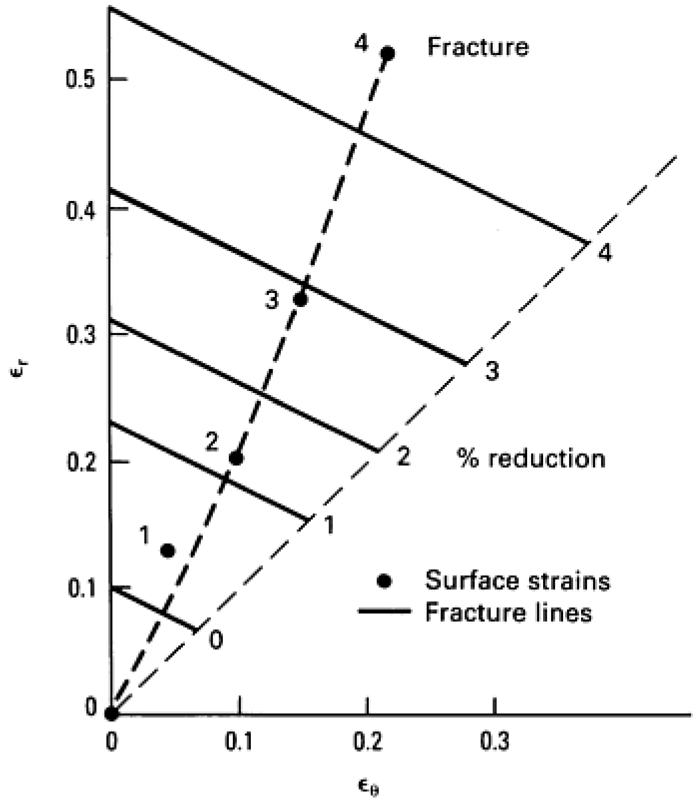

Fig. 46 Progression of surface strains and fracture line at the transition region between rough and smooth zones of the compressed disk shown in Fig. 45. Points 1, 2, 3, and 4 represent 10, 20, 30, and 45% reduction, respectively.

Example 4: Fir Tree Defect.

The criterion for contact surface fracture can be applied qualitatively for interpretation of the fir tree defect in extrusion. Such defects occur on the surfaces of extruded bars as well as in localized areas of forgings containing ribs.

In a section of a rib-web forging from a preform of sintered aluminum alloy powder, small cracks formed with a regular spacing on the rib surfaces (Fig. 47). Such defects occurred only when the thickness of the rib was greater than approximately one-half the web length (that is, extrusion reduction less than one-half). Each crack formed by shear at the corner as material flowed from the web into the vertical rib.

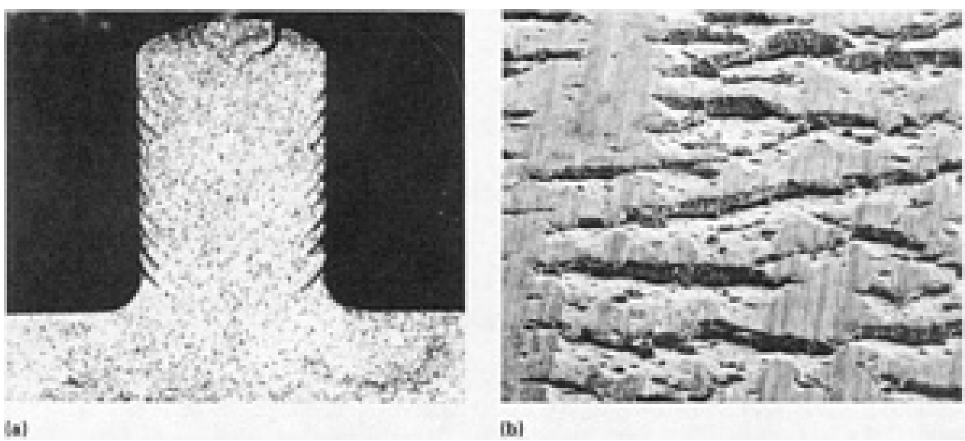

Fig. 47 Die contact surface cracking during forging extrusion of aluminum alloy powder compact. (a) Cross section. (b) Normal to vertical rib surface. Note also the cracks at the top free surface. Stress and strain states

are defined in Fig. 4(b).

The stress and strain state on a surface material element is shown in Fig. 48. As material in the web is compressed, the surface element experiences tensile strain in the direction of flow, and the strain increases as the element approaches the die corner. At the same time, there is a compressive stress from the die onto the surface element. This pressure diminishes, however, as the element nears the die corner and almost disappears as the element moves around the corner.

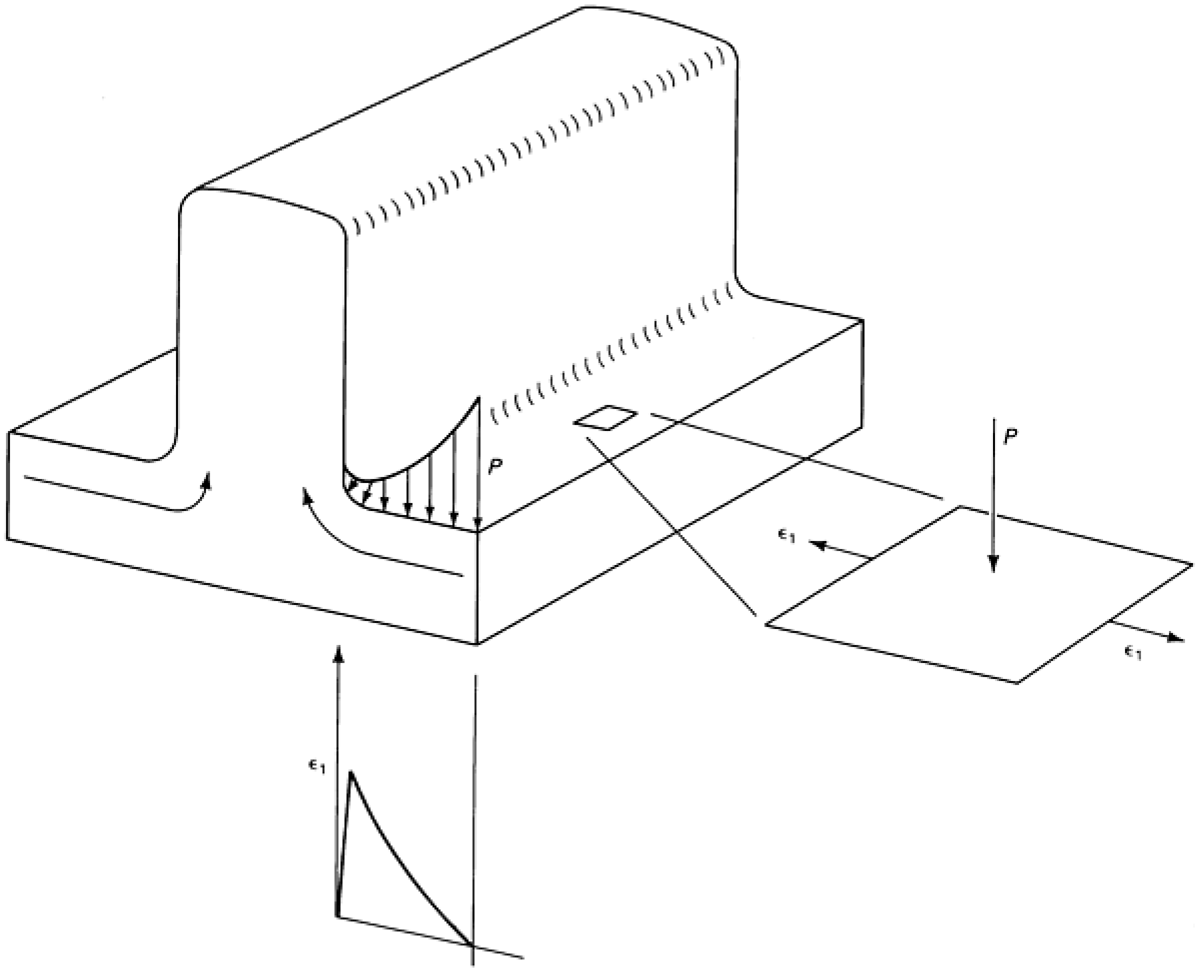

Fig. 48 Increase in strain $_1$ and decrease in die contact pressure *P* on a surface element as it moves from the web into the rib section of the extrusion forging shown in Fig. 47. Stress and strain states are defined in Fig. 5(a).

This can be illustrated schematically on the fracture strain diagram shown in Fig. 49. Because the deformation is in a state of plane strain, the strain path is represented as a vector of increasing length along the vertical axis. Meanwhile, the fracture line decreases in height because the pressure acting normal to the element is progressively decreasing. When the strains cross the fracture line, fracture occurs.

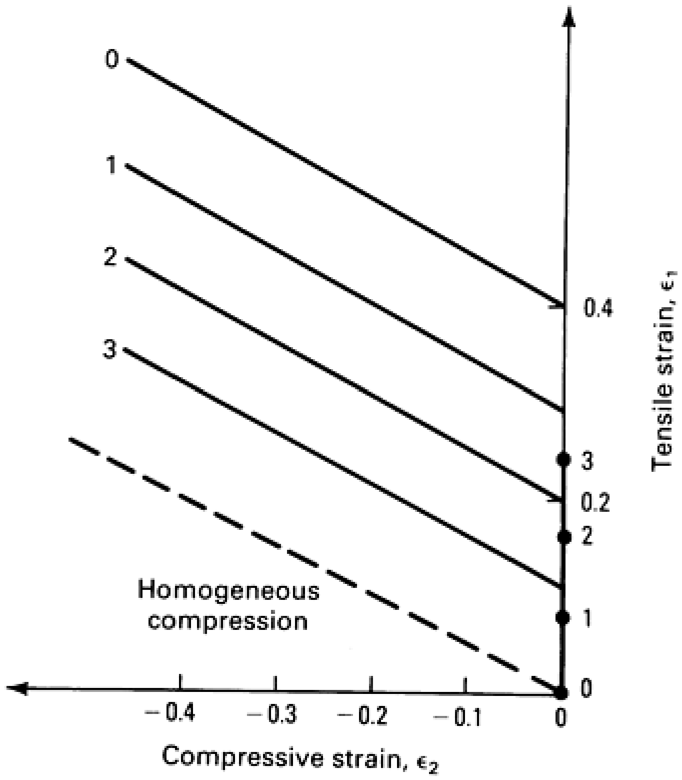

Fig. 49 Progression of strain $_1$ and decline of fracture line due to decrease in pressure *P* for an element moving from the web to rib section during the forging extrusion shown in Fig. 48. At stage 3, the strain exceeds the fracture line.

This phenomenon does not take place when extruding ribs of small thickness, because the extrusion reduction, and therefore the pressure, is larger, which maintains the fracture line at a high level. For thick ribs, two solutions were considered. One approach is to increase friction along the rib walls by roughening the die surface or avoiding lubrication of the die rib. This produces greater back pressure at the die corner, elevating the fracture line and preventing cracking. Such an approach is difficult to implement and can be used only with segmented dies because the formed rib cannot be removed from the die. The second approach is to use a draft angle on the rib, which has the same effect as increased friction. An angle of 10° prevented fracture in the current case, but other alloys may require a smaller or larger angle. A quantitative analysis combining the pressure effect on the fracture line and plasticity analysis would provide a method of predicting he draft angle in order to prevent fracture.

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Introduction to Process Design for Bulk Forming

H.L. Gegel and J.C. Malas, Air Force Wright Aeronautical Laboratories/Materials Laboratory; S.M. Doraivelu and V.A. Shende, Universal Energy Systems, Knowledge Integration Center

Introduction

In designing material-working processes for components made of complex engineering materials, the most important task is the selection of the controlling process parameters that will ensure part quality as well as specific mechanical and physical characteristics. The controlling process parameters are the sequence and number of material flow operations, the heat-treating conditions, and the associated quality assurance tests. When designing forging processes, special features such as nonlinear irreversible finite-deformation flow must be considered. Simultaneously, the complex interdependence of forging process parameters, as shown in Fig. 1, and their effect on the quality of the finished part, the reliability, and the ability to inspect must be considered.

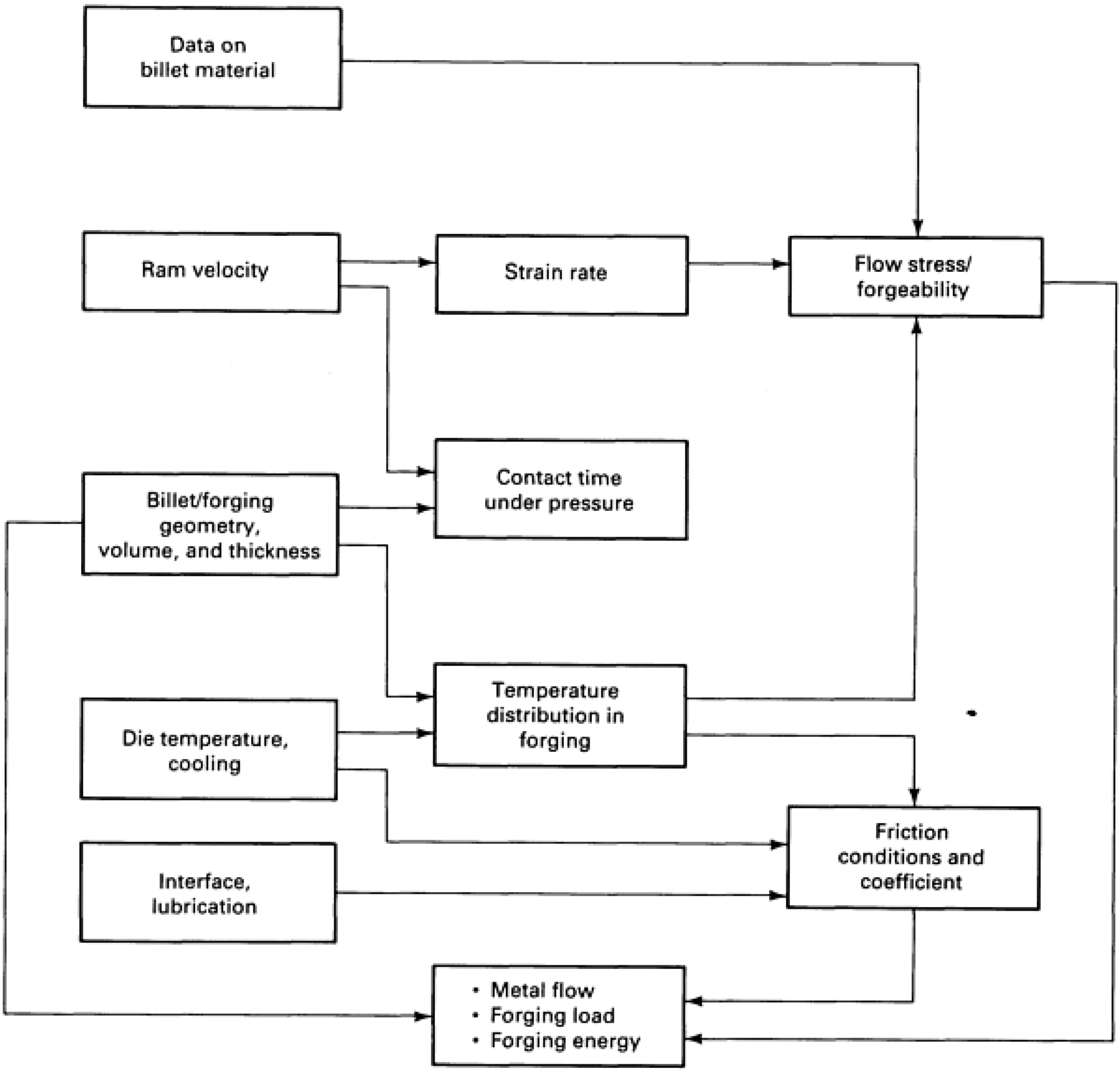

Fig. 1 Interdependence of forging process parameters.

Another important goal in forging is to determine the optimum means for producing defect-free parts on a repeatable basis. The optimization criteria depend on the manufacturing goals and the product specifications; establishing the appropriate criteria requires in-depth views--both global and local--of manufacturing processes and material behavior. From an optimization viewpoint, manufacturing processes require the determination of material flow mechanics to achieve proper process design and to develop a rational strategy for process control.

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The authors wish to express their appreciation for the helpful suggestions and comments provided by the following individuals who reviewed the articles in this Section: S. Gopinath, Knowledge Integration Center, Universal Energy Systems; V.K. Jain, Department of Mechanical Engineering, University of Dayton; J.S. Gunasekera, Department of Mechanical Engineering, Ohio University; T.E. Howson, Wyman-Gordon Company; T. Furman, A. Hayes, and C. White, Ladish Company, Inc.; E. Raymond, J. Walters, and R. Wallies, Cameron Iron Works, Inc.; S.C. Jain and S.K. Srivatsa, General Electric Company. Special acknowledgment and sincere thanks are also due to L.L. Jenney and D. Ashok, Knowledge Integration Center, Universal Energy Systems, for their assistance in preparing this document.

Introduction to Process Design for Bulk Forming

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Modeling

Modeling of the forging process involves both mechanics and thermodynamics. The process, as a rule, is inhomogeneous and transient over a large volume of workpiece material, and the material flow process can be characterized as highly irreversible and stochastic in nature. The mechanics of the forging process are well established, and different analytical tools are available for analyzing most of the important steps of a total forging process. Three articles in this Volume illustrate how the various analytical tools and material behavior models can be used in cooperation to design complex forging processes. The first article, "Forging Process Design," summarizes:

- The controllable factors in the forging process
- The various tasks performed in the manufacture of forgings and the software available to support task automation
- The required data base of information required to support the software
- The methodology of forging process design

The next article, "Modeling Techniques Used in Forging Process Design," reviews:

- The concepts involved in material modeling and the approaches used to understand the fundamentals of flow, fracture, and workability
- The methods available for the analytical modeling of the forging process and the application of these methods for various forging operations
- Physical modeling through simulation using model materials (for example, Plasticine, wax, and polymers) or through laboratory tests

Finally, the third article, "Acquisition of Data for Forging Process Design," discusses the use of compression testing for:

- Generating flow curves
- Determining workability parameters
- Developing and interpreting processing maps

- Obtaining interfacial frictional data

In addition to the articles mentioned above in this Section, supplementary information on computer-aided forging process design can be found in the articles "Open-Die Forging," "Closed-Die Forging," and "Introduction to Workability" in this Volume.

Forging Process Design

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Introduction

THE TRADITIONAL BUILD-AND-TEST METHODS of developing a complete manufacturing process were heavily experience based, because the analytical approach to process design was inadequately developed. The part geometries are generally quite complex, and the simpler analytical methods (as opposed to the computer-aided numerical methods) could not handle the task of providing accurate solutions to engineering problems. The advent of numerical methods such as general-purpose finite-element method (FEM) codes to handle large deformation plasticity has made accurate analysis possible, but these techniques still have several limitations. FEM codes such as ALPID (Ref 1, 2) (developed under programs funded by the U.S. Air Force) are analysis tools, not design tools, and are quite expensive to use in terms of required computer time and facilities. Because the potentially viable processing routes are numerous, many FEM process simulations would be necessary to identify optimal processing methods unless some means of restricting the solution space are implemented.

The problem of large design solution spaces is not restricted to the design of the deformation process, but also applies to the selection of correct combinations of processing temperatures and strain rates and to the identification of optimal heat treatment techniques.

In the design and manufacture of forging dies, two methodologies can be used. In the first method, the design of the die is obtained by making use of a computer-aided engineering (CAE) systems approach. This approach integrates dynamic materials behavior modeling, geometric modeling, and analytical process modeling (Ref 3). Advanced process modeling is the heart of this approach, and the understanding of dynamic materials behavior in a quantitative way for all materials--composites, metals, alloys, polymers, and ceramics--makes the integration possible and generic.

In the second method, well-established empirical rules are used to arrive at an acceptable design alternative. Currently, both of these procedures follow a trial-and-error approach to achieve the required goal. Either one used by itself is a time-consuming and costly process. The CAE procedure lacks empirical aids during the selection of an initial design, and the experience-based method lacks analytical aids for verification of the final design. The optimum die design procedure would be to use both of these methods.

An initial design can be selected on the basis of empirical rules; such a design can be further refined by computation involving analysis and simulation as followed in the CAE approach. This procedure is complex because it involves two fundamental types of activities, information handling and problem solving. This can be made easier if an intelligent system that makes use of the techniques is available from software engineering (SE), data base management systems (DBMS), operating systems (OS), analytical modeling (AM), or artificial intelligence (AI).

To summarize, automation of engineering methodology in forging design and manufacture should address the following basic issues:

- Engineering disciplines of significant diversity are involved and need to be integrated in a uniform fashion
- The potential design solution space is large and needs to be intelligently reduced so that the design-analysis iterations remain practical

The global approach to automation is conceptually straightforward, and is suitable for incremental implementation as newer software tools become available. The automation of the initial-guess design is done through the use of knowledge-based expert systems (KBES); when possible, a rough analysis (for example, using slab analysis for plasticity) is provided as a pre-screen to detail analysis (usually FEM), and the detail analysis is used primarily as a verification tool. It is useful at this point to note that the solution space is reducible through two means:

- The use of KBES rough analysis-detail analysis iterations can provide a local reduction of admissible solutions, that is, in a specific domain such as plasticity, heat transfer, or material engineering
- The use of a knowledge-based integration shell provides a more global reduction of the solution space by implementing a methodology that controls design procedures across interdisciplinary domains; this approach embeds procedural knowledge in the integration shell, and also provides a uniform user interface

This article provides a list of controllable factors, various tasks performed in the manufacture of forgings, software tools available to support task automation, and data bases of information required to support software tools. Forging process design tasks are then outlined to illustrate how the various tools and data are used in the method.

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Forging Process Design

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Controllable Factors In Forging Process Development

As a first step towards identifying the procedures, it is useful to define the features of the product or process that are controllable, and by whom. Table 1 shows applicable product specification, process design, and die design related factors. This basic inspection can then be used to guide the development of the task hierarchy, or method.

Table 1 Applicable product specification, process design, and die design related factors

Controllable factor	**Controlled by**
Product specification related factors	
Final component geometry	Customer/end-user
Sonic outline	Customer/end-user

Alloy composition	Primary melter
Alloy cleanliness	Primary melter
Mechanical properties	Heat treater/forging metallurgist
Final microstructure	Heat treater/forging metallurgist
Final finishing (for example, chem-milled)	End-user/forger
Forging identification	Forger
Process design related factors	
Heat treatment	End-user/forging metallurgist
Forging sequence design	Forging designer/metallurgist
Preform design	Forging designer
Stock allowances	Manufacturing/forging designer
Billet size requirements	Forging designer
Forging temperature	Forging metallurgist
Forging die temperature	Forging metallurgist
Lubrication practice	Forger
Forging strain rate	Forging metallurgist
Load/ram velocity requirements	Forging designer/FEM analyst
Press selection	Manufacturing engineer
Die design related factors	
Forging dimensional accuracy	Die designer/FEM analyst
Defect-free forgings (no laps, and so forth)	Die designer/FEM analyst

Die life	Die designer
Die strength	Die designer

Forging Process Design

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Tasks Performed in Forging Manufacture

If the overall procedure in the production of forgings in a typical company were examined, the following major tasks would be found to be necessary components of the manufacturing operation:

- Receive and interpret request for quotation (RFQ)
- Input part geometry into computer-aided design (CAD) system
- Identify constraints imposed by specifications in RFQ
- Identify billet stock sources
- Check incoming billet stock for quality
- Prepare billet for heating/forging
- Determine forging sequence (blocking, preforming, and so forth)
- Design dies
- Select forging temperatures and strain-rates
- Choose forging technique (hot-die, isothermal, and so forth)
- Select forging equipment
- Manufacture dies
- Ascertain furnace cycles and environments (atmosphere control, and so forth)
- Determine lubricants required
- Select intermediate operations such as trimming of preforms
- Inspect finish forging
- Use identification method (serial numbers, and so forth) to track workpieces, drawings, dies, and so on
- Determine heat treating and finishing operations
- Test finish forging to see it meets customer specifications
- Verify packing and shipping requirements

Most forge houses have standard in-house process specification sheets, and "travelers," or specification sheets that actually go with the individual forging (or batch of forgings) to ensure that proper practice is followed.

Forging Process Design

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Software Tools Available to Support Task Automation

Five main classes of tools are necessary for the implementation of an integrated system for forging process design:

- *Geometry Representation Tools.* A significant portion of the design and analysis task is manipulation and definition of geometry
- *Knowledge-based expert systems (KBES)* provide experience-based initial guesses to design problems. These initial guesses can then be iterated upon by rough and detailed analysis tools
- *Rough analysis tools* are fast and cost effective: They prescreen initial guesses provided by the KBES so that inappropriate designs are not passed to more expensive detail analysis tools
- *High-accuracy detail analysis tools* serve mainly to validate selected designs
- *An intelligent DBMS* stores all data used by the aforementioned tools. Access to this data, whether it be by the user or by one of the aforementioned tools, should be guided and controlled; the forging knowledge-based DBMS must control the sequence of creation and use of the data by users and tools according to the dictates of the methodology

These tools are further examined below, with a brief discussion of their functionality, input requirements, and outputs. The input requirements and outputs do not represent a description of the "file formats," but rather of the conceptual use of the tools. Further, input data that would be data based is not noted here, but is described later in the section "Required Data Base to Support Software Tools." (Data base information is data that is insensitive to a specific forging operation; for example, materials flow stress, thermophysical properties, and so forth.)

Geometry Representation Tools

CAD Systems. It is necessary to provide geometry information to the simulation and expert systems programs in an appropriate fashion. Much of the U.S. manufacturing industry has already started to use computer-aided drafting systems, and it would be ideal to use this computerized geometry representation for forging process design.

Computer-aided design commercialization is fairly mature, and options range from low-cost PC-based systems to full-function mainframe software. The choice is mostly a matter of performance versus cost.

- *Input requirement:* Interactive geometry creation, or geometry data files (IGES, that is Initial Graphics Exchange Specification, or proprietary) (Ref 4)
- *Output:* Engineering drawings, cross sections for analysis, section geometric properties, solid model geometric properties, numerical control (NC) machining "tapes"

Callable Graphics Libraries. The above-mentioned CAD software is generally highly proprietary and, as such, is not readily useable in applications software. Furthermore, CAD systems offer a capability exceeding the requirements of graphics application codes. It is therefore necessary to use callable graphics libraries that can be integrated into graphics application routines. Several such graphics libraries are commercially available.

- *Input:* These are not used directly by the end-user, rather, they are programming tools for the software applications developer
- *Output:* Graphics output to and input from the user

Knowledge-Based Expert Systems

Automated forging design (AFD) is a knowledge-based system implementing rules for establishing stock allowances to a finished part for the various manufacturability criteria, such as forgeability, machining, die mismatch, and so forth. In addition, it establishes a suitable parting line. This software is being developed under a U.S. Air Force Manufacturing Science program (Ref 5).

The program can currently handle two-dimensional geometries; thus, for general three-dimensional components, the designer has to determine which set of two-dimensional sections should be provided to the AFD expert system.

- *Input requirements:* Material specification, two-dimensional geometry of the required part
- *Output:* Two-dimensional geometry of the required part with a forging envelope applied as well as a

parting line for final forging operation

Blocker Initial Design (BID). This program (Ref 6) adds allowances to the finish forging according to experience guidelines that generate a preform geometry for the finish forging. The BID program is also being developed under the U.S. Air Force Manufacturing Science program.

BID could be used iteratively to work back all the way to a starting billet requirement. The program can currently handle two-dimensional geometries; thus, for general three-dimensional components, the designer has to determine which set of two-dimensional sections should be provided to the AFD expert system.

It is worth noting that the use of a two-dimensional section is quite appropriate, because this is in fact the way experts currently handle design. It would be advantageous to attempt to identify the two-dimensional sections that should be given to the KBES.

- *Input requirements:* Geometry at the end of a forging step, and material specification
- *Output:* Preform geometry for the forging step

Material Modeling Environment (MME). Given the nominal strain to be developed in a deformation step, MME (Ref 7) provides a material processing map that identifies ranges of the stable temperature and strain rate required to forge the part. Further, based on microstructural observation data, a desired range for generating the required microstructure can be superimposed on the stability map. This allows determination of press velocities and forging temperatures.

- *Input requirements:* Flow stress data as a function of strain, strain rate, and temperature; microstructural observations
- *Output:* Multiple graphic views of the data set, stability and structure evolution maps, and constitutive representation of the material response suitable for ALPID (Analysis of Large Plastic Incremental Deformation) simulations

Rough Analysis Tools

FINISHR. This FORTRAN program (Ref 5) is intended to give approximate load distributions on the forging dies using the slab method. Again, this program operates on two-dimensional sections; however, this is consistent with the functionality of the KBES.

- *Input requirements:* Geometry at the end of forging step, and material specification
- *Output:* Estimated load distribution on the dies

Detail Analysis Tools

ALPID System. This system of programs includes two-dimensional rigid-visco-plastic FEM analysis programs, including a coupled heat-transfer capability. The system also includes a remeshing-interpolation program to permit continued simulation after the initial mesh becomes too distorted. There is a postprocessor included in the system. More details about ALPID are given in the section "Required Data Base to Support Software Tools" in this article. The ALPID program has been actively used in various research programs as well as in industry, and is considered reliable and mature.

- *Input requirements:* Geometry of dies and workpiece, FEM mesh, boundary conditions, material constitutive "equation," thermal/physical properties of materials
- *Output:* Geometric description, in textual or graphic representation, of the progress of deformation in terms of mesh distortion, detailed distributions of strain, strain rate, velocity, stress, and temperature

TOPAZ. This is a FEM heat transfer analysis program developed at Lawrence Livermore Laboratories. TOPAZ (Ref 8) provides complete capabilities, including convection, conduction, and radiation. An associated program, FACET (a subprogram of TOPAZ), can provide view factor calculation for the radiation.

- *Input requirements:* Geometric description of part, FEM mesh, thermophysical properties of forging, boundary conditions, heat transfer coefficients, and view factors for radiation
- *Output:* Transient temperature field in the forging, as a stand-alone result or as input to NIKE for analyzing the thermally induced stresses

NIKE. This FEM stress analysis program (Ref 9) (also developed at Lawrence Livermore Laboratories) can provide several material models, that is, elastic, elastic-plastic, and so forth. For one of the intended applications in the integrated forging methodology, the elastic-plastic capability is of interest, to simulate heat treatment induced stresses in the forged part. It is also possible to use NIKE for detailed stress analysis of the dies, although other commercial packages are popularly used.

It is important to note that NIKE and TOPAZ are coupled to provide thermal-stress analysis. At present, remeshing is not provided; however, if significant deflections do occur, the heat treatment is not viable anyway, so the simulations are quite appropriately terminated at this point.

- *Input requirements:* Geometric description of the part, FEM mesh, boundary conditions, and thermomechanical properties
- *Output:* Stress and deflection field in the forging/die

NIKE and TOPAZ are provided with their own mesh generator and postprocessor, and if integration and uniform user interface are required, some code development will be necessary.

Knowledge-Based Intelligent DBMS

KI SHELL. This is a knowledge-based integration shell (Ref 10) for providing a frame-based methodology. The shell is built on top of a relational data base. This shell has been under development for 10 years at The Ohio State University, and is now being used commercially.

Relational Data Base. The KI SHELL is mapped onto a relational data base.

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Required Data Base to Support Software Tools

Workpiece and Die Thermophysical Properties. The data for performing heat treatment simulations on the finish forging include specific heat, density, thermal conductivity and elastic modulus of the forging material, all as a function of temperature. The thermal properties of the die material are necessary to conduct the deformation simulation for nonisothermal forging. The elastic modulus of the die is necessary for the stress analysis of the dies to verify the capability to withstand the loading.

Workpiece Constitutive Response. The flow stress of the workpiece, or billet material, as a function of strain, strain rate, and temperature is required for dynamic material modeling (using MME), as well as for deformation simulation (using FINISHR and ALPID). The constitutive response is also important if plastic deflections are to be predicted in heat treatment simulations (NIKE and TOPAZ).

Process Window for Microstructure Control in the Forging. The dynamic material modeling approach implemented by the material modeling environment (MME) package provides direct guidance for selecting stable processing conditions when taking defects into consideration. It is still necessary to further restrict the viable processing range in terms of microstructure control. Thus data on processing-microstructure correlations is necessary to ensure that forging is done at a temperature and strain rate that not only gives stable flow but also develops the desired microstructure.

Die-Workpiece Interface Data. The thermomechanical deformation simulation (ALPID) requires data on the effective friction factor and the heat transfer coefficient at the interface. In general, these data vary with the lubricant used during forging and the force applied on the interface.

Tooling Data Base. A typical tool setup involves back-up die-blocks behind the forging die; further, these back-up dies are frequently used in conjunction with multiple sets of contour dies. It is useful to data base this inventory, in order to help determine whether new back-up tools will be necessary. In general, the contour dies are useful for only a specific part, and the need to data base them is uncertain.

Press/Hammer Data. In order to determine the appropriate forging equipment for a particular forging operation, information on the press/hammer capacity is needed. Equipment data of relevance for presses is the velocity load capability and the velocity stroke capability. For hammers, the energy availability and stroke frequency are important. This data can be compared with the requirements computed by the deformation analysis tools, to support equipment selection.

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Forging Process Design Method

Having outlined the conceptual method of automating forging design and manufacturing, it is appropriate to examine the detailed, domain-specific aspects of the methodology. In the previous sections, the tasks that must be performed and the available tools to support these tasks were examined. The proper sequencing of these tasks, propagation of information between tasks, and the correct data basing of the results to prevent redundancy while maintaining a uniform user interface are the objectives of knowledge-based integration. Providing the detailed description of sequence, information propagation, and so forth, in a specific application (in this case, forging manufacture) is referred to as method design.

Forging Process Design Task Outline. An overview of the tasks that must be performed in developing a forging manufacturing plan is presented in Fig. 1. This representation shows inputs, redesign when analysis invalidates a design, and data base information required by a process designer or planner. This section provides a conceptual description of the tasks presented in the representation in Fig. 1.

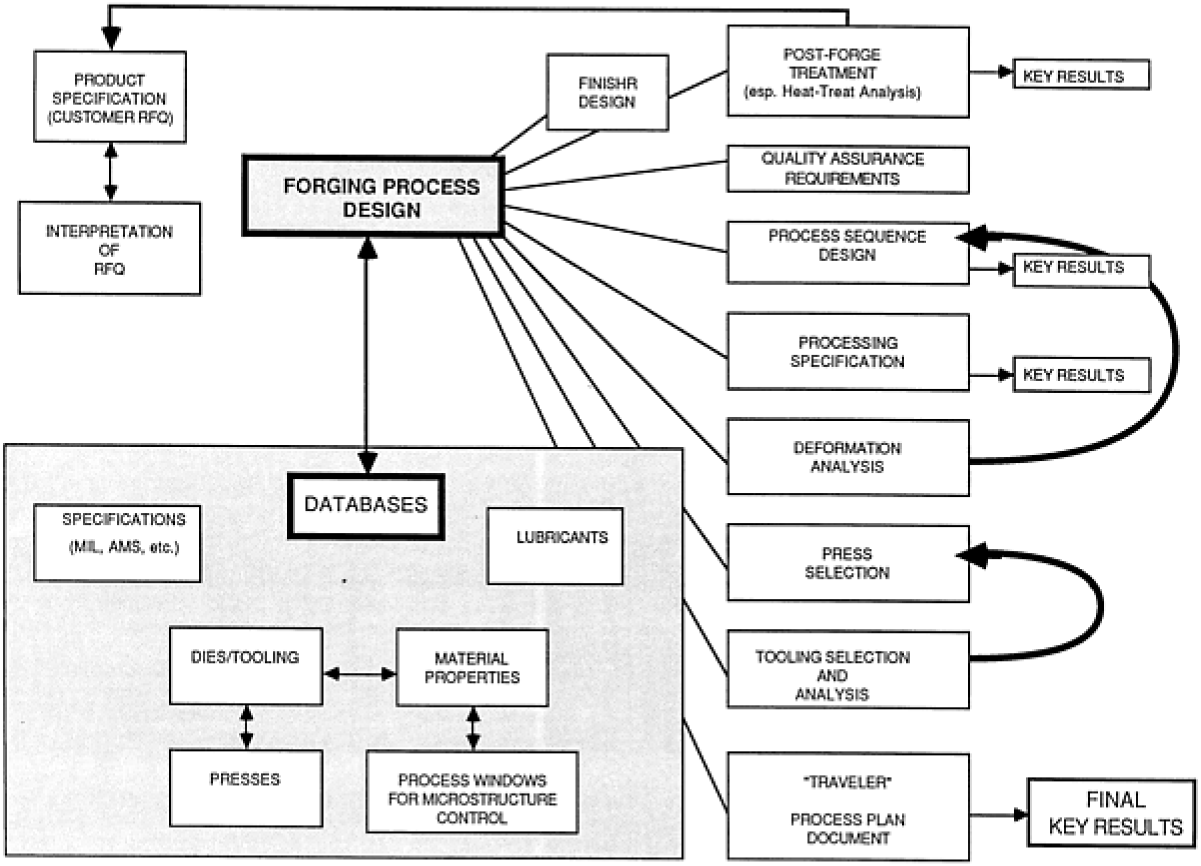

Fig. 1 Forging process design task overview.

Task 1: Integration of Geometry Representation. As is the case with most process design, forging design is conducted backwards, starting with the finished component requirement. The finished part representation is usually in the form of a computer-aided drafting geometry data base, plus the structure/property specifications, which are typically not data based at this point. The approach, therefore, will be to leave the integration of structure and property data to future work.

Computer-aided drafting technology is already in the marketplace as a competitive business, and such tight coupling with the data base would require access to proprietary data base structures. It has been proposed that integration be accomplished through standardized geometry files by means of standards such as the IGES.

Task 2: Initial-Guess Finished Forging Geometry. The identification of an initial-guess finished forging must take into account criteria such as the forging material, recommended process, available equipment, and so forth. In addition, if the property and structure specifications require the finished forging to be heat treated, the selected design should be capable of developing the required properties with available heat treatment practices without resulting in either cracking or distortion.

At present, a KBES prototype is available to provide an initial guess for a finished forging AFD along with the rough analysis tool (FINISHR) to estimate producibility of the finished forging. (Both the codes were developed under U.S. Air

Force funded programs.) There is, however, no such KBES rough-analysis support to evaluate the heat treatability of the finish forging, so it will be necessary to go directly to detail analysis. NIKE and TOPAZ can be used for evaluation of heat treatability. NIKE, a stress analysis code coupled with TOPAZ, a heat transfer code, can be used to provide thermal transients and associated stresses and deflections, if any, to determine heat treatability.

It is already apparent at this point that a large amount of data is necessary to support each aspect of design and analysis. This need to access significant amounts of diverse data in a context-sensitive fashion is a circumstance ideally suited to the capabilities of knowledge-based integration, which makes all data readily accessible to the user.

Task 3: Forging Sequence Design. After the finished forging has been adequately defined, it is necessary to design a sequence of forging steps, tracing backwards to an acceptable starting billet geometry (usually a cylinder or a cuboid).

In this task, the solution space can again be reduced through another KBES prototype, BID. This expert system starts with the finish forged geometry (or the intermediate step geometry) and applies criteria sensitive to the material, equipment, and geometry to provide an input geometry to that forging step.

Task 4: Processing Parameter Selection. The dynamic material modeling approach implemented in the MME product provides a fundamental methodology for selecting temperature and strain-rate at which stable deformation can be expected.

In addition to stable flow, data base information on processing-microstructure correlations will be used to identify the process window within which both stable metal flow and appropriate evolution of microstructure are achieved.

Task 5: Detail Analysis of the Forging Sequence. The initial-guess design and rough analysis constitute the automatic die and process design. In order to avoid costly die tryouts, the proposed sequence for manufacturing the forging can be verified using the ALPID system for FEM analysis. This system of programs, developed under U.S. Air Force sponsorship, is capable of analyzing any two-dimensional metal forming operation, including temperature-transient effects.

The ALPID system is integrated into the knowledge-based system by means of an intelligent preprocessor, to which relevant design information can be propagated. The preprocessor then generates the necessary inputs, including an appropriate constitutive "equation" representation, to the ALPID program.

It should be noted that unlike the design tasks, which work backwards from the final requirement, the analysis task progresses in the forward direction. This reversal of methodology is done primarily because, in general, initial conditions are not known for intermediate steps in a nonisothermal forging sequence without reheats.

Task 6: Die Design and Stress Analysis. The detail analysis of the deformation sequence, using ALPID, also provides the detailed loading distribution on the die surface. At present, there is no KBES for recommending an appropriate die block size, and designer judgment must be relied on. However, the KI SHELL can provide support in terms of propagating the forging design geometry to any appropriate stress analysis program.

Task 7: Generate Key Results. The culmination of process design and analysis is the generation of an appropriate manufacturing plan. In the forging industry, especially for critical applications, the manufacturing specification (plan) actually accompanies the forging. These "travelers" should be generated by an integrated system. In addition, because the design and analysis tasks may extend over several terminal sessions, intermediate status should be made available as "key results" (see Fig. 2 and 3). An actual traveler is an extended and complex document, but a schematic representation is provided in Fig. 4.

PROCESS SEQUENCE DESIGN RESULTS

FINISH FORGING:

Volume: ____________
Weight: ____________
Drawing #/ CAD file: ____________

BLOCKER:

Volume: ____________
Weight: ____________
Drawing #/ CAD file: ____________

BENDER:

Volume: ____________
Weight: ____________
Drawing #/ CAD file: ____________

FINAL PREFORM:

Number of PREFORMING Operations: ____________
Volume: ____________
Weight: ____________
Drawing #/ CAD file: ____________

(Repeat for as many preforms as required.)

BILLET:

Volume: ____________
Weight: ____________
Drawing #/ CAD file: ____________

Fig. 2 Example of key results; results of forging sequence design.

PROCESS PARAMETERS DESIGN RESULTS	
FINISH FORGING:	Strain-Rate: __________ Temperature: __________ Lubrication: __________ Load Estimate: __________
BLOCKER:	Strain-Rate: __________ Temperature: __________ Lubrication: __________ Load Estimate: __________
BENDER:	Strain-Rate: __________ Temperature: __________ Lubrication: __________ Load Estimate: __________
FINAL PREFORM:	Number of PREFORMING Operations: __________ Strain-Rate: __________ Temperature: __________ Lubrication: __________ Load Estimate: __________
(Repeat for as many preforms as required.)	

Fig. 3 Example of key results; results of forging parameter selection.

PROCESS AND INSPECTION TRAVELER

1 — BILLET MATERIAL SPECIFICATION

Alloy:
Specifications:
Melting Specs:
Material Source:
Heat #:
Conversion:
Surface Condition:

5 — POST-FORGE INSPECTION

Dimensional Check:
Defects:

2 — BILLET MATERIAL QUALITY ASSURANCE

(Mill Data or Acceptance Tests)

Composition:
Ultrasonic:
Property Data:
Macro-Etch:
Microstructure:

6 — HEAT-TREATMENT

Furnace Preheat:
Furnace Setpoint:
Time at Temperature:
Cooling from Furnace:

} Repeat these blocks as appropriate

3 — BILLET PREPARATION

Conversion:
Billet Shape:
Cut Mult Weight:
Conditioning:
Identification:
Coating:

7 — FINISHING OPERATIONS

Machine to Sonic Shape:
Cut Test Samples:

4 — FORGING PRACTICE

(this will be repeated for each step in forging sequence)

Preform Conditioning:
Lubricaion Practice:
Fritting:
Furnace:
Heating Practice:
Temperature:
Maximum Time:
Atmosphere Control:
Press:
Tooling Assembly ID:
Press Controls:
Velocity:
Stroke:
Load:
Identification (Serial #):
Trimming:
Hot Inspection:

8 — FINAL QUALITY ASSURANCE

Test Type:
Test Locations:
Identification:
Ultrasonic:
Mag Particles:
Die Penetrant:
Miicrostructure:
Macro-Slice:
Fracture Toughness (KIC):
Tensile:
Fatigue:
Creep-Rupture:

Fig. 4 Completed process specification (traveler).

The preceding task outline provides a framework of what an automated, knowledge-based system for forging process design should accomplish. The actual implementation of such a system requires a much more detailed specification of the methodology involved. A representation of a preliminary method description is shown in Fig. 5.

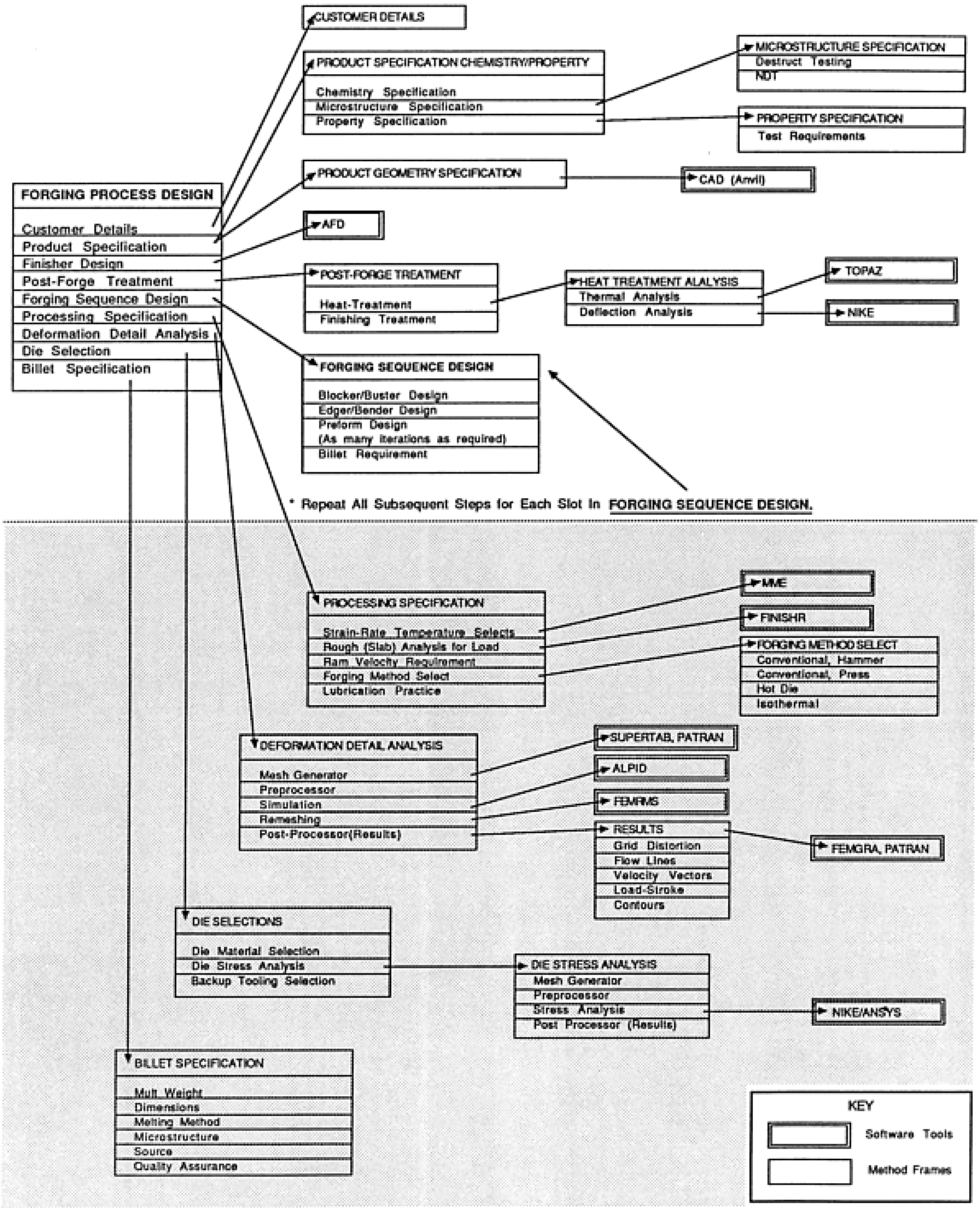

Fig. 5 Description of method for forging process planning and specifications.

Forging Process Design

H.L. Gegel and J.C. Malas, Air Force Wright Aeronautical Laboratories/Materials Laboratory; S.M. Doraivelu and V.A. Shende, Universal Energy Systems, Knowledge Integration Center

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Modeling Techniques Used in Forging Process Design

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Introduction

AN UNDERSTANDING of the flow behavior of the workpiece material under processing conditions is necessary in order to exploit the full potential of process modeling techniques. Mechanistic and dynamic material modeling approaches are being used to understand the fundamentals of flow, fracture, and workability. The former approach is based on activation energy analysis and is limited to processes that can be described by steady-state equations applied to pure, crystalline, and simple alloys. The latter approach is based on continuum and thermodynamic fundamentals and is used to understand the intrinsic workability of simple as well as complex alloys. Dynamic material modeling is required for obtaining realistic predictions of the total performance of any nonlinear deformation process and for reducing the cost of the design process. The basic concepts and procedures involved in each of these approaches will be discussed in this article.

Modeling Techniques Used in Forging Process Design

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Constitutive Equations for Material Modeling

Constitutive equations describe the nonlinear relationship that exists among such process variables as effective stress, effective strain rate, and temperature at different deformation levels. They are required for the development of dynamic material models and for the realistic modeling of various unit processes. They are unique for each material under each processing condition. Therefore, these equations are developed by using data, obtained under simplified experimental conditions, that can be extended to complex situations by means of well-known hypotheses or, for the case of plastic deformation, such criteria as the von Mises yield criterion. These equations affect the convergence and accuracy of finite-element modeling and must satisfy certain requirements, including the following:

- The constitutive equations must be continuous
- The surface generated by the constitutive equations must have the same characteristics as the surface generated by the function that describes the dissipation of power applied to a specimen under testing conditions
- The equations are to be generated to cover the range of processing conditions experienced during the production process
- The equations should be represented in a form compatible with a finite-element analysis program

Basic Concepts (Yield Criteria)

Fully Dense Materials. In general, the constitutive equations for material modeling are expressed as:

$$\bar{\sigma} = f(\dot{\bar{\epsilon}}, T, \sigma^*) \qquad \textbf{(Eq 1)}$$

where $\dot{\bar{\epsilon}}$ is effective strain rate, T is instantaneous temperature, and σ^* is a history-dependent variable defining the current state of the material. Each of these variables is discussed in more detail in this section.

According to the Tresca and the von Mises yield criteria, the flow stress $\bar{\sigma}$ of homogeneous and isotropic materials without porosity can be expressed by:

$$\bar{\sigma} = \sigma_{\mathrm{I}} - \sigma_{\mathrm{III}} \qquad \textbf{(Eq 2)}$$

$$\bar{\sigma} = \sqrt{\frac{1}{2}[(\sigma_{\mathrm{I}} - \sigma_{\mathrm{II}})^2 + (\sigma_{\mathrm{II}} + \sigma_{\mathrm{III}})^2 + (\sigma_{\mathrm{III}} - \sigma_{\mathrm{I}})^2]} \qquad \textbf{(Eq 3)}$$

where $\bar{\sigma}$ is the effective stress and σ_{I}, σ_{II}, and σ_{III} are the principal stresses.

In the case of a uniaxial stress state ($\sigma_{\mathrm{II}} = \sigma_{\mathrm{III}} = 0$), both criteria lead to the same result, namely:

$$\bar{\sigma} = \sigma_{\mathrm{I}} \qquad \textbf{(Eq 4)}$$

Therefore, in uniaxial loading, the stress $\bar{\sigma}$ applied externally on the cross-sectional area of the specimen is equal to the flow stress and is given by:

$$\bar{\sigma} = \sigma_{\mathrm{I}} = \frac{F}{A} \qquad \textbf{(Eq 5)}$$

where F is the instantaneous force and A is the instantaneous cross-sectional area. Hereafter, $\sigma = \bar{\sigma}$ = flow stress.

The true strain for compression $\bar{\epsilon}$ is defined as:

$$\bar{\epsilon} = \int_{h_0}^{h_f} \frac{dh}{h} = \ln \frac{h_f}{h_0} = -\ln \frac{h_0}{h_f} \qquad \textbf{(Eq 6)}$$

where h, h_0, and h_f are the instantaneous, initial, and final heights of the specimen, respectively. Hereafter, $\epsilon = \bar{\epsilon}$ = true strain.

The effective strain rate $\dot{\bar{\epsilon}}$ is defined as:

$$\dot{\bar{\epsilon}} = \frac{d\bar{\epsilon}}{dt} = \frac{dh/h}{dt} = \frac{1}{h}\frac{dh}{dt} = \frac{v}{h} \qquad \textbf{(Eq 7)}$$

where v is the instantaneous velocity of the cross head. Hereafter, $\dot{\epsilon} = \dot{\bar{\epsilon}}$ = effective strain rate.

If the strain rate does not remain constant during the test, the mean strain rate is considered for representing the flow stress values. This mean strain rate is defined as the average strain rate experienced during the deformation.

The instantaneous temperature T of the specimen during deformation is referred to as the adiabatic temperature. It is defined as $T = T_0 + \Delta T$, where T_0 is the forming temperature or the initial test temperature of the specimen and ΔT is the increase in temperature during forming.

The current state of the material, such as plastic hardness, is defined by the history-dependent variable σ^* (Ref 1). Experimental results are used to arrive at the exact functions.

Porous Materials. The design of billet consolidation processes requires a special yield function for development of the plasticity analysis. The plastic flow behavior of porous powder metallurgy (P/M) materials is more complicated than that of ingot materials because the hydrostatic component of stress influences the onset of plastic flow. The effect of hydrostatic stress is taken into account by considering a yield function of the form:

$$AJ'_2 + BJ_1^2 = Y_R^2 = \delta Y_0^2 \qquad \textbf{(Eq 8)}$$

where J_2 is the second invariant of the deviatoric stress; J_1 is the first invariant (that is, the hydrostatic component of stress); Y_0 and Y_R are the yield stresses of fully dense and partially dense materials, respectively; and A, B, and δ are functions of relative density.

Many researchers have determined these constants through heuristic arguments and the use of experimental results. For example, R.J. Green presented an analytical method that considered a uniform cubic array of spherical voids in a solid under states of stress corresponding to pure shear and hydrostatic compression (Ref 2). The results for these two stress states allowed determination of the two variables A and B. He assumed that the stress distribution is uniform in two directions through the minimum section of the array and that the effect of the voids on the stress in the third direction is negligible on the planes riding between the voids. M. Oyane, S. Shima, and Y. Kono determined A and B using more stringent assumptions (Ref 3). They found poor agreement between their theoretical and experimental results and therefore reported experimentally determined values in a paper published later. Shima and Oyane abandoned this analytical approach entirely; instead, they refined empirical relations obtained for their experimental values (Ref 4). H.A. Kuhn and G.L. Downey also presented experimentally obtained values for these two variables (Ref 5). S.M. Doraivelu *et al.* determined these variables while taking into account the distortion energy due to the total stress tensor (Ref 6). The final equation obtained in Ref 6 is:

$$(2 + R^2) J'_2 + \frac{(1 - R^2)}{3} J_1^2 = (2R^2 - 1)\, Y_0^2 \qquad \textbf{(Eq 9)}$$

Flow Curves

The relationship between flow stress (or true stress or effective stress) and true strain is frequently called the flow curve because it determines the stress required to cause the material to flow plastically at any given strain. Many attempts have been made to fit mathematical equations to these curves at different temperatures and strain rates. Table 1 lists the various equations as well as the materials, temperatures, and strain rate ranges for which they are valid.

Table 1 Summary of flow stress-true strain equations

Equation	Materials studied	Temperature studied		Strain rate range studied, s^{-1}
		°C	°F	
$\sigma = K\epsilon^n$ **(Eq 10)**	Aluminum, Al-Mg-Si alloy, copper, low-carbon steel, nickel, zirconium, Inconel alloy, uranium	30-700	85-1290	**10^1-10^3**
	Al-4.2Cu alloy	100-600	212-1110	**0.25-16**
	Aluminum	-50-400	-60-750	. . .
	18-4-1 alloy steel	1000	1830	**300-680**
	Low-carbon steels with varying amounts of chromium	30-1200	85-2190	**0.25-12.5**
	Commercial purity aluminum; Al-Mn, Al-3Mg, and Al-4.5Mg alloys	30-480	85-900	**0.25-16**
	Low-carbon (0.15% C) steel, zirconium	30-1100	85-2010	. . .
$\sigma = K_1 + B_1\epsilon^n$ **(Eq 11)**	Aluminum	-50-400	-60-750	. . .
	Copper, 99.99% Al	30	85	. . .
$\sigma = K_2 + (B_2 + \epsilon)^n$ **(Eq 12)**	Aluminum	-50-400	-60-750	. . .
	Copper, brass, annealed stainless steel	30	85	. . .
$\sigma = K_3 + B_3 \log \epsilon$ **(Eq 13)**	Steels	800-1100	1470-2010	**10^1-10^3**
$\sigma = K_4 - (K_4 - B_4) \exp(-C_4 \epsilon)$ **(Eq 14)**	Aluminum, copper, brass, bronze	-50-400	-60-750	. . .
$\sigma = K_5 + B_5[1 - \exp(-C_5\epsilon)]^n$ **(Eq 15)**	Nickel and nickel alloys	0.6-0.9 T_m (°K)[(a)]		. . .
$\sigma = K_6[1 - \exp(-C_6\epsilon^{n'n})]^{1/n}$ **(Eq 16)**	**Vacuum-melted and zone-refined iron**	**500-800**	**930-**	**3.74 × 10^{-4} to**

			1470	**4.54 × 10^{-2}**

Source: Ref 3, 4, 5, 6, 7, 8, 9

(a) T_m, melting temperature of the alloy.

It is clear from the different equations proposed that none is entirely satisfactory for all materials and testing conditions. One researcher has attempted to fit his data on aluminum to Eq 10, 11, 12, and 13 (Table 1) and has concluded that Eq 10 provides the best fit (Ref 7). Other researchers have found that the flow curves for copper deviate from Eq 10 and consist of two parabolic curves, as given in Eq 11 (Ref 8). At higher strains, the exponential form of Eq 14, 15, and 16 seems to be appropriate because these lead to a steady-state stress at higher strains.

In most cases, the behavior of these alloy systems during forced dissipative flow has characteristics that are strongly dependent on initial conditions, such as the α+ β and β(Widmanstätten) microstructures shown in Fig. 1. Both of these microstructures are used as forging preform conditions, and each microstructure requires different thermomechanical processing during billet conditioning.

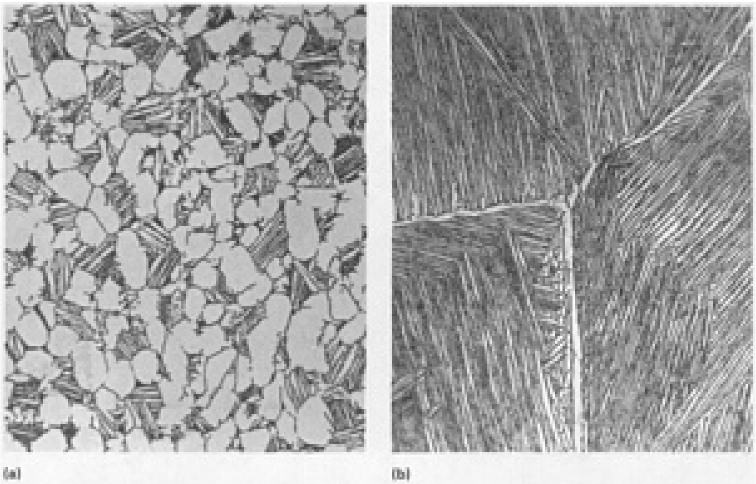

Fig. 1 Two types of initial microstructures in Ti-6Al-2Sn-4Zr-2Mo alloy. (a) α+ β preform microstructure. (b) β(Widmanstätten) preform. Both 500×

Isothermal constant strain rate testing of these materials at elevated temperatures reveals that, with time, the flow curves evolve into what is sometimes called a steady state. This evolution with time toward steady-state behavior is shown in Fig. 2 for a Ti-6Al-2Sn-4Zr-2Mo alloy having both types of initial microstructure. When the two flow curves approach each other asymptotically at approximately 0.60 effective strain, the transients associated with structural change have died away, and the Widmanstätten initial condition has evolved toward an equiaxed structure. Diffusional processes, if given sufficient time, will cause the structure to become equiaxed.

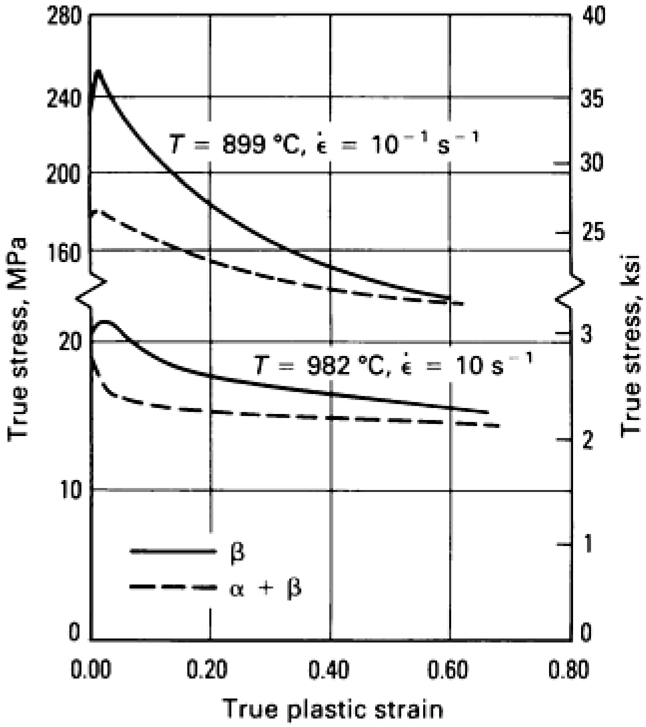

Fig. 2 Evolution with time of α + β and β Ti-6Al-2Sn-4Zr-2Mo alloy preforms to the same steady-state behavior

Deterministic modeling cannot be used to predict accurately the long-term behavior of complex engineering alloys, because it is necessary to choose *a priori* which atomic mechanisms may be operating and to decide how they can be used to control microstructural evolution and mechanical stability during forced dissipative flow. The problem that must be confronted in developing material behavior models for use in computer-aided engineering is of a statistical nature in a very complex setting. The mechanism, for example, by which the effort variable $\sigma(t)$ reaches the value $\sigma(t_2)$, where $t_2 > t$, is generally irrelevant to its behavior after time t_2 because the forced dissipative flow process is irreversible. The behavior of the workpiece material at t_2 is dependent on the evolution of certain intrinsic control parameters that govern how power is dissipated by random, fluctuating, generalized forces. These forces, for example, may be such system variables as chemical potential, temperature, and stress field gradients.

In practice, experimentation by measuring the flow stress of the workpiece material (the output) as a function of the kinematic variables (the input) of the system, in combination with mathematical modeling based on physical laws, is used to derive constitutive equations and information about workability. This methodology is called the black-box approach, and it can be used to derive a material model for the intrinsic workability of any material.

Flow Stress-Strain Rate Relationships

The most common relationships used to correlate flow stress-strain rate data at various temperatures and strain levels are summarized in Table 2, along with the materials and temperature and strain rate ranges for which they are valid. It can be seen that, for most materials, the power law or logarithmic expression seems to fit well. In the case of steel, Eq 18 (Table 2) seems to fit well. Equations 17 and 18 (Table 2) are analogous to the relationships obtained from creep data at low and high stresses, respectively (Ref 10).

Table 2 Summary of flow stress-true strain rate equations

Equation	**Materials studied**	**Temperature range studied**		Strain rate range studied, s^{-1}
		°C	**°F**	

$\sigma = K\bar{\dot{\epsilon}}^m$ **(Eq 17)**	Aluminum, Al-Mg-Si alloy, Mg-Al-Be alloy, copper, nickel, zirconium, low-carbon steel, Monel alloy, Inconel alloy, uranium	30-700	85-1290	**10^1-10^3**
	Titanium, zirconium, nickel, molybdenum, tungsten	20-1200	70-2190	**0.2-60**
	Super-pure aluminum	195-550	385-1020	**0.86-7.1**
	Al-4.2Cu alloy	30-600	85-1110	**. . .**
	Al-5.7Zn alloy	0.95 T_m (°K)[(a)]	. . .	**. . .**
	Pure aluminum, copper, low-carbon (0.17% C) steel	-199-1200	-325-2190	**. . .**
	Pure aluminum	500	930	**10^2-10^3**
	Aluminum, copper	250-900	480-1650	**110-600**
	18-4-1 alloy steel	1100	2010	**300-680**
	En 58, En 2A	30-600	85-1110	**3-250**
	12 different steels	900-1200	1650-2190	**1.5, 8, 40, 100**
	Zn-Al alloy	30-300	85-570	**. . .**
	Sn-Pb alloy	. . .		**10^{-2}-10^2**
$\sigma = K_1 + D\log\dot{\epsilon}$ **(Eq 18)**	Pure aluminum, copper, low-carbon (0.17% C) steel	-199-1200	-325-2190	**1-40**
	Super-pure aluminum, aluminum alloys 1060-O, 1100-O, 6061-O, 2024-O,7075-O, 6061-T6, 7075-T6	30	85	**10^{-3}-10^3**
	Aluminum	-50-400	-60-750	**. . .**
	Lead, aluminum, copper	30	85	**10^{-4}-10^3**
	Tool steels	1000	1830	**90-906**

	Pure lead	30	85	**105-258**
	High-strength steel	524-1055	975-1930	. . .
	Low-carbon steel	20-1055	70-1930	. . .
	Constructional steel	765-1055	1410-1930	. . .
	High-strength steel	. . .	. . .	**6.5 × 10^{-2}-430**
	Pure aluminum	**300-550**	**570-1020**	**1-45**

Source: Ref 3, 4, 5, 6, 7, 8, 9

(a) T_m, melting point of alloy.

In Eq 17 and 18, *K* is a temperature-dependent strength parameter, and *m* is the strain rate sensitivity factor. The *m* value is one of the most important control parameters, and the physical meaning behind it and its effect on intrinsic workability must be understood. For complex alloys, at a given deformation, *m* varies as a function of strain rate and temperature.

Flow Stress-Temperature Relations

Equation 19 has been used to represent the flow stress as a function of temperature (Ref 11, 12, 13):

$$\bar{\sigma} = \bar{\sigma}T_{Az} e^{-sT} \quad \textbf{(Eq 19)}$$

where $\bar{\sigma}T_{Az}$ and *s* are constants and T_{Az} is absolute zero temperature (-273.13 °C, or -459.5 °F).

A similar relationship has been defined between Brinell hardness (HB) and temperature, as follows (Ref 14):

$$HB = HBT_{Az}\ e^{-sT} \quad \textbf{(Eq 20)}$$

Many researchers have found that Eq 20 is valid for many metals and alloys in temperature ranges in which structural transformation, precipitation, dynamic strain aging, and so on, are not present (Ref 14). Whenever softening takes place by dynamic recovery and recrystallization, the value of *s* changes (Ref 15).

In the temperature range in which structural transformation takes place, the flow stress increases; this is represented as a small hump in the flow stress-temperature diagrams. This effect has also been found by many researchers for a number of metals and alloys (Ref 15, 16, 17, 18). The increase in flow stress has been observed at approximately 850 °C (1560 °F) for alloy steels (Ref 15, 16).

In high strain rate testing at a definite temperature range, thermal instability occurs, and deformation takes place in the form of bands (due to adiabatic heating). This is known as adiabatic shearing, and it can lead to fracture if it is severe (Ref 19). In this region, an increase in flow stress also occurs because of the hindrance of dislocation motion by the shear bands. This type of adiabatic shearing occurs in testing under very high strain rate conditions (Ref 19). In the case of alloy steels, similar increases have been observed at the low temperature range of 150 to 200 °C (300 to 390 °F) (Ref 16). Therefore, Eq 19 has been found to be invalid in regions in which the increase in flow stress occurs.

Combined Effect of Temperature and Strain Rate

To correlate flow stress and strain rate at different forming temperatures, it has been proposed that (Ref 20):

$$\bar{\sigma} = f(\dot{\bar{\epsilon}}\, e^{[Q/(RT)]}) = f(z) \qquad \textbf{(Eq 21)}$$

where Q is the activation energy and R is the gas constant.

A slightly different approach to this problem has also been taken (Ref 21). It has been proposed that strain rate and temperature be combined into velocity-modified temperature. The flow stress at a particular strain is then a function of velocity-modified temperature T_{Av}, which is defined as:

$$T_{Av} = T\left(1 - k \ln \frac{\dot{\epsilon}}{\dot{\epsilon}}\right) \qquad \textbf{(Eq 22)}$$

where k is the Boltzmann constant. Equation 22 has been verified for the case of steel and aluminum over a large temperature range, but only for a small range of strain rates (Ref 20). Based on Eq 22, values of activation energy, Q, independent of temperature have been obtained for a number of pure metals and simple alloys deformed at temperatures greater than 0.5 T_m (Ref 22).

However, this approach has been found to be invalid at lower temperatures and for more complex alloys, in which precipitation processes can occur within the temperature range of interest. In addition, in high strain rate tests where adiabatic conditions are approached, there is some ambiguity as to whether the initial temperature or a corrected temperature should be used.

The combined effect of temperature and strain rate has been described through the use of the following exponential relationship (Ref 23):

$$\dot{\epsilon} = \alpha_3\, e^{(\beta_3 \sigma)}\, e^{[-Q/(RT)]} \qquad \textbf{(Eq 23)}$$

Because Eq 23 does not correspond to any theoretical model, the parameters α_3 and β_3 do not have simple physical interpretations. This difficulty has been overcome through the use of Eq 24 (Ref 24, 25). Equation 24 is based on the thermally activated motion of dislocations over local and long-range barriers:

$$\dot{\epsilon} = \alpha_5\, e^{[(-\Delta H)/(kT)]}\, e^{[V(\sigma - \sigma_\beta)/(kT)]} \qquad \textbf{(Eq 24)}$$

where $\sigma\beta$ is the internally developed back stress and V is the activation volume.

It has been proposed that, under steady-state conditions, Eq 24 can be written as (Ref 25):

$$\dot{\epsilon} = \alpha_5\, e^{[(-\Delta H)/(kT)]}\, e^{[(V'\sigma)/(kT)]} \qquad \textbf{(Eq 25)}$$

where $V' = V(\lambda - 1/\lambda)$ is the apparent activation volume and $\lambda = \sigma\beta/\sigma$. However, other researchers have related experimental steady-state stress, strain rate, and temperature by using the following hyperbolic equation (Ref 26):

$$\dot{\epsilon} = \alpha_6\, [\sin h\, (\beta_6 \sigma)]^{1/m}\, e^{[Q/(RT)]} \qquad \textbf{(Eq 26)}$$

In Eq 23, 24, 25, and 26, α_3 to σ_6 are constants that depend on strain and temperature, and β_3 to β_6 are also constants.

With Eq 26, the average activation energy has been calculated on a computer for copper at somewhat lower strain rates. One researcher has used Eq 26 to fit his experimental data for various steels, aluminum, and copper and has also calculated activation energies (Ref 27).

For process modeling applications, continuous low-order polynomial equations are adequate because they generally help obtain rapid convergence and yield good accuracy in finite-element modeling. For example, this method has been used to describe the constitutive behavior of Ti-6Al-2Sn-4Zr-2Mo for both the transformed β and the $\alpha + \beta$ microstructures (Ref 28). The polynomial representations describe flow softening and steady-state behavior with a high degree of accuracy.

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Deformation Mechanisms and Deformation Maps

Deformation Mechanisms. The activation analysis discussed in Ref 29 has been used to obtain quantitative data on the deformation mechanisms that take place in metals and alloys. According to Ref 29, the activation enthalpy ΔH is given by:

$$\Delta H \cong Q = -k \left[\frac{\delta \ln \dot{\epsilon}}{\delta(1/T)}\right]_{\sigma} = kT^2 \left(\frac{\delta\sigma}{\delta T}\right)_{\dot{\epsilon}} \left(\frac{\delta \ln \dot{\epsilon}}{\delta\sigma}\right)_{T} \quad \textbf{(Eq 27)}$$

and the activation volume V is given by:

$$V = kT \left(\frac{\delta \ln \dot{\epsilon}}{\delta\sigma}\right)_{T} \quad \textbf{(Eq 28)}$$

Substituting Eq 28 in Eq 27 gives:

$$\Delta H \cong Q = -VT \left(\frac{\delta\sigma}{\delta T}\right) \quad \textbf{(Eq 29)}$$

Because Eq 27, 28, and 29 are used for creep and interrupted tests, they are modified for continuous compression tests as follows:

$$\Delta H = 2.3\ k \left[\frac{\delta \log \dot{\epsilon}}{\delta \log \sin h\ (\alpha\sigma)}\right]_{T} \left[\frac{\delta \log \sin h\ (\alpha\sigma)}{\delta\ (1/T)}\right]_{\dot{\epsilon}} \quad \textbf{(Eq 30)}$$

$$\Delta H = 2.3\ k \left[\frac{\delta \log \dot{\epsilon}}{\delta \log \sigma}\right] \left[\frac{\delta \log \sigma}{\delta\ (1/T)}\right]_{\dot{\epsilon}} \quad \textbf{(Eq 31)}$$

The experimental activation energy values have been calculated for various metals and alloys using several independent flow stress-true strain curves rather than the more often used single-interruption test, which accounts for sudden changes in strain rate and temperature. One researcher, while calculating the activation energy of Fe-30Cr alloy steel, has shown that correct values of ($\delta\sigma/\delta\dot{\epsilon}$) or ($\delta\sigma/\delta T$) can also be obtained from flow stress measurements made on continuous tests at varying temperatures and strain rates (Ref 30). Therefore, there is justification for the use of flow stress-true strain curves obtained from continuous tests for the calculation of experimental activation energies. These experimental values are compared with the available creep and self-diffusion data to predict the deformation mechanism based on available dislocation models. The activation energy values for various materials under self-diffusion, creep, and hot-working conditions are available in the literature.

When the structure changes because of the occurrence of other mechanisms along with the thermally activated diffusion or recovery or recrystallization mechanism, the activation energy given by Eq 27 is modified as follows:

$$\frac{\Delta H}{k} = -\left[\frac{\delta \ln \dot{\epsilon}}{\delta (1/T)}\right]_{\sigma} + \left[\frac{\delta \ln \alpha_5}{\delta (1/T)}\right]_{\sigma} + \frac{\sigma}{k}\left[V + \frac{1}{T}\frac{\delta}{\delta(1/T)}\right]_{\sigma} \quad \textbf{(Eq 32)}$$

In such cases (for example, copper), it would be difficult to predict the mechanism from the existing dislocation models alone. Other mechanisms have lower activation energies, so their possible frequency of occurrence is higher than the frequency demanded by the thermally activated process (Ref 31). It can therefore be concluded that compression, like creep, is also a thermally activated process, despite the difference of order in strain rate values (Ref 31).

Rate-controlling dislocation mechanisms (see below) are often identified from the magnitude of the measured activation parameters and their variation with temperature. Details on the effects of stress and temperature on the activation parameters are available in the literature. Some of the important mechanisms include:

- Overcoming *P-N* stress
- Intersection of dislocation
- Nonconservative motion of jogs
- Cross slip of screw dislocation
- Climb of edge dislocations
- Recovery and recrystallization

The deformation mechanisms that control the plastic behavior of various materials at different temperatures and strain rates have been identified by calculating the values of activation energy Q and activation volume V and comparing them with those of the above mechanisms.

Deformation Maps. During the past two decades, various investigators have extensively researched and reviewed the effects of strain, strain rate, temperature, and microstructure on the flow behavior of metals during deformation processing (Ref 1, 10, 32, 33). More recently, attempts have been made by H.J. Frost and M.F. Ashby (Ref 34) and R. Raj (Ref 35) to describe the deformation and fracture processes that occur during deformation processing. Both approaches are deterministic in the sense that shear strain rate equations (valid for steady state) are written, assuming that the equations are dependent on a number of basic atomic processes, such as dislocation motion, diffusion, grain-boundary sliding, twinning, and phase transformation. These approaches, as well as an extension of the work of Raj, are briefly described below.

Ashby-Frost Deformation Maps. Deformation maps of normalized stress versus absolute temperature for any polycrystalline material can theoretically be constructed, showing the area of dominance of each flow mechanism. Each flow mechanism must have an equation relating to shear strain rate $\dot{\gamma}$, shear stress τ, absolute temperature T (°K), and structure. The term structure includes all parameters describing the atomic structure, such as bonding, crystal class, defect structure, grain size, dislocation density and arrangement, solute concentration, and volume fraction of second-phase particles. Maps of this type are limited to pure polycrystalline materials, simple alloys, and steady-state conditions. A typical example is shown in Fig. 3.

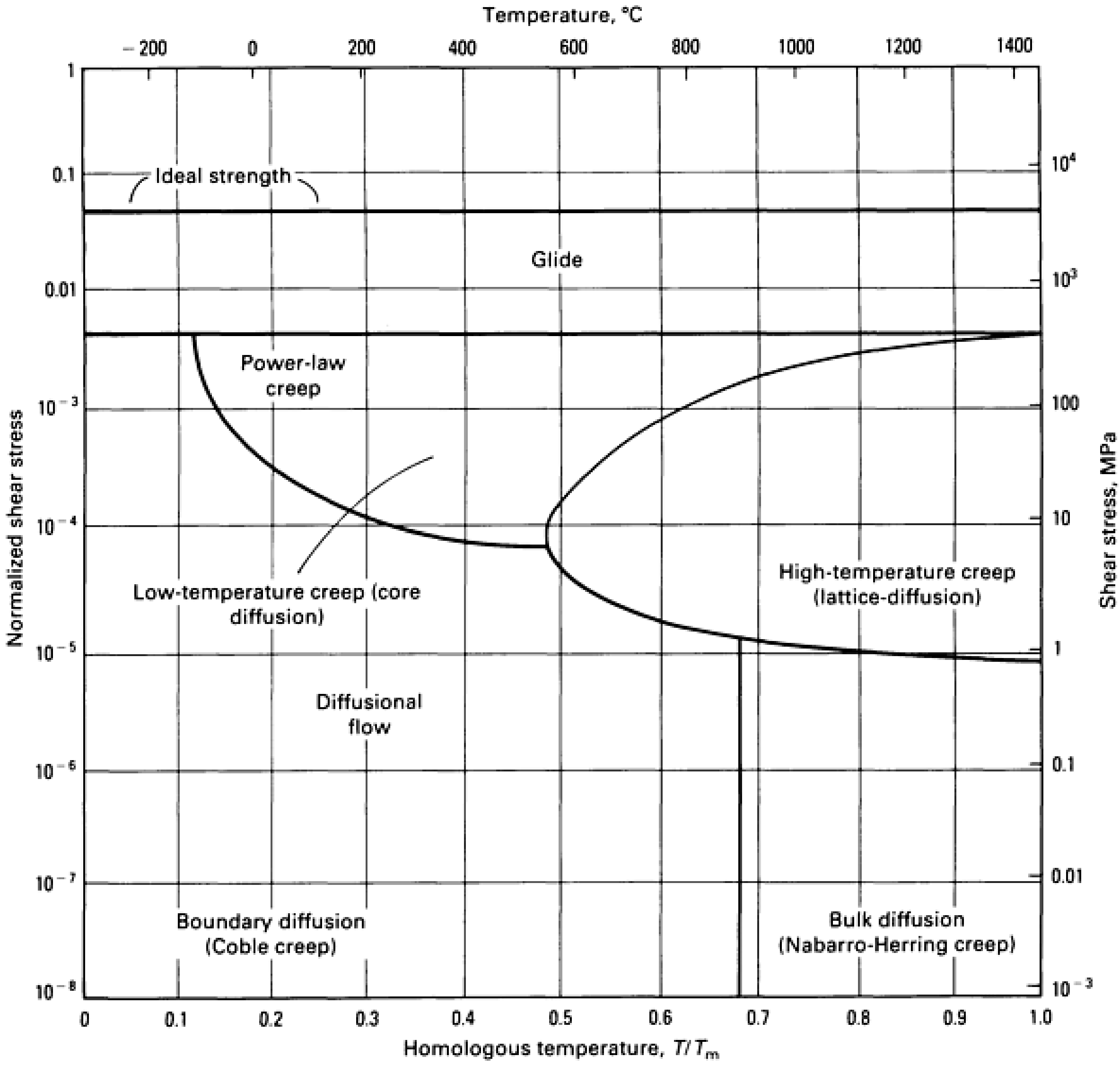

Fig. 3 Example of an Ashby-Frost deformation map

Damage Nucleation Maps. R. Raj extended the deformation map concept to a processing map that represents the nucleation of damage as a function of temperature *T* and effective strain rate $\dot{\bar{\epsilon}}$(Ref 35). A Raj processing map, such as that shown in Fig. 4, is a composite processing map based on various damage mechanisms. The processing map is very useful in that it defines regions in which it is safe to process the workpiece material and avoid defect nucleation. The boundaries are the loci of bifurcation points, where the differential equation takes on a new solution or where the flow process changes from a stable to an unstable process.

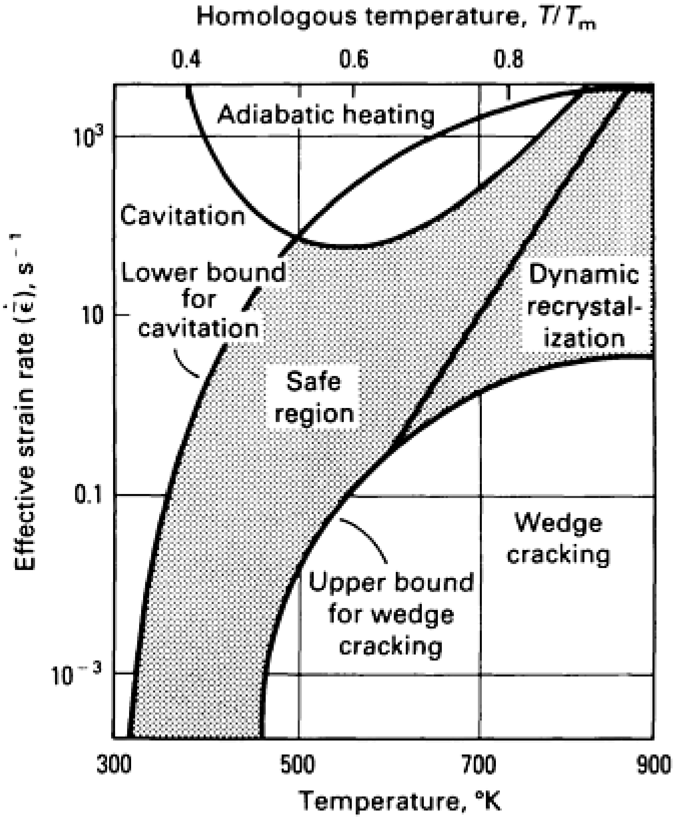

Fig. 4 Example of a Raj damage nucleation deformation map

Like the Ashby-Frost deformation maps, the Raj processing map is highly idealized. Neither approach to describing the workability of materials is suitable for complex engineering alloys, because these models consider only the deterministic aspect of predicting the behavior of a workpiece material under processing conditions. Experience in designing bulk forming processes clearly indicates that predicting the future behavior of a material involves not only deterministic aspects but also statistical and indeterministic features.

Rao-Raj Deformation Maps. K.P. Rao superimposed room-temperature ductility data for specimens deformed under various elevated-temperature and strain rate combinations onto a Raj damage nucleation map (Ref 36). Rao observed that low ductility values are located in the region where the Raj map predicts wedge cracking, which is a form of plastic instability. Peak values of ductility were found in the middle of the safe region. This behavior is shown in Fig. 5.

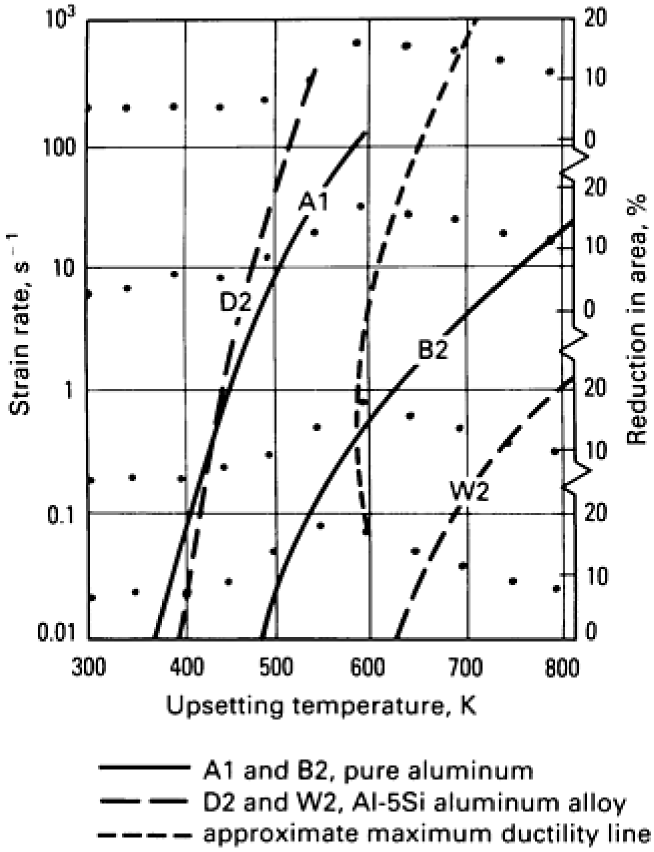

Fig. 5 Raj damage nucleation map with superimposed room-temperature ductility data for specimens previously deformed under various temperature-strain ratio conditions

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Dynamic Material Modeling

Control of microstructure during any material-processing operation requires answers to two fundamental questions:

- What conditions does the process require the workpiece material to withstand?
- How does the workpiece material respond to the demands imposed by the process?

The first question can be answered by modeling the process with finite-element methods. The second question, which deals with how the workpiece material responds to the demands of the process, can be answered only if the flow behavior of the workpiece material has been adequately characterized under processing conditions. This necessitates an understanding of how the workpiece will dissipate the instantaneous power applied to it by the process.

For example, during the forging process, the press provides instantaneous power to the workpiece in the amount described by:

$$\bar{\sigma}\dot{\bar{\epsilon}} = \sigma_1\dot{\epsilon}_1 + \sigma_2\dot{\epsilon}_2 + \sigma_3\dot{\epsilon}_3 \quad \textbf{(Eq 33)}$$

where $\bar{\sigma}$ is the effective stress, $\dot{\bar{\epsilon}}$ is the effective strain rate, and the terms on the right-hand side are the products of principal stresses and principal strain rates.

The workpiece will dissipate the instantaneous power applied by a metallurgical process commensurate with the level of power supplied. For example, when energy is supplied at a very high strain rate, the material dissipates it by fracture processes. On the other hand, when energy is supplied at a lower and more controlled rate, the material dissipates it by superplastic flow if the material has the correct microstructure and is deformed under superplastic conditions. The efficiency of energy dissipation of these metallurgical processes in both cases may be the same, but the variation of efficiency with respect to strain rate may not be favorable for achieving a steady-state condition.

The applied power also sets up an entropy production rate by a material system. This rate is controlled by the second law of thermodynamics and is directly related to grain size. The rate of entropy production by the material system reaches a maximum level when the material system has the potential to develop very fine grain structure. The rate of entropy production begins decreasing when grain growth takes place. To study this phenomenon, a parameter known as entropy rate ratio *s* has been defined by the dynamic material modeling approach (Ref 37, 38). Therefore, to control microstructures and to avoid defect formation, a new type of material behavior model termed a dynamic material model is required in order to understand how the workpiece dissipates power while producing entropy to satisfy the demands of the process. This model is capable of producing information consistent with the unifying aspects of the finite-element model in such a way that it can become a nonholonomic constraint in the finite-element model for obtaining optimal solutions during the design of various unit processes.

Basic Concepts

According to the dynamic material models described in Ref 37 and 38, power *P* (rate of work done) is dissipated coherently by partitioning it into *J,* whose energy primitive is potential energy, and *G,* which is heat. Kinetic energy *K* is the primitive for *G.* The partitioning is given by:

$$P = \bar{\sigma}\dot{\bar{\epsilon}} = \int_0^{\bar{\sigma}} \dot{\bar{\epsilon}}\, d\bar{\sigma} = \int_0^{\dot{\bar{\epsilon}}} \bar{\sigma}\, d\dot{\bar{\epsilon}} \quad \textbf{(Eq 34)}$$

Power dissipated by plastic work is denoted by the area *G* under the curve, which is illustrated schematically in Fig. 6. This is designated as the dissipator content. Area *J*, above the curve, is designated as the dissipator co-content. The fundamental assumption in Ref 37 and 38 is that *J* is related to structural changes and that *G* is related to continuum effects. Plastic instability and fracture processes are associated with *G*, and microstructure evolution is associated with *J*. Both *J* and *G* are complementary functions, and they are related by a Legendre transformation (Ref 39).

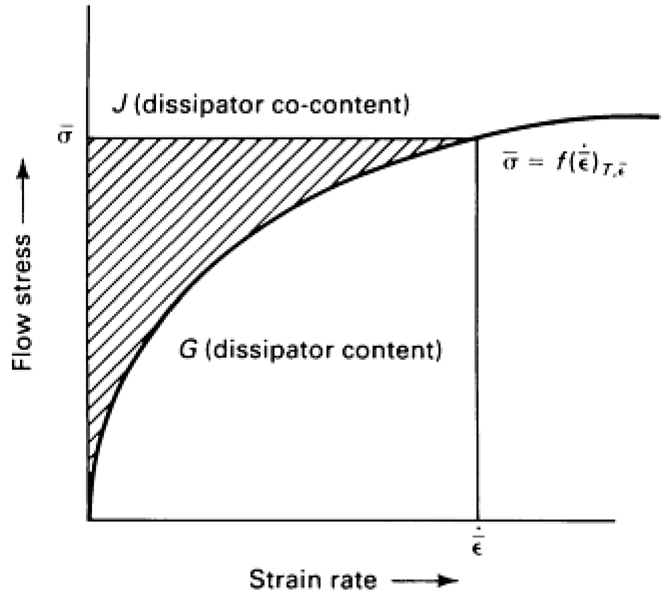

Fig. 6 Schematic showing the constitutive relation of the material system as an energy dissipator during forming. See text for details.

It follows from Eq 34 that the partitioning of power between *J* and *G* is given by:

$$\frac{dJ}{dG} = \frac{\dot{\bar{\epsilon}}\, d\bar{\sigma}}{\bar{\sigma}\, d\dot{\bar{\epsilon}}} = \frac{d \log \bar{\sigma}}{d \log \dot{\bar{\epsilon}}} \qquad \text{(Eq 35)}$$

This ratio is generally known as the strain rate sensitivity parameter, *m*, of the workpiece material. Using this parameter, the dynamic constitutive equation given below can be developed:

$$\sigma = k\dot{\bar{\epsilon}}^m \qquad \text{(Eq 36)}$$

Equation 36 is termed a dynamic constitutive equation because it is a result of integrating along the dynamic (actual) path that an element of material takes according to the principle of least action, when it is instantaneously deformed at a particular strain rate. The strain rate sensitivity is assumed to be constant along this trajectory up to that strain rate and at the temperature and effective strain under consideration. The strain rate sensitivity parameter, calculated from continuous flow stress values measured at different constant strain rate tests, has generally been observed to be independent of temperature and strain rate for pure metals, but in engineering alloys it has been shown to vary with temperature and strain rate:

$$J = \frac{m}{m+1}\bar{\sigma}\dot{\bar{\epsilon}} \qquad \text{(Eq 37)}$$

$$G = \frac{1}{m+1}\bar{\sigma}\dot{\bar{\epsilon}} \qquad \text{(Eq 38)}$$

Equations 37 and 38 have been obtained by integrating $dJ = \dot{\bar{\epsilon}} d\bar{\sigma}$ and $dG = \bar{\sigma} d\dot{\bar{\epsilon}}$ (from Eq 35). The upper limit of $m = 1$ fixes the maximum value of J to be $0.5P$. By normalizing instantaneous J with this maximum value, an efficiency factor η can be defined as $\eta = J/J_{max} = 2m/(m + 1)$. This dimensionless parameter η becomes important for controlling the dissipated power J and for formulating a Liapunov function, because the system reaches a maximum value of η at the lowest energy state during stable conditions. The Liapunov function is a system quantity associated with Liapunov stability criteria and is regarded as a general and accepted method in engineering design (Ref 40). This function is an arbitrary term that relates changes in the total energy of a given system. The Liapunov criteria for stability require the system to lower its total energy continuously.

Therefore, the Liapunov function V_1 is formulated as shown below:

$$V_1 = \eta(\log \dot{\bar{\epsilon}}) \quad \textbf{(Eq 39)}$$

Because η itself forms the condition for stability, $\delta\eta/\delta$ ($\log \dot{\bar{\epsilon}}$) should be less than 0 in the stable region. This condition definitely ensures that the system is approaching the steady-state condition in which it experiences a minimum energy state and maximum η without fracture.

The next step is to identify a dimensionless control parameter in order to study the influence of temperature on material behavior and workability. For this purpose, the total power dissipated is represented in terms of the rate of entropy applied $\dot{S}_{app}$ and temperature T as shown below, and a methodology has been established to determine the relationship between the rate of entropy produced by the system $\dot{S}_{sys}$, and the rate of entropy applied to the system $\dot{S}_{app}$, using the following steps:

$$P = \bar{\sigma}\dot{\bar{\epsilon}} = \dot{S}_{app} T \quad \textbf{(Eq 40)}$$

and by definition:

$$\left.\frac{\delta P}{\delta T}\right|_{\dot{S}} = \dot{S}_{sys} \quad \textbf{(Eq 41)}$$

Furthermore:

$$\frac{\delta P}{\delta T} = \frac{P}{-T^2}\frac{\delta(\ln P)}{\delta(1/T)} = \frac{P}{T}\left[-\frac{1}{T}\frac{\delta(\ln P)}{\delta(1/T)}\right] \quad \textbf{(Eq 42)}$$

where:

$$\left.\frac{1}{T}\frac{\delta(\ln P)}{\delta(1/T)}\right|_{\dot{\epsilon}} = -\frac{1}{T}\frac{\delta(\ln\bar{\sigma})}{\delta(1/T)} \quad \textbf{(Eq 43)}$$

define a new coefficient s such that:

$$s = \left[-\frac{1}{T}\frac{\delta \ln\bar{\sigma}}{\delta(1/T)}\right] \quad \textbf{(Eq 44)}$$

The minus sign in Eq 42, 43, and 44 can be neglected and the value can be treated as a positive because flow stress decreases with respect to T.

It follows that the rate of entropy production by the system is given by:

$$\dot{S}_{sys} = \frac{P}{T} s \qquad \textbf{(Eq 45)}$$

where P/T can be thought of as the applied rate of entropy input $\dot{S}_{app}$.

According to the second law of thermodynamics, s should be greater than unity for stable material flow. This implies that the workpiece should store entropy at least as fast as the entropy production rate of working heat for stable flow. Therefore, $\delta s/\delta (\log \dot{\bar{\epsilon}})$ should also be less than 0 for stable flow if s is treated as a Liapunov function, that is:

$$V_2 = s (\log \dot{\bar{\varepsilon}}) \qquad \textbf{(Eq 46)}$$

These two conditions--$\delta \eta/\delta (\log \dot{\bar{\epsilon}}) < 0$ and $\delta s/\delta (\log \dot{\bar{\epsilon}}) < 0$--are important and are used for the development of processing maps.

The processing map provides the necessary conditions for working a material under stable processing conditions. It represents a conservative estimate of the conditions in terms of T and $\dot{\bar{\epsilon}}$. However, it is necessary to examine the effective stress rate path, which is determined by process modeling, to determine whether instability can be caused by such processes as grain-boundary cavitation or wedge cracking when the processing conditions are marginal. A value of $\sigma_m/\bar{\sigma}$ of $\frac{1}{3}$ corresponds to a pure tensile state of stress.

Under this condition, any deformation process leading to cavity nucleation would eventually produce ductile fracture. When $\sigma_m/\bar{\sigma}$ is equal to $-\frac{2}{3}$, large amounts of plastic flow are possible. This state of stress corresponds to conditions that can be associated with ideal streamlined flow during extrusion. Figure 7 shows the distribution of $\sigma_m/\bar{\sigma}$ during the uniaxial compression of a cylindrical rod, and Fig. 8 shows plane-strain compression. Both of these are under conditions of high friction.

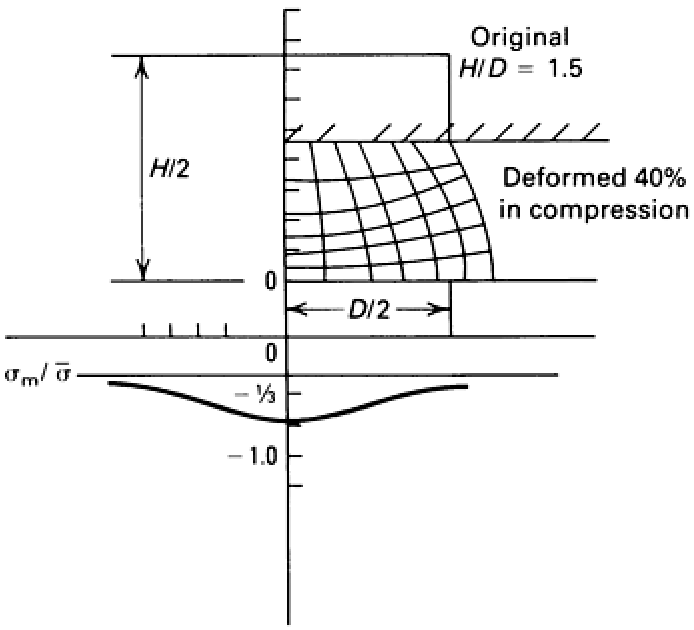

Fig. 7 Mesh distortion and distribution of $\sigma_m/\bar{\sigma}$ during uniaxial compression (from finite-element method analysis)

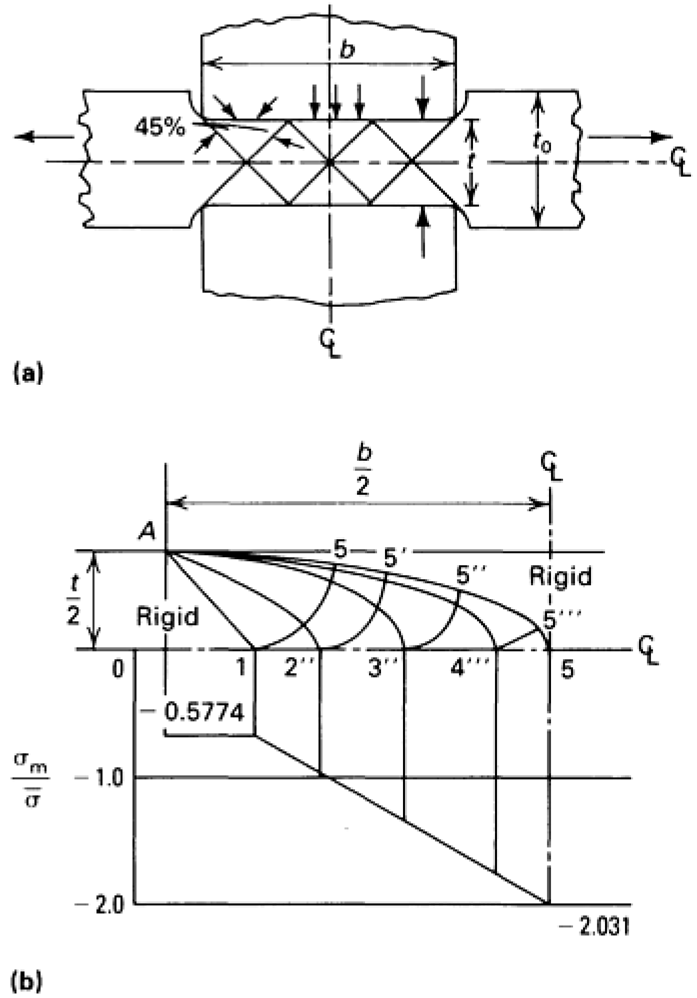

Fig. 8 Slip line field (a) and distribution of $\sigma_m/\bar{\sigma}$ (b) during plane-strain compression

Intrinsic Workability

Materials being formed (forged) undergo large, nonlinear, irreversible deformation that is made possible by linking together several atomic processes. This string of processes, in addition to providing the degree of freedom needed for producing large deformation, is also responsible for the evolution of structure. Each of these processes can be thought of as a channel for dispersing energy supplied by the forging process, and each can give rise to entropy production. Forging, as a rule, is a shape-changing process in which the deformation is generally inhomogeneous and transient over a large volume of the deforming continuum. Steady-state phenomena often represent a limiting condition that can be achieved only under unique combinations of temperature T and effective strain rate $\dot{\bar{\varepsilon}}$. Therefore, it is highly desirable to be able to define stable regions in terms of the kinematic variables T, $\bar{\varepsilon}$, and $\dot{\bar{\varepsilon}}$.

A solution to this problem would require identification of the limiting conditions. These limiting conditions would be loci of bifurcation points where two atomic mechanisms are operating simultaneously to produce a maximum in the energy dispersal rate at a unique combination of temperature T and effective strain rate $\dot{\bar{\varepsilon}}$. Under these unique conditions, the deformation process would be steady state. One branch of the deformation process would be stable, and the other would be unstable. Processes such as grain-boundary cavitation, wedge cracking, and cleavage are considered to cause the deforming continuum to become unstable, because the free surfaces formed increase the free energy of the system rather than decrease it. Therefore, workability is an intrinsic characteristic of the material; the ability of the material to dissipate power at any state of stress by favorable metallurgical mechanisms generates entropy at a faster rate than it is applied and maintains the total energy of the system at the lowest level possible.

Determination of Processing Maps

Using flow stress values calculated at different temperatures and effective strain rates, the parameters η and s are determined as functions of temperature and effective strain rate. From these values, $\delta\eta/\delta$ (log $\dot{\bar{\epsilon}}$) and $\delta s/\delta$ (log $\dot{\bar{\epsilon}}$) values are calculated, and all the values less than or equal to 0 are grouped to determine the stable region. The stable region and the contours of η are presented as a processing map of effective strain rate versus temperature, as shown in Fig. 9.

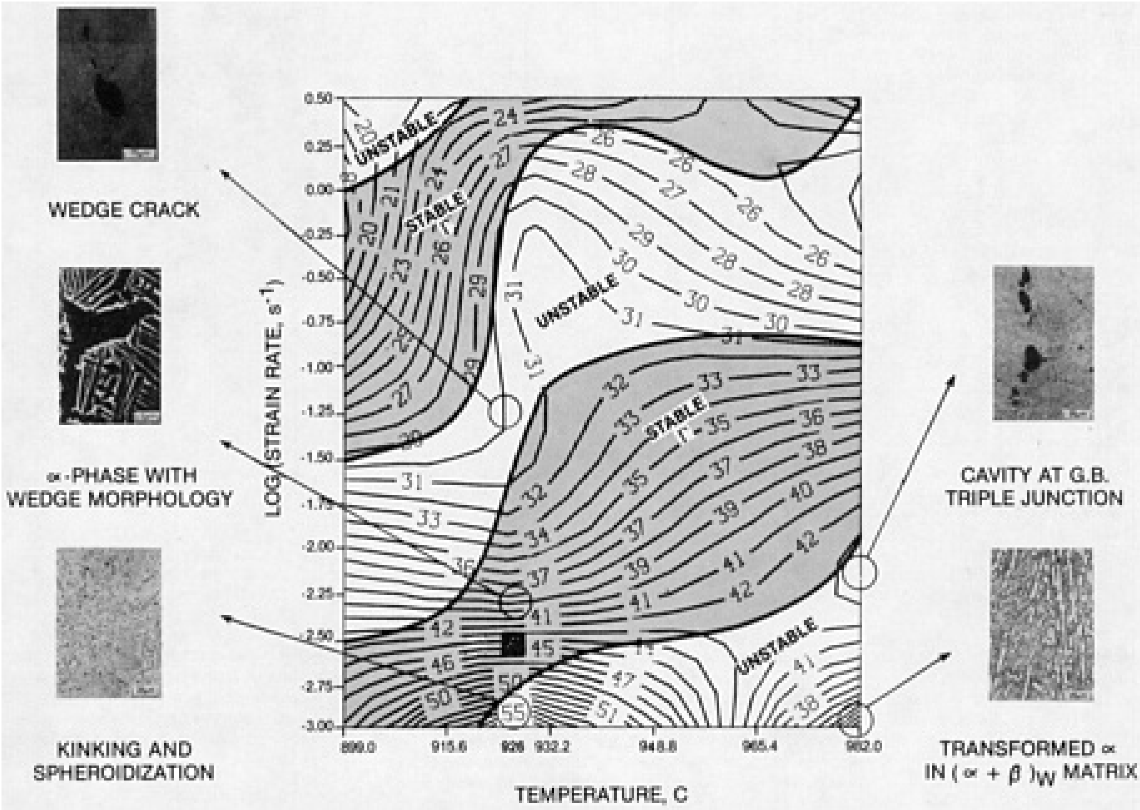

Fig. 9 Processing map for βTi-6Al-2Sn-4Zr-2Mo alloy with stable (Γ) regions and unstable regions identified

Selection of Processing Conditions

According to the processing map developed for βTi-6Al-2Sn-4Zr-2Mo (Fig. 9) using the above procedure, two Γ(stable) regions can be obtained in the map. There is one Γregion in the high strain rate regime in which the efficiency is of the order of 26% (upper left portion of Fig. 9). If hammers are to be used for forging, this processing region can be selected. However, if a press is to be used, then it is highly recommended to select processing conditions in the Γregion in the low strain rate regime and high-efficiency regime (lower-central portion of Fig. 9). Parts produced by either hammer forging or press forging have acceptable quality. This confirms the prediction that two stable regions in the Ti-6Al-2Sn-4Zr-2Mo processing map are valid.

The next obvious question is how to determine the optimum processing conditions from the processing map in order to obtain maximum intrinsic workability of the workpiece material. It is possible to answer this question to a certain extent by using the efficiency values, although it is not possible to characterize the metallurgical mechanisms based on efficiency alone. In the Γregion, where higher efficiency is observed, it is always possible to obtain higher workability because the rate of dissipation by the favorable stable mechanism is higher. This conclusion requires thorough experimental and metallurgical studies that are beyond the scope of this article. However, a few microstructures are included in the Ti-6Al-2Sn-4Zr-2Mo processing map in which peaks in the efficiency surface are observed. Peaks outside the stable regions clearly show defects in the microstructure, while peaks inside the stable Γregions show an acceptable and stable microstructure.

Another interesting point concerns the microstructure observed at 926 °C (1700 °F) and a strain rate of 5.6×10^{-3} s^{-1}. Although observed in the stable region, this microstructure (center micrograph on the left side of Fig. 9) is not acceptable, because the α-phase formation at the grain-boundary triple junction is a potential initiation site for low-cycle fatigue cracking. Therefore, additional microstructural studies are required for selecting optimum processing conditions for the given application. Based on a similar argument, a temperature in the range of 899 to 920 °C (1650 to 1690 °F) and a strain-rate of the order of 1 to 10 s^{-1} are recommended if the use of hammer-forging equipment is preferred.

It is also of interest to include observations made by some forging companies. These companies have tried to forge billets at temperatures beyond 926 °C (1700 °F) using a low strain rate of 10^{-3} s^{-1}. These forgings have poor properties when compared to forgings produced below 926 °C (1700 °F) at the same strain rate. Again, this observation confirms the prediction of unfavorable material flow under the unstable conditions of low strain rates at temperatures greater than 926 °C (1700 °F). Wedge cracks were observed in these regions.

Therefore, the successful application of Liapunov stability theory for determining optimum processing conditions for Ti-6Al-2Sn-4Zr-2Mo demonstrates that this is a viable engineering approach for defining safe regions in processing space. This information can then be incorporated into the finite-element code used for the simulation of metal-forming operations.

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Modeling Techniques Used in Forging Process Design

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Analytical Process Modeling of Forging Operations

Several methods are available for the analytical modeling of the forging process. Generally, they fall into the following categories:

- The slab method, which restricts the change of stress to only one direction
- The uniform deformation energy method, which neglects redundant work involved in internal shearing due to nonuniform deformation
- The slip-line field solution, which is limited to rigid-plastic materials under plane-strain conditions
- The bounding methods, which can provide fairly good estimates of upper and lower limits of the deformation force but cannot provide details of local stress and strain distributions
- The finite-element method, which provides the information required for die design and process control

All of the above approaches, except the finite-element method, are capable of providing approximate solutions to processing problems, but are subject to some limitations. Therefore, the following discussion will consider the most commonly used approximate methods separately from the finite-element method.

Approximate and Closed Form Solutions

In general, the boundary conditions in metal-forming operations are too complicated to be accounted for by analytical solutions to the plasticity problem. The need to obtain at least approximate solutions has been satisfied by simplifying assumptions, but each of the analytical approaches has its limitations. The most commonly used approximate methods will be briefly discussed without attempting a rigorous description of the equations that are solved, because this information is available in several other sources (Ref 41, 42, 43, 44, 45, 46, 47). These methods are described in detail with examples in the above references and are presented in increasing order of complexity.

The Sachs (Slab) Method. In this approach, the deformation is assumed to be homogeneous, and the force equilibrium equations are set up and solved using an appropriate yield criterion. The major deficiencies of this approach are:

- Redundant work is not accounted for
- Stress and strain gradients are accounted for in only one direction and are assumed to be uniform in the perpendicular direction

The slab method is a quick way of obtaining approximate load and strain estimates in axisymmetric and plane-strain problems and is therefore widely used.

The slip-line field approach was developed for plane-strain problems. It assumes that the material is rigid and ideally plastic (that is, the material does not strain harden). The theory is based on the fact that any state of stress in plane strain can be represented as the sum of a hydrostatic stress and a pure shear stress. Given the force and velocity boundary conditions, this slip-line field is constructed. The main advantage of this method over the slab method is that it can provide local stress calculations even when the deformation is not homogeneous. The major limitations of the slip-line field approach are:

- It is usable only for plane-strain problems
- It assumes rigid and ideally plastic materials
- The method is tedious, and solutions are difficult to verify

The upper bound method is based on the limit theorem stating that the power dissipated by the boundary forces at their prescribed velocities is always less than or equal to the power dissipated by the same forces under any other kinematically acceptable velocity field. A kinematically acceptable velocity field must satisfy the velocity boundary conditions and material incompressibility. This method allows kinematically admissible velocity fields to be set up as a function of an unknown parameter. Power dissipation is then minimized with respect to the unknown parameter to yield a reasonable estimate of load.

The main disadvantage of this method is that the choice of velocity field is rather arbitrary, and the poorer the selection, the more the estimated load will exceed the true load. Another limitation is that no local stress field is compared.

The lower bound method is not of great practical significance, because forming loads are underestimated. However, it does provide an indication of how conservative the upper bound solution is if the lower bound solution is known.

The lower bound approach is based on the limit theorem stating that the power dissipation of the actual surface forces at their prescribed velocities is always greater than the power dissipation of the surface tractions corresponding to any other statically admissible stress field. A statically admissible stress field must satisfy force equilibrium and not violate the yield criterion.

Finite-Element Methods

Because of the rapid advancement of high-speed digital computer technology, numerical methods of analysis have been developed. These include the finite-element method (FEM) and the finite-difference method (FDM). Due to the complexity of material flow during metal forming, the finite-element method is the most suitable for analyzing such problems. Finite-element methods, as applied to metal-forming analysis, can be classified into either elastic-plastic or rigid-viscoplastic methods, depending on the assumptions made with regard to the material flow behavior.

The elastic-plastic method assumes that the material deformation includes a small, recoverable elastic part and a much larger, nonrecoverable plastic part. It can give details regarding deformation loads, stresses and strains, and residual stresses. This method has been applied to a large variety of problems, including upsetting (Ref 48), indentation (Ref 49), extrusion (Ref 50), and expansion of a hole in a plate (Ref 51). However, because of the large change in the material flow behavior between elastic and plastic deformation and the need to check the status of each element, the deformation steps must be small, and this makes the method uneconomical.

The rigid-viscoplastic method assumes that the deformation stresses are primarily dependent on deformation (strain) rates. Several programs based on the variational approach have been written by various researchers and have been applied to the same range of problems as the elastic-plastic finite-element method (Ref 52). Although predictions regarding residual stresses cannot be made with the rigid-viscoplastic finite-element method, the larger steps that can be used in modeling metal-forming procedures make the method very economical, especially for modeling hot deformation.

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Modeling Techniques Used in Forging Process Design

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Analysis of Large Plastic Incremental Deformation (ALPID)

The computer program ALPID, a rigid-viscoplastic FEM program that uses the approach of Kobayashi *et al.,* was developed at Battelle Columbus Laboratories under the Air Force Processing Science Program (Ref 53). Important contributions (Ref 53) to the FEM formulation included the incorporation of convenient features, such as capabilities for handling arbitrary die geometries and remeshing. Details of this FEM formulation and some of its applications to practical problems can be found in Ref 53. The features of ALPID are discussed below.

Finite-Element Problem Formulation in ALPID. The Cartesian coordinate is used to derive the minimum principle. Generalization of this formulation to other coordinate systems can be made without difficulty. With regard to Eq 47, 48, 49, 50, 51, 52, 53, 54, and 55, a body of volume, *V*, is considered with the traction, *F*, prescribed on a part of the surface, S_F, and the velocity, *V*, prescribed on the remainder of the surface, S_U. The body is composed of a rigid-plastic material that obeys the von Mises yield criterion and its associated flow rule. Body forces are assumed to be absent. The actual stress, σ_{ij}, and velocity field, *v*, satisfy the following relations.

Equilibrium conditions, neglecting body forces, are:

$$\sigma_{ij,j} = 0 \qquad \textbf{(Eq 47)}$$

where σ_{ij} is the stress tensor and $_{,j}$ denotes partial differentiation with respect to *j*.

The compatibility condition is given by:

$$\dot{\epsilon}_{ij} = (V_{i,j} + V_{j,i}) \qquad \textbf{(Eq 48)}$$

where $\dot{\epsilon}_{ij}$ and V_i are the strain rate and velocity, respectively.

The constitutive relations are given in Eq 49, 50, and 51:

$$\sigma'_{ij} = \frac{2}{3}\left[\frac{\bar{\sigma}}{\dot{\bar{\epsilon}}}\dot{\epsilon}_{ij}\right] \qquad \textbf{(Eq 49)}$$

where σ'_{ij} is the deviatoric component of the stress tensor:

$$\bar{\sigma} = \sqrt{\frac{3}{2}[\sigma'_{ij}\ \sigma'_{ij}]} \qquad \textbf{(Eq 50)}$$

and

$$\dot{\bar{\epsilon}} = \sqrt{\frac{2}{3}[\dot{\epsilon}_{ij}\ \dot{\epsilon}_{ij}]} \qquad \textbf{(Eq 51)}$$

Equations 49, 50, and 51 are valid for fully dense ingot metallurgy alloys. For powder metallurgy alloys, Eq 49, 50, and 51 must be modified to include hydrostatic stress, as shown in a later section in this article. The flow stress is generally a function of total strain, strain rate, temperature, relative density, and microstructure.

The boundary conditions are given by:

$$\sigma_{ij}\, n_i = F_j \text{ on } S_F \qquad \textbf{(Eq 52)}$$

and

$$v_i = V_i \text{ on } S_U \qquad \textbf{(Eq 53)}$$

and |*fs*| is the frictional stress with the proper sign, where n_i is the unit vector normal to the surface.

Equations 51, 52, and 53 can be put into the variational principle as:

$$\delta\Phi = \sigma\left[\int E(\dot{\epsilon}^*)\, dV + \int \frac{1}{2} K(\dot{\epsilon}^*)^2\, dV - \int (f_s dv_s)\, ds - \{F_j V_j^*\, ds\}\right] = 0 \qquad \text{(Eq 54)}$$

where the work function $E(\dot{\epsilon})$ can be expressed as:

$$E(\dot{\epsilon}) = \int_0^{\dot{\epsilon}} \bar{\sigma}\, d\dot{\bar{\epsilon}} \qquad \text{(Eq 55)}$$

In Eq 54, K is a large positive constant that penalizes the dilational strain, and * denotes the restriction of the velocity fields to the trial space. The standard procedure of discretionization of this function can be found in Ref 54.

Computational Scheme of ALPID. The processing phase of ALPID has two major stages. The first is the guess generation stage, which generates an initial solution (guess) assuming a linear material relationship. The second is the solution stage for the actual nonlinear constitutive behavior of the material. These stages will be briefly discussed; Fig. 10 shows a simplified flow chart that outlines the operation of ALPID.

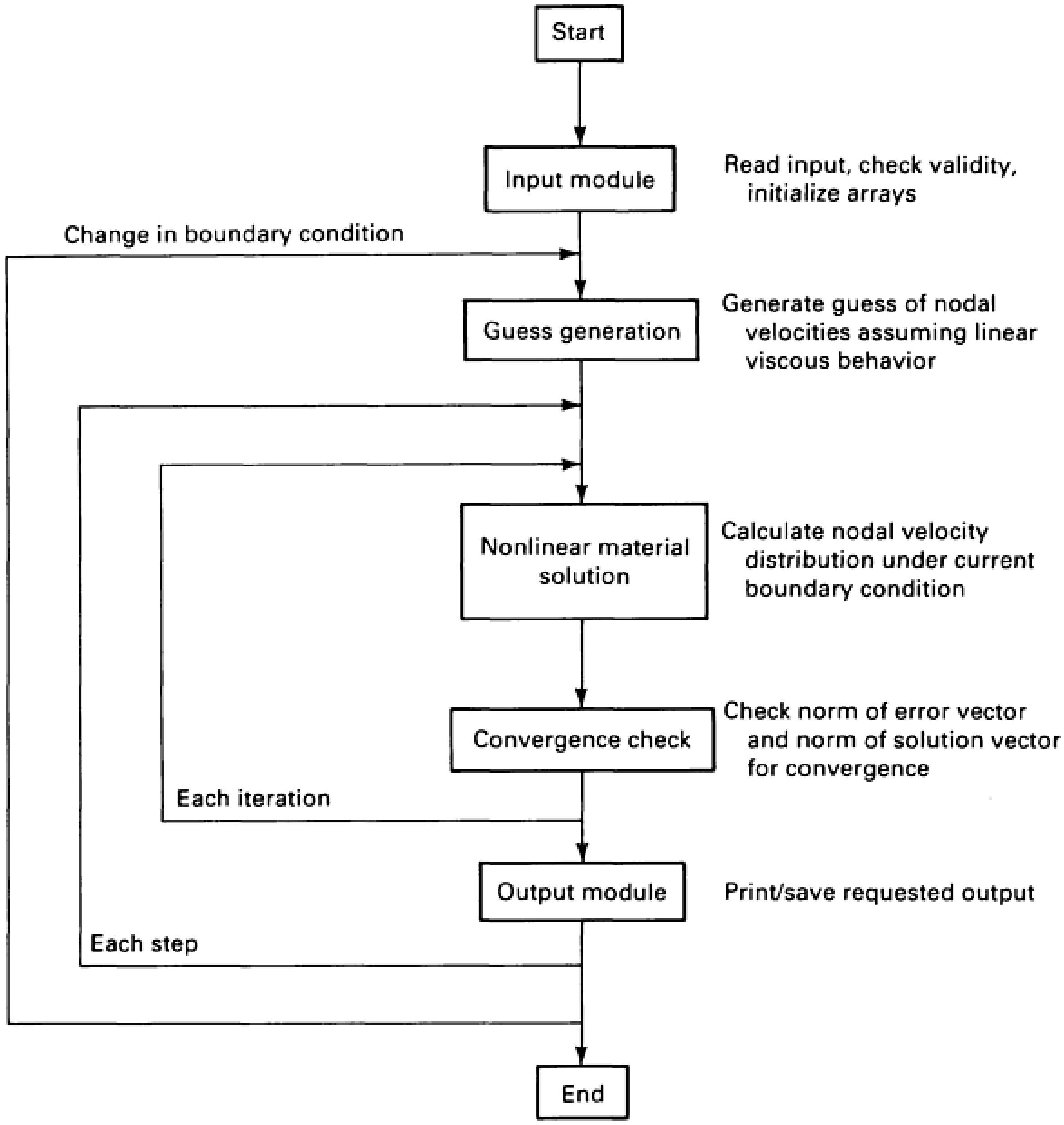


Fig. 10 Simplified flow chart showing steps in the operation of ALPID

Guess generation provides a good guess of the velocities at the nodal points in the finite-element mesh. Because ALPID uses the Newton-Raphson method to solve the nonlinear system of equations, it is important to have a good initial guess. This initial guess is necessary when previous solutions are not available or not applicable. A guess of the velocity distribution is also required each time the boundary conditions change. Boundary conditions change whenever a node comes into contact with a die or whenever a node in contact with a die becomes a free node. The steps in guess generation are:

- Generation of the element stiffness matrix assuming linear behavior of the materials
- Application of displacement boundary conditions
- Addition of element stiffness matrices to the global stiffness matrix
- Application of force boundary condition
- Solution of resulting linear equations

Solution Stage for Nonlinear Materials. The guesses of velocities generated in the previous stage of execution are used to estimate the material properties and to identify rigid and viscoplastic elements. This information is used to generate the element stiffness matrix. The steps involved in the solution for nonlinear materials are:

- Obtain yield stress of the elements to identify the rigid element
- Generate element stiffness matrix using

    1. linear behavior for rigid elements and
    2. actual constitutive behavior for viscoplastic elements

- Apply shear friction boundary condition, if applicable
- Add to the global stiffness matrix
- Apply Coulomb friction boundary condition, if applicable
- Determine iteration strategy and step length
- Solve a system of linear equations
- Check for convergence

This process is repeated until convergence is obtained. Execution stops if the solution does not converge in a specified number of attempts. When convergence is obtained, the nodal coordinates are updated, and all of the post calculations are completed. If there is no change in the boundary conditions, the solution from each step is used as the initial guess for the next step.

The stiffness matrix generation involves the following steps:

- Obtain the shape function and shape function derivatives at the Gauss integration points of an element
- Change from natural to global coordinates to obtain the [B] matrix
- Obtain the strain rate hardening parameter
- Calculate $[B]^T$ [D] [B] matrix at each integration point and multiply by weighting factor
- Repeat for each integration point to obtain the stiffness matrix of each element
- Impose volume constancy constraint on stiffness matrix

Versions of ALPID. Many versions of ALPID are available. ALPID 1.0 was validated for solving two-dimensional (2-D) plane-strain and axisymmetric problems related to metal forming under isothermal processing conditions. In ALPID 2.0, the rigid-viscoplastic formulation is coupled with a heat-transfer module to solve these problems under hot-working conditions (Ref 55). A new yield function was developed to enable the analysis of compressible materials (Ref 6). Based on these criteria and on plasticity theory developed for porous materials, ALPID 1.4 was developed to solve 2-D plane-strain and axisymmetric problems related to P/M metal-forming operations under isothermal processing conditions (Ref 56).

ALPID-3D was also developed and validated to solve problems involving complex material flow in three directions (Ref 57). These finite-element programs are being used for simulating such metal-forming operations as forging, extrusion, and rolling and for predicting lap or folding formation in rib-web forging, center burst formation in extrusion, and the occurrence of plastic instability (such as adiabatic shearing) in rolling. Some industries are using these tools for press selection, for optimizing process and die design, and for ensuring die filling in 2-D axisymmetric and plane-strain problems.

Attempts at developing process control algorithms have been successful (Ref 58). Optimal design procedures have been introduced as nonholonomic constraints for the development of control algorithms to ensure that, during spike forging, the control elements keep the deforming material within the allowable temperature and strain rate range as specified by the dynamic material modeling approach. Therefore, the technical feasibility of using analytical techniques to solve process design and control problems in metal forging has been established.

ALPID Results: Field Variables and Flow Lines. The available results of finite-element analysis include the following field variables:

- Stress distribution
- Strain distribution
- Strain rate distribution
- Velocity distribution
- Displacement as represented by FEM mesh distortion

These results are normally too tedious to evaluate as printed numbers; graphical representation by way of contour plots, velocity vector plots, and mesh distortion plots are usually used. The resolution (accuracy) with which the field variables are computed are a direct function of the FEM mesh density. In addition to the above field variables, the load requirement as a function of stroke is also computed, and a graphical dis la of results is available.

The mesh distortion results from ALPID can be used to obtain some estimate of the expected orientation of flow lines (Fig. 11), but are not always adequate for two reasons. First, the grid distortion plots represent the FEM mesh distortion, not the actual flow lines. The original FEM mesh may have arbitrary topology, and this can result in distortion plots that have no relationship to flow lines. This technique is adequate only when the initial FEM mesh is uniform and suitably oriented. Second, even when the original FEM mesh has suitable orientation and topology to represent flow lines, if remeshing is performed in the course of simulation, all flow line orientation developed prior to remeshing is lost.

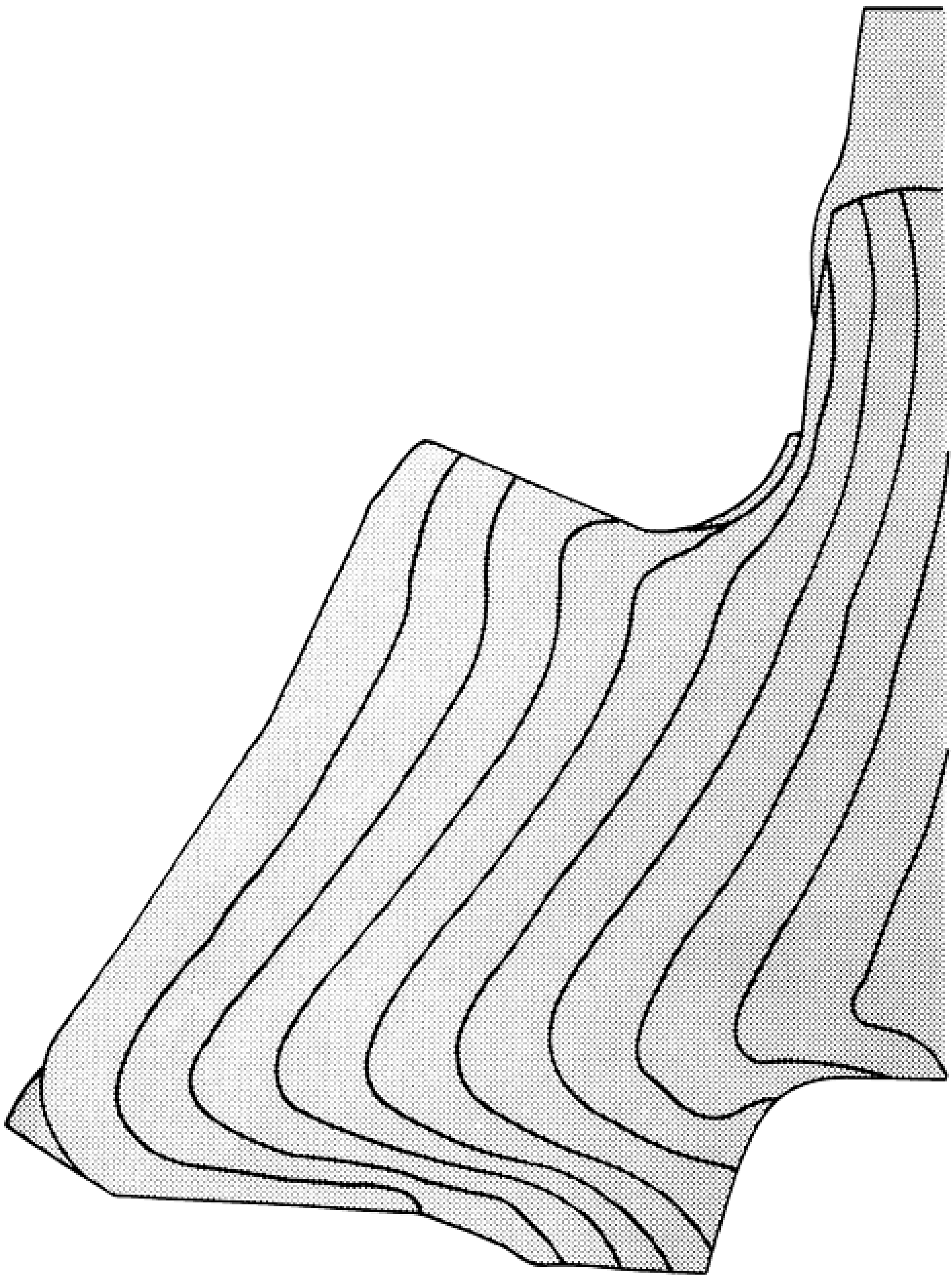
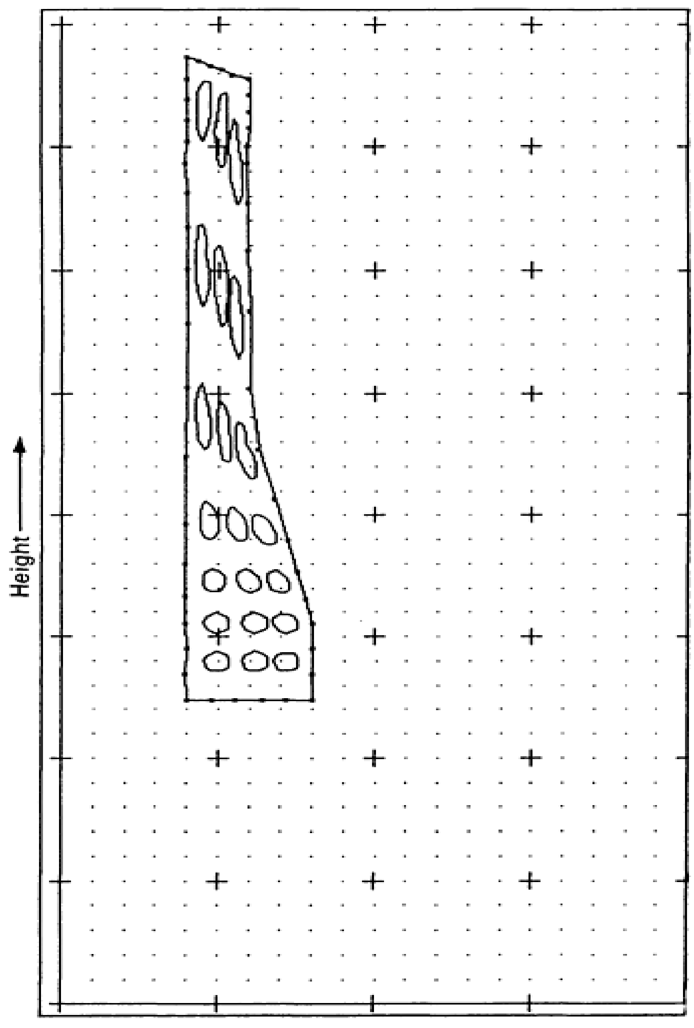


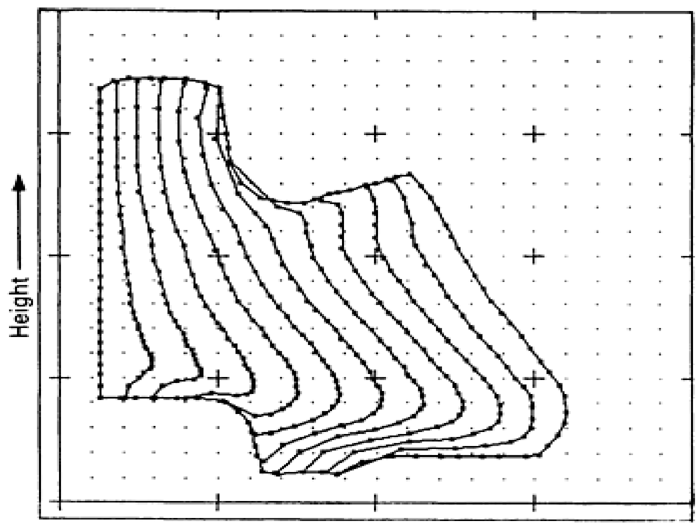
Fig. 11 Typical flow lines in a forging

An ALPID postprocessing feature has recently been added for representing flow lines typically observed in worked metals (Ref 59). It is especially significant that the ability to develop the simulated flow lines can bridge remeshing operations. The addition of this feature offers two options to account for the cases typically encountered, as follows.

No Preexisting Flow Lines in Billet. When the ALPID simulation is for a primary working operation, no prior flow lines exist. The technique offered here is to place circles on the simulation cross section and then provide the ability to follow the distortion of these circles into flow lines. An example of this technique is given in Fig. 12(a).



(a)



(b)

Fig. 12 ALPID simulations of metal flow after forming. (a) Flow lines in extrusion of a cast billet; distance between consecutive dots is 3.18 mm (0.125 in.). (b) Flow lines in a disk forging; distance between consecutive dots is 19.7 mm (0.775 in.).

Preexisting Flow Lines in Billet. When the ALPID simulation is for an operation using an already worked billet or a forged preform, prior flow lines exist. In this case, the technique offered is to provide flow lines as input to the simulation and then to follow these flow lines through the course of the simulation. An example of this technique is given in Fig. 12(b).

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Example Applications of ALPID

Spike Forging. In spike forging, a cylindrical billet is forged in an impression die containing a central cavity (Ref 60). The deformation characteristics of spike forging are such that the portion of the material near the outside diameter flows radially, while the portion near the center of the top of the surface is extruded, forming a spike.

In this application, the primary objective was to determine the conditions that would allow complete die filling in spike forging. For this purpose, the constitutive equations for the $\alpha+\beta$ Ti-6Al-2Sn-4Zr-2Mo microstructure at 954 °C (1750 °F), which exhibits strain rate hardening, were selected for the analysis. The die shape and the specimen with the selected mesh pattern are illustrated in Fig. 13.

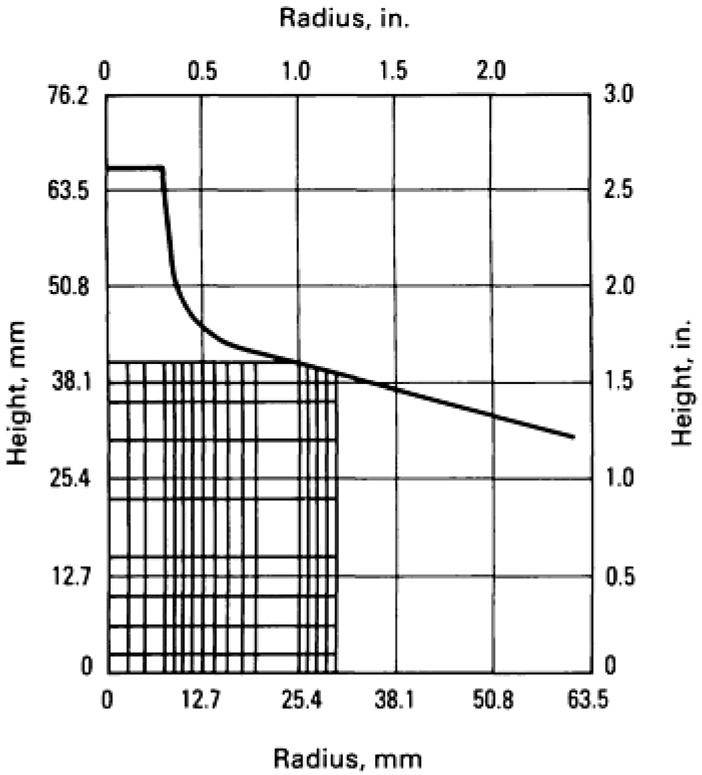


Fig. 13 Die shape and specimen showing mesh pattern selected for ALPID simulation of spike forging. See also Fig. 14.

Simulations were performed for frictional conditions of friction parameter *m*' = 0 to 0.8 in steps of 0.2. However, the results are presented for only two frictional conditions, namely, *m*' = 0.2 and 0.6. Figures 14(a) and 14(b) show the flow pattern obtained for various steps in the operation at these two frictional conditions. Complete die filling was obtained without remeshing when a frictional condition of *m*' = 0.6 was used. The total average deformation undergone by the billet during die filling was 67%. The lateral flow and the radial flow were found to be sensitive to frictional conditions--the higher the friction, the higher the spike height for the amount of ram displacement.

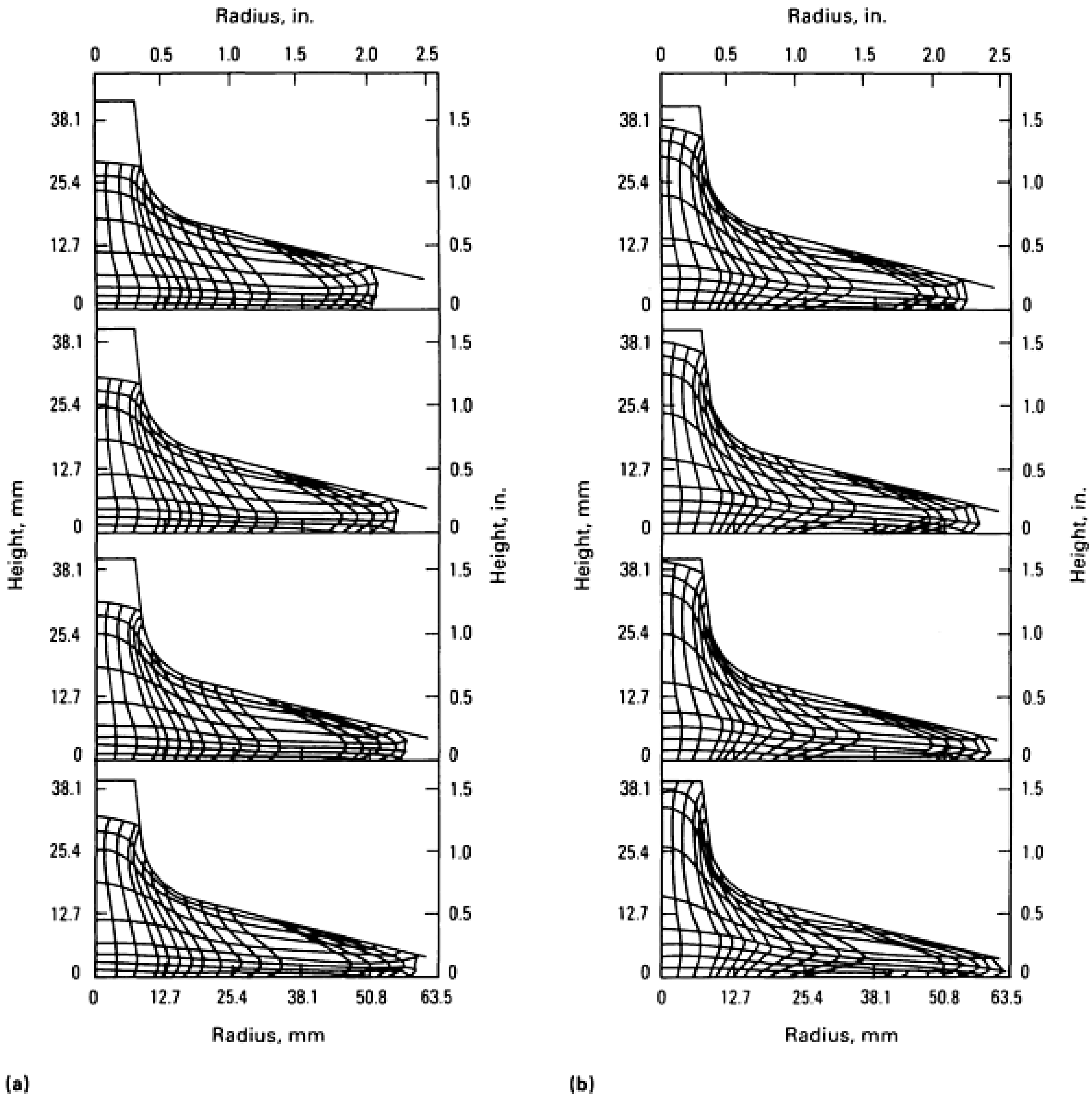


Fig. 14 Simulations of flow patterns during spike forging at reductions of (top to bottom) 64, 65, 66, and 67% and two different friction conditions. (a) Friction parameter, *m*' = 0.2. (b) *m*' = 0.6

The simulation of a rib-web forging of titanium alloy is shown in Fig. 15 and was made to investigate the possibility of defect generation due to improper die design (Ref 61). These isothermal predictions substantiated the intuition and experience of titanium forging designers. The defect that would have formed in the actual forging was a lap. Other simulations for the nonisothermal forging of the same titanium alloy showed that temperature gradients worsened the problem, indicating that this die design was very poor.

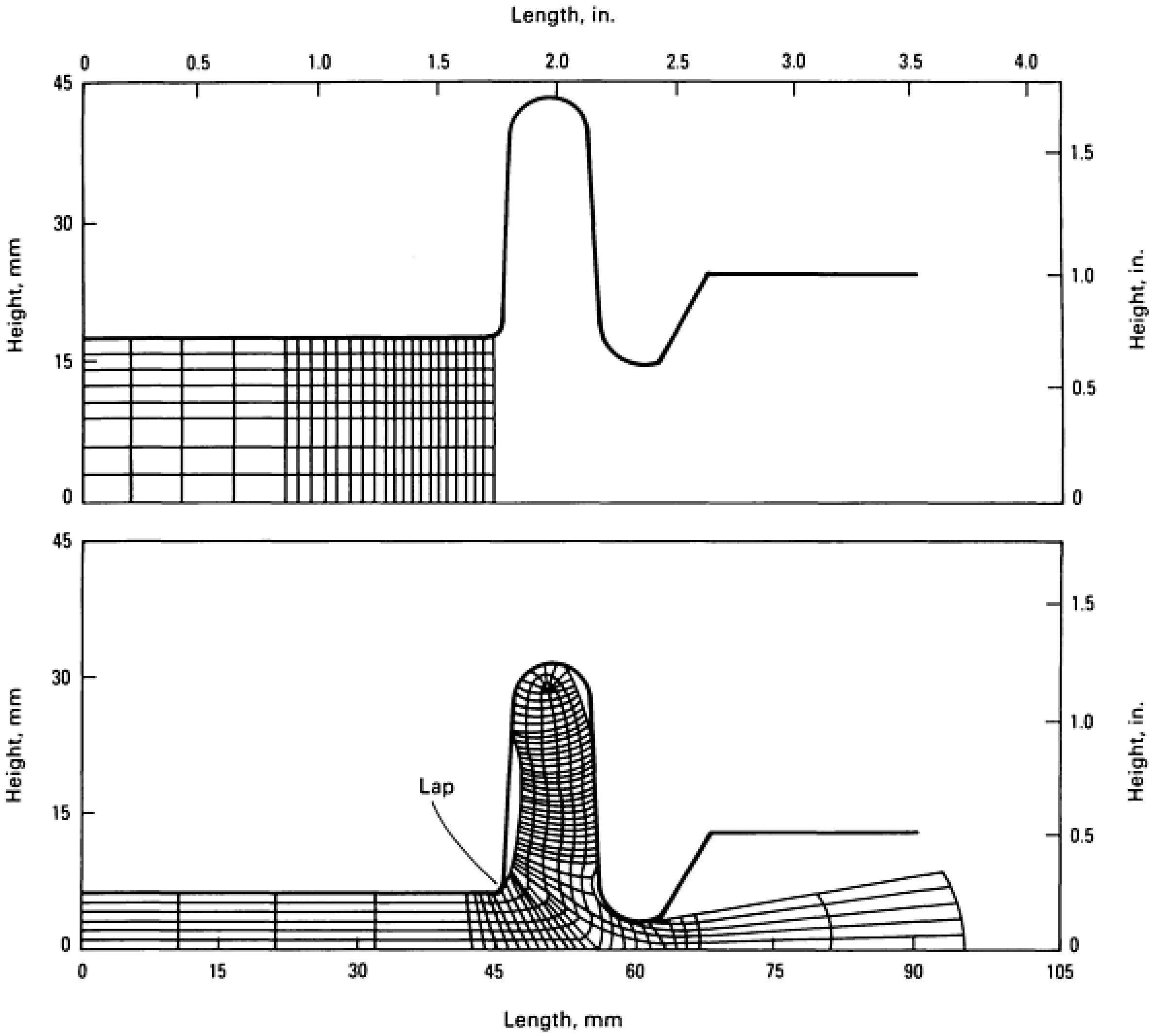


Fig. 15 ALPID simulation of metal flow during the forging of a rib-web titanium part. In this case, ALPID correctly predicted formation of a forging defect (lap).

For a truck-gear forging, the nature of the grid distortion in the final die position is shown in Fig. 16 (Ref 61, 62). The significance of this simulation is that various flash geometries can be simulated for optimizing material flow and obtaining complete filling. This is an axisymmetric forging of a steel alloy; it should be noted that the flash at the part centerline (left-hand side in Fig. 16) has buckled, as observed in the actual forging, because of the material from opposite sides meeting along the central axis.

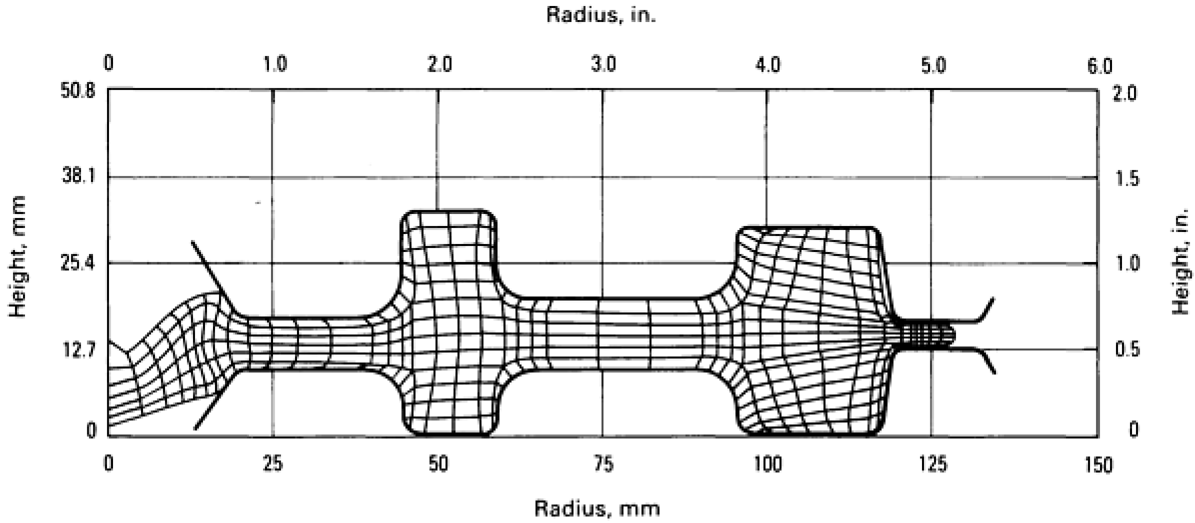


Fig. 16 ALPID simulation of final die position in the forging of a truck gear showing bending of flash that occurs at the center of the part (left)

Three-Dimensional Wedge Deformation by Forging. Metal flow during a three-dimensional (3-D) deformation process is illustrated in Fig. 17 (Ref 61, 62). The 3-D shape changes that occur during forging were accurately predicted by the ALPID model. Careful examination of the deformation steps reveals that the specimen bulges at the anticipated locations and rotates at the thin side of the tapered wedge. This simulation represents one of the first attempts at modeling 3-D metal flow during open-die design.

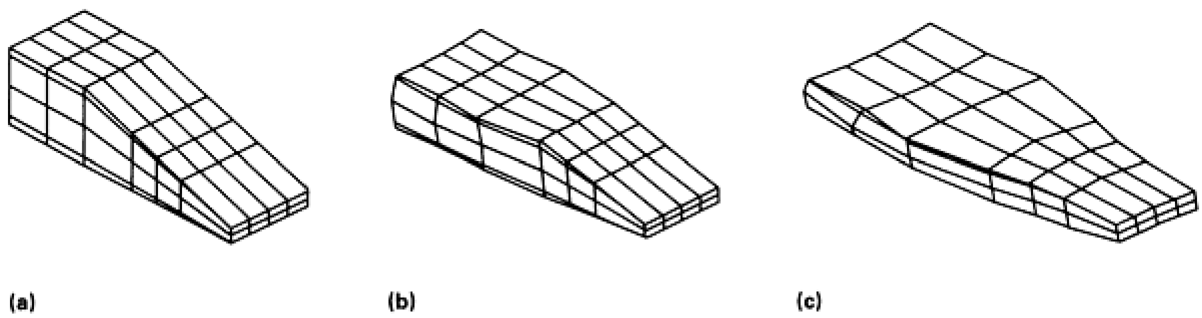


Fig. 17 Wedge formation simulated by 3-D finite-element modeling. Friction parameter *m*' = 0.4. (a) 0% reduction. (b) 30% reduction. (c) 60% reduction.

Powder Consolidation by Double-end Pressing. Double-end pressing (Fig. 18a) is generally used for the consolidation of P/M forging preforms. To obtain quality preforms, density distributions along the length and across the cross section must be controlled during consolidation. For this reason, ALPID 1.4 was utilized to predict density distribution in this application (Fig. 18b).

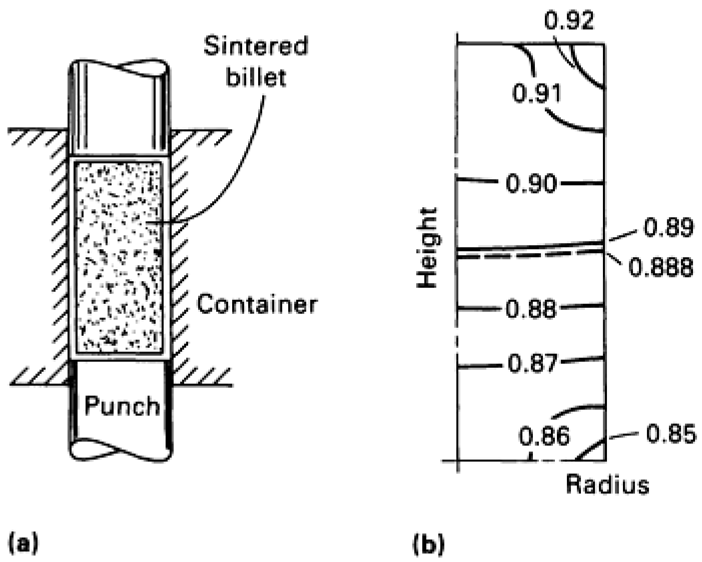


Fig. 18 Schematic showing double-end pressing process for the forging of P/M materials (a) and ALPID-predicted relative density distribution in the forged billet (b). Initial relative density was 0.8; agreement with actual density measurement (dashed line) was excellent.

Only one-fourth of the cylinder is shown in Fig. 18(b) because of the geometrical symmetry of the process. The excellent agreement between predicted and observed results is worth noting. Figure 18(b) illustrates the density distribution predicted by ALPID (solid contour lines), along with an experimental validation of the predictions (dashed contour lines).

Optimization of Ti-6Al-2Sn-4Zr-2Mo Disk-Forming Process. The approach of optimizing deformation processes by using material-stability-preserving maps is demonstrated in the design of a Ti-6Al-2Sn-4Zr-2Mo disk-forging process (Ref 58). The closed-die isothermal forging of this disk was previously analyzed using ALPID, with emphasis on the prediction of metal flow near the flash (Ref 63). Because of symmetry, it is sufficient to analyze only one quarter of the cross section of the forging. The predicted nodal-point velocity plots for 48, 68, and 72.1% reductions in height are shown in Fig. 19. The plots clearly show the transition in metal flow from primarily upsetting deformation to die cavity filling to the final stage of forming flash.

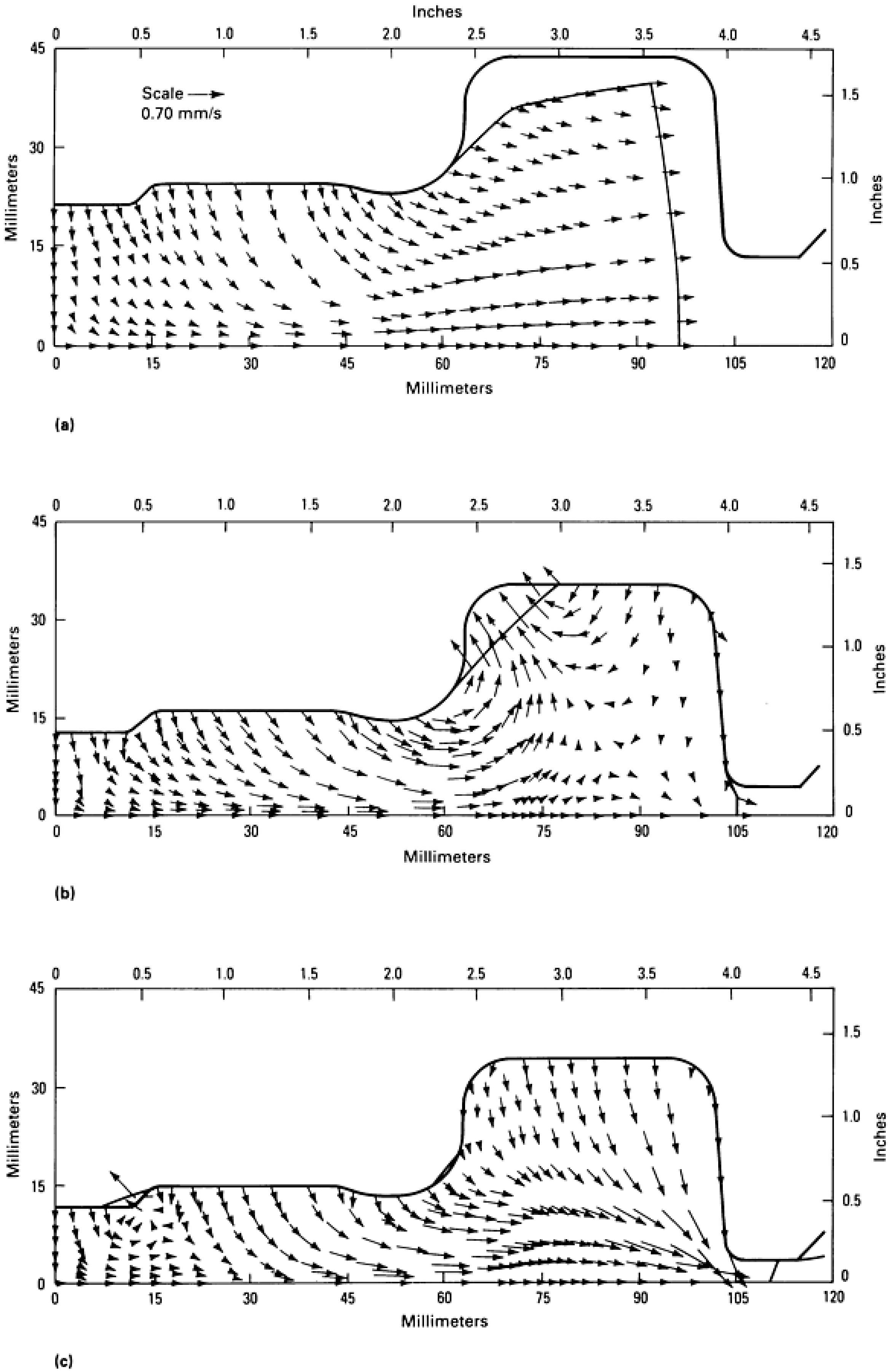
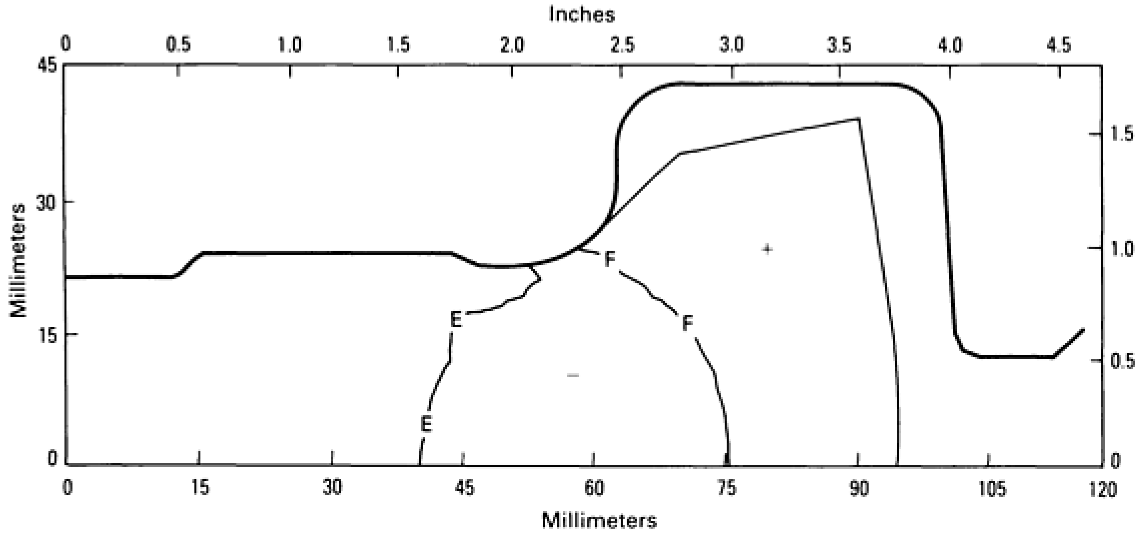


Fig. 19 Nodal-point velocity plots of disk-forging simulation with constant die velocity at 48% (a), 68% (b),

and 72.1 % (c) reductions in height. See also Fig. 20, 21, and 22.

In the current study, the process design goal was to fill the die cavity completely and to avoid the possibility of creating defects that are governed by the state of stress in the forging process. The state of stress is described here as the stress rate path, which is defined as the ratio of the mean hydrostatic stress to the effective stress $\overline{\sigma}$. This is a fundamental quantity in plasticity theory because a material changes shape according to the applied effective stress rate path. When this ratio is positive, a tensile state of stress exists; when it is negative, a compressive state of stress exists. The magnitude of the ratio describes the approximate state of stress. For example, when $\sigma_m / \overline{\sigma} = +\frac{1}{3}$, the stress state is uniaxial tension; when $\sigma_m / \overline{\sigma} = -\frac{2}{3}$, the deformation state of ideal axisymmetric extrusion exists.

The distributions of the stress rate path ratio for the disk simulation at 48, 68, and 72.1% reductions in height are shown in Fig. 20. At 48% reduction (Fig. 20a), the bore region of the disk has an approximate value of -1.0, and the rim region has positive values that reach a maximum of +0.37.


Inches
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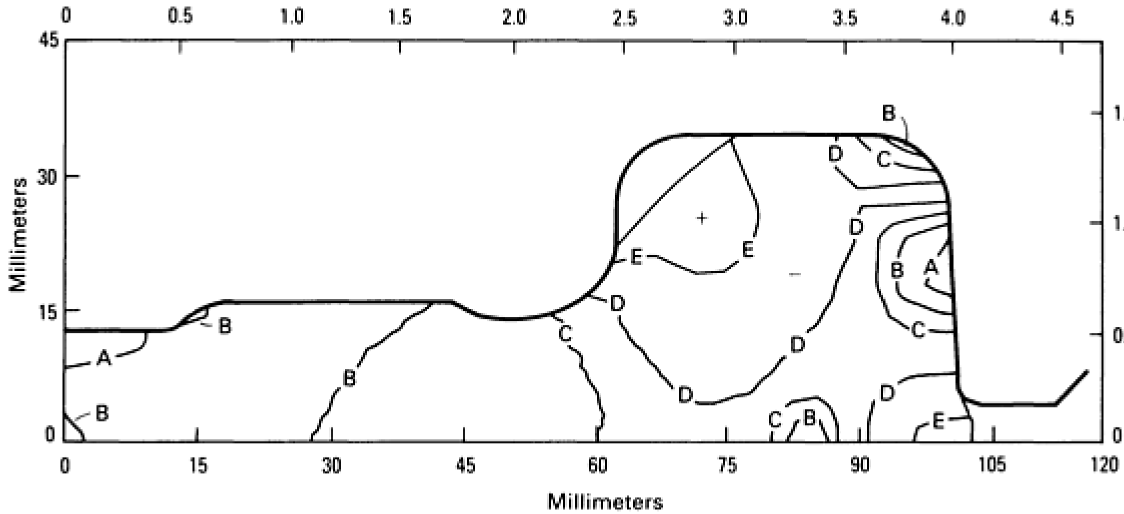
F

(a)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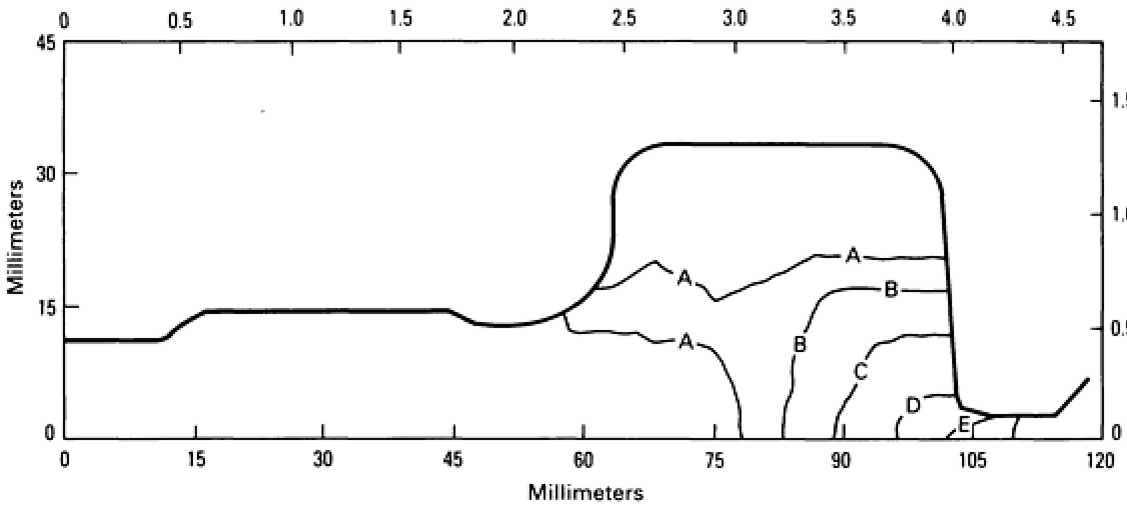
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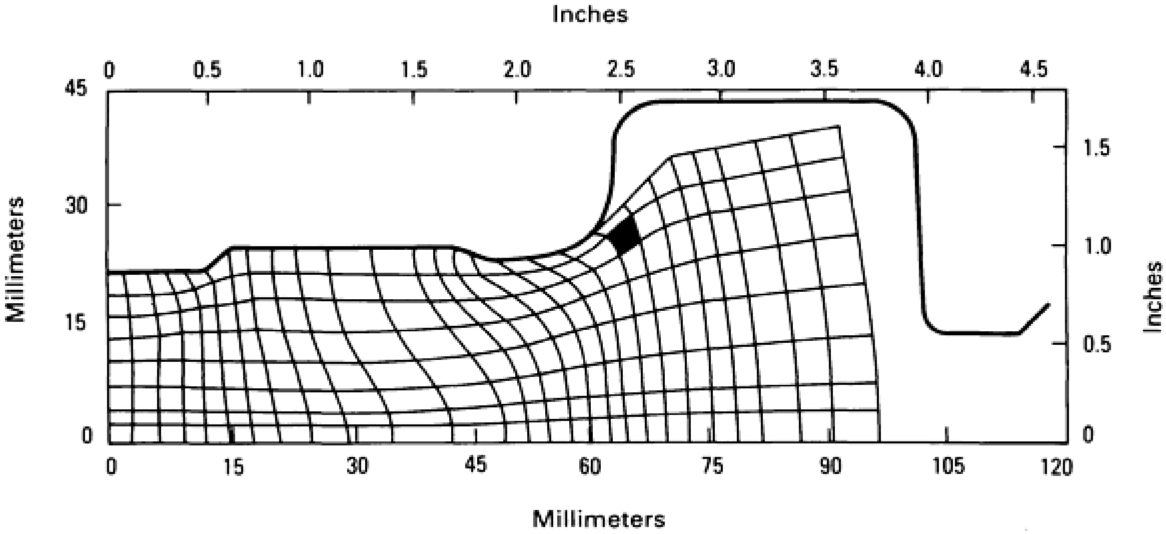
(c)

Fig. 20 Effective strain rate-path ratio contour plots of disk-forging simulation at 48% (a), 68.8% (b), and 72.1% (c) reductions in height. See also Fig. 19, 21, and 22.

The transition from compression to tension (neutral surface) is controlled by the die radii. As the workpiece touches the outside die wall and continues to fill the die cavity, the tensile state of stress reduces to a small region that is filling the inside die corner. This situation presents a potential problem, namely, that of producing defects such as cavities or cracks in the finished forging. Therefore, enhanced metal flow and good intrinsic workability of the workpiece are needed in this location at the final stages of die filling in order to relax the stresses and thus avoid the possibility of defect formation.

Based on the stability map for Ti-6Al-2Sn-4Zr-2Mo with an initial β (Widmanstätten) microstructure, a strain rate of 3×10^{-3} s^{-1} and a temperature of 926 °C (1700 °F) were determined as the optimum conditions for this material and application. In optimizing the ALPID simulation of the isothermal disk-forging process, the die velocity was changed to maintain an effective strain rate of 3×10^{-3} s^{-1} with a 0.1% tolerance limit in Element No. 47, which is positioned near the inside die corner (Fig. 21) where final die filling is critical.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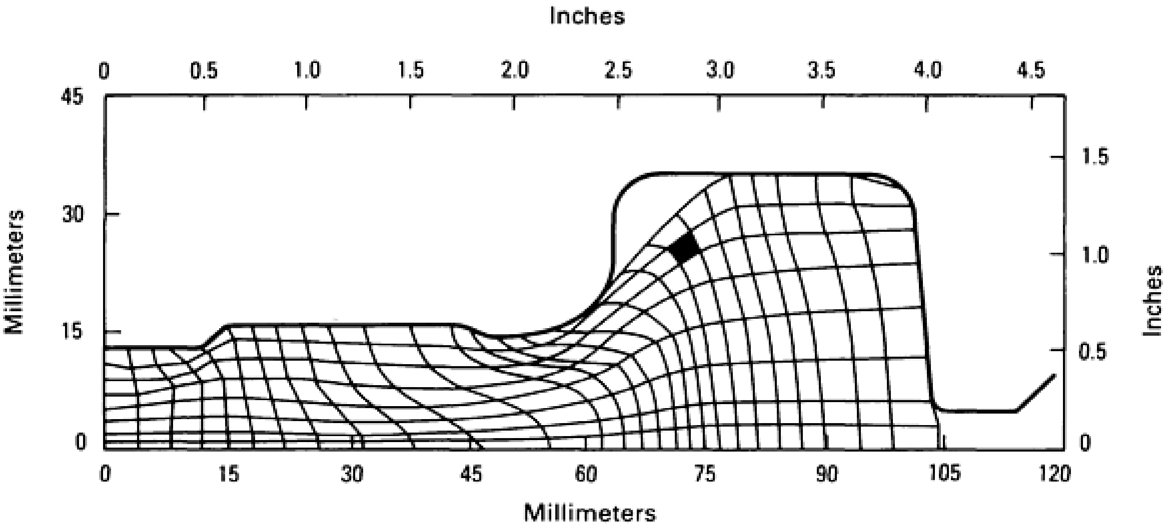
Millimeters
45 30 15 0
0 15 30 45 60 75 90 105 120
Inches
1.5 1.0 0.5 0
Millimeters

(a)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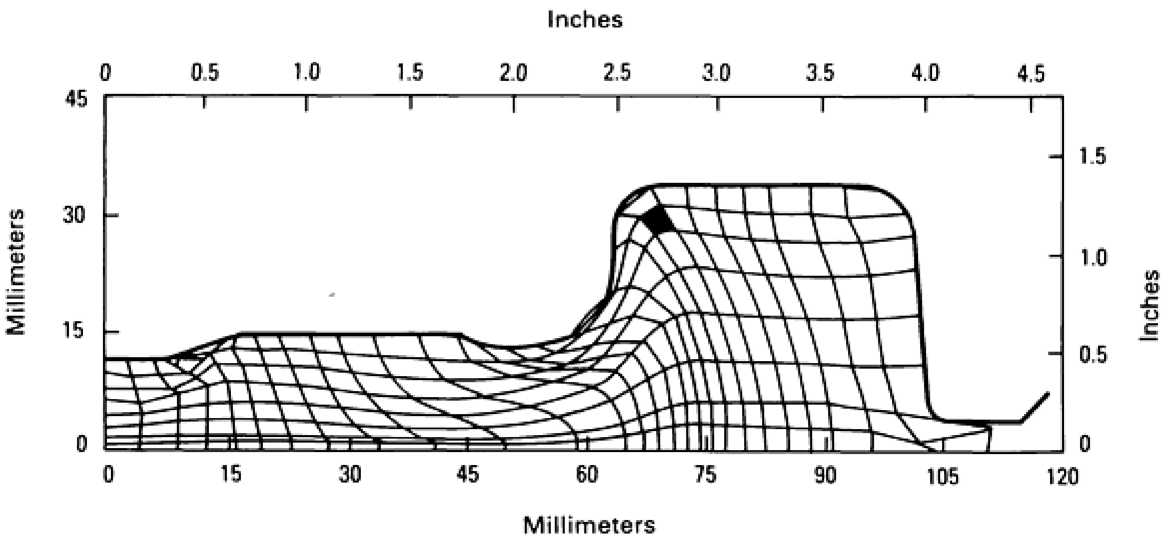
Millimeters

(b)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Millimeters
45 30 15 0
0 15 30 45 60 75 90 105 120
Inches
1.5 1.0 0.5 0
Millimeters

(c)

Fig. 21 Grid distortion plots of disk-forging simulation with constant die velocity at 48% (a), 68% (b), and 72.1% (c) reductions in height. The black grid is Element No. 47; see text for explanation. See also Fig. 19, 20, and 22.

The optimal die velocity as a function of stroke is shown in Fig. 22, and a total deformation time of 413.4 s was required for this type of process control. The die velocity gradually increases from 0.04 mm/s to a speed of 0.20 mm/s at a stroke of 17 mm; the die velocity remains constant at about 0.20 mm/s until the workpiece touches the die wall at a stroke of 26.5 mm. At this point, the die velocity decreases drastically to a speed of 0.01 mm/s at the final stroke. The time-varying die velocity results of this simulation would provide the enhanced workability and metal flow required for the workpiece to flow around the die corner radii, to fill the inside die corner, and to fill the die cavity completely. Similarly, for complicated die geometries, it is possible to vary die velocity as a function of time and to optimize the material flow to achieve a given range of strain rate.

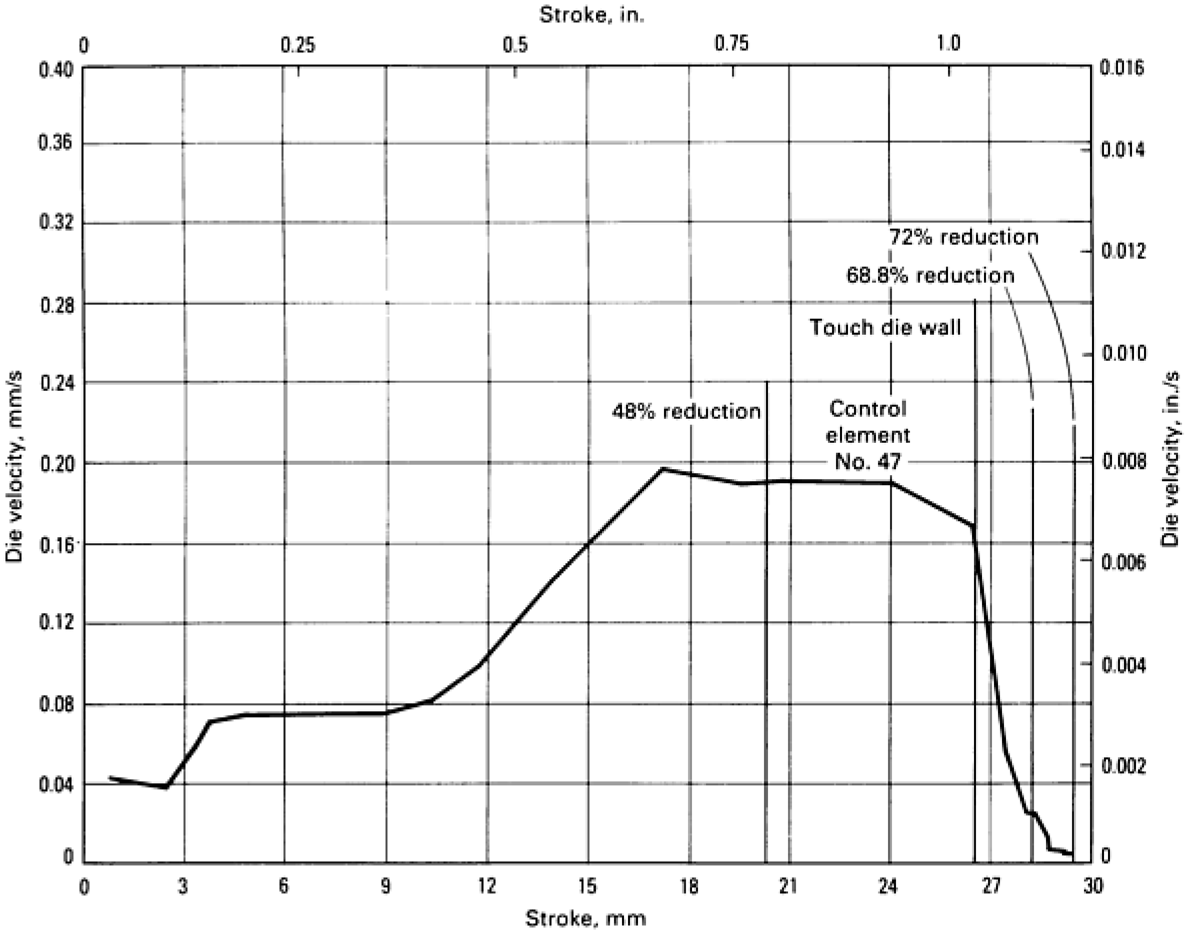

Fig. 22 Die velocity versus stroke curve for disk-forging simulation. See also Fig. 19, 20, and 21.

It is currently feasible to preprogram large hydraulic forging presses for constant or variable die velocity over a given load range (Ref 64, 65). Feedback control systems can also be installed for regulating the ram speed with respect to forging pressure or product temperature. Continuous control of the ram speed requires a combination of direct pumping and accumulator drive systems in order to maintain the time-dependent power requirements of the forging process. The accumulator drive system provides a higher penetration speed, but toward the end of the stroke, as the force required for forging increases, the ram speed and load available at the ram decrease. The direct-drive system delivers the maximum available load during the entire ram stroke and thus provides the very high pressures required for final die filling. Therefore, the two drive systems are complementary, with the result being higher deformation rates and pressures. More information on drive systems for both hydraulic and mechanical forging presses is available in the article "Hammers and Presses for Forging" in this Volume.

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Modeling Techniques Used in Forging Process Design

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Physical Modeling

Whenever an analytical method for designing a physical system is devised, it is essential that its validity be tested by physical modeling. Such testing not only provides the feedback that aids in the development/refinement of the analytical model but also demonstrates the accuracy of the analytical predictions. Therefore, physical modeling provides greater confidence in the application of these analytical techniques toward the solution of actual problems.

Approaches

Two approaches are generally used in designing physical models. The first consists of simulating, as closely as possible, a production unit process on a laboratory scale and monitoring or controlling the important process parameters. The product resulting from such a test can then be analyzed to determine how closely its characteristics match those predicted by the analytical model. The second approach to physical modeling is to devise a laboratory test that is faster, easier, and less expensive than a subscale production process and yet provides an equally thorough test of the accuracy and robustness of the analytical model. Model materials and visioplasticity techniques are utilized in this approach for analyzing flow behavior.

Model Materials

Model materials such as Plasticine, wax, and polymers are used for physical simulation because:

- The load required for deformation is relatively small
- Observation of the deformation pattern is easy
- Test specimens are easy and inexpensive to make
- Inexpensive die materials such as wood and plastic can be used

- Experimental procedures are simple

Plasticine and wax are perhaps the most commonly used model materials; they are discussed in more detail below.

Plasticine is widely used in qualitative comparisons of different conditions in the hot forming of steels, especially in studies of flow patterns in extrusion, rolling, and forging and in studies of shape profile in rolling and upsetting (Ref 66, 67). There are two methods for investigating the flow pattern with Plasticine. One method is to paint a grid on the surface of the specimen with a stamp. In this method, the investigation is reduced to a two-dimensional problem, the painted grid is visible throughout the experiment, and the grid is photographed through an acrylic plate. The second method is to build up the specimen with layers of different-colored Plasticines. In this method, flow patterns cannot be observed continuously. The strain rate sensitivity factor, *m*, of Plasticine is approximately 0.06; therefore, Plasticine cannot be used as the model material for the physical simulation of the isothermal forging of superplastic alloys.

Wax has also been used, and the flow properties of many wax-base model materials have been measured experimentally (Ref 68, 69). The *m* values of wax are higher than those of Plasticine. Recently, wax has been used in studies of the hot isothermal forging of superplastic alloys. However, because *m* values depend significantly on temperature, the experimental conditions must be controlled exactly. In addition, many investigators are not yet experienced in handling the wax. Data are limited on the properties of wax compared to those of Plasticine.

Strain-Rate Sensitive Model Materials. The *m* values of the Plasticine-base model materials were changed by the addition of kaolin, petroleum jelly, lanolin, and resin to Plasticine (Ref 70). A resin that was solid at room temperature was produced. Petroleum jelly was generally used when the flow stress of Plasticine had to be decreased, and kaolin was used when the flow stress had to be increased. In this case, the proportion of petroleum jelly or kaolin was up to 5%. In the mixtures of resin/lanolin and petroleum jelly or lanolin, the proportion of resin/lanolin was up to 10%.

Table 3 shows the *m* values at a compression strain of 20% and the composition of materials indicated by weight ratio. The flow stress in plane compression at two constant cross-head velocities was measured by using an electronically controlled tensile-testing machine. Cross-head velocities of 10 and 100 mm/min (0.4 and 4 in./min) were used. The *m* values were calculated from two flow stresses at the two velocities at a strain of 0.2. The dimensions of the compression specimen were 20 × 15 × 30 mm (0.8 × 0.6 × 1.2 in.). The specimens were kept for 2 days in a thermostatic oven controlled at 20 °C (70 °F).

Table 3 *m* values of mixtures of Plasticine and various additives

Composition of mixture, weight ratio						*m* value
Plasticine	**Petroleum jelly**	**Kaolin**	**Lanolin**	**Resin + lanolin**[(a)]	**Resin + lanolin**[(b)]	
100	. . .	. . .	. . .	. . .	. . .	**0.05**
100	2	. . .	. . .	. . .	. . .	**0.07**
100	. . .	3	. . .	. . .	. . .	**0.03**
100	. . .	. . .	2	. . .	. . .	**0.07**
100	5	. . .	. . .	6	. . .	**0.15**
100	5	. . .	. . .	5	. . .	**0.09**

100	2	. . .	. . .	3	. . .	**0.09**
100	. . .	. . .	5	. . .	5	**0.31**
100	. . .	. . .	10	. . .	10	**0.43**
100	13	. . .	. . .	. . .	10	**0.14**
100	**2**	**. . .**	**10**	**. . .**	**10**	**0.40**

Source: Ref 71

(a) Ratio of resin to lanolin 10 to 4.

(b) Ratio of resin to lanolin 10 to 5.

The *m* value of Plasticine was 0.05. The *m* value of the mixture of Plasticine and kaolin was 0.03, and the flow stress increased. The *m* value of the mixtures of Plasticine and petroleum jelly or lanolin increased slightly, and the flow stress decreased. The *m* values increased significantly with the addition of resin to Plasticine. The highest *m* values were obtained in mixtures containing both resin and lanolin. Consequently, mixtures of Plasticine, resin, and lanolin should be valid model materials for the isothermal forging of superplastic alloys.

Aluminum as a Model Material. Aluminum alloys 1100-O and 6061-O are also used for physical modeling. Grids can be easily made on the surface of the sample by etching or engraving. Excellent results have been obtained when these materials have been used for the simulation of aluminum precision forging processes.

Computation of Strain for Two-Dimensional Flow

To analyze the metal flow in deformation processing, it is necessary to determine the distribution of the effective strain values in the deformed body. The formulas defining these strains are derived in terms of the position of an element before and after deformation with reference to some coordinate system. Therefore, the strains are computed by measuring the deformation of some geometrical pattern using a grid mesh or an array of circles. Choice of the pattern depends on such factors as the type and accuracy of the information sought, the material, and the extent of specimen deformation.

In most commercial metal-forming processes, the resultant deformation may not be homogeneous. Deformation in the present case, however, can be assumed to be homogeneous because the deformed material can be divided into a number of small regions of homogeneous deformation. The most common method used to compute strains, called visioplasticity, involves establishing a velocity vector field to obtain strains by the grid method (Ref 72). The method is tedious and time consuming, and its accuracy depends on the computation of the velocity field. The technique is exact only in an engineering sense. It cannot be used for the case of unsymmetrical deformation, such as the wedge test, where a reference coordinate system cannot be established. Other methods involving the measurement of the dimensions of the deformed and undeformed grids are more appropriate for this case, because they are direct and easier to use.

A method has recently been developed for computing strains in deformation processing from the dimensions of the deformed and undeformed grid mesh (Ref 73). In this method, the deformation of a linear element in a small quadrilateral block is expressed in terms of normal and shear strains. Because actual strains in deformation processing are large, no limitations are applied to their magnitude, and the equations for strain are developed in terms of coordinate displacement.

Consider an element *OABC* (Fig. 23), which, after plane-strain deformation, assumes the shape of *O'A'B'C'*.Let l_1, m_1 and l_2, m_2 be the direction cosines of two line segments *OB* and *OA*, respectively. The lengths *OB* and *OA* can be computed from the digitized data of the deformed and undeformed grids. The strain parameter in the *x* direction e_{xx}, can then be obtained from a quadratic equation of the form:

$$A' e_{xx}^2 + B' e_{xx} + D' = 0 \quad \textbf{(Eq 56)}$$

where A', B', and D' are related to the lengths of the deformed and undeformed line segments and the direction cosines of the undeformed segments. All necessary relationships are given below:

$$e_{xx} = \frac{\partial u}{\partial x} + \frac{1}{2}\left\{\left(\frac{\partial u}{\partial x}\right)^2 + \left(\frac{\partial v}{\partial x}\right)^2 + \left(\frac{\partial w}{\partial x}\right)^2\right\} \quad \textbf{(Eq 57)}$$

$$A' = A_1C_2 - A_2C_1 \quad \textbf{(Eq 58)}$$

$$B' = B_1C_2 - B_2C_1 \quad \textbf{(Eq 59)}$$

$$D' = D_1C_2 - D_2C_1 \quad \textbf{(Eq 60)}$$

where:

$$A_1 = 4l_1^2;\ A_2 = 4l_2^2 \quad \textbf{(Eq 61)}$$

$$B_1 = 2\left[1 - \left(\frac{O'B'}{OB}\right)^2 + l_1^2 - m_1^2\right]; \quad B_2 = 2\left[1 - \left(\frac{O'A'}{OA}\right)^2 + l_2^2 - m_2^2\right] \quad \textbf{(Eq 62)}$$

$$C_1 = 2l_1m_2;\ C_2 = 4l_2m_2 \quad \textbf{(Eq 63)}$$

$$D_1 = 1 - \left(\frac{O'B'}{OB}\right)^2; \quad D_2 = 1 - \left(\frac{O'A'}{OA}\right)^2 \quad \textbf{(Eq 64)}$$

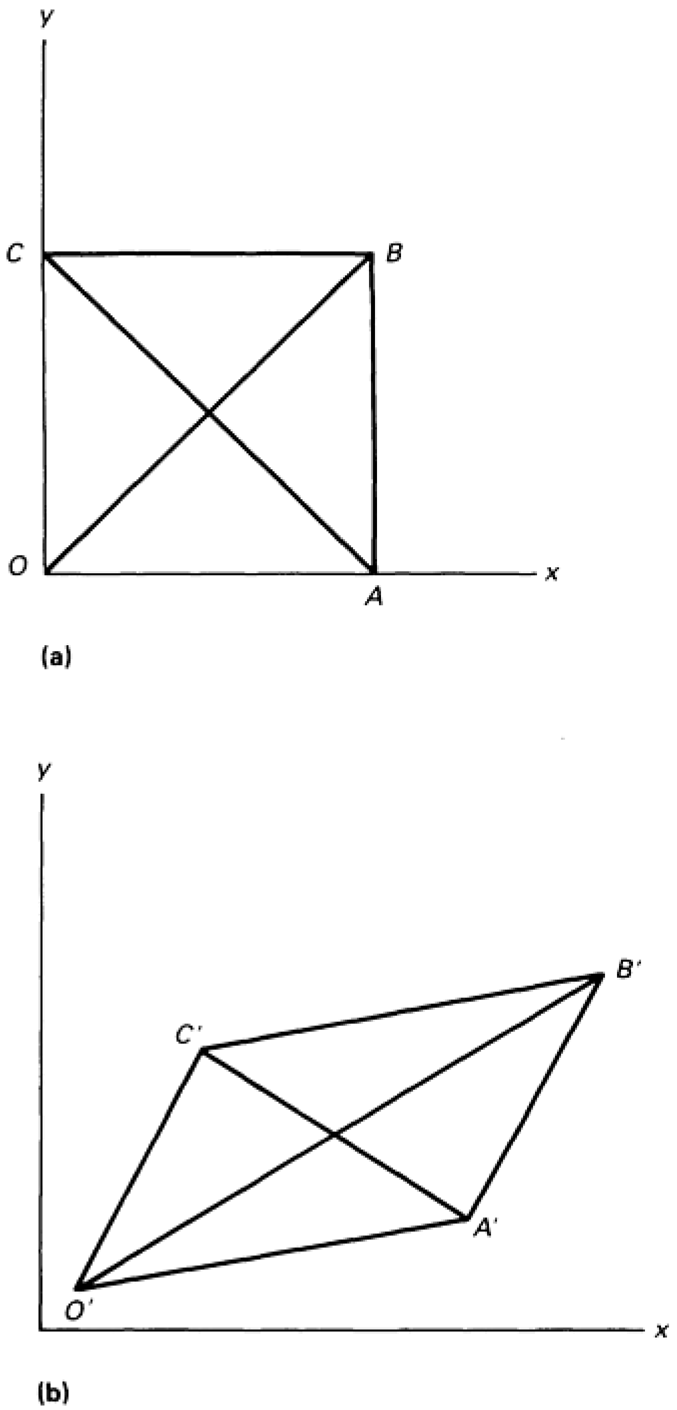

Fig. 23 Undeformed orthogonal grid (a) and deformed grid following homogeneous strain (b)

Shear strain e_{xy}, can be obtained by using Eq 65 or 66:

$$A_1 e_{xx}^2 + B_1 e_{xx} + C_1(1 + 2e_{xx})e_{sy}\ D_1 = 0 \quad \textbf{(Eq 65)}$$

$$A_2 e_{xx}^2 + B_2 e_{xx} + C_2(1 + 2e_{xx})e_{sy}\ D_2 = 0 \quad \textbf{(Eq 66)}$$

The true effective and principal strains are given as:

$$\bar{\epsilon} = \frac{2}{3}\sqrt{3\epsilon_x^2 + \frac{3}{4}e_{xy}^2} \quad \textbf{(Eq 67)}$$

$$e_{1,2} = \frac{e_x + e_y}{2} \pm \sqrt{\left(\frac{e_x - e_y}{2}\right)^2 + \frac{1}{4}(e_{xy})^2} \quad \textbf{(Eq 68)}$$

$$\epsilon_{1,2} = \ln(1 + e_{1,2}) \quad \textbf{(Eq 69)}$$

Equation 56 will result in two values of e_{xx}, one of which will be inadmissible and can therefore be discarded. The values of normal strains in the *x* and *y* directions are determined as follows:

$$e_x = \sqrt{1 + 2e_{xx}} - 1 \quad \textbf{(Eq 70)}$$

$$e_y = -\frac{e_x}{1 + e_x} \quad \textbf{(Eq 71)}$$

It should be pointed out that Eq 70 and 71 are based on the Lagrangian definition of strain and make no assumptions concerning the extent of deformation. The true effective strain is given by:

$$\bar{\epsilon} = \ln(1 + e) \quad \textbf{(Eq 72)}$$

where *e* is the effective engineering strain.

Therefore, the analysis of grids to compute strain variations on the meridian plane of the specimen involves three steps:

- Digitization of the undeformed and deformed grids
- Calculation of the effective strain using Eq 56, 57, 58, 59, 60, 61, 62, 63, 64, 65, 66, 67, 68, 69, 70, 71, and 72.
- Displays of the effective strain as contour plots

Development of Grid Mesh

Deformation in a body is characterized by the distribution of strain. The grid deformation method of strain measurement involves applying a grid mesh on the surface or on an interior plane of a specimen. The grid can be defined as an array of lines or dots that indicate points on the specimen, and it is usually rectangular, with dots or lines being repeated in two perpendicular directions. The distance between the discrete nodes on the grid before and after the deformation is measured, and the resulting data are analyzed. The grid method is an optical technique. Sighting of the grid is accomplished with or without photography. The accuracy of the grid depends on the precision with which sighting can be carried out.

Accuracy in the measurement of the grid does not lie in the precision of the measuring instrument but in the ability of the investigator to view the points clearly. If a dot or point (obtained by the intersection of two grid lines) is much larger than the resolution of the instrument, the resulting measurement will be inaccurate. The size of the point, therefore, plays an important role in the measuring process. In addition, a point under consideration should be clearly visible with the least amount of distortion before and after deformation has occurred.

A number of methods have been used in attempts to apply a suitable grid pattern to wedge-shaped specimens. These include photochemical etching and engraving techniques. The engraving techniques are accomplished by means of a computer numerical controlled engraver and are found to be accurate and appropriate. A typical specimen with engraved grid lines is shown in Fig. 24.

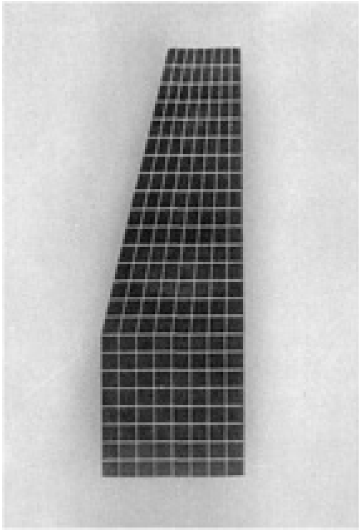

Fig. 24 Undeformed wedge specimen with grids engraved on meridian plane.

Tool Setup (Segmented Die Design) for Plane-Strain Wedge Testing

To analyze the two-dimensional flow of metal and to verify the ALPID code for complex deformation, wedge-shaped specimens with grids engraved on the meridian plane were compressed such that a range of strains varying from 0 to 0.75 was obtained. A one-piece channel-shaped die with a punch was designed and fabricated for a 90 Mg (200 kip) testing machine. A specimen of aluminum alloy 2024-O was compressed using this die. The compressed specimen could not be ejected undamaged from the die. The concept of segmented dies was therefore developed for convenient ejection of the specimen without damage. These dies (Fig. 25) consisted of several segments assembled to provide the same die cavity as that of a one-piece die, and they could be dismantled, after forging was complete, to eject the workpiece. The major advantage of segmented dies is that, through the use of different inserts, a new family of parts can be forged. The major disadvantage of these dies is that metal tends to extrude through the various joints of the die during metal deformation. The segments must therefore be held together with a pressure greater than the flow stress of the metal during deformation but must still be easy to dismantle after the forging process is complete.

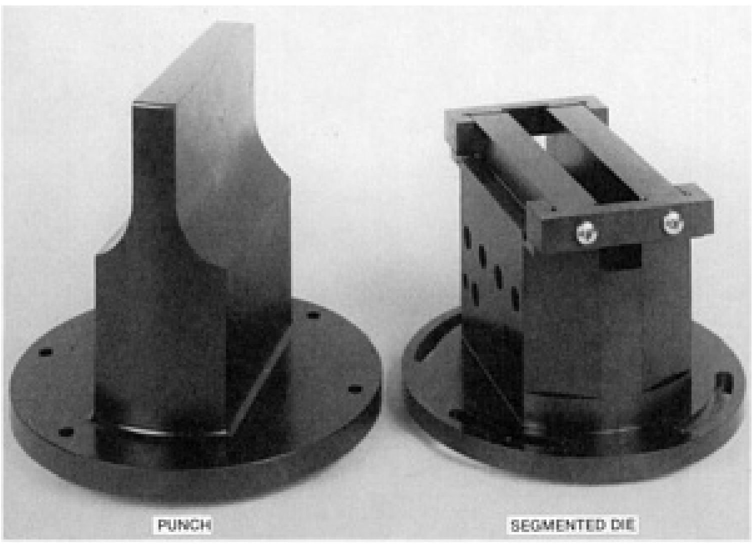

Fig. 25 Components of segmented dies for two-dimensional wedge testing

Example of Experimental Procedure

To verify ALPID for a variety of materials and deformation spectra, wedge specimens were machined from aluminum alloys 1000-F and 6061-T6. The specimens were annealed at 450 °C (840 °F) for 1 h. One 1100-O aluminum specimen was compressed to a deformation of 54.5%, and three 6061-O aluminum alloy specimens were compressed to height reductions of 14, 32, and 40% for this study.

Before the actual compression of the wedge specimen, the punch, die, and specimen surfaces were coated with a high-pressure molybdenum disulfide-base spray lubricant to minimize friction along the sliding interfaces. The specimen was then mounted in the die, and the die segments were fastened together with bolts and restrainers. The duration of the test was determined by the extent of deformation--typically 250 s for a 50% reduction in height (12.7 mm, or 0.5 in., ram displacement).

The deformed specimens were cleaned with soap and methanol to remove the thin film of lubricant and were then photographed. Because of the compression of the specimens, the horizontal grid lines became thin, and the vertical ones became thick. The thin lines could not be effectively resolved by the camera because of the reflection of light from the shiny surface of the aluminum specimen. The specimen surface was therefore coated with carbon black in order to print the deformed grid on photographic paper for digitizing. The computer files containing the deformed and undeformed digitized data were used by a computer program to obtain the effective strains at various nodes.

A number of compression tests were performed. Figure 26 shows a plot of ram travel versus compression load for the two materials. It can be seen that the load increases with the amount of deformation, but the rate of increase is larger for 6061 aluminum than for 1100 aluminum. The deformed grid meshes for the two materials for various amounts of deformation are shown in Fig. 27. The ALPID code was then used to generate the distorted grid for both materials for the amount of deformation used in the experimental tests. In Fig. 27, the flow patterns as depicted by the grid meshes are almost identical in all cases; the difference in flow behavior is attributed to the estimated value of the die/workpiece friction used to generate the ALPID simulations.

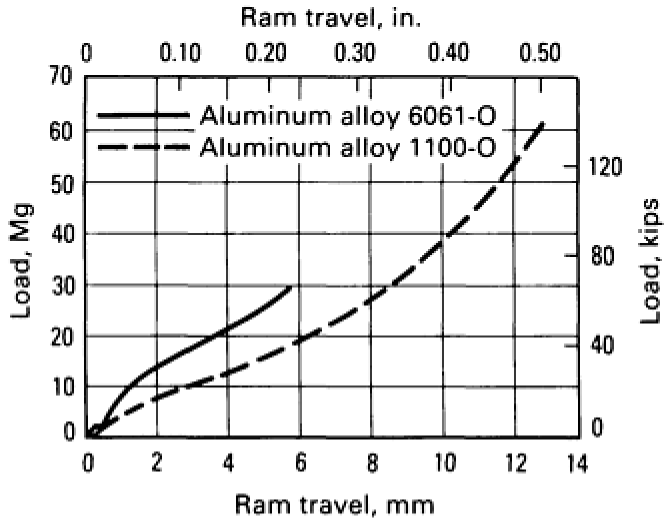

Fig. 26 Load versus ram travel for wedge specimens of aluminum alloys 1100-O and 6061-O

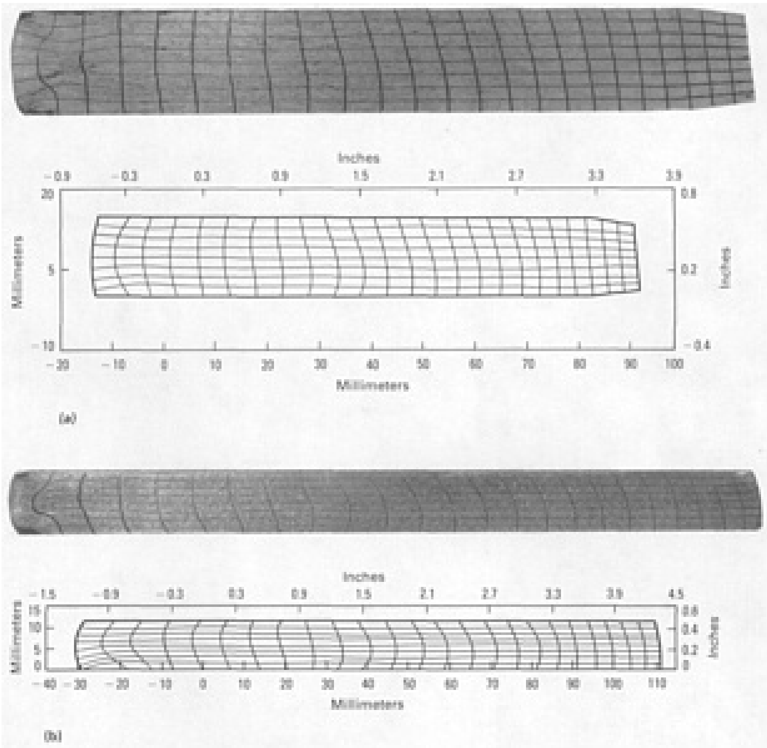

Fig. 27 Grid patterns showing metal flow in 6061-O (a) and 1100-O (b) aluminum alloy wedge specimens at 40% deformation. At top are experimental results; at bottom are ALPID simulations.

To quantify the verification of the ALPID code, the experimental and ALPID-generated values of effective true strain were used to plot the contour maps (Fig. 28). The ALPID-generated maps are very similar to the experimental ones.

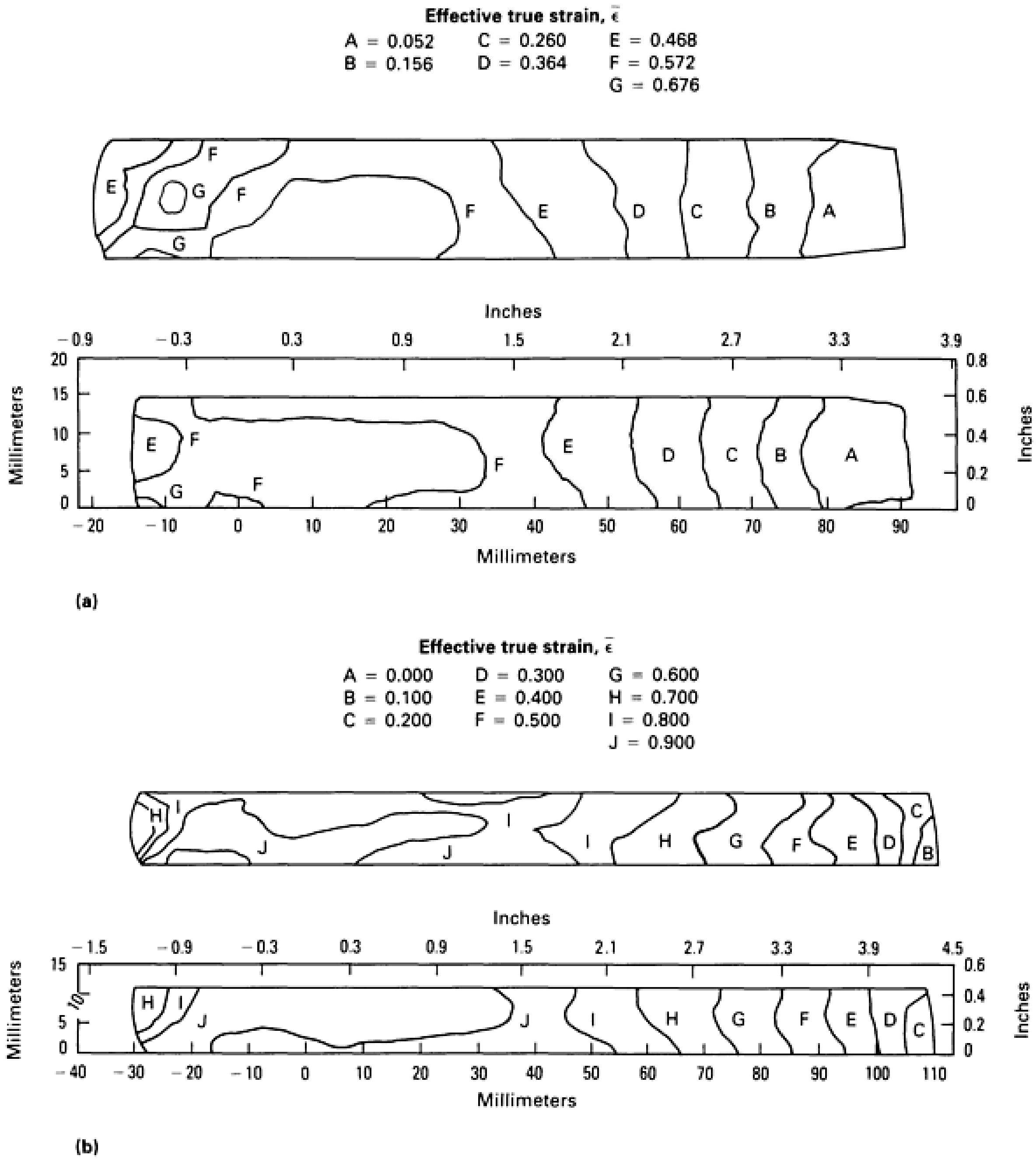

Fig. 28 Effective true strain contour maps for 6061-O (a) and 1100-O (b) aluminum alloy wedge specimens at 40% deformation. Experimental results are given at top; ALPID simulations are at bottom.

The reasons for the probable minor differences at some isolated locations in the experimental and ALPID-generated values of effective strains are:

- Error introduced in digitizing the deformed and undeformed grids on an electronic tablet
- Error in aligning the crosshairs of the cursor with the node points
- Error introduced in locating the grid node point precisely
- Distortion of the grid network during the printing process
- Undeformed grids not being identical on all specimens
- Imprecise constitutive equations for the materials under consideration
- Uncertainty in estimating the friction factor at the tool/specimen interface, which may vary from

specimen to specimen and with pressure and type of lubricant used

Development of Three-Dimensional Grid for Strain Calculation

To analyze the flow of material in deformation processing, it is necessary to determine the magnitude of strains in the deformed body. The formulas defining the strains are derived in terms of the position of an element before and after deformation with reference to a coordinate system. Therefore, the strains are computed by measuring the deformation of some geometrical pattern, for example, a grid mesh or an array of circles. Choice of the pattern depends on such factors as the type and accuracy of the information sought, the material, and the extent of specimen deformation.

Although in most plastic deformation processes the resulting strain is not homogeneous, the deformation will be assumed to be homogeneous. The rationale behind this assumption is that deformed material can be divided into numerous smaller regions where the deformation is homogeneous.

The Indirect Method. The development of a specimen shape to study metal flow in three dimensions is a tedious task. Therefore, an indirect approach has been developed (Ref 73). In this method, a rectangular block 19 × 12.7 × 6.4 mm (0.75 × 0.50 × 0.25 in.) thick with fine (0.38 mm, or 0.015 in., diam) through holes drilled and grid lines (2-2-2, 3-3-3, etc.) engraved at 1.27 mm (0.050 in.) intervals is used, as shown in Fig. 29. The holes are filled with 0.38 mm (0.015 in.) diam wires made of the same material. The specimen is then compressed between flat dies to a specified deformation, for example, 40%. The distance between the horizontal grids (1-1-1, 2-2-2, etc.) is measured to determine the strain in the *z* direction. The top surface (1-1-1-1) is photographed to obtain the flow pattern in the *x*-*y* plane. The specimen is machined to the level 2-2-2, and the hole pattern is photographed again. The process is repeated to the level 5-5-5. An indirect 3-D grid pattern is thus generated. This pattern is used to determine the true effective strain in each element.

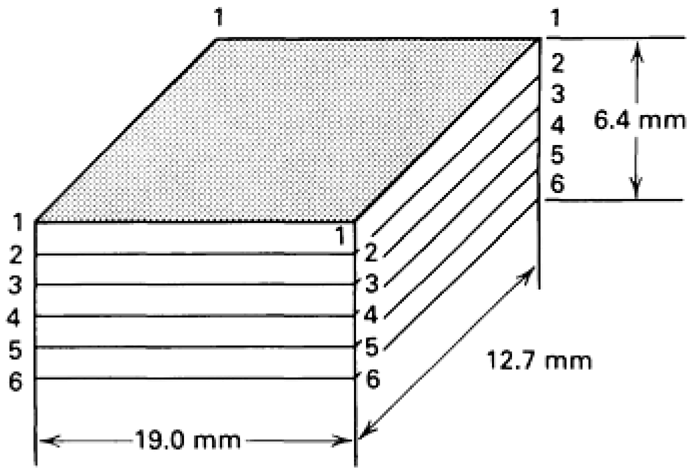

Fig. 29 Standard rectangular block specimen used to study three-dimensional material flow

The multicolored workpiece method was developed for determining the velocity field in true 3-D deformation (Ref 74). Figure 30 shows the workpiece. After every deformation, thin slices are removed from the workpiece, and each newly exposed surface is photographed.

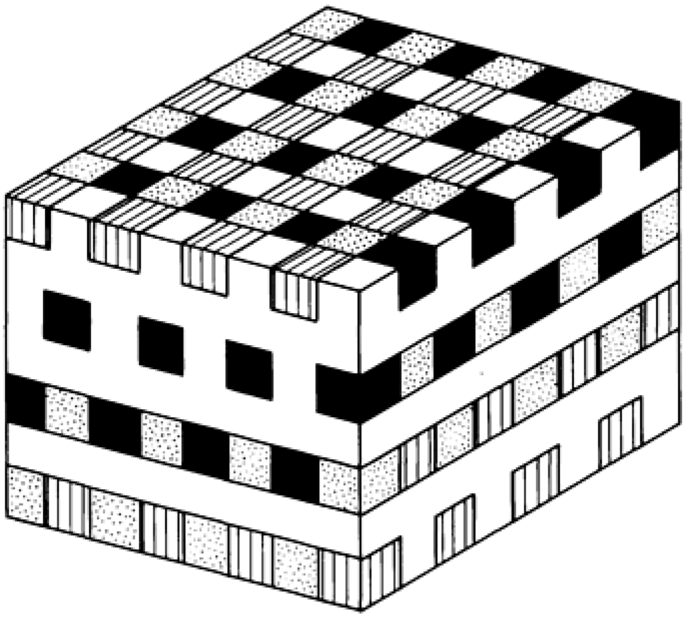

Fig. 30 Multicolored model material workpiece consisting of four-color checkerboard

Software has been developed that can compute the coordinates of each nodal point in the deformed workpiece from measurements of the above-mentioned photographs. The coordinates of these nodal points form the basis for determining strain rates. Two different methods for the strain calculations have been developed: the parallelepipedon method and the tetrahedron method. Figure 31 shows some results from an analysis of the upsetting of a cube with sticking friction between workpiece and tools. These results were obtained through application of the multicolored workpiece method.

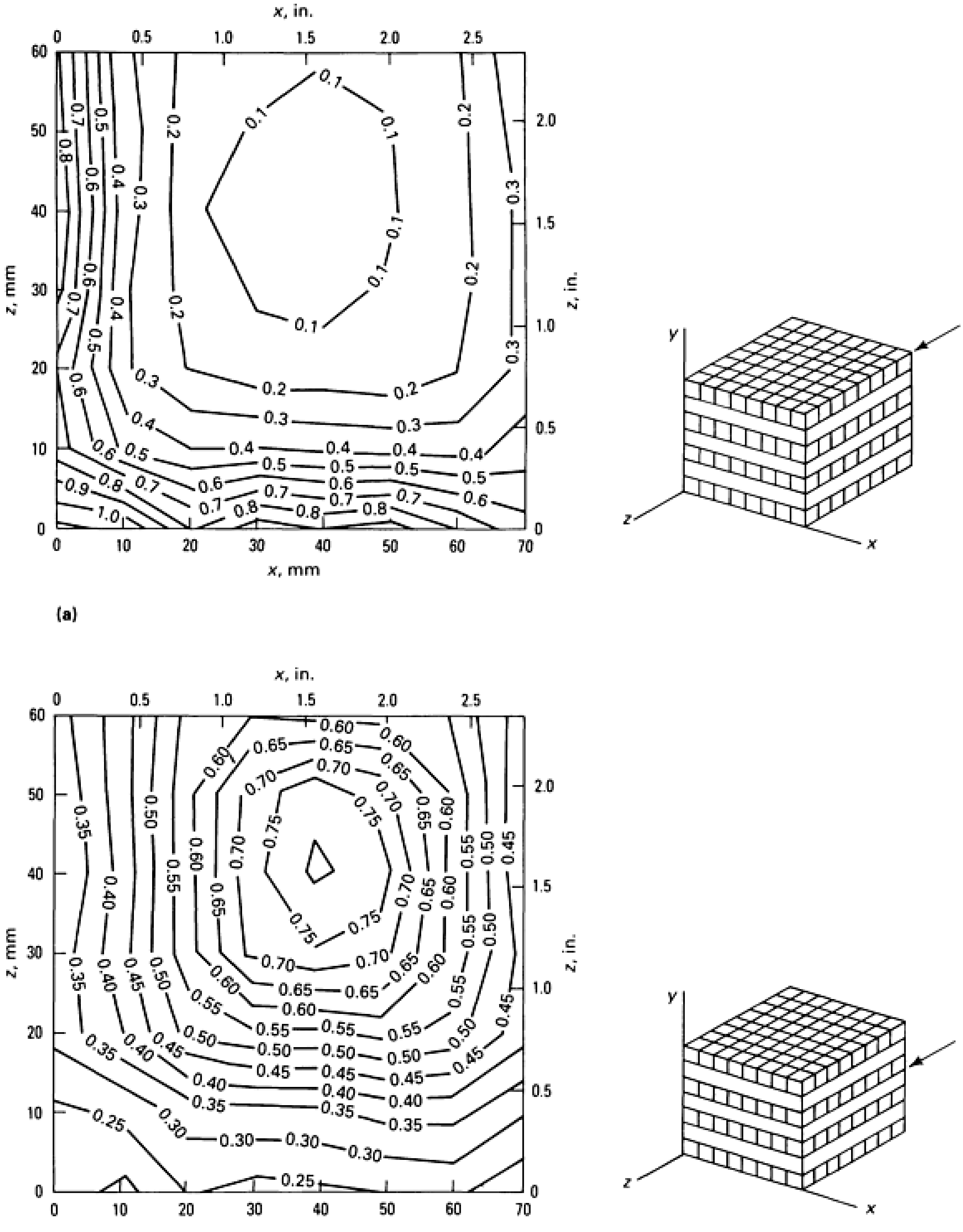

Fig. 31 Equivalent strain distribution in a cube after upsetting. (a) Distribution near the upper horizontal tool/workpiece interface (see arrow at right). (b) Distribution near the horizontal symmetry plane (see arrow)

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Modeling Techniques Used in Forging Process Design

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Acquisition of Data for Forging Process Design

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Introduction

A number of experimental techniques are available for gathering necessary material-behavior data. These techniques include hot compression tests, hot torsion tests, hot tensile tests, and subscale metal-forming operations. These methods are reviewed in Ref 1 and 2 and are discussed in detail in the Section "Evaluation of Workability" in this Volume.

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Hot Compression Testing

Hot compression testing has become increasingly popular for several reasons, in particular:

- Uniform deformation can be maintained to large strains with proper lubrication
- Isothermal conditions can be easily achieved
- The compressive state closely represents the conditions present in forging, extrusion, and rolling processes

Among the various types of hot compression tests, the constant strain rate test is preferred because it yields both the flow stress data required for finite-element method applications and the strain rate sensitivity and temperature sensitivity data needed for dynamic material modeling from the same set of tests (Ref 3).

Generation of Flow Curves. Compression testing is usually conducted on a servohydraulic computer-controlled load-frame with data acquisition instrumentation. Upon completion of the test, load-stroke data that have been written into microcomputer memory are converted from the resolution-step format generated by an analog-to-digital converter to the actual force (MPa) and displacement (mm) units. This new data set is written to disk for permanent storage and is also read for further analysis. Software is then used to calculate true stress and true strain, as discussed below.

First, plastic deformation is determined from the load-stroke curve by determining the elastic component and then subtracting it from the stroke. The true stress and true strain are then calculated. The resulting true stress-true plastic strain data are stored on disk for plotting the flow curves and for further analysis.

Software routines, which correct the true stress-true plastic strain curves for the temperature increase ΔT due to the heat of deformation, are based on:

$$\Delta T = \frac{h\bar{\sigma}\bar{\epsilon}}{\rho C} \quad \textbf{(Eq 1)}$$

where $\overline{\sigma}$ is the true stress, $\overline{\epsilon}$ is the true plastic strain, ρ is the density, C is the heat capacity, h is the deformation heat factor = (A/1 + m), m is the strain rate sensitivity, and A is the heat retention factor.

The calculated temperature increase is used to offset the pseudo flow softening by correcting the flow stress using:

$$\Delta\bar{\sigma} = \Delta T\left(\frac{d\bar{\sigma}}{dT}\right) \quad \textbf{(Eq 2)}$$

$$\Delta\bar{\sigma} = \left(\frac{h\bar{\sigma}\bar{\epsilon}}{\rho C}\right)\left(\frac{d\bar{\sigma}}{dT}\right) \quad \textbf{(Eq 3)}$$

The value of $d\overline{\sigma}/dT$ is determined from the uncorrected experimental data at small strains by plotting $\overline{\sigma}$ versus T at a particular strain rate. The slope of the least squares fit of this plot is taken as $d\overline{\sigma}/dT$.

Typically, several corrected flow curves are generated on a single plot, as shown in Fig. 1. Figure 2 shows the corrected flow curve and the uncorrected flow curve.

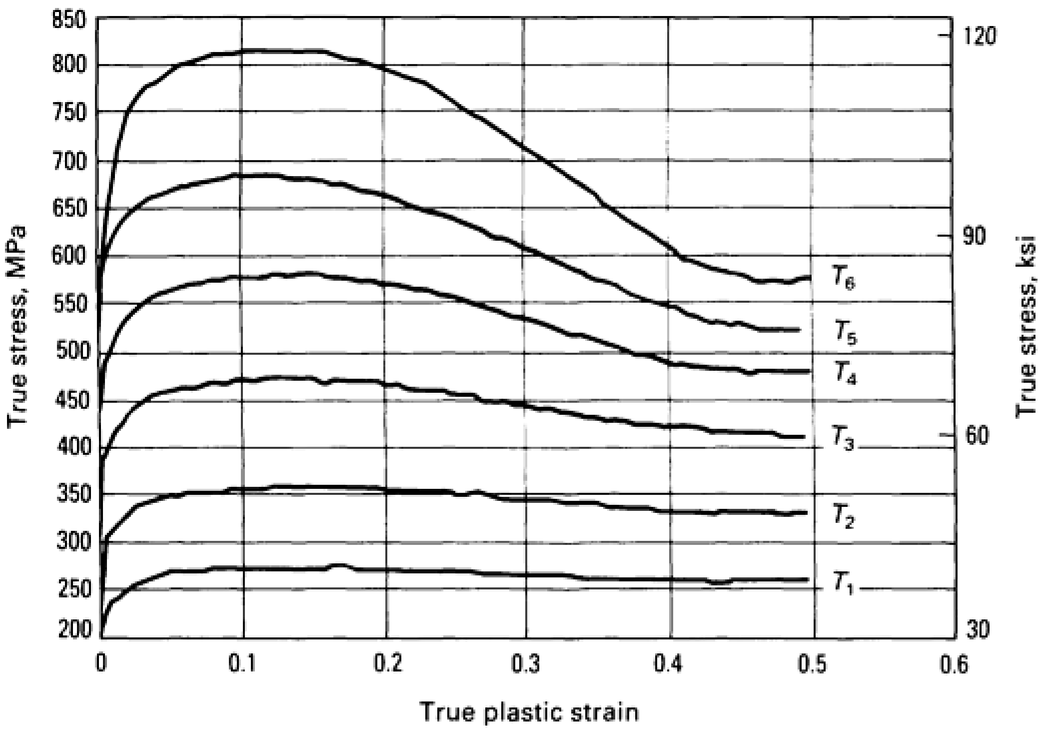

Fig. 1 Corrected flow curves at different temperatures with log $\dot{\epsilon}$ = 0.

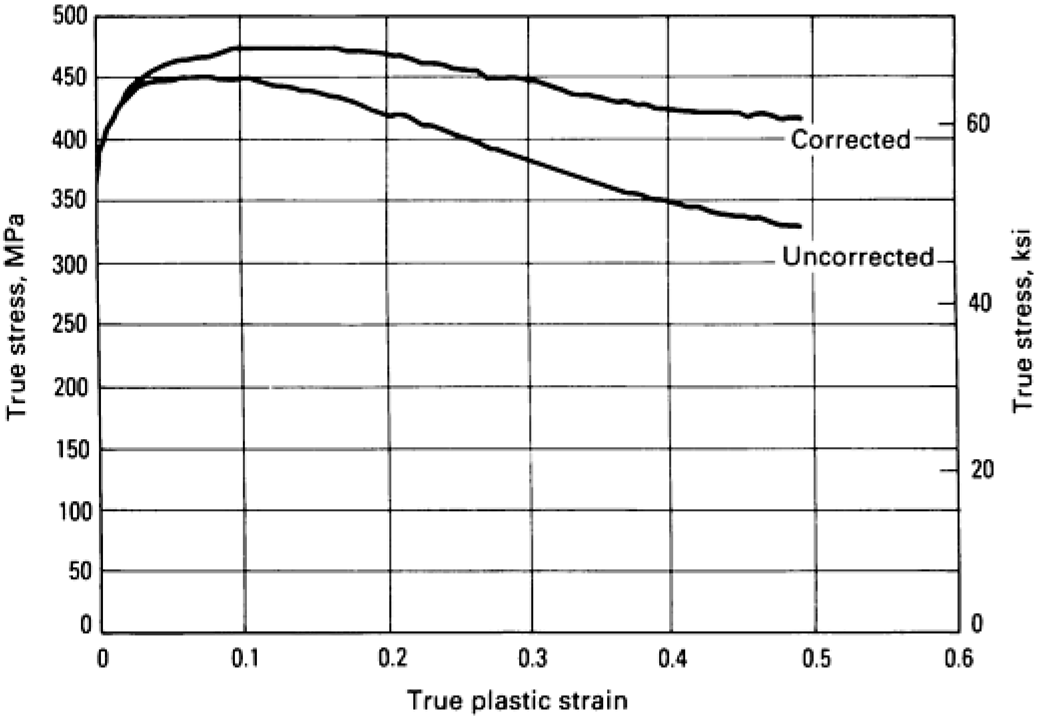

Fig. 2 Corrected and uncorrected flow curves at 1100 °C (2010 °F) and log $\dot{\epsilon}$ = 0.

Effective Stress/Effective Strain Rate/Temperature Relationships. Effective stress and effective strain rate values are extracted at various strain levels and test temperatures from the flow curves and are converted into log $\overline{\sigma}$ and log $\dot{\epsilon}$ The constitutive relationship is obtained by fitting data points to piecewise quadratic functions. Quadratic functions are selected to ensure that the convexity conditions is satisfied. At the intersection of two equations, suitable methods are used to obtain a smooth transition from one equation to another. In the region where two equations overlap, log $\overline{\sigma}$ values are generated by averaging the values obtained by two close equations. Therefore, a large number of log $\overline{\sigma}$ values are generated at very close intervals of log $\dot{\epsilon}$ values. These values can be used for representing flow stress as a function of log $\dot{\epsilon}$ and *T*, and they are also used as a subroutine in the finite-element analysis. Linear interpolation could be used if a value is required between the generated values. A similar procedure is used to generate intermediate points between the experimental data along the temperature axis. A minimum of 40 compression tests are generally recommended to obtain a good fit and accurate results.

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Workability Data

Determination of m, η, and $\partial\eta/\partial \log \dot{\epsilon}$. The strain rate sensitivity parameter m is calculated by using the flow stress values generated as a function of log $\dot{\epsilon}$ at various test temperatures for a given effective strain. A pair of flow stress values taken at very close intervals of log $\dot{\epsilon}$ values are inserted into Eq 4, and the values of m are obtained as a function of log $\dot{\epsilon}$ at a given temperature and effective strain. This should be repeated at each test temperature to cover the entire range of test temperatures and strain rates for the given effective strain.

The efficiency parameter η can be determined by using the calculated m values and Eq 4:

$$\eta = \frac{2m}{m+1} \qquad \textbf{(Eq 4)}$$

The values of η as a function of temperature and log $\dot{\epsilon}$ can be output compatible with a graphics package such as MOVIE.BYU (the graphics software package used to generate the graph). A three-dimensional (3-D) plot of η as a function of temperature and log $\dot{\epsilon}$ and the corresponding efficiency contour plot can then be generated. A sample representation of a 3-D plot is shown in Fig. 3 (Ref 4).

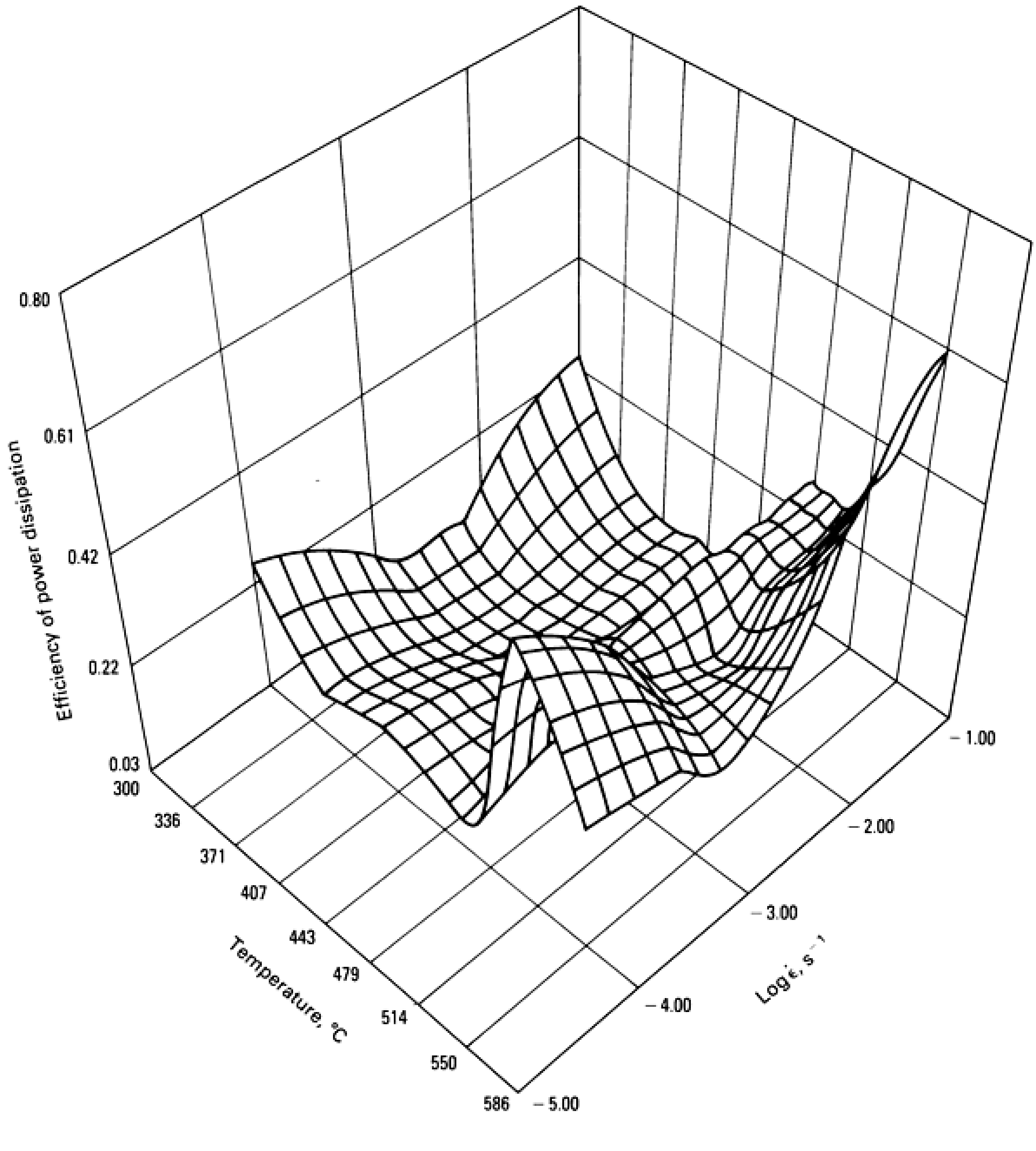

Fig. 3 Three-dimensional surface plot of efficiency of power dissipation as a function of strain rate and temperature of aluminum alloy 2024 with 20 vol% SiC.

Values of $\partial\eta/\partial \log \dot{\epsilon}$ are required to identify stable and unstable regions. The loci of all values of $\partial\eta/\partial \log \dot{\epsilon} = 0$ separate these regions. These values are calculated based on the values at very close intervals of log ε and using the formula $(\eta_2 - \eta_1)/(\log \dot{\epsilon}_2 - \log \dot{\epsilon}_1)$. The values can be plotted on a two-dimensional (2-D) efficiency contour plot.

Determination of *s* and $\partial s/\partial \log \dot{\epsilon}$ The entropy rate ratio, *s*, can be calculated by using the log $\dot{\epsilon}$ values generated from the experimental values at very close temperature intervals for a given effective strain rate and strain (see the section "Dynamic Material Modeling" in the article "Modeling Techniques Used in Forging Process Design" in this Volume). The following relationship is used for this calculation:

$$s = \frac{\partial \log \bar{\sigma}}{T\partial(1/T)} = \left.\frac{\log \bar{\sigma}_2 - \log \bar{\sigma}_1}{[(T_1 + T_2)/2][(T_2 - T_1)/T_1T_2]}\right|_{\dot{\epsilon},\epsilon} \quad \textbf{(Eq 5)}$$

This value will be represented at the temperature:

$$T = \frac{T_1 + T_2}{2} \quad \textbf{(Eq 6)}$$

Similarly, *s* is calculated at various temperatures and effective strain rates for a given effective strain, and these values will be subsequently used for the calculation of $\partial s/\partial \log \dot{\epsilon}$ The variation of *s* with strain rate is also calculated by using a pair of values and Eq 7:

$$\frac{(s_2 - s_1)}{(\log \dot{\epsilon}_2 - \log \dot{\epsilon}_1)} \quad \textbf{(Eq 7)}$$

For delineating the stable and unstable regions, the loci of all values of $\partial s/\partial \log \dot{\epsilon} = 0$ are used. These points can also be plotted on the same efficiency 2-D contour plot, along with the values of $\partial\eta/\partial \log \dot{\epsilon}$ for the development of processing maps. Such a map is shown in Fig. 4 (Ref 4).

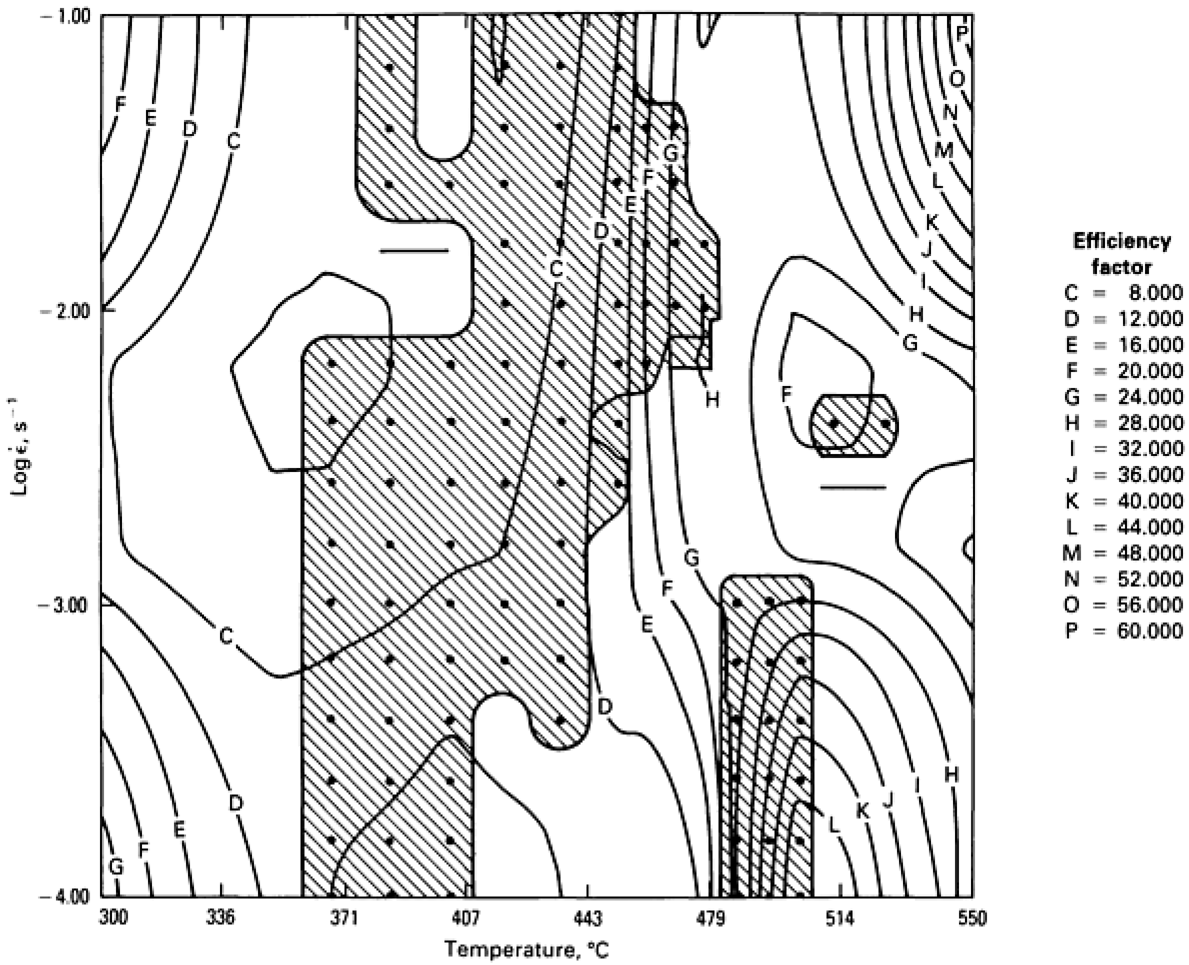

Fig. 4 Processing map showing areas of identical efficiency as a function of log $\dot{\epsilon}$ and temperature. The contour plot also shows stable regions (shaded areas) recommended for processing aluminum alloy 2024 with 20 vol% SiC.

Development and Interpretation of Processing Map. The processing map developed for each initial condition of the experiment provides stable and unstable regions in temperature and effective strain rate space. Stable regions are recommended for processing. In the stable region, the optimum combination of temperature and effective strain rate can be selected based on the required microstructure, the availability of equipment, and the ease with which processing parameters can be controlled.

On the 2-D efficiency contour map, the lines that represent the loci of all

$$\frac{\partial \eta}{\partial \log \dot{\epsilon}} = 0$$

and

$$\frac{\partial s}{\partial \log \dot{\epsilon}} = 0 \qquad \textbf{(Eq 8)}$$

values can be superimposed as shown in Fig. 4 to generate a comprehensive processing map.

Microstructural data should be incorporated into the processing map to correlate the types of microstructures and metallurgical processes. The aim of this evaluation is to identify dissipative processes such as the occurrence of dynamic recrystallization, microcracking, phase transformation, and primary and secondary grain growth. These can be correlated with different regimes on the processing maps in order to interpret the map and to arrive at an optimum range of temperature and strain rate for billet conditioning or hot working.

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Interface Data

Determination of *m*' (Friction Factor). The ring compression test is commonly used for the measurement of the interface friction factor. Ring specimens of standard size are compressed between flat dies to a known reduction under optimum working conditions selected on the basis of dynamic material behavior modeling. The change in the internal diameter of the ring is measured to quantify the friction. The measured dimensions (reduction in height and variation in external diameter) are placed on the appropriate calibration curves to arrive at the friction factor. These analytical calibration curves are available in the literature.

The test can be conducted with different surface roughnesses of the flat dies, using a variety of lubricants. Thus, a complete variation of the friction coefficient is obtained as a function of temperatures, lubricants, surface conditions, and so on.

Determination of h (Heat-Transfer Coefficient). The thermal resistance of a contact is also a surface effect, coupled with the thermal and mechanical properties of the materials making up this contact as well as the interstitial fluid. The absence of either physical contact or an interstitial fluid results in poor contact. The most reliable contact involving metallic conduction results in the case of completely welded or brazed joints. All other cases fall in between these two extremes.

When two surfaces are held together under pressure, the effective contact area is a function of the contact materials, surface conditions, surface flatness, and the contact pressure. The thermal interface conductance is a function of the effective contact area, which is made up of many small points of contact and is only a small fraction of the nominal contact area. Figure 5 shows a representative view of the asperities that make up a contact. These contact points are distributed quite uniformly, so that an increase in pressure causes an increase in the number of points of contact, resulting in an increase in total true contact area.

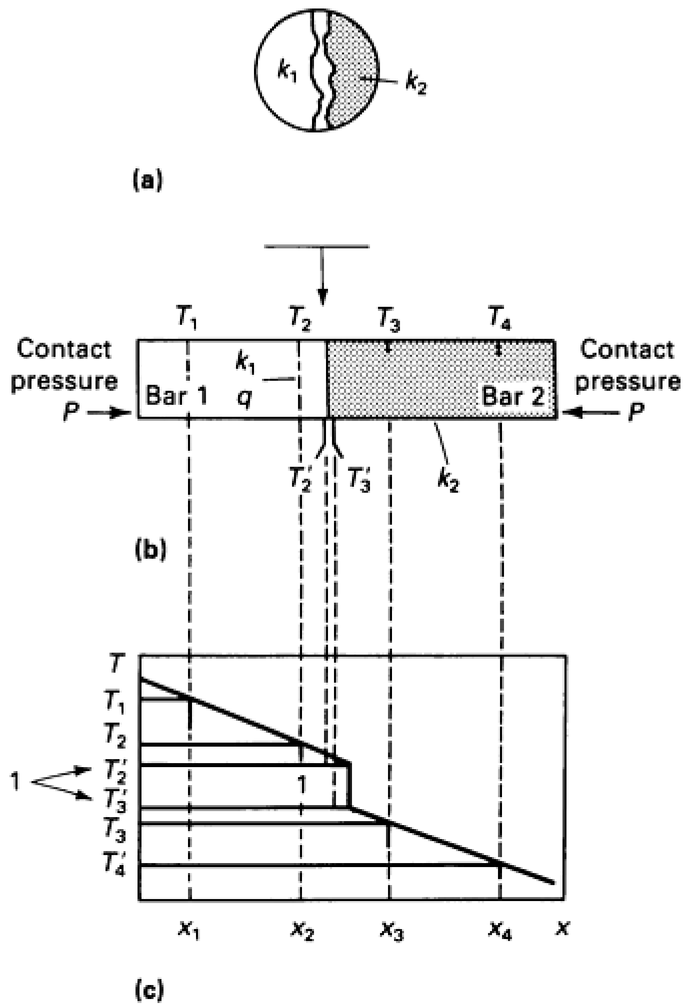

Fig. 5 Physical arrangement and temperature variation for the contact conductance phenomena. (a) Magnified view of contact cross section of bars. (b) Diagram of two bars jointed by contact pressure *P* with $T_1 > T_2$ and heat flowing from bar 1 to bar 2. (c) Temperature variations at points x_1 through x_4 used to extrapolate junction temperatures T'_2 and T'_3.

From the basic Fourier equation, the steady-state heat flow at any part of the heat path is given by:

$$q = k\frac{dT}{dx} \qquad \textbf{(Eq 9)}$$

If the thermal conductance of the interface is defined as:

$$h = \frac{q}{\Delta T} \qquad \textbf{(Eq 10)}$$

where q is the heat flow (in Btu/h/ft^2), k is the thermal conductivity (in Btu/h/ft/°F), T is the temperature (in °F), h is the conductance at interface (in Btu/h/ft^2/°F), and x is the distance in the direction of heat flow (in feet). Then:

$$h(\Delta T) = k\frac{dT}{dx} \qquad \textbf{(Eq 11)}$$

or

$$h = \frac{k(dT/dx)}{\Delta T} \quad \text{(Eq 12)}$$

The thermal resistance is defined as $r = 1/h$.

Figure 5 shows two bars held in position by a pressure, P. Under steady-state conditions, neglecting any losses when $T_1 > T_2$, the heat flux flows from left to right, and $q_1 = q_j = q_2$, where q_j is the heat flux through the junction of cross-sectional area A. If temperature measurements are made at locations x_1, x_2, x_3, and x_4 as shown in Fig. 5 and if the conductivities k_1 and k_2 are assumed to be independent of the temperature, the junction temperatures T'_2 and T'_3 can be obtained by extrapolation. The interface conductance, h, for the joint can then be determined by:

$$h = \frac{k_1[(T_1 - T_2)/(x_2 - x_1)]}{(T'_2 - T'_3)} \quad \text{(Eq 13)}$$

A test fixture, as shown in Fig. 6, is used to measure the thermal contact conductance between two dies. It consists of a hollow punch that is machined, ground, and then screwed onto a steel punch adaptor. The adaptor is fastened to the upper platen of the testing machine. A copper disk with cartridge heaters is used to heat the punch head.

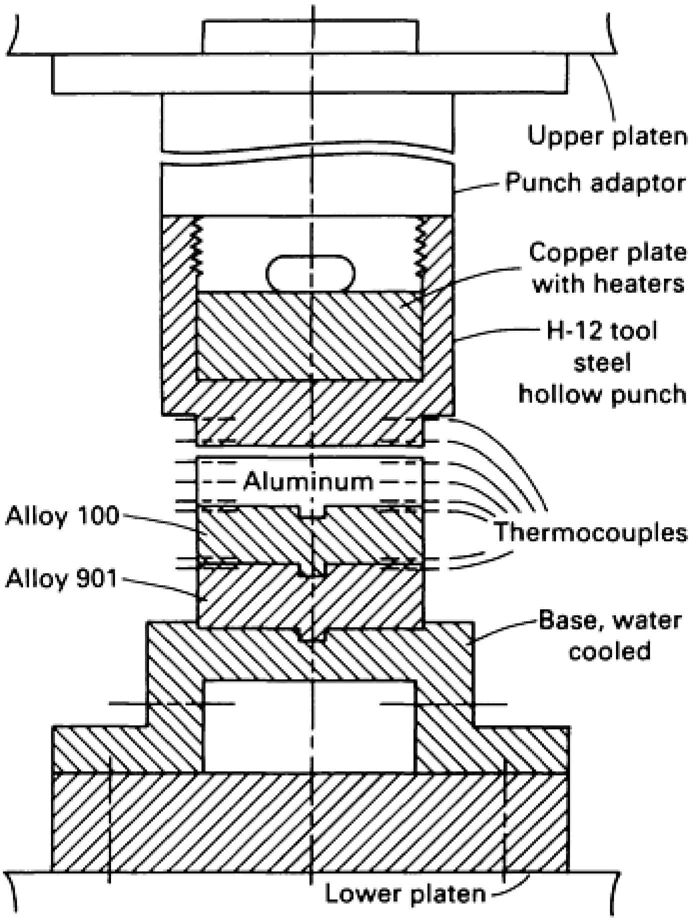

Fig. 6 Schematic of heat-transfer coefficient test fixture for measuring thermal contact conductance between two dies. The sample material tested is aluminum.

The punch is machined in two pieces to facilitate insertion of the copper plate. A voltage regulator is used to vary the heater output. The base of the fixture is mounted on the lower platen of the testing machine and can be cooled by water. A number of instrumented dies machined from various superalloys, such as Waspaloy, Alloy 901 (UNS N09901), Alloy 100 (Ni-15Co-10Cr-5.5Al - 4.7Ti - 3.0Mo - 1.0V - 0.6Fe - 0.15C-0.06Zr-0.015B), H-12 tool steel, and type 347 can be stacked on the base. This simulates the behavior of the stacked dies currently used by major forging companies. The punch head is heated to a high temperature and brought into contact with the lower dies. Each die disk is instrumented for temperature measurement. The temperature is measured at three different levels by thermocouples inserted in 0.51 mm (0.020 in.) diam holes machined by electrical discharge machining. Two thermocouples are used at each interface level. Therefore, temperature is measured at six different locations in each die to obtain temperature profiles and interface temperatures.

Acquisition of Data for Forging Process Design

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Introduction

A BLANK is a shape cut from flat or preformed stock. Ordinarily, a blank serves as a starting workpiece for a formed part; less often, it is a desired end product. This article will discuss the production of blanks from low-carbon steel (such as 1008 and 1010) sheet and strip in dies in a mechanical or hydraulic press.

Improving the quality of blanked edges by shaving is discussed later in this article. Fine edge blanking is treated in the article "Fine Edge Blanking and Piercing" in this Volume. Shearing, a method of making blanks without using a die, is dealt with in the articles "Shearing of Plate and Flat Sheet" and "Slitting and Shearing of Coiled Sheet and Strip" in this Volume.

Methods of Blanking in Presses

Cutting operations that are done by dies in presses to produce blanks include cutoff, parting, blanking, notching, and lancing. The first three of these operations can produce a complete blank in a single press stroke. In progressive dies, two or more of these five operations are done in sequence to develop the complete outline of the blank and to separate it from the sheet, strip, or coil stock.

Trimming is defined as the cutting off of excess material from the periphery of a workpiece. It is usually done in dies and is similar to blanking. It is often the final operation on a formed or drawn part.

The applications of these methods are described in examples throughout this article. Other examples of these methods of producing blanks are provided in the articles "Piercing of Low-Carbon Steel," "Blanking and Piercing of Electrical Steel Sheet," "Press Forming of Low-Carbon Steel," "Press Forming of High-Carbon Steel," and "Deep Drawing" in this Volume.

Cutoff. This operation consists of cutting along a line to produce blanks without generating any scrap in the cutting operation, most of the part outline having been developed by notching or lancing in preceding stations. The cutoff line can take almost any shape--straight, broken, or curved. After being cut off, the blanks fall onto a conveyor or into a chute or container.

A cutoff die can be used to cut the entire outline of blanks whose shape permits nesting in a layout that uses all of the material (except possibly at the ends of the strip), as shown in Fig. 1. Alternating positions can sometimes be used in nesting (middle, Fig. 1) to avoid producing scrap except at strip ends. Cutoff is also used to cut blanks from strip that has already been notched to separate the blanks along part of their periphery, as described in the following example.

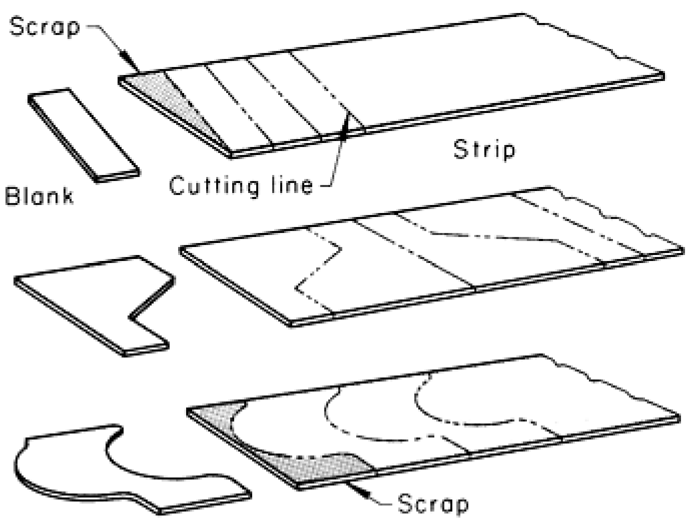


Fig. 1 Nested layouts for making blanks by cutoff

Example 1: Use of Cutoff to Separate Blanks Partly Outlined by Notching.

Figure 2 shows the layout for the cutoff of blanks for automobile body-bolt brackets. The brackets were completed by piercing and forming.

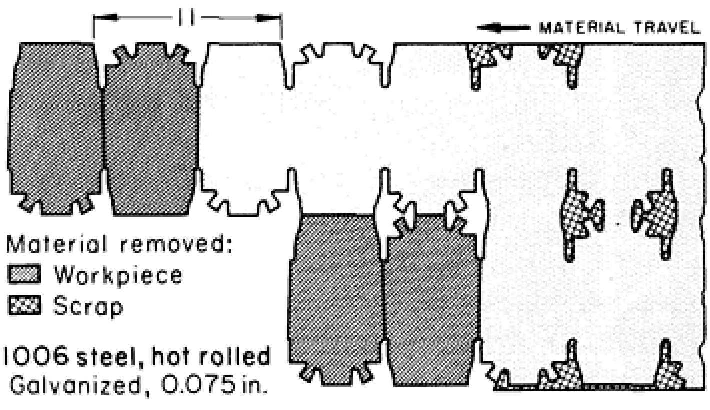


Fig. 2 Layout for the cutoff of four blanks at each press stroke from notched and seminotched strip. Dimensions given in inches

The coiled strip of galvanized hot-rolled 1006 steel was 1.90 mm (0.075 in.) thick by 495 mm (19.5 in.) wide. This was wide enough for two blanks, as shown in Fig. 2. The blanks were alternated in the layout to facilitate trimming and piercing.

The strip was notched and seminotched in the first stations of a progressive die (making a small amount of scrap). In the following stations, straight cutoff punches made four blanks at each stroke without producing any additional scrap.

The work was done in a 3.6 MN (400 tonf) single-action mechanical press with an air cushion. The press was equipped with a double roll feed and made 50 strokes (200 blanks) per minute.

Advantages of cutoff in making blanks include:

- The die has few components and is relatively inexpensive
- Waste of material in blanking is minimized or eliminated
- The die can be resharpened easily, and maintenance costs are low

Disadvantages of cutoff include:

- It can be used only to make blanks that nest in the layout without waste
- Cutting of one edge causes one-way deflection and stress
- Accuracy may be affected adversely by the method of feeding

Parting (Fig. 3) is the separation of blanks by cutting away a strip of material between them. Like cutoff, it can be done after most of the part outline has been developed by notching or lancing. It is used to make blanks that do not have mating adjacent surfaces for cutoff (Fig. 3) or to make blanks that must be spaced for ease of handling in order to avoid distortion or to allow room for sturdy tools. Some scrap is produced in making blanks by parting; therefore, this method is less efficient than cutoff in terms of material use.

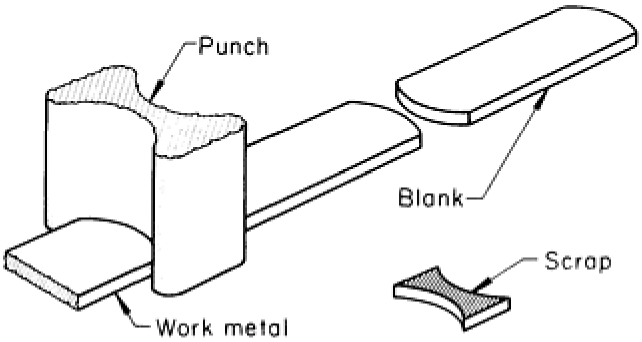


Fig. 3 Use of a parting punch to make blanks not having mating adjacent surfaces

Blanking (also called punching) is the cutting of the complete outline of a workpiece in a single press stroke. Because a scrap skeleton is usually produced, blanking involves some material waste. However, blanking is usually the fastest and most economical way to make flat parts, particularly in large quantities.

The skeleton left by blanking sometimes has only scrap-metal value, but many shops have organized programs to maximize the use of cutouts and sizable scrap skeletons in making other production parts. Material waste is completely avoided by using the scrap skeleton that remains from certain blanking operations to provide perforated stock for such items as air filters for forced-air furnaces.

Piercing (with a flat-end punch), also called punching or perforating, is similar to blanking except that the punched-out (blanked) slug is the waste and the surrounding metal is the workpiece. Piercing is discussed in the article "Piercing of Low-Carbon Steel" in this Volume.

Notching is an operation in which the individual punch removes a piece of metal from the edge of the blank or strip (Fig. 4). Notching is done for such reasons as the following:

- To free some metal for drawing (Fig. 4a) and for forming (Fig. 4b) while the workpiece remains attached to the strip
- To remove excess metal before forming (Fig. 4c)
- To cut part of the outline of a blank that would be difficult to cut otherwise (Fig. 2 and Fig. 28)

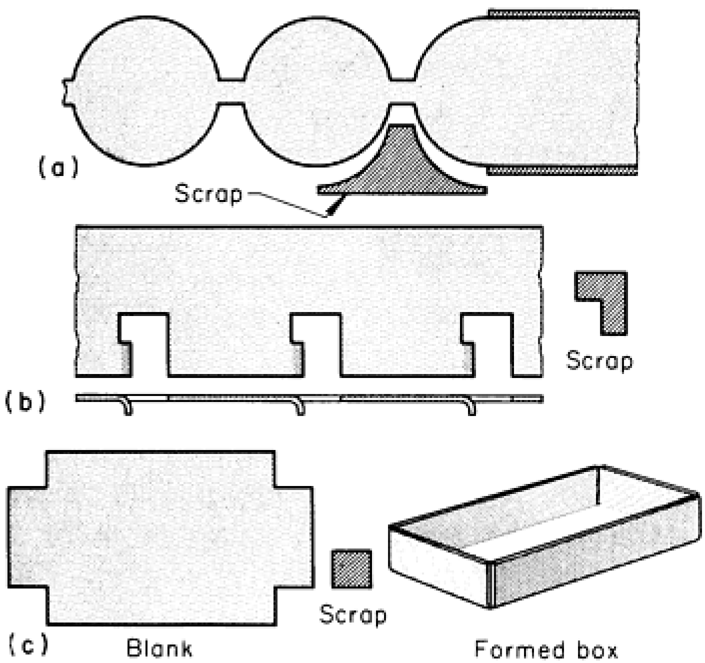


Fig. 4 Notched work illustrating the use of notching for freeing metal before drawing (a), and before forming (b), and for removing excess metal before forming (c)

The piercing of holes of any shape in a strip to free metal for subsequent forming or to produce surfaces that later coincide with the outline of a blanked part is sometimes called seminotching. The pierced area may outline a portion of one part or of two or more adjacent parts in a strip. Figure 2 illustrates a progressive-die layout incorporating seminotching.

Lancing is a press operation in which a single-line cut or slit is made part way across the strip stock without removing any metal. Lancing is usually done to free metal for forming (Fig. 5). The cut does not have a closed contour and does not release a blank or a piece of scrap. In addition to its use in freeing metal for subsequent forming, lancing is also used to cut partial contours for blanked parts, particularly in progressive dies.

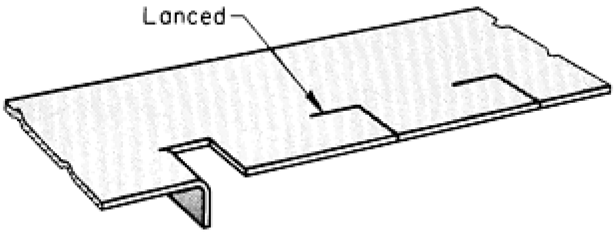


Fig. 5 Strip that was lanced to free metal for forming

Trimming is an operation for removing excess metal (such as deformed and uneven metal on drawn or formed parts) and metal that was used in a previous operation (such as a blankholding flange for a draw operation). Trimming is done in several ways, depending on the shape of the workpiece, the accuracy required, and the production quantity.

Figure 6 illustrates the tooling for trimming a horizontal flange on a drawn shell in a separate operation. The drawn shell is set on a locating plug for trimming. After scrap from a sufficient number of trimmed shells has accumulated, the piece of scrap at the bottom is severed at each stroke of the press by the scrap cutters shown in Fig. 6, and falls clear. Except that the die must be constructed to accept and locate the drawn shell, the operation is identical to the blanking of a flat workpiece and produces square edges of the same accuracy and quality. A drawn shell or formed part can be trimmed in a press without leaving a flange on the completed part by using one of three methods: pinch trim, shimmy trim, or trim and wipe-down.

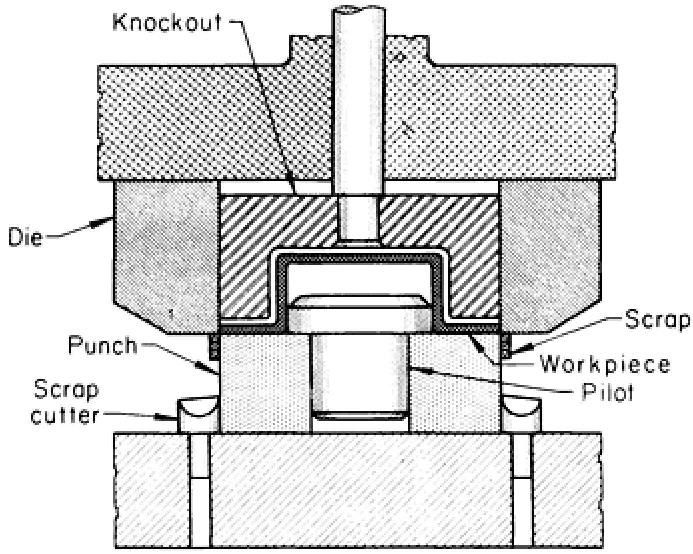


Fig. 6 Single-operation die for trimming a horizontal flange on a drawn shell

Pinch trimming, shown as a separate operation in a push-through die in Fig. 7, is done only on a part that has at least a narrow flange as-formed. The shell must be free from wrinkles at or near the trimming line. The trimmed edge is not square with the sidewall, but has the general shape shown in the lower right corner of Fig. 7. The accuracy of height resulting from pinch trimming is affected by variations in wall thickness and flange radius. To ensure an even pinch-off and to avoid sharp or rough edges, clearance between punch and die must be held to a minimum, and the punch must be kept sharp.

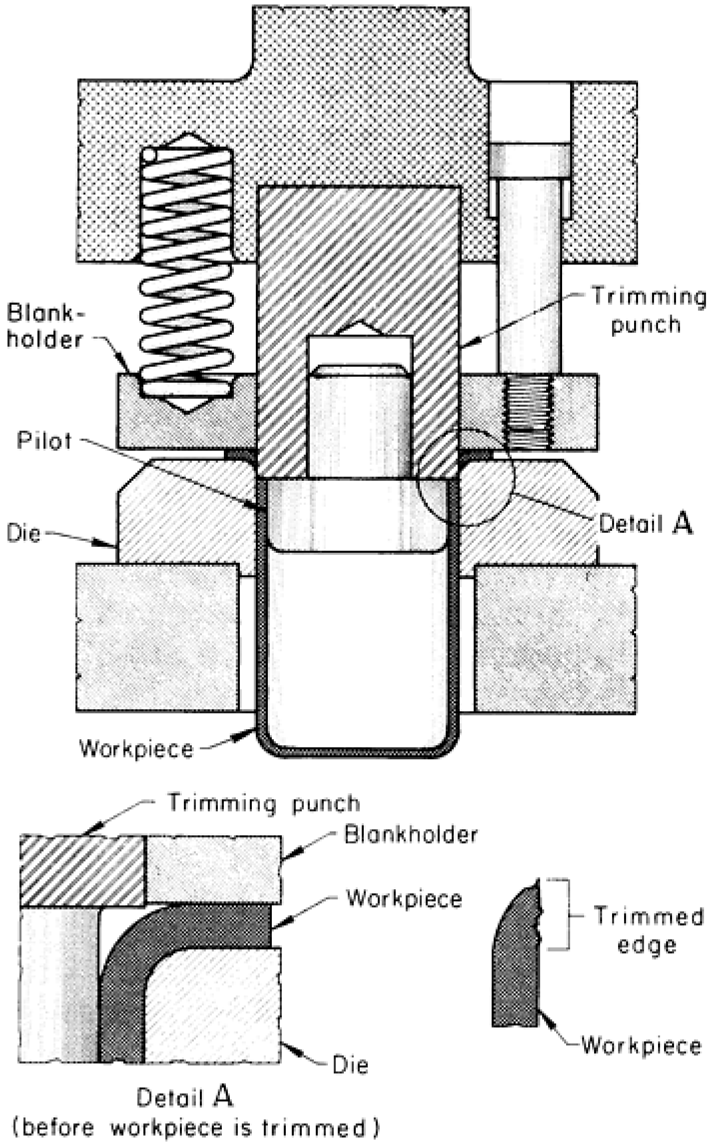


Fig. 7 Pinch trimming a drawn shell in a push-through die

Pinch trimming is also done without a blankholder or hold-down, using a die otherwise similar to that shown in Fig. 7. The scrap rings can be blown off the die at each stroke. In another method, the scrap rings climb the punch until they are severed by being compressed against a scrap cutter, after which they are spread apart and allowed to run out along a track for disposal. Pinch trimming without a blankholder is particularly well suited to the high-volume production of eyelets and other small parts.

Pinch trimming is primarily a mass production method. The production rate is high because only one stroke of the press is required to complete the trim. The method is often combined with drawing in a compound draw-and-trim die to reduce production costs further. The disadvantages of pinch trimming are excessive burrs, sharp cut edge, and high die maintenance.

Shimmy Trimming. In trimming with a shimmy die (also known as Brehm or model trimming), the drawn shell is held in a close-fitting die of the exact shell height and trimmed in segments by the successive horizontal oscillations of an internal cam-actuated punch toward the outside of the shell. The resulting trimmed edge is square and closely resembles the conventional blanked edge on a flat part. Shell height is more accurate than with pinch trimming. In addition to its application to shells that must have square, accurate edges, shimmy trimming is used on shells that have a wrinkled or otherwise nonuniform top edge as-drawn (cutoff is done below the defects) and on shells that cannot be produced economically with even the narrow flange needed for pinch trimming.

Tooling costs for shimmy trimming are much higher than those for pinch trimming. Shimmy trimming is also slower because it requires four or more oscillations of the punch in one press stroke and cannot be combined with other operations in a compound die. Shimmy dies are inexpensive to maintain because they remain in alignment and therefore are not likely to wear by shearing or chipping.

Trim and Wipe-Down. In this type of trimming, a flange is cut to width using a die such as that shown in Fig. 6 and then wiped or straightened into line with the sidewall of the shell or formed part. Because of narrow flange width, trimming and wiping down may be two operations.

The edge is square with the sidewall, but the shell height may be slightly irregular because of the forming characteristics of the metal. In addition, a ring may be visible at the original location of the flange radius.

Trimming, other than shimmy trimming, is frequently combined with one or more other operations in a compound die. Trim stock is often left on a drawn or formed workpiece so that it can be trimmed to size in a second operation. This is done to obtain the most accurate relationship of some other feature, such as a pierced hole, to the trimmed outline of the workpiece.

Characteristics of Blanked Edges

The sheared edges of a blank produced in a conventional die are not smooth and vertical for the entire thickness of the part, but exhibit the characteristics represented on an exaggerated scale in Fig. 8. The blank is shown in the position in which it would be cut from the work metal by the downward motion of the punch. A portion of the stock remaining after removal of the blank is shown at the top of the illustration.

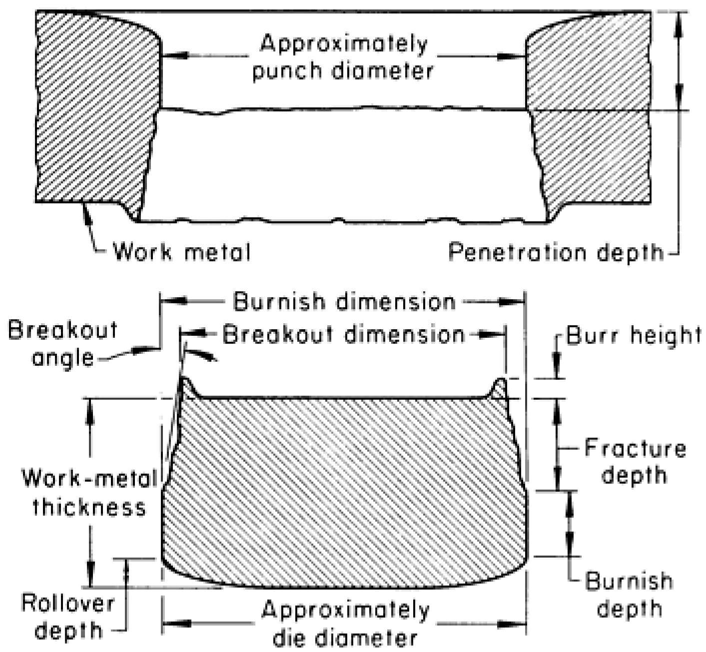


Fig. 8 Characteristics of the sheared edges of a blank. Curvature and angles are exaggerated for emphasis.

Rollover on the lower edges of the blank develops by plastic deformation of the work metal as it is forced into the die by the punch. Compression of the metal above the rollover zone against the walls of the die opening burnishes a portion of the edge of the blank, as shown in Fig. 8. As the punch completes its stroke, the remaining portion of the blank edge is broken away or fractured (resulting in die break), and a tensile burr is formed along the top of the blank edge.

The angle of the fractured portion of the edge is identified in Fig. 8 as the breakout angle. The breakout dimension of the blank and the burnish dimension of the hole in the scrap skeleton are approximately equal to the corresponding punch dimension, and the burnish dimension of the blank is very close to the corresponding die dimension. Therefore, the punch determines the hole size, and the die governs the blank size.

Penetration depth, or the amount of penetration of the punch into the work metal before fracture occurs, is shown on the edge of the remaining stock or scrap skeleton in Fig. 8. This depth is approximately equal to the sum of the rollover depth and the burnish depth on the blank, except when low die clearance produces secondary burnish. It is usually expressed as a percentage of the work metal thickness.

The percentage of penetration (before fracture) depends on the properties of the work metal, as shown in Table 1, which gives approximate values for various steels and nonferrous metals under typical blanking conditions. The percentage of penetration affects energy consumption and cutting force in blanking, as described in the section "Calculation of Force Requirements" in this article.

Table 1 Approximate penetration of sheet thickness before fracture in blanking

Work metal	Penetration, %
Carbon steels[a]	
0.10% C, Ann	50
0.10% C, CR	38
0.20% C, Ann	40
0.20% C, CR	28
0.30% C, Ann	33
0.30% C, CR	22
Silicon steels	30
Nonferrous metals	
Aluminum alloys	60
Brass	50
Bronze	25
Copper	55
Nickel alloys	55

Zinc alloys	50

(a) Ann, annealed; CR, cold rolled

Die Clearance

The terms clearance, die clearance, and punch-to-die clearance are used synonymously to refer to the space between punch and die. Clearance is important for the reliable operation of the blanking equipment, the quality and type of cut edges, and the life of the punch and die. In general, the effects of clearance on these factors in blanking are the same as in piercing and are discussed in the article "Piercing of Low-Carbon Steel" in this Volume. The same article also describes the edge characteristics of slugs produced in piercing holes (see Fig. 2 of that article). The data in that illustration can serve as a guide for selecting clearances for blanking. All clearance values given in this article are per side, except where indicated.

Optimal blanking clearance may sometimes be less than optimal piercing clearance. This is partly because the blanked edge is generally close to the stock edge, and material expansion is therefore less restricted. A piercing tool must move a great deal of material away from its cutting edge, and for longest life, the clearance should be selected to eliminate as much compressive loading on the work metal as possible.

A part blanked using clearance much greater than normal may exhibit double shear, which is ordinarily evident only with extremely small clearance (see edge types 4 and 5 in Fig. 2 in the article "Piercing of Low-Carbon Steel" in this Volume). In addition, a part blanked using large clearances will be smaller than the die opening (except for a deeply dished blank), and it is difficult to correct the tooling to compensate for this. In some applications, retaining the blank becomes almost as great a problem as expelling the slugs into a die cavity after piercing, because of the increased clearance.

Relief in a blanking die (Fig. 9) is the taper provided so that the severed blank can fall free. The relief angle may range from $\frac{1}{2}$ to 2° from the vertical wall of the die opening. Relief in a die is sometimes called draft or angular clearance. In some dies, the relief may start at the top of the die surface and have a taper of only 0.002 in./in. (0.002 mm/mm) per side. In other dies, there is a straight, vertical wear land between the top of the die and the relief.

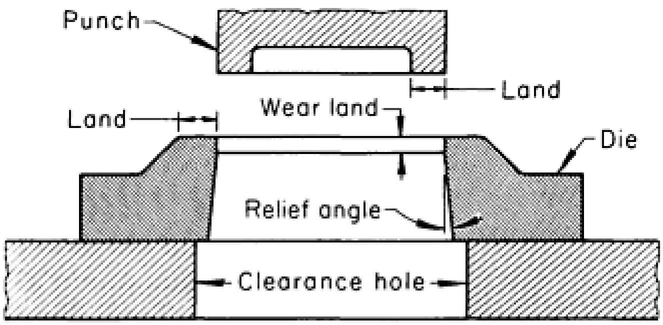


Fig. 9 Relief in a blanking die

Calculation of Force Requirements

Calculation of the forces and the work involved in blanking gives average figures that are applicable only when the correct shear strength for the material is used and when the die is sharp and the punch is in good condition, has correct clearance, and is functioning properly. The total load on the press, or the press capacity required to do a particular job, is the sum of the cutting force and other forces acting at the same time, such as the blankholding force exerted by a die cushion.

Cutting Force: Square-End Punches and Dies. When punch and die surfaces are flat and at right angles to the motion of the punch, the cutting force can be found by multiplying the area of the cut section by the shear strength of the work material:

$$L = S_s\, tl \qquad \textbf{(Eq 1)}$$

where L is the load on the press (in pounds) (cutting force), S_s is the shear strength of the stock (in pounds per square inch), t is the stock thickness (in inches), and l is the length or perimeter of the cut (in inches). The shear strengths of various steels and nonferrous metals are given in Table 2.

Table 2 Shear strengths of various steels and nonferrous metals at room temperature

Metal	Shear strength	
	MPa	ksi
Carbon steels		
0.10% C	241-296	**35-43**
0.20% C	303-379	**44-55**
0.30% C	358-462	**52-67**
High-strength low-alloy steels	310-439	**45-63.7**
Silicon steels	414-483	**60-70**
Stainless steels	393-827	**57-129**
Nonferrous metals		
Aluminum alloys	48-317	**7-46**
Copper and bronze	152-483	**22-70**
Lead alloys	13-40	**1.83-5.87**
Magnesium alloys	117-200	**17-29**
Nickel alloys	242-800	**35-116**

Tin alloys	20-77	**2.90-11.1**
Titanium alloys	414-483	**60-70**
Zinc alloys	**97-262**	**14-38**

Cutting Force: Dies With Shear. For cutting large blanks, shear can be applied to the face of the die by grinding it at an angle to the motion of the punch (Fig. 10), but shear is not used in cutting small blanks. Shear reduces shock in the press, as well as blanking noise and blanking force, but the same amount of work is done as with a flat die surface.

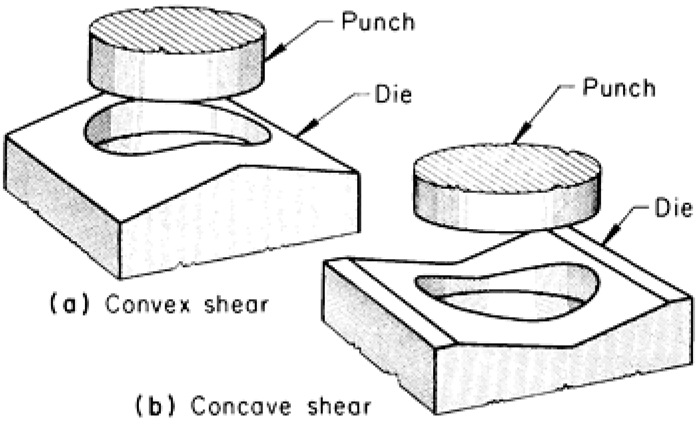


Fig. 10 Convex shear and concave shear on blanking dies. Angle and depth of shear are exaggerated for emphasis. Normally, depth of shear does not greatly exceed stock thickness.

The most common type of shear used on the die is convex (Fig. 10a). The apex of the die face is slightly rounded to avoid initiating a crack in the work metal. Concave shear (Fig. 10b) is somewhat more difficult to grind on the die, but holds the work metal more securely during blanking. A radius approximately equal to that of the grinding wheel is produced where the ground surfaces meet.

A third type of shear, sometimes used on a die for a large blank, consists of a wavy or scalloped surface around the die opening. This technique uses several convex and concave shear surfaces around the die opening. The punch load is distributed over the entire die surface, thus minimizing punch shift.

The amount of shear in a die can be less than or greater than stock thickness. Shear that is equal in depth or greater than the stock thickness is called full shear.

The cutting force for a die with shear can be calculated after first finding the work done (energy used) in blanking. The work done in blanking equals the force required in blanking (load on the press) multiplied by the distance that the force acted:

$$W = Ls \quad \textbf{(Eq 2)}$$

where W is the work done in blanking (in inch-pounds), L is the load (in pounds), and s is the distance the load acts (thickness multiplied by percentage of penetration before fracture) (in inches). To obtain accurate work values, the percentage penetration must be known accurately.

Cutting or blanking force is reduced by the use of angular shear in the die; the amount of reduction in force depends on the depth of the angular shear. The reduced average cutting force on the press is:

$$L_{sh} = \frac{W}{s + s_1} \quad \textbf{(Eq 3)}$$

where L_{sh} is the average cutting force (in pounds), with angular shear, W is the work done in blanking (in inch-pounds), s is the distance (in inches) that the load acts (thickness multiplied by percentage of penetration before fracture), and s_1 is the depth of angular shear (in inches).

In simplified practice, some plants ignore partial shear in calculating cutting force for blanking. When full shear is used, force is calculated as without shear and then reduced by 30%.

Stripping force is the force needed (when drop-through is not used) to free the blank from the die or the strip from the punch when they stick or jam because of springback. Stripping force can be calculated using:

$$L_{st} = kA \qquad \textbf{(Eq 4)}$$

where L_{st} is the stripping force (in pounds), k is a stripping constant (in pounds per square inch), and A is the area of the cut surface (in square inches) (stock thickness t multiplied by length or perimeter of cut l). Approximate values for the constant k (as determined by experiment for low-carbon steel) are:

- 1500 for sheet metal thinner than 1.57 mm (0.062 in.) when the cut is near an edge or near a preceding cut
- 2100 for other cuts in sheet thinner than 1.57 mm (0.062 in.)
- 3000 for sheet more than 1.57 mm (0.062 in.) thick

Factors That Affect Processing

Factors that affect the processing of blanks include the following:

- Size and shape of the blank
- Material for blanking
- Form in which the material is supplied
- Thickness of the blank
- Production quantity and schedule
- Quality specifications
- Availability of equipment and tools
- Number and type of subsequent operations required for completing the work

The size and shape of the blank affect the form and handling of the material blanked, the blanking method, and the handling of the completed blank. The thickness of the blank affects the press load required (see the section "Calculation of Force Requirements" in this article), the selection of equipment, and the choice of blanking and handling methods (see the section "Effect of Work Metal Thickness" in this article).

Production quantity and schedule determine the choice of equipment. A total production of fewer than 10,000 pieces is considered a short run; 10,000 to 100,000 pieces, a medium run; and more than 100,000 pieces, a long run.

Quality specifications and tolerances for thickness, camber, width, length, flatness, and finish affect the handling of the material. The availability of single-, double-, or triple-action presses (rated at various force capacities, sizes, speeds, lengths of stroke, strokes per minute, and shut heights) affects the selection of the processing method. The availability and capacity of auxiliary press equipment can have an effect on the selection of a tooling system and on whether a part can be made in-plant.

Operations that follow blanking also affect the choice of equipment, the processing method, and the handling procedures. Such subsequent operations may include piercing, bending, forming, deep drawing, machining, grinding, or finishing. Only rarely is the blank a final product.

Selection of Work Metal Form

Work metal for blanking in presses is usually in the form of flat sheets, strip, or coil stock. Less frequently, steel plate is blanked in presses (see the section "Effect of Work Metal Thickness" in this article). In some applications, the metal is preformed before blanking.

Special preparation of the work metal is usually not required for the blanking operation itself. However, annealing, leveling, or cleaning is often needed because of subsequent forming operations on the blank, as discussed in the article "Press Forming of Low-Carbon Steel" in this Volume.

Sheet or Strip. Flat sheet is usually the work metal for large blanks, such as automobile roofs. Square-sheared sheet can be used as a blank, or it can be blanked in a die. Small quantities of blanks, regardless of size, are usually made from straight lengths of sheet or strip.

Coil stock is used for mass production, whenever possible. In continuous production, the use of coil stock can save as much as one-third of the time needed for producing an equal quantity from flat stock. In addition, there are fewer scrap ends when coil stock is used.

Sheet metal is the least expensive when it is supplied in large coils from the mill. For most applications, the coil must be slit to the proper width for blanking, and some edge material must be trimmed off. Parts can sometimes be made most economically in a progressive die by using coil stock that is the width of the developed blank.

Blank Layout

In the medium and high production of medium-size blanks, the cost of material is 50 to 75% of the total cost of the blank; for large blanks, it may be more than 95% of the total cost of the blank. Substantial savings in net material cost can often be achieved by coordinating blank layout with the selection of stock form and width to minimize the amount of scrap produced.

Several trial layouts may be needed to find the width of stock and the layout that use the material most efficiently while taking into account the possible effects of orientation of parts on subsequent operations. The layout must include the minimum workable scrap allowance between blanks, providing just enough material to support or hold down the strip during blanking. Scrap allowances, based on the use of well-maintained equipment and good shop practice, are given in Table 3.

Table 3 Scrap allowance for blanking

Work metal	Scrap allowance when length of skeleton segment between blanks or along edge is:											
	2*t* or less						Greater than 2*t*					
	Thickness of stock, *t*		**Edge of stock to blank**		**Between blanks in row**		**Thickness of stock, *t***		**Edge of stock to blank**		Between blanks in row	
	mm	**in.**	**mm**	**in.**	**mm**	**in.**	**mm**	**in.**	**mm**	**in.**	**mm**	in.
Metals in general	Up to 0.53	Up to 0.021	1.27	0.050	1.27	0.050	Up to 1.12	Up to 0.044	1.27	0.050	1.27	**0.050**
Standard strip stock	0.56-1.40	0.022-0.055	1.02	0.040	1.02	0.040	Over 1.12	Over 0.044	0.9*t*	0.9*t*	0.9*t*	**0.9*t***

	Over 1.40	Over 0.055	0.7*t*	0.7*t*	0.7*t*	0.7*t*						
Extra-wide stock and weak scrap skeleton	Up to 1.07	Up to 0.042	1.52	0.060	1.27	0.050	Up to 0.84	Up to 0.033	1.52	0.060	1.27	**0.050**
	Over 1.07	Over 0.042	1.4*t*	1.4*t*	1.2*t*	1.2*t*	Over 0.84	Over 0.033	1.8*t*	1.8*t*	1.6*t*	**1.6*t***
Stock run through twice	Up to 1.07	Up to 0.042	1.52	0.060	1.27	0.050[a]	Up to 0.84	Up to 0.033	1.52	0.060	1.27	**0.050[a]**
	1.09-1.40	0.043-.055	1.4*t*	1.4*t*	1.02	0.040	0.86-1.12	0.034-0.044	1.8*t*	1.8*t*	1.02	**0.040**
	Over 1.40	Over 0.055	1.4*t*	1.4*t*	0.7*t*	0.7*t*	Over 1.12	Over 0.044	1.8*t*	1.8*t*	0.9*t*	**0.9*t***
Stock run through twice; blanks in rows 1 and 2 interlock	Up to 1.07	Up to 0.042	1.52	0.060	1.27	0.050[b]	Up to 0.84	Up to 0.033	1.52	0.060	1.27	**0.050[b]**
	Over 1.40	Over 0.042	1.4*t*	1.4*t*	1.4*t*	1.4*t*	Over 0.84	Over 0.033	1.8*t*	1.8*t*	1.8*t*	**1.8*t***
Stainless, silicon and spring steels	Up to 1.40	Up to 0.042	1.52 min	0.060 min	1.52 min	0.060 min	Up to 0.84	Up to 0.033	1.52 min	0.060 min	1.52 min	**0.060 min**
	Over 1.40	Over 0.042	1.4*t*	1.4*t*	1.4*t*	1.4*t*	Over 0.84	Over 0.033	1.8*t*	1.8*t*	1.8*t*	**1.8*t***
Nickel-base magnetically soft alloys	**All**	**All**	**1.52**	**0.060**	**1.52**	**0.060**	**All**	**All**	**1*t***	**1*t*[c]**	**1*t***	**1*t*[c]**

(a) Allowance between blanks in the same row and also between blanks of the first and second rows.

(b) Allow 1.52 mm (0.060 in.) between blanks at first and second rows.

(c) When the blank edge is parallel to the edge of the stock or when the length of the skeleton segment between blanks is more than 4*t*, scrap allowance is 1.8*t*.

The percentage of scrap in a strip layout can be calculated as:

$$100\left(1-\frac{A_B}{A_S}\right) \quad \textbf{(Eq 5)}$$

where A_B is the area of blanks produced in one press stroke and A_S is the area of strip consumed by one press stroke, or strip width times feed length.

Round blanks can be staggered in rows, at the same spacing as for hexagons (Fig. 11), for the most efficient blanking from a long strip. With such a layout, 20 to 40% more blanks can be made from a given amount of material than by blanking each circle from a separate square. With the layout shown in Fig. 11, each press stroke (after the third stroke) produces four blanks--spaced to provide enough room for mounting the punches and dies.

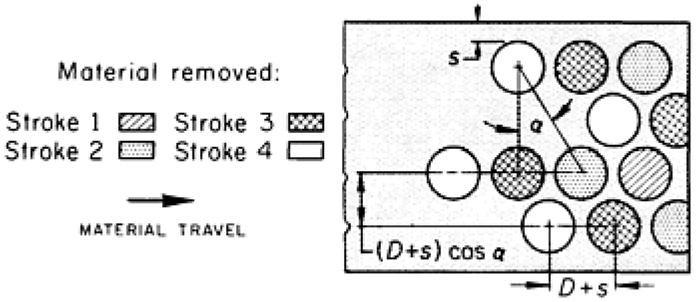


Fig. 11 Strip layout for blanking four circles per stroke with minimal material waste

The percentage of scrap loss for the layout of Fig. 11 can be calculated using Eq 5 and the following:

$$\begin{aligned} A_B &= \frac{n\pi D^2}{4} \\ A_S &= wl \\ l &= D + s \\ w &= (D + 2s) + (n - 1)(D + s)\cos\alpha \end{aligned} \qquad \textbf{(Eq 6)}$$

where n is the number of rows of blanks across the strip width, D is the blank diameter (in inches), l is the feed length (one blank made in each row per press stroke) (in inches), w is the strip width (in inches), s is the scrap allowance from the edge of the strip to the blank and between blanks (in inches), and α is the angular displacement between blanks (in degrees). The area of the holes pierced in a blank is not considered in these calculations, because it does not affect the efficiency of the layout.

Rectangular blanks can generally be laid out more easily than other shapes.

Odd-shape blanks are generally more difficult to lay out for the greatest economy.

Nesting, or the interlocking of blanks in the layout to save material, should be done wherever the shape of the blank permits. Nesting is possible with many irregular blanks.

Figure 12 shows a layout in which irregular blanks are nested so that an appreciable amount of material is saved. A double die can be used with such a layout, blanking two pieces per stroke. A single die can be used for short runs; the strip is turned around after the first pass and fed through the die again.

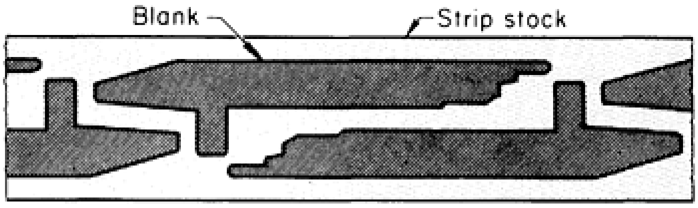


Fig. 12 Nesting of irregular blanks in layout to save material

Another way of nesting blanks is a layout such as that illustrated in Fig. 13. With this layout, three punches cut four pieces per stroke (after the third press stroke) in a shearing action that produces no scrap except at the ends of the strip. Other strip layouts in which the blanks have been nested are shown in the articles "Press Bending of Low-Carbon Steel" and "Press Forming of Low-Carbon Steel" in this Volume.

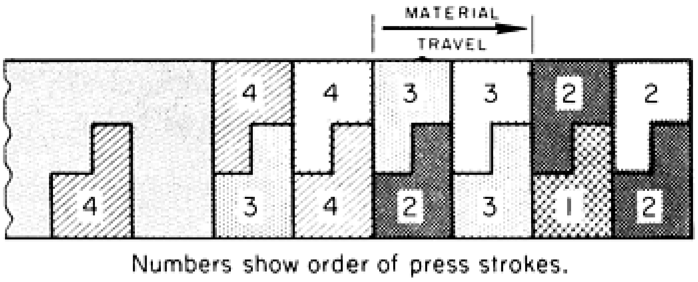


Fig. 13 Layout for the scrapless blanking of four blanks per press stroke, using three punches. The three shaded shapes for each press stroke denote the blanks produced directly by the three punches. The unshaded shape for each press stroke denotes a blank produced by the action of adjacent punches.

Use of Full Stock Width. Blanks that have two parallel sides can sometimes be made most economically in a layout that uses the full width of the stock. The remaining outline of the blank is produced by shearing, lancing, notching, parting, or a combination of these.

Effect of Rolling Direction. For blanks that must later be bent or formed, consideration must be given in layout to the orientation of the blanks with respect to the direction of rolling (grain direction). Ideally, blanks should be laid out so that severe bends are made with the bend axis at right angles to the direction of rolling or, if this is not practical, with the bend axis at an angle to the direction of rolling. Stretching should be in the direction of rolling, whenever possible. Examples and illustrations of blank layouts made to take advantage of the direction of rolling are provided in the articles "Press Bending of Low-Carbon Steel" and "Press Forming of Low-Carbon Steel" in this Volume.

Welded Blanks

Welded blanks are used when they have advantages over one-piece blanks, as in the following situations:

- The welded blank may cost less than an equivalent one-piece blank if scrap or other low-cost metal can be used to make the welded blank or if tooling and production for the one-piece blank cost more than for the welded blank.
- Stock for a welded blank may be more readily available than stock for an equivalent one-piece blank.
- The blank may have a shape that would waste more material if it were made in one piece instead of being welded. Material can sometimes be saved by welding projecting portions, such as tabs and ears, to simpler shapes.
- The welded blank, when used in subsequent forming operations, may reduce the cost of tooling. Flat or simple shapes are welded in a layout designed to avoid the presence of seams in certain portions of the blank and to permit automatic welding, if possible.

Large blanks that would cost extra because of width or for other reasons if they were made in one piece can sometimes be made at less cost by welding.

Difficult shapes that would waste a considerable amount of material if they were made in one piece can sometimes be made by welding two or more simple blanks together. In the application described in the following example, two developed blanks were welded and then formed into a bent channel.

Example 2: Use of Two Gas Metal Arc Welded Blanks to Form an Automobile-Frame Rail.

Figure 14 shows a side rail for an automobile frame that was formed from 5.05 mm (0.199 in.) thick hot-rolled commercial-quality 1008 steel. The blank for this rail had a shape that could be made in one piece only with excessive waste of metal in scrap. The two pieces used were blanked from nested layouts with little scrap waste.

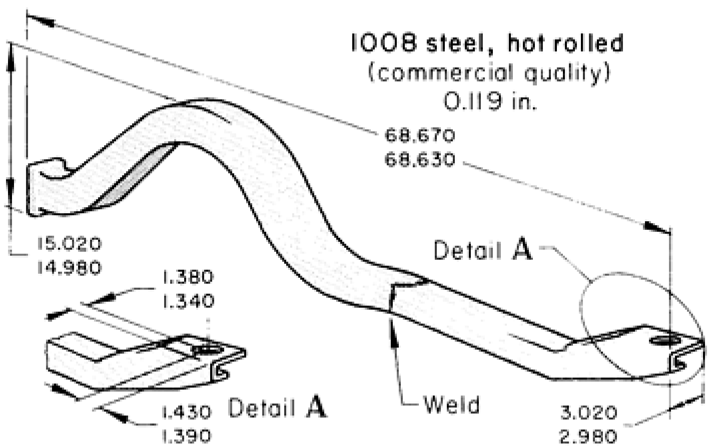


Fig. 14 Automobile-frame side rail that was formed from two blanks joined by gas metal arc welding. Dimensions given in inches.

Blanking was done in a die that made both pieces in a single press stroke. The two portions were butted and joined by high-speed automatic gas metal arc welding and then formed. Tolerance on all dimensions shown in Fig. 14 was ±0.51 mm (±0.020 in.).

The savings in metal exceeded the cost of welding, but if production needs had been fewer than 10,000 pieces, the savings in metal would not have paid for the welding and the blanking dies. The production quantity was 300,000 rails made in lot sizes of 20,000 pieces.

Open Shapes. A blank with a large cutout can sometimes be made at less cost than a one-piece blank by welding simpler pieces together.

Waste metal can sometimes be joined by welding to make a blank that costs less than a one-piece blank.

Welding methods used in making blanks include resistance welding methods (lap-seam, spot, foil butt-seam, mash-seam, flash, and high-frequency butt) and fusion welding.

Lap-seam welding (wheel electrodes) is a frequently used method. Tooling is simple if the components are joined first by tack welding. The disadvantages of lap-seam welding are loose edges, and joints that are double the thickness of the work metal.

Spot welding is fast and needs only simple, inexpensive tooling. The disadvantages of spot welding are loose edges, and joints that are not tight and are not as strong as those made by other methods.

Foil butt-seam welding is a fast method that makes smooth, tight joints with no loose edges. Its disadvantages include the cost of adding foil to one or both sides of the seam and the need to use starting tabs to make strong joints.

Mash-seam welding makes smooth joints that often need no grinding and tight joints that have no loose edges. Disadvantages include the short life of electrodes, and the high cost of tooling resulting from the difficulty in maintaining the small overlap.

Flash welding uses simple tooling; the joints are tight and free from loose edges. However, the length of joint that can be produced by flash welding is limited, and the joints are rough and therefore must be ground before the weldment can be worked in a die.

High-frequency butt welding is fast and can be used to join two dissimilar metals. Electrode life is good. However, costly equipment is required, and the technique is generally suitable only for mass production.

Fusion welding methods include gas metal arc welding and gas tungsten arc welding.

Presses

Most blanking is done in single-action mechanical presses. Some dies can be used only with a particular type of press; the die is usually made to suit a specific press. The force capacity rating must be adequate for the work and must be well above the calculated cutting force (see the section "Calculation of Force Requirements" in this article). Press capacities are given in kilonewtons (tons of force) at a certain distance above the bottom of the stroke. This distance must suit the die and the operation. Most blanking is done near the bottom of the stroke where the available force is greatest. In compound dies, blanking may be done near midstroke, where the available force is much lower than that at the bottom.

Size of bed, shut height, stroke length, and speed must all be suitable for the die and the work. Some types of dies can be run at high speeds, and some need moderate or slow speeds, as discussed in the following section in this article.

Construction and Use of Short-Run Dies

Small and medium quantities of blanks are often produced in punch presses by the use of inexpensive short-run dies. These include steel-rule dies, template dies (sometimes called plate dies or continental dies), and subpress dies. Although most applications of such dies are for production quantities of a few hundred to 10,000 pieces, suitably constructed dies of these types have been used for quantities of 100,000 pieces or more.

Short-run dies are used to a limited extent to blank initial quantities of parts that are to be mass produced. Because they can be made and put into operation more quickly than conventional dies, short-run dies make it possible to expedite the delivery of completed parts.

In addition, short-run dies are used to produce trial lots of parts that may be subject to extensive changes in design. If the trial lots show that die design changes are needed, the changes can be made at less cost before the conventional die is completed. After the conventional die has been set up, either the entire production can be transferred to it or both dies can be used.

For small quantities (<100 pieces), even the most inexpensive short-run blanking die may not be justified. Such small quantities of blanks are generally cut at less cost with standard tools, such as a nibbler, a squaring shear, or a rotary shear. Small quantities of blanks can also be made by contour band sawing, routing, gas cutting, filing, or machining.

Steel-rule dies are simple, inexpensive dies that are made by setting thin, bevel-edge strips of high-carbon tool steel on edge to outline the blank. The rule is set tightly into a slot in plain or impregnated plywood, and the plywood is backed by a steel subplate, as shown in Fig. 15. The die plate or template is attached directly to a steel subplate, and both upper and lower subplates are fastened to master die shoes, which are mounted in a conventional press.

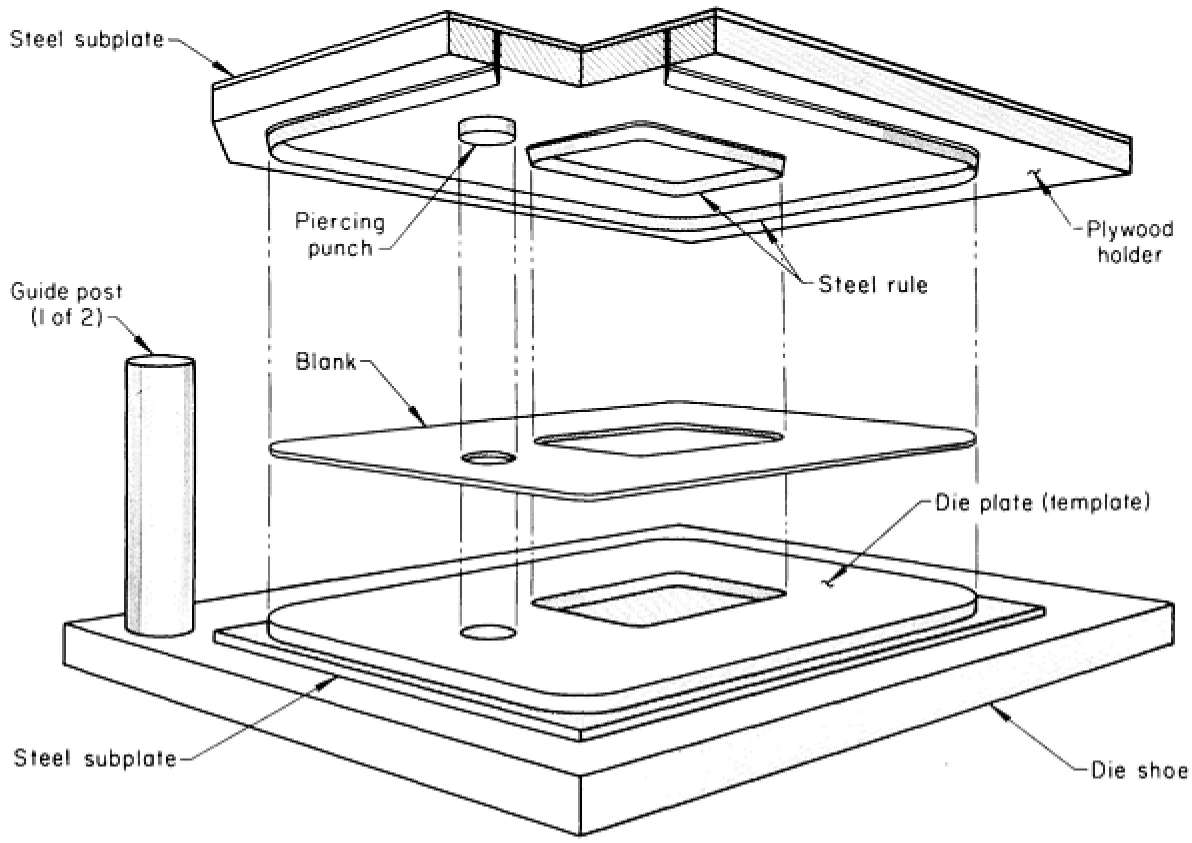


Fig. 15 Exploded view of a steel-rule die. See text for discussion.

Steel-rule dies are used for blanking, piercing, notching, and shallow forming. For work on flat blanks or on flat, sheared stock of low-carbon steel up to about 13 mm ($\frac{1}{2}$ in.) thick, a steel-rule die can usually be made more rapidly and at less cost than any other kind of die. Blanks as large as 1.2 × 2.1 m (4 × 7 ft) have been made in steel-rule dies.

Die. The steel rule that is used as the die is made of high-carbon steel or of a tool steel such as W1 or W2, in spring temper and other hardnesses. It is available in stock lengths in several thicknesses, ranging from 0.36 to 4.32 mm (0.014 to 0.170 in.), and in widths of 31.8 mm (1.25 in.) and narrower. Printers' rules of thickness from 1 to 12 points (0.36 to 4.32 mm, or 0.014 to 0.170 in.) are sometimes used. The finished rule usually has a square back edge, and a cutting edge that is ground to a 45° bevel or to a V-edge.

The back edge is fitted tightly into sawed slots in hard plywood as shown in Fig. 15 so that it will cut the outline of the blank. The steel subplate is used to back up the steel rule and to support the plywood.

Punch. For blanking low-carbon steel, a die plate (high-carbon or tool steel template) of the same shape as the required blank is used as the punch, opposing the steel rule. Other punch elements and die parts are added as needed to the die for piercing holes and slots at the same time that the blanking is done. Solid steel blocks, instead of a steel rule, can be used in the die to cut sharp corners and notches in the blank. For some work, including the cutting of paper and other soft materials, the punch can be a block of hard wood or a thick sheet of rubber or other soft material with a working surface that extends beyond the area enclosed by the steel rule.

Stripper. For cutting paper and leather, the steel-rule die is stripped by elastic material, such as sponge rubber, which is added to the die. In blanking low-carbon steel, blocks of tougher solid rubber can be used as strippers. Positive spring-loaded steel stripper plates are also used.

The accuracy of the blanks produced in steel-rule dies depends mainly on the skill of the diemaker and the care used in their construction. For noncritical parts blanked in steel-rule dies, the tolerance may be as large as ±0.8 mm ($\pm\frac{1}{32}$ in.); for more critical parts, the work can be located accurately to maintain a tolerance of ±0.13 mm (±0.005 in.). Closer tolerances on blanks can be obtained at increased cost by using rotary-head millers or jig boring machines in constructing the dies.

Because holes and slots made by steel-rule blanking are pierced with conventional punch and die elements that are added to the steel-rule die, they can be produced to the same tolerances as in conventional blanking. Steel-rule dies commonly blank laminations with burrs only 0.05 mm (0.002 in.) high.

Cost. A steel-rule die made to blank low-to-moderate quantities of low-carbon steel generally costs about 20% as much as a conventional die made for the mass production of similar work. The following example describes the use of a steel-rule die for stopgap and trial production of an automobile part. A die change was made inexpensively; after it was proved successful in the steel-rule die, the same change was then included in the design of the conventional die for production use.

Example 3: Use of a Steel-Rule Die for Temporary Production.

A steel-rule die was used to blank a part for an automobile frame in order to begin production without waiting until the conventional die could be delivered. It was expected that the steel-rule die would have to produce 325 blanks before production could be changed to the conventional die.

The die, shown in Fig. 16, was used in a 2.2 MN (250 tonf) straight-side press to blank annealed cold-rolled low-carbon steel, 3.96 mm (0.156 in.) thick by 229 × 229 mm (9 × 9 in.), with three 14 mm ($\frac{9}{16}$ in.) diam holes and three round-end slots 16 × 38 mm ($\frac{5}{8}$ × 1$\frac{1}{2}$ in.). Tolerances were ±0.81 mm (±$\frac{1}{32}$ in.) on the blank outline, and ±0.13 mm (±0.005 in.) on the pierced holes and slots. No burr limits were specified.

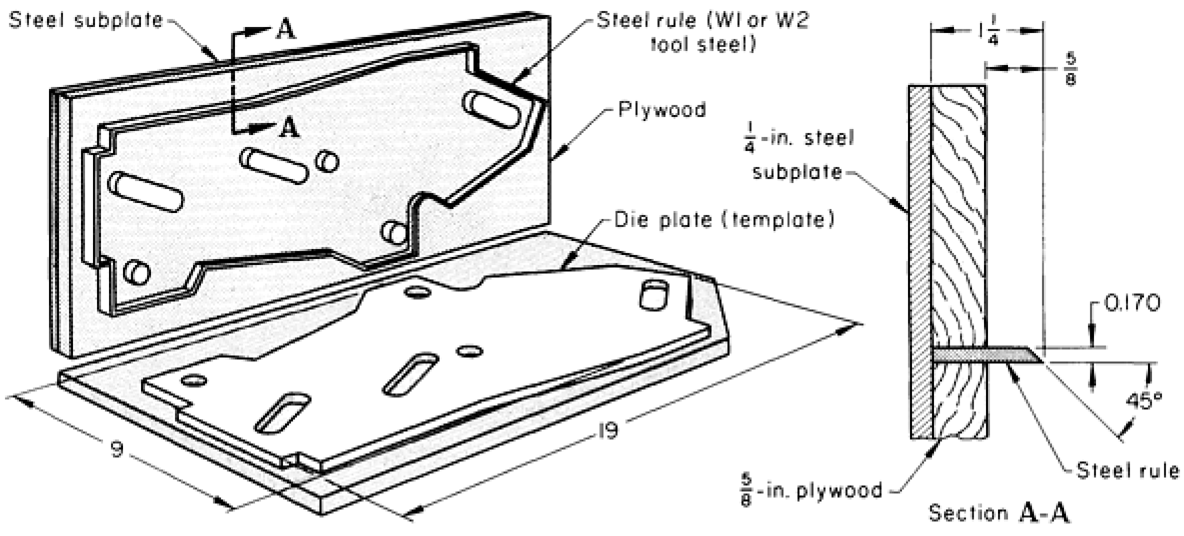


Fig. 16 Steel-rule die that was used as temporary tooling for the blanking of low-carbon steel sheet 3.96 mm (0.156 in.) thick. Dimensions given in inches

The steel rule was made of 12-point rule stock (4.32 mm, or 0.170 in., thick × 32 mm, or 1$\frac{1}{4}$ in., wide) set full depth into hard plywood 16 mm ($\frac{5}{8}$ in.) thick. A die plate of steel, which fit inside the rule, was used as the punch. The die was made by measuring a developed formed blank.

The ease and low cost of making a change in a steel-rule die proved important in this application because it was decided that one of the holes would not be needed. The punch that had been added for that hole was simply removed from the steel-rule die. After tryout, that hole was also eliminated in the conventional die before it was completed. The change, if made after the conventional die had been completed, would have cost much more.

The cost of the steel-rule die was 20% of the cost of the conventional die. Because the steel-rule die was fed and unloaded by hand, production was only a few pieces per minute. More than 1000 blanks were made in this steel-rule die.

Template dies (also called plate dies) are competitive with steel-rule dies in terms of cost and the quantity they can produce. Figure 17 shows an exploded view of the elements of a template die. Punches and die elements can be added, as in steel-rule dies, to combine piercing with blanking.

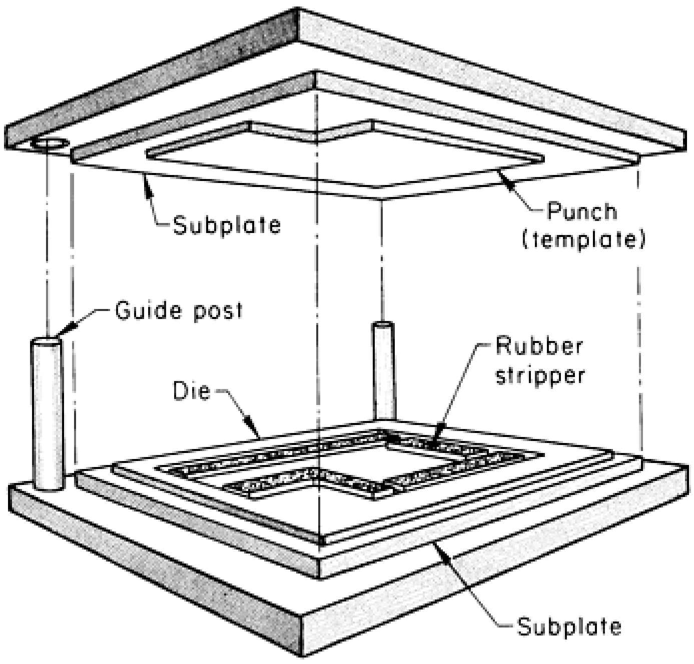


Fig. 17 Exploded view of a template die

Punch. The punch or template (Fig. 17) is made to fit the outline of the blank to be produced. The punch is usually made of medium-carbon steel plate (1040, 1050, or 4140) or of ground flat stock such as O2 tool steel, on which the edges may be flame hardened. In more difficult applications or where longer life is needed, the punch can be made of D2 or equivalent tool steel and hardened.

Die. The die is usually assembled of doweled, hardened blocks of steel, ground to fit the punch with proper clearance. The same materials are used as for the punch.

Construction. Typical clearance for template dies is 0.076 mm (0.003 in.). The construction shown in Fig. 17 is satisfactory for blanking low-carbon steel in the same types of applications as steel-rule dies. For severe blanking, a template die can be made stronger by using one-piece construction or adding pins to prevent the die blocks from spreading, and by nesting the die or die segments into a recess in the die backing plate (subplate).

Operation. A continuous strip of stock can be fed into a template die, feeding the stock against a stop for each press stroke and using a side guide for the stock. Blocks of tough rubber are usually used as strippers (Fig. 17), pushing the blank back into the strip so that the blank is removed from the die by the feed motion of the strip. A typical application of template dies is described in the following example.

Example 4: Blanking Laminations in a Template Die.

Motor laminations of 1.52 mm (0.060 in.) thick pickled hot-rolled low-carbon steel were blanked in a template die. The blank measured 318 × 140 mm (12.5 × 5.5 in.). The punch was made of a one-piece template, except for inserts that were provided where changes might be needed. The die was made with 0.076 mm (0.003 in.) clearance all around the punch (template).

The die was made of D2 tool steel. Rubber blocks were used for strippers, which pushed the blank back into the skeleton in the vertical press so that the blank was unloaded by the feeding of the stock. Hand feeding produced fewer than ten pieces per minute.

The burr was about 0.051 mm (0.002 in.)--well below the specified limit of 0.102 mm (0.004 in.). Estimated die life was 100,000 pieces.

Subpress dies are short-run die sets that are attached to the press bed, but in which the punch shoe is not attached to the ram. They require less setup time than conventional dies, but have a lower production rate. The length of stroke of a subpress die is limited because springs are used to raise the punch. Subpress dies are sometimes used to blank the precision parts used in instruments and timepieces.

Self-contained notching tools can be purchased ready to install as subpress tools. Notching units, consisting of both punch and die mounted in individual C-frame units, are available in a variety of standard-corner, V, and square-edge sizes for notching low-carbon steel in thicknesses to 6.4 mm ($\frac{1}{4}$ in.). Special shapes of irregular outline can be incorporated into standard notching units. The notching units can be used singly or in groups, and they can be used in combination with piercing tools of similar construction.

Each unit is self-contained and self-stripping by means of springs. The punches are held in close alignment and are not attached to the ram of the press. Each unit is located, pinned, and bolted to a die plate, template, or T-slot plate and is mounted on the bed of any type of press or press brake of adequate shut height.

This type of subpress tool can be used to make small blanks, but it is more commonly used to notch and pierce precut blanks. The units can be reused to produce parts of different shapes by relocating the tools.

Construction and Use of Conventional Dies

Conventional blanking dies consist basically of one or more mating pairs of rigid punches and dies and are the standard tooling for the production blanking of sheet metal in a press. Mating pairs of metal punches and dies are combined in various ways, and additional components are added to make up compound, progressive, transfer, and multiple dies.

Conventional dies are costly, specialized tools that are generally used for only one product, but they are so efficient, accurate, and productive that they are typically the best method of mass production at the lowest cost per piece. They are occasionally used for short-run production when tolerances are exceptionally stringent or when other reasons make the use of short-run dies impractical.

Conventional dies are more accurate than most short-run tooling, and they retain their accuracy for a greater number of pieces. They can also usually be resharpened after wear has affected their action or the quality of the work. Before dies are worn out, they have generally been resharpened many times. Conventional dies commonly produce several million blanks before replacement.

Tool materials used for blanking low-carbon steel sheet in conventional dies include (in order of increasing lot size for which they are recommended) 1020 steel; W1, O1, A2, and D2 tool steels; and, for extremely long runs, carbide. For long runs on steel thicker than about 6.4 mm ($\frac{1}{4}$ in.), M2 tool steel is often used instead of carbide because of the limited shock resistance of carbide. Type D2 tool steel is probably the most commonly used and most widely available tool material for the mass-production blanking of steel and other metals.

Cold-rolled sheet and hot-rolled pickled-and-oiled sheet are far less damaging to tools than gritblasted or hot-rolled unpickled surfaces. Tool materials that have a high resistance to abrasion, such as A2 or D2 tool steel, are recommended for use in tools for the blanking of sizable production lots of hot-rolled unpickled steel. Detailed information on the selection of tool materials and on tool life is available in the article "Selection of Material for Blanking and Piercing Dies" in this Volume.

Single-Operation Dies. The simplest conventional blanking dies are single-operation dies. They are used as separate units to produce blanks or as parts of more complex dies that perform several operations on a workpiece. The separate stations of a progressive die are similar to single-operation dies (although integrally constructed), and transfer presses use many single-operation dies.

The drop-through die is one of the commonest types (Fig. 18). In this die, the severed blank is forced through the die opening by the downward motion of the punch, and it drops through into a chute or container. This type of blanking die has a minimum number of parts and is relatively inexpensive. Another major advantage of drop-through blanking dies is their simple and reliable blank ejection system, which is usually compatible with the use of this type of construction in progressive or transfer dies. In other types of dies, the ejection system may be more complicated than the die itself.

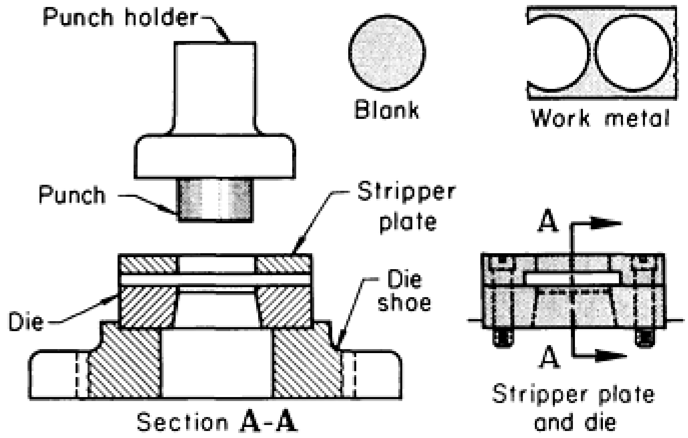


Fig. 18 Elements of a conventional drop-through blanking die

The disadvantages of drop-through dies for blanking include the following:

- Unless parallels are placed between the die and bolster, blanks must be small enough to go through the hole in the bed
- Blanks may distort by dishing
- Some shapes make drop-through difficult
- The die must be on the lower shoe and the punch on the upper shoe of the die set

Two other types of single-operation blanking dies--inverted and return dies--can be used when, because of size or susceptibility to damage, blanks or workpieces cannot be unloaded by dropping through the die, but can be removed between the die and punch faces.

Inverted dies (Fig. 19) have the punch on the lower shoe and the die on the upper shoe. A knockout pin releases the blank from the die, and the blank is removed mechanically or manually from the top of the punch. A scrap cutter is usually included so that the scrap can be blown or knocked away. The scrap is sometimes allowed to stack up in successive collars around the punch and is stripped off manually after a number of pieces have been blanked.

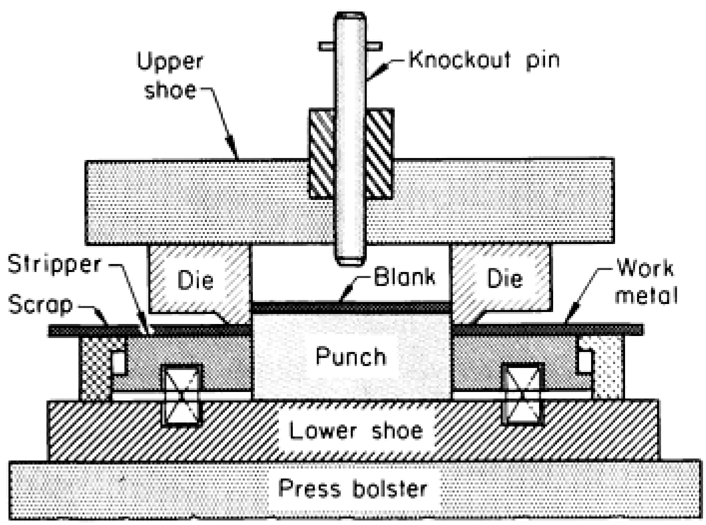


Fig. 19 Inverted blanking die with the punch on the lower shoe and the die on the upper shoe

Return dies (Fig. 20) are made in the usual way, with the punch on the upper shoe and the die on the lower shoe. The punch shears the blank and presses it into the die cavity, as with other types of blanking dies. A spring-loaded pressure plate or die cushion acts as an ejector for the die, returning the blank to the surface of the die, where it can be picked off manually or mechanically. A spring-loaded plate on the upper shoe acts as a blankholder on the downstroke and as a punch stripper on the upstroke. Like many inverted dies, a variation of the return die shown in Fig. 20 includes a scrap cutter (not shown in Fig. 20) that parts the ring of scrap so that it can drop away, can be blown away, or can be removed mechanically.

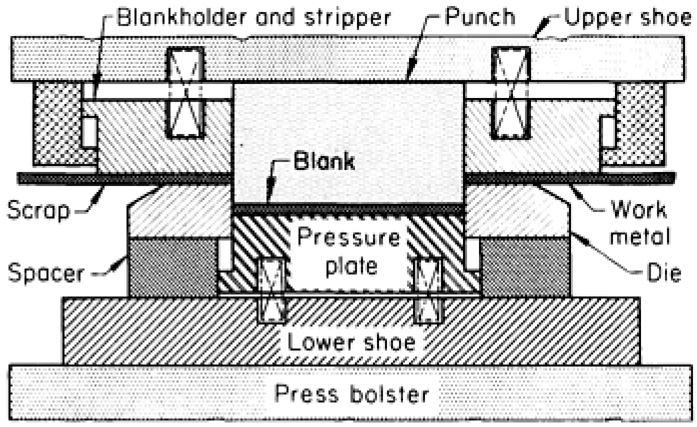


Fig. 20 Return blanking die with spring-loaded pressure plate that acts as a die cushion and an ejector for the die

Inverted and return dies have the advantage of not needing a clearance hole to let the workpiece or blank drop through. The main disadvantages of inverted and return dies are:

- They are more expensive than drop-through dies because they have more parts
- They may require careful adjustment and synchronization of external ejectors and air blasts, which adds to setup costs

Of the two types, inverted dies are generally simpler to construct and have less complicated knockout mechanisms than the pressure-plate or die-cushion ejectors in return dies.

Return dies are better suited to continuous strip operation because the strip remains in line and is not pressed down by the die over the punch. If the workpiece must be clamped before blanking, the blankholder or the pressure plate (or both) in a return die holds the workpiece or blank through the entire working stroke.

Instead of being removed by the methods described above, the severed blank is sometimes pushed back (completely or partly) into the strip to be removed later, as is sometimes done in a progressive die. Pushback can also be used for other purposes, such as to provide knockouts for fitting in electrical panels or junction boxes and in other sheet metal products. A flattening operation is sometimes added to assist pushback.

Compound dies perform several operations on the same workpiece in the same stroke of the press--such as blank and pierce, or blank, pierce, and form. Figure 21 shows the elements of a compound die for simultaneous blanking and piercing. In this die, the blanking punch is in the bottom; a hole in the punch is used as the piercing die. The piercing punch and the blanking die are in the top.

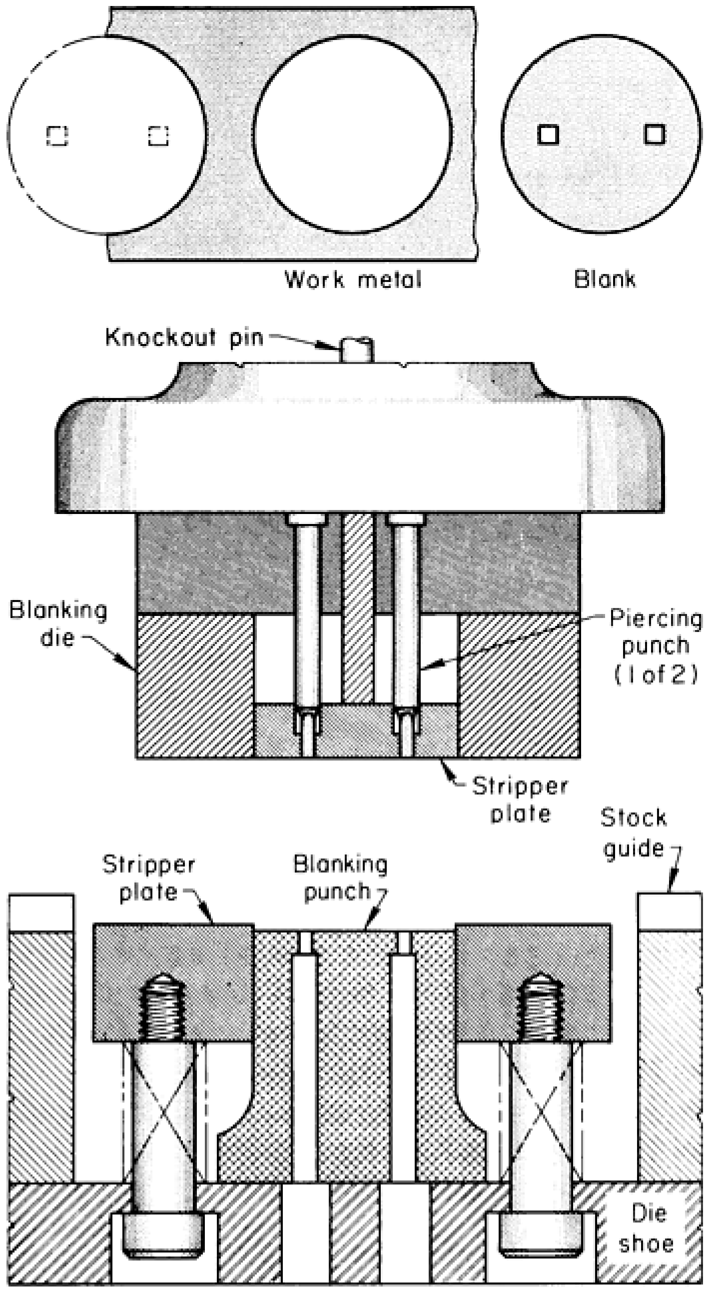


Fig. 21 Elements of a compound die for simultaneous blanking and piercing

Compound dies are generally more economical in mass production than a series of one-operation dies, and they are usually more accurate. For example, a compound die that blanks and pierces a workpiece can hold the spacing between pierced holes or the relation of the pierced hole to the edge of the blank more accurately than would be possible if the individual operations were done in separate dies. This is because of possible variation in locating the blank for piercing or in locating a prepierced strip for blanking.

Because the complexity of operation causes greater difficulties in unloading the workpiece, compound dies usually run slower than single-operation dies; the maximum speed of a compound die is about 250 strokes per minute. Other disadvantages of compound dies in comparison to single-operation dies are that they are more specialized (so that a change in the product is more likely to make the die obsolete) and that initial and maintenance costs are both higher. An advantage of compound dies is that, because of their slower operation, they generally produce more pieces per sharpening than single-operation dies.

A complex compound die can sometimes be more economical than two simpler compound dies in making the same part. This is illustrated in the following example.

Example 5: Replacing Two Two-Operation Dies With a Four-Operation Die.

The cup shown in Fig. 22 was originally produced from annealed cold-rolled low-carbon steel in two compound dies in separate press operations. The first die was a blank-and-draw die; the second was a pierce-and-pinch-trim die. Two separate dies were used on the assumption that the large pierced hole in the cup would not leave enough tool thickness to sustain all four operations. A 400 kN (45 tonf) open-back inclinable press was used for each of the two operations at a production rate of 500 pieces per hour.

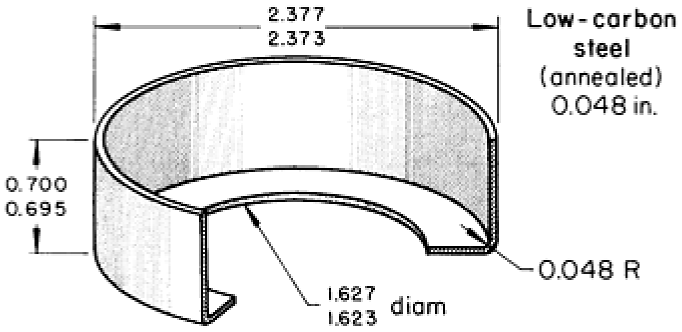


Fig. 22 Cup that was blanked, drawn, pierced, and trimmed in a compound die. This part was previously made in two compound dies: blank and draw, and trim and pierce. Dimensions given in inches

Examination of the procedure led to the design of a die to blank, draw, pierce, and trim--all in one press stroke. The same press was used at the same speed, thus cutting the labor cost per piece in half. Tooling cost also was reduced because the cost of the new die was less than the combined cost of the original two dies.

Production Applications. The following example describes production applications in which compound dies were used for blanking and other operations because of their inherent accuracy.

Example 6: Blanking, Forming, and Piercing a Brake-Drum Back in Compound Dies.

An automobile brake-drum back (Fig. 23) was formed from hot-rolled 1012 steel in two press operations. A compound die was used to blank and notch the part in a 1.8 MN (200 tonf) press at the rate of $17\frac{1}{2}$ strokes per minute, making one part per stroke. The tolerances shown in Fig. 23 were maintained in production with ordinary shop practice, as was the tolerance on the 14° 24' angle (±0° 30'). The die, made of vanadium tool steel, required reworking after 50,000 pieces. Chlorinated oils were used as lubricant.

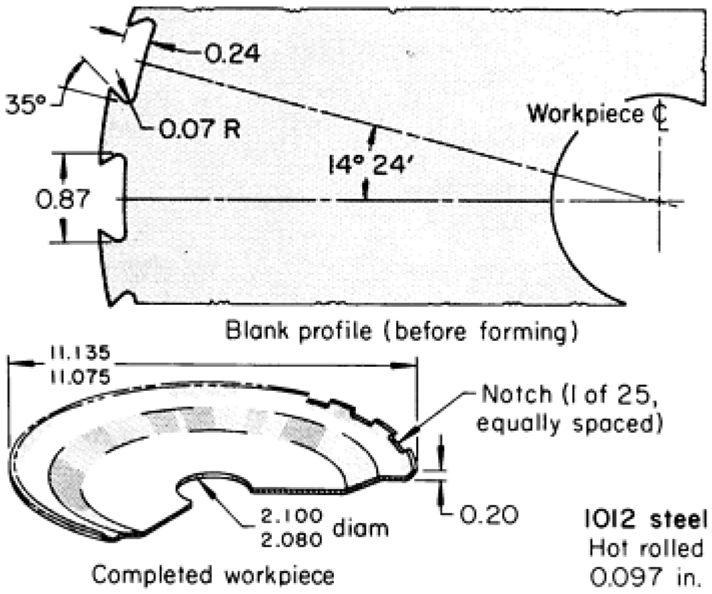


Fig. 23 Brake-drum back that was produced in two press operations, in a blank-and-notch die and in a restrike-and-form die. Dimensions given in inches

In the second press operation, a compound die in a 3.6 MN (400 tonf) press formed the workpiece, pierced the center hole, and flattened the workpiece at the notches. This die was made of tungsten oil-hardening tool steel. Production rates, lubrication, and die life were the same as in the blank-and-notch operation.

Progressive Dies. In a progressive die, the workpiece, while attached to the strip (or to the scrap skeleton), is fed from station to station at each stroke. At each stroke, the die performs work at some or all of the stations. The workpiece is cut off and unloaded at the last station. Each station can be simple or compound. Figure 24 shows the principal parts of a two-station blank-and-pierce progressive die.

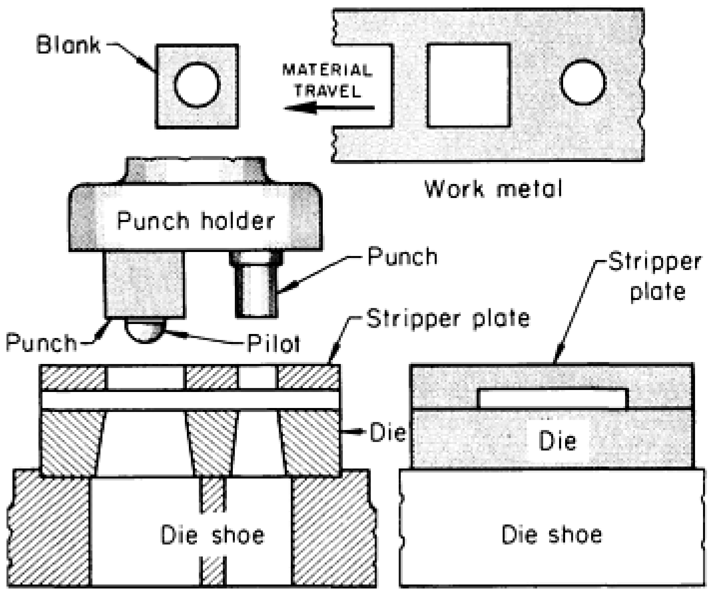


Fig. 24 Principal components of a two-station blank-and-pierce progressive die. Pierced blank and work metal (steel strip) are shown at top. The use of a pilot in the pierced hole ensures accuracy to within a few thousandths of an inch.

In producing the simple blank shown in Fig. 24 from coiled strip in a progressive die, the round hole is pierced at the first station. The strip is then fed left, to the next station. There the pilot enters the hole as the blanking punch moves down to complete the blank. Accurate relation of the hole to the outline of the blank depends on accurate fit of the pilot in the hole. The completed part is not separated from the strip until the last operation, regardless of the number of operations. After the first piece, one piece is completed at each stroke.

A progressive die is expensive, and because it is usually set up in an automatic press with scrap cutter, feeder, straightener, and uncoiler, the total cost of the auxiliary equipment is also high. Other disadvantages of progressive dies are:

- The part cannot be turned over between operations
- Material may be wasted because the workpiece may not nest well in the strip layout

Coil stock (or, less often, flat strip stock) is used. Many operations on parts of small and medium size can be done in conjunction with blanking in a progressive die, but the planning may be complicated. Soft or thin stock may be troublesome because the pilot may distort the locating holes. Setup and maintenance may be difficult.

The following example describes an application in which a progressive die was preferred to a compound die because it was too difficult to cut sharp corners in the compound die.

Example 7: Change From a Compound to a Progressive Die to Cut Sharp Corners.

Figure 25 shows a fast-idle cam on which the corners had to be sharp. A progressive die was used because it was difficult to make and maintain sharp corners in a compound die. The part was blanked from coiled 1010 steel strip, 3.05 × 60 mm (0.120 × $2\frac{3}{8}$ in.), with maximum hardness of 55 HRB. A seven-station progressive die cut the steps in the cam.

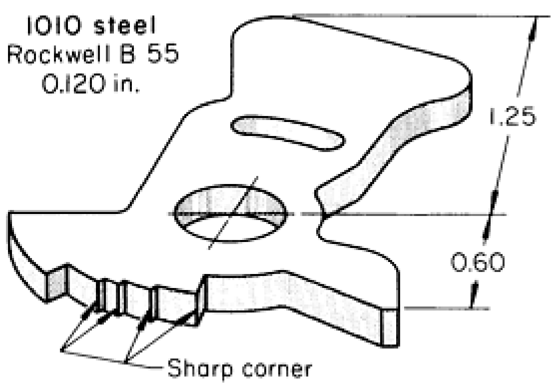


Fig. 25 Fast-idle cam on which sharp corners were cut in a progressive die. Dimensions given in inches

The punches were made of M2 tool steel and hardened to 60 to 63 HRC, and the die and the punch holders were made of O1 tool steel and hardened to 56 to 58 HRC. Approximately 10,000 to 15,000 cams were made between sharpenings of the progressive die, using a punch-to-die clearance of 0.10 mm (0.004 in.). This die was mounted in a 530 kN (60 tonf) press that was run at 76 strokes per minute.

In another operation, the steps in the cam were shaved to remove die break so that surfaces were flat across the thickness. Tools were of the same materials as in the progressive die and had a life of 5000 to 6000 pieces between sharpenings. The shaving was done at 16 strokes per minute in a 360 kN (40 tonf) press. Finally, the cam was liquid carburized 0.08 to 0.15 mm (0.003 to 0.006 in.) deep and oil quenched to file hardness. Zinc plating (0.005 mm, or 0.0002 in., minimum thickness) and a chromate postplating treatment followed.

Transfer dies, in which separate workpieces are fed from station to station by transfer fingers, are used for blanking only when coil stock is used. Blanking is done at the first station and is followed by other operations.

Transfer dies are typically used for additional operations on precut blanks made in a separate press (blanks that permitted close nesting for best use of the stock). When equally high utilization of stock can be obtained, coil stock can be used in a transfer die, with blanking done at the first station.

Like progressive dies, transfer dies and their related equipment (presses, special attachments, and feeding devices) are expensive and are best suited to mass production. Production rates are high.

Multiple dies (also called multiple-part dies) make two or more workpieces at each stroke of the press. The workpieces can be pairs of right-hand and left-hand parts, duplicate parts, or unrelated parts. Punch height can be staggered to reduce shock and blanking noise. Such dies are used in mass production.

Multiple dies can be multiples of single-operation dies or multiples of compound dies. They generally cost only slightly more than similar dies that make only one part per stroke. A die that makes two parts per stroke may cost only 5% more than a die that makes only one.

Such dies are primarily used for blank-and-form sequences. Draw operations are more difficult to combine with blanking or other operations because of blankholder needs and slower draw operation. Unloading of the work is sometimes difficult.

The use of multiple dies depends on the size and shape of the workpiece, size of the press, production quantity, possible savings in material and labor, and costs for setup and maintenance. Advantages of multiple dies include savings in material by better blank layout, reduced labor costs, and increased production. Disadvantages include increased setup and maintenance costs.

It is often better to increase the production of a single-part die by some simple change, such as putting the die in a faster press, rather than to replace the single-part die with a multiple die. A multiple die may also have to be run slower than the simpler die if a press of greater force capacity must be used to provide ample force. The use of a multiple compound die to blank, pierce, and form three pieces per stroke is illustrated in the following example.

Example 8: Blanking, Piercing, and Forming Three Pieces per Stroke in a Multiple Compound Die.

A multiple compound die was used to make a thick dished washer with three flats equally spaced on the edge circle, as shown in Fig. 26. The part was made of hot-rolled 1008 or 1010 steel, 2.36 ± 0.18 mm (0.093 ± 0.007 in.) thick.

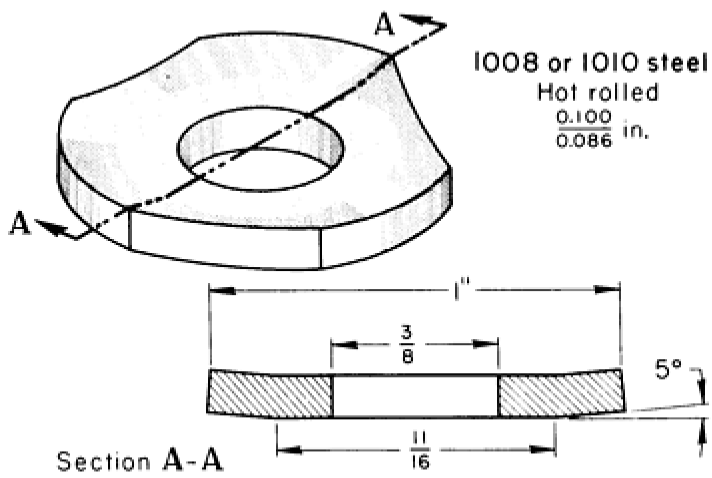


Fig. 26 Dished washer that was blanked, pierced, and formed, three pieces per stroke, in a multiple compound die. Dimensions given in inches

Operations in the compound die were blank, pierce, and form. The blanks were nested so closely in the stock layout that the pilot holes (half-circles) had to be notched in the edge of the stock. Three parts were made with each stroke of the die, and the parts were pushed partly back into the scrap skeleton so that they were carried out of the press for unloading. Production was 500,000 pieces per month. The die, made of D2 tool steel, needed to be resharpened after 150,000 strokes (450,000 pieces) and required reconditioning (replacement of some parts) after 3.5 million pieces.

Blanking of Low-Carbon Steel

Operating Conditions

To achieve high productivity and low unit cost, most blanking is done in high-speed mechanical presses. Speeds as high as 1200 strokes per minute are used. The equipment for high-speed blanking ordinarily includes a short-stroke press, automatic feed devices, and dies designed for bottom ejection.

In most blanking operations, press speed is limited by the length of feed, which is governed by blank size, or by the relationship between the force capacity of the press and the load. The combination of blanking with forming or drawing in compound dies also restricts press speed. Blanking speed may be as low as ten strokes per minute in producing blanks that are extremely large or that present handling problems for other reasons.

Regardless of the number of strokes per minute, the velocity of the punch always approaches zero near the bottom of the stroke. Within the usual range for production work in conventional blanking dies, the speed of the press has little practical effect on the speed of the punch during the blanking portion of the stroke. This effect, however, is critical for fine edge blanking, in which punch speed during the interval while the punch is cutting through the work metal is usually about 7.6 to 1.5 mm/s (0.3 to 0.6 in./s) (see the article "Fine Edge Blanking and Piercing" in this Volume).

Lubrication requirements are generally less critical for blanking than for forming or deep drawing; stock to be blanked is often fed into the press with no lubrication other than the residue remaining on the stock from the lubrication at the mill. The stock is sometimes coated with a light mineral oil or a light chlorinated oil. However, lubrication is important in dies that have close clearance between punches and stripper. At speeds of 40 strokes and more per minute, such dies must be lubricated constantly with a spray of light mineral oil to prevent galling of the punches in the stripper. Additional information on lubrication requirements in blanking is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Effect of Work Metal Thickness

Stock thickness affects the selection of material for dies and related components, as well as the selection of die type and design. The amount of shear and relief (angular clearance or draft) built into a blanking die and the amount of clearance between punch and die all depend on blank thickness.

Work metal thickness is also a factor in the selection of blanking method, handling procedure, and handling equipment. Blanking in a punch press is usually the fastest and most economical way of producing blanks thinner than about 6.4 mm ($\frac{1}{4}$ in.) in medium or large quantities.

Plate stock, in thicknesses of 6.4 to 25 mm ($\frac{1}{4}$ to 1 in.), is less frequently blanked in presses than sheet or strip. Blanks of such thick material are often made by gas cutting, sawing, nibbling, or routing instead of by shearing or by press operations; selection of the method depends primarily on plate thickness and production quantity.

Almost all blanks thinner than 3.2 mm ($\frac{1}{8}$ in.), except for intricate shapes chemically blanked from foil, are produced with conventional dies in mechanical or hydraulic presses. In only two of the examples of commercial practice presented in this article was work metal thicker than 3.2 mm ($\frac{1}{8}$ in.) (Example 3: 3.96 mm, or 0.156 in., and Example 9: 4.75 mm, or 0.187 in.).

Because of its strength and rigidity, material thicker than 3.2 mm ($\frac{1}{8}$ in.) is seldom blanked from coil stock or in a progressive die. On the other hand, because of its lack of strength and extreme flexibility, material thinner than 0.51 mm (0.020 in.) generally requires special handling techniques. The articles "Piercing of Low-Carbon Steel," "Blanking and Piercing of Electrical Steel Sheet," and "Press Forming of High-Carbon Steel" in this Volume contain additional information on the effect of work metal thickness on processing.

Distortion is often a problem in blanking complex shapes from thin low-carbon steel sheet by repeated strokes of a notching die. Distortion of such parts can be minimized by the use of hardened stock and by performing the entire blanking operation in one stroke in a single die.

Accuracy

Blanking in conventional dies readily produces parts within a total tolerance of 0.051 to 0.254 mm (0.002 to 0.010 in.), depending on the accuracy of the dies and the condition of the press. The tolerances given in Table 4 in the article "Piercing of Low-Carbon Steel" in this Volume generally also apply to blanks. The total tolerances listed under the column head "Location" apply to the relationship of a point on the periphery of the blank to a hole or other reference feature on the blank; the values listed under "Size" apply to a diameter for round blanks or to a similar control dimension for other blank shapes.

The production of blanks to these tolerances is illustrated by the examples in this article and the article "Piercing of Low-Carbon Steel." The following example describes the use of a compound die to maintain a total (envelope) tolerance of 0.13 mm (0.005 in.) on the relationship of a cam surface to a hole.

Example 9: Blanking and Piercing a Cam to 0.13 mm (0.005 in.) Total Tolerance on Cam Surface in Relationship to Hole Position.

A compound die was used to blank and pierce the cam shown in Fig. 27 so that the hole would be in accurate relationship to the cam surface. The die was made of A2 or equivalent tool steel and hardened to 62 HRC, and it had a clearance per side equal to 10% of the stock thickness.

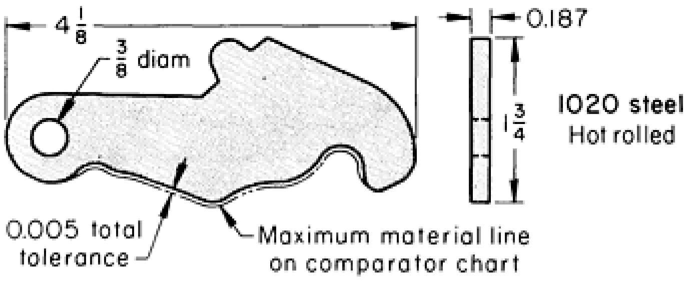


Fig. 27 Cam that was blanked and pierced in a compound die within an envelope tolerance of 0.13 mm (0.005 in.) TIR. Dimensions given in inches

The cam, used in the hinge mechanism of an automobile door, was blanked from hot-rolled 1020 steel 4.75 mm (0.187 in.) thick. Samples of the cam were inspected with an optical comparator, which compared the relationship of hole and cam surface to an outline that showed the full 0.13 mm (0.005 in.) tolerance, as illustrated in Fig. 27.

In another operation, the cam surface was machined to remove die break so that the edge would be square. The cam was then case hardened.

The part was produced in a 1.4 MN (160 tonf) open-back inclinable press at 60 strokes per minute. A sulfur-base lubricant was used. Die life was 40,000 pieces per sharpening.

Blanking and Piercing With Compound Dies. Example 6 in this article describes other applications of compound dies in blanking and piercing to conventional tolerances. For parts made in a progressive die, the relationship of the blank outline to features of the part produced in other stations of the die depends on the accurate fit of the pilot in the pilot hole. Transfer dies ordinarily provide better accuracy than progressive dies because the positive location of separate parts can be more precise than the roll feeding of coil stock with pilots.

Holding close tolerances on parts made in a progressive die is particularly difficult on soft or thin material because distortion of the locating holes by the pilot is more likely. Handling problems that might contribute to excessive variation in location or dimensions are not usually encountered in transfer dies, except with blanks of extremely thin material. Shaving (see the following section in this article) is used to improve the accuracy of blank outlines to meet close tolerances or to improve edge quality.

Short-run dies are generally less accurate than conventional dies. By using more accurate methods of constructing short-run dies, closer tolerances on blanked work can be obtained, but at some increase in die cost (see the section "Construction and Use of Short-Run Dies" in this article). Generally, making blanks by methods other than the use of dies in presses, except for machining or grinding, results in a lower level of accuracy.

Shaving

Shaving is an operation that can be performed after blanking to give a smooth, square edge and greater accuracy than can be achieved in ordinary blanking. Shaving removes only the blanked edge--cutting away the deformed, broken, and burred edge that was left in blanking. The elimination of these irregularities and the removal of locally work-hardened metal minimize breakage of the work metal during subsequent flanging, particularly the flanging of holes. The scrap produced in shaving is so thin that it resembles the chips produced in finish machining, rather than the usual scrap that is produced in a press.

When shaving is planned for, a small amount of extra stock is left on the workpiece to be removed in shaving. Shaving can be done in a separate operation, or it can be included in one station of a progressive die.

The shaving operation produces a straight, square edge, generally to about 75% of the metal thickness. Two shaves make a better, straighter edge (to about 90% of the metal thickness) than does a single shaving operation. To eliminate rollover from blank edges, which requires the removal of a greater amount of stock, it may be better to consider machining the workpiece rather than shaving.

Punch-to-die clearances range from 0 to $1\frac{1}{2}$% of stock thickness per side. Sturdy guideposts in a heavy die set are necessary to maintain the close alignment needed to prevent damage to the punch and die.

Shaving causes more wear on a die than ordinary blanking, so that the die produces fewer parts per grind and needs more frequent maintenance. Slivers of shaving scrap (chips) can jam feeding mechanisms, can become embedded in workpieces, or can mar the punch and die surfaces if not removed after each press stroke. Because of these problems, special attention must be given to die design when shaving is included among the operations done in a progressive die, as illustrated in the following example.

Example 10: Shaving in a Progressive Die.

In evaluating methods for high-volume production of the shaved low-carbon steel part shown in Fig. 28, the use of a single progressive die for all cutting and forming operations was projected as the most economical method, principally because this method would involve fewer operations and less handling than the other methods considered. However, two major problems were anticipated. First, the life of the shaving tools was expected to be much shorter than that of the other tools in the progressive die (which would have resulted in costly interruption of production to sharpen or replace the shaving tools), and, second, it appeared likely that misfeed could occur from jamming of the feeding mechanism by slivers of shaving scrap.

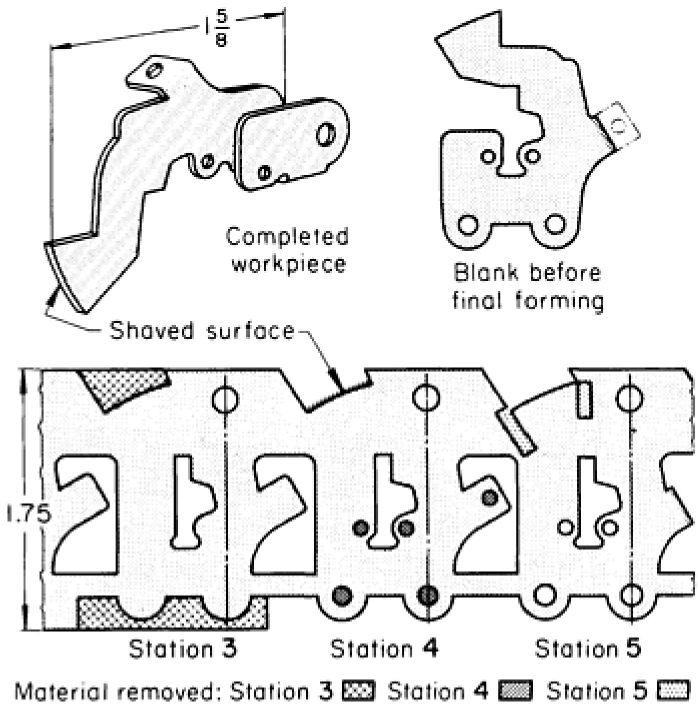


Fig. 28 Formed part on which a blanked edge was made smoother and more accurate by shaving (station 4) after notching (station 3) in a progressive die. Dimensions given in inches

By designing the die and feeding mechanism to eliminate difficulties from these two sources, efficient and economical production was obtained. The tools were of A2 air-hardening tool steel, hardened to 54 to 58 HRC for the forming sections and to 60 to 62 HRC for the cutting sections. The shaving section was made with a replaceable insert to minimize downtime when sharpening was needed. Damage to the progressive die from an accumulation of shaving scrap was prevented by including in the die a stock stop (station 3, Fig. 28) and misfeed and double-thickness protectors.

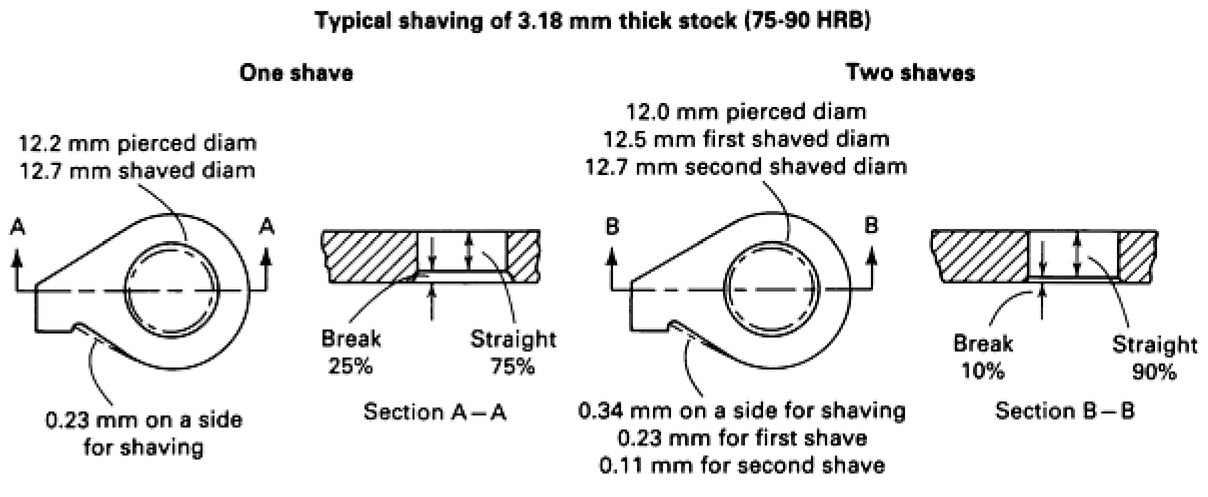
The shaving was done in station 4, as shown in Fig. 28. The production rate was 80 pieces per minute in a 530 kN (60 tonf) mechanical press with a 50 mm (2 in.) stroke. Tolerance on most sections of the part was ±0.13 mm (±0.005 in.). Additional information on the use of shaving to maintain close tolerances and to improve the quality of hole walls is available in the article "Piercing of Low-Carbon Steel" in this Volume.

Shaving allowance, or the amount of stock to be removed from the workpiece, depends on the hardness and thickness of the blank. Generally, the smallest amount of stock that will produce the desired result is left for the shaving operation. Table 4 lists shaving allowances recommended by one manufacturer. When shaving only one edge of a blank, shifting of the blank can be reduced by shaving the opposite edge as well, even if not required for function.

Table 4 Shaving allowances recommended by one manufacturer

Blank thickness		Allowance per side for steel with HRB hardness of:					
		50-66		**75-90**		90-105	
mm	**in.**	**mm**	**in.**	**mm**	**in.**	**mm**	in.
First shave (or a single shave)							
1.19	0.047	0.064	0.0025	0.076	0.003	0.102	**0.004**
1.57	0.062	0.076	0.003	0.102	0.004	0.127	**0.005**
1.98	0.078	0.089	0.0035	0.127	0.005	0.152-0.178	**0.006-0.007**
2.39	0.094	0.102	0.004	0.152	0.006	0.178-0.203	**0.007-0.008**
2.77	0.109	0.127	0.005	0.178	0.007	0.229-0.279	**0.009-0.011**
3.18	0.125	0.178	0.007	0.229	0.009	0.305-0.356	**0.012-0.014**
Second shave (add to first shave)							
1.19	0.047	0.032	0.00125	0.038	0.0015	0.051	**0.002**

1.57	0.062	0.038	0.0015	0.051	0.002	0.064	**0.0025**
1.98	0.078	0.044	0.00175	0.064	0.0025	0.076-0.089	**0.0030-0.0035**
2.39	0.094	0.051	0.002	0.076	0.003	0.089-0.102	**0.0035-0.0040**
2.77	0.109	0.064	0.0025	0.089	0.0035	0.114-0.140	**0.0045-0.0055**
3.18	0.125	0.089	0.0035	0.114	0.0045	0.152-0.178	**0.006-0.007**



Setups or Shaving. Shaving requires that the blank be accurately located over the die or the punch, because only a few thousandths of an inch of metal is removed by the operation (Table 4). Piloting pins, projecting from the punch, can engage holes in the blank to ensure proper location. If the holes are not included in the original design, it may be permissible to add them for locating purposes only.

If adding holes is not permitted, a locating device such as that shown in Fig. 29 can be used. The clamping arms engage the blank at suitable nesting points. When the punch descends, the shaved blank falls through the die. The position of the clamping arms is fixed by the two stop pins.

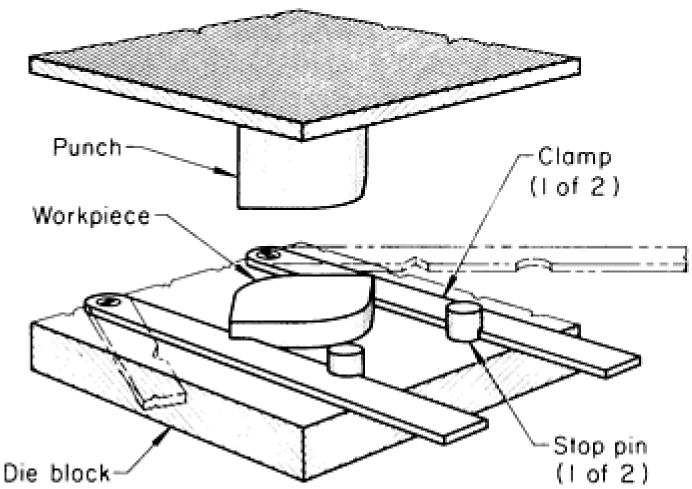


Fig. 29 Shaving die with a device for locating a blank with no holes for piloting.

Operation of this die can be improved by he use of a spring-loaded ejector and pressure pad within the die opening. As the punch ascends, the ejector lifts the shaved workpiece above the die face, thus eliminating the fall through the die block, which may result in dents or other surface defects.

Burr Removal

The shape, height, and roughness of burrs must be controlled to some degree in nearly all blanking operations. Complete elimination of burrs is not possible, but their formation can be minimized by the use of proper clearance between punch and die and by good maintenance.

Exposed burrs on the finished part can be unsafe and unsightly. Burrs on some blanked work can cause difficulties in forming and can increase the rate of workpiece breakage and die wear. Burrs can be removed by grinding, which generally removes the burr and a portion of the work-hardened edge. Tumbling in a barrel is a common method of deburring small parts. Other deburring methods include chemical and electrolytic deburring, belt grinding, polishing and ultrasonic methods, as described in *Surface Engineering*, Volume 5 of the *ASM Handbook.* Hand scrapers can be used to remove burrs from irregular shapes or soft metal parts.

Blanking In Presses Versus Alternative Methods

Fine edge blanking is primarily used where die break is unacceptable and would require removal by subsequent shaving if conventional blanking were used. In fine edge blanking, there is no die break, and the entire wall surface of the cut is burnished. Additional information is available in the article "Fine Edge Blanking and Piercing" in this Volume.

Milling is applicable mainly for cutting stacked parts, for short runs, and for making parts that are subject to frequent design change. It substitutes an inexpensive template for a conventional punch and die.

Chemical blanking may be competitive with blanking in presses for intricate parts that are only a few thousandths of an inch thick. Combs for electric shavers, for example, are more frequently made by the chemical blanking of stainless steel than by mechanical blanking methods.

Contour band sawing and gas cutting may be competitive with blanking for stacked parts and thick material.

Safety

In all blanking operations, as in all press operations, there are hazards to operators, repairmen, and personnel in the vicinity. No press, die, or auxiliary equipment should be considered operable until these hazards are eliminated by installing necessary guards and other safety devices. The operator and all persons working around the blanking operation should be instructed in all precautions for safe operation before work is started. Additional information is available in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume.

Introduction

PIERCING is the cutting of holes in sheet metal, generally by removing a slug of metal, with a punch and die. Piercing is similar to blanking, except that in piercing the work metal that surrounds the piercing punch is the workpiece and the punched-out slug is scrap, while in blanking the workpiece is punched out.

The term piercing is used in this article, and in related articles in this Volume, to denote the production of a hole by removing a slug of metal with a punch and die. However, some prefer the terms punching or perforating, limiting the term piercing to the use of a pointed punch that tears and extrudes a hole without cutting a slug of metal. The term perforating is also sometimes used in the special sense of cutting many holes in a sheet metal workpiece by removing slugs with several punches.

Piercing is ordinarily the fastest method of making holes in steel sheet or strip and is generally the most economical method for medium-to-high production. Pierced holes can be almost any size and shape; elongated holes are usually called slots. The accuracy of conventional tool steel or carbide dies provides pierced holes with a degree of quality and accuracy that is satisfactory for a wide variety of applications.

Additional information on piercing is available in the articles "Blanking of Low-Carbon Steel," "Fine Edge Blanking and Piercing," "Press Forming of High-Carbon Steel," and "Blanking and Piercing of Electrical Steel Sheet" in this Volume.

Characteristics of Pierced Holes

Pierced holes are different from through holes that are produced by drilling or other machining methods. A properly drilled or otherwise machined through hole has a side-wall that is straight for the full thickness of the work metal, with a high degree of accuracy in size, roundness, and straightness. The sidewall of a pierced hole is generally straight and smooth for only a portion of the thickness, beginning near the punch end of the hole; the rest of the wall is broken out in an irregular cone beyond the straight portion of the hole, producing fracture, breakout, or die break (Fig. 1).

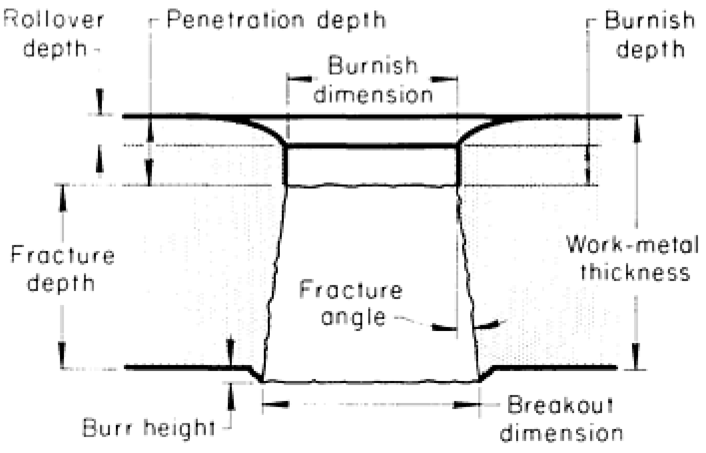


Fig. 1 Characteristics of a pierced hole. Curvature and angles are exaggerated for emphasis.

The piercing operation typically begins as a cut that produces a burnished surface on the hole wall and some rollover (curved surface caused by deformation of the workpiece before cutting begins), as illustrated in Fig. 1. The punch completes its stroke by breaking and tearing away the metal that was not cut during the beginning of the piercing operation.

The combined depth of rollover and burnish is a measure of the penetration depth of the stroke, also shown in Fig. 1. This is the part of the stroke during which the cutting force is exerted, before the metal fractures or breaks away (Fig. 1).

The amount of penetration before fracture is commonly expressed as a percentage of the stock thickness. In general, the percentage of penetration depends more on the material than on other factors, such as punch-to-die clearance. Table 1 in the article "Blanking of Low-Carbon Steel" in this Volume shows the average percentage of penetration (before fracture) in various metals under typical piercing or blanking conditions. The percentage of penetration affects energy consumption and cutting force in blanking or piercing, as described in the article "Blanking of Low-Carbon Steel" in this Volume.

Quality of Hole Wall

If the sidewall of a pierced hole is not smooth or straight enough for the intended application, it can be improved by shaving in a die or by reaming. When done in quantity, shaving is the least expensive method of improving the sidewall of a pierced hole. Shaving in one or two operations generally makes the sidewall of a hole uniform and smooth through 75 to 90% of the stock thickness.

Superior accuracy and smoothness of hole walls can be obtained by fine edge piercing. With this method, one stroke of a triple-action press pierces holes with smooth and precise edges for the entire thickness of the material. Additional information is available in the article "Fine Edge Blanking and Piercing" in this Volume.

Burr height is an important element in hole quality, and a maximum burr height is usually specified. For most applications, the limit on burr height is between 5 and 10% of stock thickness. Burr height in piercing a given workpiece is primarily governed by punch-to-die clearance and tool sharpness.

Burr condition and limits usually determine the length of run before the punch and die are resharpened. With good practice, burr height generally ranges from 0.013 to 0.076 mm (0.0005 to 0.003 in.), but may be much greater, depending on workpiece material and thickness, clearance, and tool condition. As an alternative to limiting the length of run to control burr condition, unacceptable burrs can be removed by shaving or deburring, as described in the article "Mass Finishing" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Selection of Die Clearance

Clearance, or the space between the punch and the sidewall of the die, affects the reliability of operation of piercing (and blanking) equipment, the characteristics of the cut edges, and the life of the punch and die. Published recommendations for clearances have varied widely, with most suggesting a clearance per side of 3 to 12.5% of the stock thickness for steel.

Establishment of the clearance to be used for a given piercing or blanking operation is influenced by the required characteristics of the cut edge of the hole or blank and by the thickness and the properties of the work metal. Larger clearances prolong tool life. An optimal clearance can be defined as the largest clearance that will produce a hole or blank having the required characteristics of the cut edge in a given material and thickness. Because of differences in cut-edge requirements and in the effect of tool life on overall cost, clearance practices vary among plants and for different applications.

No single table or formula can specify an optimal clearance for all situations encountered in practice. Starting with general guidelines, trial runs using several different clearances may be needed to establish the most desirable clearance for a specific application. The following general principles are useful in making adjustments:

- Rollover (plastic deformation) and burnish depths are greater in thick material than in thin material and are greater in soft material than in hard material
- Clearance (in decimal parts of an inch) needed to produce a given type of edge should vary directly with material thickness and hardness, and inversely with ductility

All clearance values given in this article are for clearance per side, except where indicated.

Edges

More specific guidance in selecting die clearances is provided by considering the types of edges produced with different clearances.

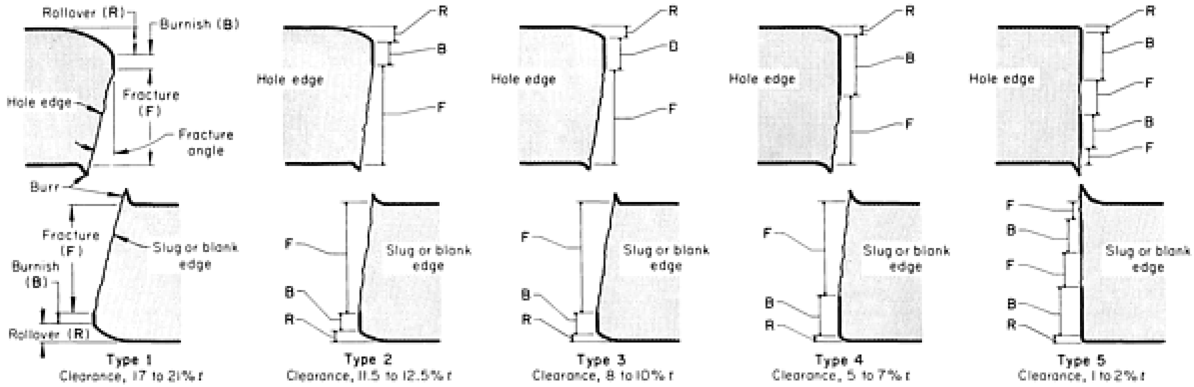
Edge Types. The acceptability of a punched hole or a blank is generally based on the condition of the cut edge and its suitability for the application. Usable holes and blanks can be obtained over a broad range of punch-to-die clearances, each resulting in a different edge condition. Figure 2 shows five types of edges that result from the use of different clearances in piercing or blanking low-carbon steel at a maximum hardness of 75 HRB. The tabular data accompanying Fig. 2 include approximate ranges of fracture or breakout angles, rollover, burnish and fracture depths, and burr characteristics for the five edge types. Table 1 lists the clearance ranges that will produce these edges when piercing or blanking various metals.

Table 1 Punch-to-die clearances for piercing or blanking various metals to produce the five types of edges shown in Fig. 2

For clearances that produce type 1, 2, and 3 edges, it is ordinarily necessary to use ejector punches or other devices to prevent the slug from adhering to the punch.

Work metal	Clearance per side, % of stock thickness				
	Type 1[(a)]	**Type 2**	**Type 3**	**Type 4**	Type 5
Low-carbon steel	21	11.5-12.5	8-10	5-7	**1-2**
High-carbon steel	25	17-19	14-16	11-13	**2.5-5**
Stainless steel	23	12.5-13.5	9-11	3-5	**1-2**
Aluminum alloys					
Up to 230 MPa (33 ksi) tensile strength	17	8-10	6-8	2-4	**0.5-1**
Over 230 MPa (33 ksi) tensile strength	20	12.5-14	9-10	5-6	**0.5-1**
Brass, annealed	21	8-10	6-8	2-3	**0.5-1**
Brass, half hard	24	9-11	6-8	3-5	**0.5-1.5**
Phosphor bronze	25	12.5-13.5	10-12	3.5-5	**1.5-2.5**
Copper, annealed	25	8-9	5-7	2-4	**0.5-1**
Copper, half hard	25	9-11	6-8	3-5	**1-2**
Lead	22	8-10	6.5-7.5	4-6	**1.5-2.5**
Magnesium alloys	**16**	**5-7**	**3.5-4.5**	**1.5-2.5**	**0.5-1**

(a) Maximum.



Edge characteristic	**Type 1**	**Type 2**	**Type 3**	**Type 4**	Type 5
Fracture angle	14-16°	8-11°	7-11°	6-11°	. . .
Rollover[(a)]	10-20% *t*	8-10% *t*	6-8% *t*	4-7% *t*	**2-5% *t***
Burnish[(a)]	10-20% *t*[(b)]	15-25% *t*	25-40% *t*	35-55% *t*[(c)]	**50-70% *t*[(d)]**
Fracture	70-80% *t*	60-75% *t*	50-60% *t*	35-50% *t*[(e)]	**25-45% *t*[(f)]**
Burr	**Large, tensile plus part distortion**	**Normal, tensile only**	**Normal, tensile only**	**Medium, tensile plus compressive[(g)]**	**Large, tensile plus compressive[(g)]**

(a) Rollover plus burnish approximately equals punch penetration before fracture.

(b) Burnish on edge of slug or blank may be small and irregular or even absent.

(c) With spotty secondary shear.

(d) In two separate portions, alternating with fracture.

(e) With rough surface.

(f) In two separate portions, alternating with burnish.

(g) Amount of compressive burr depends on die sharpness.

Fig. 2 Effect of punch-to-die clearance per side (as a percentage of stock thickness, *t*) on characteristics of edges of holes and slugs (or blanks) produced by piercing or blanking low-carbon steel sheet or strip at a maximum hardness of 75 HRB. Table 1 lists clearances for producing the five types of edges in various metals. See text for additional discussion and for applicability of the five types of edges.

Type 1. This type of edge has a large rollover radius and a large burr that consists of a normal tensile burr in addition to bending or deformation at the edge. Burnish depth is minimal. Fracture depth is about three-fourths of stock thickness, and the fractured surface has a large angle. This edge is satisfactory for noncritical applications in which edge quality and part flatness are not important.

Type 2. This edge, which has a moderate rollover radius, normal tensile burr, and a small fracture angle, provides maximum die life and a hole or blank that is acceptable for general work in which a large burnish depth is not required. Burnish depth plus rollover depth is about one-third of stock thickness; fracture depth, about two-thirds.

Type 3. This edge has a small rollover radius, a normal tensile burr, and a small fracture angle. It has low residual stress and is therefore particularly desirable for use in parts made of work-hardenable material that will undergo severe forming. The clean stress-free edge reduces the possibility of edge cracking during forming. Burnish depth plus rollover depth is one-third to one-half of stock thickness.

Type 4. This is a desirable edge for stampings used for mechanisms or parts that must receive edge finishing such as shaving or machining. The edge has a very small rollover radius, a medium tensile and compressive burr, and a small fracture angle. Burnish depth plus rollover depth is about two-thirds of stock thickness. This edge type can be recognized by the spotty appearance of secondary shear on the fractured surface.

Type 5. This edge has a minimum rollover radius and a large tensile and compressive burr, and it can be recognized by the complete secondary shear on the cut surface. It is useful in applications in which edges must have a maximum of straight-wall depth without secondary operations. On steel and other hard metals, die life is extremely short. The edge can be useful on some of the softer metals, which allow a reasonable die life.

Edge Profiles. The exact profile of the edge varies somewhat for different work metals, depending on the properties of the metal. Results are also slightly affected by:

- Face shear on punch or die
- Punch-to-die alignment
- Proximity to adjacent holes
- Distance to adjacent blanked edges
- Orientation of the different portions of the cut edge with respect to the rolling direction of the stock
- Ratio of hole size to stock thickness
- Internal construction of the die cavity
- Lubrication

The edge profiles illustrated in Fig. 2, as well as the estimates of fracture angles and the relative amounts of rollover, burnish, fracture, and burr given in the accompanying table, are intended to represent production conditions, allowing for the normal range of tool sharpness encountered in piercing and blanking low-carbon steel sheet.

The clearance values given in Table 1 for piercing and blanking various metals to produce the five types of edges were obtained in laboratory tests. The cutting edges of the punches were stoned to a radius of 0.05 to 0.15 mm (0.002 to 0.006 in.) to simulate an amount of wear corresponding to the approximate midpoint of a production run. No lubricant was used on the work metal.

As clearance is increased from the low values used for type 5 edges to those used for type 1 edges, several effects are evident. The edge profile deviates more and more from straightness and perpendicularity as rollover, fracture angle, and fracture depth increase, while burnish depth decreases proportionately. Total burr height initially decreases as its compressive component decreases, leaving only the essentially constant tensile burr on type 2 and 3 edges (usually in the range of 0.013 to 0.076 mm, or 0.0005 to 0.003 in., depending on the work metal and the tool condition).

With further increase in clearance (edge type 1), bending or deformation at and near the edge adds an additional burr component, increasing the total burr height. This part distortion immediately adjacent to the cut edge is usually accompanied by a more gradual curvature, or dishing, on blanks or slugs; the corresponding curvature is much less pronounced on the stock around a hole, which is usually restrained by a stripper (curvature of blanks or stock strip is not shown in Fig. 2). At extremely large clearances (substantially above those shown for type 1 edges), double-shear characteristics are sometimes observed on the cut edge.

Effect of Tool Dulling

The sharpness of punch and die edges has an important effect on cut-edge characteristics in piercing and blanking. At the beginning of a run, with punch and die equally sharp, the hole profile is the same as that of the slug or blank. As the run progresses, dulling of the punch increases the rollover and the burnish depth on the hole wall and increases the burr height on the slug or blank. Dulling of the die increases burnish depth and burr height on the hole edge. The punch dulls faster than the die; therefore, the changes in hole characteristics related to punch dulling proceed more rapidly than those related to die dulling.

On average, the following differences between hole edge and blank edge are observed in production work on sheet metal:

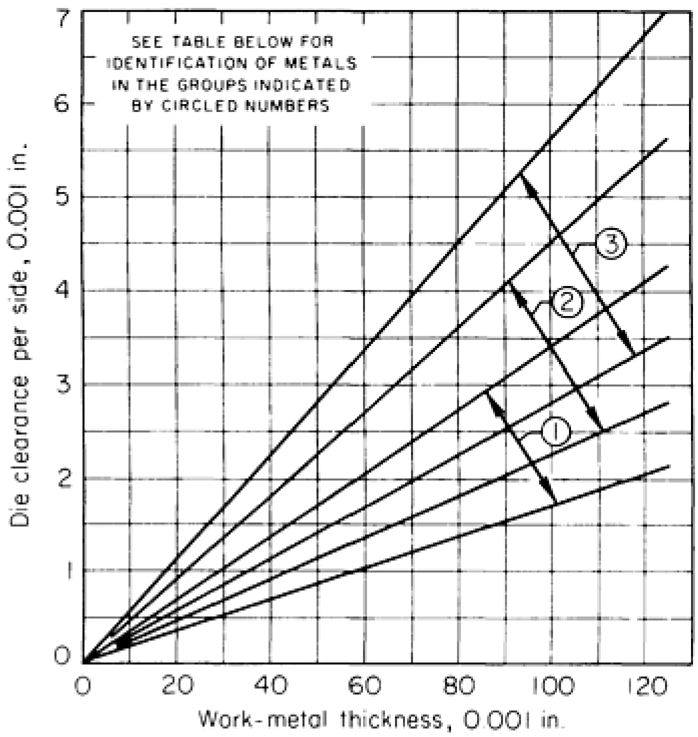
- Rollover is greater on hole edge than on slug or blank edge
- Burnish depth is greater on hole edge than on slug or blank edge
- Fracture depth is smaller (and fracture angle greater) on hole edge than on slug or blank edge
- Burr height on hole edge is less than that on slug or blank edge, and varies with tool sharpness

These differences are illustrated in Fig. 2.

Use of Small Clearance

Where relatively square edges are required, small clearances can be used to produce holes with type 4 edges. Although tool life is shorter than when larger clearances are used, this may not be an important factor in the overall costs for short or medium production runs.

Figure 3 shows the ranges of die clearance per side used by one electronics manufacturer in piercing and blanking three groups of metals up to 3.18 mm (0.125 in.) thick. The groups and the percentages of stock thickness on which these ranges of clearance were based are listed with Fig. 3. For stainless steel (not included in Fig. 3), nominal clearance per side was 2.5% of the thickness for stock thicknesses up to 4.75 mm (0.187 in.), and 4% for thicknesses between 4.75 and 6.35 mm (0.187 and 0.250 in.).



Group	Clearance per side, % of stock thickness[(a)]	
	Average	Range
1. Aluminum alloys 1100 and 5052, all tempers	2.25	**1.7-3.4**
2. Aluminum alloys 2024 and 6061, T4 and T6 tempers; brass, all tempers; cold-rolled steel, dead soft	3.0	**2.25-4.5**
3. Cold-rolled steel, half hard	**3.75**	**2.8-5.6**

Note. Incorrect clearance values twice as large as those shown here have appeared with charts of this type in some publications, apparently because of confusion between clearance per side and total clearance. Also, stainless steel has erroneously been included with the metals in groups 2 and 3 in those publications.

(a) Percentages of stock thickness on which the ranges of acceptable clearance in chart are based. See text for clearances used in piercing or blanking of stainless steel.

Fig. 3 Ranges of punch-to-die clearance per side recommended by one manufacturer for piercing and blanking of various metals up to 3.18 mm (0.125 in.) thick

The data plotted in Fig. 3 were used by the manufacturer to determine whether a given punch and die could be used interchangeably for a metal different in thickness or type from the one for which it had been designed. If the point of intersection of a vertical line for the thickness of the new material with a horizontal line for the existing die clearance was within the range shown for the new material, the punch and die were normally satisfactory for use on the new material.

Piercing of Low-Carbon Steel

Use of Large Clearance

Studies of die operation under both laboratory and production conditions have indicated that large clearances can be used to obtain maximum tool life in numerous piercing (and blanking) applications. Table 2 indicates the effect of clearance on force requirements for piercing and stripping, presenting data on the test piercing of 6.53 mm (0.257 in.) diam holes in cold-rolled low-carbon steel of various thicknesses and hardnesses. Although individual results show some inconsistency, as would be expected because of the difficulty in obtaining accurate measurements of this type, the trend toward lower stripping force with an increase in clearance is evident from these data. The amounts of punch penetration into the die needed to release the slug are also given in Table 2. Table 3 lists production data on the effect of increased clearance on tool life for piercing and blanking common metals in various thicknesses.

Table 2 Effect of clearance on piercing and stripping force required for piercing 6.53 mm (0.257-in.) diam holes in cold-rolled low-carbon steel

No lubricant was used.

Clearance per side, % of stock thickness	**Force required**				Punch penetration into die[b]	
	Piercing[a]		**Stripping (total)**			
	MPa	**ksi**	**N**	**lbf**	**mm**	in.
Stock 0.64 mm (0.025 in.) thick, 65 HRB						
6.0	455	66.0	703	158	0.20	**0.008**
12.5	462	67.0	480	108	0.20	**0.008**
Stock 0.79 mm (0.031 in.) thick, 47 HRB						
5.0	350	50.8	703	158	0.20	**0.008**
13.0	341	49.5	503	113	0.20	**0.008**
Stock 0.86 mm (0.034 in.) thick, 87 HRB						
4.5	583	84.5	578	130	0.20	**0.008**
13.0	569	82.6	334	75	0.20	**0.008**
Stock 1.07 mm (0.042 in.) thick, 85 HRB						

5.0	551	79.9	1250	282	0.18	**0.007**
12.0	527	76.5	783	176	0.18	**0.007**
Stock 1.19 mm (0.047 in.) thick, 47 HRB						
5.0	341	49.4	1160	260	0.47	**0.0185**
6.5	352	51.0	298	67	0.47	**0.0185**
8.5	339	49.2	267	60	0.47	**0.0185**
9.5	353	51.2	165	37	0.47	**0.0185**
10.5	350	50.8	133	30	0.47	**0.0185**
13.0	332	48.2	249	56	0.47	**0.0185**
Stock 1.27 mm (0.050 in.) thick, 71 HRB						
5.0	405	58.7	974	219	0.20	**0.008**
12.5	383	55.6	431	97	0.20	**0.008**
5.0	404	58.6	956	215	0.51	**0.020**
12.5	393	57.0	498	112	0.51	**0.020**
Stock 1.27 mm (0.050 in.) thick, 61 HRB						
5.0	367	53.2	1160	260	0.51	**0.020**
12.5	374	54.2	418	94	0.51	**0.020**
5.0	367	53.2	1312	295	0.51	**0.020**
12.5	364	52.7	703	158	0.51	**0.020**
Stock 1.50 mm (0.059 in.) thick, 74 HRB						
5.0	369	53.5	498	112	0.47	**0.0185**

6.8	358	52.0	605	136	0.47	**0.0185**
7.6	350	50.8	383	86	0.47	**0.0185**
8.5	349	50.6	374	84	0.47	**0.0185**
9.8	346	50.2	200	45	0.47	**0.0185**
13.0	355	51.5	89	20	0.47	**0.0185**
Stock 1.57 mm (0.062 in.) thick, 50 HRB						
5.0	371	53.8	578	130	0.47	**0.0185**
6.5	371	53.8	454	102	0.47	**0.0185**
7.3	363	52.6	325	73	0.47	**0.0185**
8.0	364	52.8	578	130	0.47	**0.0185**
9.0	361	52.4	534	120	0.47	**0.0185**
12.5	**363**	**52.6**	**249**	**56**	**0.47**	0.0185

Source: Dayton Progress Corporation

(a) Pounds per square inch of cross section cut.

(b) Penetration required for release of slug.

Table 3 Effect of punch-to-die clearance on tool life in piercing and blanking of ferrous and nonferrous metals of various thicknesses

Stock thickness		**Type**	**Hardness**		**Initial clearance**		**Increased clearance**		Tool life increase with greater clearance, %
mm	**in.**				**Clearance per side, % of stock thickness**	**Tool life per grind, holes**	**Clearance per side, % of stock thickness**	**Tool life per grind, holes**	
Low-carbon steels, cold rolled									
0.41	0.016	Zinc coated	79 HRB	. . .	6.3	30,000	12.5	140,000	**366**

0.51	0.020	1018	22 HRC	. . .	2.5	115,000	5.0	230,000	**100**
0.91	0.036	. . .	(a)	. . .	2.8	67,000	12.5	204,000	**205**
1.19	0.047	1010	. . .	. . .	5.0	10,000	12.5	68,000	**580**
1.52	0.060	. . .	77 HRB	. . .	4.5	130,000	12.5	400,000	**208**
1.78	0.070	Galvanized	32 HRB	. . .	5.0	100,000	11.0	300,000	**200**
Low-carbon steels, hot rolled									
1.35	0.053	. . .	72 HRB	. . .	5.0	80,000	12.5	240,000	**230**
3.23	0.127	. . .	94 HRN	. . .	5.0	100,000	12.5	250,000	**150**
High-carbon steels									
1.52	0.060	1070	. . .	. . .	. . .	. . .	15.0	100,000	**. . .**
1.98	0.078	1090	. . .	. . .	. . .	. . .	10.0	835,000	**. . .**
2.03	0.080	4130	73 HRB	. . .	5.0	. . .	7.5	70,000	**. . .**
3.18	0.125	. . .	9 HRC	. . .	2.5	30,000	8.5	240,000	**700**
Stainless steels									
0.13	0.005	301	45 HRC	. . .	20.0	15,000	42.0	125,000	**900**
0.51	0.020	410	. . .	. . .	3.8	5,000	12.5	136,000	**2600**
1.14	0.045	304	16 HRC	. . .	6.5	12,000	11.0	30,000	**150**
1.60	0.063	. . .	89 HRB	. . .	5.0	175,000	9.0	250,000	**60**
Co-Cr-Ni-base heat-resistant alloy									
0.91	0.036	HS-25 (L-605)	22 HRC	. . .	2.8	1500	9.5	5000	**230**
Aluminum alloys									

0.46	0.018	5086	. . .	. . .	16-20[b]	20,000	16-20[c]	70,000	**250**
1.02	0.040	3003	[d]	. . .	5.0	. . .	12.5	[e]	**. . .**
1.32	0.052	. . .	[f]	. . .	5.0	. . .	8.5	. . .	**50**
Copper alloys									
0.18	0.007	Tin-plated brass	76 HRB	. . .	7.0	. . .	14.0	[g]	**50**
1.14	0.045	Brass	. . .	. . .	3.5	15,000	7.0	110,000	**[h]**
1.19	0.047	Paper-clad brass	81 HRB	. . .	5.0	20,000	10.0	25,000	**[h]**
0.08	**0.003**	**Beryllium copper**	**95 HRB[i]**	**. . .**	**8.5**	**300,000**	**25.0**	**600,000**	**100**

Source: Dayton Progress Corporation

(a) No. 4 temper.

(b) Punch entered die 1.5 mm (0.060 in.).

(c) Punch did not enter die.

(d) H12 temper.

(e) Higher-quality parts.

(f) Soft.

(g) Eliminated die breakage.

(h) Run completed without regrind.

(i) Half hard.

On the basis of these studies and production experience, clearance per side equal to 12.5% of stock thickness has been recommended by some toolmakers and sheet metal fabricators for the general-purpose piercing (and blanking) of cold-rolled steel 0.51 to 3.18 mm (0.020 to 0.125 in.) thick in all tempers. This practice produces type 2 cut edges (Fig. 2).

The advantages observed when using a clearance of 12.5%, instead of substantially smaller clearances (as for a type 4 edge), include the following:

- Total tool life and tool life between regrinds are considerably increased (Table 3). Punch wear, normally two or three times die wear, is greatly reduced because the hole is larger than the punch size and because stripping wear is minimized
- Load on the press may be slightly reduced (Table 2)
- Burr height is smaller at the beginning of a run and increases at a slower rate during the run
- Distortion or waviness of the work surface is reduced, especially with closely spaced holes
- Stripping force is reduced (Table 2), which partially accounts for the reduced punch wear

The factors that must be considered in applying the 12.5% clearance include the following:

- A different clearance may be required for steel outside the thickness range of 0.51 to 3.18 mm (0.020 to 0.125 in.), for metals other than steel, or to meet critical edge-quality requirements
- A spring-loaded stripper should be used instead of the positive or fixed type, and the slug or blank must be prevented from adhering to the end of the punch. These precautions are especially important in transfer dies
- Hole size is larger than punch size, particularly with hard or thin materials
- With clearance above 15%, slugs of some materials (for example, 1.3 mm, or 0.050 in., thick type 410 stainless steel at a hardness of 50 HRC) may be ejected from the die at high velocity--possibly requiring special safety precautions

Clearance and Tool Size

In blanking, the die opening is usually made to the desired size of the blank, and the punch size is then equal to the die opening minus twice the specified clearance per side. Conversely, in piercing, the punch is usually made to the desired size of the hole, and the die opening is then equal to the punch size plus twice the specified clearance per side.

Clearance per side for blanking dies is ordinarily calculated from the desired percentage of clearance and the nominal thickness of the stock. However, to keep the inventory of piercing tools from becoming too large, some manufacturers use a modified practice for stocking piercing punches and die buttons in commonly used diameters. Punches are ordered to size. Work metal thickness is classified into several ranges, and die buttons are ordered to the specified clearance per side for the median stock thickness of the range to which the work metal for the given application belongs.

Hole dimensions are slightly affected as the clearance is changed. When using clearances that produce a type 4 edge, the diameter of the pierced hole is about 0.013 mm (0.0005 in.) less than that of the punch used to produce it. By increasing the clearances to those for a type 2 edge, the hole size will be equal to, or approximately 0.013 mm (0.0005 in.) larger than, the punch diameter.

With tight clearances, the slug is wedged into the die cavity. As the clearance is increased, the wedging action decreases; consequently, the slug may be as much as 0.013 mm (0.0005 in.) smaller than the die cavity.

Force Requirements

The force needed to pierce a given material depends on the shear strength of the work metal, the peripheral size of the hole or holes to be pierced, stock thickness, and depth of shear on the punch. The calculation of piercing force is the same as that for cutting force in blanking (see the article "Blanking of Low-Carbon Steel" in this Volume).

Effect of Punch Shear. Shear is the amount of relief ground on the face of a punch (Fig. 5). It is used to reduce the instantaneous total load on the tool and to permit thicker or higher-strength materials to be pierced in the same press. It distributes the total piercing load over a greater portion of the downstroke by introducing the cutting edge in increments rather than instantaneously.

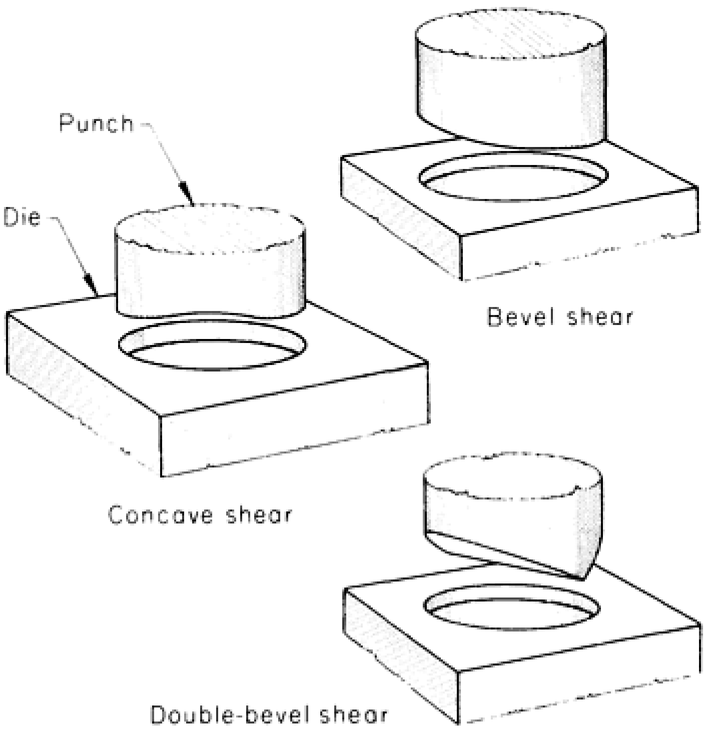


Fig. 5 Three types of shear on piercing punches. Angle and depth of shear are exaggerated for emphasis.

Piercing force (but not contact edge pressure or total work done) varies with the amount of shear on the punch face. With the bottom of the punch flat and parallel to the face of the die, piercing takes place on the entire periphery at once, requiring maximum force. The load on the press and tools increases rapidly to a maximum after impact and then releases suddenly when piercing is completed. By grinding shear on the punch as shown in Fig. 5, the maximum load is decreased, but the punch travels correspondingly farther to complete the piercing. Load release is also somewhat less sudden.

Shear location is ordinarily selected so as to confine distortion to the scrap metal (slug). Thus, in piercing, shear is ground on the punch because the punched-out metal is to be scrap. Concave shear and double-bevel shear (Fig. 5) provide a balanced load on the punch. Scalloped shear, sometimes ground on round punches, also provides a balanced load on the punch. An unbalanced load may cause deflection and tool breakage or excessive wear.

The amount of shear is determined by trial. However, shear equal to one-third of stock thickness (t/3) will reduce piercing force about 25%, and shear equal to stock thickness will reduce piercing force about 50%. Shear can be applied to punches for large holes but not to small-diameter punches, because they lack column strength.

When a number of small holes are being pierced and the press load must be reduced, the punches can be ground to different lengths. This enables the punches to start cutting at different times and reduces the maximum load. In selecting a press, it should be noted that the reduction in maximum impact load on the press achieved by staggering punch length or by using shear is not sufficient to enable the use of a press that is significantly lower in tonnage rating, strength, or rigidity.

Piercing of Low-Carbon Steel

Presses

Presses used in piercing are the same as those used in other pressworking operations. Open-back gap-frame presses of the fixed upright, fixed inclined, or inclinable type are common. The stock can be fed from the side with minimal interference from the press frame, and the parts can be removed from the front by the operator or ejected out the back by gravity or air jets.

Adjustable-bed or horn presses are used for piercing holes in tubing and in the sides of drawn or formed shells and boxes. Adjustable-bed and gap-frame presses are generally rated at capacities of less than 1.8 MN (200 tonf).

Straight-side presses are commonly used for compound-die and progressive-die operations. Increased accuracy, speed, and stability are required for these operations.

The turret punch press is a special machine in which the punches and dies are mounted in synchronized indexing tables. Several sets of punches and dies are mounted in the table, which can be manually or automatically indexed into operating position. A flat blank is pierced and notched in a turret punch press by positioning it under the operating punch and tripping the punching mechanism. The blank is secured to a free-floating table on which a template containing the hole pattern is also attached. Each hole size and shape is coded so that all such holes can be pierced before indexing a new punch-and-die set under the press ram. The table is moved so that a pin will drop into a hole in the template; this places the blank in the proper position for piercing a hole. After the holes of one size and shape have been pierced, a new punch and die are indexed into operating position, and piercing continues in this manner until the part is finished. Almost any size or shape of hole can be pierced, within the capacity of the machine.

Turret punch presses can be programmed for tape control for increased production. Turret movement can also be controlled semiautomatically or automatically, on numerical control (NC) or computer numerical control (CNC) punch presses. A closed-loop direct or alternating current drive connected to both the upper and lower turret assemblies provides the automatic turret with either unidirectional or bidirectional movement. Selected CNC presses even offer an optimization feature that automatically determines the most efficient and most cost-effective punching sequence for a specific workpiece.

Piercing of Low-Carbon Steel

Tools

A typical piercing die consists of:

- Upper and lower die shoes, to which punch and die retainers are attached
- Punches and die buttons
- A spring-actuated guided stripper (Fig. 6)

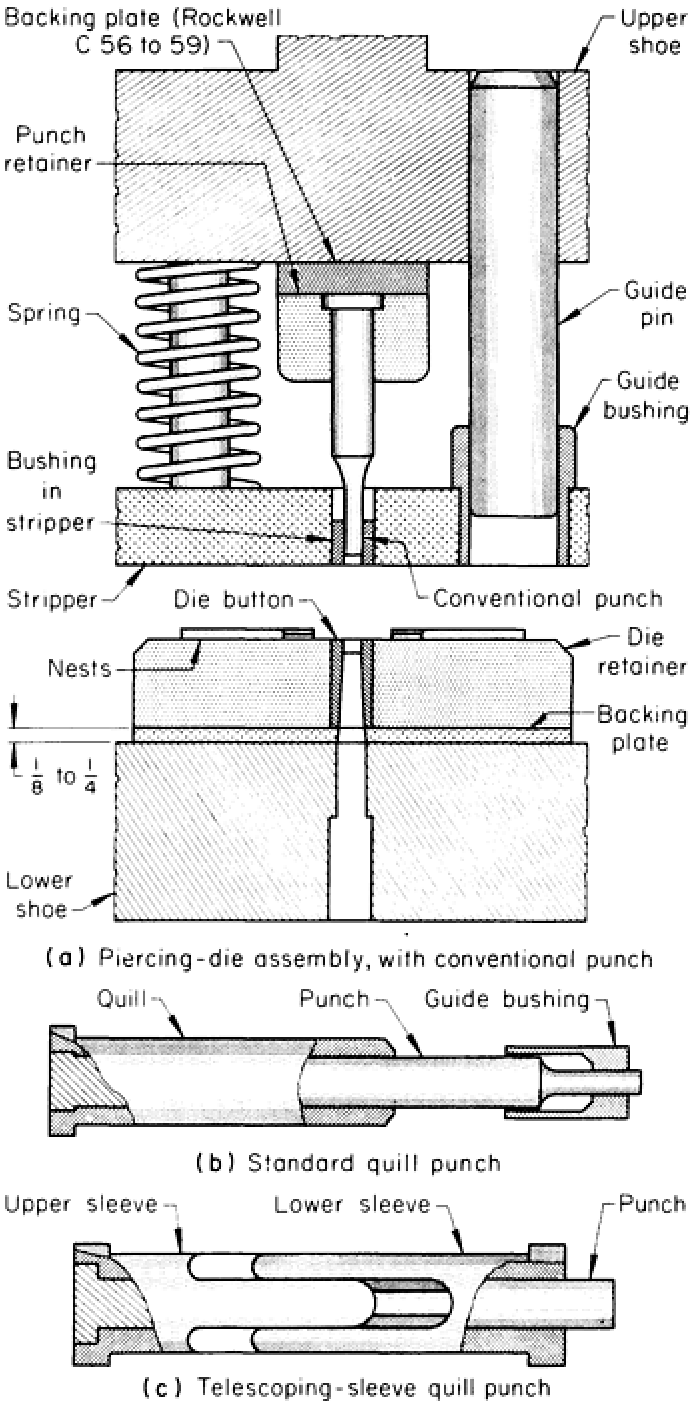


Fig. 6 Typical piercing die, and three types of punches used. See text for discussion. Dimensions given in inches

Small workpieces are generally pierced in compound dies that blank and pierce in the same stroke. Piercing is also done in the stations of a progressive die or a transfer die.

Any of these dies can be constructed as multiple dies, in which two or more workpieces are pierced at each stroke of the press. Additional information on dies is available in the article "Blanking of Low-Carbon Steel" in this Volume.

Punches. Figure 6 shows three types of punches used for piercing: conventional, standard quill, and telescoping-sleeve quill. Conventional punches are generally available with a standard maximum shank diameter of 25 mm (1 in.), and they can be used to pierce round, square, oblong, or rectangular holes that have a size and shape not exceeding 25 mm (1 in.)

in diameter or a size and shape that can be machined or ground on the end of the shank, as shown in Fig. 6(a). Punch shanks are also available in other sizes and shapes. Standard-size punch retainers, backing plates, die buttons, and die button retainers are also generally available.

The head-type punch shown in Fig. 6(a) is held by a carefully positioned and ground punch retainer. This type of punch cannot be replaced without removing the retainer. Headless punches are available with locking devices that permit replacement without retainer removal.

A spring-loaded and guided stripper is incorporated into the die design. Conventional punches for small-diameter holes and accurately spaced holes are supported and guided by hardened bushings pressed into the guided stripper plate. Piloting punches can also be guided in the same manner. For larger punches that do not need guidance or support, clearance holes are drilled in the stripper plate, and guide bushings for both the punch and the plate are omitted.

Figures 6(b) and 6(c) show two types of quill punches that replace conventional punches when piercing conditions are more severe or when more support is required for the punch when piercing small-diameter and accurately spaced holes to close tolerances. The type shown in Fig. 6(b) was designed to hold and align a small-diameter punch. The quill containing a close-fitting punch is pressed into the punch retainer. The punch can easily be changed and still maintain the original alignment and fit in the retainer. A punch guide bushing is used in the stripper plate, and a hardened backup plate supports the head of the punch. Quills are available for punches with body diameters ranging from 1.0 to 9.52 mm (0.040 to 0.375 in.). The nib or end can be ground to a smaller diameter if desired.

The telescoping-sleeve quill punch shown in Fig. 6(c) provides complete support to small-diameter punches and eliminates the weaknesses encountered in other punch-mounting designs. The upper part of the sleeve is press fitted into the punch retainer; the lower part, into the guided stripper plate. The inside diameter of the sleeve will accommodate punch bodies ranging from 0.38 to 9.52 mm (0.015 to 0.375 in.) in diameter. However, a punch body at the larger end of the range and with a ground nib is suggested for best results. When piercing holes in printed circuit boards, a 2° cone-type taper is ground on the bottom surface of the lower sleeve to concentrate the holding and stripping force at the edge of the hole.

Quill punches have been used to pierce holes in low-carbon steel having a thickness up to twice the punch diameter. Supporting sleeves or quills can be used for long, narrow punches of rectangular, oblong, or other shape.

Piercing dies can be ground into a hardened die block, or they can be die buttons that are press fitted into a die retainer, as shown in Fig. 6(a). To coordinate the quill, guide bushing, and die button, the punch retainer, stripper plate, and die retainer can be clamped together and jig bored and ground at the same time. This is possible because the quill, guide bushing, and die button are available with the same body diameters.

Guided Strippers. For all three types of punches, the function of the guided stripper is threefold. On the downstroke, the spring-actuated stripper contacts the work metal ahead of the punches and acts as a hold-down. On the upstroke, the metal is stripped from around the punches. The third function is to guide small punches. To ensure that the force at the points of contact and release is sufficient to accomplish the stripping, stripper springs must exert the calculated force in the open-die position (not just in the closed-die position).

Self-Contained Tools. Piercing tools can also be purchased as individual units consisting of frame, punch, die, and spring or hydraulic strippers. The self-contained unit is not attached to the press ram, but is located, pinned, and bolted to a die plate, template, or T-slot plate mounted on the bed of any type of press having adequate shut height. The units can be reused by relocating, pinning, and bolting. They can be used singly or in groups and with notching units of the same construction. Punch and die sizes can be replaced as desired, and standard sizes and shapes are available for piercing thicknesses up to 19 mm ($\frac{3}{4}$ in.). The units are available in various styles for the horizontal and vertical piercing of flat or flanged workpieces.

Tool Materials. The materials used for piercing punches and dies are selected to suit the service requirements. In general, the materials used are the same as those for blanking (see the article "Blanking of Low-Carbon Steel" in this Volume).

Piercing involving unusual shock and high impact may require a shock-resistant tool steel such as S7. As in blanking, M2 high-speed steel is used for long punch life, particularly in piercing thicker steel, or where high abrasion resistance is required. In Example 7 in this article, a cam-actuated punch made of an air-hardening tool steel was replaced by a punch made of low-carbon steel, carburized and hardened. This change increased tool life tenfold.

Information on the selection of tool material and data on tool life are given in the article "Selection of Material for Blanking and Piercing Dies" in this Volume.

Use of Single-Operation Dies

Single-operation piercing dies are used:

- When piercing is the only operation to be performed
- When the holes to be pierced are so close to the edge of the work that the incorporation of a piercing operation into a compound die would weaken the die elements
- When the required accuracy or the sequence of operations prevents the inclusion of piercing in compound, progressive, or transfer dies

In large work, such as panels for automobiles, holes are often pierced in a separate operation for numerous reasons. For example, the holes may distort during forming, accuracy of position may be impaired if holes are pierced before forming, or separate operations may provide a more balanced work load and reduce maintenance.

As an example, when a hole is pierced and coined to a bevel with the same punch, coining extrudes an excessive burr between the punch and the die unless a close punch-to-die clearance is maintained. Die maintenance can be reduced by using separate piercing and coining dies.

Use of Compound Dies

Compound dies are used for most piercing operations in which accuracy of position is important. Except for the production of small lots, the use of a compound die is usually the most economical method for making a pierced and formed part to commercial tolerances. The following example describes the use of a compound die to blank and pierce a U-shaped bracket before it was formed.

Example 1: Piercing Five Holes with a Compound Blank-and-Pierce Die.

The mounting bracket shown in Fig. 7 was made of 4.55 mm (0.179 in.) thick hot-rolled 1010 to 1025 steel strip, 200 mm ($7\frac{7}{8}$ in.) wide, in two operations: blank and pierce, then U-form. A compound die was used to blank the outline and pierce three round holes and two slots. The holes were reamed to size in a secondary operation to hold the 0.025 mm (0.001 in.) tolerance on the diameter. The compound die was made of oil-hardening tool steel (O2) hardened to 58 HRC. Punch-to-die clearance was 5% of stock thickness per side.

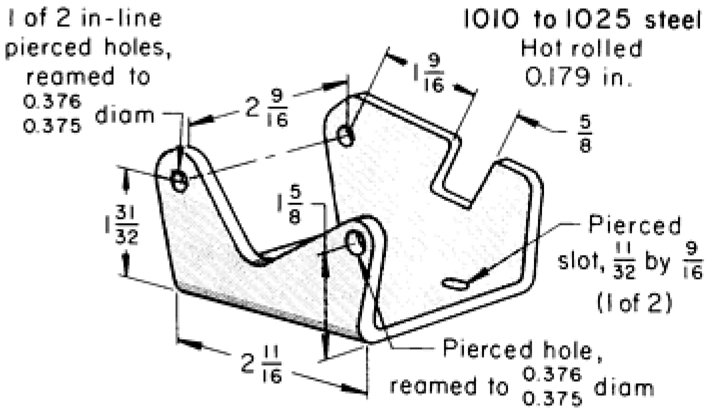


Fig. 7 Bracket that was blanked and pierced in a compound die before being formed. Dimensions given in inches

The blank was located by pins in the 8.7 × 14 mm ($\frac{11}{32} \times \frac{9}{16}$ in.) slots for the forming operation. A spring-loaded pressure pad held the blank firmly against the punch during forming. To overcome springback, the flanges were overbent by 2 to 3°. The forming die was made of air-hardening tool steel (A2).

Total tolerance on location of the in-line holes was 0.25 mm (0.010 in.). Production lots were 50 to 100 pieces. Expected die life was 15,000 to 20,000 pieces before regrinding.

Piercing of Low-Carbon Steel

Use of Progressive Dies

Progressive dies perform blanking, piercing, and other operations in successive stations of a die. Each station in a progressive die is similar to a simple die or a compound die. The workpiece in a progressive die remains connected to the strip of work metal until the last station in the die, so that the feeding motion carries the work from station to station.

In a progressive die, piloting holes and notches are pierced in the first station. Other holes can be pierced in any station if they are not affected by subsequent cutting or forming. Holes for which the relative position is critical are pierced in the same station; other holes are distributed among several stations if they are close together or near the edge of a die opening. Tolerances on hole shape, size, or location dictate whether holes are pierced before or after the part is formed.

It is often advisable to add idle stations or to distribute the work over one or two additional stations, so that holes will not be pierced near the edge of a die block. The die block is therefore stronger, and there is less chance of the die cracking in operation or fabrication. Adding stations also allows better support for the piercing punches and increases strength to the strip.

Progressive dies are more expensive than a set of single-operation dies for the same part; therefore, progressive dies are generally used for high production. However, because one part is made at each press stroke, direct labor costs are greatly reduced, and one operator can often attend to more than one progressive die. Manufacturing costs can also be reduced by making a pierced and formed part in a progressive die, rather than in two separate dies (one compound and one single-operation die).

The amount of scrap produced in progressive dies is generally high because the nesting of parts is somewhat limited and because material must be provided for connecting tabs and carrier strips. A fully automatic press with cutoff, feed, straightener, and coil cradle or reel is normally used with a progressive die; therefore, press costs are high. Pierced parts can sometimes be made most economically in a progressive die by using coil stock that is the exact width of the developed blank. In the following example, the high-volume production of a bracket in a progressive die is compared with the production of smaller quantities in separate operations in utility tooling.

Example 2: Producing a Bracket in Large Quantities in a Progressive Die and in Small Lots with Utility Tooling.

Figure 8 shows a bracket and the strip development for producing it in a five-station progressive die in a 670 kN (75 tonf) mechanical press that had a 102 mm (4 in.) stroke, an air-actuated stock feeder, and an automatic oiler. Material for the bracket was coiled cold-rolled low-carbon steel strip, 2.41 mm (0.095 in.) thick by 13 mm ($5\frac{1}{4}$ in.) wide, in the No. 2 (half-hard) temper.

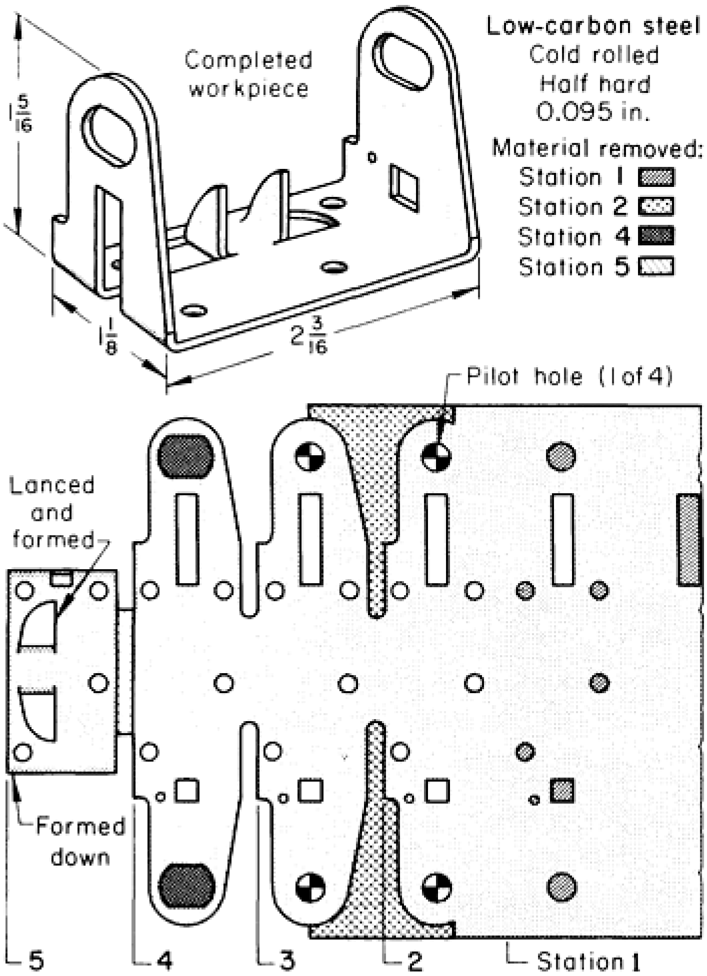


Fig. 8 Bracket that was produced more economically and more accurately in a progressive die (strip layout shown) than by the separate-operation method used for small quantities. Dimensions given in inches

The die was made of D2 tool steel and hardened to 59 to 60 HRC. Setup time was 1.5 h, and the press was stopped for die maintenance at intervals of 15,000 pieces. Production rate was 1200 pieces per hour. A light paraffin oil was the lubricant. The tolerance on the dimensions of all of the pierced holes was +0.051, -0.025 mm (+0.002, -0.001 in.), and the tolerance on the position of the square hole and the two rounded slots was ±0.13 mm (±0.005 in.).

Small quantities of the brackets were made from coil stock 29 mm ($1\frac{1}{8}$ in.) wide in the following operations, using utility tooling:

- Cut stock into 13 mm (5 in.) lengths (3500 pieces per hour)
- Trim end in a single-operation die, two strokes per piece (1000 pieces per hour)
- Pierce two holes 10.1 × 13.3 mm (0.398 × 0.523 in.), one at each end, in a single-operation die, two strokes per piece (1000 pieces per hour)

- Pierce a 4.88 mm (0.192 in.) wide slot and a 5.11 mm (0.201 in.) square hole, and lance and form two ears, all in a compound die for accurate relative position (2000 pieces per hour)
- Bend the ends down, locating on the ears (1000 pieces per hour)
- Drill four holes 4.90 mm (0.193 in.) in diameter in a multiple-spindle drilling machine (500 pieces per hour)
- Drill small hole next to square hole (500 pieces per hour)

The short-run method required eight times as many man-hours per 1000 pieces as the progressive-die method, and the brackets produced were less accurate.

Piercing of Low-Carbon Steel

Use of Transfer Dies

Transfer dies are used for piercing in applications that are similar to those for which progressive dies are used. A number of operations are done in successive stations of the transfer die.

Blanking, cutoff, lancing, notching, forming, and drawing (as well as piercing) can be done in transfer dies. The method differs from progressive-die operation in that the workpiece does not remain attached to the strip for feeding, but is fed from station to station by transfer fingers. Production quantities must be large enough to justify the cost of tooling and equipment.

Piercing of Low-Carbon Steel

Accuracy

Accuracy in the dimensions between pierced holes is highest for holes that are pierced by the same die in one press stroke. Accuracy in the location of holes relative to an edge or some other feature is highest when the workpiece is blanked and pierced (or pierced and trimmed) in the same stroke in a compound die.

When the above procedures are used, total tolerances of 0.25 mm (0.010 in.) on hole location and 0. 13 mm (0.005 in.) on hole size are readily met in normal production, and closer tolerances can be met with suitable tools, as indicated in Table 4. Tooling cost and per-piece cost usually increase in piercing to closer tolerances. Accuracy is typically somewhat lower for holes pierced by different dies or in different stations of a progressive or transfer die because of piloting and nesting tolerances.

Table 4 Typical accuracy in piercing

For holes that are pierced with a conventional die in the same press stroke. Location will be less accurate for holes that are pierced with different dies, or pierced in different stations of a progressive die or a transfer die.

Finish on tools	**Die retainers used**		Total tolerance on pierced holes			
			Location[a]		Size[b]	
	Typical material	**Locating holes for tools**	**mm**	**in.**	**mm**	in.
Commercial ground	1020 or 4130[c]	Drilled in a drill press	0.25	0.010	0.13	**0.005**
Commercial ground	1020 or 4130[c]	Jig bored and jig ground	0.10	0.004	0.13	**0.005**

Precision ground	**4130(d)**	**Jig bored and jig ground**	**0.05**	**0.002**	**0.03**	**0.001**

(a) Relationship between hole centers or between hole center and edges or other reference points on the workpiece.

(b) Diameter for round holes, or other control dimension for holes of other shapes.

(c) Can be hardened; other grades of steel also can be used.

(d) Hardened and tempered before being jig ground

Tolerances smaller than the lowest given in Table 4 can be met with the use of special tooling and gaging and close control over the press operations, but only at increased cost and a lower production rate. The use of shaving to produce holes to a tolerance of less than 0.025 mm (0.001 in.) in size is described in Example 11. The use of fine-edge blanking for improved accuracy and edge quality is discussed in the article "Fine Edge Blanking and Piercing" in this Volume.

Accuracy of hole location is increased by the use of a rigid stripper, precisely aligned on guideposts, to guide the punches. Typical clearance of round punches in the stripper (using drill bushings as guides) is 0.005 to 0.013 mm (0.0002 to 0.0005 in.) total. Lubrication is important in dies that have such close clearance between punches and stripper. At speeds of 40 strokes and more per minute, the die must be lubricated constantly with a spray of light machine oil to prevent galling of the punches in the guides.

Accuracy often requires that holes be pierced after forming. In some cases, it may be necessary to pierce a hole after forming in order to avoid distortion of the hole. In Example 8, holes on opposing flanges were cam pierced in one stroke after forming for accurate alignment. In Example 7, two slots were pierced in flanges after forming, instead of being machined, for accurate alignment and location.

Noncritical holes, for which there are no close tolerances on size or spacing, can be pierced for venting (to provide for free passage of air or other fluids), for lightening, for improved bond with a molded plastic cover, for increased flexibility of a workpiece, and even to provide controlled strength. In the following example, noncritical holes were pierced to weaken a part so it would deflect under impact or shock loading.

Example 3: Piercing Noncritical Holes.

The workpiece shown in Fig. 9 is a wheel spider that was designed to be enclosed in molded plastic and to deflect under impact load. The spider was made of hot-rolled 1008 or 1010 steel, dead soft, pickled and oiled. The steel strip, 3.2 mm ($\frac{1}{8}$ in.) thick by 406 mm (16 in.) wide, was hand fed into a two-stage progressive die in a 4 MN (450 tonf) mechanical blanking press. In the first stage, noncritical holes were pierced to make the spider deflect upon impact and to provide a better bond with the molded plastic. The piece was blanked in the second stage. The pierced blanks were then fed into a 5.3 MN (600 tonf) mechanical forming press, where the part was formed. The forming die was sprayed with soluble oil.

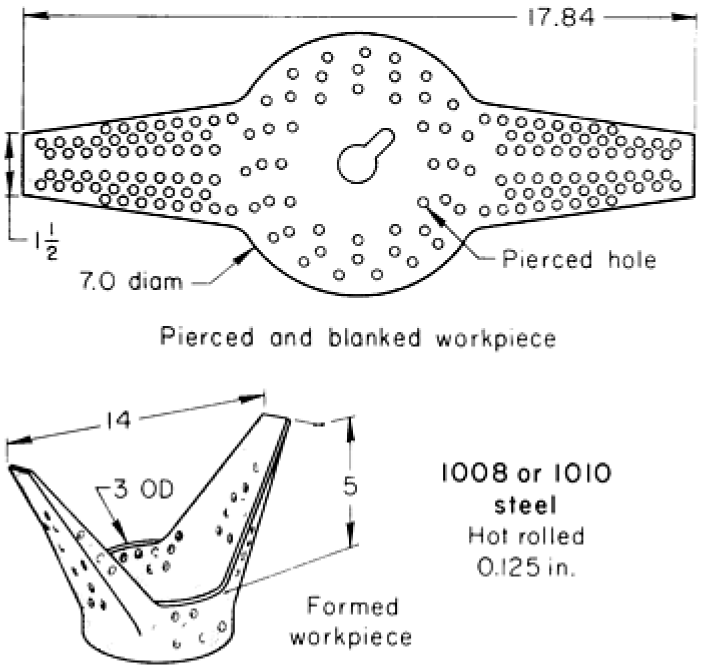


Fig. 9 Wheel spider in which noncritical holes were pierced for deflection under impact and to improve bonding to plastic. Dimensions given in inches

Hole Size

Pierced holes can be of almost any size, ranging from holes as small across as the thickness of the stock to the largest size that can be adapted to the equipment available. Some holes can be pierced that are smaller across than the stock thickness, but such piercing is not common.

Small holes and slots are pierced in much the same manner as large holes, but small holes are more difficult to pierce because slender punches are comparatively weak. To minimize deflection and breakage, punch length is limited to that needed for the operation, and punches are specially stiffened and guided (Fig. 6). The minimum hole size that can be pierced in a specific application is ordinarily found by trial.

Piercing of Thick Stock

The effects of work metal thickness on piercing are generally the same as on blanking. Cutting force increases with thickness; consequently, tool design and material, press selection, and operating conditions are influenced by work metal thickness. The relationship between die clearance and work metal thickness is discussed in the sections on die clearance in this article. The minimum pierced hole size is usually expressed as a function of work metal thickness, as described in the preceding section. The following example illustrates the piercing of unusually thick flat workpieces prior to forming.

Example 4: Piercing a Square Hole in 19 mm ($\frac{3}{4}$ in.) Plate.

A hole 17 mm ($\frac{11}{16}$ in.) square was pierced through high-strength low-alloy steel plate 19 mm ($\frac{3}{4}$ in.) thick in a die in a 1.1 MN (120 tonf) open-back inclinable press. The die was made to pierce holes one at a time in any of several flat workpieces. A typical workpiece size was 19 × 305 × 940 mm ($\frac{3}{4}$ × 12 × 37 in.).

The production rate for piercing in D2 tool steel dies was 300 to 360 pieces per hour. Maximum yearly production was 1000 pieces.

After the holes were pierced, the workpiece was heated to 815 °C (1500 °F) and formed into a curved shape. The shape was used as a digger bucket--one of many that were bolted to a large wheel (digger rim) of a machine used for digging trenches for sewers and pipelines.

Radial Piercing of Curved Surfaces. The radial piercing of thick curved workpieces is done in the same manner as the piercing of flat workpieces, except that the die must be designed to accommodate the curved parts. The piercing of a hole through round stock, such as a radial hole through a cylinder, is done by using a die with a heavily loaded stripper, both of which fit the round shape of the workpiece. The round hole is then readily pierced with little bulging of the workpiece, even though the hole can be as large as 40% of the diameter of the workpiece. The center of the hole should be at a distance from the end of the workpiece that is at least equal to the work thickness (diameter of the rod).

Piercing of Thin Stock

Because of its strength and rigidity, material thicker than 3.2 mm ($\frac{1}{8}$ in.) is seldom blanked or pierced from coil stock or in a progressive die. On the other hand, material thinner than 0.51 mm (0.020 in.), because of its lack of strength and extreme flexibility, generally requires special handling techniques. The following example describes the blanking of steel shims from thin coil stock in a compound die.

Example 5: Piercing of Shims from 0.25 mm (0.010 in.) Strip in a Compound Die.

The shim shown in Fig. 10 was made from coil stock of cold-rolled 1008 steel, 19 mm ($\frac{3}{4}$ in.) wide by 0. 25 mm (0.010 in.) thick, in a pierce-and-cutoff compound die. Thickness tolerance was +0, -0.025 mm (+0, -0.001 in.). The coiled strip was fed automatically from a stock reel by an air-operated slide feed into a 220 kN (25 tonf) open-back inclinable press that operated at 150 strokes per minute.

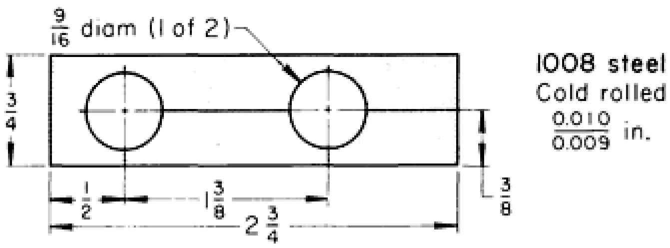


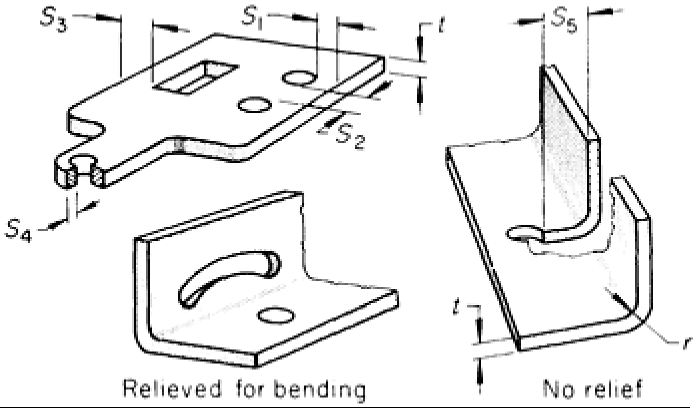
Fig. 10 Shim that was pierced and cut off from thin coiled strip in a compound die. Dimensions given in inches

The die, made of oil-hardening D3 tool steel, produced 230,000 shims before it needed sharpening. Die life was indefinite because a thickness of 38 mm ($1\frac{1}{2}$ in.) had been provided for grinding allowance in repeated sharpenings in order to restore the cutting edge.

Hole Spacing

Recommended minimum spacings for pierced holes are given in Table 5. These minimum spacings apply when ordinary pressworking practices are followed, without confinement of the workpiece in the die or other special procedures to prevent distortion.

Table 5 Recommended minimum spacings for pierced holes in flat and formed steel and nonferrous metal workpieces



Dimension	Work metal thickness (t), mm (in.)	Minimum distance, mm (in.)
S_1 and S_2	<1.57 (0.062)	**3.05 (0.120)**
	1.57-9.65	
	(0.062-0.380)	**3.05 (0.120) (but at least 1.5*t*[(a)]**
S_3 and S_4	<0.81 (0.032)	**1.52 (0.060)**
	0.81-3.18	
	(0.032-0.125)	**2*t***
	3.18-9.65	
	(0.125-0.380)	**2.5*t***

(a) For steel. Minimum for nonferrous metals, 2*t*.

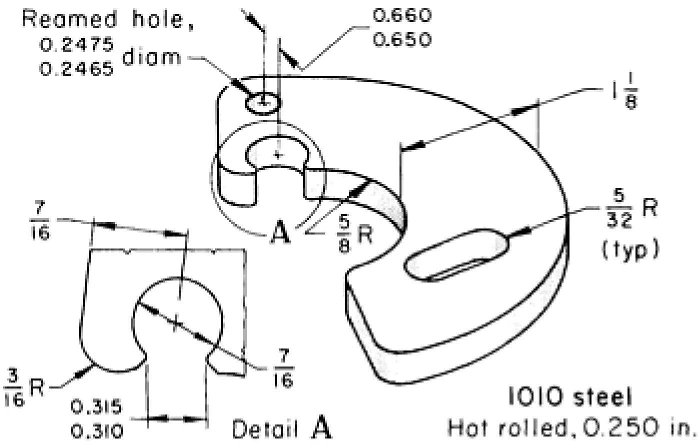
(b) For flanges with no relief. Value for S_5 can be reduced when work metal is relieved for bending (as shown at lower left above) to prevent distortion of the pierced hole in forming.

As noted in Table 5, S_5 (the distance from the edge of a hole to the inside of a flange) can be reduced when the metal is relieved near the pierced hole (as by a slot) to prevent distortion of the hole in forming. Accuracy in the shape and location of pierced holes often demands that the holes be pierced after the workpiece has been formed.

When a round hole must be pierced so as to leave almost no metal between the edge of the hole and the edge of the part, the hole can be cut through the edge in a keyhole shape that minimizes bulging and does not leave sharp points. In the following example, spacing between the keyhole and the end of the part was only 58% of the recommended spacing given in Table 5, and spacing for the round hole and the slot was only 50% of the recommended spacing. Confinement of the blank in the die prevented distortion at the keyhole and the slot.

Example 6: Piercing Holes at Less Than Recommended Minimum Edge Distance.

The flyweight shown in Fig. 11 was part of a centrifugal device used to release pressure. The part was made of hot-rolled 1010 steel, pickled and oiled, 6.4 mm ($\frac{1}{4}$ in.) thick. Preliminary design had a wall thickness of 2.4 mm ($\frac{3}{32}$ in.) between the 11 mm ($\frac{7}{16}$ in.) diam hole and the edge of the part. The original tooling called for a compound die to pierce and blank the part completely in one press stroke. However, with the fragile punch required, it was impossible to hold the 8.00/7.87 mm (0.315/0.310 in.) dimension on the keyhole opening (Detail A, Fig. 11).



Sequence of operations	
Shear 1-2 m (4-8 ft) long by 100 mm (3 $\frac{7}{8}$ in.) wide Blank outline; pierce round hole (1000 pieces/h) Ream and deburr round hole (300 pieces/h) Pierce keyhole and oval slot (770 pieces/h)	
Operating conditions	
Type of press	**670 kN (75 tonf) mechanical**
Press speed	**55 strokes per minute**
Die material	**A2 tool steel at 58-60 HRC**
Lubricant	**Sulfur-base, EP type[(a)]**
Production rate[(b)]	**300 pieces per hour**

Die life per grind	**20,000 pieces**
Total die life	**1 million pieces**

(a) Applied to strip by roller.

(b) Lot size was 2500 pieces; annual production, 10,000 pieces.

Fig. 11 Flyweight in which holes were pierced at less than recommended minimum distances from the edge. Overall length of the flyweight was 90 mm ($3\frac{1}{2}$ in.). Dimensions given in inches

Production was successful when the part was made in three separate dies, using the sequence of operations shown in the accompanying table. A keyhole punch was used to pierce the 11 mm ($\frac{7}{16}$ in.) diam hole. The hole-to-edge spacing was increased from 7.9 to 11.1 mm ($\frac{5}{16}$ to $\frac{7}{16}$ in.).

Distortion was minimized by confining the blank in a nest during piercing. In the compound die, the hole shown in Detail A and the edge of the part were connected with a radius tangent to both. When the keyhole punch was used, the punch surface intersected the outer edge at a 45° angle--a change that did not interfere with the function of the part but avoided difficulty in making a transition radius tangent to the outer edge.

The hole to be reamed was pierced with a 5.74 mm (0.226 in.) diam punch and a 6.25 mm (0.246 in.) diam die. The hole was later reamed to 6.29/6.26 mm (0.2475/0.2465 in.) in diameter for nearly its full length and then deburred. This hole was perpendicular to the part surface within +0° 45'.

Effect of Forming Requirements

It is simpler to pierce holes in a flat sheet than in a part that has been formed. Holes near a bend radius (see the illustration in Table 5) are usually distorted when the part is formed. If distorted holes are unacceptable or if an accurate relation of holes to other features in a workpiece is specified, piercing must be done after forming.

Dies for piercing after forming are generally more complex, more expensive, and require more maintenance than dies for flat blanks. These dies often have cam-actuated punches.

In Example 9, when the part was U-formed after piercing, one hole closed in during forming, resulting in an elongated hole. This deformation was taken into consideration during product design. The following two examples describe applications in which it was necessary to pierce holes after forming.

Example 7: Piercing of Accurately Located Slots after Forming.

Figure 12 shows a formed part that, because of close tolerances, should not be slotted before forming. The original procedure was to drill the holes and mill the slots to hold the alignment of the slots to the end radii and to the tongue within 0.08 mm (0.003 in.). Results from machining were unsatisfactory, and it became necessary to pierce the slots after forming in order to hold tolerances.

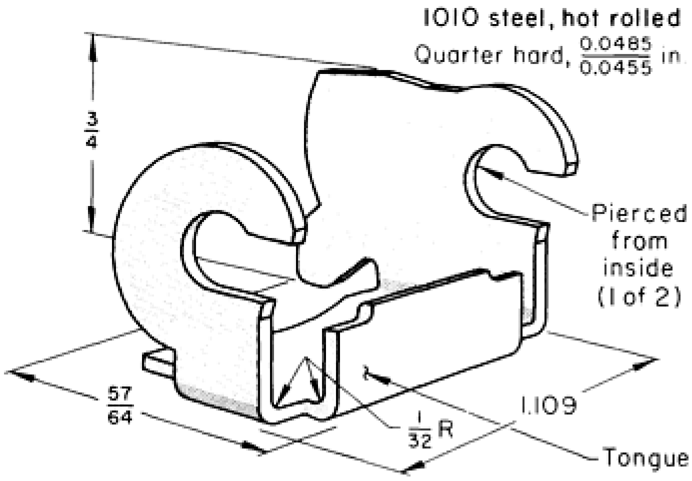


Fig. 12 Part in which accurately aligned slots were pierced (after forming) by two punches that were cam operated from the inside. Dimensions given in inches

The size and shape of the part and the location of the slots precluded piercing from the outside. Piercing was done from the inside by two punches split on the centerline of the part and moved outward by cam action.

The original punches were made of air-hardening tool steel hardened to 58 to 60 HRC. Because of breakage, the maximum punch life was 10,000 pieces. Changing the punch material to carburized and hardened 1025 steel increased the average punch life to 100,000 pieces. Annual production was about 240,000 pieces in lots of 50,000 to 60,000.

Example 8: Cam-Piercing Holes in Opposing Flanges of a Formed Part for Accurate Alignment.

To maintain alignment of the two opposing holes in the automobile-frame control-arm bracket shown in Fig. 13, the holes were cam pierced in one press stroke after the part was formed. Tolerance on hole alignment was ±0.13 mm (±0.005 in.).

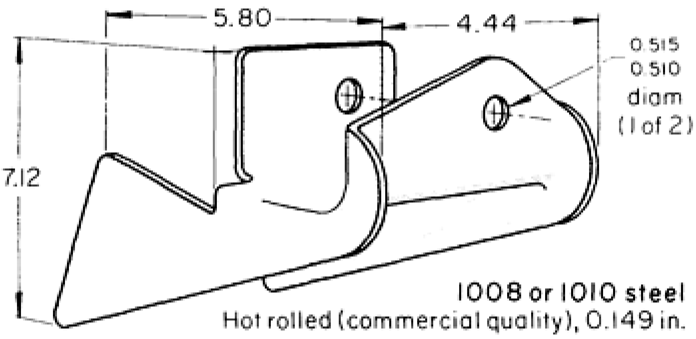


Fig. 13 Bracket in which accurately aligned holes on opposing flanges were cam pierced in one press stroke after forming. Dimensions given in inches

The bracket was made of commercial-quality 1008 or 1010 steel, as-rolled, 3.78 mm (0.149 in.) thick, in five operations:

- Blank two workpieces per stroke
- Prebend, form, and re-form in three separate dies, side by side
- Form ear
- Trim in two stages
- Restrike and campierce

Blanking was done at 1800 pieces per hour in a 4.4 MN (500 tonf) coil-fed automatic blanking press. The remaining operations were done at 135 pieces per hour in a 2.7 MN (300 tonf) or a 3.6 MN (400 tonf) mechanical press.

The use of high-production equipment, including the cam-operated piercing die, was economical for the annual production of 350,000 pieces in 20,000-piece lots. Drilling would have been used for making the holes if 10,000 or fewer pieces had been needed per year to meet production requirements.

Piercing Holes at an Angle to the Surface

For piercing holes that are not perpendicular to the surrounding surface, the workpiece is securely clamped to the die with a pressure pad, and the dies are usually ground to fit the contour of the part. The shape of the punch nose depends on the angle of contact with the workpiece and on the stock thickness.

In one application, holes 7.9 mm ($\frac{5}{16}$ in.) in diameter were pierced in 6.35 mm (0.250 in.) thick 1090 steel at an angle of 83° to the surface. The holes were pierced by using sleeved punches and clamping the work tightly to the die with a pressure pad. In the following example, holes were pierced at an angle of 40° 30' to the surface of a flat workpiece, which was later formed by bending.

Example 9: Piercing Holes at an Angle to the Work Surface.

Two 12.8 mm (0.505 in.) diam holes were pierced at an angle of 40° 30' to the surface in the 2.67 mm (0.105 in.) thick cold-rolled 1010 steel flat blank for the lamp-bracket base shown in Fig. 14. To pierce the compound-angle holes, the blank was placed in a nest, which held it at the proper angle and position for piercing. By repositioning the blank in a second nest, it was possible to pierce both of these holes with one punch and die.

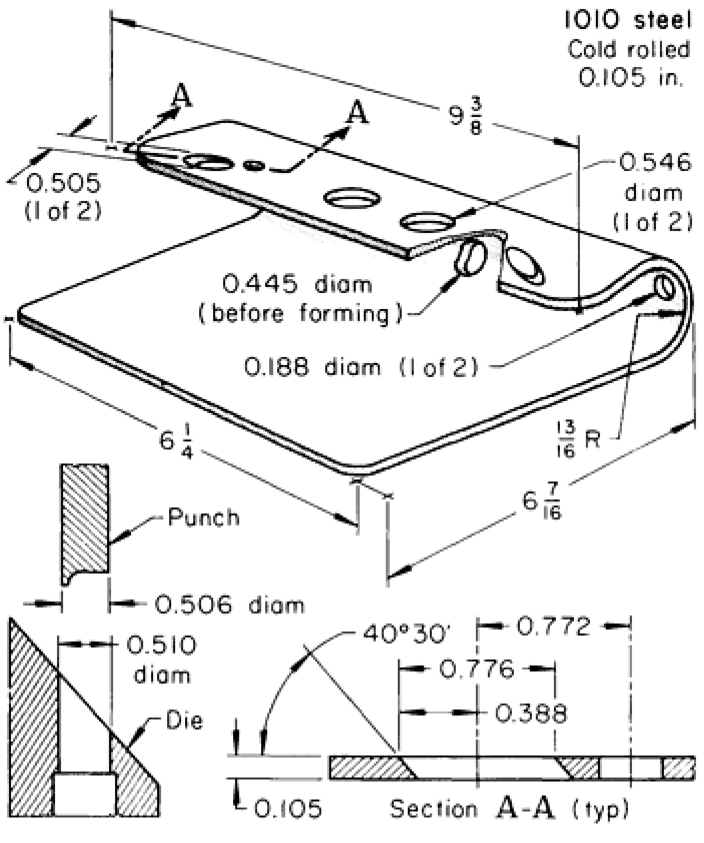


Fig. 14 Lamp-bracket base in which two holes were pierced at an angle to the surface using the punch shown at lower left. All holes were pierced before the part was formed. The 90° hole in the U-bend zone deformed to an elliptical shape during forming. Dimensions given in inches

The face of the die button was ground flush with the surface of the nest. The die had a straight land with a minimum length of 9.5 mm ($\frac{3}{8}$ in.). There was a 16 mm ($\frac{5}{8}$ in.) diam relief below the land. The punch was ground with a step, as shown in Fig. 14. This step curled the slug, permitting the use of a long straight land in the die. The punch and die were made of M2 high-speed steel, hardened to 58 to 59 HRC. Punch-to-die clearance was 0.05 mm (0.002 in.) per side. Die life was 15,000 holes per grind.

The punch was mounted in a heavy quill designed for quick changing, which proved unnecessary. A spring-loaded pressure pad guided and added support to the punch and held the blank securely in the nest. The pressure pad was interlocked with the die so that shifting could not take place after piercing started. The die was run in a 90 kN (10 tonf) mechanical press, which produced 300 pieces per hour.

Before the angle holes were pierced as described above, the outline of the part and seven 90° holes had first been produced in a compound blank-and-pierce die. The blanking die and punch, as well as the punches and die buttons for the 90° holes, were made of O1 tool steel and hardened to 59 to 60 HRC. The die was mounted in a 900 kN (100 tonf) mechanical press producing 300 pieces per hour. Die life was 40,000 pieces per grind.

When the workpiece was bent through 180° after all of the holes had been pierced, the 11.3 mm (0.445 in.) diam hole changed in shape, assuming final dimensions of 10.4 × 11.3 mm (0.411 × 0.443 in.). This deformation did not affect the function of the hole.

Special Piercing Techniques

Piercing operations that require special tooling and techniques include the piercing and forming of flanged holes, piercing with a fastener and with a pointed punch, and tube piercing.

Flanged holes (sometimes called extruded, countersunk, dimpled, or burred holes) are generally used for assembly purposes, such as providing more thread length for a tapped hole, greater bearing surface, or a recess for a flathead screw or rivet. The flanged hole can be produced by forcing a punch of the desired hole diameter through a smaller prepierced hole or by using a shouldered or pointed punch that both pierces the hole and flanges it.

The depth of flange that is formed depends on the elongation of the metal, and the flange is thinnest at its outer edge. A deeper flange can be made by extruding metal into the flange. Such flanging is done by first piercing a smaller lead hole and then using a punch that extrudes metal around the hole into the die clearance to produce a flange and simultaneously coins or forms a slight chamfer (depressed cone) or other shape of recess into the hole (see also the article "Press Bending of Low-Carbon Steel" in this Volume). This kind of extruded flange has uniform wall thickness. Such extrusion causes more metal flow and greater work hardening than ordinary piercing of flanged holes.

Piercing with a fastener (self-piercing) is primarily used as an assembly technique. A rivet, for example, can be used as a punch to pierce a hole through the material that it will join. The following example describes an application in which a square nut with a sharp face served as a piercing tool and then became part of an assembly.

Example 10: Automatic Assembly of Self-Piercing Nut into a Bracket.

A square nut was automatically assembled into a body-bolt bracket, as shown in Fig. 15. The nut served as the piercing punch to make a 17.4 mm (0.687 in.) square hole in the embossed portion of the bracket. The bracket was made of galvanized, hot-rolled 1006 steel, 1.9 mm (0.075 in.) thick. After being pierced, the metal sprang back into two grooves in the nut, locking the nut into the pierced square hole. The nut was fed from a special installing head that was loaded from a rotary hopper.

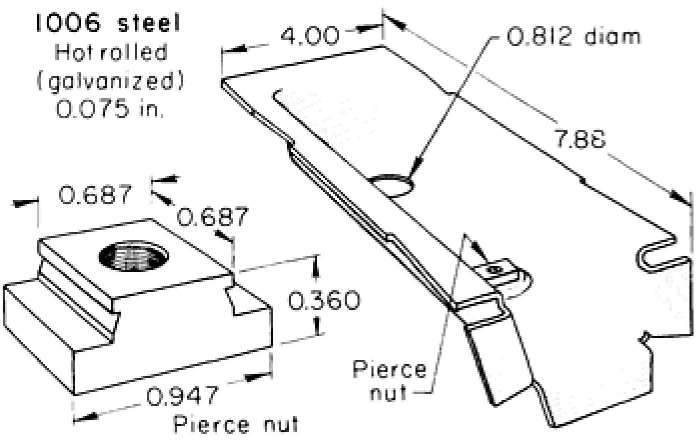


Fig. 15 Bracket with a square nut that pierced a hole for itself. Dimensions given in inches

Two assemblies were completed (pierced and installed) at each stroke of a 640 kN (72 tonf) open-back inclinable press running at 45 strokes per minute. Maximum daily production was 12,000 assemblies. The bracket was fed by a gravity-slide feed at the front of the press and was unloaded at the rear with the help of an air blast.

The 20.6 mm (0.812 in.) diam hole was pierced at the same time the nut was inserted. Commercial punches and die buttons were used.

A standby unit for piercing a square hole was available in case a supply of the square nuts was not on hand when the part was scheduled to run. The nuts were later inserted and clinched using pneumatic equipment.

Holes made with a pointed punch, such as a cone-point, nail-point, or bullet-nose (ogive) punch, have rough or torn flanges. Such a hole is satisfactory for holding a sheet metal screw, acting as a spacer, or providing a rough surface.

Tube piercing and slotting are done in dies when production lots are large enough to pay for special tooling. Simple dies, as well as more complex tooling such as cam dies, are designed to hold, locate, and pierce tubes, drawn cups, and other round parts.

A mandrel can be used in a horn die for work on tubing and other round parts as well as for piercing, slotting, and notching. One version of a die that uses a mandrel permits piercing two opposing holes in a tube in one stroke, with the slug from one wall going through a hole in the mandrel to act as the punch for the hole in the opposite wall.

Tubes can also be pierced with opposing holes without using a mandrel. The bottom half of the tube is supported by the die, which has a nest with the same diameter as the outside diameter of the tube. A similar nest for the upper half of the tube is in a combined hold-down and punch guide. Therefore, the tube is completely surrounded during piercing. Because the bottom side of the tube is supported by the die, the lower hole is pierced without any distortion. The tube will collapse slightly around the hole in the top side of the tube. The amount of distortion of the top hole varies with the size of the tube and the hole.

Holes on opposite sides of a tube can be pierced simultaneously, using the tool shown in Fig. 16, which does not require a mandrel. The tool consists of identical upper and lower assemblies--one attached to the press ram and the other to the press bed. Each assembly consists of a punch and a spring-loaded combined nest, stripper, and punch guide. When used for multiple-hole piercing, the assemblies are usually mounted to upper and lower plates having tapped holes or T-slots. There will be a slight indentation around each hole, as shown in Fig. 16.

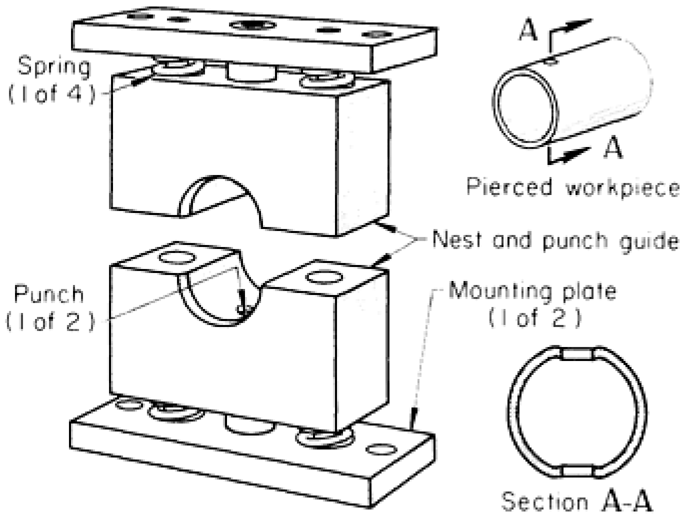


Fig. 16 Tool for tube piercing without a mandrel, and a pierced tube showing the indentations around the holes

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Piercing of Low-Carbon Steel

Shaving

Shaving is done in a separate operation or is included in one station of a progressive die (see the article "Blanking of Low-Carbon Steel" in this Volume for more information on shaving). The inclusion of a shaving operation in a progressive die generally increases the need for die maintenance, and the slivers of shaving scrap can jam the feeding mechanism. A replaceable insert can be used in a shaving die for easier maintenance.

Shaving allowance depends on the workpiece material and on its thickness.

Shaving plus burnishing is used to produce greater accuracy in a pierced hole than can be obtained by shaving alone, as shown in the next example.

Example 11: Use of Blanking, Piercing, Shaving, and Burnishing in Making Gear Blanks to Close Tolerances.

The small gear blank illustrated in Fig. 17 was produced from a 50 mm (2 in.) wide strip of cold-rolled 1010 steel of No. 2 temper in a five-station progressive die to the following specifications:

- Critical tolerance of +0.013, -0.010 mm (+0.0005, -0.0004 in.) on the center hole
- Finished blanks flat within 0.05 mm (0.002 in.)
- Surface finish of 0.70 μm (28 μin.) or smoother for 70% of the center-hole surface

These specifications were met by piercing, shaving, and burnishing the center hole in the sequence of operations indicated by the strip progression. A sulfurized and chlorinated extreme-pressure (EP) lubricant was applied to the coil stock by roller coating.

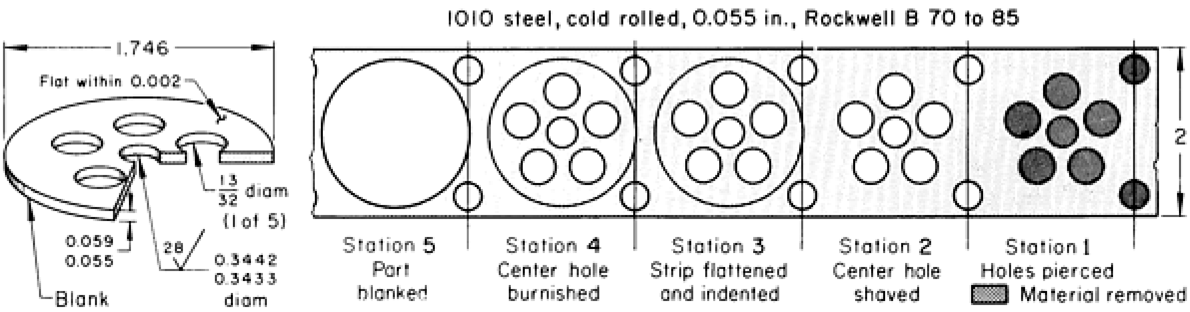

Fig. 17 Gear blank that was produced with an accurate center hole by piercing, shaving, and burnishing in a five-station progressive die. Dimensions given in inches

Annual production was 2 million pieces in four lots. The gear blanks were made at 150 pieces per minute in a 530 kN (60 tonf) press. The dies, made of M2 high-speed tool steel, had a life of about 100,000 pieces before regrinding and a total life of about 10 million pieces.

Piercing Versus Alternative Methods

Piercing is primarily used when accurate holes are required and when the production lot is large enough to justify the tooling costs. Alternative methods are used for smaller production lots or for holes that have a diameter less than stock thickness. Examples of alternative methods are drilling (including electrochemical machining), milling and sawing, and electric-arc and gas cutting. For increased efficiency, all of these methods can be applied to work that is stacked or nested.

Safety

Piercing, like other press operations, involves potential hazards to operators, maintenance people, and others in the vicinity. The articles "Presses and Auxiliary Equipment for Forming of Sheet Metal" and "Blanking of Low-Carbon Steel" in this Volume contain information and literature references on safe operation.

Introduction

FINE-EDGE BLANKING (also known as fine blanking) is a process developed in Switzerland to produce precise blanks in a single operation with smoother edges and closer tolerances than are possible with conventional blanking. In fine-edge blanking, a V-shaped impingement ring (Fig. 1) is forced into the stock to lock it tightly against the die and force the work metal to flow toward the punch, so that the part can be extruded out of the strip without fracture or die break. Die clearance is extremely small, and punch speed is much slower than in conventional blanking.

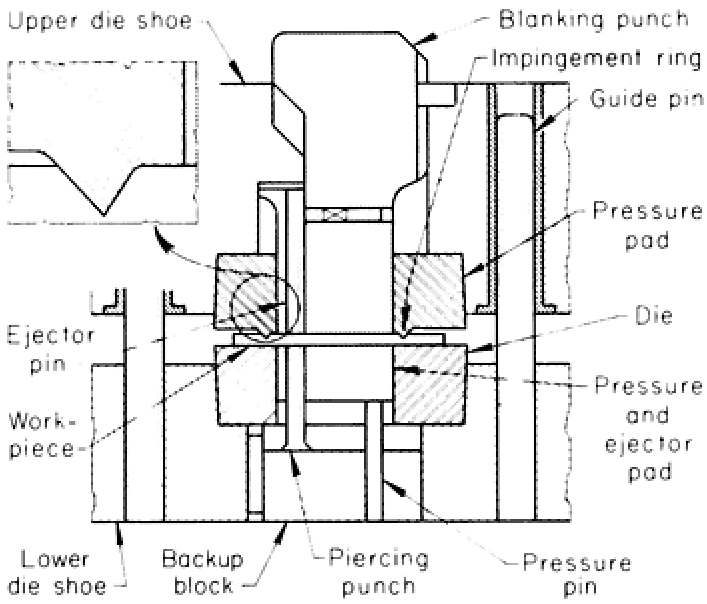

Fig. 1 Typical tooling setup for fine-edge blanking a simple shape

Fine-edge piercing can be done either separately or at the same time as fine-edge blanking. In piercing small holes, an impingement ring may not be needed.

No further finishing or machining operations are necessary to obtain blank or hole edges comparable to machined edges, or to those that are conventionally blanked or pierced and then shaved. A quick touch-up on an abrasive belt or a short treatment in a vibratory finisher may be used to remove the small burr on the blank. Specially designed single-operation or compound blanking and piercing dies are generally used for the process.

Comparison With Conventional Blanking

Fine-edge blanking and piercing (Fig. 2) are actually more akin to cold extrusion than to a cutting operation. They are similar to conventional blanking using a compound die (see the articles "Blanking of Low-Carbon Steel," "Piercing of Low-Carbon Steel," "Blanking and Piercing of Electrical Steel Sheet," and "Selection of Material for Blanking and Piercing Dies" in the Section "Blanking and Piercing of Steel Sheet, Strip, and Plate" in this Volume), but with several important differences.

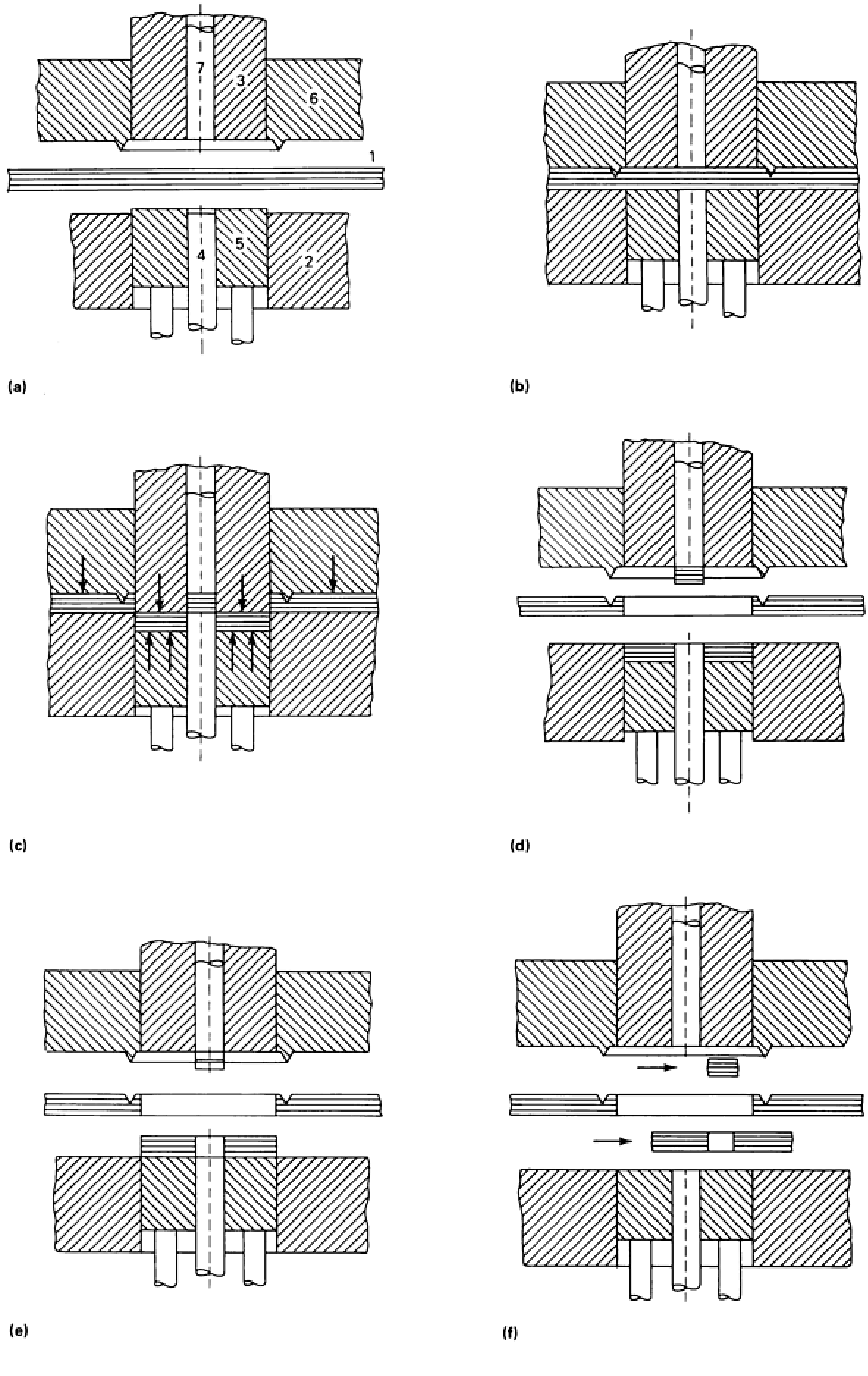

Fig. 2 The fine-edge blanking process. (a) Work material is fed into the die. 1, work material; 2, die plate; 3, punch; 4, piercing punch; 5, ejector/counterpunch; 6, impingement ring; 7, slug ejector. (b) The die closes,

and the V-shaped impingement ring is embedded into the work material. (c) The part is blanked. The impingement ring maintains pressure while blanking pressure and counterpressure are exerted against the part. (d) Slugs are ejected from the top down. (e) Part is ejected from the bottom up by reversing counterpressure. (f) Parts and slugs are removed from the die area.

First is the V-shaped impingement ring, which restricts the flow of metal away from the cutting edge and presses the material to be cut against the punch. Another important difference is the independent control of the three forces acting on the workpiece (clamping, counterpressure, and blanking) obtained through the use of a triple-action press (see the section "Presses" in this article).

The very small clearance between punch and die is another difference between fine-edge blanking and conventional blanking. Combined with correspondingly tighter tolerances on both punch and die and the fact that the punch enters the die only a slight amount if at all, this feature prevents cracking of the workpiece and results in smooth blanked and pierced edges.

Process Capabilities

Holes with diameters as small as 50% of stock thickness can be pierced in low-carbon steel. In high-carbon steel, the smallest hole diameter is about 75% of stock thickness. Holes can be spaced as close to each other, or to the edge of the blank, as 50 to 70% of stock thickness. Total tolerances obtainable are 0.013 mm (0.0005 in.) on hole diameter and for accuracy of blank outline; 0.025 mm (0.001 in.) on hole location with respect to a datum surface; and 0.025 mm (0.001 in.) on flatness.

No die break shows on the sheared surface of the hole. Blank edges may be rough for a few thousandths of an inch of thickness on the burr side of the part when the width of the part is about twice the stock thickness or less. Finish on the sheared edge is governed by the condition of the die edge and the land within the die. Parts fine-edge blanked from stainless steel will have a surface finish of 0.8 μm (32 μin.) or better. Smooth edges also are produced on spheroidize-annealed steel parts. Burr formation increases rapidly during a run, necessitating frequent grinding of the cutting elements.

Chamfers can be coined around holes and on edges. Forming near the cut edge, or forming offset parts with a bend angle up to 30°, is possible under restricted conditions.

Metals up to 3.18 mm (0.125 in.) thick having a tensile strength of 586 to 793 MPa (85 to 115 ksi) are easily blanked. Parts up to 13 mm ($\frac{1}{2}$ in.) thick can be blanked if press capacity is available. Material thicker than 3.18 mm (0.125 in.), especially steel having a carbon content of 0.25% or more, requires an impingement ring on the die so that the corners on the part will not break down. The edges of parts made of 1018 steel work harden as much as 7 to 12 points Rockwell C during blanking.

In tests on 0.60% C spring steel with a hardness of 37 to 40 HRC, the surface finish on the sheared edges was 0.8 μm (32 μin.) or better, but punch life was only 6000 pieces.

The cutting speed for fine-edge blanking is 7.6 to 15.2 mm/s (0.3 to 0.6 in./s).

Work Materials

Any material suitable for cold forming can be fine-edge blanked, including low- and medium-carbon steels, some alloy and stainless steels, copper and brass, and aluminum alloys (Table 1).

Table 1 Materials for fine-edge blanking

Material	Condition
Carbon steels	
1008-1024	**Up to fully hard**
1025-1095	**Spheroidize annealed**
Alloy steels	
AISI 4000 series, 8000 series	**Spheroidize annealed**
Stainless steels	
AISI types 301, 302, 303, 304, 316, 430, 416	**Fully annealed; shorter tool life than for plain carbon steels**
Aluminum alloys	
1*xxx*, 3*xxx*, 5*xxx* series	**H (strain hardened) temper; O (annealed) temper**
6061, 7075	**T3 or T4 for thinner gages**
Copper alloys	
C26000 (cartridge brass, 70%), C26800 (yellow brass, 66%)	**Annealed, quarter hard, or half hard**
C27400 (yellow brass, 63%)	**Fully hard in thin gages**
C17*xxx* (beryllium copper alloys)	**Annealed only**
Other nonferrous metals and alloys	
Monel alloy 400	**Annealed**
Soft bronzes	**Annealed**
Silver, gold	**Excellent results**

Carbon and Alloy Steels. The easiest materials to fine-edge blank are low-carbon steels (<∼0.25% C). Thin parts, however, have been fine-edge blanked from plain carbon steels containing up to about 0.95% C. Alloy steels can be fine-edge blanked only in the fully annealed, spheroidized condition (Table 1).

Stainless Steels. Austenitic stainless steels, such as AISI types 301 to 304 and 316, can be fine-edge blanked in the fully annealed condition. These materials, as well as the ferritic type 430 and martensitic type 416 (both fully annealed), have good blanked edges but cause higher tool wear than do plain carbon steels.

Most aluminum alloys, with the exception of alloy 2024, can be fine-edge blanked in the O (annealed) temper. Wrought alloys of the 1*xxx*, 3*xxx*, and 5*xxx* series can be fine-edge blanked in the H (strain hardened) temper with excellent results. In thinner gages, alloys 6061 and 7075 can be fine-edge blanked in the T3 or T4 condition.

Copper alloys are easily fine-edge blanked. The most workable alloys, such as alloy C27400 (yellow brass, 63%) can be fine-edge blanked even in the full hard condition. Other brasses, such as C26000 (cartridge brass, 70%) and C26800 (yellow brass, 66%) can be worked in the annealed, quarter-hard, or half-hard condition. Beryllium-copper alloys can be fine-edge blanked in the annealed condition, as can pure copper, soft bronzes, Monel alloys, nickel-silvers, and silver and gold.

Blank Design

Limitations on blank size depend on the thickness, tensile strength, and hardness of the work metal and on available press capacity. For example, perimeters of approximately 635 mm (25 in.) can be blanked in 3.18 mm (0.125 in.) thick low-carbon steel (1008 or 1010). It is possible to blank smaller parts from low-carbon or medium-carbon steel that is about 12.7 mm ($\frac{1}{2}$ in.) thick.

Sharp corner and fillet radii should be avoided when possible. A radius of 10 to 20% of stock thickness is preferred, particularly on parts over 3.18 mm (0.125 in.) thick or those made of alloy steel. External angles should be at least 90°. The radius should be increased on sharper corners or on hard materials.

Parts with tiny holes or narrow slots to be pierced or with narrow teeth or projections to be blanked may be unsuited to fine-edge blanking. Hole diameter, slot width, or projection width should be at least 70% of metal thickness for reasonably efficient blanking, although features as small as 50% of stock thickness have been successfully formed.

These limitations have been exceeded. For instance, a 16 mm ($\frac{5}{8}$ in.) diam hole was pierced in each end of a 1018 steel link 25 mm (1 in.) wide and 7.9 mm ($\frac{5}{16}$ in.) thick. Because the part had a 13 mm ($\frac{1}{2}$ in.) radius on each end, the wall thickness was 3.92 mm ($\frac{3}{16}$ in.). The part was offset 2.54 mm (0.100 in.) in the same die. Holes 3.18 mm (0.125 in.) in diameter were pierced in a part made of 4 mm (0.156 in.) thick aluminum alloy 5052-H34, leaving a wall thickness of 1.0 mm (0.040 in.). A 1.6 mm (0.062 in.) diam hole was pierced in the same part.

The sheared faces of holes pierced during fine-edge blanking are usually vertical, smooth, and free from die break, provided the maximum hole dimensions are not more than a few times the stock thickness. As in conventional piercing, there is a slight radius around the punch side of the hole, but there are no torn edges on the die side of the blank. A rough-sheared surface on the blank may be caused by too great a punch-to-die clearance, or improper location and height of the impingement ring for the material being blanked. On parts blanked to a small width-to-thickness ratio, a small, rough surface may be noticeable, but may not be detrimental.

Presses

Triple-action hydraulic presses or combination hydraulic and mechanical presses are used for fine-edge blanking. The action is similar to that of a double-action press working against a die cushion. An outer slide holds the stock firmly against the die ring and forces the impingement ring into the metal surrounding the outline of the part. The stock is stripped from the punch during the upstroke of the inner and outer slides. An inner slide carries the blanking punch. A lower slide furnishes the counter-action to hold the blank flat and securely against the punch. This slide also ejects the blank.

The stripping and ejection actions are delayed until after the die has opened at least to twice the stock thickness to prevent the blank from being forced into the strip or slugs from being forced into the blank. Because loads are high and clearance between punch and die is extremely small, the clearance between the gibs and press slides must be so close that they are separated only by an oil film.

Force requirements for fine-edge blanking presses are influenced not only by the work metal and the part dimensions, but also by the special design of the dies and pressure pads used for fine-edge blanking (Fig. 3). Depending on part size and shape, an 890 kN (100 tonf) press can blank stock up to 8 mm (0.315 in.) thick; a 2.2 MN (250 tonf) press can blank stock up to 11.94 mm (0.470 in.) thick; and a 3.6 MN (400 tonf) press can blank stock up to 12.7 mm (0.500 in.) thick.

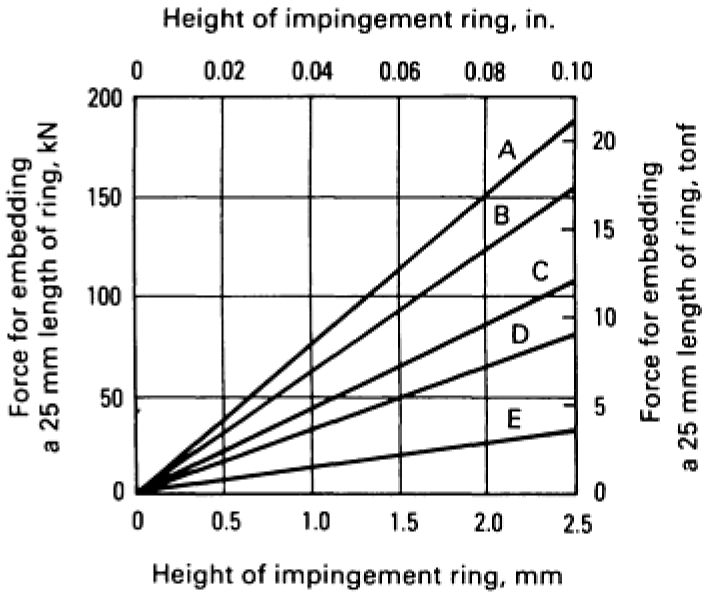

Fig. 3 Force required for embedding impingement rings of various heights into several different work metals. A, stainless steel; B, prehardened alloy steel; C, mild steel, half-hard brass, hard copper, 6*xxx* aluminum alloys in the H temper; D, soft copper; series 6000 aluminum alloys, half hard; E, commercially pure aluminum, H temper

When impingement rings are used on both the pressure pad and the die, the calculation of force is still based only on the pressure pad impingement ring. The reduced height of impingement rings when used in pairs allows the use of a lower clamping force, thereby reducing the overall load on the press. This is because the lower impingement ring is pressed into the workpiece by the reaction force. If coining, embossing, or other forming is done during the blanking, the additional force required for those operations must be added to the force requirements.

Press Safety. Blanking and piercing are potentially dangerous operations. Information and references on the safe operation of presses for sheet metal forming is available in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume.

Fine-Edge Blanking and Piercing

Tools

The design of tools for fine-edge blanking is based on the shape of the part, the method of making the die, the required load, and the extremely small punch-to-die clearance. The considerable loading and required accuracy dictate that the press tools be sturdy and well supported to prevent deflection. The small clearance presupposes precise alignment of the punch and die.

Design. A basic tool consists of three functional components, the die, the punch, and back-pressure components. To produce high-quality blanks, the punch-to-die clearance must be uniform along the entire profile and must be suitable for the thickness and strength of the work metal. Clearance varies between 0.005 and 0.01 mm (0.0002 and 0.0004 in.).

The components of a typical tooling setup for fine-edge blanking of a part of simple shape are shown in Fig. 1. The profile part of the blanking punch is guided by the pressure pad. A round punch is prevented from rotating by a key fastened to the upper die shoe. The hardened pressure pad is centered by a slightly conical seat in the upper die shoe; this pad contains the V-shaped impingement ring.

Some diemakers put a small radius on the cutting edge of the die. This causes a slight bell-mouth condition, which produces a burnishing action as the blank is pushed into the die, improving the edge finish.

If holes are to be pierced in the part, the blanking punch will contain the piercing die. The slug is ejected by ejector pins or through holes in the punch.

The die is centered in the lower die shoe by a slightly conical seat, as is the upper pressure pad. Both the die and the upper pressure pad are preloaded to minimize movement caused by compression. The pressure and ejector pad is guided by the die profile, and is supported by pressure pins and the lower slide. The backup block for the piercing punch also guides the pressure pins.

The die components are mounted in a precision die set with precision guide pins and bushings. Some designers prefer pressing the guide pins into the upper shoe.

Materials and Life. Because of the high loads, close tolerances, and small clearances involved in fine-edge blanking, the die elements are made of high-carbon high-chromium tool steels, such as AISI D2 or D3, or of A2 tool steel heat treated to about 62 HRC.

Punch and die life vary with tool material and hardness, punch-to-die clearance, type of work metal, and workpiece dimensional and surface finish requirements.

For most work metals under the usual operating conditions, punch life for fine-edge blanking of 3.2 mm ($\frac{1}{8}$ in.) thick stock is 10,000 to 15,000 blanks between regrinds--assuming that the blanks are of simple shape and that punch wear is such that only 0.05 to 0.13 mm (0.002 to 0.005 in.) of metal must be removed to restore the punch to its original condition.

The effect of work material on punch and die life can be illustrated by the following data. In one application, after blanking 33,000 pieces made of 1010 cold rolled steel, 0.23 mm (0.009 in.) was ground from the punch and 0.15 mm (0.006 in.) was ground from the die. Production rate was 35 pieces per minute. When blanking 8617 and 8620 steel, it was necessary to grind 0.23 mm (0.009 in.) from the punch after 12,000 pieces and 0.18 mm (0.007 in.) from the die after 23,000 pieces. The production rate was 27 to 30 pieces per minute.

In another instance, 15,000 to 30,000 pieces per punch grind were produced when blanking annealed 1040 and 1050 steel; 25,000 to 50,000 pieces were produced for 1010 steel. Punch life for blanking fine-tooth gears made of annealed high-carbon steel was 10,000 to 15,000 pieces, and for steel with a hardness of 32 to 34 HRC was 5000 to 15,000 pieces. The reason for grinding the punch was to remove the small radius on the edge of the punch, which must be kept sharp and flat to obtain a good edge on the part.

Total die life may be 200,000 to 300,000 blanks per tool. The die is usually sharpened once for each two or three punch sharpenings. It may be necessary to remove from the die an amount of metal up to half the work metal thickness to restore the die to its original condition.

In some production applications of blanking simple shapes from 2.5 mm (0.100 in.) thick 1010 steel, life between regrinds was about 40,000 blanks for punches and about 80,000 blanks for dies, when punch and die wear of 0.13 to 0.18 mm (0.005 to 0.007 in.) was allowable and the surface finish of the cut edge was 1.6 μm (63 μin.) or better.

Lubrication

The work metal must have a film of oil on both sides to lubricate the punch and die during fine-edge blanking. The lack of a lubricant on either side can reduce punch or die life between sharpenings by as much as 50%. Oils used for conventional blanking are usually satisfactory. In severe applications, a wax lubricant may be used. More information on lubricants is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Applications

Initially, most applications of fine-edge blanking were in the production of components for instruments, watches, and office machines. These included levers, gears, fingers, tooth segments, and similar parts.

Fine-edge blanking is now being applied to a wider variety of materials, as well as to thicker stock, in the automotive, farm equipment, ordnance, machine tool, printing machine, household appliance, and textile machine industries. Gears, racks, sprockets, and other toothed forms are easily produced and are common applications.

Introduction

ELECTRICAL STEELS are used for various static and rotating electrical devices. They are magnetically soft materials; that is, they are not permanent magnets but have properties that make them useful in electrical applications. Most of the parts produced from electrical steels must be laminated. A lamination consists of flat blanked sheets of a particular shape that are stacked to a given height and fastened together by riveting, bolting, or welding. Electrical steel sheet is available in coils or cut-to-length. For most applications, stock thickness ranges from 29 to 24 gage (0.343 to 0.607 mm, or 0.0135 to 0.0239 in.).

The properties and selection of electrical steel sheet are discussed in detail in the article "Magnetically Soft Materials" in *Properties and Selection: Nonferrous Alloys and Special-Purpose Materials*, Volume 2 of the *ASM Handbook.* Information on the metallography and microstructure of electrical steels and other types of magnetically soft materials is available in the article "Magnetic and Electrical Materials" in *Metallography and Microstructures*, Volume 9 of the *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

Materials

The general category of magnetically soft materials encompasses many types of materials, including iron-nickel, iron-cobalt, and iron-aluminum alloys; ferrites; and austenitic stainless steels. The discussion in this article, however, will be limited to the most commonly used magnetically soft materials: low-carbon electrical steels and oriented and nonoriented silicon electrical steels. Table 1 lists some of the characteristics and applications of these materials.

Table 1 Silicon contents, densities, and some applications of electrical steel sheet

AISI type	**Nominal Si + Al content, %**	**Assumed density, Mg/m^3**	Characteristics and applications
Low-carbon steel			
. . .	0	7.85	**High magnetic saturation; magnetic properties may not be guaranteed; intermittent-duty small motors**
Nonoriented silicon steels			

M47	1.05	7.80	**Ductile, good stamping properties, good permeability at high inductions; small motors, ballasts, relays**
M45	1.85	7.75	**Good stamping properties, good permeability at moderate and high inductions, good core loss; small generators, high-efficiency continuous-duty rotating machines, ac and dc**
M43	2.35	7.70	
M36	2.65	7.70	**Good permeability at low and moderate inductions, low core loss; high reactance cores, generators, stators of high-efficiency rotating machines**
M27	2.80	7.70	
M22	3.20	7.65	**Excellent permeability at low inductions, lowest core loss; small power transformers, high-efficiency rotating machines**
M19	3.30	7.65	
M15	3.50	7.65	
Oriented silicon steels			
M6	3.15	7.65	**Highly directional magnetic properties with lowest core loss and highest permeability when flux path is parallel to rolling direction; heavier thicknesses used in power transformers, thinner thicknesses generally used in distribution transformers. Energy savings improve with lower core loss.**
M5	3.15	7.65	
M4	3.15	7.65	
M3	3.15	7.65	
High-permeability oriented steel			
. . .	**2.9-3.15**	**7.65**	**Low core loss at high operating inductions**

Low-Carbon Steels. For many applications that require less than superior magnetic properties, low-carbon steels (AISI 1010, for example) are used. Higher-than-normal phosphorus and manganese contents are often used to increase electrical resistivity. Such steels are not purchased to magnetic specifications. Although low-carbon steels exhibit power losses higher than those of silicon steels, they have better permeability at high flux density. This combination of magnetic properties, coupled with low price and excellent formability, makes low-carbon steels especially suitable for applications such as fractional-horsepower motors, which are used intermittently.

Nonoriented Silicon Steels. Except for saturation induction, the magnetic properties of iron containing a small amount of silicon are better than those of pure iron. Few commercial steels contain more than 3.5% Si because the steel becomes brittle and difficult to cold roll at silicon levels above 4%.

The commercial grades of silicon steel in common use (0.5 to 3.5% Si) are made primarily in electric or basic-oxygen furnaces. Nonoriented grades are melted with careful control of impurities; better grades have sulfur contents of about

0.01% or less. Continuous casting and vacuum degassing can be used. After hot rolling, the hot bands are annealed, pickled, and cold rolled to final thickness as continuous coils.

Semiprocessed grades of strip are not sufficiently decarburized for electrical use; therefore, decarburization and annealing to develop potential magnetic quality must be done by the user. This procedure is practical for small laminations accessible to the annealing atmosphere. Fully processed grades are strand annealed in moist hydrogen at about 825 °C (1520 °F) to remove carbon. The final annealing operation is very important and is carried out at a higher temperature (up to 1100 °C, or 2000 °F, for continuous strip) to cause grain growth and the development of magnetic properties. Use of a protective atmosphere is vital. The steel often is coated with organic or inorganic materials after annealing to reduce eddy currents in lamination stacks.

Most finished nonoriented silicon steel is sold in full-width coils (860 to 1220 mm, or 34 to 48 in.) or slit-width coils, but some is sold as sheared sheets. All coils are sampled and tested according to ASTM A 343 and graded as to quality.

Oriented Silicon Steels. Grain size is as important in silicon steel as in iron with regard to core losses and low-flux-density permeability. For high-flux-density permeability, however, crystallographic orientation is the deciding factor. Like iron, silicon steels are more easily magnetized in the direction of the cube edge: <100>. For special compositions, rolling and heat-treating techniques are used to promote secondary recrystallization in the final anneal at about 1175 °C (2150 °F) or higher, which results in a well-developed texture with the cube edge parallel to the rolling direction {110} <001>. Conventional oriented grades contain about 3.15% Si.

About 1970, improved {110} <001> texture was developed in silicon steel through the modification of composition and processing. The high-permeability material usually contains about 2.9 to 3.2% Si. Conventional oriented 3.15% Si steel has grains about 3 mm (0.12 in.) in diameter. The high-permeability silicon steel tends to have grains about 8 mm (0.31 in.) in diameter. Ideally, grain diameter should be less than 3 mm (0.12 in.) to minimize excess eddy-current effects from domain-wall motion. Special coatings provide electrical insulation and induced tensile stresses in the steel substrate. These induced stresses lower core loss and minimize noise in transformers.

Size and Shape. Flat laminations of a wide variety of shapes and sizes are blanked and pierced from electrical sheet. However, most are shaped like those shown in Fig. 1. Laminations similar to those shown in Fig. 1(a) can range in diameter from less than 25 mm to 1.3 m (1 to 50 in.) or more, and laminations similar to those shown in Fig. 1(b) can range in length from less than 25 to 305 mm (1 to 12 in.) or more.

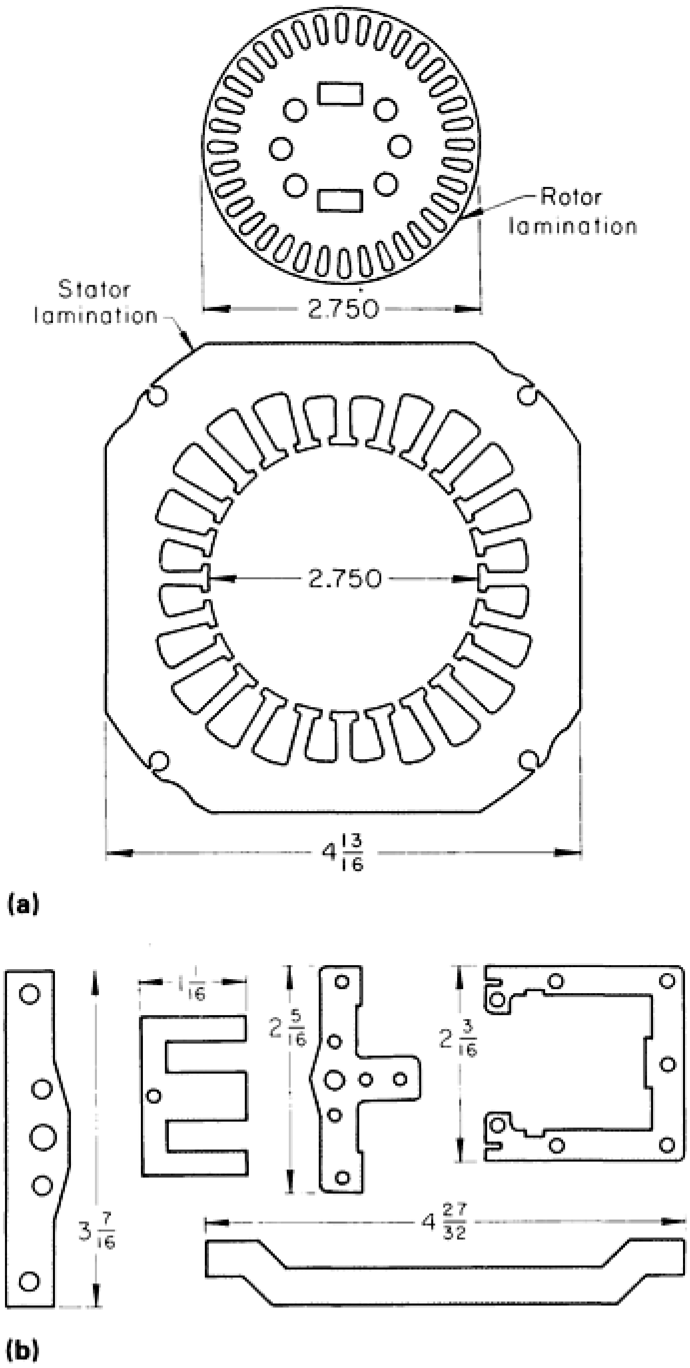

Fig. 1 Typical laminations blanked and pierced from electrical sheet. (a) Laminations for rotating electrical machinery are blanked and pierced in single-station dies (Fig. 3) or progressive dies (Fig. 4). Slots can also be made in precut blanks, one at a time, with notching dies. (b) Typical laminations blanked and pierced from electrical sheet for application in units other than rotating machines. Dimensions given in inches.

Punchability. Materials used for electrical sheet can be classified in the following order with respect to decreasing ease of blanking, piercing, and notching:

- Conventional flat-rolled low-carbon steels such as 1008
- Nonoriented silicon steels
- Oriented silicon steels

To a large extent, applications also follow the above classification (Table 1). Each group has certain distinct characteristics that affect punchability. In addition, differences in composition and hardness within any specific group cause considerable variation in punchability (see the section "Effect of Work Metal Composition and Condition" in this article).

Presses

A general-purpose punch press in good mechanical condition is acceptable for stamping laminations, but large-volume production of laminations by progressive-die methods requires the use of high-productivity presses (see the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume). Most high-productivity presses have heavy bed and crown members to minimize deflection and vibration. Bed deflection for lamination presses should be no more than 0.006 mm/mm (0.006 in./in.) of bed length (measured left-to-right between uprights), with a load equal to the rated capacity of the machine distributed over two-thirds of the bed area between tie rod centers. Deflection of the slide should not exceed 0.006 mm/mm (0.006 in./in.) of the length between the pitman centers, with rated load evenly distributed between those centers. Bending deflection and shear deflection are both considered in these standards. Double-crank presses with two or four points of suspension are preferred for progressive-die applications because of their better resistance to off-center die loads. Parallelism of the bed and slide should be 0.012 mm/mm (0.012 in./in.) of bed dimensions, both left-to-right and front-to-back.

Presses designed for producing laminations have heavy connections, large diameters of the mainshaft and connection bearings, close gib clearances, and thick bolsters. Because of the close gib fits (needed for accurate vertical motion), recirculating oil systems must be used to provide forced-feed lubrication of bearings and slides.

The fact that a die was built with uniform punch-to-die clearance at all cutting edges does not necessarily mean that the clearance is uniform at the instant the punch begins to enter the work metal. The act of applying the load to the work metal can cause lateral deflections in the die and press, which can change the clearances. To minimize these undesirable deflections, the mechanical condition of the press and die must be maintained at a high level. The total force capacity exerted at each stroke must be in proper relation to the force capacity of the press and to the type of press frame (some types of press frames will deflect laterally more than others). Close-fitting gibs and bearings are essential in minimizing lateral deflection. The die should be built with large guideposts and close-fitting bushings.

A preventive maintenance program must be established to ensure that all presses are kept in top condition. Special attention should be given to bearing clearances, the condition of the counterbalance springs or cylinders, and the parallelism of the slide.

Auxiliary Equipment

When producing motor laminations in individual dies for each operation with upright or inclined presses, blanks can be loaded and unloaded manually. However, when individual dies are used for the simultaneous production of stator and rotor laminations, feeding and stacking equipment is necessary for optimal efficiency. The use of an inclined press is preferred because gravity assists in loading the die and removing the laminations. When progressive dies are used, automatic feeding and scrap-cutting equipment is required.

Stock reels, cradles, and straighteners are required when coil stock is used. Several types and sizes are available (see the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume).

Feed Mechanisms. In progressive-die operations, the common types of feed mechanisms, for example single-roll or double-roll, hitch, grip, and slide, are used to feed strip or coil. Cam feed, which has a fixed feed length, is widely used for large-volume production. This method is accurate at high speeds because it eliminates the slippage that usually occurs in the overriding clutch-and-brake mechanisms of roll feeds.

Magazine feeds have a mechanism that ejects the blank from the bottom of a stack into the die or onto a magnetic belt or a chain feed. In inclined presses, the blank may slide by gravity into the die nest after leaving the magazine.

Stacking. Figure 2 shows a method that can be used for stacking laminations when each operation is done in an individual die. Blanks are fed to the press (inclined 35 to 45° to the rear) from a magazine feeder. Laminations drop from the press into a chute, where they are picked up by a driven elevating belt that conveys them to the stacking chute, from which they fall onto a stacking mast. Stacking masts are usually 380 to 915 mm (15 to 36 in.) high. The tops of the masts are either threaded or have tapped holes so that they can be picked up by handling machines and moved to subsequent operations. The bases of the stacking masts are large or weighted to prevent the masts from tipping or falling when being loaded or moved to the assembly floor.

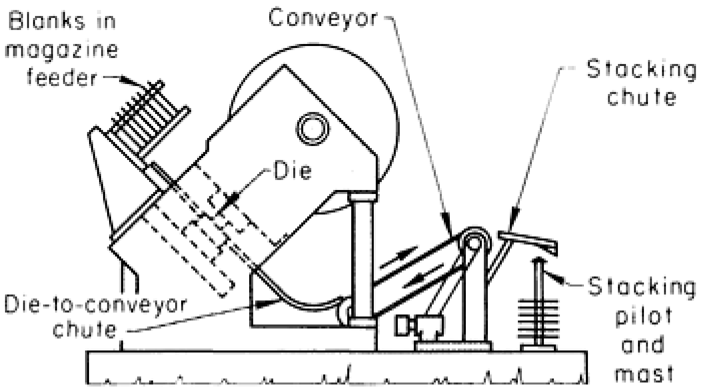

Fig. 2 Method for stacking laminations stamped in individual dies.

Scrap disposal is a major consideration in producing laminations. The removal of scrap from a trimming operation or the removal of slugs from piercing holes and slots requires consideration during die design. Scrap is discharged through holes in the die shoe onto chutes. The chutes convey the scrap into containers or into automatic scrap-conveying systems below the floor. When the die is in the upper shoe, a mechanically operated pan can be used to catch the slugs on the upstroke. The slugs are then ejected into a container on the downstroke.

Blanking and Piercing of Electrical Steel Sheet

Dies

Single-station and progressive dies are both used for making laminations.

Single-Station Dies. Each single-station die performs one operation, and a set of dies for a lamination can be mounted in one press or in different presses. Simple laminations such as those shown in Fig. 1(b) are usually produced in one operation. More complex parts may require several operations. Figure 3 shows a typical sequence for the production of stator and rotor laminations in four operations.

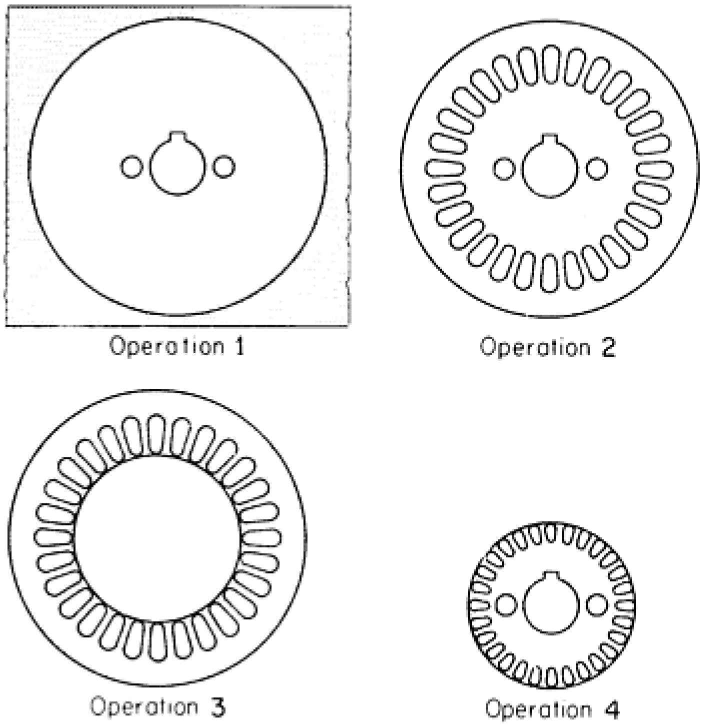

Fig. 3 Sequence of operations for producing stator and rotor laminations using single-station dies. Operation 1, stock blanked and pierced; operation 2, stator lamination notched; operation 3, rotor lamination separated from stator lamination; operation 4, rotor lamination notched. Compare with Fig. 4.

Single-station dies can be used for punching any lamination, regardless of the size, composition, shape, or quality requirements. However, because production with single-station dies is relatively slow, the cost per piece is high for mass production. Laminations such as those shown in Fig. 1 can be produced in large quantities at a lower cost in progressive dies.

The size of the workpiece and the quantity required influence the degree to which operations can be combined and the complexity of any one die. For example, in Fig. 3, the slots made in operations 2 and 4 can be punched in one stroke in a multiple die for each operation if the die sections are strong enough. An alternative method is to punch the slots in 28 strokes with a single-notch die in a high-speed notching press equipped with an indexing mechanism. Single-notch dies are used in the production of laminations for the following reasons:

- Tool costs are lower, and the single-notch die can be used on several different laminations. The cost is sometimes less than 5% of that for a multiple die that can pierce all the holes and slots in one press stroke
- Laminations more than about 380 mm (15 in.) in diameter are sometimes too large to be notched by any other method because of tool and equipment costs. However, many larger-diameter laminations are multiple pierced
- Limited production does not warrant the cost of a multiple die
- Available equipment must be used

Progressive dies perform a series of operations at two or more die stations during each stroke of the press. Each working, or active, station in the die performs one or more operations. The work material progresses through successive stations until a completed part is produced (Fig. 4). Idle stations, in which no work is performed, are added to provide strength to the die, to facilitate material travel through the die, to simplify construction, or to increase flexibility for die changes.

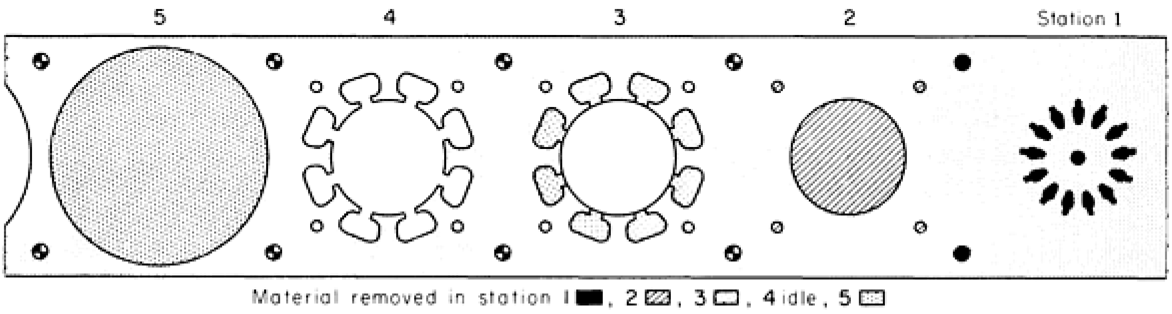

Fig. 4 Blanking and piercing sequence for rotor and stator laminations in a five-station progressive die. Two pilot punches were used at each station. Station 1, pierce pilot holes, rotor slots, and rotor-shaft hole; station 2, pierce stator rivet holes and blank rotor; station 3, pierce stator slots; station 4, idle; station 5, blank stator. Compare with Fig. 3.

One of the more common progressive dies used in the electric-motor industry is a five-station die that produces a rotor lamination and a stator lamination with each stroke of the press (Fig. 4). This die can be provided with carbide inserts for the punch and die sections. It has a spring-actuated guided stripper. The die components are mounted on a precision die set with ball bearing guide bushings and hardened guide pins. Slender punches are guided through the stripper by bushings. Such a die usually has four active stations and one idle station.

The progressive die described above is the blanking or scrap-all-around type. For the most efficient use of material, the cutting-off or parting methods of severing the blank from the strip are used where layout permits.

The principal advantages of progressive dies for blanking laminations are:

- Handling between operations is eliminated; therefore, cost per piece is lower
- Laminations from progressive dies are generally stacked in chutes that allow the press to be operated at uninterrupted maximum capacity. Stacking chutes fastened to the bottom of the bolster or die shoe keep the laminations oriented in a smooth, uninterrupted flow from the die. Therefore, laminations are better controlled with regard to burr direction and are easier to handle for assembly

Two disadvantages of progressive dies for blanking laminations are:

- A progressive die is, to a great extent, a single-purpose die. Even a minor change in part design can necessitate an expensive die alteration or can make the die obsolete
- Progressive dies are more susceptible to damage from accidents than single-station dies. Progressive dies run at high speed and may make many strokes before the press can be stopped. Misfeed detectors built into the die can help prevent damage. Die damage can halt production in progressive operations. In single-station operations, if the inventory of processed material is sufficient to keep other dies running, a breakdown of a die does not interrupt production

The minimum size of lamination that can be made depends on the slot size and spacing, work metal thickness, and tolerances on the slot dimensions. The die must be strong enough to withstand the blanking pressure. Figure 5 shows some very small laminations that were made in progressive dies. The 6.48 mm (0.255 in.) diam rotor was made in three stations in order to have the necessary die strength. Part tolerances were such that piercing in three stations produced acceptable parts.

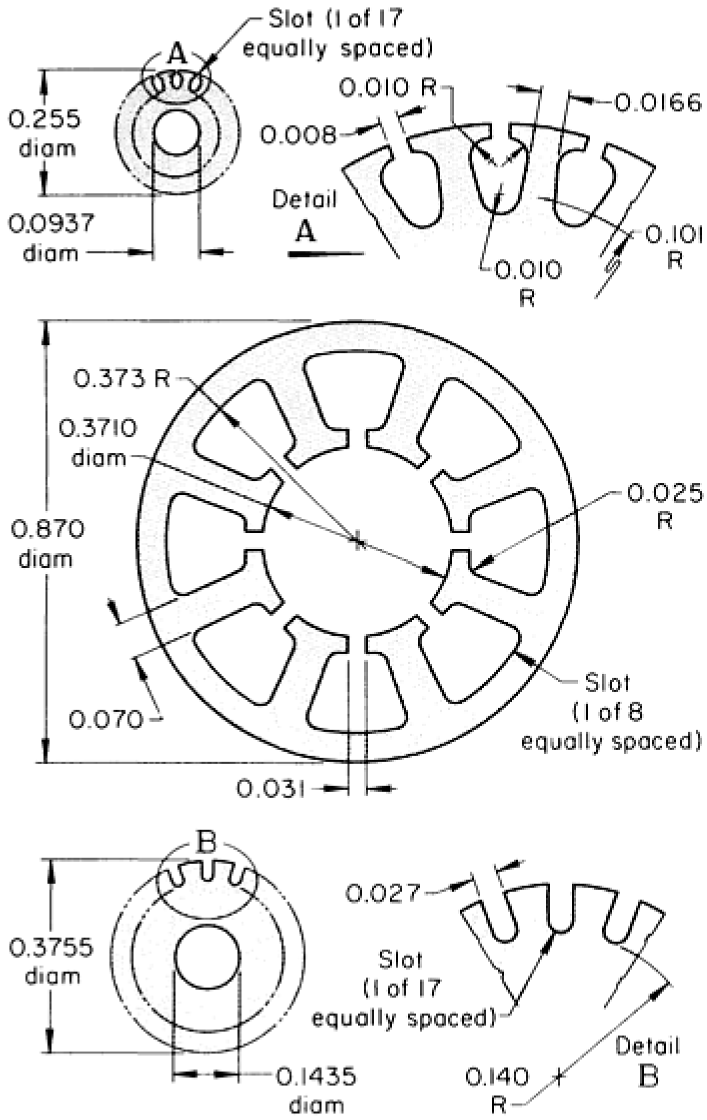

Fig. 5 Small-diameter laminations produced in progressive dies. Dimensions given in inches.

The dies for producing laminations are usually of segmented construction, which provides maximum accuracy. However, electrical discharge or electrochemical machining methods have been used to produce satisfactory dies.

There is no agreement as to the maximum size of lamination that can be efficiently produced in progressive dies. However, progressive dies are seldom used for laminations larger than 380 mm (15 in.) in major dimension. The factors that limit the maximum practical size are as follows:

- Progressive dies for making laminations more than 380 mm (15 in.) in major dimension represent a large investment (and a significant loss if damaged)
- Quantity demands are usually lower for large laminations; therefore, the investment is not warranted
- Extremely large dies may require a press capacity that is so large as to be impractical
- Problems from camber and lack of flatness in the stock are magnified in stamping large laminations in progressive dies

Blanking and Piercing of Electrical Steel Sheet

Selection of Die Materials

Almost any hardened tool steel is satisfactory as die material for making a small quantity of laminations. However, for production blanking and piercing, either a high-carbon high-chromium cold-work tool steel, such as AISI D2, or carbide must be used to resist the abrasiveness of electrical steels.

The shape or size of the lamination seldom affects the choice of die material. Dies ranging from the smallest to the largest and from the simplest to the most complex have been made from both high-carbon high-chromium tool steel and carbide. In addition, both die materials have been used to blank and pierce all compositions and thicknesses of electrical sheet. The composition and thickness of the stock rarely affect the choice between carbide and tool steel.

Production Quantity. If the dies are of the same design and construction, the total quantity of parts to be produced is the major factor in choosing die material. If the run is so short that it can be made with tool steel dies without sharpening, tool steel is more economical. However, for longer runs, carbide dies have 10 to 20 times as much life per grind as tool steel dies.

Uniform quality of cut edges and minimum burr height will be retained over a much longer run by carbide dies than by tool steel dies. In some cases, the edge condition of the lamination is not critical. However, when automatic stacking and core assembly equipment is used after blanking and piercing, burr height is important. Excessive burr height can cause short circuiting of the laminations in the core.

Cost. Depending on size and design, a die with carbide cutting edges will cost at least $1\frac{1}{2}$ times as much as a steel die. However, in terms of cost per piece, carbide dies may be more economical. Press downtime and die maintenance affect cost per piece; carbide dies can run about ten times as long per sharpening as tool steel dies.

Press condition is important in the operation of blanking and piercing dies. To achieve the maximum potential usage of carbide dies, press condition must be maintained at a high level. Although tool steel punches and dies can chip and shear because of misalignment, carbide punches and dies are more likely to break. Therefore, the presses used for tool steel dies can be in less than top level condition and continue to produce quality laminations.

Effect of Stock Thickness

Electrical sheet that is to be blanked and pierced usually ranges in thickness from 29 to 24 gage (0.343 to 0.607 mm, or 0.0135 to 0.0239 in.). Thinner or thicker stock is used for special applications. The blanking and piercing of extremely thin electrical sheet requires close control of equipment and technique. The processing of thick sheet (>1.27 mm, or 0.050 in.) can also cause difficulty, although the force-capacity rating of the press is the major factor that determines the maximum thickness of sheet that can be blanked and pierced.

Punch-to-die clearance for electrical sheet generally ranges from 3 to 7.5% of stock thickness per side, with clearances as large as 20% reported for grain-oriented stock. These values are similar to those used for low-carbon steel, but the stock thicknesses are thinner than those commonly used for the low-carbon steels. This results in close die clearance and requires good diemaking practice and accurate press equipment.

Thin Sheet ($\leq$0.254 mm, or 0.010 in.). Under carefully controlled conditions, laminations can be blanked and pierced from sheet as thin as 0.051 mm (0.002 in.), but the press must be in top condition. Further, the feeding mechanism must be capable of feeding within ±0.076 mm (±0.003 in.) total error per stroke at a feed rate of 23 m/min (900 ft/min).

Punches and dies of hardened tool steel, such as D2, or carbide are satisfactory, although carbide dies and punches will have at least ten times the life of their tool steel counterparts. The punches must be rigidly supported and guided. The entire tool must be made rugged and accurate enough to maintain alignment. To avoid shearing the punch and die during press setup, it is important that the die be handled carefully to prevent the possibility of some of the components moving out of alignment. The press bed, the bottom of the die shoe, the face of the press slide, and the top of the punch holder must be clean and free of any irregularities that would cause a deviation from parallelism.

The punch and the die as a unit should be aligned square with the centerline of the press. The press slide should then be brought down slowly to meet with the top of the punch holder, and the punch holder should be fastened to the face of the slide. The slide should then be adjusted downward so that the punches enter the die cavities. Finally, the die shoe should be fastened to the bolster or press bed.

Dies with this close clearance are often designed as a unit, and the die is not fastened to the press ram. Therefore, the tool is not subject to the inaccuracies of the press.

A back taper of 0.002 mm/mm (0.002 in./in.) per side is commonly used in the die. Because of this angular clearance in the die, total die life is limited by the maximum punch-to-die clearance that can be tolerated. Each time a die having a back taper of 0.002 mm/mm (0.002 in./in.) per side is sharpened, the hole diameter will increase 0.1 μm (4 μin.) for each 0.0254 mm (0.001 in.) ground from the top of the die. After grinding 2.54 mm (0.100 in.) from the die, the punch-to-die clearance will increase 0.005 mm (0.0002 in.) per side.

If this amount of clearance is too great, the original clearance can be restored by installing new die sections or, if the dimensional tolerance permits, by using an oversize punch. The amount that can be removed from the die depends on the amount of back taper in the die, the stock thickness, and the maximum punch-to-die clearance permissible. Dies sometimes have a straight land that is 1.6 to 3.2 mm ($\frac{1}{16}$ to $\frac{1}{8}$ in.) wide before beginning the back taper.

Lubricant is applied to the stock during blanking and piercing to keep wear on the cutting edges of the punch and die at an acceptable level. Use of a water-thin lubricant with rapid evaporation and low residue makes it unnecessary to perform a burn-off operation before hydrogen annealing. Difficulties in producing acceptable laminations from thin sheet are magnified as the plan area of the lamination increases.

Effect of Work Metal Composition and Condition on Blanking and Piercing

Each of the three most widely used electrical sheet materials (low-carbon steels and nonoriented and grain-oriented silicon steels) has distinctive punching characteristics. These characteristics often necessitate specific procedures to produce laminations of the desired quality at the lowest cost.

Low-carbon steels such as 1008 are used as electrical sheet when their electrical properties can meet requirements, primarily because they cost less than silicon steels and the cost is lower for blanking and piercing. More pieces per die sharpening are usually obtained in blanking these steels than in blanking silicon steels. One study of die wear in making stator and rotor laminations similar to those shown in Fig. 1 and ranging in diameter from 92 to 149 mm ($3\frac{5}{8}$ to $5\frac{7}{8}$ in.) showed that, with tool steel cutting edges, 120,000 to 150,000 pairs were punched per sharpening when stamping 1008.

The condition of the low-carbon steel stock influences power requirements and punching characteristics. When annealed, this steel has a tensile strength of 380 to 414 MPa (55 to 60 ksi), but the strength of full-hard material may be over 690 MPa (100 ksi). Therefore, the material condition must be known before the force-capacity requirements of the presses can be determined. Low-carbon low-silicon steels in the annealed condition are soft, and they are likely to roll at the edges and form excessive burrs. Therefore, punch-to-die clearances must be as close for these steels as for electrical sheet of the same thickness. An annealed product is usually specified, but whether annealed stock is stamped or individual laminations are annealed after stamping is often a matter of convenience, because of press capacity, annealing facilities, or other factors.

Nonoriented silicon steels are available with silicon contents ranging from 0.5 to 3.25%. As silicon content increases, the sheet becomes more brittle and more abrasive. As a result, the edges of higher-silicon steel are less likely to roll and make excessive burrs, but die wear is increased because of abrasion.

Many nonoriented silicon steels are coated with an organic or inorganic material (core plating) to insulate one lamination from another. This organic core plating also improves the punchability of electrical sheet. In one application, carbide dies produced about 3.5 million laminations from core-plated M-36 (2.5% Si) between resharpenings. When similar laminations were produced from uncoated M-36, dies required sharpening after each 1.2 million parts. Heating of the coated blanks by welding or die casting may destroy the organic coating. Additional information is available in the section "Core Plating" in this article.

General practice is to use approximately the same punch-to-die clearances for all silicon steels. The tensile strength of the particular steel must be considered in determining press capacity because silicon steels may vary in strength, depending on whether they are fully annealed at the time of stamping.

Oriented silicon steels are relatively high in silicon (3.15% Si + Al) and have most of their grains (crystals) oriented with the cube edges parallel with the rolling direction and face diagonals at 90° to the rolling direction ({110} <001>). Because of this orientation, these steels have blanking and piercing characteristics that are different from those of nonoriented steels. Tensile strength will vary as much as 20% between the rolling direction and the transverse direction (strength is greater parallel with the rolling direction).

A magnesium hydroxide coating is applied to grain-oriented steel after normalizing. This coating prevents the coiled strip from welding together during annealing. Magnesium hydroxide, in contrast to the organic coatings, is highly abrasive and greatly increases die wear; therefore, it is not recommended for stamped laminations. Tool steel dies wear so rapidly under these conditions, because of the high silicon content of the steel, that carbide cutting edges are almost always used for the blanking and piercing of grain-oriented steel.

Because mechanical properties vary with direction, cutting properties also vary in grain-oriented steels. Cutting across the rolling direction results in a clean break, but the edges are smeared when cutting is parallel with the rolling direction. Therefore, punch-to-die clearance is more critical on the sides parallel with the rolling direction.

Camber and Flatness

Camber in electrical sheet is the deviation (parallel to the stock surface) of a side edge from a straight line that extends to both ends of the side, and it is customarily limited to 6.4 mm ($\frac{1}{4}$ in.) for any 2.5 m (96 in.) length or fraction thereof. Flatness, or the degree to which a surface of a flat product approaches a plane, is expressed in terms of the deviation from a plane. Flatness tolerances have not been established for electrical sheet; the operations employed to flatten other steel products cannot be used because of their effect on magnetic quality. Flatness requirements should be specified for a particular application.

Camber and flatness are interrelated; the edge of a 2.5 m (8 ft) section of sheet may come within the 6.4 mm ($\frac{1}{4}$ in.) tolerance while lying freely on a flat surface. However, the seemingly flat sheet may have a number of faint waves (sometimes called oil cans). If this sheet is then flattened (as it is in dies), the flattening of these waves causes multidirectional elongation of the sheet, and the edge of the sheet may then be forced into a camber different from that when the sheet is not under flattening pressure. Minimum camber and maximum flatness are desirable for blanking electrical sheet and are especially important in progressive-die operations.

Effect on Progressive-Die Operation. If there is no camber, it is easy to start the stock through the die correctly by aligning the straight edge of the sheet against a straight-edge starting guide, with the end of the material covering the first die stage. Feed rolls are then engaged, and the blanking and piercing can begin.

Even though the edge of the sheet is cambered, it must still be used in the starting alignment. Therefore, the material may be misaligned to some degree as it enters the die. A small degree of misalignment is not readily apparent to the press operator.

Minor misalignment in starting the material through a progressive die may not cause immediate problems. Operating difficulties result from various misalignments, which have a cumulative effect.

In the first stage of the die, pilot holes are pierced into the sheet, often into a portion that will later be scrap. At subsequent die stations, bullet-nose pilots engage the pilot holes as the die closes. The piloting action may cause the sheet to move slightly into true position before the cutting edges of the die meet the sheet. Powered feed rolls move the sheet between press strokes to an approximate position for the next die station. The feed rolls then open, releasing the sheet so that there is no conflict between the locating action of the pilots and the feed rolls.

When the original lineup is not correct or when there are cumulative effects of camber against the stock guides, the sheet may wander from side to side on the die face. Stock guides are provided in progressive dies to limit wandering due to camber and misalignment.

Interference between the pilots and the stock guides may cause the stock to distort and jam in the die, preventing proper flow of the stock. If the stock jams, the press must be stopped at once.

Camber can cause other difficulties. For example, a change in camber or multidirectional elongation of the sheet as the press flattens the waviness may cause the pilots to distort the piloting holes; thus, misaligned rotor and stator laminations are made. This leads to misalignment of the slots in the stacked core. As slots are blanked out, stresses are released that also can change the amount of camber and flatness.

There is no single solution to the problem of camber in the production of laminations in progressive dies, because no two shipments of material are exactly alike. Some manufacturers of laminations use less efficient single-station dies because of difficulties with camber, even though production volume could justify the use of progressive dies. Other manufacturers use progressive dies only for laminations below a certain size.

Burr Height

It is impossible to blank and pierce laminations without producing some burr along the cut edges (Fig. 6). The amount of burr (measured as burr height) depends on the composition and condition of the electrical sheet, the thickness of the sheet, the clearance between punch and die, and the edge condition (sharpness) of the punch and die.

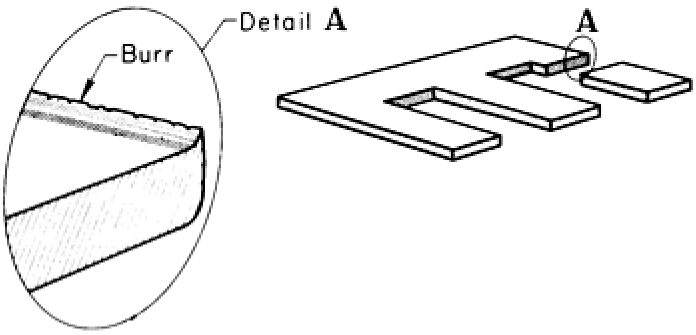

Fig. 6 Burr (exaggerated) produced along the edges of a blanked lamination.

The amount of burr that can be tolerated depends on end use. Burr height influences the stacking factor, which in turn influences magnetic characteristics. Maximum burr height is usually limited to 0.05 to 0.13 mm (0.002 and 0.005 in.).

Length of Die Run. A die is usually run until the maximum allowable burr height is reached, at which time the punch and die are removed for sharpening. Close control is required with this method of determining the length of the die run.

Optimal Die Run. For the greatest economy and convenience of operation, die maintenance requirements and die life (in addition to maximum burr height) should be considered in determining the optimal die run. A die should not be run too long before resharpening; otherwise, excessive stock must be removed from both the punch face and the die face to restore the cutting edges. As a result, fewer laminations can be made during the life of a die.

A common method of determining the optimal die run is to establish an arbitrary number of pieces to be run before die sharpening. At the end of this run, the punch face is sharpened. By knowing the number of pieces run and the amount removed from the die during sharpening, the number of pieces produced per unit of length removed from the die can be established. Using this procedure, the number of pieces run between sharpenings can be varied, an optimal die life between sharpenings can be determined.

Lubrication

Although uncoated electrical sheet is sometimes blanked and pierced without lubrication, the use of some type of lubricant is preferred. Organic core plate (used on nonoriented silicon steels) serves as a lubricant, and no further lubrication is needed when blanking and piercing sheet that has core plating.

Tool life will be greatly improved by using a lubricant in the blanking and piercing of electrical sheet that has no coating or has been coated with magnesium hydroxide, which acts as an abrasive rather than a lubricant. Oil-type lubricants, such as those used in blanking and forming operations, are not ordinarily used for blanking and piercing electrical sheet, because removal is too expensive.

Some plants purchase nonoriented silicon steel sheet without core plate and then subject the punched laminations to an oxidizing anneal, in which the lubricant is burned off; the only requirement is to select a lubricant that will leave the least residue when it is burned off. Water-soluble oils (1 part oil to about 20 parts water) have been used when annealing follows punching. Other low-viscosity low-residue oils, such as the aliphatic petroleums, also burn off with little residue.

Liquid lubricants can be applied in several ways. In low-production operations, the work metal can be dipped into the lubricant just before punching. In high-production operations, in which the stock is continuously fed, the lubricant can be brushed on just before it enters the press, or it can be dripped onto the sheet a few feet from the press. The top of the sheet is then rubbed with a felt wiper that spreads the lubricant over the entire surface. The bottom of the sheet can be coated with lubricant by having a trough under the sheet that catches the excess drip. A piece of felt or similar material in the trough acts as a wick to wet the underside of the moving sheet. A more complete discussion of lubricants is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Molybdenum disulfide is a good lubricant for the blanking and piercing of electrical sheet. The amount used is usually so small that no removal is required. When there is an excess, it can be removed by immersing the sheet for 4 to 5 min in a dilute solution of stripper-type cleaner at 80 °C (180 °F).

Molybdenum disulfide is the basic ingredient of several compounds that are available as dry powder, paste concentrate, and dispersion in liquid. A common method of applying dry molybdenum disulfide is shown in Fig. 7. The sheet passes through a box containing the dry powder, and felt wipers remove the excess. A slurry can be used instead of the dry powder. Sheets can also be coated by a spray timed with the stroke of the press; either powder or a liquid suspension can be sprayed. In low-production operations, a liquid suspension of molybdenum disulfide can be applied to the sheet by brush.

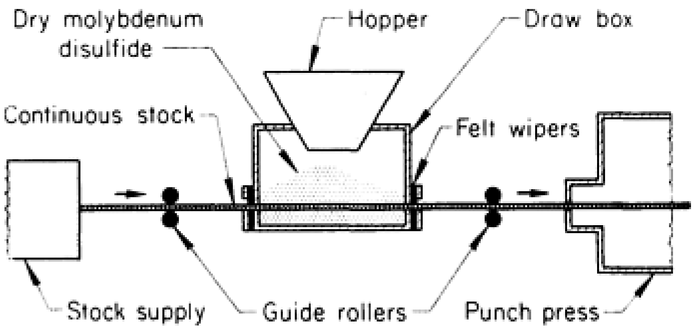

Fig. 7 Setup for applying dry molybdenum disulfide to both sides of electrical sheet.

Core Plating

Core plating, or insulation, is a surface coating or treatment applied to electrical steel sheet to reduce interlaminar loss and sometimes to increase punchability. This treatment does not reduce eddy currents within the laminations. Interlamination resistance is usually improved by annealing the laminations under slightly oxidizing conditions and then core plating. Core plating can be classified as organic or inorganic.

Organic insulation generally consists of enamels or varnishes applied to the steel surface. Steels having organic coatings cannot be stress relieved without impairing the insulating value of the coating, but the coating will withstand normal operating temperatures. Coatings are about 0.0025 mm (0.0001 in.) thick.

Inorganic insulation usually includes chemical or thermal treatments; it has a high degree of electrical insulation and can withstand stress relieving. Inorganic coatings form a very thin surface layer on the steel and increase lamination thickness only slightly.

Introduction

BLANKING AND PIERCING DIES, as discussed in this article, include the punches, dies, and related components used to blank, pierce, and shape metallic and nonmetallic sheet and plate in a stamping press. The primary measure of the performance of die materials in blanking and piercing service is the number of acceptable parts produced.

Sectional views of the blanking dies and the blanking and piercing punches used for making simple parts are shown in Fig. 1. Parts that are more complex require notching and compound dies.

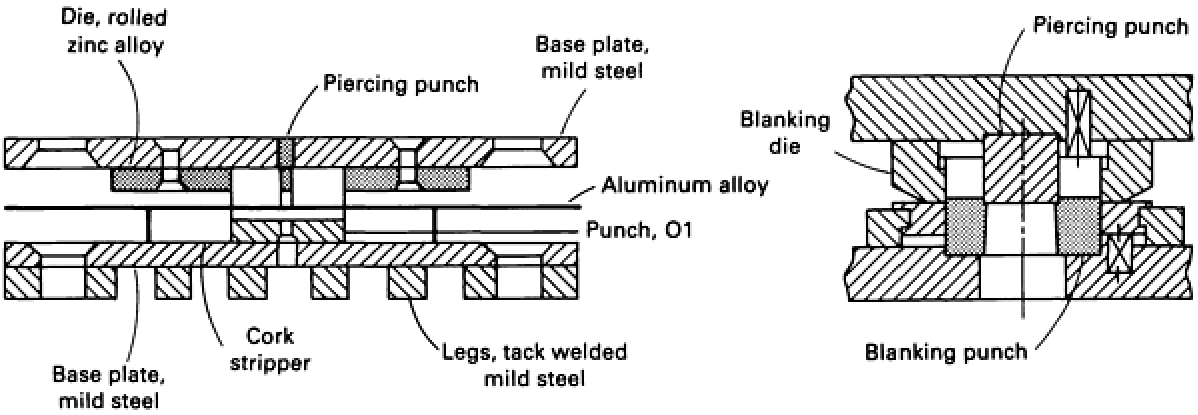

Fig. 1 Sectional views illustrating typical tools used for blanking and piercing simple shapes. Tooling at left is for short-run production of parts similar to parts 1 and 2 in Fig. 2 made from relatively thin-gage metal. Tooling at right is for longer production runs.

A common indication of tool deterioration is the production of a burr along the sheared edge of the workpiece. When tools are new, there is minimal clearance between punch and die, and the cutting edges are sharp. Under these conditions, the break in the stock begins at the underside (the side not in contact with the punch) because there the stock is subjected to the greatest tensile stress from stretching of the outer fiber. As more and more parts are produced, the cutting edges of the punch and the die become rounded by wear, and the stress distribution in the stock is changed. Stress on the underside is reduced, breaking at that point is delayed, and deformation accompanied by work hardening occurs. When breaking starts, it nucleates from both sides simultaneously, and a burr develops on both the slug and the surrounding area of the sheet from which it was cut. The height of this burr increases with tool wear. Acceptable burr height varies with the application, but is usually between 0.025 and 0.125 mm (0.001 and 0.005 in.).

Materials for Specific Tools

The compositions and properties of the tool materials referred to in this article are presented in *Properties and Selection: Irons, Steels, and High-Performance Alloys*, Volume 1 of the *ASM Handbook.*

Punches and Dies. Table 1 lists typical materials for punches and dies used for blanking parts of different sizes and degrees of severity from several different work materials about 1.3 mm (0.050 in.) thick in various quantities. Illustrations of typical parts are presented in Fig. 2. Typical materials for the punches and dies used to shave several work materials of this same thickness in various quantities are listed in Table 2.

Table 1 Typical punch and die materials for blanking 1.3 mm (0.050 in.) sheet

See Fig. 2 for illustrations of typical parts.

Work material	Tool material for production quantity of:				
	1000	**10,000**	**100,000**	**1,000,000**	10,000,000
Part 1 and similar 75 mm (3 in.) parts					
Aluminum, copper, and magnesium alloys	Zn[a], O1, A2	O1, A2	O1, A2	D2, CPM 10V	**Carbide**
Carbon and alloy steel, up to 0.70% C, and ferritic stainless steel	O1, A2	O1, A2	O1, A2	D2, CPM 10V	**Carbide**
Stainless steel, austenitic, all tempers	O1, A2	O1, A2	A2, D2	D4, CPM 10V	**Carbide**
Spring steel, hardened, 52 HRC max	A2	A2, D2	D2	D4, CPM 10V	**Carbide**
Electrical sheet, transformer grade, 0.64 mm (0.025 in.)	A2	A2, D2	A2, D2	D4, CPM 10V	**Carbide**
Paper, gaskets, and similar soft materials	W1[b]	W1[b]	W1[c], A2[d]	W1[d], A2[d]	**D2, CPM 10V**
Plastic sheet, not reinforced	O1	O1	O1, A2	D2, CPM 10V	**Carbide**
Plastic sheet, reinforced	O1[e], A2	A2[f]	A2[f]	D2[f], CPM 10V	**Carbide**
Part 2 and similar 305 mm (12 in.) parts					
Aluminum, copper, and magnesium alloys	Zn[a], 4140[g]	4140[h], A2	A2	A2, D2, CPM 10V	**Carbide**
Carbon and alloy steel, up to 0.70% C, and stainless steels up to quarter hard	4140[h], A2	4140[h], A2	A2	A2, D2, CPM 10V	**Carbide**
Stainless steel, austenitic, over quarter hard	A2	A2, D2	D2	D2, D4, CPM 10V	**Carbide**
Spring steel, hardened, 52 HRC max	A2	A2, D2	D2	D2, D4, CPM 10V	**Carbide**
Electrical sheet, transformer grade, 0.64 mm (0.025 in.)	A2	A2, D2	A2, D2	D2, D4, CPM 10V	**Carbide**

Paper, gaskets, and similar soft materials	4140(i)	4140(i)	A2	A2	**D2, CPM 10V**
Plastic sheet, not reinforced	4140(i)	4140(h), A2	A2	D2, CPM 10V	**Carbide**
Plastic sheet, reinforced	A2(e)	A2(e)	D2(e)	D2(e), CPM 10V	**Carbide**
Part 3 and similar 75 mm (3 in.) parts					
Aluminum, copper, and magnesium alloys	O1, A2	O1, A2	O1, A2	A2, D2, CPM 10V	**Carbide**
Carbon and alloy steel, up to 0.70% C, and ferritic stainless steel	O1, A2	O1, A2	O1, A2	A2, D2, CPM 10V	**Carbide**
Stainless steel, austenitic, all tempers	A2	A2, D2	A2, D2	D2, D4, CPM 10V	**Carbide**
Spring steel, hardened, 52 HRC max	A2	A2, D2	D2, D4	D2, D4, CPM 10V	**Carbide**
Electrical sheet, transformer grade, 0.64 mm (0.025 in.)	A2	A2, D2	D2, D4	D2, D4, CPM 10V	**Carbide**
Paper, gaskets, and other soft materials	W1(b)	W1(b)	W1(j), A2	W1(j), A2	**D2, CPM 10V**
Plastic sheet, not reinforced	O1	O1	A2	A2, D2, CPM 10V	**Carbide**
Plastic sheet, reinforced	O1(k)	A2(f)	A2(f)	D2(f), CPM 10V	**Carbide**
Part 4 and similar 305 mm (12 in.) parts					
Aluminum, copper, and magnesium alloys	A2	A2	A2, D2	A2, D2, CPM 10V	**Carbide**
Carbon and alloy steel, up to 0.70% C, and ferritic stainless steel	A2	A2	A2, D2	A2, D2, CPM 10V	**Carbide**
Stainless steel, austenitic, up to quarter hard	A2	A2	A2, D2	D2, D4, CPM 10V	**Carbide**
Stainless steel, austenitic, over quarter hard	A2	D2	D2	D2, D4, CPM 10V	**Carbide**

Spring steel, hardened, 52 HRC max	A2	A2, D2	D2	D2, D4, CPM 10V	**Carbide**
Electrical sheet, transformer grade, 0.64 mm (0.025 in.)	A2	A2, D2	D2	D2, D4, CPM 10V	**Carbide**
Paper, gaskets, and other soft materials	W1[(b)]	W1[(b)]	W1[(l)]	W1, A2	**D2, CPM 10V**
Plastic sheet, not reinforced	A2	A2	A2	A2, D2, CPM 10V	**Carbide**
Plastic sheet, reinforced	**A2[(f)]**	**A2[(f)]**	**D2[(f)]**	**D2[(f)], CPM 10V**	**Carbide**

Note: Although carbide is recommended in this table only for 10 million pieces, it should usually be considered also for runs of 1-10 million pieces.

(a) Zn refers to a die made of zinc alloy plate and a punch of hardened tool steel.

(b) For punching up to 10,000 parts, the W1 punch and die would be left soft and the punch peened to compensate for wear if necessary.

(c) For punching 10,000-1,000,000 pieces, the W1 punch can be soft so that it can be peened to compensate for wear, or it can be hardened and ground to size.

(d) Of the two alternatives listed, A2 tool steel is preferred if compound tooling is to be used for quantities of 10,000-1,000,000.

(e) This O1 punch may have to be cyanided 0.1 to 0.2 mm (0.044 to 0.008 in.) deep to make even 1000 pieces.

(f) For the application indicated, the punch and die should be gas nitrided 12 h at 540-565 °C (1000-1050 °F).

(g) Soft.

(h) Working edges are flame hardened in this application.

(i) May be soft or flame hardened.

(j) For punching 10,000-1,000,000 pieces, the punch would be W1, left soft so that it can be peened to compensate for wear, and the die would be O1, hardened.

(k) Cyaniding of the punch is advisable, even for 1000 pieces.

(l) For punching 10,000-1,000,000 pieces, the W1 die would be hardened and the W1 punch would be soft, so that it can be peened to compensate for wear.

Table 2 Typical punch and die materials for shaving 1.3 mm (0.050 in.) sheet

Work material	Tool material for production quantity of:			
	1000	**10,000**	**100,000**	1,000,000
Aluminum, copper, and magnesium alloys	O1[(a)]	A2	A2	**D4[(b)], CPM 10V**
Carbon and alloy steel, up to 0.30% C, and ferritic stainless steel	A2	A2	D2	**D4[(b)], CPM 10V**
Carbon and alloy steel, 0.30-0.70% C	A2	D2	D2	**D4[(b)], CPM 10V**
Stainless steel, austenitic, up to quarter hard	A2	D2	D4[(b)]	**D4[(b)], CPM 10V**
Stainless steel, austenitic, over quarter hard, and spring steel hardened to 52 HRC	**A2**	**D2**	**D4[(b)]**	**M2[(b)], CPM**

(a) Type O2 is preferred for dies that must be made by broaching.

(b) On frail or intricate sections, D2 should be used in preference to D4 or M2. Carbide shaving punches may also be practical for this quantity.

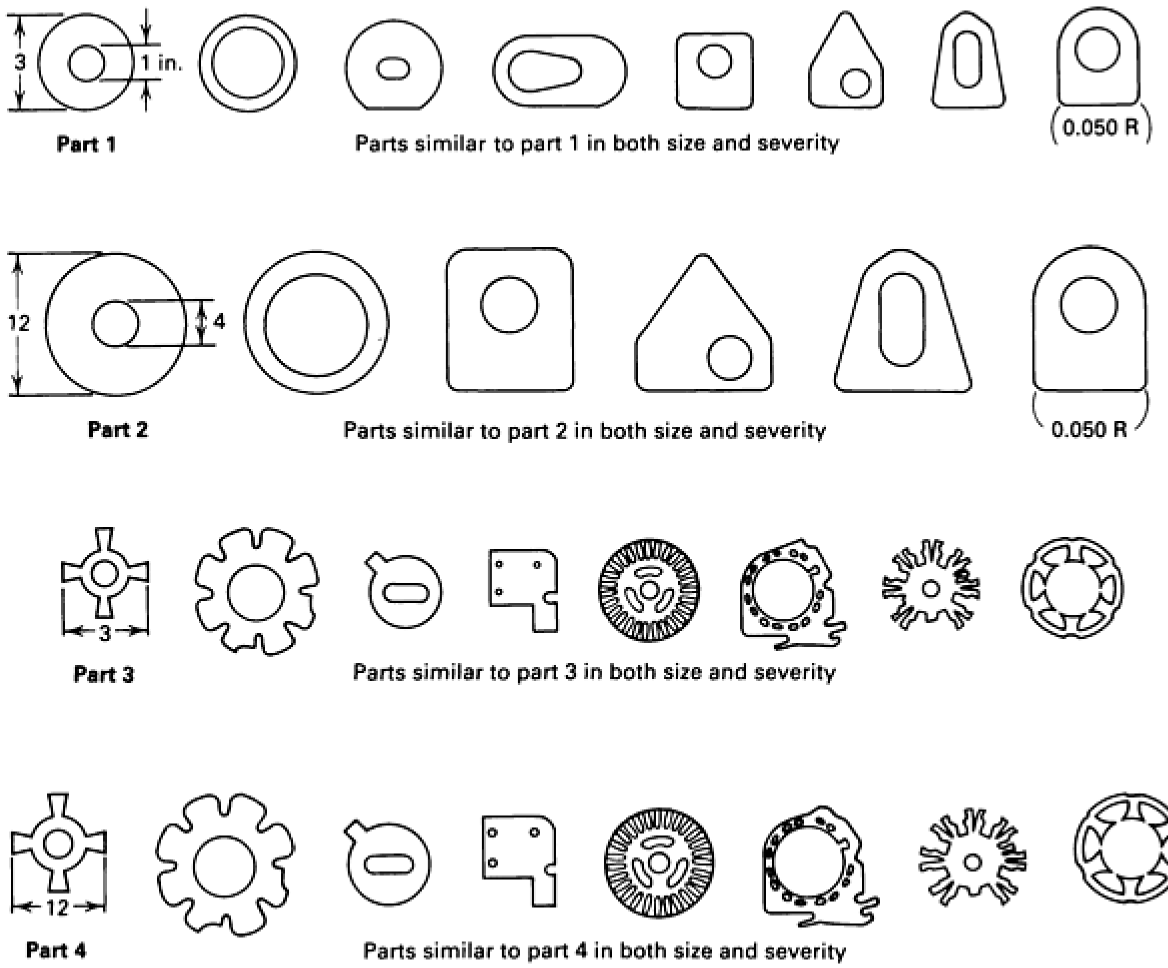

Fig. 2 Typical parts of varying severity that are commonly produced by blanking and piercing. Parts 1 and 2 are relatively simple parts that require dies similar to those illustrated in Fig. 1. Parts 3 and 4 are more complex, requiring notching and the use of compound or progressive dies. Dimensions given in inches

Tables 1 and 2 can be used to select punch and die materials for parts made of sheet that is thicker or thinner than the 1.3 mm (0.050 in.) used for the parts illustrated in Fig. 2. For thicker sheet, the punch and die material recommended for the next greater production quantity should be used instead of the material recommended for the production quantity that will actually be made (in Tables 1 and 2, the column to the right of the production quantity that will actually be made). For thinner sheet, the punch and die material recommended for the next lower production quantity should be used instead of the material recommended for the production quantity that will actually be made (in Tables 1 and 2, the column to the left of the production quantity that will actually be made).

Table 3 lists typical materials for perforator punches used on several different work materials. The usual limiting slenderness ratio (punch diameter to sheet thickness) for piercing aluminum, brass, and steel is 2.5:1 for unguided punches and 1:1 for guided punches. For piercing spring steel and stainless steel, this ratio ranges from 3:1 to 1.5:1 for unguided punches and from 1:1 to 0.5:1 for accurately guided punches. Typical hardnesses for these perforator punches are given in Fig. 3.

Table 3 Typical materials for perforator punches

Work material	Punch material for production quantity of:		
	10,000	**100,000**	1,000,000

Punch diameters up to 6.4 mm ($\frac{1}{4}$ in.)			
Aluminum, brass, carbon steel, paper, and plastics	M2	M2, CPM 10V	**M2, CPM 10V**
Spring steel, stainless steel, electrical sheet, and reinforced plastics	M2	M2, CPM 10V	**M2, CPM 10V**
Punch diameters over 6.4 mm ($\frac{1}{4}$ in.)			
Aluminum, brass, carbon steel, paper, and plastics	W1	W1	**D2, CPM 10V**
Spring steel, stainless steel, electrical sheet, and reinforced plastics	**M2**	**M2, CPM 10V**	**M2, CPM 10V**

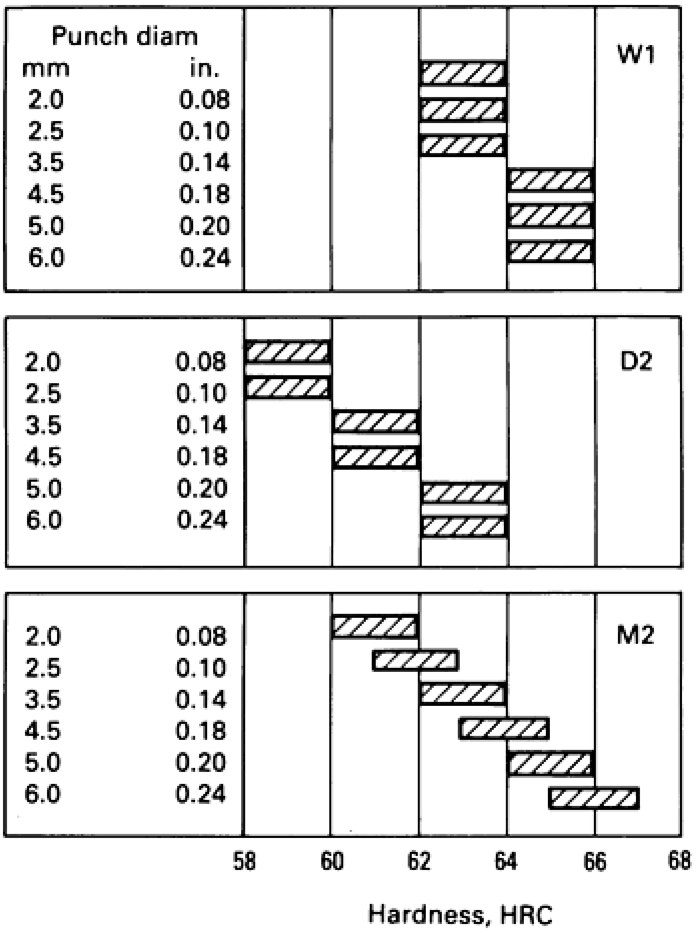

Fig. 3 Typical hardnesses for tool steel perforator punches. Regardless of material, punches should be tempered back to 56 to 60 HRC if they are to be subjected to heavy shock or used to pierce thick material.

Table 4 lists typical materials for perforator bushings of all three types (punch holder, guide or stripper, and perforator or die). These recommendations are particularly applicable to precision bushings--for example, where the outside diameter is ground to a tolerance of -0, +0.008 mm (-0, +0.0003 in.) and is concentric with the inside diameter within 0.005 mm (0.0002 in.) total indicator reading. The hardness of W1 bushings should be 62 to 64 HRC, and that of D2 bushings, 61 to 63 HRC.

Table 4 Typical materials for perforator bushings

Work material	Bushing material for production quantity of:		
	10,000	**100,000**	1,000,000
Aluminum, brass, carbon steel, paper, and plastics	W1[(a)]	W1[(a)]	**D2**

(a) When bushings are of a shape that cannot be ground after hardening, an oil- or air-hardening steel is recommended to minimize distortion.

Die plates and die parts that hold inserts are usually made of gray iron, alloy steel, or tool steel. For stamping thick sheet or hard materials, either class 50 gray iron or 4140 steel heat treated to a hardness of 30 to 40 HRC should be used. For long-run die plates for stamping thick or hard materials, steels such as 4340 and H11 are preferred when inserts are pressed into the die plates, and 4340 is nearly always used when inserts are screwed in. Die plates for stamping thin or soft sheet can be made of class 25 or class 30 gray iron or carbon steel.

Secondary Tooling. Punch holders and die shoes for carbide dies are made of high-strength gray iron or low-carbon steel plate. Yokes for retaining carbide sections are usually made of O1 tool steel hardened to 55 to 60 HRC. Backup plates for carbide tools are preferably made of O1 hardened to 48 to 52 HRC. Strippers can usually be made of low- or medium-carbon steel (1020 or 1035) plate. Where a hardened plate is used for medium-production work, 4140 flame hardened, W1 conventionally hardened, or W1 cyanided and oil quenched are often preferred. Hardened strippers for carbide dies and high-production D2, D4, or CPM 10V dies are made of O1 or A2, hardened to 50 to 54 HRC.

Custom-made hardened guides and locator pins are usually made of W1 or W2 for most medium- or long-run dies or of alloy steels such as 4140 for low-cost short-run dies. Commercial guide pins are often made of SAE 1117 and then carburized, hardened, and finished to a surface roughness of 0.4 μm (16 μin.) rms.

Applications of Specific Materials

Rolled zinc alloy tooling plate is available in the form of 6.4 mm ($\frac{1}{4}$ in.) plate from the principal suppliers of zinc-base die-casting alloys. Dies of this material are sheared in with a flame-hardened O1 punch, and strippers of 9.5 mm ($\frac{3}{8}$ in.) sheet cork are invariably used with them.

Tools of hot-rolled low-carbon steel plate (0.10 to 0.20% C) can be used for short runs of small parts if these tools have been surface hardened, either by carburizing to a depth of 0.25 to 0.50 mm (0.010 to 0.020 in.) or by cyaniding to a depth of 0.1 to 0.2 mm (0.004 to 0.008 in.). Because of distortion in heat treatment, use of this material is limited to the blanking of small, symmetrical shapes.

For the long-run blanking of soft materials, various sizes of aircraft-quality 4140 steel plate have been used. In this application, 4140 is normally flame hardened to about 50 HRC. Flame hardening the working edge of a large die has an advantage over through hardening in that very little warping or change of size occurs. However, tools with inside or outside corners may have soft spots after flame hardening and, if so, will perform poorly.

The tool steels in Table 1 are assumed to have been hardened and tempered by conventional methods to their maximum usable hardness (58 to 61 HRC. In addition to these tool steels, type O6 has given satisfactory service in multiple-stage progressive dies, and type A10, because of its low austenitizing temperature, high dimensional accuracy, and good dimensional stability, is often used to make large dies for stamping laminations.

In some applications, M2 high-speed steel tools may produce smaller burrs than D2 tools (for equal numbers of parts). In addition, steel-bonded carbides and high-vanadium-carbide powder metallurgy tool materials such as CPM 10V should be considered for critical applications. Steel-bonded carbides belong to the cemented carbide family and are also produced by the powder metallurgy process, but differ from cemented carbides in that they have variable physical properties (specifically hardness) obtained by heat treatment of their matrices. Crucible particle metallurgy tool steels and their applications are described in the article "Particle Metallurgy Tool Steels" in *Powder Metal Technologies and Applications*, Volume 7 of the *ASM Handbook.*

Cemented tungsten-carbide tooling should be considered where production life must be four or more times that possible with D4 tool steel. Partial or complete carbide inserts in tool steel dies may be considered for lower quantities, especially where close tolerances and minimum burr height are desired or where tool life between resharpenings needs to be extended. However, brazed inserts are hazardous, and the cost of dovetailed or mechanically held inserts approaches that of complete carbide dies.

Composition and hardness of carbides frequently used in blanking and piercing dies are as follows:

Composition, %			Hardness, HRA
W	**C**	**Co**	
75.1	4.9	20.0	**86**
78.9	5.1	16.0	**86**
81.7	5.3	13.0	**88**

The first material listed above should be used where shock is appreciable. The second material combines toughness and wear resistance and is preferred for heavy-duty service, such as the piercing of silicon steel. Where close tolerances must be held in piercing silicon steel laminations, the third material is useful. The fourth material is best for guides and guide rolls and for applications involving very light shock. The data in Fig. 4 show that the difference in wear life between two different tool steels at the same hardness is negligible compared with the difference between the average life of conventional tool steel dies and the life of a carbide die or a CPM 10V die.

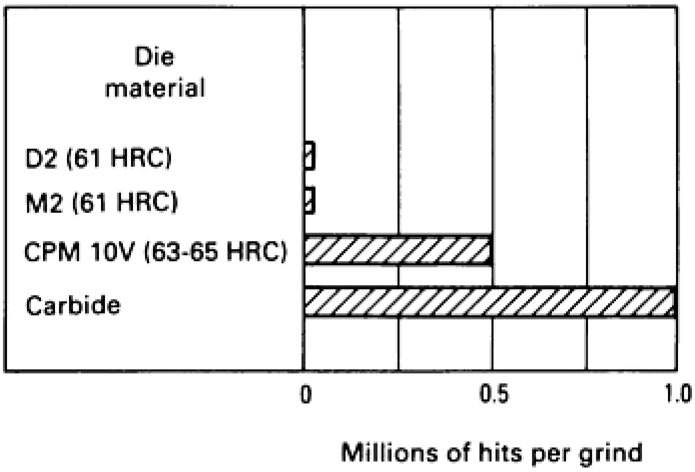

Fig. 4 Relative service lives of three steel dies and one carbide die. Die life was obtained under the same operating conditions; that is, the blanking of 3.25% Si electrical steel sheet 0.36 mm (0.014 in.) thick. Dies were reground when they had worn sufficiently to produce a burr 0.13 mm (0.005 in.) high.

Introduction

THE PRESSES described in this article are mechanically or hydraulically powered machines used for producing parts from sheet metal. Forging equipment is described in the article "Hammers and Presses for Forging" in this Volume.

Power presses can be classified according to the following characteristics: source of power, type of frame, method of actuation of slides, and number of slides in action. Presses in any of these classes are available in a range of capacities (tonnage or bed area), although the range is not necessarily the same for all types of presses. Characteristics of 18 types of presses are summarized in Table 1.

Table 1 Characteristics of 18 types of presses

Type of press	Frame type							Frame position				Action			Actuation method				
	Open-back	Gap	Straight-side	Arch	Pillar	Solid	Tie rod	Vertical	Horizontal	Inclinable	Inclined	Single	Double	Triple	Crank	Front-to-back crank	Eccentric	Toggle	Screw
Bench	X	X	. . .	. . .	. . .	X	. . .	X	. . .	X	X	X	. . .	. . .	X	. . .	X	. . .	X
Open-back inclinable	X	X	. . .	. . .	. . .	X	. . .	X	. . .	X	. . .	X	X	. . .	X	. . .	X	. . .	. . .
Gap frame	X	X	. . .	. . .	. . .	X	X	X	X	X	X	X	X	. . .	X	X	X	X	. . .
Adjustable-bed horn	. . .	X	. . .	. . .	. . .	X	. . .	X	. . .	. . .	. . .	X	. . .	. . .	X	. . .	X	. . .	. . .
End wheel	. . .	X	. . .	. . .	. . .	X	. . .	X	. . .	. . .	. . .	X	. . .	. . .	. . .	X	X	. . .	. . .
Arch frame	. . .	. . .	X	X	. . .	X	. . .	X	. . .	. . .	X	X	. . .	. . .	X	. . .	. . .	. . .	. . .
Straight-side	. . .	. . .	X	X	. . .	X	X	X	X	. . .	X	X	X	X	X	X	X	. . .	. . .
Reducing	X	X	X	. . .	. . .	X	X	X	X	. . .	. . .	X	. . .	. . .	X	. . .	. . .	. . .	. . .
Knuckle lever	. . .	. . .	X	. . .	. . .	X	X	X	. . .	. . .	X	X	X	. . .	. . .	. . .	. . .	X	. . .
Toggle draw	. . .	. . .	X	. . .	. . .	X	X	X	. . .	. . .	. . .	. . .	X	X	X	. . .	X	X	. . .

Cam-drawing	X	X	X	. . .	. . .	X	X	X	. . .	X	. . .	. . .	X	. . .	X	. . .	. . .	. . .	. . .	X
Two-point single-action	. . .	X	X	. . .	. . .	X	X	X	. . .	. . .	X	X	. . .	. . .	X	. . .	X	. . .	. . .	. . .
High-production	. . .	. . .	X	. . .	. . .	X	X	X	. . .	. . .	X	X	. . .	. . .	X	. . .	X	. . .	. . .	. . .
Dieing machine	. . .	. . .	. . .	. . .	X	. . .	. . .	X	. . .	. . .	. . .	X	. . .	. . .	X	. . .	. . .	. . .	. . .	. . .
Transfer	. . .	X	X	. . .	. . .	X	X	X	. . .	. . .	. . .	X	. . .	. . .	X	X	X	X	. . .	. . .
Flat-edge trimming	. . .	. . .	. . .	X	. . .	X	. . .	X	. . .	. . .	. . .	. . .	X	. . .	. . .	. . .	. . .	. . .	. . .	X
Hydraulic	. . .	X	X	. . .	X	. . .	X	X	X	X	. . .	X	X	X	. . .	. . .	. . .	. . .	. . .	. . .
Press brake	X	X	. . .	. . .	. . .	X	. . .	X	. . .	. . .	. . .	X	. . .	. . .	X	. . .	. . .	. . .	. . .	. . .

	Type of drive				**Suspension**			**Ram**		Bed		
	Over direct	**Geared, overdrive**	**Under direct**	**Geared, underdrive**	**One-point**	**Two-point**	**Four-point**	**Single**	**Multiple**	**Solid**	**Open**	Adjustable
Bench	X	. . .	. . .	. . .	X	. . .	. . .	X	. . .	X	X	**X**
Open-back inclinable	X	X	. . .	. . .	X	X	. . .	X	X	. . .	X	**. . .**
Gap frame	X	X	. . .	. . .	X	X	X	X	X	X	X	**. . .**

Adjustable-bed horn	X	X	. . .	. . .	X	X	. . .	X	. . .	X	X	**X**
End wheel	X	X	. . .	. . .	X	. . .	. . .	X	. . .	X	X	**. . .**
Arch frame	X	X	. . .	. . .	X	. . .	. . .	X	. . .	. . .	X	**. . .**
Straight-side	X	X	X	X	X	X	X	X	X	X	X	**. . .**
Reducing	X	X	. . .	. . .	X	. . .	. . .	X	. . .	. . .	X	**. . .**
Knuckle lever	X	X	. . .	. . .	X	X	. . .	X	X	X	X	**. . .**
Toggle draw	. . .	X	. . .	X	X	X	X	. . .	X	. . .	X	**. . .**
Cam-drawing	X	X	. . .	. . .	X	. . .	. . .	. . .	X	. . .	X	**. . .**
Two-point single-action	. . .	X	. . .	X	. . .	X	. . .	X	. . .	X	X	**. . .**
High-production	X	X	. . .	. . .	X	X	. . .	X	. . .	. . .	X	**. . .**
Dieing machine	X	. . .	X	. . .	X	X	. . .	X	. . .	. . .	X	**. . .**
Transfer	X	X	. . .	. . .	. . .	X	. . .	X	. . .	. . .	X	**. . .**
Flat-edge trimming	. . .	X	. . .	. . .	X	. . .	. . .	X	. . .	. . .	. . .	**. . .**
Hydraulic	. . .	. . .	. . .	. . .	X	X	. . .	X	X	X	X	**X**

Press brake	. . .	**X**	. . .	. . .	. . .	**X**	**X**	**X**	. . .	**X**	. . .	. . .

The Joint Industry Conference (JIC), a committee of press builders and large-press users formed some years ago, set guidelines for uniformity with respect to nomenclature, bed and ram sizes, force ranges, and symbols for presses. Although JIC is no longer in existence, most press builders adhere to the standards either completely or in part. Under the JIC press classification system with respect to the number of slides, the first letter in the designation is *S* for single-action, *D* for double-action, and *T* for triple-action presses. Other designations used for visible identification of presses suggested under the JIC classification system are given in Fig. 1. Most press builders place these markings in a prominent position on the fronts of the presses.

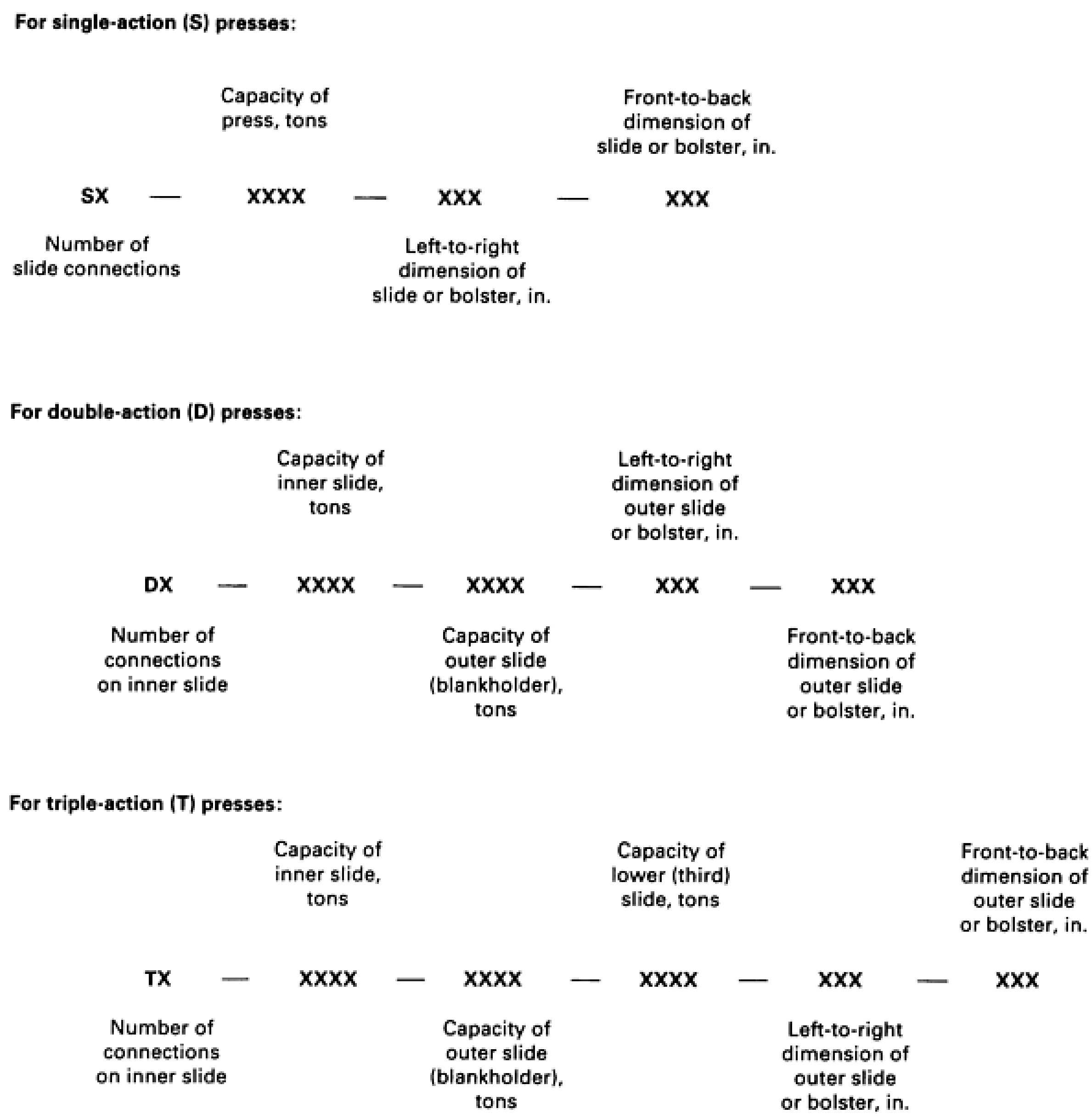

Fig. 1 Designations used for press identification under the JIC classification system.

Source of Power

Power presses for sheet metal forming can be driven either hydraulically or mechanically. The performance characteristics and other operational features of hydraulic and mechanical presses are compared in Table 2.

Table 2 Comparison of characteristics of mechanical and hydraulic presses

Force	**Capacity**	**Stroke length**	**Slide speed**	**Control**	Preferred uses
Mechanical					
Varies depending on slide position	Practical maximum of ~54 MN (6000 tonf)	Limited	Higher than hydraulic, and can be varied. Highest at midstroke	Full stroke is usually required before reversal.	**Preferred for operations requiring maximum pressure near the bottom of the stroke. Preferred for cutting operations such as blanking and piercing, and for relatively shallow forming and drawing (depths to about 102 mm, or 4 in.). Good for high-production applications and progressive and transfer die operations**
Hydraulic					
Relatively constant (does not depend on slide position)	**445 MN (50,000 tonf) or more**	**Capable of long (2.5 m, or 100 in.) strokes**	**Slower pressing speeds, with rapid advance and retraction. Speed is uniform throughout the stroke.**	**Adjustable; slide can be reversed at any position.**	**Good for operations requiring steady pressure throughout the stroke. Preferred for deep drawing, die tryout, flexible-die forming, drawing of irregular-shape parts, straightening, operations requiring high and variable forces, and operations requiring variable or partial strokes**

Mechanical Presses

In most mechanical presses, a flywheel is the major source of energy that is applied to the slides by cranks, gears, eccentrics, or linkages during the working part of the stroke. The flywheel runs continuously and is engaged by the clutch only when a press stroke is needed. In some very large mechanical presses the drive motor is directly connected to the press shaft, thus eliminating the need for a flywheel and a clutch.

Two basic types of drive, gear and nongeared, are used to transfer the rotational force of the flywheel to the main shaft of the press.

Nongeared Drive. In a nongeared drive (also known as a flywheel drive), the flywheel is on the main shaft (Fig. 2a), and its speed, in revolutions per minute, controls the slide speed. Usually press speeds with this type of drive are high, ranging from 60 to 1000 strokes per minute. The main shaft can have a crankshaft, as shown in Fig. 2(a), or an eccentric.

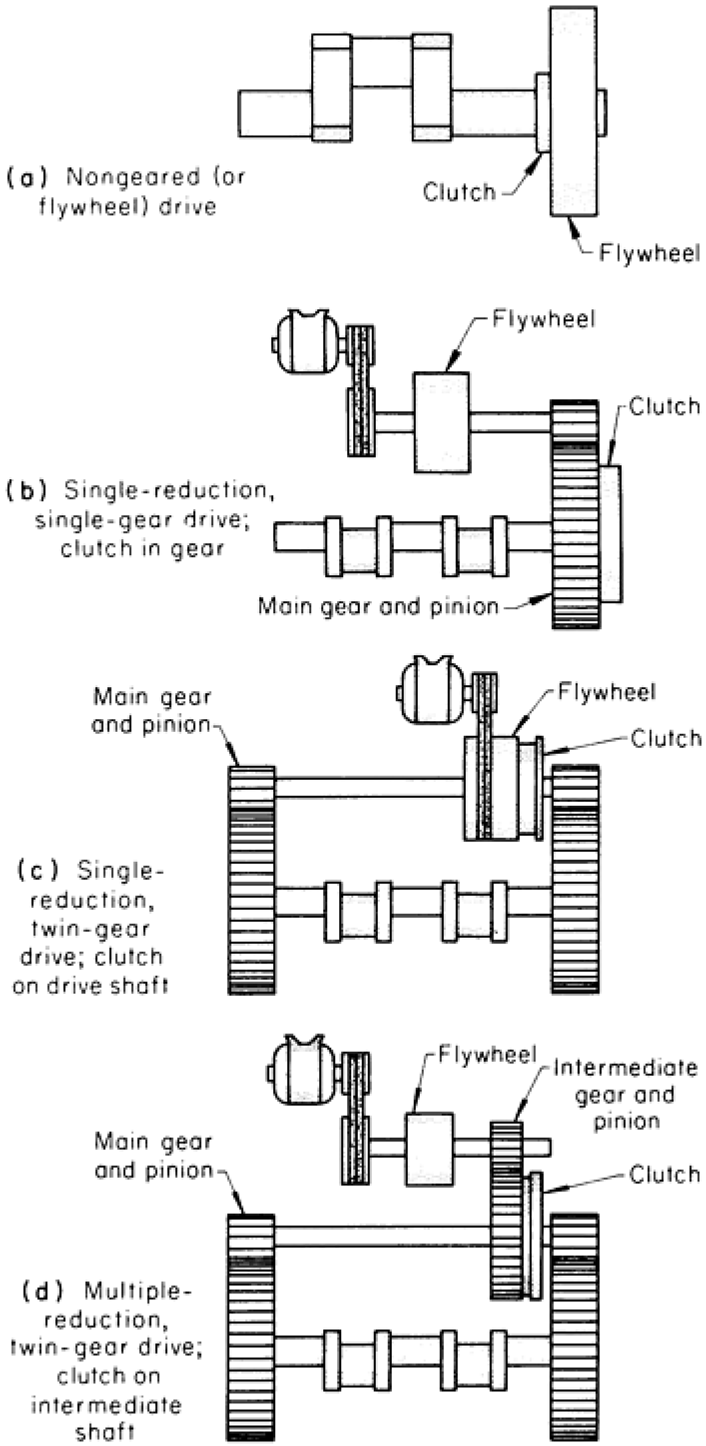

Fig. 2 Four types of drive and clutch arrangements for mechanical presses.

Energy stored in the flywheel should be sufficient to ensure that the reduction in the speed of the flywheel will be no greater than 10% per press stroke. If the energy in the flywheel is not sufficient to maintain this minimum in speed reduction, a gear-driven press should be used.

Gear drives (Fig. 2b, c, and d) have the flywheel on an auxiliary shaft that drives the main shaft through one or more gear reductions. Either single-reduction or multiple-reduction gear drives are used, depending on size and tonnage requirements. In gear-driven presses, there is more flywheel energy available for doing work than there is in the nongeared presses, because the speed of the flywheel is higher than that of the main shaft. The flywheel shaft of a gear-driven press often is connected to the main shaft at both ends (Fig. 2c), which results in a more efficient drive.

A single-reduction gear drive develops speeds of 30 to 100 strokes per minute. Speed for a multiple-reduction twin gear drive (Fig. 2d) is usually 10 to 30 strokes per minute, which provides exceptionally steady pressure.

Hydraulic Presses

Hydrostatic pressure against one or more pistons provides the power for a hydraulic press. Most hydraulic presses have a variable-volume, variable-pressure, concentric-piston pump to provide them with a fast slide opening and closing speed. It also provides a slow working speed at high forming pressure.

The principal components of a typical hydraulic press are shown in Fig. 3. A bolster plate is attached to the bed to support the dies and to guide the pressure pins between the die cushion and the pressure pad.

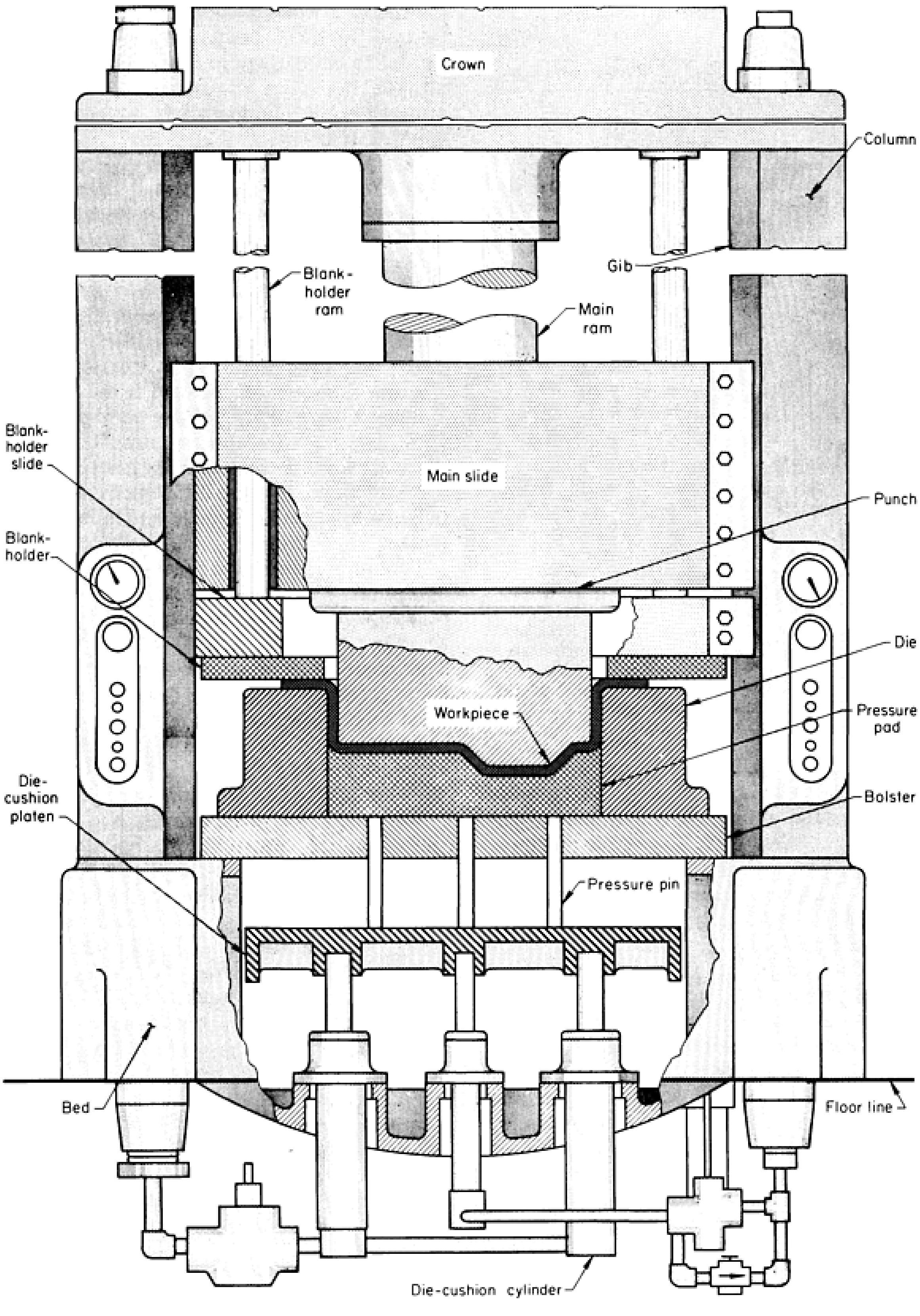

Fig. 3 Principal components of a double-action hydraulic press with a die cushion.

The capacity of a hydraulic press depends on the diameter of the hydraulic pistons and on the rated maximum hydraulic pressure, the latter being a function of the pump pressure and related mechanisms. Hydraulic presses with capacities up to 445 MN (50,000 tonf) have been built, but most have a capacity of less than 133 MN (15,000 tonf). The typical hydraulic press is rated at 900 kN to 9 MN (100 to 1000 tonf). Gap-frame presses are rated at 45 to 450 kN (5 to 50 tonf).

Because of their construction, hydraulic presses can be custom designed at a relatively low cost. They can be designed with a number of slides and motions, or separate hydraulic circuits can be used for various independent actions. In addition, side action can be provided within the frame of the press by means of separate cylinders. Such side action in a mechanical press is usually provided by cams and is complex and expensive. Most hydraulic presses are straight-side models, but small, fast, gap-type presses designed to compete with mechanical open-back inclinable presses have been developed.

Hydraulic press slides, or platens, are actuated by numerous combinations of hydraulic drives. Hydraulic presses usually have a longer stroke than mechanical presses, and force can be constant throughout the stroke. Hydraulic presses have an adjustable stroke for one or more slides. Accumulators or large-volume pumps can provide fast motion for a slide to open and close. High-pressure pumps provide the working force at a slower speed.

Usually all slides are operated by one pumping system. The relation of each action to the others, interaction, and timing all depend on the controls.

Press Selection

Proper selection of a press is essential for successful and economical operation. The purchase of a press represents a substantial capital investment, and return on investment depends upon how well the press performs the job required. No general-purpose press exists that can provide maximum productivity and economy for all applications. Compromises usually must be made to permit a press to be employed for more than one job. Careful consideration should be given to both present and future production requirements.

Important factors influencing the selection of a press include size, force, energy, and speed requirements. The press must be capable of exerting force in the amount, location, and direction, as well as for the length of time, needed to perform the specified operation or operations. Other necessary considerations include the size and geometry of the workpieces, the workpiece material, operation or operations to be performed, number of workpieces to be produced, production rate needed, accuracy and finish requirements, equipment costs, and other factors.

Size, Force, and Energy Requirements. Bed and slide areas of the press must be large enough to accommodate the dies to be used and provide adequate space for die changing and maintenance. Space is required around the dies for accessories such as keepers, pads, cam return springs, and gages; space is also needed for attaching the dies to the press. Shut height of the press, with adjustment, must also be suitable for the dies.

Presses with as short a stroke as possible should be selected because they permit higher-speed operation, thus increasing productivity. Stroke requirements, however, depend upon the height of the parts to be produced. Blanking can be done with short strokes, but some forming and drawing operations require long strokes, especially for ejection of parts.

Size and type of press to be selected also depend upon the method and direction of feeding; the size of sheet, coil stock, blank, or workpiece to be formed; the type of operation; and the material being formed and its strength. Material or workpiece handling and die accessibility generally determine whether the press should be of gap-frame or straight-side construction, and whether it should be inclined or inclinable.

Physical size of a press can be misleading with respect to its capacity. Presses having the same force rating can vary considerably in size depending upon differences in length of stroke, pressing speed, and number of strokes per minute.

The force required to perform the desired operations determines press capacity, expressed in tons or kilonewtons (kN) (see the section "Press Capacity" in this article). The position on the stroke at which the force is required and the length of stroke must be considered.

Energy or work (force times distance), expressed in inch-tons or joules (J), varies with the operation. Blanking and punching require the force to be exerted over only a short distance; drawing, forming, and other operations require force application over a longer distance. The major source of energy in mechanical presses is the flywheel, the energy varying with the size and speed of the flywheel. The energy available increases with the square of the flywheel speed.

Possible problems are minimized by selecting a press that has the proper frame capacity, drive motor rating, flywheel energy, and clutch torque capacity.

Speed Requirements. Press speed is a relative term that varies with the point of reference. Fast speeds are generally desirable, but they are limited by the operations performed, the distances above stroke bottoms where the forces must be applied, and the stroke lengths. High speed, however, is not necessarily the most efficient or productive. Size and configuration of the workpiece, the material from which it is made, die life, maintenance costs, and other factors must be considered to determine the highest production rate at the lowest cost per workpiece. A lower speed may be more economical because of possible longer production runs with less downtime.

Simple blanking and shallow forming operations, however, can be performed at high speeds. Mechanical presses have been built that operate to 2000 strokes/min with 25 mm (1 in.) stroke, but applications at this maximum speed are rare. Speeds of 600 to 1400 strokes/min are more common for blanking operations, and thick materials are often blanked at much slower speeds. For drawing operations, contact velocities are critical with respect to the workpiece material, and presses are generally operated at slide speeds from 10 to 300 strokes/min, with the slower speed for longer stroke drawing operations.

Mechanical Versus Hydraulic Presses. Mechanical presses are the most frequently used for the blanking, forming, and drawing of sheet metal, but hydraulic presses are being increasingly applied. There are applications for which hydraulic presses offer certain advantages and, in some cases, are the only machines that can be used. For example, very high force requirements can only be met with hydraulic presses. A comparison of characteristics and preferred uses for both mechanical and hydraulic presses is presented in Table 2.

Type of Press Frame

Presses are broadly classified, according to the type of frame used in their construction, into two main groups: gap-frame presses and straight-side presses. Details of construction vary widely in each group.

Gap-frame presses (Fig. 4) are sometimes called C-frame presses because the frame resembles the letter C when viewed from the side. The gap makes the die area accessible from either side, as well as from the front, for ease in die setting or for feeding stock. Coil stock often is supplied to gap-frame presses by feeders from stock reels and straighteners.

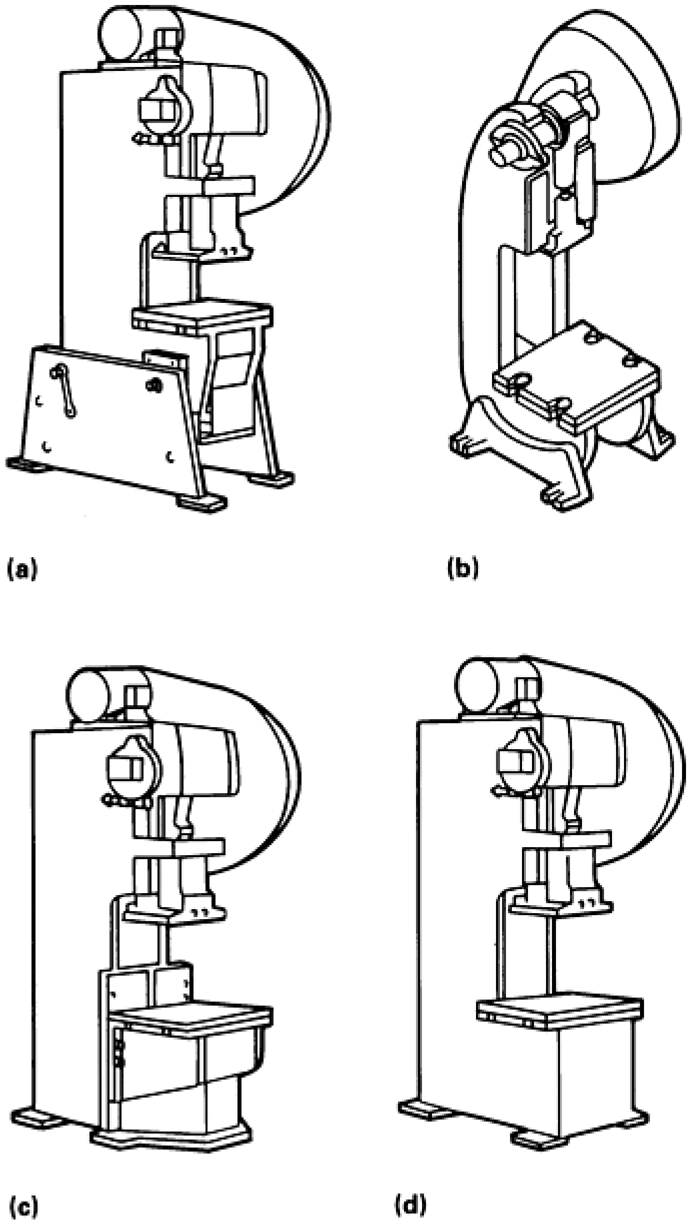

Fig. 4 Four types of gap-frame presses. (a) Open-back inclinable. (b) Bench press. (c) Adjustable-bed stationary. (d) Open-back stationary.

Workpieces usually are ejected through an opening in the press bed, or through the back of an open-back press. (A few gap-frame presses with solid backs are in service, but solid-back presses are coming into increasing disfavor because work cannot be ejected through the back of the press and because the design in general is less convenient than is the open-back type).

Gap-frame construction has one disadvantage: The gap opens under load, thus causing angular deflection. (A straight, vertical deflection would be less critical.) Gap deflection resulting from overload causes misalignment of punches and dies, which is a major cause of premature die wear.

Tie rods extending from the top of the frame to the front corners of the bed can be used to minimize deflection. Because the tie rods close the gap, the accessibility to the die area and the width of parts fed into the die are limited. Tie rods can be removed for die setup.

Straight-side presses have a frame made up of a base, or bed; two columns; and a top member, or crown. In most straight-side presses, steel tie rods hold the base and crown against the columns. Straight-side presses have crankshaft, eccentric-shaft, or eccentric-gear drives (see the section "Slide Actuation in Mechanical Presses" in this article).

A single-action straight-side press is shown in Fig. 5. The slide in this illustration is equipped with air counterbalances to assist the drive in lifting the weight of the slide and the upper die to the top of the stroke. Counterbalance cylinders provide a smooth press operation and easy slide adjustment. Die cushions are used in the bed for blank-holding and for ejection of the work.

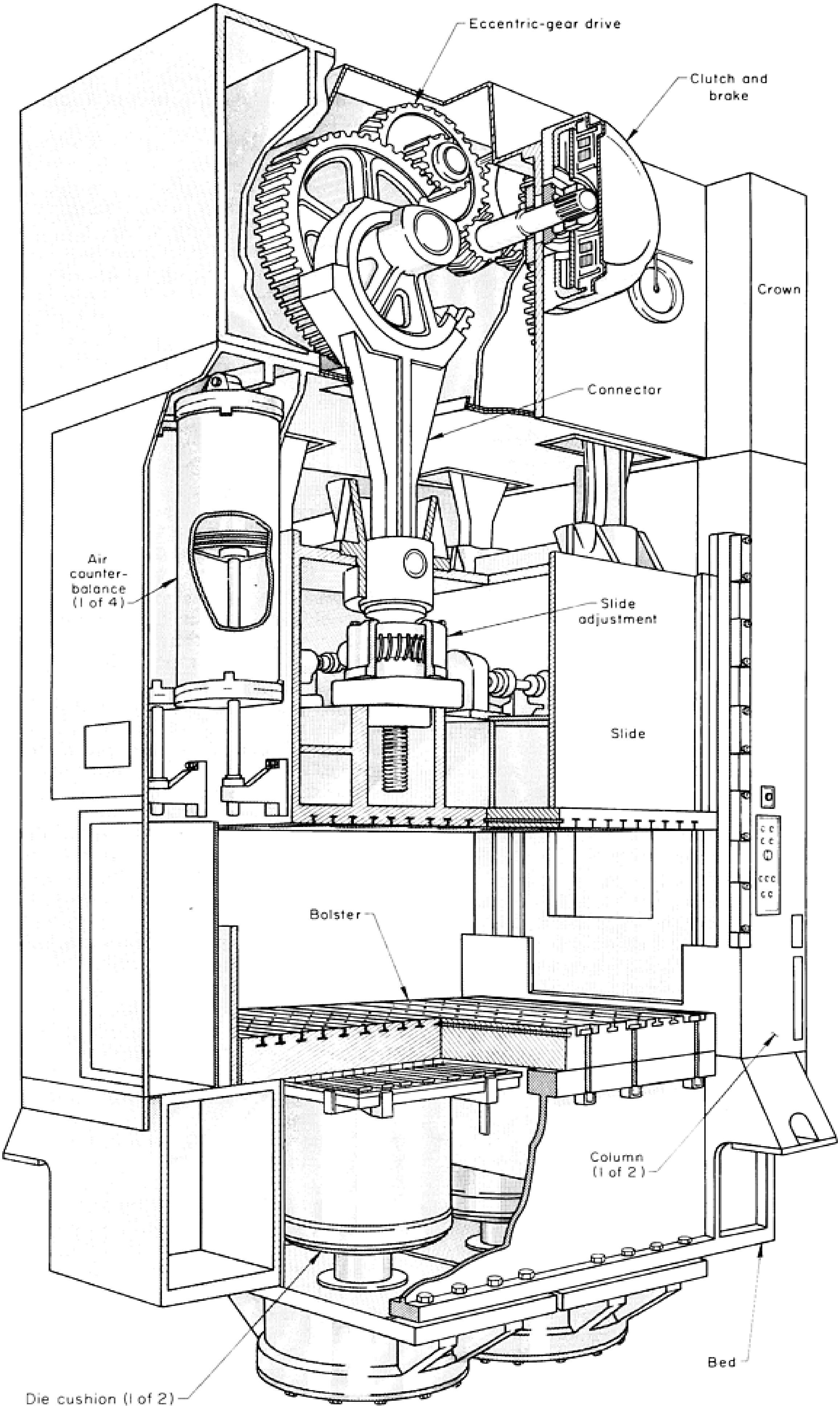
Eccentric-gear drive
Clutch and brake
Crown
Connector
Air counter-balance (1 of 4)
Slide adjustment
Slide
Bolster
Column (1 of 2)
Bed
Die cushion (1 of 2)

Fig. 5 Principal components of a single-action straight-side mechanical press. The press shown has a large bed, four-point suspension, and an eccentric drive with counterbalance cylinders. Slide adjustment is motorized.

The straight-side design permits the use of an endless variety of bed and slide sizes. Presses range from 180 kN (20 tonf) capacity and a bed of 510 × 380 mm (20 × 15 in.), to 36 MN (4000 tonf) capacity with a bed as large as 915 mm (360 in.) left-to-right by 455 mm (180 in.) front-to-back. The size and shape of the slide usually determine the number of points of suspension, or connections between the main shaft and the slide, that are needed.

The straight-side design also can provide high pressures with minimum deflection. A straight-side press deflects less under off-center loads than does a gap-frame press.

Presses and Auxiliary Equipment for Forming of Sheet Metal

Slide Actuation in Mechanical Presses

Rotary motion of the motor shaft on a mechanical press is converted into reciprocating motion of the slides by a crankshaft, eccentric shaft, eccentric-gear drive, knuckle lever drive, rocker arm drive, or toggle mechanism, each of which is discussed below.

Crankshafts. The most common mechanical drive for presses with capacities up to 2.7 MN (300 tonf) is the crankshaft drive (Fig. 6). A crankshaft is used in both gap-frame and straight-side presses. The crank-shaft drive is used most often in the single-suspension design, although some double-crank (two-point suspension) presses, particularly in the 900 to 1800 kN (100 to 200 tonf) range, also have crankshafts.

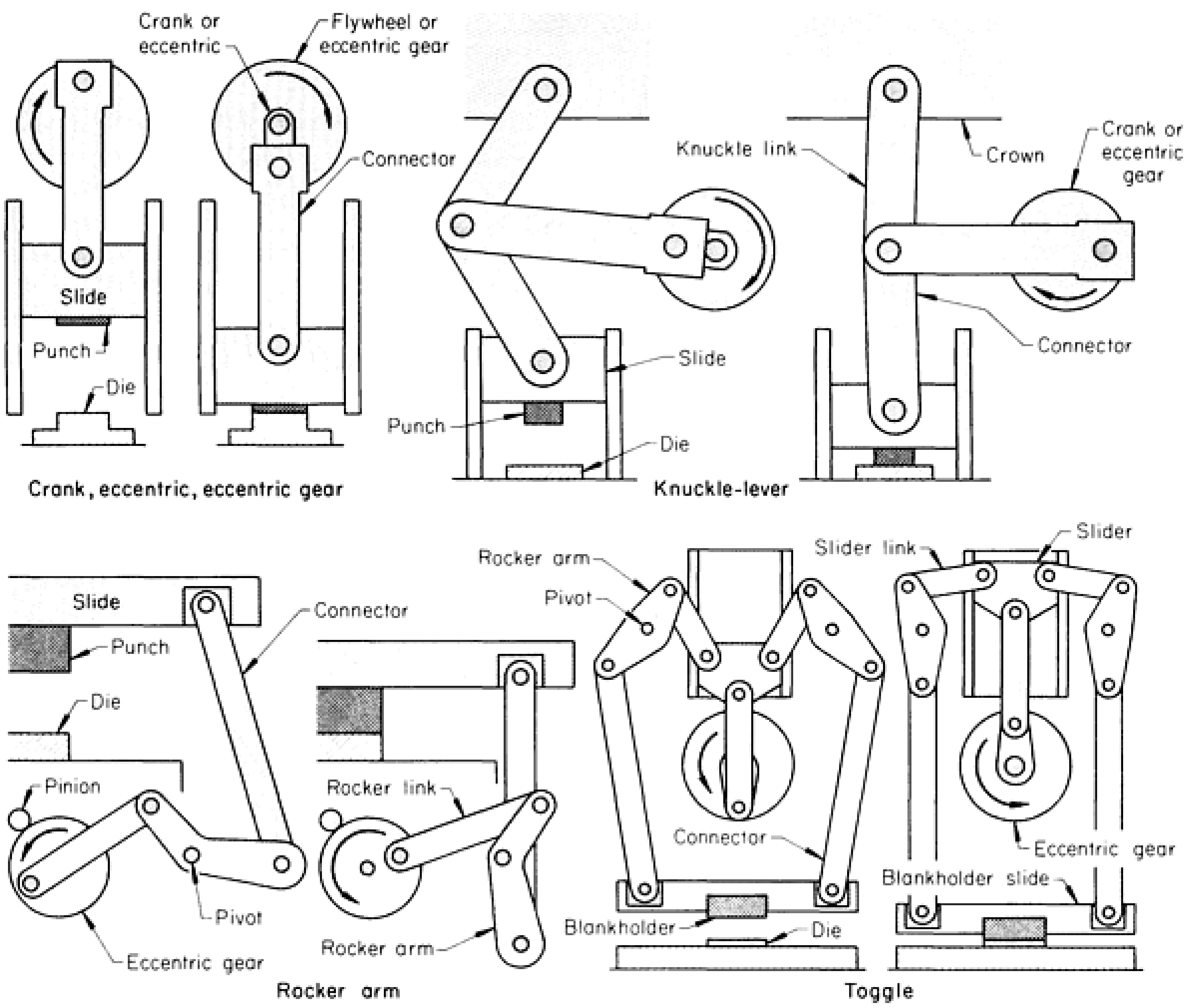

Fig. 6 Operating principles of various drive systems for mechanical presses.

The crankshaft imparts a sine-curve speed relation to the press slide. The stroke of a crankshaft-actuated press can be as short as 25 mm (1 in.), in a small gap-frame press, or as much as 760 mm (30 in.), in a straight-side press. However, most mechanical presses with longer strokes are actuated by an eccentric gear, because it provides greater strength. Crankshaft drives are usually limited to strokes of 152 to 305 mm (6 to 12 in.).

The main advantage of a crankshaft-driven press is its lower cost, particularly when capacities do not exceed 2.7 MN (300 tonf).

Points of suspension refer to the number of connections between the slide and the actuating mechanism. Presses can have single-point, two-point, or four-point suspension, depending on the number of points at which the slide is pushed or pulled. The simplest mechanical presses have a pitman that connects the eccentric shaft or the crankshaft to the slide at only one point.

Many wide mechanical presses are built with two-point suspension by connecting the slide to the crankshaft (or eccentric) with two pitmans instead of one, for better distribution of force on the slide.

The largest straight-side mechanical presses usually have four-point suspension, for more uniform loading of large slides. Four-point suspension is usually accomplished by two interconnected crankshafts or eccentrics and four pitmans; each pitman is connected near a corner of the slide.

Hydraulic presses also can have one, two, or more points of suspension by operating the slide with as many rams as desired.

Eccentric shafts are similar to crankshafts. The eccentric completely fills the space between the supporting bearings of the press crown, thereby eliminating the deflection commonly caused by the unsupported portion where the crank cheeks normally would be. Eccentric drives (Fig. 6) often are used in high-speed, short-stroke, straight-side presses with progressive dies.

The height of the workpiece is the main limitation of the eccentric-shaft drive, because the stroke is always equal to twice the eccentricity. When eccentricity is increased, the space available in the press crown determines the maximum stroke that can be used. In most presses of this type, the maximum stroke is usually limited to 152 mm (6 in.). A few presses have been built and used for high-speed operations in which strokes longer than 152 mm (6 in.) were needed. This was accomplished in the press by balancing the eccentric shaft to minimize vibration.

Eccentric-gear drives (Fig. 6) are used almost universally for large, straight-side presses that operate at speeds under 50 strokes per minute. In place of a crankshaft, an eccentric is built as an integral part of the press drive gear. The eccentric gear permits strokes as long as 1.3 m (50 in.); however, with such long strokes, speeds are usually only 8 to 16 strokes per minute. With the eccentric as part of the gear, accuracy of alignment of the slide is determined by accuracy and alignment of the gears. In a two-point suspension, the parallel condition of the ram is determined by the alignment of the driving gears. The principal advantage of the eccentric gear is that it permits greater torque loads at points above the bottom of the stroke. It also permits multiple-point construction with greater versatility and range of stroke length than is possible with a crankshaft.

The chief limitation of the eccentric-gear design is that it usually requires an overhung flywheel. In addition, a single-gear eccentric press usually costs more than a crankshaft or eccentric-shaft press of equal capacity. A further limitation is that eccentric-gear presses are more likely to stick at the bottom of the stroke than are crankshaft presses. Sticking is caused by greater friction in the connector, which is inherent in the large-diameter eccentrics needed for an equal press stroke. Sticking usually occurs during setup, if the press is moved slowly until the bottom of the stroke is reached. At this point, a skilled setup man can usually detect whether the press is likely to stick.

Knuckle lever drives combine the motions of a crank and knuckle lever to drive the press slide (Fig. 6). Their use is limited to operations such as coining or embossing, in which the work is done almost entirely at the bottom of a short stroke. The knuckle lever mechanism permits large capacity in a relatively small press. High mechanical advantage is inherent at the bottom of the stroke. These presses are rated to deliver full capacity at 1.6 to 6.4 MM ($\frac{1}{16}$ to $\frac{1}{4}$ in.) above the bottom of the stroke. The very quick increase in force as the slide nears the bottom of the stroke is the reason its usefulness is limited to operations performed at the bottom of the stroke. Knuckle lever presses usually have capacities of 1.4 to 8.9 MN (150 to 1000 tonf).

Rocker arm drives apply crank or eccentric motion to a rocker arm that is connected to the press slide (Fig. 6). In this mechanism, the linkage is driven by an eccentric gear and a connecting rod. The rocker arm drive is a variation of the knuckle lever drive. However, a press with rocker arm drive is not limited to coining operations; it can also be used for drawing or forming operations.

The rocker arm drive is used mainly in large-bed underdrive presses. The linkage operates from below the press bed and pulls the slide into the work by a link running up through each of the press columns. In most rocker arm drives, the rocker pin and the connecting eccentric pin do not stop in a vertical plane; thus, the load on the eccentric shaft is relieved at the point of maximum load on the slide, and sticking at the bottom of the stroke is prevented. In addition, a high press capacity is obtained because of the mechanical advantage.

Toggle mechanisms are the most widely used means of providing the second action in double-action mechanical presses. The toggles operate an outer slide, which clamps the blank against the die, while the punch, operated by the inner slide directly from the crankshaft, performs the draw operation. Principal components of a toggle mechanism are shown in Fig. 6.

Number of Slides

Mechanical (and hydraulic) presses have one, two, or three slides and are referred to as single-, double-, or triple-action presses. Each slide can be moved in a separately controlled motion.

A single-action press has one reciprocating slide (tool carrier) acting against a fixed bed. Presses of this type, which are the most widely used, can be employed for many different metal-stamping operations, including blanking, embossing, coining, and drawing. Depending on the depth of draw, single-action presses often require the use of a die cushion for blankholding. In such applications, a blankholder ring is depressed by the slide (through pins) against the die cushion, usually mounted in the bed of the press (see the section "Die Cushions" in this article).

A double-action press has two slides moving in the same direction against a fixed bed. These slides are generally referred to as the outer (blankholder) slide and the inner (draw) slide. The blankholder slide is a hollow rectangle, while the inner slide is a solid rectangle that reciprocates within the blankholder.

Double-action presses are more suitable for drawing operations, especially deep drawing, than are single-action presses. In single-action presses, force is required to depress the cushion. In double-action presses, the blankholder slide has a shorter stroke and dwells at the bottom of its stroke before the punch mounted on the inner slide contacts the work. As a result, practically the entire capacity of the press is available for drawing. Another advantage is that the four corners of the blankholder are individually adjustable so that nonuniform forces can be exerted on the work when required. A double-action press equipped with a die having an open bottom permits pushing the stamping through the die to perform other operations, such as ironing, after drawing.

Deep-drawing operations and irregularly shaped stampings generally require the use of a double-action press. Most operations performed on double-action presses require a cushion either for lift-out or for reverse drawing of the stamping.

A triple-action press has three moving slides: two slides moving in the same direction as in a double-action press and a third, or lower, slide moving upward through the fixed bed in a direction opposite to the blankholder and inner slides. This action permits reverse drawing, forming, or beading operations against the inner slide while both upper actions are dwelling.

Cycle time for a triple-action press is necessarily longer than it is for a double-action press because of the time required for the third action. Because most drawn stampings require subsequent restriking and/or trimming operations, which are done in faster, single-action presses, most stamping manufacturers consider the triple-action press too slow.

Press Accuracy

Suggested criteria for the accuracy of a press are:

- Maximum tolerances for parallelism between slide and bed; 0.08 mm/m (0.001 in./ft) at the bottom of the stroke for all slides; 0.24 mm/m (0.003 in./ft) at midstroke for punch slides; and 0.4 mm/m (0.005 in./ft) at midstroke for blankholder slides
- Feed, if used, should be accurate within ±0.076 mm (±0.003 in.) at 23 m/min (75 ft/min) (see the section "Press Feeds" in this article)
- Gib clearance should be set as close as required to do the job

Parallelism. In a single-point press, the slide guides basically determine parallelism of the slide face with respect to the bed or bolster, both in the front-to-back and right-to-left directions. In a two-point press, slide guides determine parallelism only in the front-to-back direction. Out-of-parallel conditions in the right-to-left direction because of faulty adjusting-screw timing, variations in throw between the cranks or eccentrics, or bicycling (periodic tilting of the slide in opposite directions) due to timing errors in the drive cannot be corrected by adjusting the slide guides.

In a four-point press, parallelism is determined strictly by the press-drive and adjusting-screw accuracy and timing. Out-of-parallel conditions at midstroke, however, can be caused by improper centering of the slide guide adjustment.

Gib Clearance. Clearances between press slide guides and gibs are required to compensate for inaccuracies in machining and in the throw and timing of two- and four-point presses, and to allow for expansion and contraction of the guides and gibs. With the bronze-to-cast iron or cast iron-to-cast iron gibbing normally used, some slight clearances must also exist for the oil or grease lubricant.

If sufficient clearance is not provided, excessive pressure on the guides occurs at some point during the stroke, resulting in galling of the mating surfaces. The amount of clearance also affects the repetitive registry of punch with die. If the clearance between punch and die is less than the clearance between the press guides, the punch can mount the die, thereby causing premature wear. Clearance between the press guides also depends upon the length of the gibs. A reasonable allowance can be 0.08 to 0.15 mm (0.003 to 0.006 in.), the exact amount varying with gib length. For high-speed presses, the clearance between a gib-type guide and the press slide at normal operating temperature is generally no more than 0.038 mm (0.0015 in.).

Clearances between the mating surfaces of press guides can be reduced by using self-lubricating reinforced-phenolic liners. The modulus of elasticity of this material is about $\frac{1}{50}$ that of steel. As a result, deformation of the liners due to the inaccuracies discussed does not raise excessive compressive stresses in the liners, providing the liners are of sufficient thickness.

Near-zero clearances can be achieved by using preloaded rolling-contact (ball or roller) bearing guides. When offset or lateral loads are encountered, however, tending to cause the press slide to tilt, only a few rolling-contact members at the top and bottom of the guides take the loads. As a result, stresses exerted on the balls or rollers can be very high.

Press Capacity

Capacity, or rating, of a press is the maximum force that the press can apply. Hydraulic presses can exert maximum force during the full press stroke. Mechanical presses exert maximum force at a specified distance above the bottom of the stroke (usually 1.6 to 13.0 mm, or $\frac{1}{16}$ to $\frac{1}{2}$ in.), and the force decreases to a minimum at midstroke.

The tonnage rating of a press may have little relation to the bed area. This is especially true in the automotive and appliance industries, where presses have large bed areas and die spaces but relatively low tonnage ratings. Coining presses have small bed areas and high tonnage ratings.

Overloading of the press can cause damage to both the die and the press. Several devices based on the strain-gage principle have been developed for accurately measuring the load on a mechanical press with a given die. Misfeeds or double blanks are common causes of press overloading. Detectors built into the die stop the press before overloading occurs.

The capacity of a mechanical press involves consideration of the frame capacity, drive capacity, flywheel energy, and motor size.

Frame Capacity. The press frame must be able to work at its rating without deflecting beyond predetermined standard limits. For general-purpose applications, the bed deflection should not exceed 0.17 mm/m (0.002 in./ft) between tie rod centers (or per foot of left-to-right bed dimension on presses without tie rods) when the rated load is evenly distributed over the middle 60% of the distance between tie rod centers (or of the left-to-right bed length). Slide deflection should not exceed 0.17 mm/m (0.002 in./ft) between pitman centers when the rated load is evenly distributed between pitmans. Both bending and shear deflections are considered. These specifications can be revised to suit more precise applications.

Drive capacity is the capacity a mechanical press develops through the gear train and linkage. The capacity can vary because of the mechanical advantage developed by different types of press linkage, and generally is expressed in distance above the bottom of the stroke.

Variation in the capacities of presses with eccentric-gear, crankshaft, or eccentric-shaft drive at any point in the stroke is almost equal. However, the capacity decreases between the bottom of the stroke and mid-stroke. The capacity of both the knuckle lever drive and the rocker arm drive, however, is much less above the point of rating on the stroke than is that of the crank drives, because the knuckle lever drive loses mechanical advantage more rapidly than do crank drives. In addition to loss in capacity, velocity of the slide in the knuckle lever drive and the rocker arm drive is considerably greater at points high above the bottom of the stroke than is the case in the eccentric-gear or crankshaft drives.

Flywheel energy for a given job may be insufficient, although the press frame and shaft may be adequately strong. For a greater working distance or for faster operation, more energy and power must be provided.

Blanking operations are completed in a brief portion of the press cycle. The flywheel instantly supplies practically all of the energy required by its resistance to deceleration. The motor may take the remainder of the press cycle to restore lost energy to the flywheel by bringing it back up to speed. Draw operations may take up to one-fourth of the press cycle.

For intermittent operation, 20% is arbitrarily considered the maximum the flywheel may be slowed when energy from it is being used. For continuous operation, 10% is considered the limit, because of the short time available to restore lost energy. The low-speed torque characteristics of the press drive motor greatly affect the amount the flywheel can be safely slowed, because the ability of the motor to restore lost kinetic energy is a function of these characteristics.

The amount of energy, *E,* available at 10% slowdown can be calculated from the following equation:

$$E = \frac{N^2 D^2 W}{5.25 \times 10^9} \quad \textbf{(Eq 1)}$$

where E is expressed in inch-tons, N is the rotary speed of the flywheel (rpm), D is flywheel diameter (inches), and W is flywheel weight (pounds). The metric version of Eq 1 is:

$$E = \frac{N^2 D^2 W}{6.8 \times 10^6} \quad \textbf{(Eq 2)}$$

where E is given in kilojoules, N remains rpm, D is given in meters, and W is expressed in kilograms.

If calculation indicates that the flywheel will not furnish the necessary energy, it may be necessary to increase the weight, diameter, or speed of the flywheel, or to use different type of drive or motor.

To change the press speed, devices are used to change the speed of the flywheel. The energy of the flywheel is directly proportional to the square of the flywheel speed of rotation. Therefore, the standard energy of a variable-speed press is calculated at its slowest speed. The intended operating speed should be used in checking the suitability of a press for a specific operation.

Motor Selection. The primary function of the main drive motor on most mechanical presses is to restore energy to the flywheel. During flywheel slowdown, most of the energy is derived from the flywheel, with some contribution from the motor. Following the working stroke, the motor must restore the energy expended by the flywheel while returning the wheel to speed. If the capacity of the press is not adequate to satisfy the operation, slowdown of the flywheel during the working portion of the stroke becomes excessive, resulting in overloading of the motor. Stoppage of the press can occur if there is insufficient time between production strokes to permit recovery of flywheel speed.

When slowdown is rapid and the working stroke is long, it is desirable to use a high starting torque with relatively low starting current, good heat-dissipating capacity, and sufficient slip (slowdown). The slip designation of a motor is the amount of motor slowdown at rated full-load torque. Under conditions of moderate slowdown and short working strokes, general-purpose motors can be satisfactorily used.

Many presses employ alternating current (ac) induction motors having slip ratings of 3 to 5%, 5 to 8%, or 8 to 13%, the choice depending upon flywheel design, press speed, and other parameters. For short-stroke presses operating at speeds above 40 strokes/min, general-purpose motors with a slip rating of 3 to 5% are often satisfactory. Motors with a slip rating of 5 to 8% are often used for press speeds of 20 to 40 strokes/min. For long-stroke presses operating at speeds less than 20 strokes/min high-slip (8 to 13%) motors are usually required.

Clutches and Brakes in Mechanical Presses

Both clutches and brakes are essential to the operation of mechanical presses. No other part must work more perfectly if the press is to operate successfully. The clutch must deliver and control the surge of force that is required to shape the work metal. When the press runs continuously, the clutch transmits power from the flywheel to the main shaft. In a single-stroke press, the clutch must accelerate the rotating parts of the drive from stop to full speed at each stroke of the press. The brake must decelerate this moving mass in order to stop the slide at the end of each upstroke. The brake must be large and efficient enough to stop the press in an emergency, or during inching.

Clutches and brakes in presses that are stopped at the end of each stroke need more maintenance than do those in presses operating continuously or that are stopped only a few times a day.

Positive clutches are mainly used on presses of less than 900 kN (100 tonf) capacity. Positive clutches always are on the main shaft and use pins, keys, or jaws to lock the shaft and flywheel together. They usually are engaged by a foot treadle or an air cylinder.

Positive clutches can be engaged or disengaged only once during each press stroke. Usually a throw-out cam disengages the keys, pins, or jaws near the top of the stroke. These clutches can be arranged for one-stroke or continuous operation.

Positive clutches accelerate the slide very rapidly, because there is no slip. Because they are shaft mounted, they have a minimum mass to move.

Mechanical positive clutches (Fig. 7) cost less than other types and are compact and easy to operate, but are limited in many respects and usually require excessive maintenance. They are not recommended for one-stroke work, because wear on the clutch would be severe.

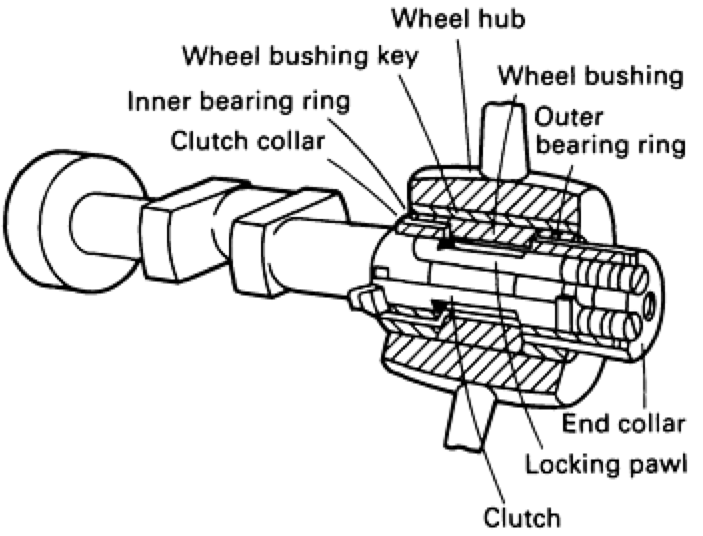

Fig. 7 Mechanically actuated positive clutch mounted on press crankshaft.

Pneumatic positive clutches are much more efficient than their mechanical counterparts. A pneumatic positive clutch is usually a jaw clutch with 16 or more points of engagement. The jaws are engaged by an air cylinder or a diaphragm and are disengaged by springs. No throw-out cams are used. The clutches have electric controls like those used with friction clutches. A press with a pneumatic positive clutch can be used for a single stroke, operated continuously, jogged in either direction, and stopped for emergencies. The brake used with this type of clutch is usually spring operated and air released as a fail-safe measure.

Friction clutches are preferred to positive clutches for most press applications. They are mounted on the crankshaft, eccentric shaft, intermediate gear shaft, or drive shaft, as shown in Fig. 2. The location of the clutch brake unit is determined by factors such as press size, press speed, type of clutch brake unit, and inertia of the press drive.

Friction clutches allow the slides to be stopped or started at any point in the stroke. This makes setting and adjusting dies convenient, especially in large presses. With a well-designed air friction clutch, sudden power failure causes the press to stop immediately. Friction clutches using high-pressure oil or magnetic attraction have been employed to some degree.

Most friction clutches are air engaged and spring released. Brakes are spring engaged and air released. Some clutches are combined with integral brakes; others are constructed as individual units for separate mounting. The brakes for presses equipped with friction clutches are a drum type or single- or multiple-disk type and are operated in unison with the clutches. Some designs use diaphragms or air tubes for actuation instead of pistons with packings.

One of the greatest advantages of friction clutches is their compatibility with electric and electronic controls. In this respect, they are superior to the positive clutch, with the exception of the pneumatic type.

Eddy Current Clutches. A high degree of control over the press is the major advantage of eddy current clutches, which are, in reality, press drives. Ram speed at any point on the stroke can be programmed.

Eddy current drives consist of a constant-speed flywheel, a variable-speed clutch-and-brake rotor, and a stationary brake field assembly. The clutch-and-brake rotor is directly connected to the press drive shaft.

Clutching and braking are controlled by the current in the coils. In practice, the press drive is usually controlled automatically to speed up the motion of the slide during the idle portion of the stroke, and to slow it down just before the tooling contacts the work.

Presses and Auxiliary Equipment for Forming of Sheet Metal

Press Accessories

The setup and operation of a mechanical press are made more versatile through the use of built-in accessories. Included are bolster plates, rolling bolster assemblies, speed change mechanisms, shut height adjusters, and slide counterbalances.

Bolster plates are used in most hydraulic and mechanical presses, between the bed and the die. They provide a flat surface on which to mount the dies and can be remachined to remove nicks and worn areas. T-slots in the top surface facilitate clamping of the die to the bolster plate. Clearance holes in the bolster plate permit pressure pins to extend from the die cushion to the die. Some press beds have a large hole through the top surface for drop through of parts or for mounting of die cushions. For this reason, bolster plates are thick, to minimize deflection and support the die properly. When parts are ejected through the die, holes of the proper size and location are cut in the bolster.

The width, length, and thickness dimensions of bolster plates have been standardized for each bed size, as have the size and location of T-slots, pressure pin holes, and holes for fastening to the bed. Standardization facilitates interchangeability of dies between presses. Filler plates can be used either above or below the bolster to reduce the shut height of the press. This is in addition to the normal slide adjustment.

Rolling bolster assemblies are made for some large straight-side presses for fast tooling change. Dies are set up on an assembly outside the press. When a press run is finished, the punch and blankholder are undamped from the slides and the assembly is moved out of the press. Then another assembly is moved into place, the punch and blankholder are clamped to the sides, adjustments are made, auxiliary equipment is set up, and the press is made ready to run.

Speed change drives are used mostly to change the number of strokes per minute, but some drives also can be used to change speeds during the press stroke for fast approach to the work, slower working stroke, and quick return. Changing speed during the stroke permits an increase in production without increasing the working speed. Press builders commonly supply charts that show the slide speed at any point on the press stroke.

In simple blanking operations the speed of the working stroke is not critical. In drawing and in some forming operations, the plastic-flow characteristics of the material being formed impose specific speed limitations.

A two-speed drive is combined with clutching and braking in some presses. A clutch can have planetary gears for a two-speed drive. Some two-speed drives have two flywheels with a common brake. With a two-speed gearbox and two speeds from the clutch, a press can have four speeds.

Variable-speed drives may incorporate a speed change belt with adjustable cone pulleys connecting the motor to the flywheel or a steplessly variable electric drive. The eddy current drive, originally developed for inching of mechanical presses, also provides variable speed.

Shut height adjustment is provided in mechanical presses to change the distance between the slide and the bed to fit dies of different sizes. Small, single-point presses have a screw arrangement to provide this adjustment. In heavier presses, a gear drive makes it easier for the operator to move the massive slide. As press size increases, this gear drive is motorized. Motorized slide adjustment is used also in many smaller presses. Air counterbalances on most large presses relieve the load of the slide and die from the adjusting mechanism.

Some presses have dials that indicate the shut height in thousandths of an inch. If the dies to be used in the press are similarly marked, die-setting time will be greatly reduced. Other presses have a motorized adjustment with a dial control. The operator sets the desired shut height, and the slide automatically positions itself.

Counterbalances in press slides provide smooth cycling and reduce backlash and gear wear by:

- Counteracting the moving weight of slides, components, and die members attached to the slides
- Reducing the load on the press brake, thus providing faster stopping
- Taking up clearance on the main bearings, reducing the breakthrough shock for cutting operations
- Reducing backlash in the drive gearing
- Easing the adjustment of slides by reducing the load on the adjusting screws

Excessive counterbalance pressure can prevent the normal breathing of bearings and consequently prevent good lubrication of the bearings.

Most presses manufactured with slide counterbalances use pneumatic cylinders as a means of counterbalance, although springs have been used. To prevent too great an increase of pressure through the full range of press stroke, a surge tank is used in conjunction with the cylinders. The tanks are of such a size that the pressure does not increase more than 20 to 25%. A pressure control valve allows the counterbalance pressure to be adjusted to take care of variation in die weights.

The counterbalance cylinder is attached to the press frame, and the cylinder rod to the press slide. Usually the cylinders are attached to either the crown or the press uprights.

Die Cushions

Die cushions, often referred to as pressure pads, are used to apply pressure to flat blanks for drawing operations. They also serve as knock-out or ejection devices to remove stampings from the dies.

Single-action presses do not have an integral means for blankholding and require the use of cushions or other means of applying uniform pressure to the blanks for drawing operations, except for shallow draws in thick stock. The most common means of pressure control for drawing operations on single-action presses are pneumatic and hydropneumatic die cushions. Figure 8(a) shows a single-action press set up for use with a die cushion.

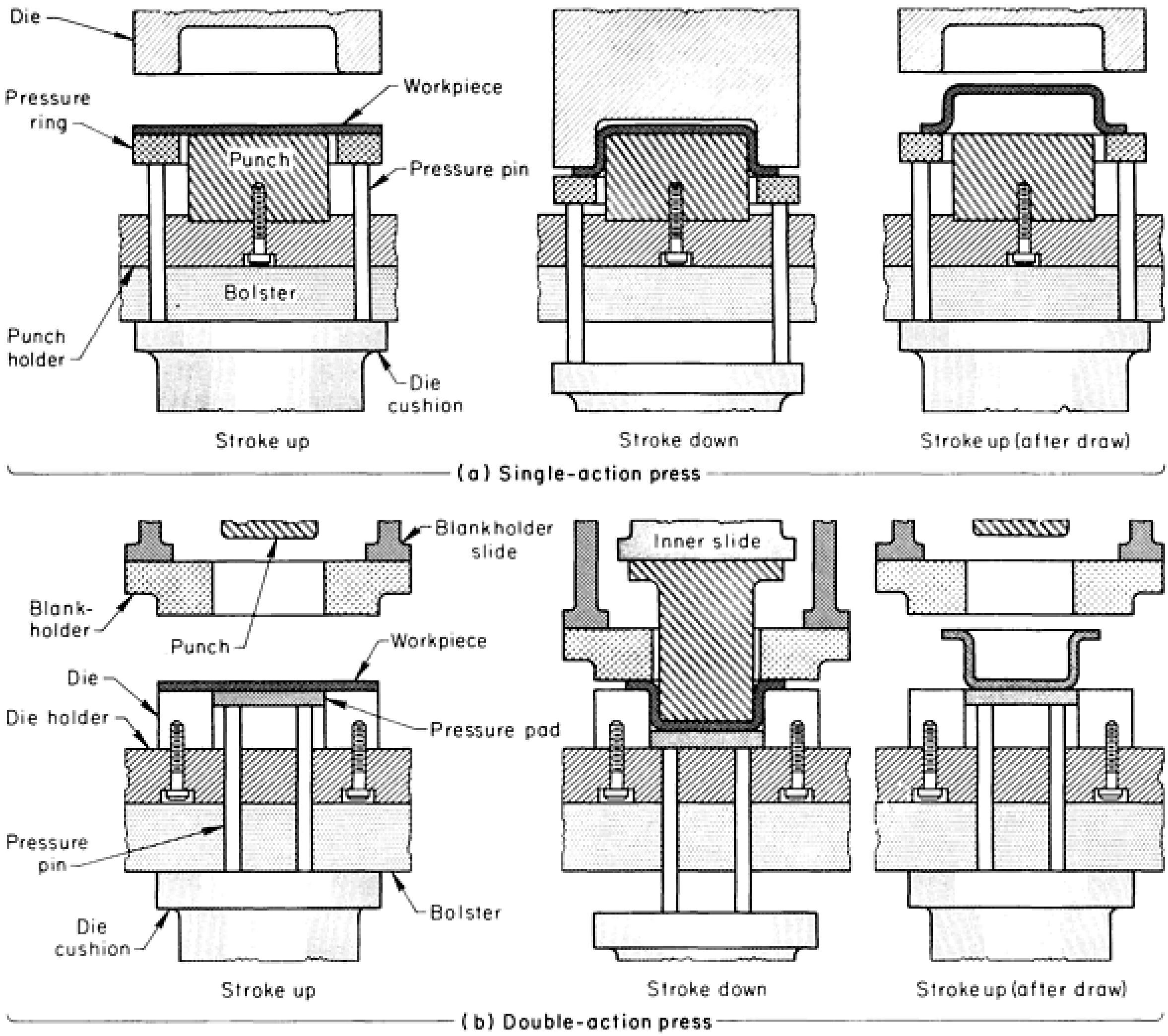

Fig. 8 Setups incorporating die cushions in single-action and double-action presses.

Double- and triple-action presses feature integral blankholders and do not require cushions for drawing operations. Cushions are sometimes employed on double-action presses, however (Fig. 8b), for ejection of triple-action draws, for keeping the bottoms of the stampings flat or ensuring that they hold their shapes, or for preventing slippage while drawing. For such applications, the cushions must be equipped with locking devices to hold the cushions at the bottom of stroke for a predetermined length of the return stroke of the press slide.

Most die cushions are located in the press beds, but there are applications that require installation within or on the press slides. In either case, the functions are similar, and the operations are the same. The recommended capacity of a die cushion (the amount of force it is capable of exerting) is generally about 15 to 20% of the rated press force. Strokes of the cushions are usually one-half the strokes of the press slides, but should not exceed bolster thickness less 13 mm ($\frac{1}{2}$ in.).

Cushions with different strokes and higher capacities are available, but the size of the press bed opening limits the size, type, and capacity of the cushions. Consideration must be given to the press capacity at the point at which the draw is to begin, because the force and energy required to depress the cushion is added to that required to draw the stamping. As a result, the force and energy needed for a high-capacity cushion may not leave enough for the operation to be performed.

In pneumatic cushions, the maximum pressure is controlled by the diameter and number of cylinders and the available air pressure. Shop line pressure is generally used, but it is possible to use a booster or intensifier to increase the air pressure. Most cushions are normally rated at a pressure of 690 kPa (100 psi), and it is generally recommended that the pressure not exceed 1380 kPa (200 psi). Surge tanks, if required, must conform to local codes and are generally approved for a maximum pressure of 860 kPa (125 psi).

A pneumatic die cushion for a single-point press normally uses one cylinder and one piston. Two or more cushions may be stacked, however, when a high-capacity unit is required in a limited bed area in which vertical space is available. For multiple-point presses, when the pressure pad area requirement is too large for one cushion, multiple cushions can be arranged alongside one another. The cushions may be individually adjustable or tied together. A multiple-die cushion is often preferable to a hydropneumatic die cushion because of the speed restrictions of the latter. Presses to be used with progressive dies can be equipped with a cushion whose position may be changed from right to left in the press bed.

Hydropneumatic Cushions. These die cushions are used when higher forces are required or when space does not permit the use of double- or triple-stage cushions. Hydropneumatic cushions are slower acting than the pneumatic cushions; therefore, they are usually used on large presses and on slow presses. They can be adjusted to hold a large, light blank for deep drawing or shallow forming or to grip heavy-gage material as tightly as is required for curved-surface or flat-bottom forming.

A typical hydropneumatic cushion is connected to a surge tank, as shown in Fig. 9. Two individually controlled air lines are required: one connected to the operating valve of the cushion and the other connected to the top of the surge tank. The air pressure supplied to the operating valve determines the capacity of the cushion on the downstroke. The pressure of the air in the surge tank determines the stripping force available on the upstroke. The surge tank may be separate from or integral to the cushion, depending upon the space available beneath the press bed.

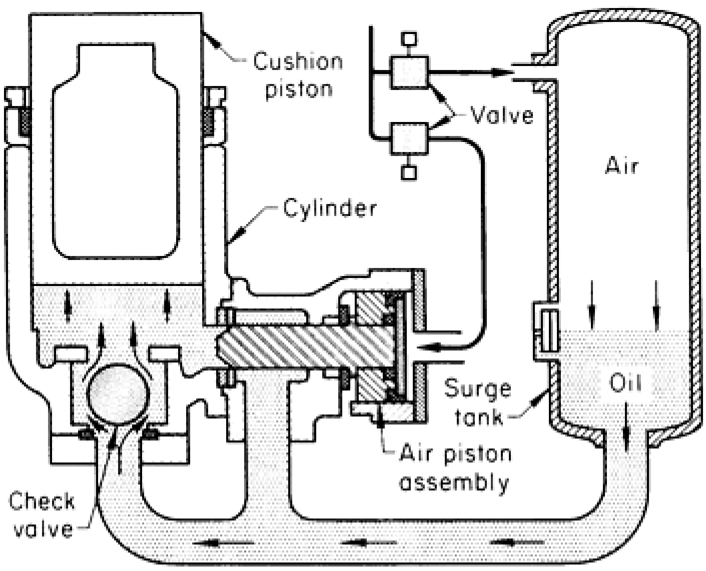

Fig. 9 Components and operating principle of a hydropneumatic die cushion.

The pressure of the air in the surge tank is transmitted to the hydraulic fluid, which is free to pass upward through the check valve and force the cushion piston upward. Pressure is also exerted against the face of the operating valve stem, but it is not sufficient to overcome the opposing air pressure working on the operating air piston.

When a downward force is applied to the cushion, the check valve is immediately closed and the pressure of the fluid that is trapped beneath the piston begins to rise. When the pressure against the small face of the operating valve stem reaches a predetermined point, it exceeds the magnitude of the air pressure on the larger area of the air piston and opens the operating valve. As long as the cushion piston continues its downward movement, the fluid beneath it is maintained under constant pressure by the throttling action of the operating valve; the additional fluid replaced by the piston is forced through the valve to the surge tank. Oil pressures are generally limited to about 6.9 MPa (1 ksi).

When the stroke has been completed and the downward force on the cushion piston is removed, the pressure of the fluid beneath the piston is immediately lessened, reducing the air pressure on the air piston and thereby closing the operating valve. Fluid from the surge tank under pressure from the air behind it passes upward through the check valve and raises the cushion piston to top stroke.

Auxiliary Equipment

Most primary press operations are automated, so that equipment for feeding and unloading is used even for fairly short runs. Hand feeding, with its attendant hazards, is often confined to secondary operations on partly completed workpieces. Goals for planning automated operations should include:

- Maximum safety to the operator and to the equipment
- High or nearly continuous production
- Improved quality of the product and minimum scrap
- Reduction in cost of the finished parts

The shape and position of the part before and after each operation must be carefully studied to determine whether design changes, such as adding tabs or extra stock to the blank, will facilitate handling.

Automatic handling equipment can be divided into the following categories: feeding equipment, unloading equipment, and transfer equipment.

Coil-handling equipment moves coiled stock to the press area and uncoils it with a minimum of damage to the stock and danger to the tools and operator. Reliable coil handling is important, because coil stock is being increasingly used to supply material to presses.

Other auxiliary equipment discussed in this section includes lubricant applicators, straighteners, and revelers.

Press Feeds

Mechanical feeds are important for high production, combined operations, and operator and press safety. Some feeds supply the presses with stock from a strip or coil; others feed blanks or partly completed workpieces. Either kind, with or without auxiliary hand feeding, can be used with almost any kind of press. For progressive-die work, the feed length should be accurate and should repeat within ±0.076 mm (±0.003 in.). The stock must advance accurately so that the pilot pin can easily enter the piloting hole and position the strip. Too great a variation in feed length could result in distorted pilot holes and scrap parts.

Feeds for coil stock feed the work metal from a coil to the press. Choosing the optimum type of feed depends mainly on the type of press, strokes per minute, length of feed per stroke, accuracy needed, and the kind of strip (width, thickness, stiffness, and surface condition). The two most common kinds of feeds are slide and roll feeds.

Slide feeds are made in a variety of sizes and capacities. The basic principle of a slide feed is the use of a feed block that is moved between positive stops to advance the material the distance required at each stroke. Slide feeds are very accurate and are particularly suitable for use with coil stock. When strip stock is used, the ends of the strip must be hand fed into the press.

Some slide feeds are powered by the press through an eccentric mounted on the crankshaft extension (Fig. 10). The eccentric can be a simple one-piece unit keyed to the crankshaft, or it can be adjustable to vary the feed in relation to the rotation of the crankshaft. When changes in feed length are frequent, the adjustable type is usually warranted.

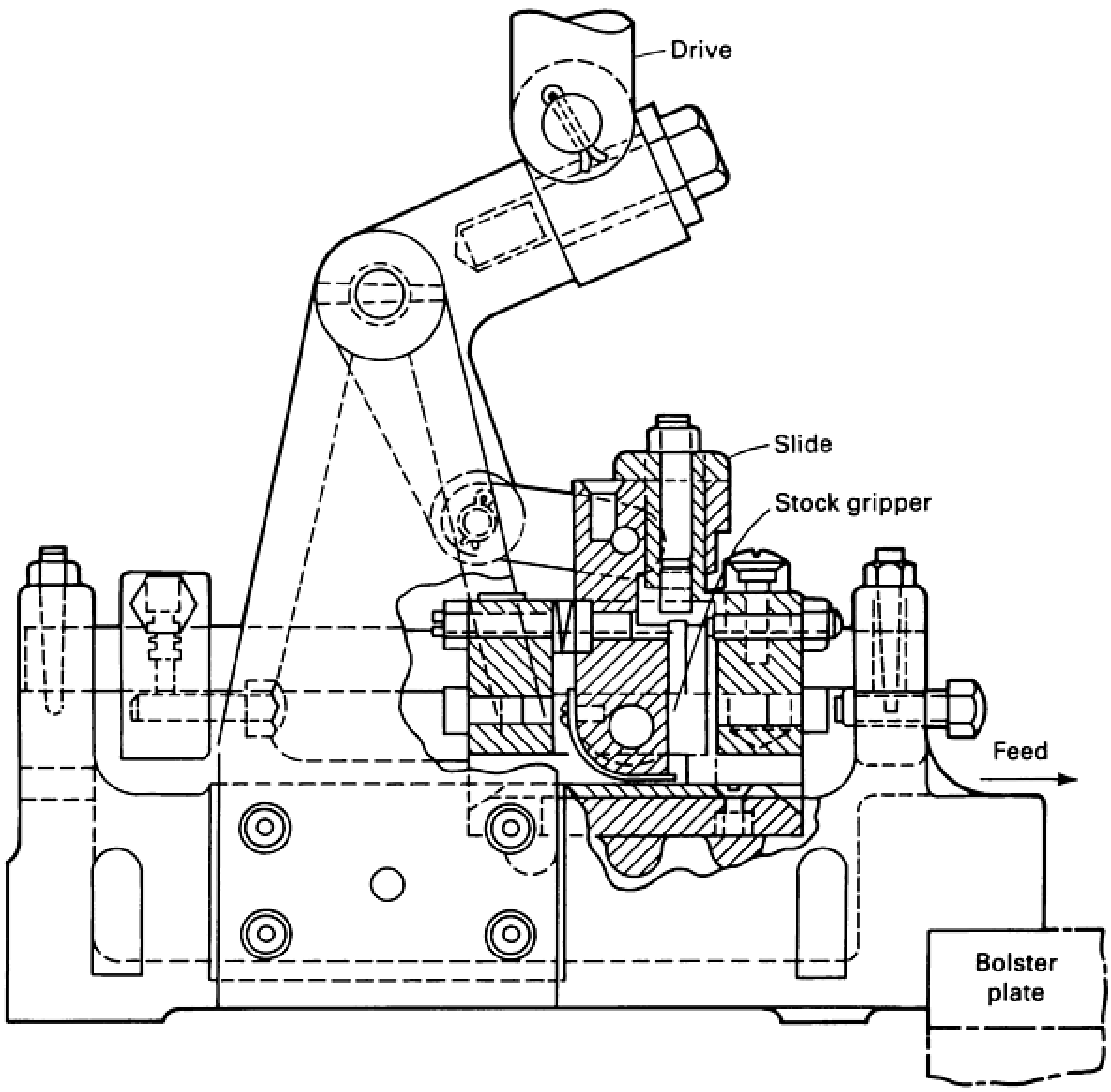

Fig. 10 Press-driven slide feed.

The feed block is mounted on hardened slides and either has a feed-blade holder with an adjustable feed blade (usually carbide-tipped) or a pair of eccentric gripping cylinders. The material is gripped during the feed stroke and released on the return stroke. Accurate control of feed length is obtained by the use of adjustable stops.

The direction of feed--left to right, right to left, or front to back--is governed by the location of the crankshaft extension on the press and the arrangement of the die. A mechanical slide feed, feeding from left to right on a press with a front-to-back crankshaft, can be provided by using an appropriate linkage. Slide feeds also can be powered by air cylinders or cams on the press slide.

Roll feeds are available in sizes suitable for use with almost any width and thickness of stock and are used in every type of presswork, from blanking to complex operations in progressive dies.

A roll feed essentially consists of a pair of rolls that can turn in one direction only. The rolls exert pressure on the stock by the use of springs or some other device and are rotated by the motion of the press crankshaft.

Roll feeds are suitable for extremely thin material and material with highly polished surfaces. If hard chromium-plated rolls are substituted for standard ground-steel rolls, polished surfaces will not be scored or marked during feeding. Rubber-coated or plastic-coated rolls can be used on soft finished or prepainted stock.

There are two advantages to using roll feeds for feeding thin stock. With patterned rolls, a flange can be formed on a waste edge of the stock as a stiffener. With a single-roll feed, the stock usually is pulled through the die.

The best method of feeding extra-thin stock is the use of double-roll feeds (Fig. 11), in which roll feeds at each side of the die are set so that the stock between them is always under slight tension. Double-roll feeds eliminate manual feeding of end sections when strip stock is processed, and are suitable only when a substantial scrap skeleton remains.

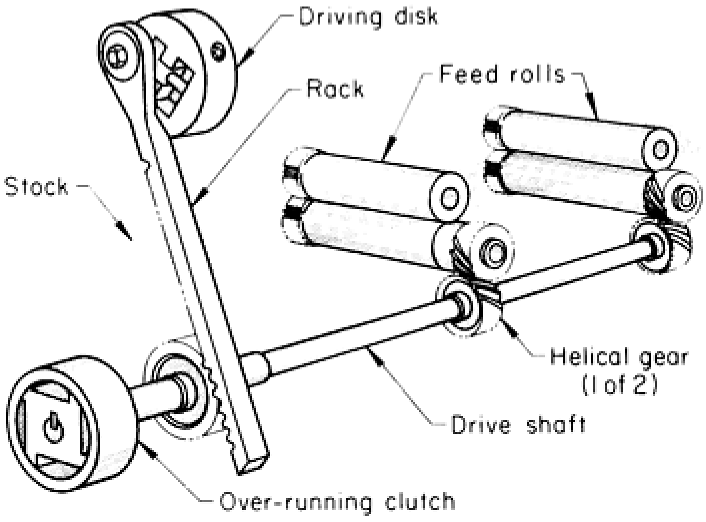

Fig. 11 Components of a double-roll feed.

Rack-and-pinion-actuated roll feeds are available in almost all sizes but are used most in relatively heavy stamping and drawing operations. In larger presses it is common to use double-roll feeds of the rack-and-pinion type that are attached to the press bolster.

Feeding of blanks or previously formed stampings to presses is accomplished in several ways. Selection of a specific method depends upon many factors, including safety considerations, production requirements, and cost.

Manual Feeding. Feeding of blanks or stampings by hand is still a common practice, but this method is generally limited to low-production requirements which do not warrant the cost of automatic or semiautomatic feeds. Manual feeding, however, requires the use of a guard or, if a guard is impossible, hand-feeding tools and a point-of-operation safety device. The use of tools and a safety device eliminates the need for the operator to place hands or fingers within the point of operation and safeguards the operator who inadvertently reaches into the point of operation (see the section "Press Safety" in this article).

Chute Feeds. Simple low-cost chutes are often used for feeding small parts, with the blanks or stampings generally sliding by gravity along skid rails in the bottoms of the chutes. Side members guide the workpieces, and rollers are sometimes added to facilitate sliding. Production rates to 1800 parts per hour are not uncommon for gravity chute feeds.

Blanks or stampings are generally placed in the inclined chutes manually, but the setup can be automated by using hoppers, prestacked magazines, or other means to supply the chutes. Windows are provided at the point at which the workpieces enter the chutes when proper orientation is required.

Push feeds are used when blanks must be oriented in a specific relation to the die, or when irregularly shaped parts are fed that do not slide down a chute and orient themselves properly in the die nest. Workpieces can be manually placed in a nest in a slide, one at a time, and the slide pushed until the piece falls into the die nest. An interlock is generally provided so that the press cannot be operated until the slide has correctly located the part in the die. Slide length should be sufficient to allow placement of workpieces in the pusher slide nest outside a barrier guard enclosure. Strippers, knockouts, or air can be used to eject finished parts from the die. In some cases, holes can be provided in the bottom plates of the slides through which finished pieces fall on the return stroke of the pusher.

Transfer Feeds. In some automated installations, blanks are lifted one at a time from stacks by vacuum or suction cups and moved to the die by transfer units. Separation of the top blank from a stack is usually done magnetically, pneumatically, or mechanically. The top level of a stack can be controlled by a height detection system that regulates a stack-elevating cylinder. Two or more stacks can be arranged to be automatically moved into the elevating station when the previous stack has been used up.

Dial feeds are another method of feeding secondary operations that is being increasingly applied because of improved safety provisions and increased productivity. Such feeds consist of rotary indexing tables having nests or fixtures for holding workpieces as they are carried to the press tooling. Parts can be placed in the nests or fixtures at the loading station (away from the point of operation) either manually or by other means, such as with the use of hoppers, chutes, magazines, vibratory feeders, or robots. Dial feeds can be built into or added to presses.

Industrial robots are being used extensively for press loading and other industrial applications. These mechanical arms, manipulators, or universal transfer and positioning units are more sophisticated versions of the mechanical hands or swinging arms long used for press loading and unloading (see the section "Press Unloading" in this article). The main difference between these devices and true robots is that true robots can be programmed to perform different operations. Various types of tooling can be attached to the arms to handle different sizes and shapes of workpieces. Not only do such units increase safety, but they also substantially boost production rates. Robots are particularly suitable for low-volume production requirements and for operations in which there are large differences in the size and geometry of the workpieces to be handled.

Press Unloading

Methods used to unload stampings from presses vary depending upon workpiece size, weight, and geometry; production requirements; material from which the stamping is made; press and die design; surface quality requirements; and safety considerations.

Gravity and Air Ejection. Gravity is the simplest and least expensive method of unloading presses, but it is not applicable for many operations. In some cases, dies can be designed so that the stampings fall through a hole in the press bed. The use of open-back inclinable presses facilitates unloading by means of gravity when there are no holes in the beds; stampings fall out of the open backs of the presses. When press inclination is not practical, chutes are sometimes provided to carry the stampings away. Air ejection is still common for light-weight parts but is expensive and noisy.

Kickers, Lifters, and Shuttle Extractors. Kickers consist of pivoted levers, generally air actuated, that are mounted in the dies and throw stampings out of the dies when the dies open. Lifters are similar devices, but simply move vertically and require other means for stamping ejection. Pan shuttle-type extractors swing to and from the die area, catching stampings as they are stripped from the punches or upper dies and dropping them outside the presses. Actuation of the pans can be from either the press rams or the independent drives.

Mechanical hands, often called iron hands, are actuated by air or electrical mechanisms commonly used to remove stampings from presses. Gripping fingers or jaws are mounted on arms that swing or reciprocate into the die area to lift the stampings and place them on a mechanism for transfer to the next press or operation site. Standard units are available as swing arm or straight-path types.

Interchangeable jaws or fingers are designed to grip the flanges of stampings. Vacuum cups or electromagnetic elements are used in place of jaws or fingers for curved surfaces and fragile or easily damaged workpieces.

Industrial robots, discussed previously in this article, are also used for press unloading. An important advantage of robots is their programmability to suit various workpieces and requirements.

Transfer Equipment

Several methods are used to automatically transfer stampings from press to press for high-production requirements. When applicable, the use of chutes on which the stampings slide provides the lowest-cost method. Power-driven slat or belt conveyors are commonly used. Adjustable-speed drives for the conveyors are often desirable to accommodate various cycle times.

Shuttle-type transfer devices are extensively used. With some units, the stampings are pushed by reciprocating fingers that extend and retract as required; other units use the lift-and-carry (walking-beam) method. Shuttle units are driven by hydraulic, pneumatic, or electric power, or they are driven mechanically from the press. Adjustable side rails are often provided to accommodate workpieces having different widths.

Lift-and-Carry Devices. One lift-and-carry device, which employs a parallelogram motion, is illustrated in Fig. 12. Two rails move into slots milled in a die, rise vertically to lift a stamping from the die, retract and lower to deposit the stamping on a set of idle rails, and return to pick up the next stamping. Each time the presses cycle, the stampings are progressively moved from one press to the next. This type of transfer unit maintains full control of the stampings, from unloading them from one die to loading them into the next die.

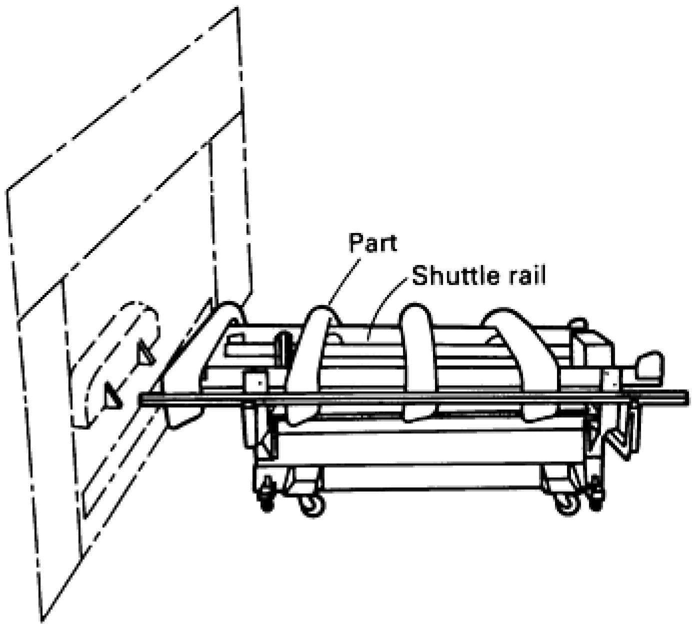

Fig. 12 Lift-and-carry transfer device for stampings.

Turnover or turnaround devices are sometimes added to transfer systems in order to change the positions of the stampings as they pass from one press to another. Turnaround devices generally consist of turntables that lift the stampings, rotate them the required amount, and lower them onto the transfer system. Turnover devices often have one arm and use one or more vacuum cups. In operation, the stamping is transferred to a position above the arm, the arm is raised, the cup or cups engage the stamping, vacuum pulls the stamping against the cups, and the arm rotates about 180°. At the end of the arm movement, the vacuum is released, and the arm returns to its horizontal position. Other devices are of the Ferris wheel type.

Industrial robots, electrically interlocked to two or more presses, are also being used for the automatic unloading, transferring, and loading of stampings. Advantages include increased flexibility, with programmability permitting different stampings to be produced over the same press line.

Computer Numerical Control (CNC). Transfer systems controlled by programmable CNC units are available for automating press lines. Such systems are independent of the presses, can be adapted to stampings of all sizes, and are easily reset. The transfer slides have integral drives, transfer level is programmable in three coordinates, and transfer rates can be varied along certain sections. Grippers or suction cups are used to handle the stampings. Modular construction of the CNC transfer units permits their use with conveyor belts, buffer storage devices, and turnover or turnaround units.

Stackers or conveyor loaders are often provided at the ends of the lines to stack or remove finished stampings that are unloaded from the last press. Low-profile under-the-die conveyors are used for some applications.

Coil-Handling Equipment

Coil cradles, reels, uncoilers, re-coilers, and other types of coil-handling equipment are important to the successful operation of a press.

Coil cradles may be either nonpowered or powered. In the nonpowered type, the stock is pulled from the coil by a powered feed, a straightener, or pinch rolls, or by the equipment being fed.

A powered cradle is preferable for coils that weigh more than 900 kg (2000 lb) or when stock is going directly from the reel to the press feed. In a powered cradle, the coil is supported by chain-driven or gear-driven rolls or by a driven conveyor belt. The drive should be automatically self-equalizing to prevent skidding of the coil.

Coil cradles should have motors that can stand frequent starting of inert loads. A slack loop is created between the coil and the straightener or feed devices by starting and stopping the motor intermittently on signal from a dancer roll, paddle, or other control device. This intermittent operation may cause a standard motor to fail prematurely. With a variable time delay (electronic or adjustable-cam), the motor can overrun to a controllable extent after the control has commanded it to stop.

A variable-speed drive reduces the number of starts and stops, prolongs the life of the motor and drive, and often makes it possible to match the speed of the cradle to that of the machine being fed. A clutch can be used so that the motor will run continuously and the slat conveyor or rolls are driven only when stock is required.

Stock Reels and Uncoilers. Commercial stock reels can accept coils weighing as much as 22,700 kg (50,000 lb). There are reels of the proper size and type for almost any pressworking application.

Selection of a reel should be based on the maximum coil weight and the widths of stock to be unwound. It is better to overestimate future requirements than to underestimate them and find out later that reel capacity limits improvement in equipment and production methods.

Plain or nonpowered reels are usually adequate when the press feed or stock straightener has pinch rolls with enough gripping power to pull the stock from the reel. When stock is going directly from reel to press feed, the reel should be powered so that the feed does not have the job of both feeding the press and unwinding the coil. If the stock becomes taut between the reel and the feed, the feed may start to advance and the stock may slip, resulting in a short feed length. If a straightener is used between the press feed and the reel, a plain reel can be used. However, materials with low tensile strength and lightweight materials should be unwound from a powered reel; otherwise, they might be stretched between the reel and the feeding device.

Powered reels with variable speed and a loop control are preferred for a smooth operation. Noncontact sensor units, such as photoelectric cells or proximity switches, on the loop control should be used for soft metals, polished surfaces, and prepainted stock. These prevent damage inherent with contact-type (rolling or sliding) sensor units. Without powered reels or loop control a sudden pull can cause the stock feed to slip and mark the work metal.

Other equipment useful for handling coil stock includes re-coilers, turnstiles, down-layers, coil cars, coil grabs, and coil ramps.

Re-coilers are used for winding coil stock after slitting and for winding the scrap skeleton after pressworking.

Turnstiles (or horns) are two-arm or three-arm devices used to store coils temporarily before processing. In function, a turnstile resembles a coil ramp. Turnstiles may be equipped with hydraulic push-off devices, which add to their speed and efficiency.

Downlayers, sometimes called up-enders, are turnover devices for rotating the coil from a horizontal to a vertical position.

Coil grabs, for use with cranes, are devices that can handle stock in the horizontal or vertical position. Some similar devices are available for use with forklift trucks. Other devices will pick up a coil and change the position from horizontal to vertical.

Coil ramps are inclined storage units for use with reels or cradles. Most coil ramps operate by gravity.

Other Auxiliary Equipment

Lubricant Applicators. In blanking or forming, a lubricant is usually applied to metal that is fed into the press from coils. The lubricant can be swabbed or brushed onto the metal as it leaves the reel, but this is inefficient and wasteful and produces inconsistent results. An automatic applicator improves efficiency and uniformity. The type of applicator used depends on whether the lubricant is a powder or a liquid and, if a liquid, on its viscosity and flow characteristics. Roller coating, drip feeding, and spraying are common. Information on these application methods, as well as on the types of lubricants used, is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Straighteners have upper and lower rolls alternately mounted in a staggered position. The minimum number of rolls that can be used is three; however, five-roll or seven-roll straighteners are most common for the usual range of stock thickness. Straightening of stock less than 0.51 mm (0.020 in.) thick requires additional rolls; as many as 17 have been used for some thin stock.

Some straighteners have a separate screw adjustment for each of the upper rolls; others have one adjustment for the entire series of upper rolls. A straightener should not be overloaded. When stiff, thick metal is passed through a straightener designed for thin metal, it may deflect the rolls permanently or break their shafts. Stiff, thick stock requires larger, stronger rolls spaced well apart. Thin metal requires more straightening rolls than does thick metal. These rolls are usually smaller in diameter and more closely spaced.

Stock straighteners are available in a wide range of capacities and speeds, with either powered or nonpowered straightening rolls. Either the upper or lower set of rolls, or sometimes both sets, is powered. Nonpowered rolls can be used when there is enough pulling or pushing force to get the stock through the rolls. Powered pinch rolls are used to push or pull the stock through the straightener.

Thin stock requires more working to straighten than does thick stock. For this reason, two sets of pinch rolls are used, and all straightening rolls are power driven. The speed of powered straighteners can be adjusted so that the material is delivered by the rolls at the rate it is fed into the press plus 10%. The ideal condition is to have the stock run through the rolls continuously, so that there are no breaks or bends in the stock when it is stopped on the rolls. When straighteners are operated intermittently, breaks or bends occur in the stock and are almost impossible to remove.

Roller levelers, like straighteners, have staggered pairs of meshing rolls, but the rolls are smaller and more closely spaced. All of the rolls are powered, and some of the upper and lower working rolls have backup rolls. Levelers with backup rolls can impose strains on the metal to remove stack edges or a crowned center. More information on the use of roller revelers is available in the article "Slitting and Shearing of Coiled Sheet and Strip" in this Volume.

Presses for High Production

Mass-produced parts are often formed in presses that are made especially for high-production operation. High speed, or the highest number of strokes per minute, is not the only factor in a high production rate. The capability of a press to run continuously for several hours without full operator attention and with a minimum of wear and vibration contributes more to high productivity than does running at high speed for a short period and then stopping for reconditioning of dies.

The more common types of high-production presses are discussed in this section.

Dieing machines, also known as die presses, are set up with conventional progressive dies for long-run operation. These machines are used extensively for the blanking of laminations; however, drawing and forming can be done. The height of the bed above the floor makes it easy to install stacking chutes for laminations and other parts.

Dieing machines are single-action underdrive presses. The drive mechanism for a dieing machine is located beneath the press bed. Four guide rods from a guided lower crosshead pass up through bronze bushings in the bed and are fastened to a platen to which the upper die half is attached. The lower crosshead is reciprocated by a crank-shaft through connecting rods. By this action, the die halves are pulled together, rather than pushed together, as in a conventional press.

The size of the guide rods and bushings results in excellent die alignment and long die life. The underdrive construction keeps the center of gravity of the press low. The progressive dies mounted in the machine are near eye level, and there are no columns or side members to obstruct the operator's view. Ejection chutes for finished parts and scrap are comparatively high above the floor so that containers are easy to position. Pneumatic cushions, fastened to the top of the platen for better accessibility for service and adjustment, are used as strippers and blankholders.

Stock is fed through the guide rods with either single- or double-roll feeders. A scrap cutter can be mounted on the end of the machine. Both devices are operated by the upper platen or by a power take-off on the end of the crankshaft.

Multiple-slide machines are fully automatic machines for mass production of small parts from metal strip or wire in coil form. Detailed information on these machines is given in the article "Forming of Steel Strip in Multiple-Slide Machines" in this Volume.

Transfer Presses. Performing multiple operations on a single press can increase productivity and decrease costs. Transfer presses eliminate the need for secondary operations; annealing requirements between operations; and in-process inspection, storage, and handling of workpieces.

Transfer presses should be considered whenever 4000 or more identical stampings requiring three or more operations are needed daily. A total production run of 30,000 to 50,000 identical parts is generally economical between tooling changes. Most transfer presses are designed to make more than one part, and they are often used for families of parts that are similar in size, shape, and thickness. One press is being used to produce 22 different parts.

Stampings are being produced in a wide range of sizes and shapes. Any configuration that can be grasped by mechanical fingers is suitable, and the parts do not have to be concentric. Practically any operation that can be done in any other press can be performed on transfer presses. Typical operations include blanking, piercing, forming, trimming, drawing, flanging, embossing, and coining.

Major users of transfer presses are the automotive and appliance industries. Automotive parts produced on these presses include wheel covers, taillight assemblies, control and suspension arms, transmission parts, catalytic converters, and timing-gear case covers. Appliance components include refrigerator, freezer, washer, and dryer parts.

Fine Blanking Presses. The fine blanking process, discussed in the article "Fine Edge Blanking and Piercing" in this Volume, is generally performed in special triple-action presses designed specifically for the purpose. The presses are available in a range of sizes varying in capacity from 220 to 22,000 kN (25 to 2500 tonf) or more.

Basic components of most fine blanking presses are the frame, upper and lower tables for supporting the tooling, a power system, a stock feeder and lubricator, a control system, and a tool safety device. The frames are generally of welded plate construction, with four-column or double-frame web design, but some smaller presses have single-casting frames. Most fine blanking presses are designed for vertical operation of the ram, but horizontal presses are available. On vertical presses, ram movement for shearing is usually upward, but some presses have a downward movement.

Flexible-Die Forming Presses. Forming, and sometimes blanking, with flexible dies (rubber pads or diaphragms backed by oil under high pressure) is an economical method because it requires only half a die, and materials of different thicknesses can be formed with the same tool. Also, one pad or diaphragm can be used to produce different workpieces, thereby reducing tooling costs. No scratch marks are produced on the side of the blank facing the flexible die.

Another advantage of flexible-die forming is that localized stress concentrations are avoided because of the uniformly distributed pressure achieved with a rubber pad or diaphragm and the gradual wrapping of the blank around the tool. A limitation is that the process is slower than forming with mating die halves, thus sometimes restricting applications to low-volume requirements. However, depending upon workpiece complexity and size, the method may be competitive for part production runs to 20,000.

Flexible-die forming is used extensively by the aircraft and aerospace industries, as well as by other manufacturers with low-volume requirements. The three major types of flexible-die forming are rubber pad, fluid cell, and fluid forming, all of which are performed on either standard or special hydraulic presses. These processes are discussed in the articles "Rubber-Pad Forming" and "Deep Drawing" in this Volume.

Press Safety

The safest press is one operating continuously with a stock feeder and part unloader. This type of machine does not require the full attention of an operator, and there is no need for him to reach into the danger area. Flywheels, gears, and other moving parts likely to catch an operator or passerby are usually covered.

For hand feeding, shields should be interlocked with press controls, so that the press will not run unless the shields are in place. The best practice is to make the guard or shield a part of the die, so that protection is automatically in place when the setup is made or installed. Shields also can be attached to the press frame and adjusted for various kinds of work. These guards should suit all the work done in the press, should be easy for the setup man to adjust, and should give the operator an unobstructed view.

Usually, it is more difficult to guard hand-fed secondary operations, because the workpiece requires special handling. However, if production rate and quantity warrant the expenditure, standard or special devices can replace hand feeding of presses.

Available safeguards include barriers or interlocking guards that keep the operator away from danger, sweep and pulling devices that push the operator's hands away, and devices that require both hands to trip the press. All safeguards should be inspected and adjusted before and after every press run.

Important considerations in choosing safety devices are: number of operators at the press, size and type of press, size and shape of workpieces, length of press stroke, and number of strokes per minute. Protective devices cannot do the job by themselves; they should be used with a well-planned and strictly enforced safety program. More information on press safety is available in the Selected References.

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Introduction

PRESS FORMING is a process in which sheet metal is made to conform to the contours of a die and punch--largely by bending or moderate stretching, or both (see the Section "Forming Processes for Sheet, Strip, and Plate" in this Volume). The suitability of a tool material for a press forming die is determined by the number of parts that can be produced using that die. This number is in turn influenced by such process variables as size of the part, type and thickness of the metal being formed, lubrication practice, quantity of parts needed, and the allowable variation in dimensions.

Process Variables

Part Size. For small stampings, cast or plastic dies are uneconomical unless they are made from a model already available and with only minor finishing operations required on the dies. When the cost of patternmaking is included, cast or plastic dies are usually more expensive than are dies machined from other materials. The cost of the die material is usually a small fraction of the total cost of dies for a small part, and the availability of material in such a size that would minimize machining on the dies is usually a greater factor in cost than is any other.

As the size of the part increases, cost savings resulting from minimizing machining by the use of a casting close to final size more than offsets the cost of a pattern. However, tool steel or carbide inserts must be used on high-production dies subject to severe wear and galling. The selection of both the material and the locations of the inserts should be conservative when it is important that production not be interrupted to alter the tooling. If tools can be taken out of production, gray cast iron dies may be used, with the wear surface flame hardened and inserts added later if needed because of wear on the critical surfaces.

Work Metal. High-hardness sheet metals wear dies more rapidly than do softer materials, but other factors, such as the presence of scale on the surface of hot-rolled unpickled steels, cause two to five times more wear. However, scaled surfaces cause less galling, which, on tool materials, may be an even more serious condition than wear, because galling or "pickup" on a die causes frequent interruptions of production forming for reconditioning of the die.

Soft brass and aluminum cause less wear and galling than does carbon steel; stainless steels and heat-resistant alloys cause more wear and galling. When galling is anticipated, it is desirable to use materials such as D2 tool steel that can be treated subsequently, if necessary, to eliminate the difficulty. Possible treatments include chromium plating of any hardened steel, the hardening of alloy cast iron, and the nitriding of tool steels such as A2 and D2, which are preferred for nitriding because of the presence of nitride formers such as chromium and molybdenum.

Sheet Thickness. Thick sheets of any metal exert greater pressure on the dies than do thin sheets of the same metal. Both abrasive wear and adhesion (galling) increase with increasing sheet thickness.

Lubrication Practice. In making parts at low and medium production (up to 10,000 pieces), it is often economical to use lubricants. Lubrication is required when zinc alloy dies are used. However, the most effective lubricants are difficult to apply and remove, and they add significantly to cost. Efficient application of lubricants is particularly difficult in high-production operations in which presses are automatically fed. In such operations, it is often economical to use die metals that are more costly but more resistant to galling in combination with the usually less-effective lubricants that can be applied automatically. Examples of these materials are aluminum bronze, nitrided D2 tool steel, and carbide, which often can be used for forming low-carbon steel with only mill-oil lubrication.

More information on lubricants is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Quantity. The number of parts to be produced is an important factor in material selection for large dies in which the cost of material is equal to or greater than the cost of machining. In smaller dies, the difference between the cost of the most expensive and the cheapest steels is less important than the assurance of long life without the necessity for rebuilding tools if the quantity should be increased above original expectations, or if the die material should prove to be inadequate. However, for large dies, both the choice of tool material and the design of the dies depend on the number of parts to be produced, particularly if it is more than 1000.

Adjustable inserts are often impractical for small dies. Therefore, for high-production dies working under severe wear conditions and producing parts to close tolerances, it is often desirable to use a complete insert or to make the die of wear-resistant material such as carbide or nitrided D2 tool steel.

Dimensional requirements of a part may have an important effect on the choice of tool material when the part is to be finished without restriking. If the part is to be restruck, the material used in the restriking die is of less importance, because it will usually be subjected to less wear than will the die that performs the primary operation. A major factor in the choice between a wear-resistant material and a less costly and less wear-resistant material is the necessity for maintenance during the production run.

Materials, Die Wear, and Die Life

Table 1 lists the nominal compositions of the tool materials most often used for press forming dies. Tool materials are usually selected on the basis of providing adequate die life at minimum cost. However, the final choice often depends on availability rather than on a small difference in die life or cost.

Table 1 Tool materials commonly used for press forming dies

Material	Nominal composition
AISI tool steels	
W1	**Fe-1.0C**
S1	**Fe-0.50C-1.5Cr-2.5W**

O1	**Fe-0.9C-1Mn-0.5Cr-0.5Mo**
A2	**Fe-1C-5Cr-1Mo**
A4	**Fe-1C-2Mn-1Cr-1Mo**
D2	**Fe-1.5C-12Cr-1Mo-1V**
D3	**Fe-2.25C-12Cr**
D5	**Fe-1.5C-12Cr-1Mo-3Co**
D7	**Fe-2.35C-12Cr-1Mo-4V**
M2	**Fe-0.8C-4Cr-5Mo-6W-2V**
M4	**Fe-1.3C-4Cr-4.5Mo-5.5W-4V**
Other ferrous alloys	
Hot-rolled low-carbon steel	**Fe-0.10 to 0.20C**
Unalloyed cast iron, 185 to 225 HB	**Fe-3C-1.6Si-0.7Mn**
Alloy cast iron, 200 to 250 HB	**Fe-3C-1.6Si-0.4Cr-0.4Mo**
Cast high-carbon steel, 185 to 225 HB	**Fe-0.75C**
Cast alloy steel, 200 to 235 HB	**Fe-0.45C-1.1Cr-0.4Mo**
4140 alloy steel	**Fe-0.4C-0.6Mn-0.3Si-1Cr-0.2Mo**
4140 modified	**Fe-0.4C-1.2Cr-0.2Mo-1Al**
Nonferrous alloys	
Zinc alloy (UNS Z35543)	**Zn-4Al-3Cu-0.06Mg**
Aluminum bronze (UNS C62500), 270 to 300 HB	**Cu-13Al-4Fe**
Nonmetals	

Polyester-glass	**50% polyester, 50% glass in the form of cloth, strand, or chopped fibers**
Epoxy-glass	**50% epoxy, 50% glass as above**
Polyester-metal	**Polyester reinforced with metal powder**
Epoxy-metal	**Epoxy reinforced with metal powder**
Nylon-metal	**Polyamide reinforced with metal powder**
Polyester or epoxy-glass-metal	**Polyester or epoxy with both glass and metal as above**

Die Life. Wear determines the useful performance of a press forming die. Total wear is primarily affected by the length of the production run and the severity of the forming operation. This total wear may be produced by abrasion or adhesion (galling), or both.

The amount of wear on a given die during forming is proportional to the total accumulated distance over which the sheet metal slides against the die at a given pressure between the surfaces in contact. Thin, soft, or weak sheet metals exert the least pressure and thus cause the least wear; thick, moderately hard or strong metals cause the most rapid wear. The rate of wear for each combination of work metal and die metal may vary considerably depending on surface characteristics, speed of forming, and die lubrication. In situations in which wrinkles form in the parts, high localized pressures develop on the tools because of the ironing that takes place at these locations, and prohibitively high rates of abrasive wear and galling are almost always encountered.

Typical Tool Materials. Tooling for the part shown in Fig. 1 consists of a punch and a lower die. In operation, the punch pushes the blank through the lower die, which causes wear of the lower die. The metal closely envelops the punch, with little sliding. In this situation, a punch generally produces about ten times as many parts as a lower die made of the same material. However, at areas in which the part shrinks against the punch during forming, wear (and possibly galling) of the punch surface occurs, particularly when the forming is done in single-action dies. For a small die and punch, the cost of steel is of minor importance, and D2 tool steel may be used for production quantities as low as 10,000. If galling occurs during preproduction trials, the tool can be nitrided. Typical materials for lower dies used in press forming small parts similar to that shown in Fig. 1 are given in Table 2.

Table 2 Typical lower-die materials for forming a small part of mild severity from 1.3 mm (0.050 in.) thick sheet

For die cross section and part shape, see Fig. 1.

Metal being formed	**Quality requirements**			**Lubrication**[b]	Lower-die materials[a] for total production quantity of:				
	Finish	**Tolerance**							
		mm	**in.**		**100**	**1000**	**10,000**	**100,000**	1,000,000
Aluminum alloy 1100, brass, copper[c]	None	None	None	Yes	Epoxy-metal, mild steel	Polyester-metal, mild and 4140 steel	Polyester-glass[d], mild and 4140 steel	O1, 4140	**A2, D2**

Aluminum alloy 1100, brass, copper[c]	None	±0.1	±0.004	Yes	Epoxy-metal, mild and 4140 steel	Polyester-metal, mild and 4140 steel	Polyester-glass[d], mild and 4140 steel	4140, O1, A2, D2	**A2, D2**
Aluminum alloy 1100, brass, copper[c]	Best	±0.1	±0.004	Yes	Epoxy-metal, mild steel	Polyester-metal, mild and 4140 steel	Polyester-glass[d], mild and 4140 steel	4140, O1, A2	**A2, D2**
Magnesium or titanium[e]	Best	±0.1	±0.004	Yes	Mild steel	Mild and 4140 steel	A2	A2	**A2, D2**
Low-carbon steel, to $\frac{1}{4}$ hard	None	None	None	Yes	Mild and 4140 steel	Mild and 4140 steel	4140, mild steel chromium plated, D2	A2	**D2**
Type 300 stainless, to $\frac{1}{4}$ hard	None	None	None	Yes	Mild and 4140 steel	Mild and 4140 steel	Mild and 4140 steel	A2, D2	**D2**
Low-carbon steel	Best	±0.1	±0.004	Yes	Mild and 4140 steel	Mild and 4140 steel	Mild and 4140 steel	A2, D2, nitrided D2	**D2, nitrided D2**
High-strength aluminum or copper alloys	Best	±0.1	±0.004	No[f]	Mild and 4140 steel	Mild and 4140 steel	Mild steel chromium plated and 4140	Chromium plated O1, A2	**D2, nitrided D2**
Type 300 stainless, to $\frac{1}{4}$ hard	None	±0.1	±0.004	Yes	Mild and 4140 steel	Mild and 4140 steel	Mild steel and 4140	Chromium plated O1, A2	**D2**
Type 300 stainless, to $\frac{1}{4}$ hard	Best	±0.1	±0.004	Yes	Mild and 4140 steel	Mild and 4140 steel	Mild steel chromium plated, D2	D2, nitrided D2	**D2, nitrided D2**
Heat-resistant alloys	Best	±0.1	±0.004	Yes	Mild and 4140 steel	Mild and 4140 steel	Mild steel chromium plated, D2	D2, nitrided D2	**D2, nitrided D2**
Low-carbon steel	**Good**	**±0.1**	**±0.004**	**No[f]**	**Mild and 4140 steel**	**Mild and 4140 steel**	**Mild steel chromium plated**	**D2, nitrided D2**	**D2, nitrided D2**

(a) Description of die materials is given in Table 1. When more than one material for the same conditions of tooling is given, the materials are listed in order of increasing cost; however, final choice often depends on availability rather than on small differences in cost or performance. When mild steel is recommended for forming fewer than 10,000 pieces, the dies are not heat treated. For forming 10,000 pieces or more, such dies should be carburized and hardened. When 4140 is recommended for fewer than 10,000 pieces, it should be pretreated to a hardness of 28 to 32 HRC. Flame hardening of high-wear areas is recommended for quantities greater than 10,000 pieces.

(b) Specially applied lubrication, rather than mill oil.

(c) Soft.

(d) With inserts.

(e) Heated sheet.

(f) Use lubrication to make 1 to 100 parts.

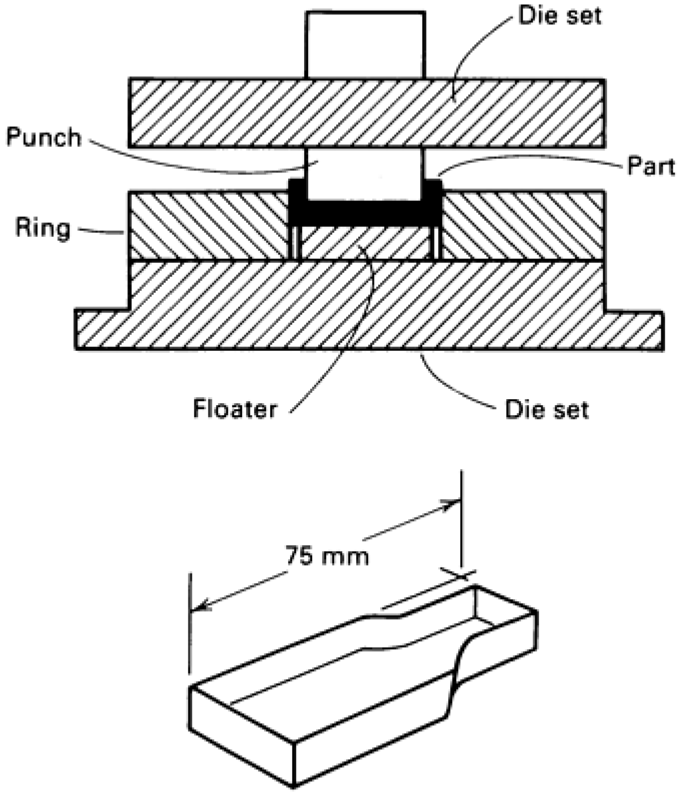

Fig. 1 Cross section of die used to form small part of mild severity. See Table 2 for typical die materials.

Tooling for the part in Fig. 2 consists of a punch, an upper die, and a lower die. Without the upper die, excessive wrinkling would occur at the shrink flanges. As for the part shown in Fig. 1, a less wear-resistant material is required for the punch and upper die than is needed for the lower die. Under conditions for which the tooling is typically made of tool steel (see Table 3), the tooling is in the form of inserts in a lower die made of cast iron, as shown in Fig. 2, and the punch is made of a cast tool steel such as D2. For example, a cast iron die with A2 or D2 inserts at points of greatest wear is typical for production quantities of 10,000 to 100,000 pieces. When this part must be held to close tolerances over lengthy production runs, type D2 tool steel inserts should be used at all surfaces subject to wear.

Table 3 Typical lower-die materials for forming a large part of mild severity from 1.3 mm (0.050 in.) thick sheet

For die cross section and part shape, see Fig. 2.

Metal being formed	Quality requirements			Lubrication[b]	Lower-die materials[a] for total production quantity of:				
	Finish	Tolerance							
		mm	in.		100	1000	10,000	100,000	1,000,000
Aluminum alloy 1100, brass, copper[c]	None	None	None	Yes	Epoxy-metal, polyester-metal, zinc alloy	Polyester-metal, zinc alloy	Epoxy or polyester-glass[d], zinc alloy	Alloy cast iron	**Cast iron, A2[e]**
Aluminum alloy 1100, brass, copper[c]	None	±0.1	±0.004	Yes	Epoxy-metal, polyester-metal, zinc alloy	Polyester-metal, zinc alloy	Alloy cast iron	Alloy cast iron	**Alloy cast iron**
Aluminum alloy 1100, brass, copper[c]	Best	±0.1	±0.004	Yes	Epoxy-metal, polyester-metal, zinc alloy	Polyester-metal, zinc alloy	Alloy cast iron	Alloy cast iron	**Alloy cast iron, A2[e]**
Magnesium or titanium[f]	Best	±0.1	±0.004	Yes	Cast iron, zinc alloy	Cast iron, zinc alloy	Cast iron	Alloy cast iron	**Alloy cast iron, A2[e]**
Low-carbon steel, to $\frac{1}{4}$ hard	None	None	None	Yes	Epoxy-metal, polyester-metal, zinc alloy	Epoxy-glass, polyester-glass, zinc alloy	Epoxy or polyester-glass[d], cast iron	Alloy cast iron	
Type 300 stainless, to $\frac{1}{4}$ hard	None	None	None	Yes	Epoxy-metal, polyester-metal, zinc alloy	Epoxy-glass polyester-glass, zinc alloy	Epoxy or polyester-glass[d], alloy cast iron	A2[e]	**D2[e]**
Low-carbon steel	Best	±0.1	±0.004	Yes	Zinc alloy	Epoxy-glass, polyester-glass, zinc alloy	Alloy cast iron	D2, nitrided A2[e]	**D2, nitrided D2[e]**
High-strength aluminum or copper alloys	Best	±0.1	±0.004	No[g]	Zinc alloy	Polyester-glass, zinc alloy	Alloy cast iron	Alloy cast iron	**Nitrided A2[e], nitrided D2[e]**
Type 300 stainless, to $\frac{1}{4}$ hard	None	±0.1	±0.004	Yes	Zinc alloy	Zinc alloy	Alloy cast iron	D2, nitrided A2[e]	**D2[e], minded D2[e]**
Type 300 stainless, to $\frac{1}{4}$	Best	±0.1	±0.004	Yes	Zinc alloy	Zinc alloy	Alloy cast iron	Nitrided D2	**Nitrided D2[e]**

hard									
Heat-resistant alloys	Best	±0.1	±0.004	Yes	Zinc alloy	Zinc alloy	Alloy cast iron	Nitrided D2	**Nitrided D2[(e)]**
Low-carbon steel	**Good**	**±0.1**	**±0.004**	**No[(g)]**	**Zinc alloy**	**Zinc alloy**	**Alloy cast iron**	**Nitrided D2**	**Nitrided D2[(e)]**

(a) Description of die materials is given in Table 1. When more than one material for the same conditions of tooling is given, the materials are listed in order of increasing cost; however, final choice often depends on availability rather than on small differences in cost or performance. When mild steel is recommended for forming fewer than 10,000 pieces, the dies are not heat treated. For forming 10,000 pieces or more, such dies should be carburized, and hardened. When 4140 is recommended for fewer than 10,000 pieces, it should be pretreated to a hardness of 28 to 32 HRC. Flame hardening of high-wear areas is recommended for quantities greater than 10,000 pieces.

(b) Specially applied lubrication, rather than mill oil.

(c) Soft.

(d) With inserts.

(e) Use as inserts in cast iron body.

(f) Heated sheet.

(g) Use lubrication to make 1 to 100 parts.

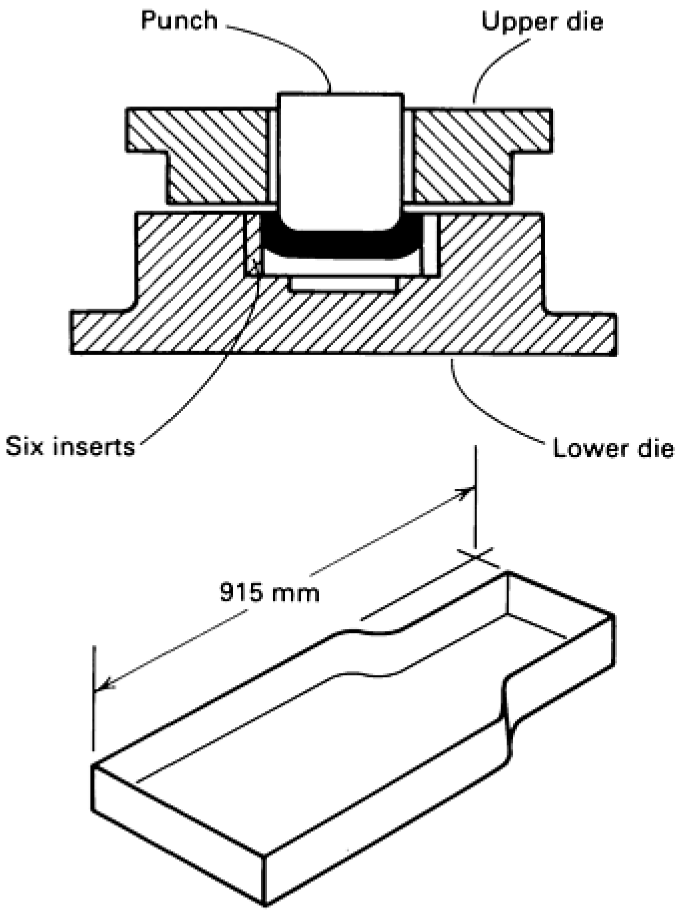

Fig. 2 Cross section of die used to form large part of mild severity. See Table 3 for typical die materials.

Typical lower-die materials for press forming large parts similar to that shown in Fig. 2 are given in Table 3. For quantities of less than 100,000 pieces, the entire lower die is typically made of the material indicated in the selection table, without inserts. The punch is made of a less wear-resistant material, which is usually the same as the lower-die material in the first column to the left of the quantity being considered.

Tables 2 and 3 may be used to select lower-die materials for parts made of sheet thicker or thinner than the 1.3 mm (0.050 in.) thick sheet used in the tables, or for parts of greater or lesser severity than those shown in Fig. 1 and 2. For parts of greater severity or sheet of greater thickness, use the die material recommended for the next greater production quantity than the quantity actually to be made (the column to the right of the actual production quantity in the table). Similarly, for parts of lesser severity or sheet of lesser thickness, use the die material recommended for the next lower production quantity (the next column to the left of the actual production quantity).

Selection for Galling Resistance. As indicated previously, galling, which is cold welding of the metal being formed to that of the dies, drastically reduces the number of parts that can be made using a particular set of dies. Galling is caused by attempts to stretch sheet metal beyond practical limits, by inadequate lubrication, by poor tool fitting, or by rough finishes on tool surfaces. Therefore, when galling is encountered, the tool fit and the thickness of the metal being formed should first be checked to determine whether clearance is adequate. If clearance is considered adequate, lubrication practice should be reviewed before considering a change in die materials. Galling is less likely to occur if the die materials and the metal being formed are dissimilar in hardness, chemical composition, and/or surface characteristics. For example, effective combinations are: aluminum bronze tools for forming carbon steel and stainless steel; tool steel tools for forming aluminum and copper alloys; and carbide tools for forming carbon steel, stainless steel, and aluminum.

Aluminum bronzes have excellent resistance to galling and are desirable for dies in applications in which the best finish is required on carbon steel or stainless steel parts. However, for medium-to-high production (10,000 to 100,000 parts), the use of inserts permits easy reconditioning of worn tools.

Nitriding minimizes or prevents galling of dies made of alloy steels or alloy tool steels (such as A2 or D2) that contain chromium and molybdenum. However, the nitrided surfaces may spall off at radii smaller than about 3.2 mm ($\frac{1}{8}$ in.), especially from dies having very complex contours.

Hard chromium plating usually eliminates galling of mild steel, alloy steel, and tool steel dies, and it is often used for severe duty. For operations involving high local pressures, hardened alloy steels or tool steels are less likely to yield plastically and cause cracking of the hard chromium plating. With dies for complex parts, hard chromium plating may spall off at radii smaller than about 6.4 mm ($\frac{1}{4}$ in.).

For some press forming operations, dies made from tool steels other than those discussed above may be desirable. For example, shock-resistant tool steels such as S1, S5, and S7 may be used for die components subjected to severe impact in service. H11 and H13, possibly nitrided for greater wear resistance, also may be used for such components. In press forming operations requiring significantly greater wear life than is routinely attained with D2 or nitrided D2, it may be necessary to specify a more wear-resistant cold work tool steel such as A7, D3, D4, or D7, or a high-speed steel such as M2, M4, or T15. Cost generally determines the desirability of changing to an alternative material, although toughness may also be a determining factor. Costs to be considered include not only material costs but also tool fabrication costs and the cost of periodic resharpening.

Other Tool Materials. Significant advances have been made in recent years in the area of tool steels made by powder metallurgy (P/M) techniques. For example, P/M high-speed steels, hot isostatically pressed to full density, offer greater ease of fabrication and significantly improved toughness compared to conventional ingot-cast steels of the same compositions. New grades that could not have been produced economically by conventional steelmaking practices have been introduced through the use of powder metallurgy. One such alloy is Crucible CPM 10V (Fe-2.45C-5.0Cr-9.75V-1.25Mo), which is an air-hardened cold-work tool steel designed specifically for tooling applications requiring long wear life and good toughness. This material can be a cost-effective alternative to carbide in applications in which breaking or chipping of carbide is a problem or in which the full potential of carbide is either not realized or not required. More information on these materials is available in the article "Particle Metallurgy Tool Steels" in *Powder Technologies and Applications*, Volume 7 of the *ASM Handbook.*

When maximum resistance to galling and wear is required, cemented carbides have traditionally been recognized as the ultimate tooling materials. However, because of the high cost of these materials and their tendency to be brittle in service, carbides are frequently used only for inserts in critical die areas. These inserts are usually made of a straight grade of tungsten carbide containing about 6% cobalt binder, but higher cobalt contents can be specified to provide greater shock resistance. The more recently developed steel-bonded carbides offer greater ease of fabrication and very often can be demonstrated to be cost-effective substitutes for the more costly cemented carbides with cobalt binder.

These materials use tool steel or stainless steel matrices filled with titanium carbide at volume fractions ranging from 15 to 45%. Several grades are available.

Selection of Material for Deep-Drawing Dies

Introduction

DEEP DRAWING is a process in which sheet metal is formed into round or square cup-shaped parts by making it conform to a punch as it is drawn through a die (see the article "Deep Drawing" in this Volume). In conventional deep drawing, successive draws are made in the same direction. The types of dies and other tooling used for conventional deep drawing are illustrated in Fig. 1.

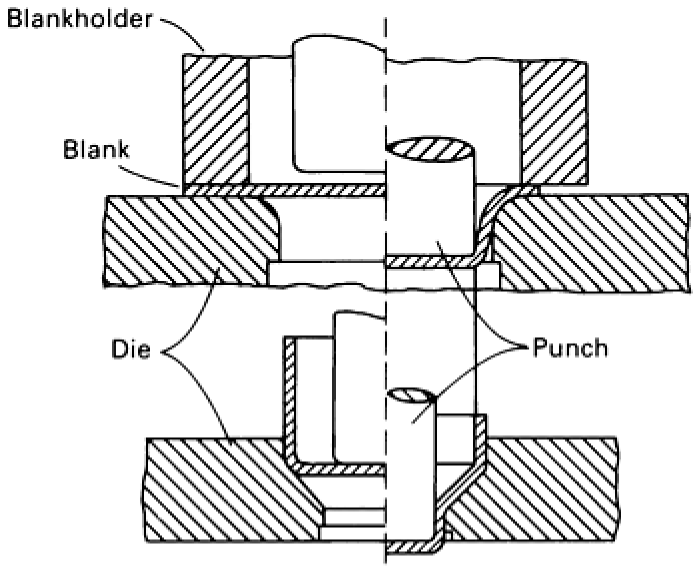

Fig. 1 Schematic showing tools used for the first draw (top) and the first redraw (bottom) in deep drawing.

It is sometimes necessary for redrawn shells to have a wrinkle-free sidewall of uniform thickness or a section in the bottom of the cup that is sharply raised, usually by forming in two operations. Such operations are difficult, impossible, or uneconomical to perform by conventional single-action drawing, but they are easily done by reverse redrawing. Figure 2 shows typical tooling for the reverse redrawing of thin-wall shells.

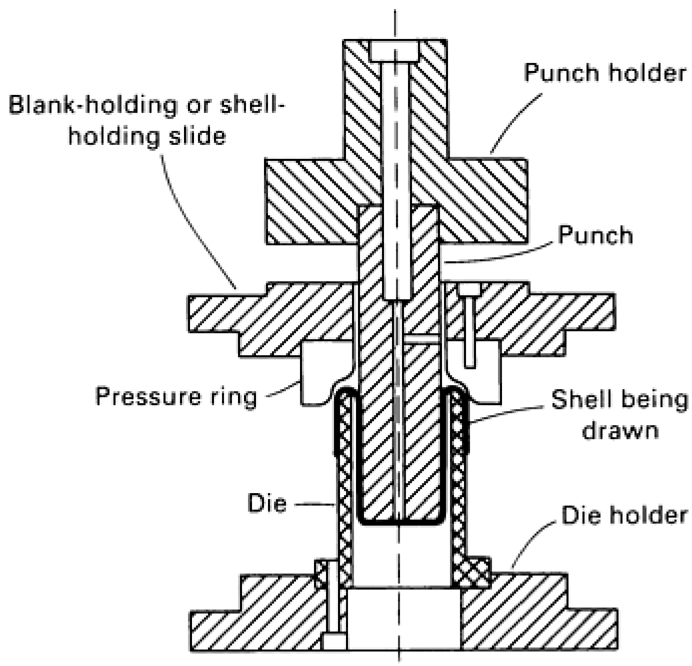

Fig. 2 Typical tooling used for the reverse redrawing of thin-wall shells.

For economical manufacture, a drawn part should always be produced in the fewest steps possible. Ironing (that is, thinning the walls of the part being drawn by using a reduced clearance between punch and die) is almost universally used in multiple-operation deep drawing. Ironing helps to produce deep draws and uniform wall thickness in the fewest operations. Each operation is designed for maximum practical reduction of the metal being drawn. Accordingly, the information given in this article is based on ironing reductions near the maximum of about 35%.

The selection of material for a drawing die is aimed at production of the desired quality and quantity of parts with the least possible tooling cost per part. In small dies (for example, those for making parts up to 75 mm, or 3 in., across), performance is the primary consideration. Material cost is a minor factor because the cost of even the more highly alloyed tool steels is probably less than 5% of the total die cost. In dies for parts larger than about 203 mm (8 in.), material cost is more important, and in a die for a 305 mm (12 in.) part, it may amount to nearly one-half the total die cost, even when the tool consists of a tool steel insert in a flame-hardened alloy cast iron die.

Selection of Material for Deep-Drawing Dies

Die Performance

The performance of a drawing die is determined by the total amount of wear (abrasive and adhesive) that occurs during a production run. The wear of a given die material is largely determined by its hardness, type and thickness of the sheet metal being drawn, sharpness of die radii, lubrication, and construction and surface finish of the die. The amount of wear on die radii can vary by a factor of 20 between the sharpest and most liberal radii. In drawing square cups, the formation of wrinkles at the corners, accompanied by high localized pressures, may produce prohibitively high rates of wear.

Lubrication. Correct lubrication of the sheet metal is essential if friction, wear, and galling are to be held to the lowest possible levels during deep drawing. In fact, deep drawing is impossible if the sheet metal is not lubricated. In actual practice, die materials are selected after trials using one or more candidate production lubricants. If excessive wear or galling occurs, a better lubricant is usually applied. For extremely difficult draws, the best lubricants are usually applied at the outset.

Table 1 lists typical lubricants used for different work metals and severities of drawing. Lubricants are marketed under proprietary names, but any supplier of lubricants can recommend commercial compounds fitting the descriptions given in Table 1. More information on lubrication in sheet forming is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Table 1 Typical lubricants for deep drawing

When more than one lubricant is given, they are listed in order of increasing effectiveness.

Metal being drawn	**10% or less**	**Severity of drawing 25% average**	50% or more
Aluminum and aluminum alloys	Straight mineral oil, 100 SUS viscosity(a); mineral oil with approximately 10% lard oil	Straight mineral oil, 200-250 SUS viscosity(a); mineral oil with approximately 15% lard oil	**Mineral oil with extreme-pressure additives--sulfur and others; coating of soap or wax dried on blanks (or shells) prior to drawing (or redrawing)**
Copper and copper alloys	5% soap solution; lard and soap emulsion	10% soap solution with stearic or oleic acid; lard oil and mineral oil with stearic acid	**Lard oil blended with 50% mineral oil, coating of soap or wax dried on blanks or draws prior to draw or redraw**
Carbon steel	Mineral oil, 250-350 SUS viscosity(a); 5% soap solution	Emulsions of lard oil, mineral oil, and sulfonated oils	**Phosphate coating impregnated with dried soap or wax**
Stainless steel	**Castor oil and soap emulsion**	**Castor oil with fillers, such as mica or zinc oxide**	**Boiled linseed oil with mica or lithopone; phosphoric acid etch with dried soap or wax film**

(a) Saybolt universal seconds at 40 °C (100 °F)

Materials for Specific Tools

Draw Rings. Table 2 lists typical materials for draw rings (both dies and backup rings) used in drawing and ironing cups of various diameters and lengths. The data in Table 2 are for round and square cups drawn from stock 1.6 mm (0.062 in.) thick in three typical production quantities. Similar data for a large square cup and a large pan are also provided. Design dimensions for all seven parts referred to in Table 2 are given in Fig. 3. The square parts have liberal corner radii consistent with favorable die life.

Table 2 Typical materials for draw rings used in the drawing and ironing of round and square parts

See Fig. 3 for part designs and overall dimensions.

Metal to be drawn	Total number of parts to be drawn		
	10,000	**100,000**	1,000,000
Cups up to 76 mm (3 in.) across, drawn from 1.6 mm (0.062 in.) sheet (parts 1, 2, and 3)			
Drawing-quality aluminum and copper alloys	W1; O1	O1; A2	**A2; D2**
Drawing-quality steels	W1; O1	O1; A2	**A2; D2**
300-series stainless steels	W1 chromium plated; aluminum bronze	Nitrided A2; aluminum bronze	**Nitrided D2 or D3; cemented carbide**
Cups 305 mm (12 in.) or more across, drawn from 1.6 mm (0.062 in.) sheet (parts 4 and 5)			
Drawing-quality aluminum and copper alloys	Alloy cast iron[(a)]	Alloy cast iron [(a)]; A2 inserts[(b)]	**A2 or D2 inserts[(b)]**
Drawing-quality steels	Alloy cast iron[(a)]	Alloy cast iron[(c)]; A2 inserts[(b)]	**A2 or D2 inserts[(b)]**
300-series stainless steels	Alloy cast iron[(d)]; aluminum bronze inserts[(b)]	A2 or aluminum bronze inserts[(b)]	**Nitrided A2 or D2 inserts[(b)]**
Square cups similar to part 6, drawn from 1.6 mm (0.062 in.) sheet			
Drawing-quality aluminum and copper alloys[(e)]	W1	O1; A2	**A2; D2**
Drawing-quality steels[(e)]	W1	O1; A2	**A2; D2; nitrided A2 or D2**
300-series stainless steels[(f)]	W1; aluminum bronze	Nitrided A2; aluminum bronze	**Nitrided A2 or D2**
Large pans similar to part 7, drawn from 0.8 mm (0.031 in.) sheet			
Drawing-quality aluminum and copper alloys	Alloy cast iron[(a)]	Alloy cast iron[(a)]; A2 corner inserts[(b)]	**Nitrided A2 or D2 inserts[(b)]**
Drawing-quality steels	Alloy cast iron[(a)]	Alloy cast iron[(a)]; A2 corner inserts[(b)]	**Nitrided A2 or D2 inserts[(b)]**
300-series stainless steels	**Alloy cast iron[(d)]; aluminum**	**Nitrided A2 or aluminum**	**Nitrided A2 or D2 inserts[(b)]**

	bronze	**bronze inserts**[(b)]	

(a) Wearing surfaces flame hardened.

(b) In flame-hardened alloy cast iron.

(c) Quenched and tempered for part 4; flame hardened for part 5.

(d) Flame hardened on wearing surface to not over 420 HB.

(e) For drawing aluminum, copper, and steel, the tool material would be used as corner inserts.

(f) For drawing stainless steel, inserts would be used for all wear surfaces.

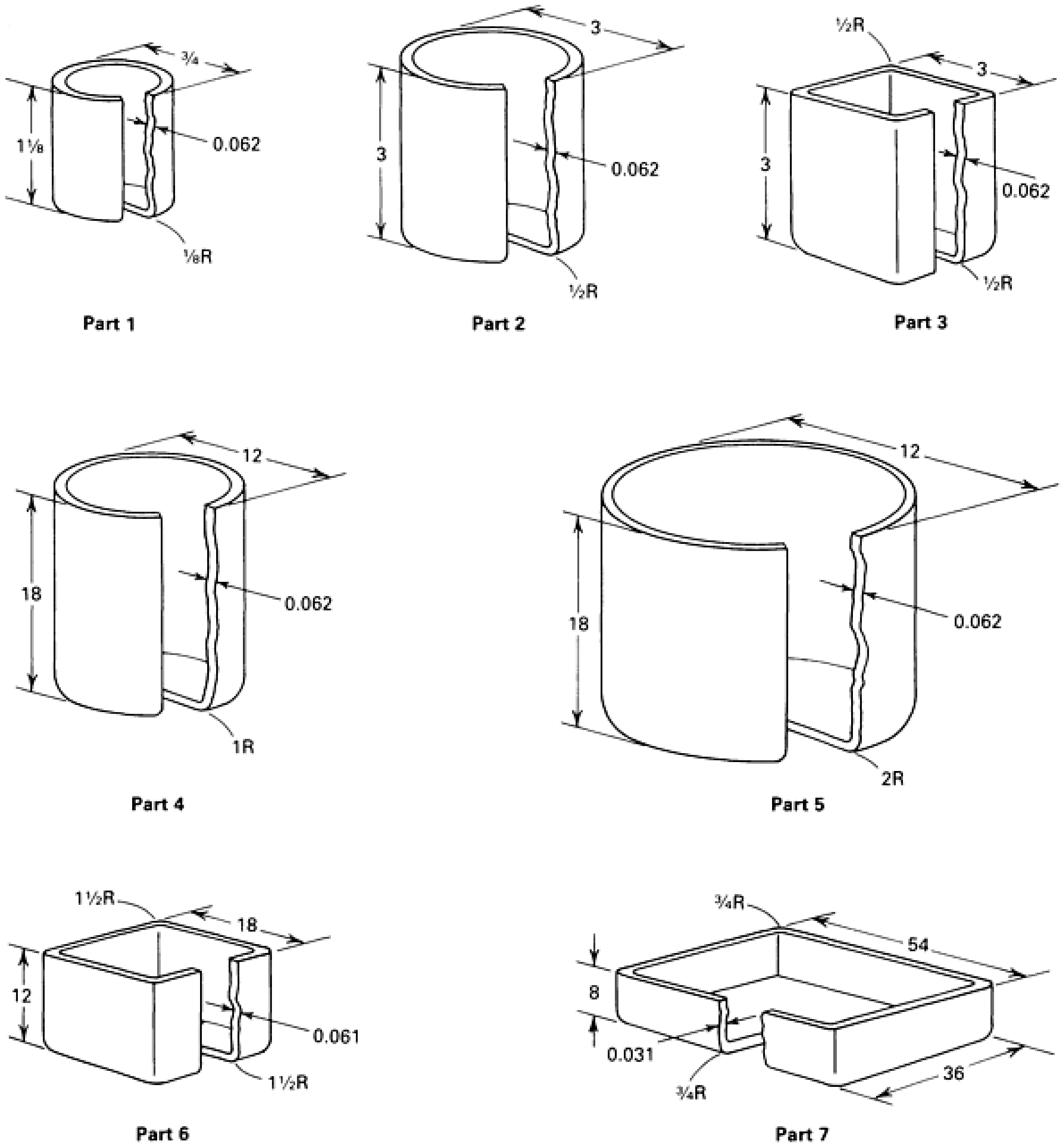

Fig. 3 Seven typical deep-drawn parts. Corner rodii comply with standard commercial practice. Table 2 lists typical die materials used for drawing parts of similar configuration. Dimensions given in inches.

Table 3 indicates the effect on material selection of changing the thickness of the sheet metal being drawn. Tool materials of increasingly greater wear resistance are required as the thickness of the work metal or the total quantity of parts is increased.

Table 3 Typical materials for draw rings used in making part 4 from flat-rolled steel of six thicknesses

See Fig. 3 for part design and overall dimensions.

Thickness of steel		Total number of parts to be drawn			
mm	**in.**	**1,000**	**10,000**	**100,000**	1,000,000

0.4	0.015	Alloy cast iron[a]	Alloy cast iron[a]	Alloy cast iron[a]	**Alloy cast iron[b], O1, A2**
0.8	0.031	Alloy cast iron[a]	Alloy cast iron[a]	Alloy cast iron[b]	**A2, D2**
1.6	0.062	Alloy cast iron[a]	Alloy cast iron[b]	Alloy cast iron[b], A2	**A2, D2**
3.2	0.125	Alloy cast iron[b]	Alloy cast iron[b]	A2, D2	**D2**
6.4	0.250	A2	A2	D2	**D2**
12.7	**0.500**	**A2[c]**	**A2[c]**	**D2[c]**	**D2[c]**

Note: Where tool steels are recommended, they are used as inserts in flame-hardened alloy cast iron.

(a) Flame hardening not necessary.

(b) Wearing surfaces flame hardened.

(c) In drawing 12.7 mm (0.500 in.) plate with A2 or D2 inserts, press speed is slower than for thinner stock, and the plate is phosphate coated.

Punches and Blankholders. Typical materials for punches and for blankholders or shellholders are listed in Table 4. These materials are for punches and blankholders used in drawing and ironing round and square steel cups similar to parts 2 through 7 in Fig. 3.

Table 4 Typical materials for punches and blankholders

See Fig. 3 for part designs and overall dimensions.

Die component	Total number of parts to be drawn		
	10,000	**100,000**	1,000,000
For round steel cups such as part 2			
Punch[a]	Carburized 4140; W1	W1; carburized S1	**A2; D2**
Blankholder[b]	W1; O1	W1; O1	**W1; O1**
For square steel cups such as part 3			
Punch[a]	Carburized 4140; W1	W1; carburized S1	**A2; D2**
Blankholder[b]	W1; O1	W1; O1	**W1; O1**

For round steel cups such as parts 4 and 5			
Punch[a]	Alloy cast iron[c]	O1[d]	**A2[c]; D2[c]**
Blankholder[b]	Alloy cast iron[c]	Alloy cast iron[e]	**O1; A2**
For square steel cups such as parts 6 and 7			
Punch[a]	Carburized 4140[f]	W1; O1[d]	**Nitrided A2; D2[d]**
Blankholder[b]	**Alloy cast iron[c]**	**W1; O1**	**O1; A2**

(a) Chromium plating is optional on punches to reduce friction between part and punch and therefore facilitate removal of the part. Cast iron, however, should not be plated.

(b) Also applies to shellholder and blankholder.

(c) Flame hardening not necessary.

(d) The punch holder is flame-hardened alloy cast iron with a nose insert of the indicated tool steel.

(e) For part 4, this blankholder is quenched and tempered; for part 5, it is flame hardened.

(f) The punch holder is alloy cast iron with a nose insert of the indicated steel.

More wear-resistant materials are required not only for the tools used in drawing and ironing harder or thicker stock or for those used for longer runs but also for tools used to achieve greater percentages of reduction during ironing. Table 5 lists typical tool steels used in punches and dies for short-, medium-, and long-run production at four levels of reduction in ironing. Typical materials for punches and dies used in the reverse redrawing of steel cups are listed in Table 6.

Table 5 Typical tool steels for punches and dies to iron soft steel sheet at various reductions

Ironing reduction, %	Total quantity of shells[a] to be ironed			
	1000	**10,000**	**100,000**	1,000,000
Ironing punches[b]				
Up to 25	W1	O1	A2	**A2; S1 carburized**
25-35	W1	A2	A2; S1 carburized	**D2**

35-50	A2	A2; S1 carburized	D2	**D2**
Over 50	D2	D2	D2	**D2**
Ironing dies				
Up to 25	W1[c]	O1	O1	**D2**
25-35[d]	W1[c]	O1	D2	**D2**
35-50[d]	O1	D2	D2	**D2**
Over 50[d]	**D2**	**D2**	**D2**	**D2**

(a) Steel sheet up to 75 HRB, or softer metals.

(b) All tool steel punches should be plated with chromium 5-10 μm (0.2-0.4 mils) thick for easier removal of the part from the punch.

(c) W1 is quenched on the inside and tempered to a minimum of 60 HRC for these applications.

(d) Draw rings must be inserted in shrink rings for ironing at reduction greater than 25% and for quantities of more than 10,000 parts.

Table 6 Typical punch and die material for the reverse redrawing of steels

Die component	Total quantity of parts[a] to be redrawn			
	1000	**10,000**	**100,000**	1,000,000
Small thick-wall cups				
Die and pressure ring	O1	O1[b]	A2[c]	**D2[c]**
Punch[d]	4140, 6150	O1, A2	D2	**D3**
Medium and large thin-wall cups				
Die and pressure ring	1018[e], 4140	4140[f], O1	A2[c]	**D2[c]**

(a) No specific finish or tolerance requirements.

(b) Dies are polished and chromium plated.

(c) A2 and D2 should be nitrided.

(d) All punches used for making more than 1000 pieces should be heat treated to 60-62 HRC, polished, and chromium plated.

(e) Carburized, hardened, and polished to a fine finish.

(f) 4140 or 6150 can be used if carburized and highly polished.

Combatting Specific Service Problems

Wear (most notably galling) is the most common sign of deterioration in deep-drawing tools. Wear can be reduced by selecting a harder and more wear-resistant material, by applying a surface coating such as chromium plating to the finished tools, or by using a surface treatment such as carburizing or carbonitriding. The following sections in this article are intended to supplement the basic information given in Tables 2, 3, 4, 5, and 6.

Galling. The typical causes of galling of deep-drawing tooling are:

- Attempts to stretch sheet metal beyond practical limits
- Poor tool fit-up, with poor alignment or insufficient die clearance for the sheet thickness
- Excessive wrinkling
- Insufficient or otherwise inadequate lubrication
- Use of tool steels that are susceptible to galling without applying a surface coating to the tools or using a lubricant of superior lubricating qualities
- Rough finishes on tool surfaces

For short runs, dies made of carburized hot-rolled steel or hardened alloy steel will often produce parts equal in quality to those drawn over most tool steel dies. Exceptions may be encountered in ironing to severe reductions or in drawing metals that tend to gall, such as austenitic stainless steels. These exceptions may be of little consequence, however, because tool steel dies may also become galled under the same circumstances. The longest die life can be expected when die surfaces have a very fine finish, with final surface scratches parallel to the direction of drawing. Die materials can be selected for resistance to galling on the basis of the following two criteria.

First, for parts drawn from carbon steel or nonferrous alloy sheet, the die material can be selected without regard to galling, and then, as a finishing operation, the punch and die should be either nitrided or chromium plated. If a tool steel such as A2, D2, D3, or D4, which contain chromium and molybdenum, has been selected, the smoothly ground tools should be nitrided and then polished or buffed.

Second, for parts drawn from stainless steel or from high-nickel alloy steel, the draw ring material with the best resistance to galling is aluminum bronze. The second choice is D2, D3, or D4, smoothly ground, nitrided, and polished. The third choice is alloy cast iron, quenched and tempered to 400 to 420 HB.

Chromium plating is used to extend the service lives of tool steel draw rings. On punches, the primary function of chromium plating is to reduce frictional forces and to facilitate the removal of parts from the punch after the sidewalls have been ironed tight to the punch. The improvement in punch life that results from chromium plating is usually somewhat less than that attained by changing the punch material to the next best tool steel.

For successful tool performance, chromium plating must always be deposited on a surface harder than 50 HRC; preferably, plating thickness should be 5 to 10 μm (0.2 to 0.4 mils) and never less than 2.5 μm (0.1 mil). This provides the required hardness and reduction of friction without excessive spalling or chipping at corners. Chromium-plated dies should be heated to 150 to 205 °C (300 to 400 °F) for a minimum of 3 h immediately after plating to minimize the possibility of hydrogen embrittlement.

Combined operations have found increasing use over the past 30 years. The more popular combined operations include one that combines drawing and coining and another that combines successive or tandem drawing (or ironing) operations. This latter combination is called double drawing or double ironing. Advancements in combined operations have paralleled advancements in die materials--for example, better selection of drawing steels as well as improvements in the engineering and construction of tools and especially in surface treatments such as those using zinc phosphate with emulsified soap.

These operations have increased production by doubling reductions and decreasing the number of operations, but at the same time have required capital investment in larger presses. In addition, tool steels of greater resistance to compression and heat have become necessary for drawing and ironing tools.

Double-drawing and double-ironing operations are successive operations in one tooling setup, with two dies placed in tandem so that a punch forces the cup through one die and then directly through the second die while the cup is still warm from deformation heating. The punches are longer than those used in conventional deep drawing and, because of their slenderness, are preferably made of S1 tool steel. Die materials are much the same as in single operations, except that selection is confined to tool steels such as A2 and D2 when temperatures are high in the second operation. These more temper-resistant steels can better withstand the effects of the higher temperatures developed by increased plastic deformation of the workpiece.

Cemented carbides. For long runs, cemented carbide inserts are widely used in deep-drawing dies. In dies up to 203 mm (8 in.) across for continuous production of over 1 million drawn parts, carbide has often proved to be the most economical die material. Such dies have maintained size in drawing 500,000 parts with 60% reductions and have made as many as 1 million parts with reductions greater than 40% when the steel to be drawn was surface treated with zinc phosphate and soap. However, cemented carbide dies do not provide satisfactory service with inferior lubricants. In addition, carbide dies are not superior to dies made of a tool steel such as D2 in complex deep-drawing operations (for example, those that combine drawing with coining or forming and in which the reduction in drawing is greater than 40%).

The cemented carbides that are most often used for deep-drawing inserts are straight tungsten carbide grades, of normal particle size, that contain about 9 to 10% cobalt or nickel binder. Steel-bonded carbides are also used for deep-drawing tools.

Plastics are the most economical tooling materials for short runs especially when a prototype part is available as a pattern for the lay-up of plastic-impregnated glass cloth facing, which is backed with chopped glass fibers impregnated with 50% resin. Among resins, polyester, epoxy, phenolic resin, and nylon have been used. The plastic dies that exhibit the longest life are those constructed so that the wearing surface is faced with glass cloth that has had most of the plastic material forced out under pressure before and during curing. Except for very short runs, plastics should not be selected as blankholder materials where burred edges of the blank slide over the plastic surface and produce severe wear or gouging.

Zinc Alloy Tools. Because they are relatively soft, zinc alloy tools should be used only in drawing (without ironing) small quantities of large-diameter thin-wall parts. Zinc alloy tools work best for drawing well-lubricated stock into parts 305 mm (12 in.) or more in diameter under circumstances in which wrinkling is not likely to occur.

Selection and Use of Lubricants in Forming of Sheet Metal

Elliot S. Nachtman, Tower Oil & Technology Company

Introduction

LUBRICANTS AND LUBRICATION are relatively low-cost components of a metal-forming system, yet lubricants are important and often indispensable for efficient forming of quality parts.

An overview of the interfacial interactions between a die and metal to be formed with the interposed lubricant film is presented in this article. Lubricant mechanisms, chemistry, qualification testing, application methods, property test methods, and some aspects of microbiology and toxicity are discussed, as are the increasingly important health and economic implications of some recent laws and regulations dealing with metal-working lubrication. Much of the discussion is relevant to metal-forming operations in general, although targeted at sheet metal forming operations. The final sections deal with lubricant selection as influenced by the metal to be formed and particular sheet metal forming operations.

Selection and Use of Lubricants in Forming of Sheet Metal

Elliot S. Nachtman, Tower Oil & Technology Company

Surfaces

In general, the specific characteristics of the surfaces of both the sheet and the dies make up the environment in which sheet metal lubrication is considered. As such they provide the boundaries between which the lubricant must operate. These surfaces are complex. The chemistry of the surface is not the same as the bulk chemistry of the sheet or tools. The lubricant interacts with an oxide layer of varying complexity; various contaminants may infiltrate the oxide layer (oil, gases, acids, rust preventives, and so forth); and the surface may be carburized or decarburized, nitrided or subjected to some other surface treatment, or coated with a polymer or phosphate. Many other surface chemistry variations are possible. Furthermore, the surface may have residual compressive or tensile stresses from prior processing. The fine structure of the metal surface may also vary with prior history and be quite different from that of the bulk material. The surface roughness also varies in many geometrically significant ways. Interposed between these non-homogeneous surfaces is a lubricating film that may be quite complex in structure, activity, and wetting ability.

The complexity of the lubricant/metal interface fundamentally affects the lubricant performance by preventing galling or wear of the tooling, as well as tearing, scratching, or imperfect forming to desired dimensions of the sheet metal.

Selection and Use of Lubricants in Forming of Sheet Metal

Elliot S. Nachtman, Tower Oil & Technology Company

Lubrication Mechanisms

In sheet metal forming, several different lubrication regimes have been identified. One or more of these regimes may be operational depending upon the sheet metal forming process. Figure 1 illustrates three of the lubrication regimes that are discussed below.

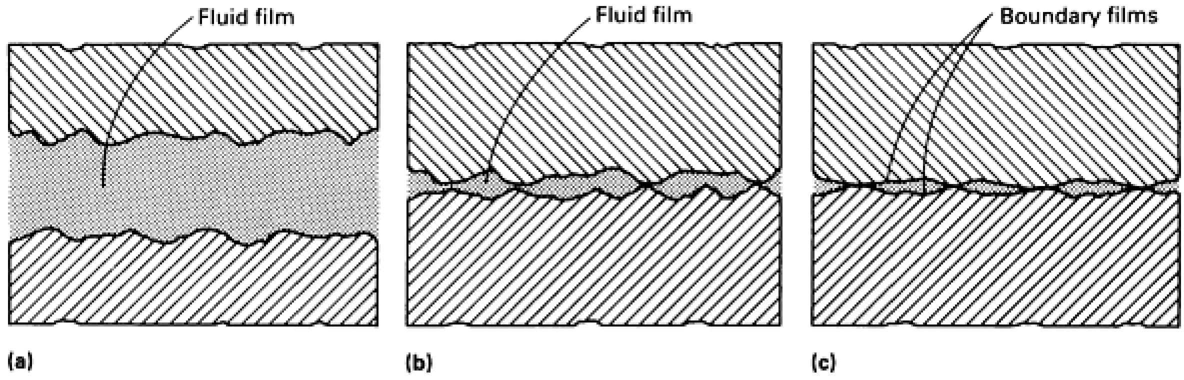

Fig. 1 Three lubrication regimes experienced in sheet metal forming. (a) Thick-film lubrication. (b) Thin-film lubrication. (c) Boundary lubrication.

Thick-film (hydrodynamic) lubrication (Fig. 1a) is the occurrence of a thick film of lubricant between tool and workpiece that completely prevents metal-to-metal contact. In this regime, the bulk properties of the lubricant (viscosity) and the mechanical system operating during deformation create the necessary conditions.

Thin-Film (Quasi-Hydrodynamic) Lubrication. The film between tool and workpiece is thinner, and some metal-to-metal contact takes place (Fig. 1b). This regime, also called mixed lubrication, occurs in bar and wire drawing and less frequently in sheet metal forming.

Boundary Lubrication (Fig. 1c). Physical adherence of the lubricant to the surface occurs, and relatively thin lubricant films may be effective. Dependence on viscosity is lower, and chemisorption (chemical adherence of the lubricant to the metal surface) becomes more important. The adherence and strength of the adsorbed film governs lubrication effectiveness.

Extreme-Pressure Lubrication. The metal surfaces are chemically altered because of reaction between the lubricant and the metal surface. These reactions often involve sulfur, chlorine, or phosphorus present in the lubricant reacting with the metal surface to form sulfides, chlorides, or phosphides. These compounds may be very complex in composition. Lubrication is provided because the films formed are low in strength and shear readily under deformation.

In solid-film lubrication, separation of tool and workpiece occurs by interposing a film consisting of molybdenum disulfide, graphite, sodium carbonate, Teflon, nylon, and other solids in a carrier. These solids act to provide a hydrodynamic film as long as they maintain surface integrity. The mechanism of solid-film lubrication is essentially the same as that of thick-film lubrication (Fig. 1a), except that the lubricant is a solid substance.

More than one of these regimes may occur during sheet metal forming. Indeed, a single regime often cannot be effective in providing the necessary film integrity between tool and workpiece. The specific regimes that are operational depend upon the severity, temperature, and geometry of the deformation mode. Detailed information on lubricating mechanisms is available in Ref 1 and 2.

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Lubricant Forms

The three principal fluids (water, oil, and synthetic) that comprise the primary ingredients of sheet metal forming lubricants are combined with additives to achieve the desired operating characteristics. Table 1 lists the relative effectiveness of four types of lubricants for various functions. Some of the more important additives used to achieve particular lubricant characteristics are described in the section "Additives" in this article. However, it is first desirable to discuss the various possible forms of the lubricant (solutions, emulsions, and pastes).

Table 1 Relative effectiveness of different types of lubricants for various functions

Function	Relative effectiveness[(a)]			
	Compounded oil	**Oil-in-water emulsion**	**Semisynthetic solution**	Synthetic solution
Provide lubrication at high pressure (boundary lubrication)	1	2	3	**4**
Reduce heat from plastic deformation (heat transfer)	5	2	2	**1**
Provide cushion between workpiece and die to reduce adhesion and pickup (film thickness)	1	2	3	**4**
Reduce friction between die and workpiece and, thus, heat	1	2	2	**1**
Reduce wear and galling between tool and workpiece (chemical surface activity)	4	3	2	**1**
Flushing action to prevent buildup of scale and dirt (fluid flow)	5	4	3	**2**
Protect surface characteristics; nonstaining	5	2	2	**1**
Minimize processing costs, welding, cleaning, and painting	5	2	2	**1**
Minimize environmental effect, air contamination, and water recovery	**5**	**2**	**2**	**1**

(a) 1, most effective; 5, least effective.

Solutions. A fluid in which all components are mutually soluble is a solution. All of the fluids mentioned may form solutions, with soluble additives selected for their mutual solubility with the fluid and for their effect on properties. It is common practice in the field to call water-base solutions synthetic fluids or lubricants; oil-base solutions are called compounded oils; synthetic fluids that are compounded are also called synthetic lubricants. This shop language can be misleading if not understood.

Aqueous solutions have the highest cooling capacity and tend to promote operational cleanliness. Resistance to biological contamination is superior except for the possible formation of mold. Corrosion protection and operator skin inflammation (dermatitis) are potential problems.

Compounded oils tend to have superior thick-film lubrication properties and resistance to biological attack. They also provide good corrosion protection. On the other hand, workpiece cleanliness, cleaning of parts, welding, and higher end-use cost may be problems.

Synthetic compounded lubricants have superior high-temperature properties, but much higher costs restrict their use in forming operations.

Emulsions. A mixture in which one immiscible fluid is suspended in another in the form of droplets is called an emulsion. The continuous phase in most sheet metal forming operations is water. The suspended phase is oil or a synthetic fluid and may contain solid lubricants such as graphite, mica, or sodium carbonate. The continuous phase also may contain additives. In all cases, however, stabilization of an emulsion requires one or more surface-active agents (surfactants), finely divided solids, or special mixing techniques.

Surfactants such as fatty acids exist in an emulsion with the acid radical of the molecule preferentially soluble in the water, and the organic, fatty portion of the molecule dissolved in the oil phase. In this way the surfactant acts as a glue across the water/oil interface, thereby improving stability. This phenomenon is illustrated in Fig. 2, which shows the emulsifier action of three different emulsion systems as droplets of oil are suspended in water. In Fig. 2(a), the surface-active ingredients sodium cetyl sulfate and cholesterol are close-packed at the oil/water interface, thus producing an excellent tight emulsion. In Fig. 2(b), the sodium cetyl sulfate and oleyl alcohol interact at the interface to form a poor, or loose, emulsion. The effect of the surfactants is minimal in reducing the surface free energy of the oil droplets; therefore emulsion stability is poor. Figure 2(c) illustrates an intermediate case, which may be desirable in some cases because relatively large oil droplets occur, rather than the small droplets that are present in a tight emulsion. The presence of larger oil droplets may benefit the performance of the lubricating film.

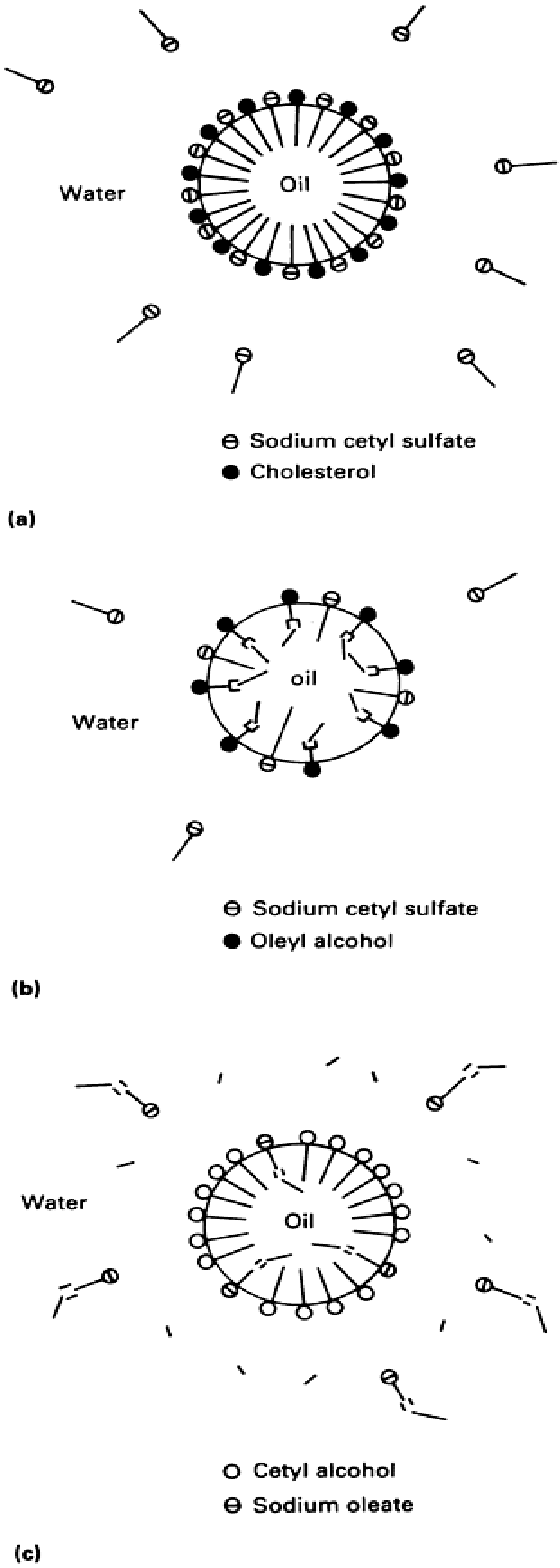

Fig. 2 Three types of emulsions seen in sheet metal forming lubricants. (a) Excellent (tight) emulsion. (b) Poor (loose) emulsion. (c) Intermediate case with poor emulsion stability.

A special case of emulsion is frequently used in sheet metal forming to take advantage of the cooling properties of water and the lubricity of suspended oil. The particle size of the suspended oil is small enough that the lubricant concentrate, even when water is added, remains clear rather than turning white or off-white, as is typically the case with an emulsion.

Emulsions generally are relatively easy to remove from parts and equipment. They provide superior cooling, but may be more susceptible to biological contamination and have less effective lubricating and corrosion-preventive properties than other lubricants.

Pastes, Suspensions, and Coatings. When difficult press work operations require film strength in excess of that which is possible with solutions and emulsions, a paste, suspension, or coating may prove to be the best lubricant choice.

Pastes may be oil- or water-base, pigmented or nonpigmented. Pigments include talc, mica, or similar inactive but effective high-strength barrier film formers. Sodium or potassium soaps are widely used in the production of all types of pastes. Buildup of lubricant in the tooling and the necessary cleaning of workpieces are the main drawbacks of pastes.

Suspensions are frequently used to provide a desired barrier film. Fine particles of a solid, such as graphite, Teflon, or clay, are suspended in a fluid carrier. Surfactants of an appropriate chemistry are generally required in order to provide suspension stability. Stability and lubricant buildup on dies are potential problems during use.

Coatings applied to sheet metal surfaces--most often at the sheet or strip finish mill--have been used alone or as undercoatings to lubricants applied at the press. Water-soluble polymers, phosphate conversion coatings, soap solutions, and organic resins (either with or without other additives), are used to form the coating. Generally, coatings are applied by dipping or spraying and then are heated to promote film formation and adhesion. Control of deposit thickness and chemistry, as well as removal, may be operating problems during production.

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Selection and Use of Lubricants in Forming of Sheet Metal

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Lubricant Chemistry

Tool life and part integrity are directly affected by lubricant choice. The mechanical and chemical properties of the lubricant are influenced by operating conditions (temperature, pressure, geometry), as well as by the surface characteristics of the tool and workpiece. Further, the lubricant chemistry has important implications with respect to cleaning costs, welding integrity, corrosion of equipment and parts, recycling and disposal costs, and operator health and safety.

Carriers (Bases)

The chemical components of the lubricant consist of a combination of a carrier (base) with various additives. Different lubricants have different viscosities: fluids, solids, pastes, or gels may be used. Fluids are currently the most widely used lubricants for sheet metal forming. Lubricating fluids are based on mineral oils or solvents, synthetic fluids, water, or some combination of these.

Oils. Mineral oils or solvents used as a base for lubricants result from fractional distillation of crude oil. The oils are generally immune to biological attack, are readily recycled, have inherent lubricity and good metal-wetting characteristics, and afford some metal corrosion protection. The viscosity of selected oil or solvents varies over a wide range, as does the molecular structure (Fig. 3). The oils selected for use are complex organic compounds, as illustrated in

Fig. 1. The naphthenic oils (Fig. 1b) tend to be more reactive, while the paraffinic oils (Fig. 1a) are more oxidation resistant.

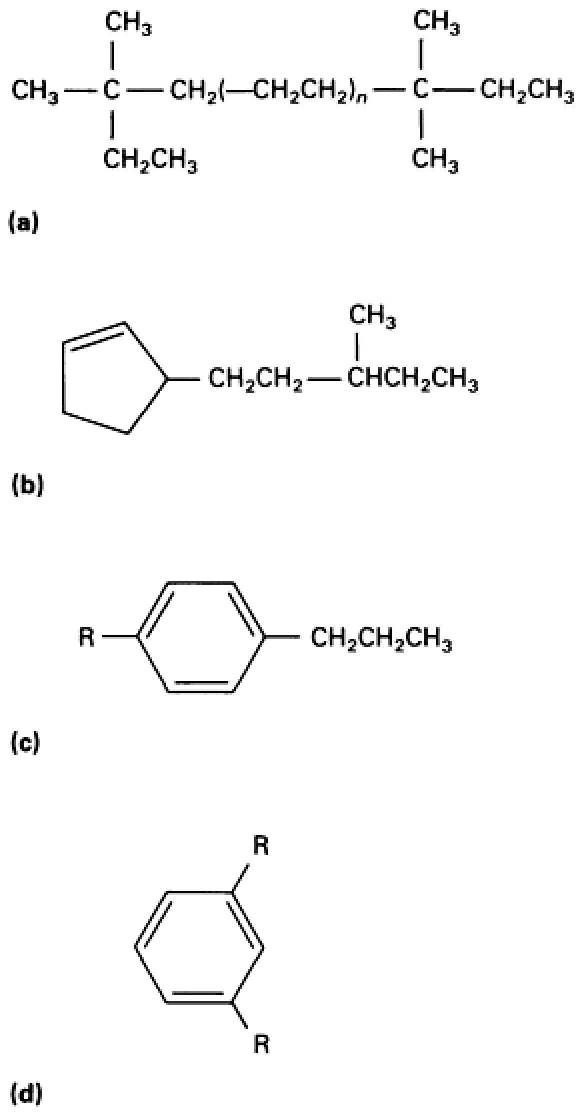

Fig. 3 Molecular structures of four mineral oil carriers used in sheet forming lubricants. (a) Paraffinic. (b) Napthenic. (c) Aromatic. (d) Alkylated aromatic. R, alkyl group.

Synthetic fluids with a specific molecular structure are becoming more widely used as lubricants. These fluids are based on synthesized hydrocarbons (polyalphaolefins) and polybutane derivatives (Fig. 4). The polyalphaolefins are similar to refined base oils in characteristics, but have superior resistance to oxidation. The polyglycols, polyolesters, and dibasic acid esters have superior high-temperature stability; however, they are not as readily combined with desired additives for corrosion protection or metalworking ability. Synthetic fluids are higher in cost than refined mineral oils.

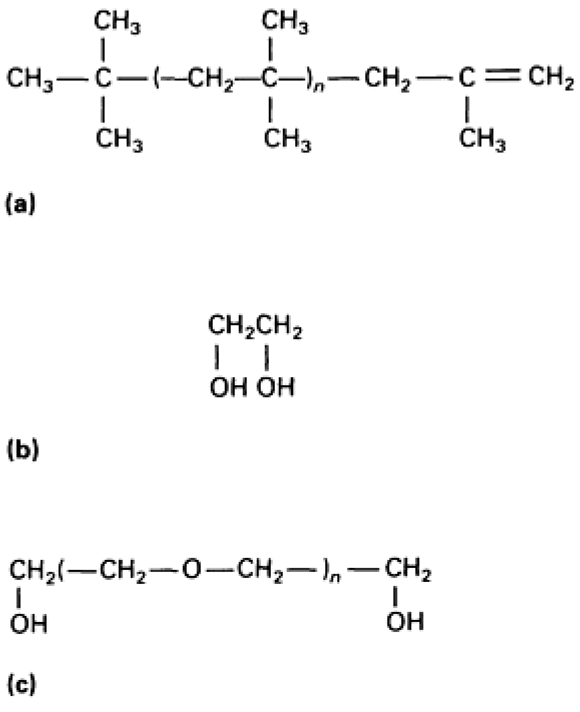

Fig. 4 Molecular structures of three types of synthetic fluids used as carriers for sheet forming lubricants. (a) Polybutene. (b) Ethylene glycol. (c) Polyethylene glycol ether.

Water. Because of the rising cost of refined mineral oil as well as the intrinsically high cost of synthetic fluids, water as the base fluid for sheet metal lubrication has become more widely used. A whole new technology is developing using various forms of water-base fluids (solutions, emulsions, gels, and pastes).

Of particular importance in the performance of water-base lubricants is the superior heat transfer capability of water. The sheet metal forming operation generates heat due to friction and metal deformation; water is the most efficient fluid for dissipating this heat.

Additives (Ref 4, 5)

Although uncompounded fluids may successfully serve as lubricants in light sheet metal forming, other processing requirements such as cleaning and corrosion protection may only be satisfied by the addition of chemical additives. Further, in difficult forming operations, additives must be used to provide necessary film characteristics to prevent die-part welding. Some common additives and their functions are discussed in this section.

Emulsifiers are used to promote stable emulsions and in some cases the cleanability of oils. Anionic and nonionic surfactants are the preferred emulsifiers. Typical nonionic emulsifiers are complex esters, fatty acids plus alcohols, monoglycerides, and ethoxylated alcohols. Some anionic surfactants used are soaps of long-chain fatty acids, alkyl aromatic sulfonates, and phosphate esters. Soaps are the most widely used anionic emulsifiers, but they may react with hard water to form compounds that can be difficult to remove from die or part surfaces.

Extreme-pressure (EP) additives are used when the fluid alone does not prevent excessive tool-part friction and wear. The film formed by interaction of the EP additive and the metal surface is created through chemical reaction. Sulfur-, chlorine-, or phosphorus-containing compounds react with the metal to form sulfides, chlorides, or phosphides. These chemical compounds are relatively weakly bonded and fracture and slide easily under the conditions of forming, providing a separating layer between tool and workpiece. Instability of the lubricant, as well as staining and corrosion, may be encountered during use. Cleaning and welding may also be adversely affected.

Thickeners are used to alter the flow characteristics (viscosity) of the fluid lubricant. Flow characteristics are influenced by temperature and pressure and affect lubricant performance. Organo-clays, polymers, natural gums, and metal hydroxides are used to vary flow properties. Stability of the lubricant system and buildup on tooling may cause operating problems.

Antimisting agents are used to reduce the incidence of airborne particles of the metalworking lubricant. Particularly in the case of solvents or low-viscosity oils, air mists may form that are detrimental to health and may also cause equipment-operating problems. To reduce mist formation, small quantities of polymers such as acrylates or polybutanes are added. When contacted with polymer, the lubricant base builds larger particles so that they do not readily form a mist.

Passivators are added to reduce the activity of the metal surface in order to prevent staining, particularly of nonferrous metals. Organic amines, sulfur, and nitrogen-containing compounds are used as passivators. Frequently, a so-called passivator is used in conjunction with corrosion prevention additives such as complex borates to improve the performance of each.

Antifoaming agents may be required to prevent foam formation if the lubricant is recirculated. Foaming is undesirable because it may interfere with reservoir cleanup and fluid flow or complicate fluid application. In order to prevent foam formation, the surface free energy of the film must be reduced. This may be achieved with a wide variety of additives, including silicones, amides, glycols, and fine particles of selected solids such as silica. The concentration of these agents needed for effective foam control is low; however, monitoring concentration to maintain control may be a problem.

Solid lubricants are often suspended in oil- or water-base fluids for extremely heavy-duty forming of sheet metal. These solids are graphite, carbonates, mica, Teflon, nylon, metal powders, and molybdenum disulfide. Buildup on tooling and cleaning of workpieces are potential problems during operations with these suspended-solid lubricants.

Corrosion inhibitors are frequently added to both water- and oil-base lubricants to provide part protection during processing and storage. Part corrosion may be a particularly difficult problem when water-base lubricants are used; however, sulfonates, carboxylates, borates, and phosphonates have been successful in alleviating corrosion. In oil-base lubricants, organic amines and sulfonates, as well as phosphates and unsaturated fatty acids, have been used successfully. Toluyltriazole has been used successfully to protect copper sheet from staining and corrosion.

Antimicrobial agents may be required in order to prevent the growth of anaerobic or aerobic bacteria. Bacteria frequently cause various operating problems (corrosion, odor, buildup of lubricant on workpieces, or emulsion instability) in emulsions or solutions that contain oil and water. In solutions formulated with various chemical components, bacteria will generally not be the problem; rather, formation of mold can cause malfunction of pumps and filters, and buildup on tooling and workpiece surfaces. It may also alter the solution chemistry. Biocides are not required when lubricant application does not involve repeated use or recirculation of the fluid. Care in handling as well as monitoring are important if biocides are to be used safely and effectively.

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Selection and Use of Lubricants in Forming of Sheet Metal

Elliot S. Nachtman, Tower Oil & Technology Company

Lubricant Effectiveness

Selection of an appropriate lubricant for a specific sheet metal forming operation or for a series of different operations is often a costly hit-or-miss proposition. There are, however, laboratory test procedures that can often reduce the possibility of error.

Because of the difficulty of translating laboratory test results to an operating condition, several simulation tests have been developed to screen lubricants for sheet metal forming. Small-capacity presses have been installed as test machines with the ability to control speed of forming, metallurgy of tools and work metal, and lubricant application method.

Strip drawing through a wedge-shaped die on a tensile machine has been developed to measure lubricant effectiveness. One end of the strip is gripped and pulled through the die. The die may also be modified in order to simulate drawing over draw beads. In these tests, resistance to sliding is measured for different lubricants applied to the strip.

Standard deep-drawing tests, such as the Swift cup test, can also be used to measure lubricant effectiveness. Poor surface finish, score marks, or splits are indications of unsatisfactory lubrication. More information on tests for measuring the drawability of metals is available in the article "Formability Testing of Sheet Metals" in this Volume; Ref 6 and 7 include more details on evaluation of lubricant effectiveness. The larger the blanks formed and the deeper the cup, the more effective the lubricant film. A wide variety of punch and sheet metal geometry, size, and tool material can be used along with the lubricant coating. The limiting draw ratio (LDR) depends on the draw ratio (DR), which is equal to the ratio of the blank diameter to the punch diameter. For a given punch diameter, the greater the blank diameter, the more severe the operation. The LDR is the maximum DR that results in a satisfactory cup.

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Selection and Use of Lubricants in Forming of Sheet Metal

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Lubricant Application (Ref 8)

The method used to apply the forming lubricant directly influences handling, part quality, and direct costs, as well as indirect costs (cleaning) and the metal forming operation itself. It is very important to deliver the appropriate volume of lubricant to the right location with the correct velocity at the right temperature in order to achieve optimum lubrication. Some of the more widely used application methods are discussed in this section.

Roller coating (Fig. 5a) is frequently used to coat strip before the strip enters the press. The strip passes through rollers, either top and bottom rollers or one or the other, and is coated with lubricant. The lubricant is fed to the roller either by dripping or by having the roller revolve, in a lubricant reservoir. The roller may be the same width as the strip and may be made of various materials. Pressure on the rollers can be varied to control lubricant thickness.

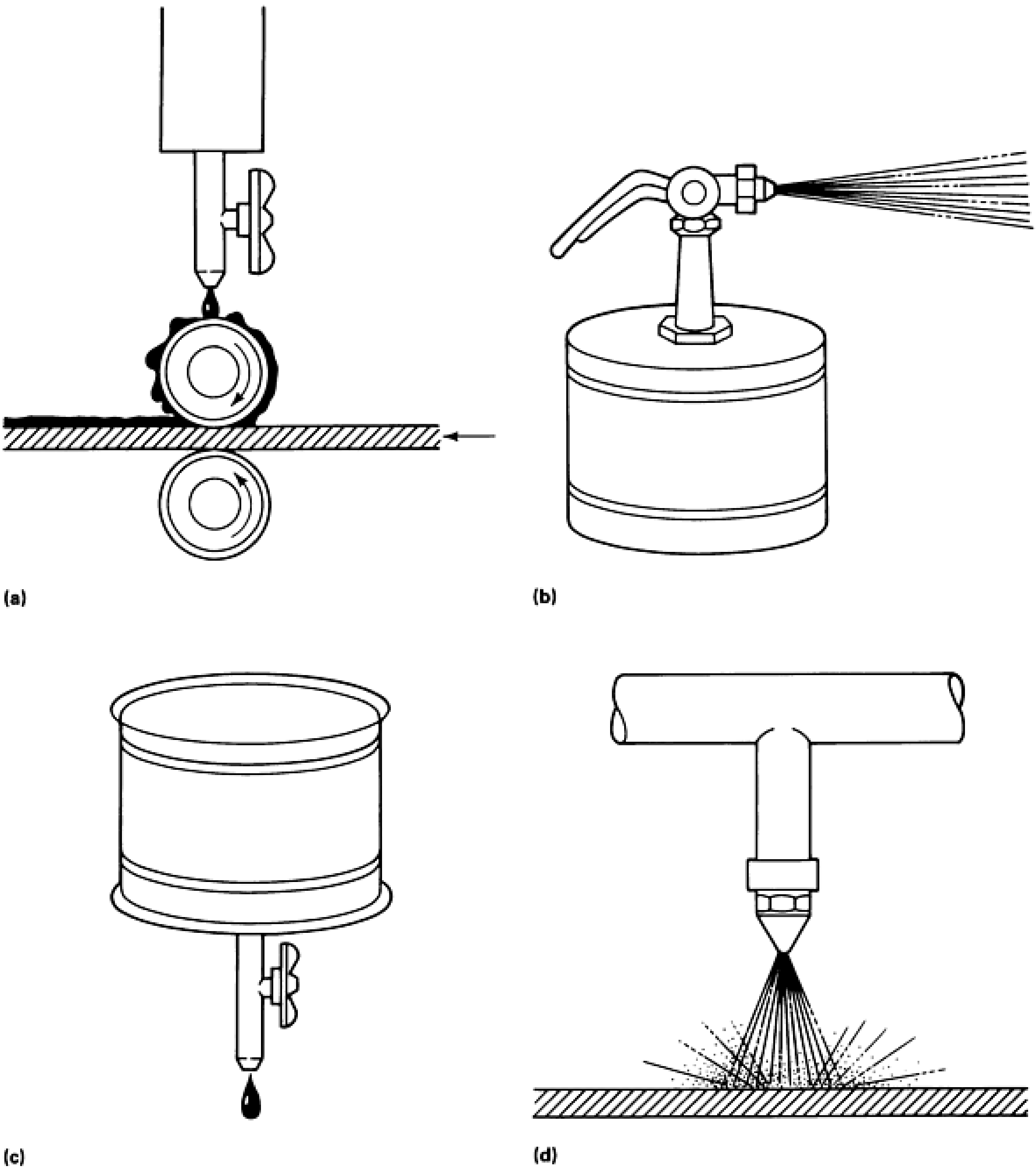

Fig. 5 Four common methods of applying lubricants. (a) Roller coating. (b) Spraying (simple spray apparatus shown here). (c) Drip application. (d) Recirculating flood.

Air or airless spray devices (Fig. 5b) are used to apply lubricant to preselected areas of the workpiece and/or tooling at the required frequency. Generally, higher-viscosity lubricants are applied by airless spray rather than by air spray units.

Drip application to the workpiece (Fig. 5c) can be used effectively when parts are relatively small and the cost of alternative application devices is not justified by the production demand. Heavy lubricants cannot be applied readily by this method, nor can the geometry of application be tailored selectively.

Flood application methods (Fig. 5d) require suitable press modifications to provide for recirculation of the fluid. Water-proofing to protect sensing elements must be provided for if a water-base fluid is used, and lubricant flooding must be controlled to confine it to the press work area. Flooding can be very useful when cooling is desirable and debris must be removed continually from the work area.

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Selection and Use of Lubricants in Forming of Sheet Metal

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Control of Lubricant Characteristics

Maintaining the physical and chemical properties of lubricants is central to control of the sheet metal forming process itself. Table 2 lists some important characteristics of metal-forming lubricants for which tests have been developed, and the applicability of testing to various types of lubricants. Control methods developed under the aegis of the American Society for Testing and Materials (ASTM) are widely used. Some of the ASTM test methods for important characteristics are discussed below.

Table 2 Lubricant characteristics and their applicability to various types of lubricants

Characteristic	Lubricant system						
	Oil-base	**Solvent-base**	**Solids**	**Water-base emulsions**	**Solutions (water)**	**Coatings**	Pastes
Particulate concentration	X			X	X		**X**
Emulsifier level				X			
Viscosity	X			X	X		
Sulfur concentration	X	X	X	X	X		
Chlorine concentration	X	X	X	X	X		
Particle size				X	X		
Solids content	X	X		X	X	X	**X**
Color	X	X		X	X		
Saponification number	X	X		X	X		**X**
Evaporation	X	X					

Flash point	X	X					
Molybdenum disulfide		X	X				
Graphite			X				
Carbonate			X				
Stearate			X				
Water content	X	X		X	X		**X**
Hard-water tolerance				X	X		
Stability	X	X		X	X	X	**X**
Bacteria, mold, fungus				X	X		**X**
Amine concentration				X	X		
Phosphate concentration				X	X		
Corrosion potential				X	X		**X**
pH				X	X		
Conductivity				X	X		**X**
Concentration				X	X		
Odor	X	X		X	X		
Coating thickness	X	X		X		X	
Foaming characteristics				**X**	**X**		

Viscosity (ASTM D 445, D 446, and D 88) is as important as any physical property of fluids used in sheet metal forming. Viscosity control is based on the measurement of the time required for a lubricant to flow through a fixed orifice at a controlled temperature.

Sulfur and chlorine (ASTM D 129 and D 808) are measured by carrying out a controlled combustion of a lubricant sample in a so-called bomb. ASTM D 129 describes a technique used for sulfur, and ASTM D 808 describes the appropriate technique for chlorine.

Corrosion potential can be very important in both evaluating the appropriateness of a specific lubricant, and for purposes of corrosion control during continued use. The methods most commonly used, for example, ASTM D 130 for copper-base metals and ASTM D 1748 for steel corrosion test strips, are based on the potential of water-lubricant interaction with the metal surface in a controlled way. Immersion in the fluid at elevated temperatures or exposure of a lubricant-coated strip in a humidity cabinet are techniques frequently used; they may be varied in many ways to attempt to duplicate as closely as possible particular operating conditions.

Emulsion stability is often of importance in maintaining a reproducible and reliable parts production line. A common method used to evaluate stability is ASTM D 1479, in which water available at the work site is used to make an emulsion. The emulsion is then stored for a specified period of time and evaluated. The concentration of oil in the bottom portion of the sample is a measure of the emulsion stability.

Fatty compounds are frequent components of a metal-forming lubricant. The test method ASTM D 94, using saponification number as a basis for fat determination, may be used as long as the basic fat component is known.

Hydrogen ion concentration (pH) is important in water-base lubricants. Variation of pH can be an important indication of fluid contamination or degradation due to biological or chemical reaction. Monitoring of pH on a regular basis can serve as an important control of fluid properties in a manufacturing environment. The pH can be measured with specially prepared pH paper or a pH meter.

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In-Plant Control

Very important test procedures that do not require sophisticated laboratory facilities or highly skilled personnel are used for in-plant control. Some of the test procedures useful for lubricant control in plants are discussed below.

Emulsion concentration is one of the most important characteristics of a lubricant in metalworking. Variation in concentration occurs during use because of water evaporation, selective drag-out, and, frequently, lack of emulsion makeup control. A number of control techniques can be used successfully in the plant. These include the use of titration, a refractometer, and acid split.

Titration can be used to measure either emulsifier concentration or, in turn, emulsion concentration. Anionic emulsifiers can be titrated by using a cationic material with an appropriate indicator. Water hardness effects on the titration must be accounted for to improve the reliability of the titration.

A refractometer may be used to measure concentration. The refractive index of the emulsion or water-base solution varies with concentration. Fluid compositions also vary; therefore, the refractive index must be calibrated for each fluid. Fluid contamination can cause a false index reading and may prevent use of this measurement method.

Acid split is used to determine water concentration in an emulsion. The emulsion is split by using a strong salt solution or acid. If oil (tramp or machine oil) enters the emulsion, then the acid split will reflect all the oil present, and thus incorrect concentration values will be obtained.

Corrosion tests have been developed for measuring the possibility of change in this characteristic of a lubricant. A plant procedure is based on taking pieces of sheet metal to be formed, coating them with the lubricant to be used, and placing the specimen in a plant location most likely to encourage corrosion. Experience with a procedure standardized as much as possible with respect to lubricant, metal, and environment can be useful for plant testing. Various tests using metal coupons coated with lubricant and subjected to a high-humidity environment (humidity cabinet) can also be used. Cast iron blocks and chips can be used for accelerated testing, using any of a number of procedures involving contact with lubricant and subsequent aging in a selected room or plant environment.

Biological testing in the plant can be carried out to advantage if reasonable control of test conditions can be maintained. Dip sticks have been developed that consist of a plastic substrate coated with a nutritive gel selected for growth of either bacteria or mold, as desired. The method, though qualitative, is useful as a fluid control procedure in the plant. References 9 and 10 contain more information on control of lubricant characteristics.

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Lubricant Toxicity (Ref 11)

Because the environment, as well as the biochemical characteristics of the human body, are extremely complex, effects on health in the workplace are difficult to isolate and measure. In general, exposure to sheet metal working lubricants may result in dermatitis, acne, tumors, and pigment changes in the skin. All of these may occur also from exposure to other by-products in the workplace, in the home, or in the environment in general.

Skin patch tests of a suspect material may reveal the potential for dermatitis. A small patch is coated with the material in question and taped to the skin.

The Ames test, which measures the effect of a test material on bacteria, has been used as a screen for possible causes of mutagenic effects in humans. Most materials which may cause mutagenic effects are not necessarily carcinogenic. Evaluation of a metal-working fluid for its possible adverse biochemical effects should be a part of the lubricant selection process. The recent requirement of a completed material safety data sheet (MSDS) on all chemicals and their mixtures can serve as an important source of information for the screening procedure. Appropriate work practices and personal hygiene can significantly reduce adverse biochemical effects.

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Microbiology of Lubricants

Microbiological contamination of lubricants can be a serious problem. When water is part of the composition of a metalworking fluid, microbes can grow in the fluid, either deliberately or accidentally as a result of contamination. Bacteria are the main problem in mineral-oil emulsions; in water solutions, molds and yeasts are the principal microbiological contaminants. These microbes feed and grow on one or more of the lubricant constituents or contaminants. If they grow by attacking a single constituent, the constituent can be so altered chemically in short order, frequently as rapidly as in one day, that the lubricant no longer operates effectively as intended. An emulsifier may no longer promote stable emulsification, a rust-preventive additive may no longer prevent rust formation, or a lubricity additive may no longer provide an effective lubricant film.

Anaerobic bacteria, often present in fluids, operate in the absence of oxygen and cause the breakdown of sulfur compounds in the fluid, creating the strong rotten-egg odor that may occur in the fluid reservoir. Aerobic bacteria grow in the presence of oxygen and are the most aggressive in promoting overall chemical breakdown of the lubricant. Molds and yeasts, when present, do not drastically degrade lubricant chemistry; they do, however, block filters, and may build up on tools and generally have a disagreeable feet and appearance.

Bacteria and mold or yeast infestation of the lubricant may be controlled by appropriate formulation of the lubricant, use of an effective biocide, pasteurization, and sometimes effective filtration. Good practices involving clean work habits and avoidance of contamination of the fluids by plant dirt, food, and so forth, will contribute to longer life of fluids. References 12, 13, and 14 contain more detailed information on the effects of microbiological contamination.

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Selection and Use of Lubricants in Forming of Sheet Metal

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Environmental and Occupational Health Concerns

In recent years, concerns about the environment, health, and safety have led to the enactment and enforcement of a number of laws and regulations. Some of the most important of these as they affect the use and disposal of sheet metal forming lubricants are discussed below.

The Environmental Protection Agency (EPA) was established by the U.S. Congress to ensure protection of the environment in which we live and work. Over the years, clean air and water have become a subject of increasing concern. Several acts addressing these concerns have been passed, requiring enforcement by EPA. Some of the more important are:

- The Toxic Substance Control Act, designed to regulate the manufacture, processing, distribution, use, and disposal of chemicals. Material Safety Data Sheets (MSDS) are required for all lubricants, with information about the toxicity, handling, and safe disposal of such materials
- The Clean Air Act, as amended in 1977, which specifically regulates the concentration of volatile lubricant components in air. It established permissible concentrations that must not be exceeded in the workplace
- The Water Pollution Control Act regulates the passage of waste into the water supply. The condition, type, and concentration of waste that may be permitted to enter the water supply is established and regulated. Disposal of lubricants, if it involves entry into a water system, must satisfy the appropriate criteria for disposal
- The Resource Conservation and Recovery Act (RCRA) regulates hazardous waste management and disposal. Lubricants, if they contain hazardous components, are categorized as hazardous materials and must be stored, transported, and disposed of as prescribed by regulation. Appropriate records must accompany each step, as regulated.

The Occupational Safety and Health Administration (OSHA) is concerned with safe working conditions in the workplace. The impact of occupational safety and health legislation on lubrication as practiced in the workplace concerns prevention of occupational illnesses due to hazardous or toxic chemicals. Companies are required to record all injuries or illnesses as they occur. The relative toxicities of approximately 1500 chemicals found in the workplace are being systematically established by OSHA. Regulation of these chemicals is occurring as appropriate information is developed.

Product liability is of increasing concern because of the exploding cost of defending a product liability case as well as the bringing of nonmeritorious claims to court. At the same time, increasing attention has been paid to safer, better products as well as a safer, better work and living environment. Lubricant manufacturers and users are faced with claims for improper formulation, usage, and performance. Appropriate steps to anticipate and eliminate possible product liability claims occupy increasing time and attention on the part of both the supplier and user of lubricants.

The laws and regulations described in this article illustrate only a few of the more important ones that govern and regulate the use, disposal, and manufacture of sheet metal forming lubricants. New developments in lubrication technology will surely be conditioned by this regulatory and liability environment.

Selection and Use of Lubricants in Forming of Sheet Metal

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Lubricant Selection

Selection of an appropriate lubricant for a specific sheet metal forming operation depends on a number of diverse criteria that must be successfully satisfied:

- Effect of lubricant on the forming operation (effect on both tool wear and part tolerance and finish)
- Application method and efficiency thereof
- Maintenance of lubricant performance, taking into account problems of recirculation, testing, and disposal
- Corrosion of tooling, machine, and finished parts
- Cleaning
- Welding, painting, or coating of the work material
- Worker response
- Supplier support
- Toxicity and health
- Cost, including cost of lubricant and its effect on overall product cost

A wide variety of operations are commonly used to form parts from sheet metal. Blanking, piercing, ironing, forming, drawing, fine blanking, and spinning are some of the operations that are used separately or in sequence to form parts, and are discussed in other articles in this Volume. Unlike bulk deformation processes, the temperature increase in sheet metal forming is generally low unless the complexity of deformation and/or the strength of the material is very high. Generally, except in the case of ironing, sheet metal forming involves little or no thickness change. Deformation is generally biaxial in the plane of the sheet. However, there can be considerable relative motion between tool and workpiece. Further, the area of tool contact is often large. Surface finish and part tolerance depend on tool geometry and finish, speed and temperature of deformation, the combination of forming steps required, and the choice and application of lubricant. Table 3 illustrates some of the types of lubricants used successfully in sheet forming of various metals at room temperature and at elevated temperatures. The type of metal, complexity of part shape, part tolerance, and surface finish specifications all influence the choice of lubricant. The more critical the operation, the more important the need for film strength and adhesion of the lubricant film to the metal substrate. Further, there may be a need for increased film thickness.

Table 3 Lubricants for forming of specific sheet materials

Metal or alloy	Recommended lubricants for:

	Cold forming	Hot forming
Aluminum alloys	Synthetic solutions, emulsions, lanolin suspensions, water suspensions, soap solutions, mineral oil plus fatty oils	**Graphite suspension**
Copper alloys	Emulsions plus fatty oils, mineral oils plus fatty oils, soap suspensions, water suspensions, tallow suspensions, synthetic solutions	**Pigmented pastes, graphite suspensions**
Magnesium alloys	Solvent plus fatty compounds, mineral oils plus fatty compounds	**Graphite plus molybdenum disulfide, soap plus water, tallow plus graphite**
Nickel, nickel-base alloys	Emulsions, mineral oils plus EP additives, water plus chlorine additives, conversion coatings plus soap	**Graphite suspension, molybdenum disulfide suspension, resin coating plus salts**
Refractory metals and alloys	Copper plating	**Molybdenum disulfide, graphite suspension**
Carbon and low-alloy steels	Emulsions, soap pastes, water, fatty oils plus mineral oils, polymers, conversion coating plus soap, molybdenum disulfide or graphite in grease, synthetic solutions	**Graphite suspension**
Stainless steels	Fatty oils plus mineral oil, water, polymers, conversion coating plus soap, mineral oil plus EP additives, pigmented soaps	**Graphite suspension**
Titanium, titanium alloys	**Water, pigmented soaps, polymers, conversion coating plus soap**	**Graphite and molybdenum disulfide suspension**

The relationship of many process variables, including speed of operation; tool geometry; number and severity of discrete forming operations; worker exposure to lubricant; fluid makeup; circulation, recycling, and disposal; and the effect of the lubricant film on subsequent operations such as welding, cleaning, and plating influence the choice of lubricant. A simple recommendation based on material, tooling, and process may prove to be far from optimum; many other factors can and should influence lubricant choice.

Nevertheless, in order to give some background information that may provide guidelines in the selection of a lubricant, a number of processes applied to various sheet materials relative to the choice of an appropriate lubricant will be discussed below.

Forming of Carbon and Low-Alloy Steels

A wide range of lubricants are successfully used to form steel sheet. The speed of operation and resulting temperature, as well as thickness of stock and complexity of shape, influence lubricant choice. Oil-, solvent-, and water-base fluids, as well as pastes and gels, may be effective, depending on operating conditions.

Blanking and Piercing. Straight oil, compound oil, or emulsions with sulfur, chlorine, and/or fatty esters are used for most operations. Solutions may be used when the operation is less severe.

Coining. Because die buildup must be prevented, light oils straight or compounded with fatty additives or chlorine-containing additives are used. Water-base solutions may be effective when formulated with alkanolamides, fatty esters, and/or long-chain derived fatty acid soaps.

Other Operations. Compression, tension, binding, or shearing forces may occur during forming on a particular part; therefore an appropriate choice of lubricant viscosity and chemistry varies widely from part to part, depending on part configuration. Essentially all classes of lubricants have been successful. Generally, however, the present emphasis is on formulating solutions or emulsions that prevent galling or tearing of the sheet metal.

Multislide Forming. Light compounded oils and water-base emulsions and solutions with chlorine and fatty additives are used successfully. Care must be taken to prevent buildup on the dies and corrosion of machine or formed parts when water-base lubricants are used.

Deep Drawing. Progressive- and multiple-die sets are being used more and more frequently, and thus carry-through of the lubricant becomes more and more important. Multiple-die lubrication sites to provide die lubrication at succeeding stations may be an alternative method for ensuring adequate lubrication. Compounded oils and emulsions or pastes are being used successfully. Chlorinated compounds and fatty esters are commonly chosen additives. Soap-base lubricants are also applied frequently and successfully.

Spinning. Both lubrication and cooling are important effects provided by appropriate lubricant choices. Water-base emulsions and solutions containing chlorinated or sulfochlorinated additives and/or fatty esters are used. Nonpigmented water-base pastes are also effective.

Roll Forming. Water-base emulsions and solutions have been used to form both bare and coated steel. In the case of galvanized steel, white rust can be a problem if water-base lubricants are used and not properly applied and removed. Solvent-base fluids are often the preferred lubricant, particularly when minimal residue is important. Light oils may also be used, both compounded and uncompounded with chlorinated compounds, soaps, or fatty esters.

Forming of Stainless Steels

In general, stainless steels are more difficult to form than carbon or low-alloy steels because of their higher strength. Care must be taken in the choice of chemically reactive lubricants containing sulfur or chlorine. Furnace treatments should be carried out on formed parts after removal of the lubricant film. Sometimes this step may be avoided if high molecular weight polymers or complex fats are used in lubricant formulation.

Blanking and Piercing. Solutions and emulsions containing sulfur and/or chlorine are particularly effective for lighter-gage material. Oils compounded with sulfur and/or chlorine are effective in heavier-gage material.

Multiple-Slide Forming. Oil- and water-base fluids are successfully used. Recently, solutions with complex esters and fatty compounds have been used, successfully eliminating cleaning of parts before use.

Deep Drawing. Because of the high work-hardening characteristics of some stainless steels and attendant high die pressure and temperatures, oils with extreme-pressure additives such as sulfur, chlorine, and phosphorus are most often the lubricants selected. Pigmented and nonpigmented pastes are often used with or without extreme-pressure additives.

Roll Forming. Thin-gage sheet may be formed by application of solvent-base as well as water-base fluids at the roller. Thicker material and/or more complex shapes, on the other hand, require oils or emulsions compounded with fatty oils, extreme-pressure additives, and soaps.

More information on forming of stainless steel sheet is available in the article "Forming of Stainless Steel" in this Volume.

Forming of Heat-Resistant Alloys

In general, heat-resistant alloys (complex iron-, nickel-, and cobalt-base materials) as a class use lubricants that are suitable for stainless steel. Cobalt-base alloys are particularly difficult to form into sheet because of their high strength and relative inertness. Care must be taken to avoid embrittlement by sulfur diffusion in nickel-base alloys. Pigmented lubricants are difficult to remove after forming, and this may discourage their use. More information on forming of heat-resistant alloy sheet is available in the article "Forming of Heat-Resistant Alloys" in this Volume.

Blanking and Piercing. Soap-base oils and emulsions are used with and without chlorinated additives. Wax and polymer additives may be used if the blanking step is complex.

Spinning. Chlorinated additives in an oil base as well as in emulsions containing fatty esters are effective. Soap-base pastes are also often chosen for application.

Forming of Refractory Metals and Alloys

Although warm or hot forming of materials such as molybdenum, tungsten, tantalum, niobium, and their alloys is often required, room-temperature deformation is successfully carried out using metal coatings with auxiliary lubricant application.

Drawing. Fluorinated complex polymers are used to form tantalum; graphite in fat has been used for forming niobium. Both metals may be formed at room temperature.

Spinning. Room-temperature spinning of niobium has been carried out using molybdenum disulfide in oil or graphite in a soap-base paste.

Forming of Aluminum and Aluminum Alloys

Aluminum and aluminum alloy sheet may be stained or corrosion may occur through the improper choice of lubricant. Aluminum oxides build up during forming and may necessitate cleaning of both parts and equipment. The lubricant may contribute to undesirable buildup. Lubricant chemistry differs from the chemistry used in formulating lubricants for steel and refractory metals or high-temperature alloys.

Blanking and Piercing. Oils or compounded oils of light viscosity are used. Fatty esters and chlorinated compounds are the compounding additions frequently chosen. Fatty compounded emulsions as well as soap-base solutions are also effective. Oxide deposits may be a problem with some compounded oils and emulsions.

Roll Forming. Solvent-base lubricants containing fatty esters as well as emulsions and solutions are frequently used lubricants. Light viscosity oils also may be used; however, oxide buildup on the rolls can be a problem. Soap-base lubricants are used if care is taken to prevent soap deposits.

Deep Drawing. Viscosity and lubricity are critical attributes of lubricants. Water-base fluids may be used if appropriate fatty additives and/or soap additives are used to provide the required barrier and lubricity. Oils of varying viscosity or dry soap films may be used for more difficult draws. Pastes that are soap- and fat-base have been applied successfully. Progressive draws involving multiple steps frequently should provide lubrication at more than one station.

Spinning. Waxes and soaps are effective lubricants. Colloidal graphite suspended in an oil carrier is also used successfully. Cleaning can be difficult depending on the severity of the spinning operation.

Forming of Copper and Copper Alloys

Staining and/or corrosion of these metals in their forming is of particular concern. Lubricants chosen must be formulated to prevent both from taking place.

Multislide Forming. Soap- and fat-containing oil and/or water-base lubricants are effective. Solvent-base lubricants containing a fatty ester or an inhibited chlorine-containing compound may be used.

Roll Forming. All classes of lubricants using fatty compounds and/or soap-base additives are used successfully in water, oil, or solvent fluids.

Deep Drawing. Depending on severity of draw, oil- or water-base lubricants in fluid or paste form may be effective. Soap and fatty compounds, as well as inhibited chlorine-containing compounds, are commonly used additives.

Forming of Magnesium Alloys

Magnesium alloys are most often formed warm or hot. Forming at these elevated temperatures is not normally required for other metals of industrial importance.

Drawing. At temperatures to approximately 120 °C (250 °F), soap-base lubricants, fatty esters, polymer additives in oil and water, and pastes formulated with chlorinated additives are successful lubricant systems. At temperatures in excess of 120 °C (250 °F), the choice of lubricant is restricted to synthetic fluids formulated with soap, fatty esters, and/or chlorinated compounds. Above 230 °C (450 °F), graphite and/or molybdenum disulfide in various carriers are preferred.

Spinning. At elevated temperatures, the synthetic fluids compounded with graphite and/or molybdenum disulfide are applied. Water carriers for the solid lubricants may be preferred to reduce the occurrence of smoke and the possibility of fire.

Forming of Nickel and Nickel-Base Alloys

These metals are difficult to wet with lubricants; thus heavy-duty lubricants with exceptional film-priming characteristics are necessary for effective lubrication. On the other hand, lubricants containing sulfur, chlorine, or solid additives such as zinc oxide or lead carbonate can, if not removed from the nickel surface, can cause embrittlement of the metal.

Shearing, Blanking and Piercing. Oils incorporating sulfur- or chlorine-containing additives may be used. Water-base lubricants of similar composition may be applied if they are removed as soon as possible after forming to prevent embrittlement. Fatty esters and polymers have grown in application as components of formulated lubricants.

Deep Drawing. Soap-base pastes as well as oils with fatty esters, amides, and/or sulfur and chlorine additives have been used. Emulsions fortified with amides and polymers also have been formulated and applied.

Spinning. Pigmented pastes and chlorinated wax in oils are successful lubricants. Plain waxes and soaps are frequently important components of these lubricants.

Forming of Titanium Alloys

Galling of titanium alloys is a particular problem because of the affinity of the metal for die materials. Notch sensitivity and embrittlement may also lead to splitting or cracking of formed parts. Cold and warm forming may be carried out with suitable films designed to prevent metal-to-metal contact. Frequently, overlays of steel sheet or plastic sheet are used with an auxiliary lubricant.

Deep Drawing. Overlays are often used with oil-base lubricants formulated with chlorinated waxes. Oxidized or phosphated coatings are successful in relatively severe drawing operations at elevated temperatures. Graphite and/or molybdenum disulfide in oil may be used.

Roll Forming. Oils compounded with sulfurized or chlorinated fats are used. Oil- or water-soluble polymers may also be added. Chlorinated waxes, high molecular weight waxes, or polymers soluble in oil may be effective for relatively moderate deformation.

Spinning. Colloidal graphite and/or molybdenum disulfide blended in oil may be used at temperatures up to approximately 205 °C (400 °F). Chlorinated wax and/or sulfurized fat in oil also may serve as lubricants. At higher temperatures, fillers such as bentonite or mica with graphite and/or molybdenum disulfide formulated into a grease are used successfully.

Forming of Platinum-Group Metals

Surface contamination due to metal contact at surfaces with iron or other metals may adversely affect surface integrity and electrical resistivity. Separation of tool and workpiece by an appropriate lubricant film is critical. Platinum and palladium can be formed by most standard sheet metal forming operations (blanking, piercing, and deep drawing). Cold welding of the workpiece to the tooling must be avoided, and therefore continuous lubricant films are important in operation. Many of the lubricants used for forming copper alloys may also be used for forming platinum and palladium. Rhodium and iridium are more difficult to form, and ruthenium and iridium are extremely difficult to form.

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Introduction

PUNCH PRESSES are used for bending, flanging, and hemming low-carbon steel when production quantities are large, when close tolerances must be met, or when the parts are relatively small. Press brakes are ordinarily used for small lots, uncritical work, and long parts.

To estimate the press capacity needed for bending in V-dies, the bending load in tons can be computed from:

$$L = \frac{lt^2kS}{s}$$

where L is press load (in tons of force), l is length of bend (parallel to bend axis) (in inches), t is work metal thickness (in inches), k is a die-opening factor (varying from 1.2 for a die opening of $16t$ to 1.33 for a die opening of $8t$, S is tensile strength of the work metal (in tons of force per square inch), and s is width of die opening (in inches).

For U-dies, the constant k should be twice the values given above. Bending of flanges with wiping dies is discussed in the section "Straight Flanging" in this article. The characteristics of the various presses commonly used for forming sheet metal are summarized in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume.

Bendability and Selection of Steels

Figure 1 shows the types of bends for which the standard AISI tempers of cold-rolled carbon steel strip are suited. Stock of No. 1 temper is not recommended for bending, except to large radii. Stock of No. 2 temper can be bent 90° over a radius equal to strip thickness, perpendicular to the rolling direction. Stock of No. 3 temper can be bent 90° over a radius equal to strip thickness, parallel to the rolling direction; it can also be bent 180° around a strip of the same thickness when the bend is perpendicular to the rolling direction. Stock of No. 4 or No. 5 temper can be bent 180° flat on itself in any direction. The No. 5 temper stock may develop stretcher strains and should not be used if these markings are objectionable.

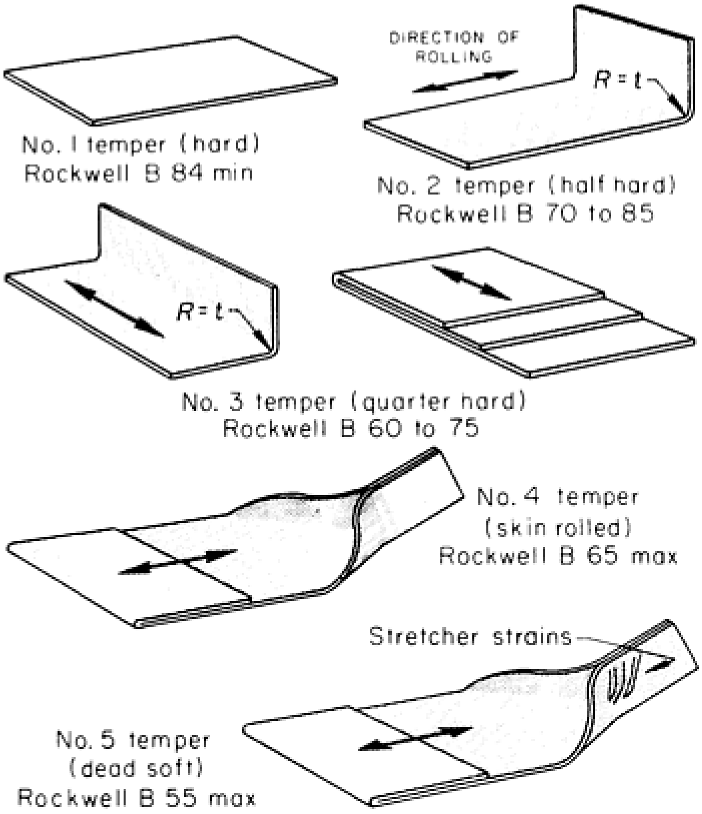


Fig. 1 The most severe bend that can be tolerated by each of the standard tempers of cold-rolled carbon steel strip. Stock of No. 1 (hard) temper is sometimes used for bending to large radii; each lot should be checked for suitability, unless furnished for specified end use by prior agreement. Hardnesses shown are for steel containing 0.25% C (max) in the three hardest tempers and 0.15% C (max) in the No. 4 and 5 tempers. Hardness for No. 1 temper applies to thicknesses of 1.8 mm (0.070 in.) and greater; for thinner sheet, hardness would be a minimum of 90 HRB.

Table 1 shows the effect of carbon content of some grades of carbon steel strip and sheet on bend radius in standard bend tests. Table 2 shows the effect of quality or temper on minimum bend radius of 1008 or 1010 steel sheet. Table 1 in the article "Press Forming of High-Carbon Steel" in this Volume shows the effect of composition on minimum bend radius by comparing the minimum radii for common grades of carbon and low-alloy steels.

Table 1 Bending limits for hot-rolled commercial-quality carbon steel strip and cold- or hot-rolled carbon steel sheet

If greater ductility is needed, drawing quality or physical quality steel can be used.

Carbon, %	**Bending limit**
0.15 or less	180° bend flat on itself, in any direction
0.15-0.25	180° bend around one thickness of the material, in any direction

Table 2 Minimum bend radii for 1008 or 1010 steel sheet

Quality or temper	Minimum bend radius, mm (in.)

	Parallel to rolling direction	Across rolling direction
Cold rolled		
Commercial	0.25 (0.01)	**0.25 (0.01)**
Drawing, rimmed	0.25 (0.01)	**0.25 (0.01)**
Drawing, killed	0.25 (0.01)	**0.25 (0.01)**
Enameling	0.25 (0.01)	**0.25 (0.01)**
Cold rolled, special properties		
Quarter hard[(a)]	1t[(b)]	$\frac{1}{2}t$
Half hard[(c)]	NR[(d)]	**1*t***
Full hard[(e)]	NR	**NR**
Hot rolled		
Commercial, mm (in.)		
Up to 2.3 (0.090)	$\frac{3}{4}t$	$\frac{1}{2}t$
Over 2.3 (0.090)	$1\frac{1}{2}t$	**1*t***
Drawing, mm (in.)		
Up to 2.3 (0.090)	$\frac{1}{2}t$	$\frac{1}{4}t$
Over 2.3 (0.090)	$\frac{3}{4}t$	$\frac{1}{2}t$

(a) 60-75 HRB.

(b) *t*, sheet thickness.

(c) 70-85 HRB.

(d) NR, not recommended.

(e) 84 HRB minimum

Press Bending of Low-Carbon Steel

Minimum Bend Radius

Minimum bend radii are limited by angle of bend, length of bend, material properties, condition of the cut edge perpendicular to the bend line, and orientation of bend with respect to direction of rolling. Minimum bend radii are larger for a larger angle of bend.

Parts in which the length of the bend (direction parallel with the bend axis) exceeds eight times metal thickness have a fairly constant minimum bend radius. When the bend length is less than eight times metal thickness, the bend radius generally must be greater.

The temper of the metal affects the minimum bend radius (Fig. 1). Steel in the higher tempers (low hardness and high ductility) can be bent 180° to a sharp radius without cracks or tears. Bend radii can usually be smaller for bends made across the rolling direction than for bends made parallel to it. However, examples in this article demonstrate that sharp bends are often made parallel to the rolling direction.

Effect of Edge Condition. When bending low-carbon steel, the condition of the edge perpendicular to the bend axis has little effect on the minimum bend radius. Steels that are susceptible to work hardening or hardening by heating during gas or electric-arc cutting may crack during bending because of edge condition. For these steels, it is often necessary to remove burrs and hardened edge metal in the bend area to prevent fracture. Edges can be prepared for bending by grinding parallel with the surface of the sheet and removing sharp corners in the bend area by radiusing or chamfering.

If the burr side is on the inside of the bend, cracking is less likely to occur during bending. This is important on parts with small bend radii in comparison with the metal thickness and on parts with metal thickness greater than 1.6 mm ($\frac{1}{16}$ in.), because fractures are likely to start from stress-raising irregularities in the burr edge if it is on the outside of the bend.

Effect of Metal Thickness. Minimum bend radii are generally expressed in multiples or fractions of the thickness of the work metal. On parts that require a minimum flange width or a minimum width of flat on the flange, stock thickness will limit both of these dimensions. If thickness is not critical in the design, the use of thinner stock can make the bending of small radii and narrow flanges feasible.

Carbon Steels. Table 2 lists minimum bend radii for 1008 or 1010 hot- and cold-rolled steel sheet in each of the available qualities. Bend radii for higher-carbon steels and two low-alloy steels are given in Table 1 in the article "Press Forming of High-Carbon Steel" in this Volume.

As suggested by Table 2 in this article, the quality of steel has a major influence on the minimum bend radius that can be made in it. This is especially true of hot-rolled steel, for which, according to Table 2, a change from commercial quality to drawing quality reduces minimum bend radius by 33 to 50%. Low-carbon steels of commercial quality differ in bendability, as indicated in Table 3, which is for typical commercial-quality steels suitable for 90 and 180° bends.

Table 3 Suitability of commercial-quality low-carbon steel sheet for bending

Class and hardness of steel, and bending conditions	90° bends	180° bends

Cold-rolled steel up to 1.6 mm (0.062 in.) thick				
Suitable commercial-quality steels	1008 or 1010, rimmed, temper passed		**1008 or 1010, rimmed, annealed**	
Maximum HRB hardness	80[(a)]		**65**	
Minimum bend radius	1*t*		**0.25 mm (0.01 in.)**	
Hot-rolled steel up to 6.4 mm (0.250 in.) thick				
Suitable commercial-quality steels[(b)]	1008, 1010	Up to 1030	1008, 1010	**Up to 1015**
Maximum HRB hardness[(c)]	68	80	68	**72**
Minimum bend radius				
Sheet up to 2.3 mm (0.090 in.) thick	$\frac{3}{4}t$	$1\frac{1}{2}t$	1*t*	$\mathbf{1\frac{1}{2}}\boldsymbol{t}$
Sheet 2.3-6.4 mm (0.090-0.250 in.) thick	**1*t***	**2*t***	$\mathbf{1\frac{1}{2}}\boldsymbol{t}$	**2*t***

(a) For 90° bends made across the direction of rolling. The acceptable maximum hardness for 90° bends parallel with the direction of rolling is 70 HRB.

(b) Rimmed or capped.

(c) Can be met on hot-rolled unpickled steel or steel pickled in sheet form. Hardness values will be higher on mill-pickled hot-rolled coil. With higher hardness values, somewhat larger bend radii will sometimes be required.

High-strength low-alloy steels, because of their higher yield strength and lower ductility, are more difficult to bend than plain carbon steels--requiring more power, greater bend radii, more die clearance, and greater allowance for springback. It may be necessary to remove shear burrs and to smooth corners in the area of the bend. Whenever possible, the axis of the bend should be across the direction of rolling. If the bend axis must be parallel with the rolling direction, it may be necessary to use cross-rolled material, depending on the severity of the bend.

All high-strength low-alloy steels are not equal in formability; however, for the more readily formable grades and the quenched-and-tempered grades, the minimum bend radii in the following table are recommended:

Steel thickness (*t*), mm (in.)	Minimum bend radius for steel with minimum yield strength, MPa (ksi), of:

	310 (45)	345 (50)
Up to 1.6 ($\frac{1}{16}$)	$\frac{1}{2}t$	**1*t***
1.6-6.4 ($\frac{1}{16}$-$\frac{1}{4}$)	1*t*	**2*t***
6.4-13 ($\frac{1}{4}$-$\frac{1}{2}$)	**2*t***	**3*t***

These minimum bend radii are for bending with the bend axis across the rolling direction. The use of smaller bend radii increases the probability of cracking. Hot bending is recommended for thicknesses greater than 13 mm ($\frac{1}{2}$ in.).

Hot bending is necessary when the product shapes are too complex or when bend radii are too small for cold forming. The high-strength low-alloy steels can be successfully hot bent at temperatures as low as 650 °C (1200 °F); however, when maximum bendability is needed, temperatures of 845 to 900 °C (1550 to 1650 °F) are recommended. Cooling in still air from these temperatures returns the material nearly to the as-rolled mechanical properties.

Orientation of Bend

Ordinarily, it is better to orient a part on the stock so that bends are made across the rolling direction. Sharper bends can be made across than can be made parallel with the rolling direction, without increasing the probability of cracking the work metal (Fig. 1 and Table 2).

When bends are to be made in two or more directions, the piece can sometimes be oriented in the layout such that none of the bends is parallel with the rolling direction. In some applications, however, as in that described in the following example, there is no practical way to avoid making bends parallel with the rolling direction. This example demonstrates approximate limits for bending parallel with the rolling direction. A choice must be made in orientation to favor one or another consideration. For example, a blank can be oriented in a strip for economy and the least possible scrap. It can be oriented so that the grain direction will reinforce the metal that receives maximum stress in service. Alternatively, it can be oriented so there is no end-grain runout on a wear surface. In any of these cases, orientation may not be optimal for bending.

Example 1: Bending Parallel with Rolling Direction.

Conflicting demands called for a choice in the orientation of the blank for the can-opener blade shown in Fig. 2. Because an orientation that would favor the bends would have meant a cross-grain surface on the cutting edge, with consequent poor wearing quality, the blank was oriented so that the grain favored the cutting edge, and the bends were made nearly parallel with the direction of the rolling.

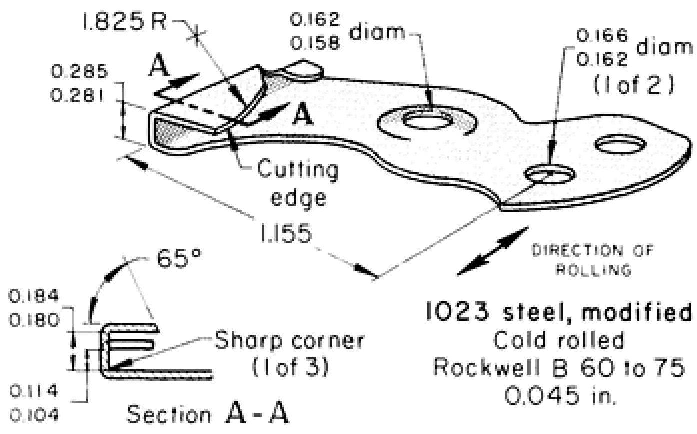


Fig. 2 Can-opener blade that was bent parallel with the direction of rolling in order to promote long service life of the cutting edge. Dimensions given in inches.

To ensure that the stock would withstand the three sharp-radius 90° bends, the steel specification called for stock that would withstand a 180° bend both parallel with and across the direction of the rolling. Steel that could be oil hardened was specified in order to limit distortion during subsequent heat treatment. A modified 1023 steel (with 0.85 to 1.15% Mn) met all of the requirements. The stock was 50 mm (2 in.) wide cold-rolled strip 1.1 mm (0.045 in.) thick. A No. 2 finish was specified to minimize the amount of polishing or burnishing before plating.

The blade was made in a 12-station progressive die, run in a 670 kN (75 tonf) mechanical press. Operations performed in the die included:

- Pierce four holes (one in the scrap area served as a pilot hole)
- Notch outline of part
- Coin cutting edge
- Emboss center hole
- Form bends
- Cut off

The die was made of D2 tool steel, except at station 12 (bending and cutoff), for which the die material was C-5 carbide at 71 HRC. The die life per grind was 300,000 pieces. Mineral oil was the lubricant.

The production rate was 4500 pieces per hour. Annual production was 8 million pieces, in 700,000-piece lots. After forming, the part was oil hardened, barrel finished in oil and sawdust, and bright nickel plated.

Orientation of bends with respect to the grain affects not only the severity of bend that can be made but also the service life of that bend. This is demonstrated in the following example.

Example 2: Bending Across the Rolling Direction.

Vibration of an internal combustion engine frequently caused failure of the fuel tank bracket shown in Fig. 3. The bracket broke at the upper bend, where a comparatively narrow tab attached the tank to the engine head. To eliminate premature failure at this bend, a reinforcing strip 2.8 mm (0.109 in.) thick was welded to the back of the upper bend at the narrowest section of stock, where the bracket overhung the engine head (Section A-A, Fig. 3). To reinforce the bracket and reduce vibration, the front leg, which was flattened back (bent 180°) was spot welded to the tank cradle in two spots (Section B-B, Fig. 3). The bend at the 19 mm ($\frac{3}{4}$ in.) wide tab, the 180° bend at the front, and the bend on the reinforcing strip were all made perpendicular to the rolling direction.

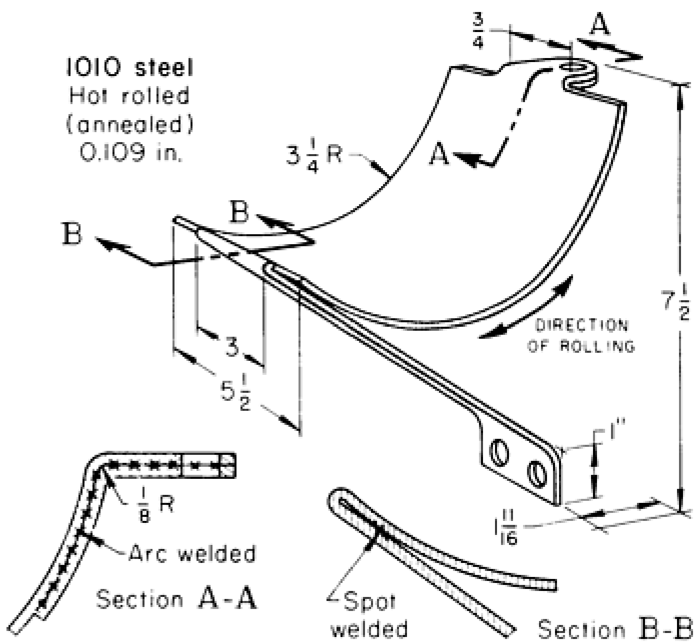


Fig. 3 Severely bent fuel tank bracket that was protected against fatigue failure by bending across the grain and by local reinforcement. Dimensions given in inches.

Because of offsets in the developed blanks, they could be nested slightly by reversing every other one in stock layout. The production procedure was as follows:

- Shear blank 267 × 451 mm ($10\frac{1}{2}$ × $17\frac{3}{4}$ in.)
- Blank and pierce two parts from sheared blank
- Form 83 mm ($3\frac{1}{4}$ in.) radius and 25 mm (1 in.) flange
- Bend 75 mm (3 in.) leg down 90°
- Flatten 75 mm (3 in.) leg
- Spot weld 75 mm (3 in.) leg
- Arc weld reinforcing strip

An 890 kN (100 tonf) single-action mechanical press was used for the second, third, and fourth operations listed above; the fifth operation was done in a press brake. Single-operation dies were used for all operations. Blanking tools were of D2 tool steel; bending tools were of O1 tool steel with a ground finish. Life of the tools before regrind was 50,000 pieces. Mineral oil was the lubricant. The production lots were about 2500 pieces, and annual production was approximately 10,000 pieces.

Die Construction

The same types of bending dies as those used in press brakes can generally be used in presses. However, there are major differences, as follows.

First, because presses ordinarily are not long and narrow like press brakes, more consideration must be given to clearance for removing the finished workpiece when the press is open, as well as clearance for the legs of the bend when the piece is being formed. The bed dimensions of a press also limit the size of workpiece that can be bent.

Second, presses cycle rapidly, and shut height is not as easy to change; therefore, fewer pieces are bent in air, as described in the article "Press-Brake Forming" in this Volume. More frequently, pieces are formed by bottoming the dies. This has the advantage of decreasing springback.

Lastly, presses are usually used for workpieces less than 610 mm (2 ft) long, and press brakes for pieces longer than 610 mm (2 ft). However, the automotive industry bends very large sheet metal structural members on large presses by mass-production methods.

V-dies are composed of a V-block for a die and a wedge-shaped punch (Fig. 4a). The width of the opening in the V is ordinarily at least eight times the stock thickness. In bending, the workpiece is laid over the V in the die, and the punch descends to press the workpiece into the V to form the bend.

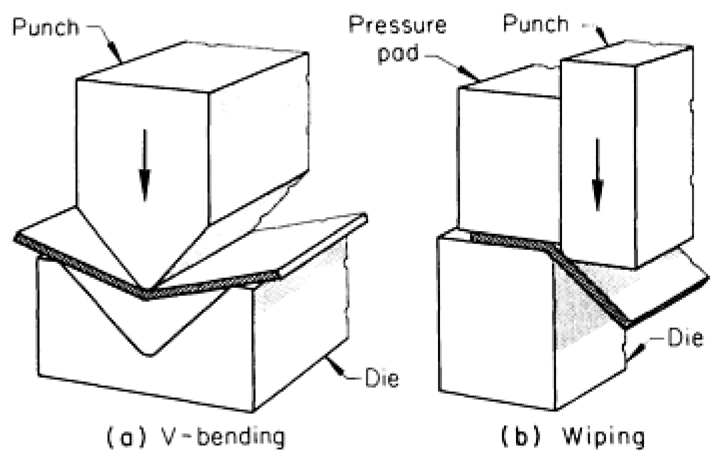


Fig. 4 Bending in a V-die (a) and a wiping die (b). The wiping die can be of inverted design. The workpiece would move downward by the action of the punch against the pressure pad, and the flange would bend upward.

The included angle of a V-bend can be changed by adjusting the distance that the punch forces the sheet metal into the V-die. When the piece must be overbent (to allow for springback), the angle of the punch is smaller than the included angle on the part. Bottoming the punch and striking the metal severely at the bend is a means of reducing springback.

In V-die bending of a flange along the edge of a wide sheet, distortion is likely to occur. Most of the sheet overhangs the die and lifts up as bending takes place. If the punch strikes too fast, the workpiece will distort and will have irregular break lines. However, if the press ram is slowed down just before the punch hits the work, distortion is minimized.

For this type of V-die work, presses are available in which the ram advances rapidly, slows down just above the work, proceeds slowly through the bottom of the stroke, and returns rapidly. In addition, there are presses in which the rate of ram advance can be controlled somewhat by the operator.

Wiping Dies. Another type of bending die is the wiping die (Fig. 4b). A pressure pad that is either spring loaded or attached to a fluid cylinder clamps the workpiece to the die before the punch makes contact. The punch descends and wipes one side of the workpiece over the edge of the die. The bend radius is on the edge of the die. To prevent the wiping action from being too severe, there may be a radius or chamfer on the mating face of the punch. When springback must be compensated for, the die is undercut to permit overbending. The flange metal can be put in slight tension by ironing it between the punch and the die. Sharp bends generally cannot be made in one operation in a wiping die without cracking the metal, because a punch or die with a sharp edge will cut the metal rather than bend it.

Interchangeable V-Dies. Figure 5 shows equipment for making various sizes of V-bends in a punch press. Four different sizes of punches can be mounted into the punch holder, which is attached to the press slide. In operation, the groove in the die that gives the needed bend is aligned with the punch, and the die is then fastened to the bolster plate on the press bed. Adjustable side and end stops can be used to position the blanks for bending.

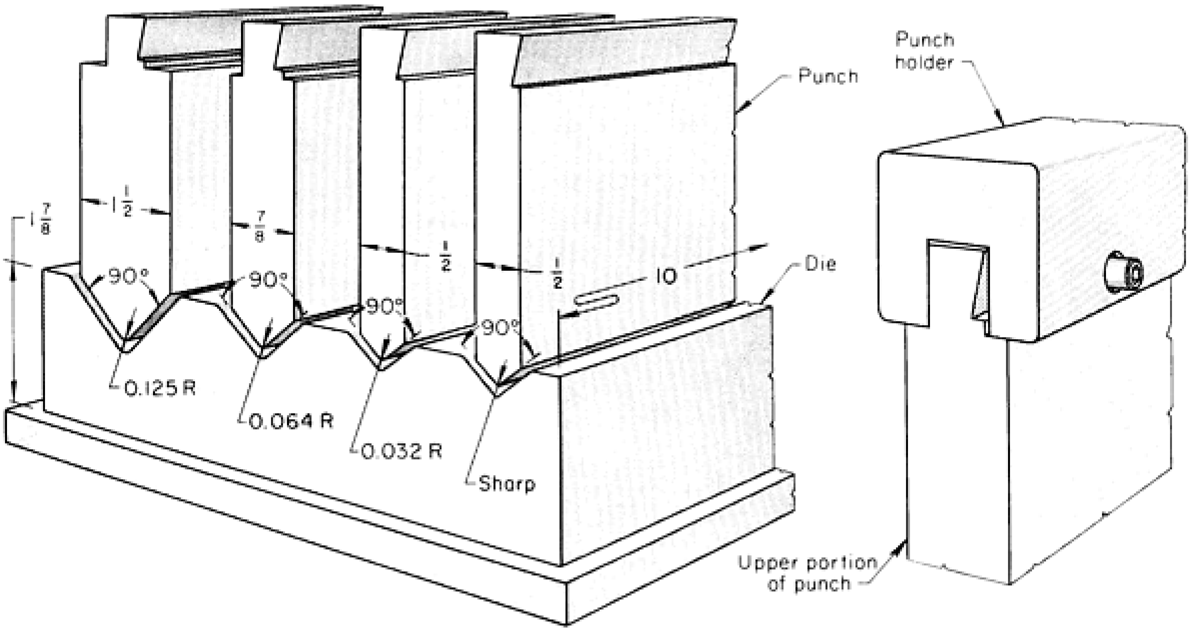


Fig. 5 V-bending punch and die for making a variety of bends in a punch press. Dimensions given in inches.

With equipment such as that illustrated in Fig. 5, rectangular boxes in a range of shapes and sizes are often produced more economically by bending flat blanks into folded-end shapes than by deep drawing. Making folded-end pans by bending in adjustable wing dies and cam dies with interchangeable punches is described in the article "Press Forming of Coated Steel" in this Volume.

U-Bending Dies. U-shaped pieces can be bent in a die such as the one shown in Fig. 6. The width of the U is adjustable by means of spacers and by changing the width of the knockout. Punches can be mounted in the press with a holder similar to that shown in Fig. 5 and can be provided to the proper width and shape to make either a U or a channel shape. Side clearance should be 10% more than stock thickness.

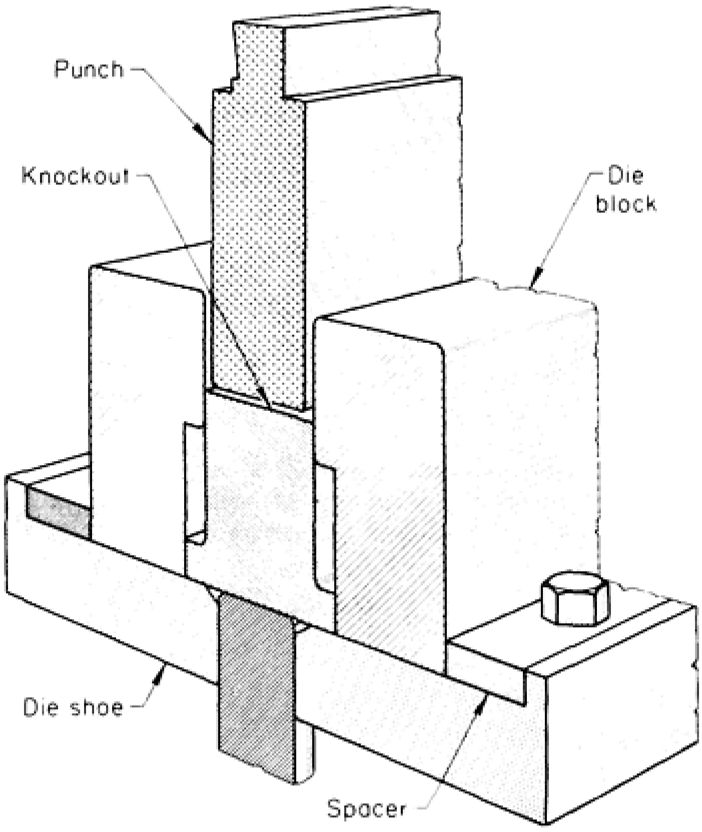


Fig. 6 Adjustable U-bending die for use in a punch press.

Rotary-bending dies (Fig. 7) are used to make bends or twists in bars or strip. These dies use cam action to rotate the workpiece.

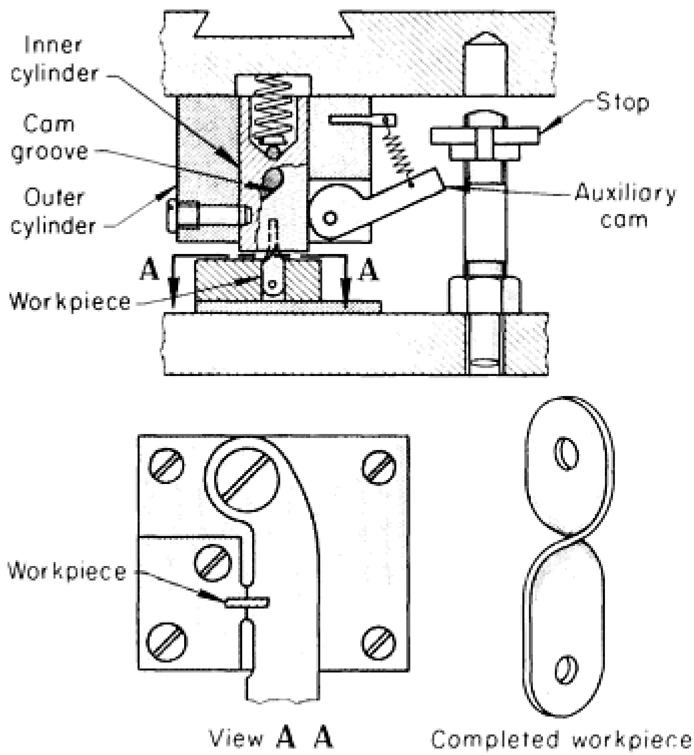


Fig. 7 Rotary-bending die used for 90° twisting of strip metal. Die is shown in closed position; inner cylinder has rotated to give workpiece a 90° twist. The auxiliary cam prevents rotation of the inner cylinder until it is free of workpiece.

As shown in Fig. 7, a 90° twist is given to strip metal to make a connecting link. The punch is made in two major parts: a hollow cylinder that is solidly mounted to the ram, and inside it a solid cylinder that is free to rotate. The inner cylinder has a 90° helical cam groove in its cylindrical surface that engages a hardened pin in the outer cylinder.

When the ram descends, a slot in the face of the inner cylinder engages the end of the workpiece. After the inner cylinder bottoms, as the ram continues to move down, the spring compresses, letting the outer cylinder move down over the inner cylinder. The action of the pin in the groove makes the inner cylinder rotate, giving the workpiece a 90° twist.

An auxiliary cam keeps the inner cylinder from rotating back in the return stroke, until it has cleared the workpiece. Near the top of the stroke, the auxiliary cam is released by a stop, allowing the inner cylinder to return to its starting position.

Cam-Actuated Flanging Dies. Horizontal motion is often needed to form, or partially form, a flange on a workpiece. One of the most commonly used methods of producing this motion at right angles to the motion of the main press ram is with an inclined surface, or cam, in the die mechanism.

As shown in Fig. 8, a blankholder contacts the work first and holds it in position. Resiliency, either in the form of pressure pins leading to a die cushion or in the form of a spring, allows the ram to continue to descend. A cam actuates the sliding punch, which either forms or completes the forming of the flange and is then retracted. The blank is placed on the pressure pad, where it is held by punch A and wiped past the cam-actuated sliding punch to form the flange. Near the bottom of the stroke, punch B contacts the cam head, which moves the sliding punch to set the flange to the 10.3 mm (0.406 in.) dimension and a 90° angle. Cam-actuated sliding punches on each side of the forming punch can be used for setting flanges on channel and U-shape parts.

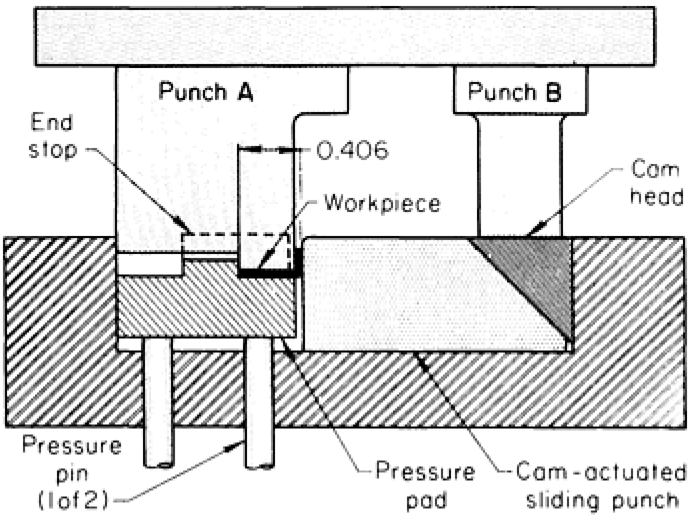


Fig. 8 Cam-actuated single flanging die used for producing a multiflanged part. See text for description of operation. Dimensions given in inches.

Cam-actuated dies are often used in combination with other tooling to produce complicated parts. When used in tandem with a progressive die, a cam-actuated die can significantly reduce the number of operations needed to produce a part. A drawer front that originally required nine separate operations (two shearing, two notching, one piercing, one box-forming, and three flanging) when using a press brake was produced in only four separate operations (slitting coil to width; notching, piercing, and cutting off in a progressive die; box-forming in a second die; and flanging the sides, top, and bottom in a cam-actuated flanging die) with the incorporation of a cam-actuated die.

Compound Flanging and Hemming Die. The compound flanging and hemming die shown in Fig. 9 is unusual in having no horizontal motion of punches or dies. There are two cushions: a spring-loaded hold-down plate and an air cushion for the die plate. As the ram descends, the hold-down makes first contact, clamping the piece securely to the die. As the ram continues to descend, the springs compress, and the angled flange is formed between the angled face of the bending punch and the die plate.

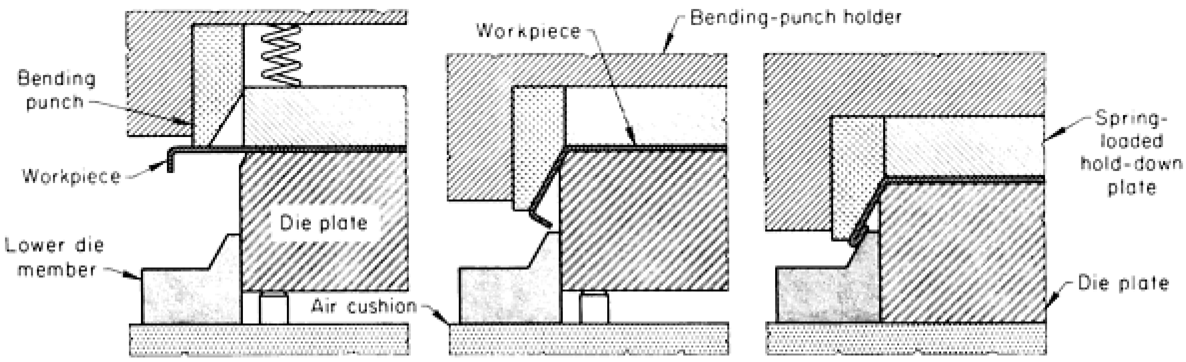


Fig. 9 Compound flanging and hemming die without horizontal motion.

The angle of the bend is set and slightly coined between the chamfer on the edge of the die plate and the angled face of the punch. At this point, the hold-down plate bottoms against the bending-punch holder, then the air cushion begins to yield, lowering the die plate. The edge of the workpiece bumps the corner of the lower die member and folds up into a hem that is completed between the angled face of the bending punch and the mating face of the lower die member.

Wing Dies. Special dies for making U-shape bends have wings that turn up on each side as the punch descends. These dies are described in the article "Bending and Forming of Tubing" in this Volume.

Die Materials. Simple bending dies are ordinarily subjected to less shock than other press-working tools; therefore, they can often be made of low-carbon steel, heat-treated low-alloy steel such as 4140, or cast iron for low production of low-carbon steel pieces. For moderately high production, they should be made of flame-hardening grades of carbon steel, such as 1045, or flame-hardening cast iron, such as class 40 gray iron. Cam-operated dies, wiping dies, and dies used to make curved flanges (shrink or stretch) must be made of a higher grade of material. Tool steels such as O1 or A2 are used for moderately long production runs. For a total tool life of 1 million or more pieces, D2 tool steel is used.

Single-Operation Dies

Single-operation dies are used for low production or on pieces that are so difficult to bend that only one operation at a time is feasible. Some single-operation dies are general-purpose dies that can make bends in simple workpieces of many different designs. Others are single-purpose dies for a particular piece.

Use of Compound Dies

In a compound die, two or more operations are combined at a single work station without relocating the workpiece in the die, so that a finished piece is produced with each stroke of the press. One or more of the operations done in the die can be bending. The press speed (strokes per minute) for compound dies is generally only slightly lower than for single-operation dies; therefore, production time and labor cost per piece are decreased almost in proportion to the number of operations done in the compound die.

In most applications, the cost of a compound die does not differ greatly from the combined cost for equivalent separate dies, and is sometimes less. Operations must be judiciously combined in making a compound die in order not to have die sections that are too thin to be heat treated without distortion, or that will break down under the cyclic loading of ordinary die operation. If these precautions are observed, die life and die maintenance costs should be nearly the same as the costs for equivalent simple dies. Compound dies can be fed individual blanks, or they can be fed strip stock, as in the following example.

Example 3: Use of a Compound Die for Production of Blower Blades from Strip Stock.

Blower blades, shaped as shown in Fig. 10, were cut off from strip stock and bent in a compound die in a 530 kN (60 tonf) mechanical press. The blades were made of 0.76 mm (0.030 in.) thick cold-rolled commercial-quality 1008 or 1010 steel, either plain or galvanized. Some aluminum blades were also made in the same die.

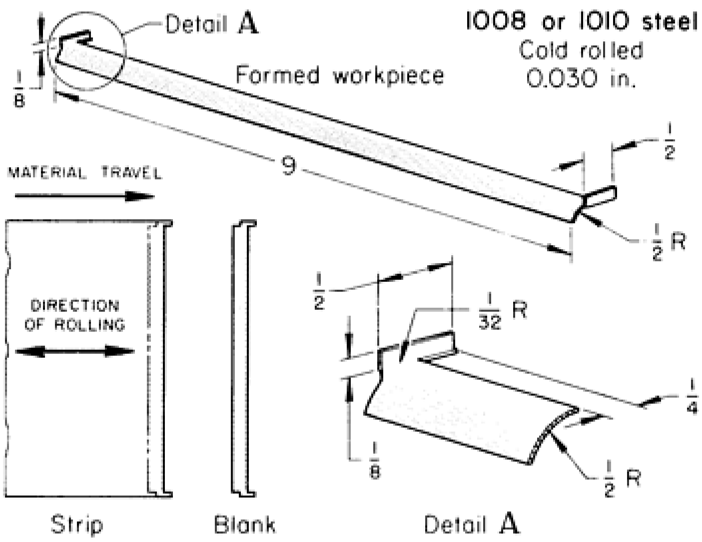


Fig. 10 Blower blade that was blanked and formed from strip stock in a compound die. Dimensions given in inches.

As shown in Fig. 10, the leading edge of one blade was made with the same cut as the trailing edge of the next. The bend axis of the flanges was parallel with the rolling direction of the metal, and the curvature on the surface of the blade behind the flanges was formed across the rolling direction.

The die material was W2 or A5 tool steel. For both materials, die life between regrinds was 1 million pieces. A 1:5 mixture of soluble oil and water was used as a lubricant because a wiping action was used in bending the flanges. The production rate was 1600 pieces per hour, and the annual production was 150,000 pieces. The flanges at the ends of each blade were used for attaching the blades at assembly.

Use of Progressive Dies

Progressive dies are similar in function to compound dies in that they combine in one die set several operations that are performed with one stroke of the press. In a progressive die, however, the operations are separated and distributed among a number of stations. The stock progresses through these stations in the strip form until the finished workpiece is cut from the strip at the last station. Consequently, at the start of a strip, there are several press strokes before a piece is produced. Thereafter, a finished workpiece is produced with each stroke of the press, up to the end of the strip.

If the strips are short, sheared from sheets, they can be fed into the die so that each strip is butted against the end of the preceding strip for continuous production. Otherwise, starting stops are used for each new strip.

Coil stock is fed into the die at one end. A hole or notch is usually made in the strip at the first station, and subsequent stations use this as a pilot to keep the strip properly aligned and positioned while the operations are performed. When the workpiece is complete, it is cut from the strip, which has acted as a holder to carry the piece from station to station. Intricate pieces, often needing no further work, can be made in one press.

In planning the strip layout for progressive-die operations, consideration must be given to development of the part outline, to provision for piloting, to distribution of press load and strength of die elements, and to ensuring minimum metal waste. Some compromise among these factors is usually necessary in developing a sequence of operations and designing a progressive die to accomplish these operations. The strip layout in the following example used the metal efficiently by having the developed flanges surround the body of the preceding part.

Example 4: Nesting of Workpieces to Minimize Stock Waste.

The bracket shown in the lower part of Fig. 11 was formed from hot-rolled 1010 steel strip in a progressive die with three working and two idle stations. As shown in the upper part of Fig. 11, pieces were nested on the strip to minimize scrap. The operations in the three working stations were as follows: pierce one pilot hole and two flange holes and notch the contour, bend two tabs upward, and flange and cut off from the center connecting tab.

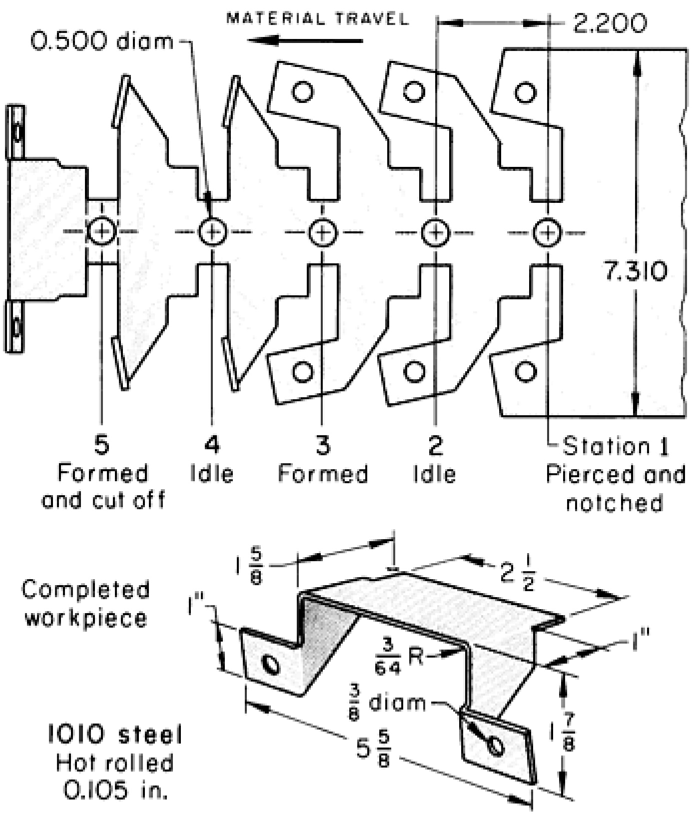


Fig. 11 Layout for progressive-die production of a bracket, with blanks nested to save stock. Dimensions given in inches.

The die was used in a 1330 kN (150 tonf) mechanical press that could make 50 strokes per minute. Allowing for setup and downtime, production was 2800 pieces per hour. A light mineral oil was the lubricant.

The die was made of W1 tool steel hardened to 58 and 60 HRC. The punch-to-die clearance for cutting elements was 6% of stock thickness per side. Annual production was 100,000 brackets.

Press Bending of Low-Carbon Steel

Use of Progressive Dies Versus Separate Dies

The choice between a progressive die and two or more separate dies (single-operation or compound) is not always clear-cut. Various considerations can influence the decision; perhaps the most important is the size of the production order. Other considerations are the rate of obsolescence of the product and the rate at which tool cost must be amortized.

The following example shows how these considerations can affect choice of type of die. In this example, increased annual production requirements justified the use of a progressive die.

Example 5: Change from Three Separate Dies to a Progressive Die.

The part shown in Fig. 12(b) was produced from 1.6 mm (0.062 in.) thick cold-rolled 1010 steel that had a hardness of 58 HRB maximum. A progressive die, which made a finished part at each press stroke, replaced three dies in which 12 press strokes were required for producing one piece. The separate dies were a standard piercing and notching die, an embossing die, and a conventional V-die. Setup time for each die was 30 min.

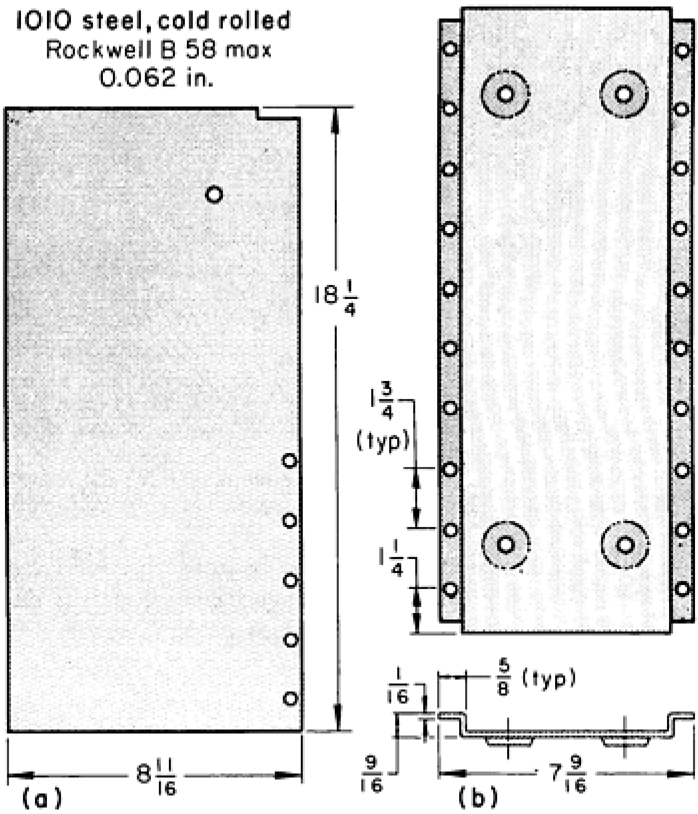


Fig. 12 Strut that was produced in fewer press strokes with a progressive die than with standard press-brake tooling. (a) Blank after first stroke with standard tooling. (b) Finished part. Dimensions given in inches.

The cost of the progressive die was justified for the annual production of 60,000 parts (break-even point was 34,000 parts). Both methods made parts that were within the maximum dimensional tolerance of ±0.4 mm (± $\frac{1}{64}$ in.), and to the maximum bend radius of 0.8 mm ($\frac{1}{32}$ in.).

The original dies had been used in an 890 kN (100 tonf) press brake that operated at 600 strokes per hour on sheared blanks 220 mm ($8\frac{11}{16}$ in.) wide by 464 mm ($18\frac{1}{4}$ in.) long. The press-brake operation consisted of piercing five holes in the flange and one in the center and notching one corner (Fig. 12a). The other holes and the three other corner notches were produced with three more strokes, with the workpiece being turned after each stroke. Four more strokes were needed for embossing four holes (Fig. 12b). Bending required four strokes, bringing the total to 12 strokes.

The progressive die, made of O1 tool steel, pierced, embossed, notched, and cut off the strut from 220 mm ($8\frac{11}{16}$ in.) wide coil stock at a rate of 225 per hour. The die was set up in a 2700 kN (300 tonf) mechanical press. Mineral oil was used as the lubricant for both methods.

Transfer Dies

Transfer dies are similar in operation to progressive dies. The important difference is in the method of transferring workpieces from station to station. The workpieces remain fastened to the stock strip in a progressive die, but they are separate in a transfer die and are transferred from station to station within the die between press strokes by mechanical fingers, levers, or cams. Transfer dies are particularly suited to the fabrication of parts that would be difficult to connect to the stock skeleton with carrier tabs. Bends that cannot be made in a single step are often made in several stages in transfer dies, and bending is often combined with cutting or other forming operations in transfer dies.

The advantages of transfer dies for bending include high production rate, greater versatility than progressive dies, and more efficient use of stock. The last advantage is ordinarily achieved by blanking in a separate press, which permits close nesting of parts. The disadvantages include high equipment cost (for dies, press attachments, and feeding devices), high setup and tool maintenance cost, difficulty in handling thin work metal, and poor applicability to large or oddly shaped parts that need variations in blankholder pressure and contour.

Transfer dies are well suited to the bending of small rings, cups, and cylinders. Transfer dies are used to make rings with one joint.

Lubrication

Lubrication is less important for most bending operations than for other types of forming. In many bending operations, no lubricant is used; in others, the mill oil remaining on the stock or a light mineral oil applied before forming is sufficient to prevent galling.

Exceptions to this practice are hole flanging, compression and stretch flanging, an severe bending in which wiping, ironing, or drawing of the work metal may call for more effective lubrication. More information on lubrication, including recommendations, is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Three examples in this article describe applications in which lubricants were used because of the nature of the bending operations performed. In Example 1, in which accuracy was important and sharp-radius bends were made parallel with the direction of rolling, the workpiece was lubricated with mineral oil before bending. In Example 2, mineral oil was applied to the workpiece before flattening a bend to 180°. In Example 3, a soluble oil-water mixture was used with a compound die that had a wiping component.

Bending Cylindrical Parts

Generally, as the bend radius becomes larger, the allowance for springback must be more generous because less of the bent metal has been stressed beyond its yield strength. Very large radii cannot be easily formed by ordinary bending, but must be stretch formed (see the article "Stretch Forming" in this Volume). For large-radius bends, in which the workpiece is formed to half a circle or more, the bend is often made in several stages, as in the following example.

Example 6: Bending a Cylindrical Part in a Progressive Die.

The part shown in Fig. 13 was made in a six-station progressive die from 1.2 mm (0.048 in.) thick cold-rolled 1010 or 1020 steel having a hardness of 65 to 75 HRB. The part was a valve used to adjust the size of the air intake in a gas burner.

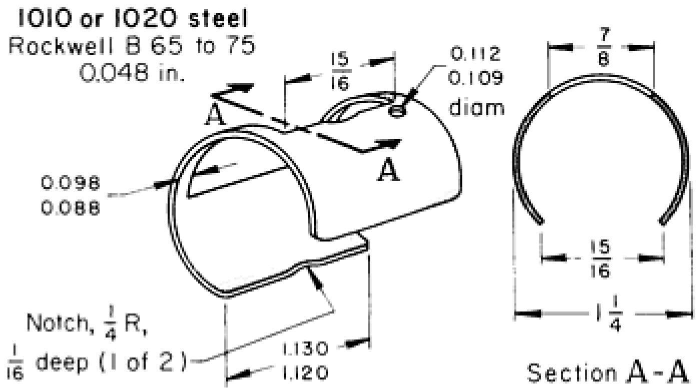


Fig. 13 Air-intake valve that was bent to circular form in three stages in a six-station progressive die. Dimensions given in inches.

The operations for making the part included piercing the 2.84/2.77 mm (0.112/0.109 in.) diam hole, blanking the rectangular cutout, notching the outside of the blank (leaving a 13 mm, or $\frac{1}{2}$ in., wide center carrier tab), bending the circle in three steps starting at the outside edge, and cutoff. The 2.84/2.77 mm (0.112/0.109 in.) diam hole was tapped with 6-32 UNC threads in an automatic tapper at 12 pieces per minute. The two 1.6 mm ($\frac{1}{16}$ in.) deep circular notches were to ensure that the air intake could not be closed completely.

The cutting and forming elements of the die were made of O1 tool steel hardened to 58 to 60 HRC. Die life was 80,000 pieces per regrind. The die was run in a 400 kN (45 tonf) press at the rate of 40 pieces per minute to produce lots averaging 10,000 pieces. The part later was zinc plated and bright dipped.

Edge Bending

Edge bending is the bending of parts with the bend radius perpendicular to the width rather than to the thickness, as is usually done. It is sometimes possible to save material by producing fairly large blanks in simple rectangular form (square-sheared) and then edge bending them into shape. Finish blanking (trimming) is frequently done after edge bending.

Before edge bending can be selected in preference to blanking from sheet, it is necessary to consider the effect of cold work from the bending operation on subsequent forming. High breakage during edge bending can eliminate the cost advantage of the savings in material. Another consideration is the higher cost of labor and tooling for edge bending, which can also offset the savings in material. The following example describes edge bending that was done to save material and to strengthen the part.

Example 7: Edge Bending.

Figure 14 shows a curved-end automotive foot-pedal lever that was produced by edge bending a blank sheared from a bar of hot-rolled 1010 steel. The bar, 6.4 mm ($\frac{1}{4}$ in.) thick, 60 mm ($2\frac{3}{8}$ in.) wide, and 368 mm ($14\frac{1}{2}$ in.) long, was sheared lengthwise to produce two blanks 41 mm ($1\frac{5}{8}$ in.) wide at one end and 19 mm ($\frac{3}{4}$ in.) wide at the other. Edge bending resulted in substantial material savings over blanking and also a stronger part.

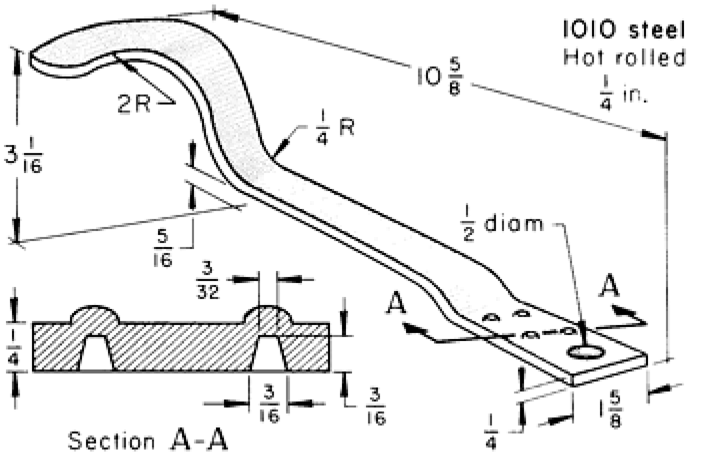


Fig. 14 Pedal lever that was edge bent to save metal and to increase the strength of the part. Dimensions given in inches.

Each blank was edge bent to a 50 mm (2 in.) radius at the narrow end. Other operations included bending the offset and making the two other bends, piercing the 13 mm ($\frac{1}{2}$ in.) diam hole, extruding four weld projections (section A-A, Fig. 14), and trimming the radius on the small end. The cutting and forming sections of the dies were made of air-hardened tool steel.

The stock was cut to length in an 1100 kN (120 tonf) end-wheel press (mainshaft extending front-to-back) operating at 60 strokes per minute. Edge bending, forming, trimming, and piercing were done by separate dies in a 1700 kN (190 tonf) open-back inclinable press at 30 pieces per minute.

Limitations. The size of the offset radii, the length and depth of the offset, and the location of the offset with respect to the flanges may preclude the edge bending of a rectangular blank. The potential cracking and thinning of the outer edge and the wrinkling of the inner edge make the use of a blanked shape more practical.

Press Bending of Low-Carbon Steel

Straight Flanging

Flange bending (flanging) in a wiping die is similar to the cantilever loading of a beam. To prevent movement during bending, the workpiece is clamped to the die by a pressure pad before the punch contacts the workpiece. The bend axis is parallel with the edge of the die.

Flanging dies are often cam actuated, with an accompanying loss of efficiency. Hold-down pads must be used, adding further to the press capacity requirement. Considering all factors, the press capacity for flanging in a wiping die may be up to ten times that for forming a similar length of bend in a V-die with a spacing of at least eight times the thickness of the work metal.

In some operations, only single flanges are bent. More often, more than one flange is bent at a time, as in Examples 3, 9, 12, and 13. Dies can be simple V-dies, U-dies, wiping dies, or complex flanging dies such as shown in Fig. 8.

Even when fairly close tolerances must be held, simple V-dies can be used to make a complex part if production is low. Flanging dies are more expensive than ordinary press-brake dies, but considering the time and labor saved in making simple flanged pieces in flanging dies, they often pay for themselves quickly.

Hemming is an operation in which flanges are flattened against the workpiece in 180° bends to make a finished or reinforced edge. If the flange to be hemmed has been bent somewhat more than 90°, the hemming die can be a simple flat bed or anvil and a simple flat punch. Flanging and hemming can both be done in one press in a compound die, as shown in Fig. 9.

Press Bending of Low-Carbon Steel

Bending of Curved Flanges

When a flange has concave curvature, the metal in the flange is in tension, and the flange is called a stretch flange (Fig. 15a). When the curvature is convex, the metal in the flange is in compression, and the flange is called a shrink flange (Fig. 15b). The amount of tension or compression in either type of flange increases from the bend radius to the edge of the flange. Excessive tension in a concave (stretch) flange causes cracks and tears; excessive compression in the convex (shrink) flange causes wrinkles.

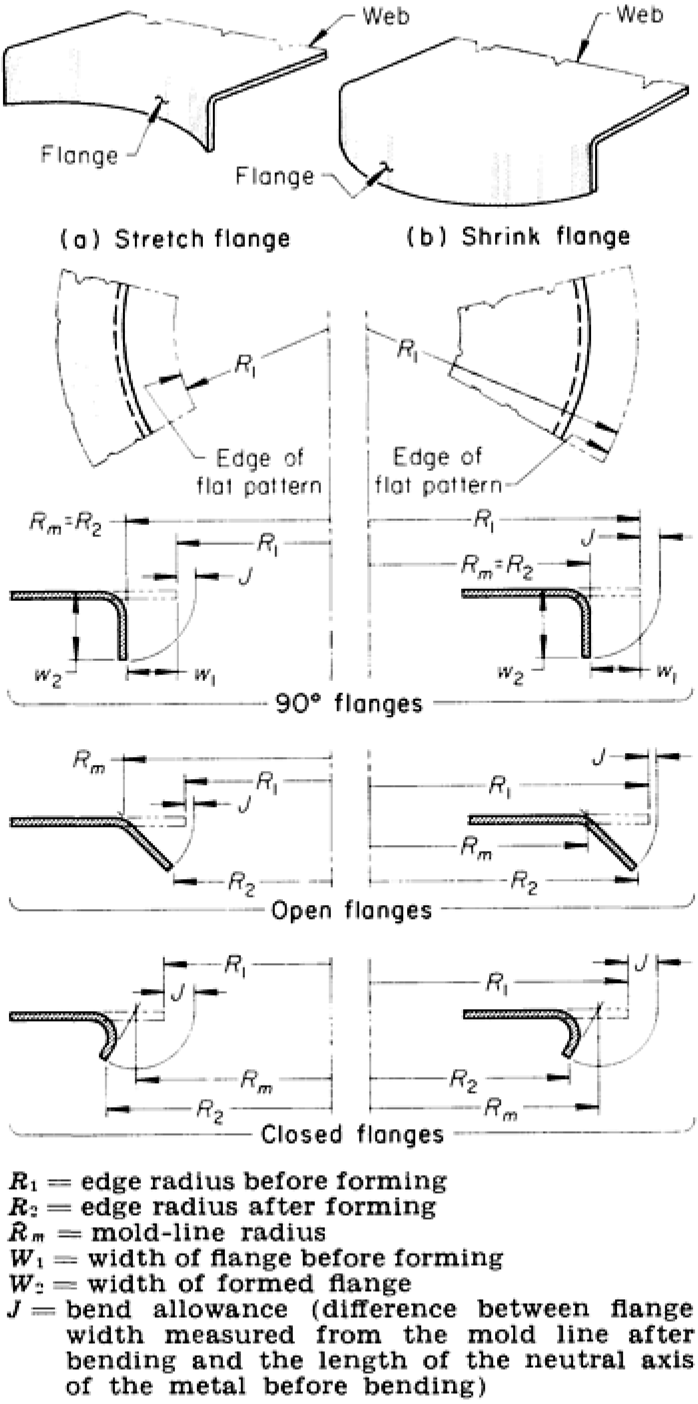


Fig. 15 Dimensional relationships for three types of stretch and shrink flanges.

Stretch and shrink flanges are commonly formed adjacent to each other, producing a reverse flange (as shown in Fig. 17, Example 9). Bend allowances for use in developing a flat blank are given in the article "Press-Brake Forming" in this Volume.

Flanging Limits. Flange radii, flange width, and angle of bend for curved flanges are primarily limited by the amount of deformation that can be tolerated by the flange edges--which depends on the type, thickness, and hardness of the metal and the method of forming. Greater fineness of detail can be achieved in conventional dies than by rubber-pad forming, because of the limited pressures in rubber-pad forming. However, rubber pads provide a uniform pressure over the entire surface of the workpiece, and they can be used to advantage where conventional dies would shear or tear the material.

The approximate percentage of deformation of the free edge is equal to 100 [(R_2/R_1) - 1], where R_1 is the edge radius before forming (flat-pattern radius) and R_2 is the edge radius after forming, as shown in Fig. 15. For 90° flanges, R_2 is the same as R_m. Positive values of percentage of deformation indicate elongation (stretch); negative values indicate compression (shrink).

The permissible limits for the conventional die forming of curved flanges in three common steels 1.0 mm (0.040 in.) thick or more, are as follows:

Steel	**Stretch, %**	Shrink, %
1010	38	**10**
1020	22	**10**
8630	**17**	**8**

These limits should be reduced slightly for thin stock, particularly for shrink flanges. The limits for stretch flanges can be increased if there is an adjoining shrink flange. The compression in the shrink flange helps to relieve the stress in the stretch flange (and vice versa). In addition, the limits of permissible stretch may be increased by filing or grinding the edges of the blank lengthwise; flanges having a calculated stretch of about 100% can be formed by this procedure, as in Example 9. Shrink flanges are more easily formed if the motion of the die causes some ironing of the metal in the direction of the edge of the flange.

Severe Contour Flanging. In small pieces, when enough press force is available, shrink flanges can be formed with sufficient metal flow that the flanges resemble drawn shapes, without using bend reliefs. Such severe flanging is shown in the following example.

Example 8: Severe Contour Flanging of Thick Stock.

The hinge half shown in Fig. 16 was formed in one stroke of an 8000 kN (900 tonf) press. By close control of clearances, it was possible to produce the contour, the sharp inside corner on the bend of the flange, and close matching of the outside planes of the flanges and the edges of the part. The metal was drawing-quality hot-rolled 1010 steel 6.07 mm (0.239 in.) thick.

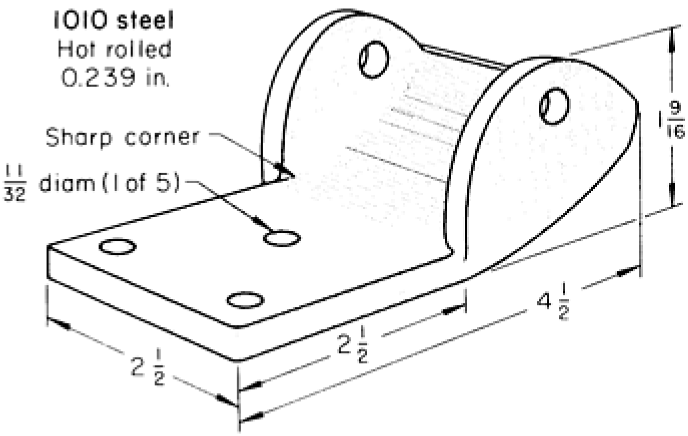


Fig. 16 Hinge half on which flanges were formed flush with adjacent surfaces. Dimensions given in inches.

A slight puckering of the metal at the corners between the flanges and the back indicated the severity of the forming. Relief cutouts were not used in this piece, but because of the severity of the forming, the five holes were pierced after forming to hold the location of the two in-line holes in the flanges within ±0.25 mm (±0.010 in.) of the three other holes. This practice is quite different from that described in Example 12, in which holes were pierced before the part was flanged and yet hole locations were held within ±0.25 mm (±0.010 in.).

The forming die was made of W1 tool steel and was hardened to 58 to 60 HRC. The die was reconditioned after each production lot of 20,000 to 25,000 pieces. The production rate was 600 hinge halves per hour.

Edge Grinding. If they are rough, the edges of stretch flanges may crack or tear at critical points. In the following example, a severe bend was made in a difficult-to-form flange, without cracking, after the rough edges had been ground to smooth the surface.

Example 9: Use of an Edge-Ground Blank to Minimize Cracking of a Severe Stretch Flange.

The crossmember of a truck chassis was made from hot-rolled low-carbon steel that had been pickled and oiled. It was bent from a developed blank into the channel shape shown in Fig. 17. In bending the blank, the edges of the stretch flanges cracked and tore. This problem was solved by filing the edges smooth in the portion of the blank where the stretch and shrink flanges would be formed. Bends were made parallel to the rolling direction.

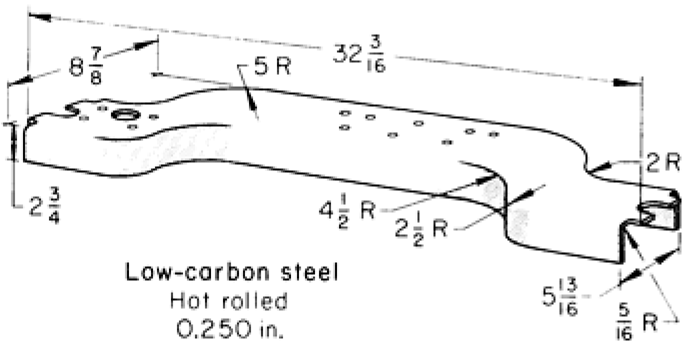
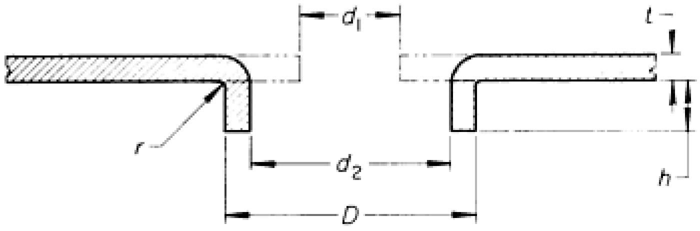


Fig. 17 Truck-frame cross member that was bent from edge-ground blanks to prevent cracking and tearing of the stretch flanges. Over the length of the two stretch flanges, the 7.9 mm ($\frac{5}{16}$ in.) inside bend radius was increased, varying to a maximum of 25 mm (1 in.). Dimensions given in inches.

Because hand filing of the rough edges was costly, grinding was tried, with good results so long as the direction of grinding was along the edge rather than across it. Cross grinding left grooves that increased stress to the same degree as did the original roughness.

Forming was done in a 3800 kN (425 tonf) hydraulic press. The die was a single-action pressure-pad type made of tool steel and had an estimated life of 300,000 pieces. The production rate was two or three channels per minute, in 800-piece production lots.

Hole Flanging. A flange formed around a pierced hole is a continuous stretch flange. One manufacturer has standardized flange dimensions for holes to be tapped in low-carbon steel. These dimensions, as related to workmetal thickness, are shown in Fig. 18.



$D = d_2 + 5t/4$ when t is 0.045 in. or less
$D = d_2 + t$ when t is greater than 0.045 in.
$h = t$ when t is less than 0.035 in.
$h = 4t/5$ when t is 0.035 to 0.050 in.
$h = 3t/5$ when t is greater than 0.050 in.
$r = t/4$ when t is 0.045 in. or less
$r = t/3$ when t is greater than 0.045 in.

$$d_1 = \sqrt{\frac{tD^2 + 4td_2^2 + 4hd_2^2 - 4hD^2}{9t}}$$

Fig. 18 Dimensions of flanged holes to be tapped, as a function of thickness, for low-carbon steel.

In thick stock, the length of the flange around a hole can be greater than that shown in Fig. 18, but the flange thickness will taper rather than be relatively uniform.

Control of Springback

Springback has little effect in the bending of low-carbon steel. It is considered only when close dimensional control is needed. Springback ordinarily ranges from $\frac{1}{2}$ to $1\frac{1}{2}$° and can be controlled by overbending or by restriking the bend area. Factors that affect springback include ratio of bend radius to stock thickness, angle of bend (degrees of bend from flat), method of bending (V-bending or wiping), and amount of compression in the bend zone.

When the bend radius is several times the stock thickness, the metal will need more overbending to stress it beyond the yield point than when the radius is 2*t* or less. A greater amount of overbending is needed to correct for springback on small bend angles than on large bend angles.

In curved flanges, the radius of curvature and the flange length have an effect on the tension or compression in the flange metal, which in turn affects springback. Springback can vary in a production run of a given part because of variation in stock thickness, variation in stock hardness or temper, tool wear, variation in tool adjustment, and variation in power input (line surges).

Multiple Bends. When more than one bend is made in a part, the effect of springback is ordinarily cumulative and may necessitate closer control of the operation than would be needed for just one bend. The following example demonstrates the variation in springback in a part with more than one bend.

Example 10: Variation in Springback in a Part with Eight Bends.

The part shown in Fig. 19 was produced from 1008 steel in three operations: blank, bend, and trim. In three of the eight bends, springback reduced the 54.74 mm (2.155 in.) dimension, and in five, it increased that dimension.

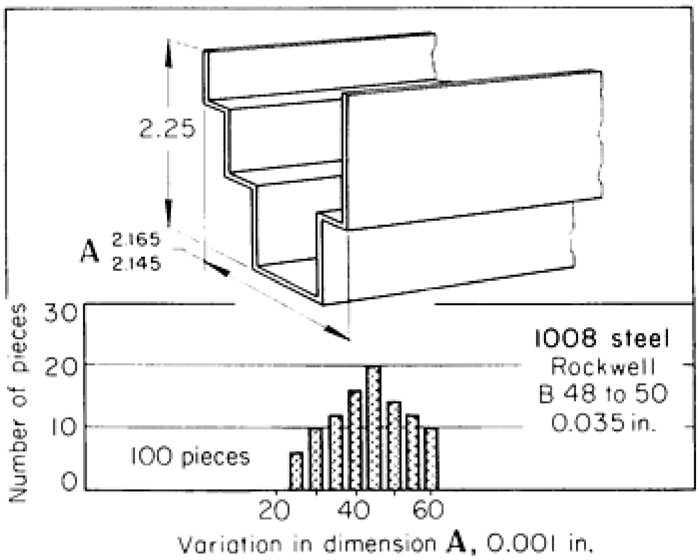


Fig. 19 Variation in flange spacing caused by springback in a part with eight bends. Dimensions given in inches.

To find the net magnitude of the springback and how much variation could be expected, 100 random samples were measured from a production lot of 10,000 pieces. As expected, the net effect of the springback was to enlarge the 54.74 mm (2.155 in.) dimension by amounts ranging from 0.64 to 1.52 mm (0.025 to 0.060 in.), as shown in Fig. 19.

The variation in stock thickness was ±0.05 mm (±0.002 in.). The inside radius on all bends was equal to stock thickness.

Restriking and overbending can be used to set flanges and to offset the effects of springback, as in the following example.

Example 11: Use of Restriking and Close-Fitting Tools to Control Springback in a Flanged Part.

The column-support bracket shown in Fig. 20 was made of 3.35 mm (0.132 in.) thick drawing-quality cold-rolled 1008 or 1010 steel having a hardness of 48 to 51 HRB. The use of close-fitting punches and dies for certain areas and restriking other areas made it possible to obtain the dimensional accuracy necessary for hole positions, cutouts, and flanges.

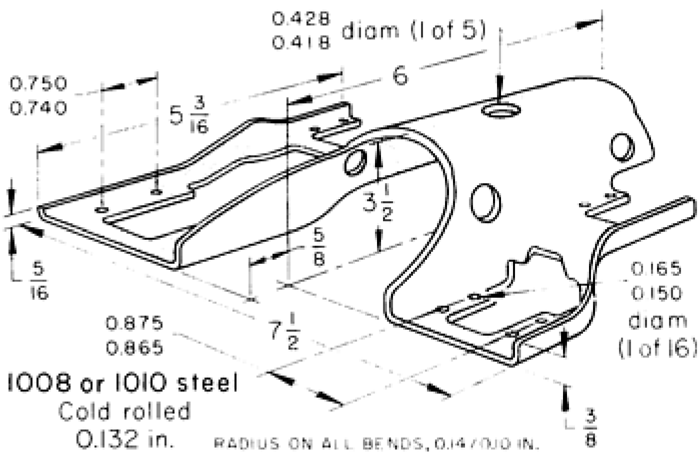


Fig. 20 Bracket with unbalanced shape that was held within bending tolerances by restriking and by extra-close

tolerances on some parts of bending dies. Dimensions given in inches.

All of the round holes were pierced in the flat strip before notching, trimming, punching, and forming. The positions of the small holes with respect to the center were important. The spacing of each set of four small holes was also critical. The flanges along the outer edges had to be uniform in height and set square with the adjacent surface. The open center design and the unbalanced form made it difficult to hold the hole distances and the uniform flange heights.

The forming was done in a progressive die, which was made of O2 tool steel. The cutting punch-to-die clearance was 10% of stock thickness per side. To hold the burr to a minimum height, the die was resharpened after each 50,000 strokes.

The die operated at 400 to 500 strokes per hour. The die was expected to produce parts at this rate for 2 years, and it would then be used for making replacement parts, for which the tolerances were less critical.

Press Bending of Low-Carbon Steel

Accurate Location and Form of Holes

Hole position sometimes cannot be held to tolerance when a workpiece is pierced before bending. When this is so, holes must be pierced after bending--which may require the use of cam-actuated punches, specially shaped dies to support overhanging formed flanges, or punches and dies that are shaped to pierce at an angle.

Holes made before bending are likely to be displaced during bending. Whether or not this displacement can be tolerated must be carefully considered in planning the sequence of operations.

The following example typifies the kind of variation in hole location that can be expected in formed pieces. In this example, if the tolerance on the spacing of the holes had been ±0.025 mm (±0.001 in.) instead of ±0.25 mm (±0.010 in.), the holes would have been pierced after forming, as they were in Example 8.

Example 12: Variation in Distance between Prepierced Holes in a Formed Part.

The bracket shown in Fig. 21 was formed in three operations: blanking and piercing, bending the two small flanges, and bending the two large flanges. The 130.0 mm (5.120 in.) spacing between hole centerlines in opposite flanges had to be held within ±0.25 mm (±0.010 in.).

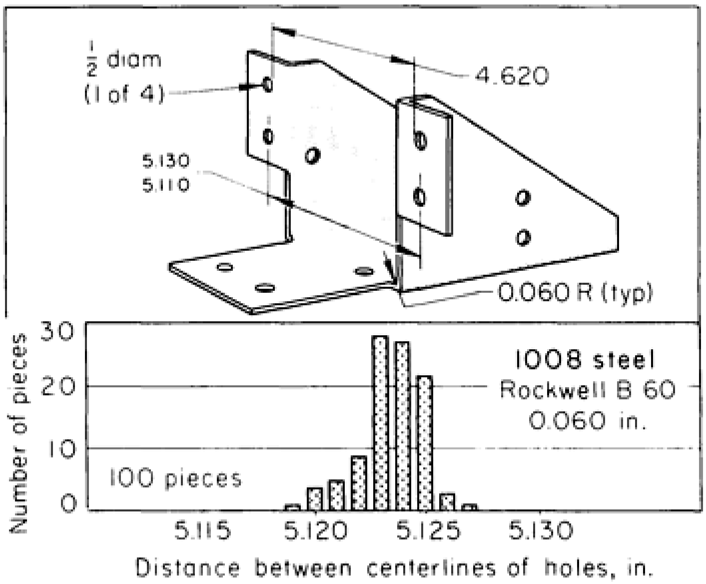


Fig. 21 Variation in distance between centerlines of holes prepieced in opposite flanges of press-bent workpieces. The plotted distances from centerline to centerline were determined by adding 13 mm (0.500 in.)

to the gage readings of distances between the inner edges of the holes. Dimensions given in inches.

A reference dimension of 117 mm (4.620 in.) between the inner edges of the holes was computed for the purpose of checking hole spacing with vernier calipers. Caliper measurements of that dimension on 100 randomly selected pieces (from a run of 10,000 pieces) showed variations of -0.025 to +0.18 mm (-0.001 to +0.007 in.) (Fig. 21).

Free bending of a workpiece is likely to displace holes from true position and to distort and elongate them. In many press-bent workpieces, this deformation is slight enough to be negligible, but it sometimes causes serious difficulties in assembly or fitting. The likelihood of distortion is minimized when holes are located at least one stock thickness away from the beginning curve of a bend. Other precautions include trapping the hole with a hold-down pad that is heavily loaded or relieving the area around the hole with a crescent-shaped cut-out. Additional information on the design and protection of pierced holes during subsequent bending is available in the section "Effect of Forming Requirements" in the article "Piercing of Low-Carbon Steel" in this Volume. Examples in that article describe applications in which holes were pierced after bending to avoid hole distortion and displacement during the bending operation.

An instance of acceptable distortion of pierced holes by subsequent bending is described in the section "Piercing Holes at an Angle to the Surface" in the article "Piercing of Low-Carbon Steel" in this Volume. That article also contains other examples on piercing holes before and after bending.

Accurate Spacing of Flanges

The distance between flanges is another dimension that depends on accuracy of bending. For assembly purposes, this distance may need to be held very closely. Ordinarily, the dimensions at the bases of the flanges are fairly uniform, and variation in the distance between flanges is greater near the free edges. If a flanged part is to fit over a mating piece, oversize dimensions may be preferable to undersize dimensions, as in the following example.

Example 13: Flange Spacing That Was Obtained by Maintaining Tight Gibs and a True Ram.

The bracket shown in Fig. 22 was produced from 5.54 mm (0.218 in.) thick commercial-quality 1010 steel by shearing, blanking, forming, and piercing. The flanges and other bends in the bracket were produced in a five-station transfer die mounted in a 3600 kN (400 tonf) mechanical press. The opposing holes in the flanges were either 16.0/15.9 or 12.8/12.7 mm (0.630/0.626 or 0.505/0.501 in.) in diameter and were pierced after forming. Bend radii were 5.6 mm (0.22 in.). Workmetal hardness was 55 HRB maximum.

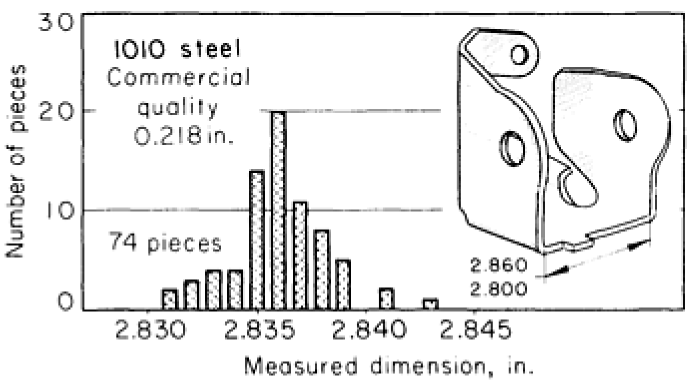


Fig. 22 Accuracy of flange spacing obtained by maintenance of small gib clearance and close ram-to-bolster parallelism in press bending of control-arm brackets for passenger-car frames. Dimensions given in inches.

For proper fit of the bracket with its mating part, the dimension between the two opposing flanges had to be held between 71.12 and 72.64 mm (2.800 and 2.860 in.); the basic dimension was 71.12 mm (2.800 in.). Figure 22 plots the distance between flanges as measured on 74 pieces during a total production run of 6345 brackets. As these data show, all samples were well within tolerance.

To obtain this degree of accuracy on the heavy-gage bracket, gib clearance in the press was kept between 0.15 and 0.20 mm (0.006 and 0.008 in.), and parallelism of the face of the ram with the top of the bolster was maintained within 0.51 mm (0.020 in.). In addition, before forming, the blanks were checked for thickness, and the forming tool was adjusted to compensate for variations. The dies were made of A2 tool steel and were hardened to 60 to 62 HRC.

Safety

Press bending, like all other press operations, involves potential hazards to operators, maintenance men, and other personnel in the vicinity. The articles "Presses and Auxiliary Equipment for Forming of Sheet Metal" and "Blanking of Low-Carbon Steel" in this Volume contain information and literature references on safe operation.

Introduction

PRESS-BRAKE FORMING is a process in which the workpiece is placed over an open die and pressed down into the die by a punch that is actuated by the ram portion of a machine called a press brake. The process is most widely used for the forming of relatively long, narrow parts that are not adaptable to press forming and for applications in which production quantities are too small to warrant the tooling cost for contour roll forming.

Simple V-bends or more intricate shapes can be formed in a press brake. Operations such as blanking, piercing, lancing, shearing, straightening, embossing, beading, wiring, flattening, corrugating, and flanging can also be carried out in a press brake. Information on press-brake forming can also be found in the articles "Press Bending of Low-Carbon Steel," "Press Forming of Coated Steel," "Contour Roll Forming," "Forming of Stainless Steel," and "Forming of Aluminum Alloys" in this Volume.

Principles

In press-brake forming, as in other forming processes, when a bend is made, the metal on the inside of the bend is compressed or shrunk, and that on the outside of the bend is stretched. The applied forces create a strain gradient across the thickness of the work metal in the area of die contact. Tensile strain occurs in the outer fiber, and compressive strain in the inner fiber; both decrease in magnitude toward the neutral axis.

The setup and tooling for press-brake forming are relatively simple (Fig. 1). The distance the punch enters the die determines the bend angle and is controlled by the shut height of the machine. The span width of the die, or the width of the die opening, affects the force needed to bend the workpiece. The minimum width is determined by the thickness of the work and sometimes by the punch-nose radius. After the tools have been set up and the shut height has been adjusted, the press brake is cycled, and the work metal is bent to the desired angle around the nose radius of the punch.

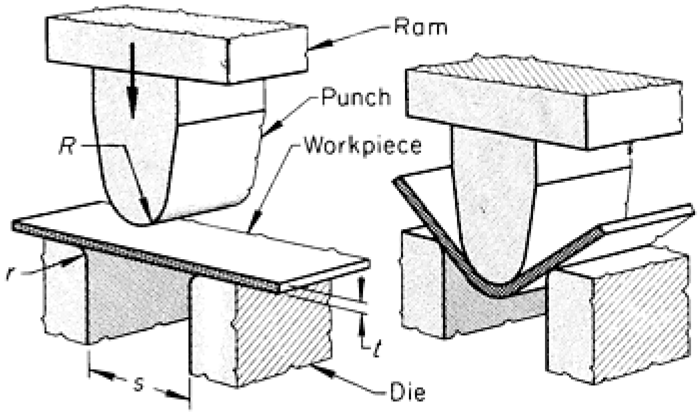


Fig. 1 Typical setup for press-brake forming in a die with a vertical opening. *R*, punch radius; *r*, die radius; *s*, span width; *t*, metal thickness.

Applicability

Press-brake forming is most widely used for producing shapes from ferrous and non-ferrous metal sheet and plate. Although sheet or plate 6.4 mm (0.250 in.) thick or less is most commonly formed in a press brake, metals up to 25 mm (1 in.) thick have often been used, as in the following example.

Example 1: Bending 25 mm (1 in.) Thick Steel Plate.

A 280 mm (11 in.) long bend was made in a 25 mm (1 in.) thick low-carbon steel plate in a 2700 kN (300 tonf) press brake to make the part shown in Fig. 2. The bend radius was 51 mm (2 in.); the included angle was 130°. The plate was air bent in a V-die with a punch that was made from a steel tube 102 mm (4 in.) in diameter.

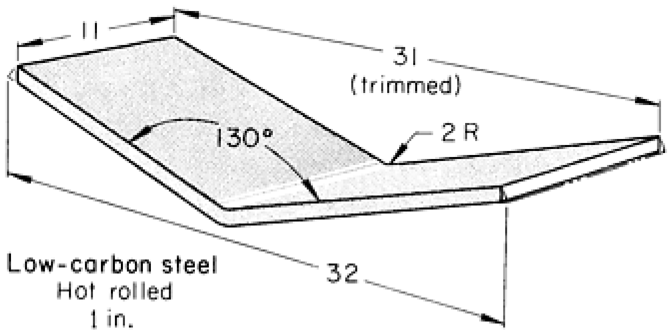


Fig. 2 Frame brace that was air bent from thick plate in a V-die in a 2700-kN (300-tonf) press brake. Dimensions given in inches.

After bending, the edges of the part were machined parallel, as shown in Fig. 2. The workpiece became part of a welded assembly--a frame brace on an industrial lift truck.

Workpiece Dimensions. The length of plate or sheet that can be bent is limited only by the size of the press brake. For example, a 5350 kN (600 tonf) press brake can bend a 3 m (10 ft) length of 19 mm ($\frac{3}{4}$ in.) thick low-carbon steel plate to a 90° angle, with an inside radius of the bend equal to stock thickness. If the included angle of the bend is greater than

90°, if the bend radius is larger than stock thickness, or if the length of bend is less than the bed length, a press of correspondingly lower capacity can be used.

Forming can be done at room or elevated temperature. For elevated-temperature forming in which the punch bottoms, the punch and die should be heated as well as the blank. In air bending, the blank is heated, and the punch is sometimes heated, depending on the area of contact between punch and blank and the metal thickness.

Work Metals. Press-brake forming is applicable to any metal that can be formed by other methods, such as press forming and roll forming. Low-carbon steels, high-strength low-alloy steels, stainless steels, aluminum alloys, and copper alloys are commonly formed in press brakes. High-carbon steels and titanium alloys are less frequently formed in a press brake, because they are more difficult to form.

The formability of all metals decreases as the yield strength increases. Therefore, in press-brake forming, power requirements and springback problems increase and the degree of bending that is practical decreases as the yield strength of the work metal increases.

Press Brakes

The primary advantages of press brakes are versatility, the ease and speed with which they can be changed over to a new setup, and low tooling costs. A press brake is basically a slow-speed punch press that has a long, relatively narrow bed and a ram mounted between end housings (Fig. 3). Rams are actuated mechanically or hydraulically.

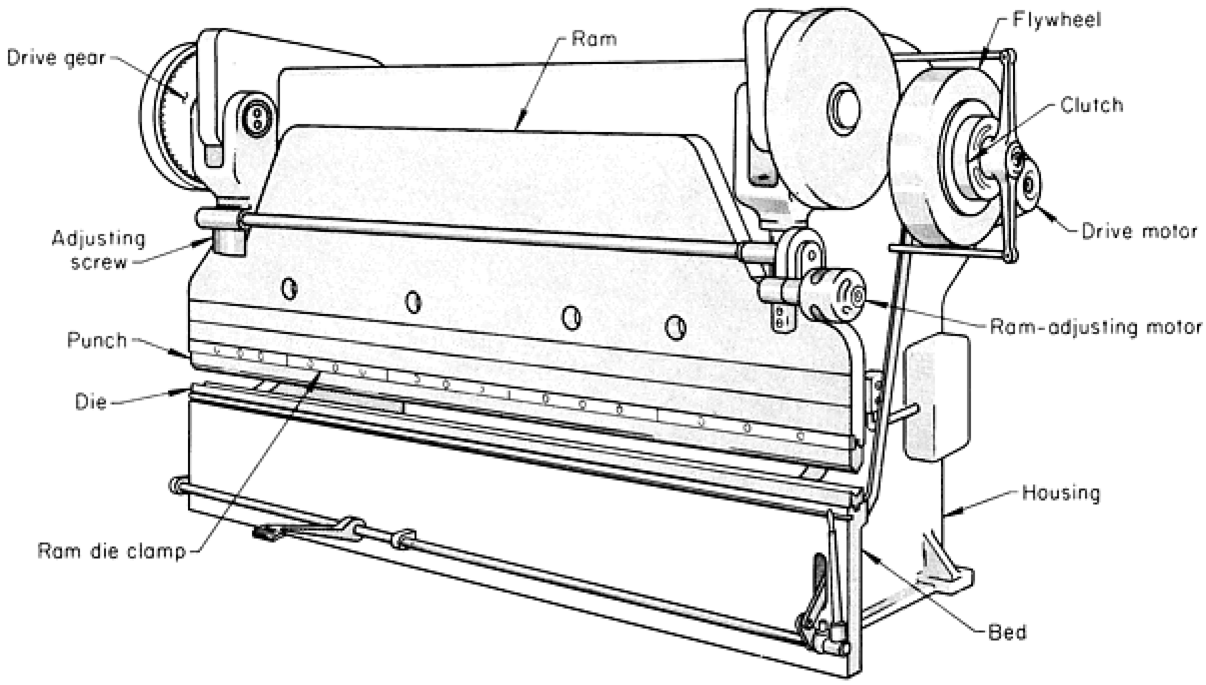


Fig. 3 Principal components of a mechanical press brake.

Mechanical Press Brakes. The ram of a mechanical press brake is actuated by a crank or an eccentric through a gear train in which there is a clutch and a flywheel. The gear train is usually designed to provide fast movement of the ram. Shut height (the distance between ram and bed at the bottom of the stroke) is adjustable by means of a screw (usually powered) in the pitman, or link, at each end of the ram. The length of the ram stroke, however, is constant.

One advantage of a mechanical press brake over a hydraulic press brake is that the mechanical type can develop greater-than-rated tonnage, because of the inertia of the flywheel moving the ram and the mechanical advantage of the crank near the bottom of the stroke. As a result, most mechanical press brakes have extra-strong frames to allow for occasional overloading. However, overloading should not be encouraged, because serious damage to the press brake may occur from improper setup. Another advantage is that operating speeds are greater in mechanical press brakes than in hydraulic press brakes. The greater speed is especially useful for long-run production of workpieces that are easily handled. Greater speed also permits instantaneous, high impact forces when the punch contacts the work metal. This impact force is useful in some operations, although it can damage the machine if the setup lacks rigidity.

A disadvantage of a mechanical press brake is that the stroke cannot be adjusted or controlled to the same degree as is possible with the hydraulic type. However, mechanical press brakes are available at additional cost with devices that permit a rapid advance to work and then a slower speed during forming.

Hydraulic Press Brakes. The ram of a hydraulic press brake is actuated by two double-acting cylinders, one at each end of the ram. Force supplied by the hydraulic mechanism will not exceed the press rating; therefore, it is almost impossible to overload a hydraulic press brake. (When thicker metal is inadvertently used, the ram stalls.) Therefore, frames can be lighter and less costly than those for mechanical press brakes, which are subject to overloading.

In hydraulic press brakes, length of stroke and location of the top and bottom of the stroke (within limits of the cylinder length) are adjustable. The point of rapid advance and return of the ram and its speed during contact with the workpiece are also adjustable; this adjustment makes possible a dwell period, which is often helpful in controlling springback. Cycles established by means of the various adjustments are reproduced by switches in the control circuit.

Even though devices are available that permit some control of the stroke of a mechanical press brake, the degree of control that is possible for a hydraulic press brake is considerably greater. For example, the ram on a hydraulic press brake can be reversed or its speed can be changed at any point on the stroke. Because of these features, a hydraulic press brake is often preferred for the segmental forming of stock longer than the dies, for the forming of large sheets that would be likely to whip in a mechanical press brake, and for the forming of difficult-to-form metals.

Hybrid press brakes incorporate both mechanical and hydraulic elements in the ram drive. The hydraulic-mechanical hybrid consists of a mechanical press brake driven by a rotary hydraulic motor. Containing a vane that rotates 270° between stops, the rotary hydraulic motor has replaced the piston used in a hydraulic cylinder. As it moves between the two stops, the motor propels the eccentric shaft through one complete cycle, driving the ram to the stroke bottom and back to the top.

The hybrid press brake combines the best features of both mechanical and hydraulic press brakes. It offers the same accuracy and operating speeds obtainable with the mechanical press brake while providing the adjustable length and controllability of the hydraulic press brake.

Selection of Machine

A mechanical press brake is usually preferred for quantity production because its speed is greater than that of a hydraulic press brake. Conversely, a hydraulic press brake is generally preferred for varied short-run production because it is more versatile.

Apart from the method of actuating the ram, major factors that must be considered in the selection of a press brake for a given application are the size, length of stroke, and tonnage capacity of the press brake. Table 1 lists capacities and other details for mechanical and hydraulic press brakes.

Table 1 Capacities, sizes, speeds, and ratings for mechanical and hydraulic press brakes

Capacity		**Bed length**	**Stroke length**	**Speed strokes per min**	**Bending capacity, m (ft), with standard stroke for low carbon steel with thickness of:**	Motor, hp
Mid stroke	**Near bottom of stroke**					

kN	**tonf**	**kN**	**tonf**	**m**	**ft**	**mm**	**in.**		**1.6 ($\frac{1}{16}$ in.)**	**4.8 ($\frac{3}{16}$ in.)**	**6.4 ($\frac{1}{4}$ in.)**	**13 ($\frac{1}{2}$ in.)**	**19 ($\frac{3}{4}$ in.)**	**25 (1 in.)**	
Mechanical press brakes															
. . .	. . .	130	15	1.2-3.0	4-10	50	2	20-50	1.2 (4)	0.2 ($\frac{3}{4}$)	. . .	. . .	. . .	. . .	**$\frac{3}{4}$-1**
. . .	. . .	220	25	1.8-3.7	6-12	50	2	20-50	2.0(6$\frac{1}{2}$)	0.5(1$\frac{1}{2}$)	. . .	. . .	. . .	. . .	**$1\frac{1}{2}$**
320	36	490	55	1.8-3.7	6-12	64	$2\frac{1}{2}$	40	3.7 (12)	0.9 (3)	. . .	. . .	. . .	. . .	**3**
530	60	800	90	1.8-4.3	6-14	75	3	40	. . .	1.8 (6)	. . .	. . .	. . .	. . .	**5**
800	90	1,200	135	1.8-4.3	6-14	75	3	36, 12	. . .	3.4 (11)	1.8 (6)	. . .	. . .	. . .	**$7\frac{1}{2}$**
1020	115	1,560	175	1.8-4.3	6-14	75	3	36, 12	. . .	. . .	3.0(10)	. . .	. . .	. . .	**10**
1330	150	2,000	225	1.8-4.9	6-16	75	3	33, 11	. . .	. . .	4.0(13)	. . .	. . .	. . .	**15**
1780	200	2,670	300	2.4-5.5	8-18	102	4	30, 10	. . .	. . .	5.5(18)	1.8 (6)	. . .	. . .	**20**
2310	260	3,560	400	2.6-5.8	$8\frac{2}{3}$-$18\frac{2}{3}$	102	4	30, 10	. . .	. . .	. . .	2.4 (8)	. . .	. . .	**20**
2980	335	4,450	500	2.6-5.8	$8\frac{2}{3}$-$18\frac{2}{3}$	102	4	30, 10	. . .	. . .	. . .	3.0(10)	1.5 (5)	. . .	**25**
3560	400	5,340	600	3.0-7.3	10-24	102	4	30, 10	. . .	. . .	. . .	3.7(12)	1.5 (5)	. . .	**30**
4630	520	6,670	750	3.0-7.3	10-24	102	4	23, 7	. . .	. . .	. . .	5.5(18)	3.0(10)	. . .	**40**
5780	650	8,900	1000	3.0-	10-	127	5	23, 7	. . .	. . .	. . .	7.3(24)	3.2(12)	1.8 (6)	**40**

				7.3	24										
7340	825	11,100	1250	4.2-6.7	14-22	152	6	20, 6	. . .	. . .	. . .	. . .	5.2(17)	3.0(10)	**50**
8900	1000	13,300	1500	4.2-7.3	14-24	152	6	20, 6	. . .	. . .	. . .	. . .	6.4(21)	3.7(12)	**50**
Hydraulic press brakes															
. . .	. . .	1,780	200	2.6-5.8	$8\frac{2}{3}$-$18\frac{2}{3}$	305	12	21, 34[a][b]	. . .	4.3 (14)	3.7(12)	. . .	. . .	. . .	**25**
. . .	. . .	2,670	300	2.6-5.8	$8\frac{2}{3}$-$18\frac{2}{3}$	305	12	25[a][c]	. . .	. . .	4.9(16)	2.4 (8)	. . .	. . .	**30**
. . .	. . .	3,560	400	2.6-5.8	$8\frac{2}{3}$-$18\frac{2}{3}$	305	12	26[a][d]	. . .	. . .	. . .	3.7(12)	1.8 (6)	. . .	**40**
. . .	. . .	4,450	500	2.6-5.8	$8\frac{2}{3}$-$18\frac{2}{3}$	305	12	25[a][e]	. . .	. . .	. . .	4.3(14)	2.7 (9)	. . .	**40**
. . .	. . .	5,340	600	3.0-7.3	10-24	305	12	25[a][f]	. . .	. . .	. . .	4.9(16)	3.0(10)	. . .	**50**
. . .	. . .	6,670	750	4.2-7.3	14-24	305	12	21[a][g]	. . .	. . .	. . .	6.7(22)	4.3(14)	3.0(10)	**60**
. . .	**. . .**	**8,900**	**1000**	**4.2-7.3**	**14-24**	**457**	**18**	**21[a][h]**	**. . .**	**. . .**	**. . .**	**. . .**	**5.5(18)**	**4.3(14)**	75

(a) Normal press speed gives rated capacity. High press speeds, m/min (in./min), together with press tonnage ratings, are as follows:

(b) 1.4 and 1.7 m/min (57 and 65 in./min) at 620 kN (70 tonf);

(c) 1.1 and 1.6 m/min (44 and 62 in./min) at 1070 kN (120 tonf);

(d) 1.3 and 1.6 m/min (51 and 62 in./min) at 1420 kN (160 tonf);

(e) 1.4 and 1.5 m/min (54 and 58 in./min) at 1780 kN (200 tonf);

(f) 1.4 and 1.3 m/min (56 and 51 in./min) at 2140 kN (240 tonf);

(g) 1.2 and 1.2 m/min (48 and 47 in./min) at 2670 kN (300 tonf);

(h) 1.5 and 1.1 m/min (58 and 44 in./min) at 3560 kN (400 tonf)

Size is determined by length of bed and length of stroke (maximum stroke, in a hydraulic press brake). The bed length must be able to accommodate the longest bend required. Bed length can also be dictated by the need to mount more than one die in the press to permit a sequence of related operations in the machine at the same time. Under these conditions, it may be necessary to shim the dies so that the punches will bottom simultaneously. For example, if the parts are 305 mm (12 in.) long and six dies are required, the parts could be mounted on a press brake with a bed length of 1.8 m (72 in.) plus allowance for space between dies.

Standard press brakes are available with a maximum bed length of 7.3 m (24 ft). Still larger press brakes are available on special order. The longer the bed, however, the more massive it must be to provide enough rigidity for holding product dimensions, until a length is finally reached at which cost is prohibitive. Similarly, for a given capacity, the maximum stock thickness that can be accommodated decreases as bed length increases.

Length of stroke is an important consideration in any operation in which the height of the sides of the member after bending (such as a deep channel or box) causes interference between the top edge of the formed section and the ram. In addition, the greater the leg height after forming, the longer the stroke must be to allow the finished part to be withdrawn (unless it can be withdrawn from the end, under which conditions length of stroke is not important). Press brakes having a stroke length as great as 152 mm (6 in.) (mechanical) and 457 mm (18 in.) (hydraulic) are available as standard equipment. Modifications for providing increased stroke length are available at extra cost.

Capacity is stated in tons of force developed by the ram at the midpoint of the stroke. Capacities of commercial press brakes range from 70 kN to 22 MN (8 to 2500 tonf). Required capacity is governed by the size and bending characteristics of the work metal and by the type of bend to be made. A formula for determining the capacity required for 90° bends using V-dies without bottoming is:

$$L = \frac{lt^2kS}{s} \quad \textbf{(Eq 1)}$$

where L is press load (in tonf), l is length of bend (parallel to bend axis) (in inches), t is work metal thickness (in inches), k is a die-opening factor (varying from 1.2 for a die opening of 16t to 1.33 for a die opening of 8t), S is tensile strength of the work metal (in tons per square inch), and s is width of die opening (in inches) (Fig. 1).

Sample Calculation. Assume a constant of 1.33, a V-die opening of 8*t*, and a bend 305 mm (12 in.) long made in 6.35 mm (0.250 in.) thick plate having a tensile strength of 30 tsi. Substituting these numerical values in Eq 1 yields:

$$L = \frac{12 \cdot 0.250^2 \cdot 1.33 \cdot 30}{2} \quad \textbf{(Eq 2)}$$

or approximately a 130 kN (15 tonf) capacity requirement for this 90° bend.

For simple bending, the force required increases proportionately with the length of the workpiece or with the square of the work metal thickness. For example, in Eq 2, if the workpiece were 1220 mm (48 in.) long, a 530 kN (60 tonf) capacity would be needed. For producing offset bends (Fig. 4b), about four times as much pressure is required as for simple V-bends.

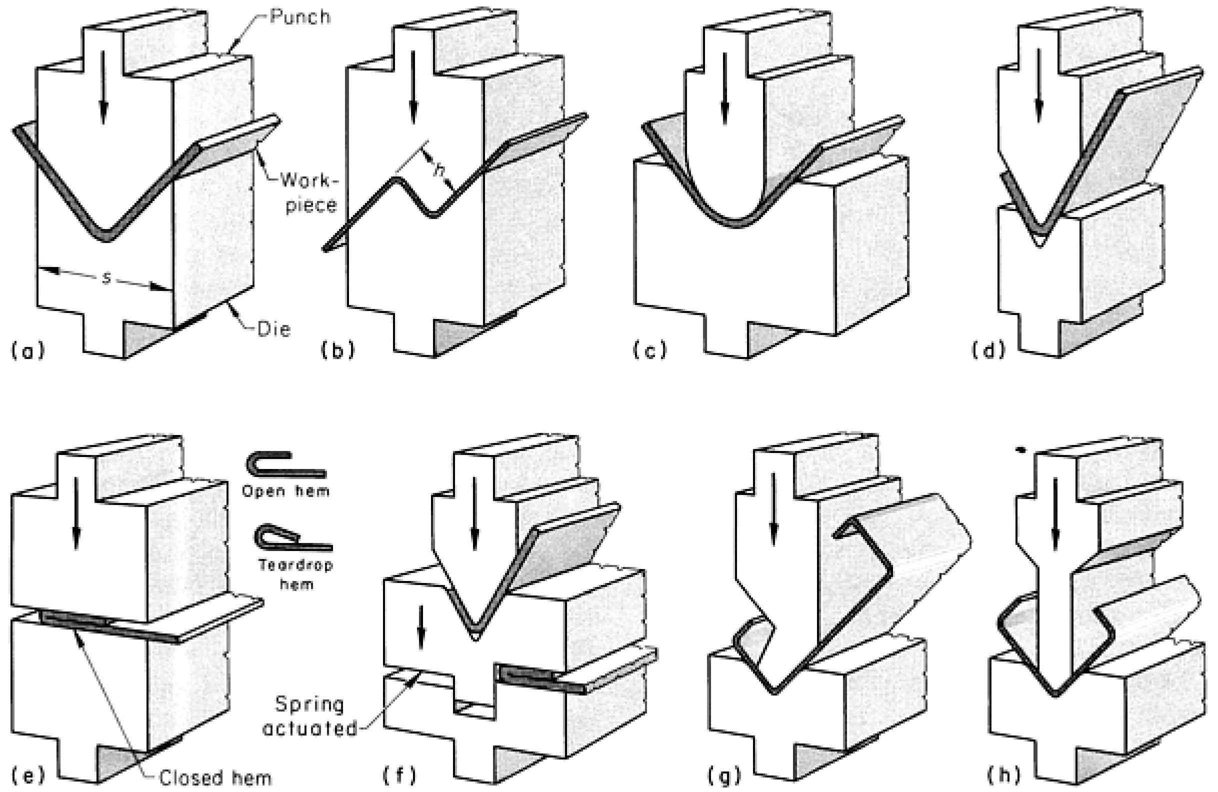


Fig. 4 Dies and punches most commonly used in press-brake forming. (a) 90° V-bending. (b) Offset bending. (c) Radiused 90° bending. (d) Acute-angle bending. (e) Flattening for three types of hems. (f) Combination bending and flattening. (g) Gooseneck punch for multiple bends. (h) Special clearance punch for multiple bends.

Dies and Punches

V-bending dies and their corresponding punches (Fig. 4a and d) are the tools most commonly used in press-brake forming. The width of the die opening (*s,* Fig. 4a) is usually a minimum of 8*t* (eight times the thickness of the work metal).

The nose radius of the punch should not be less than 1*t* for bending low-carbon steel, and it must be increased as the formability of the work metal decreases. The radius of the V-bending die must be greater than the nose radius of the punch by an amount equal to or somewhat greater than the stock thickness in order to allow the punch to bottom. Optimal dimensional control is obtained by bottoming the punch to set the bend.

When producing 90° bends in a bottoming die, the V-die is ordinarily provided with an included angle of 85 to 87°. Several trials are often necessary, and various adjustments must be made on the punch setting before the required 90° bend can be obtained.

Offset Dies. Punch and die combinations such as the one shown in Fig. 4(b) are often used to produce offset bends. Because an offset bend requires about four times as much force as a 90° V-bend, offset bending is usually restricted to relatively light-gage metal (3.2 mm, or 0.125 in., or less). The depth of offset (*h,* Fig. 4b) should be a minimum of six times the work metal thickness to provide stability at the bends.

Radius forming is done with a 90° die and a punch, each having a large radius (Fig. 4c). When the punch is at the bottom position, the inside radius of bend in the workpiece conforms to the radius of the punch over a part of the curve. The harder the punch bottoms, the more closely the work metal wraps around the punch nose, resulting in a smaller radius of bend and less springback. Uniformity of bend angle depends greatly on the uniformity of the work metal thickness.

Acute angles are formed by the die and punch shown in Fig. 4(d). The air-bending technique (see the section on "Air Bending" in this article) is often used to produce acute angles. Acute angles are formed as the first step in making a hem. For this purpose, the die is often bottomed to make the bend angle as acute as possible. A disadvantage of bottoming is that the metal becomes work hardened, so that the hem is likely to crack when formed.

Flattening dies, shown in Fig. 4(e), are used to produce three types of hems (also shown in Fig. 4e) after the metal has been formed into an acute angle. The combination die shown in Fig. 4(f) produces an acute angle on one workpiece and a hem on another, so that a piece is started and a piece completed with each stroke of the press brake.

Gooseneck punches (Fig. 4g) and narrow-body, or special clearance, punches (Fig. 4h) are used to form workpieces to shapes that prevent the use of punches having conventional width (two such workpiece shapes are also shown in Fig. 4g and h).

Tongue Design. The punches shown in Fig. 4 as well as in several other illustrations in this article are provided with a simple, straight tongue for securing the punch to the ram. Although this design of tongue is generally accepted, in some shops punches with a hook type of tongue (see Fig. 8, and the punch for operation 4 in Fig. 20) are used exclusively as a safety precaution. A punch mounted with a hooked tongue cannot fall out. In one shop, it was estimated that hooked tongues increased punch cost by about 10% over the cost of straight-tongue punches.

Special Dies and Punches

Dies that combine two or more operations to increase productivity in press-brake forming are generally more complicated and costly than those illustrated in Fig. 4. Before special dies are designed for a specific application, the increased tooling cost must be balanced against decreased time on the press brake. Generally, the quantity of identical parts to be produced is the major factor in selecting special dies.

Channel Dies. A channel die (Fig. 5a) can form a channel in one stroke of the press brake, while two strokes would be required using a conventional V-die. Because it is necessary to have an ejector in the die to extract the workpiece, channel dies cost more than conventional dies. This higher cost can be justified only on the basis of large-quantity production. It is ordinarily not necessary to have a stripper on the punch, because springback usually causes the part to release. The ejector in the die may be of the spring, hydraulic, or air-return type. The stripper for the punch (if needed) is a release-wedge device or a knockout piece. The use of a channel die, regardless of production quantities, is limited by work metal thickness, corner radii, and required flatness of the web.

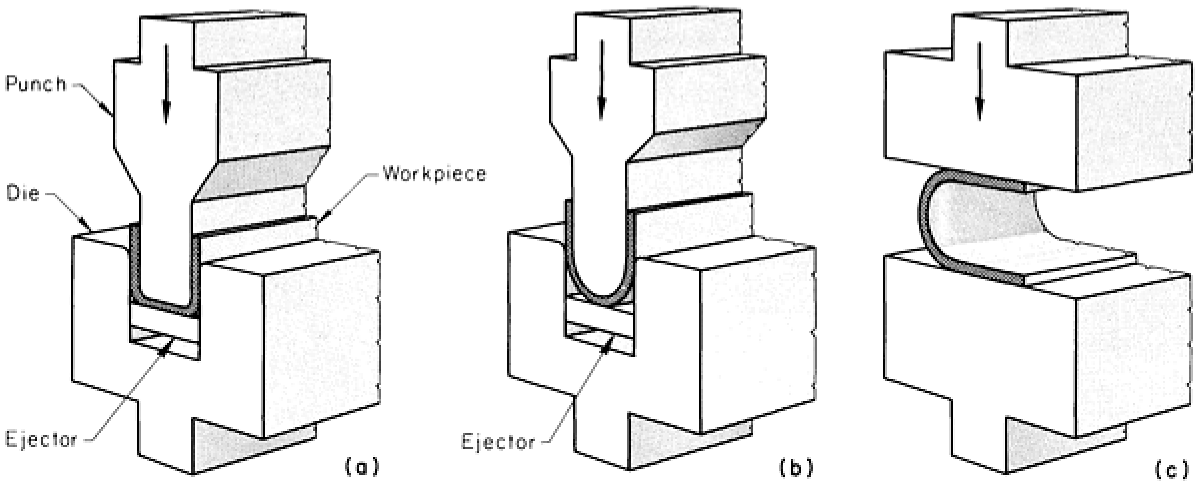


Fig. 5 Three types of special punches and dies for press-brake forming. (a) Forming a channel in one stroke. (b) Forming a U-bend in one stroke. (c) Flattening to remove springback after U-bending.

A modification of the channel die is the U-bend die (Fig. 5b). Springback is a common problem with this type of die; one means of overcoming it is to perform a secondary operation on flat dies, as shown in Fig. 5(c).

Air Bending. In air bending, the die is deep enough that setting does not take place at the bottom of the stroke. The die can have a V shape (Fig. 6), or the sides can be vertical (Fig. 1). The shape and nose radius of the punch are varied to suit the workpiece. The required angle is produced on the workpiece by adjusting the depth to which the punch enters the die opening. This permits the operator to overbend the metal sufficiently to produce the required angle after springback.

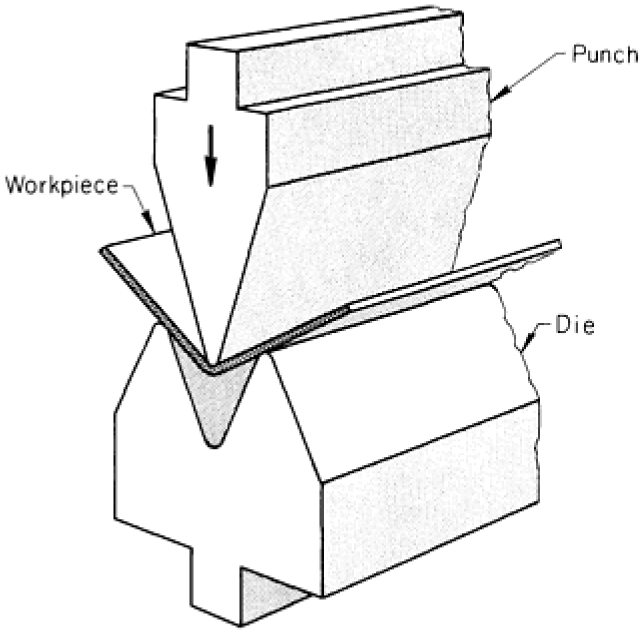


Fig. 6 Setup for air bending with an acute-angle punch and die in a press brake.

When metal is bent beyond its yield strength, the radius formed bears a definite relationship to the opening in the die. A small die opening produces a small radius; the use of a large die opening increases the radius, but also increases the amount of springback. Springback must be compensated for by overbending. Changing the size of the die opening also changes the amount of force needed to make the bend. As the die opening is increased, less force is required; as the die opening decreases, the bending leverage is less, and more force is therefore required.

For the air bending of metal up to 13 mm ($\frac{1}{2}$ in.) thick, the die opening is usually equal to eight times the work metal thickness. This keeps the bend radius approximately equal to the metal thickness. For metal thicker than 13 mm ($\frac{1}{2}$ in.) and for some high tensile strength metals, the die opening should be at least ten times the work metal thickness to increase the bend radius and therefore reduce the possibility of fracture at the bend.

The principal advantage of the air-bend method is the variety of forming that can be done with a minimum number of punches and dies. Air bending also requires less force for a given bend, thus preventing excessive strain on the press brake.

The primary disadvantage of air bending is the possible inconsistency in the bends. Because of variations in dimensions and temper of the work metal as it is received from the mill, springback can vary throughout a production run. However, the operator can adjust the ram to compensate for these irregularities. When air bending in a hydraulic press brake, the operator can use a preset pressure, check each part with a gage, and restrike if necessary. With a mechanical press brake, the shut height can be easily adjusted for a restrike and then reset for the next part.

Box-forming dies are similar to standard V-dies, except that the punch is sometimes specially made to clear the sides of the box being formed. For square boxes, the punch length can be the inside length, and the sides of the box can be formed in any sequence. However, for the forming of rectangular boxes, different tools or techniques are required. One approach is the use of a punch that is split vertically (Fig. 7) so that the punch can clear the sides on the long dimension while forming the sides of the short dimension. In most cases, however, a punch long enough for forming the long sides can be used without splitting; this is accomplished by forming the short sides first.

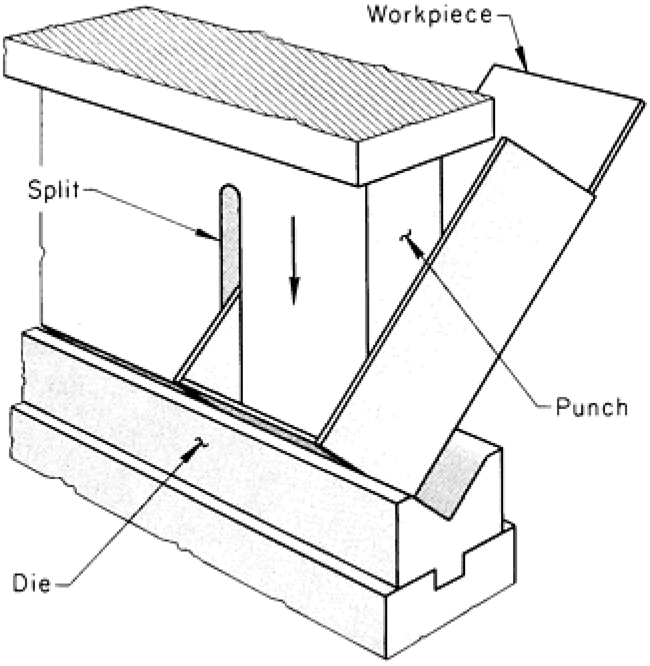


Fig. 7 Setup for forming a rectangular box using a punch split vertically to clear the long sides during forming of the short sides.

When bottoming dies are used in box forming (to overform and coin the metal to reduce springback), the metal can be work hardened excessively if the force used to form the short sides is the same as that used for the long sides. In some shops, when the short sides are less than two-thirds as long as the long sides, force is reduced for forming the short sides.

Arbor-type punches can be used when the sides of a boxlike workpiece must be folded over (Detail A, Fig. 8). The head of the punch extends beyond the punch body so that the formed-over sides can fit over the punch extensions while the remaining folds are made (Fig. 8). The extensions on the punch are approximately triangular in cross section so that the punch can be withdrawn after opposite sides of the workpiece are closed.

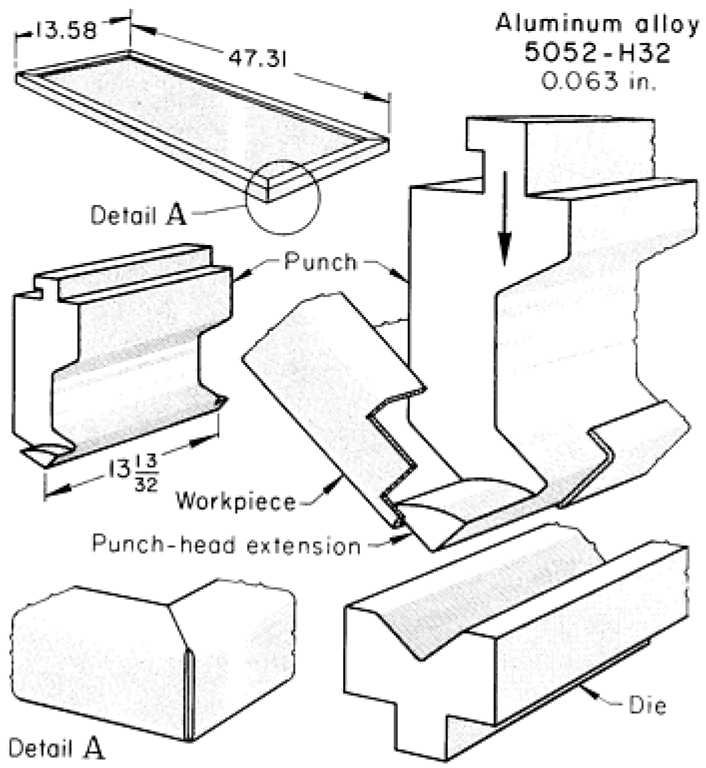


Fig. 8 Folding-over sides of a boxlike workpiece using an arbor-type punch for forming beneath reverse flanges. Dimensions given in inches.

Lock-Seam Dies. Lock seams are made in a press brake when quantities are too small to warrant more elaborate equipment. The usual procedure is to form one component with a special punch and die as shown in Fig. 9(a). The second component of the assembly is formed in a simple V-die. The two components are then locked together in a single stroke of the press brake using another special die (Fig. 9b).

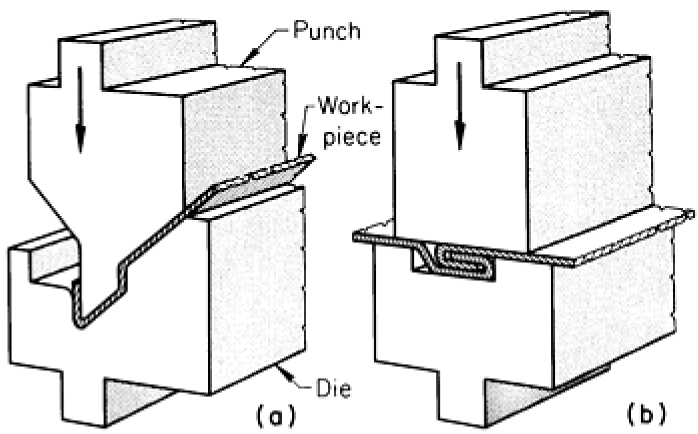


Fig. 9 Special punches and dies for producing lock seams in a press brake.

Curling Dies. Curling in a press brake is usually done in two steps using special dies such as those shown in Fig. 10(a).

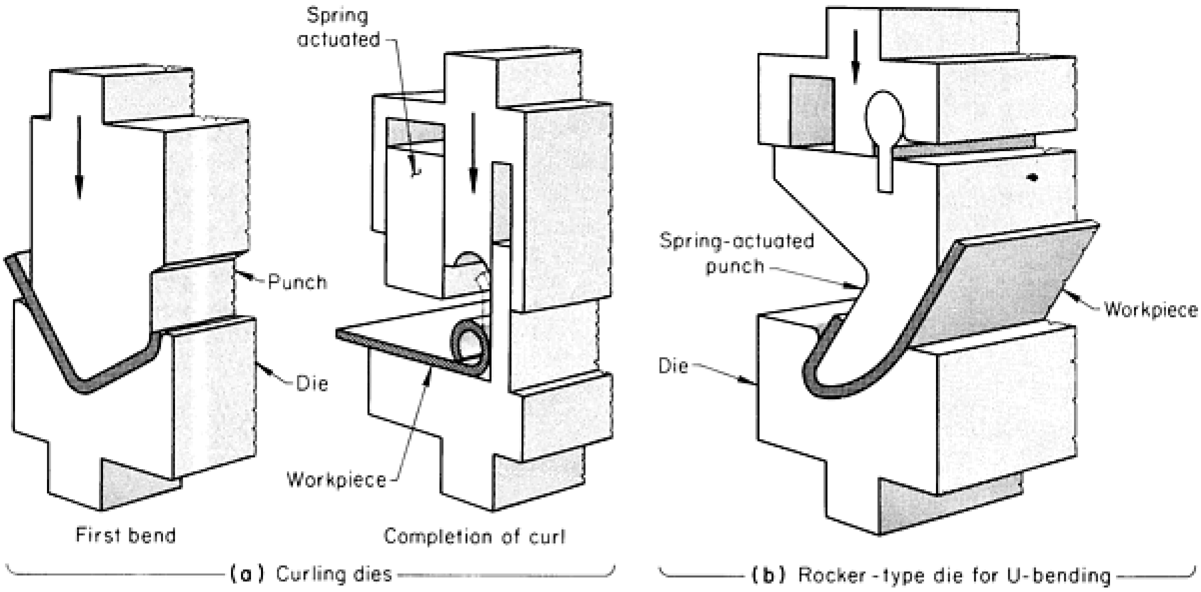


Fig. 10 Special punches and dies for curling and U-bending in a press brake.

Tube- and pipe-forming dies resemble curling dies. To ensure that the workpiece rolls up properly, the edges of the metal must initially be bent. Small tubes can be formed by using a two-operation die (Fig. 11a), while larger tubes require the use of a bumping die such as that shown in Fig. 11(b). Where accuracy is required, tubes formed in a bumping die should be sized over a sizing mandrel. If seams are required, they can be formed on the tube edges prior to the rolling operation.

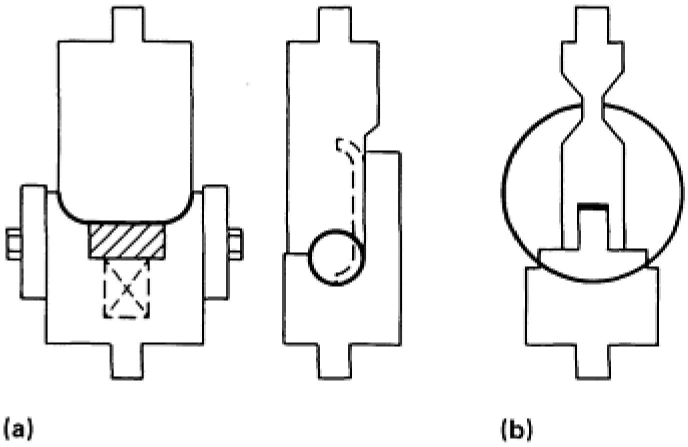


Fig. 11 Tube- and pipe-forming dies. (a) Two-operation die for the forming of small tubes. (b) Bumping die used to form large tubes.

Rocker-Type Dies. Dies that operate with spring-actuated rocker punches can produce bends that would be impossible with the punch operating in a vertical direction only. A typical rocker-type die for U-bending is illustrated in Fig. 10(b).

One-Stroke Hemming Dies. With specially designed spring-actuated dies, it is possible to hem a length of metal sheet in a single stroke. A typical die used for this operation and the movement of die components required for completing the hem are illustrated in Fig. 12. When provided with an adjustable stop, the die shown in Fig. 12 can produce hems of different widths (over a narrow range).

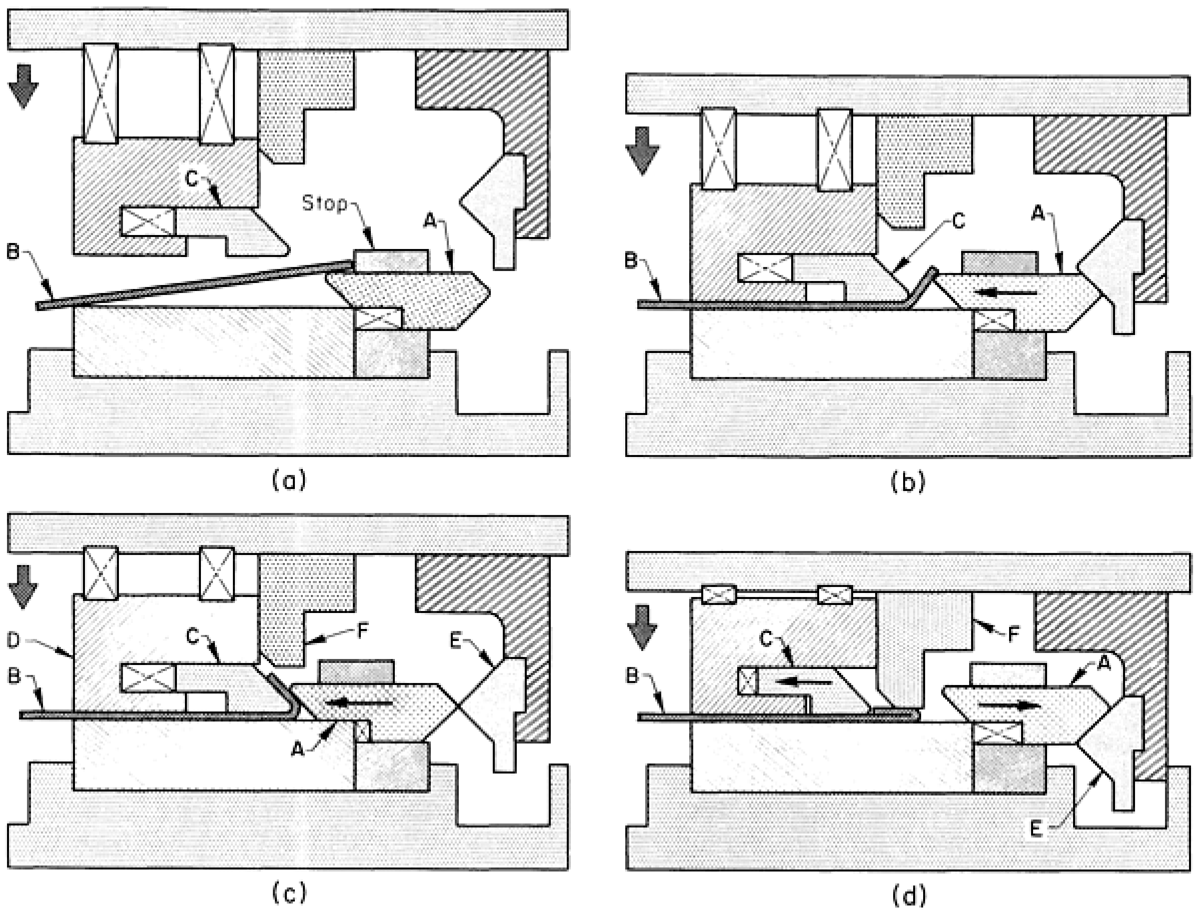


Fig. 12 One-stroke hemming die and movement of die components during hemming. (a) Workpiece, B, is placed over slide A using a component of the die as a stop, and the entire upper die section begins to move down. (b) Slide C contacts the workpiece and makes the first bend for the hem. (c) With slide D holding the workpiece rigidly in place, die component E forces slide A to the left, forming the bent section of the workpiece into an acute angle. (d) As the die continues to move down, die component E permits slide A to retract, providing clearance for F to contact and flatten the workpiece as it forces slide C to the left, thus completing the hem. On the upstroke of the press brake, die components return to original positions.

Beading dies form beads (stopped ribs) to add rigidity to flat sheets. The dies form either open beads that extend from edge to edge of the sheet or closed beads that fade out in the sheet. The hemispherical depressions that constitute the open bead are formed by the die shown in Fig. 13. Closed beads, on the other hand, necessitate the use of spring-pressure pads at the ends, which fade out to minimize wrinkling of the metal.

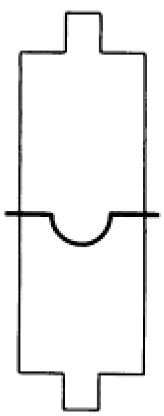


Fig. 13 Die used to form open bead in flat sheet to add stiffness to material.

Corrugating dies can be used to produce numerous corrugations, as shown in Fig. 14.

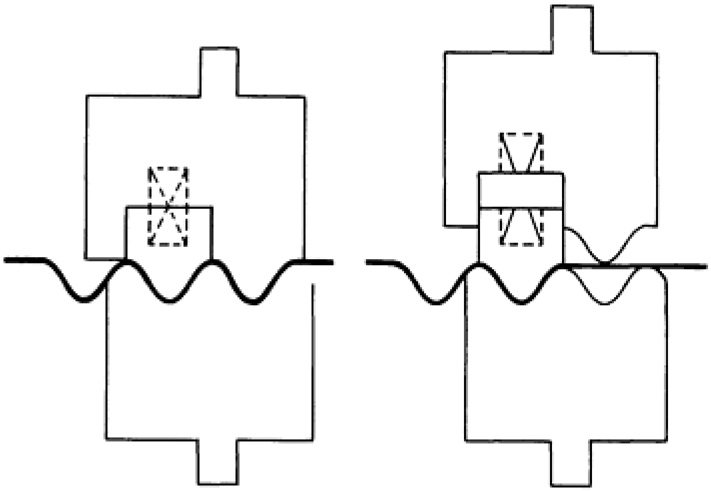


Fig. 14 Corrugating die used to form corrugations in a continuous sheet of material.

Cam-driven dies, also known as wedge-driven dies, can be used to increase production rates as well as workpiece quality. When using an acute-angle die at normal press speeds, the sheet travels in a large arc, causing a bend at the outside die edge. This problem can be solved by decreasing the speed of the operation, but a better solution is to use a cam-driven die to ensure that the sheet will lay flat while maintaining the press speed at the desired rate.

Dies for Shearing, Lancing, Blanking, Piercing, and Notching

Shearing can be done in a press brake, but hold-downs and knife supports must be used to obtain reasonable accuracy. For optimal results, a shearing machine should be used (see the article "Shearing of Plate and Flat Sheet" in this Volume). There are, however, applications in which shearing in a press brake is convenient because it can be combined with another operation.

Lancing is often done in a press brake. An example is the production of louvers, which are used in cabinet or locker doors. The punch, die, and die pad used for the simultaneous lancing and forming of sheet metal into louvers are shown in Fig. 15.

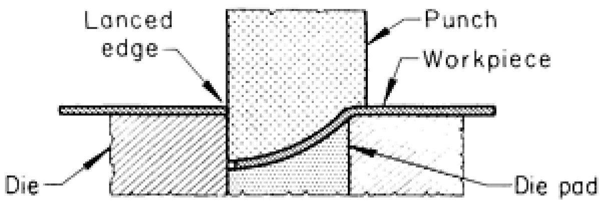


Fig. 15 Punch, die, and die pad used for the simultaneous lancing and forming of louvers in a press brake.

Blanking. When long, narrow dies are required for a given application, metal can be blanked in a press brake if adequate support can be provided by the dies or the press brake. The removal of long, narrow workpieces is sometimes a problem. Spring-type or rubber strippers can be added if excessive adherence to the punches is encountered.

Piercing and Notching. Press brakes are extensively used for piercing (punching) and notching. A press brake is more practical than a punch press for the piercing or notching of long, narrow workpieces, such as flats, channels, or other cross-sectional shapes. Press brakes are especially well adapted to piercing holes close to the edge of long panels or to notching the edges. Quick-change punching units help to extend the versatility of a press brake for piercing and notching. These punching units can be quickly changed to accommodate different workpieces by using setup templates.

Rotary Bending

Wiping dies, V-dies, and U-dies have traditionally been used in press-brake forming. Rotary bending is gaining wide acceptance in industry because it significantly reduces the time required to bend materials. Rotary bending is accomplished by using a tool that simultaneously holds and bends the material.

Key components of a rotary bender are the saddle (punch), the adjustable rocker, and the die anvil. The cylindrical rocker features an 88° V-notch cut out along its full length. To minimize marking, the edges of the rocker jaws are flatted and radiused. Figure 16 illustrates the three stages of a rotary-bending operation. Initially (Fig. 16a), the material is clamped, and the rocker rotation begins. Figure 16(b) shows how humping is controlled and confined to the space between the rocker edges. The final step (Fig. 16c) shows how the rocker clamps the workpiece in place and overbends it sufficiently to compensate for springback.

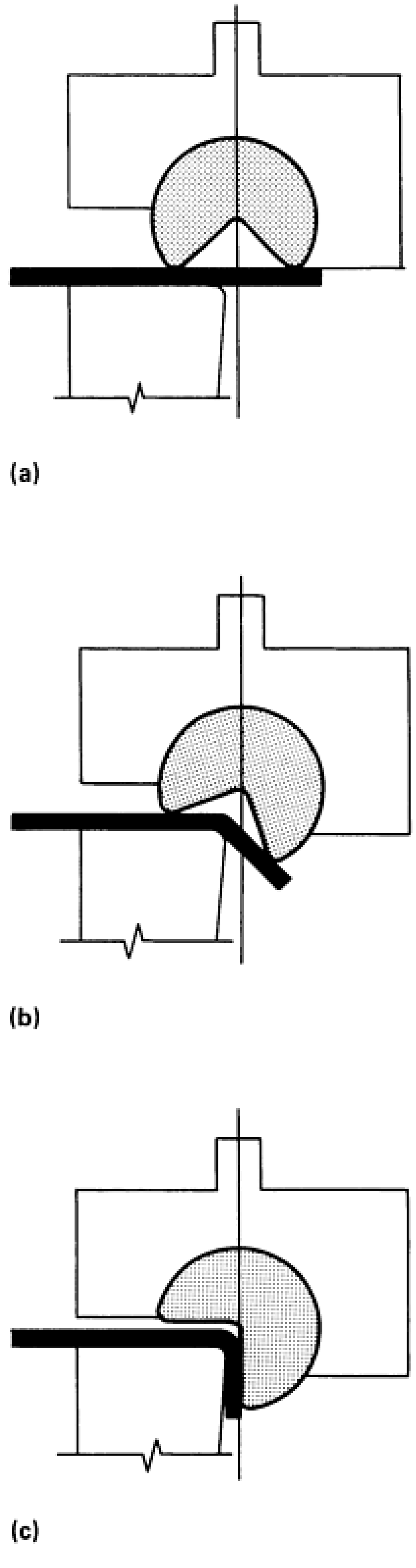


Fig. 16 Sequence of operations illustrating rotary bending in a press brake. (a) Material is clamped, and rocker rotation begins. (b) Humping is controlled and limited to space between edges of rocker. (c) Workpiece is clamped in position by rocker and overbent to allow for springback.

Rotary benders have been primarily used in conjunction with progressive dies to form Z-bends and short-leg bends in a single operation. Also, dart stiffeners can be rolled into the workpiece simultaneously as it is being bent.

Selection of Tool Material

Selection of tool material for punches and dies used in press brakes depends on the composition of the work metal, the shape of the workpiece (severity of forming), and the quantity to be produced. The tool material used for press-brake bending and forming ranges from hardwood to carbide, although the use of carbide has usually been confined to inserts at high-wear areas. Hardwood and carbide represent the rare extremes; hardwood is suitable only for making a few simple bends in the most formable work metal, and carbide would be considered only for making severe bends in a less-formable work metal (such as high-strength low-alloy steel) in high production.

Simple Bending. Most dies and punches used for simple V-bending operations are made from low-carbon steel (such as 1020) or gray iron. Both of these materials are inexpensive and give acceptable tool life in mild service.

If production runs are long or if the work metal is less formable, some upgrading of the tool material may be desired to retain accuracy over a longer period. Gray iron can be upgraded without adding greatly to tool cost by making both punch and die from a hardenable grade (such as ASTM class 40) and then flame hardening the nose of the punch and the upper edges (high-wear areas) of the V-die to 450 to 550 HB. Low-carbon steel tools can be upgraded by changing to a hardenable grade of steel (such as 1045). High-wear portions of the tools can be flame hardened (usually to 50 to 55 HRC) in the same manner as the gray iron tools.

Severe Bending. As the severity of bending and forming increases, such as in producing channels in a single stroke (Fig. 5a), tool materials should be upgraded when more than low-production quantities are needed. For operations that require severe bending, tool material requirements for press-brake operations parallel those for punch-press operations (see the article "Selection of Material for Press-Forming Dies" in this Volume).

Blanking and piercing are done in a press brake with tools of the same materials as those used in punch presses (see the article "Selection of Material for Blanking and Piercing Dies" in this Volume).

Rubber Pads. The use of rubber pads in press-brake dies (Fig. 17) enables the forming of shapes that are difficult or impossible to form without the pads. The pads also minimize damage to work metal surfaces and decrease die cost. Additional information is available in the article "Rubber-Pad Forming" in this Volume.

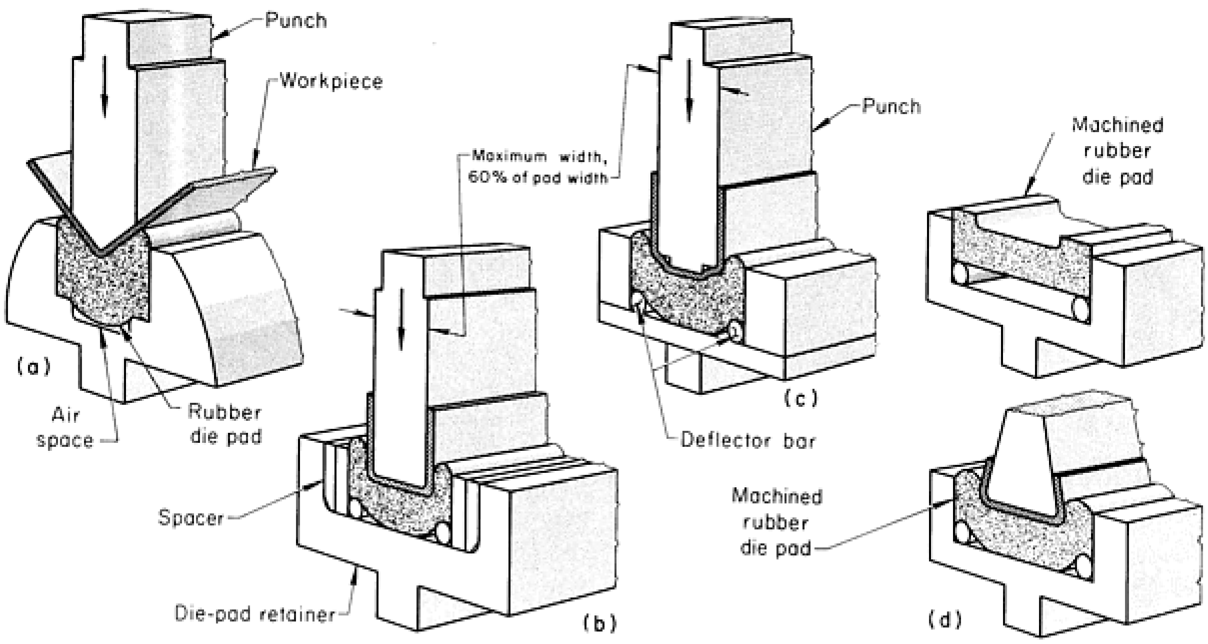


Fig. 17 Setups for rubber pad forming of various shapes in a press brake. (a) Simple 90° V-bend. Air space below die pad permits deep penetration. (b) Simple U-bend or channel. Spacers enable channels of varying widths to be formed in the same die-pad retainer. Deflector bars help to provide uniform distribution of forming

pressure. (c) Modified channel, with partial air bending. (d) Acute-angle channel. High side pressures are obtained by using a conforming rubber die pad and deflector bars.

Urethane rubber is the type most widely used. Pads inserted into the bottom of the die can be used for forming V and channel sections in various metals ranging from soft aluminum to low-carbon steel up to 12 gage (2.657 mm, or 0.1046 in.) in thickness. When using the urethane-pad technique, the urethane is, in effect, the die. It is almost impossible to compress urethane; its shape changes but not its volume. With minimum penetration of the punch, the pad begins to deflect, exerting continuous forming pressure around the punch. At the bottom of the stroke, the urethane has assumed the shape of the punch. When the pressure is released, the pad returns to its original shape.

Urethane pads are generally used for short-run production. However, in one plant, 14,000 boiler-casing channels were formed from 16 gage (1.52 mm, or 0.0598 in.) low-carbon steel in 4.9 m (16 ft) lengths on the same urethane pad before replacement.

Urethane rubber is made in several different grades ranging in tensile strength from 18 to 76 MPa (2600 to 11,000 psi) and in hardness from Durometer 80A to 79D. (Additional information is available in the article "Miscellaneous Hardness Tests" in *Mechanical Testing*, Volume 8 of *ASM Handbook*, formerly 9th Edition *Metals Handbook*.) Selection of grade depends on work metal hardness and thickness and on severity of forming. Experimentation is often needed to determine the optimal grade of urethane for the application.

Work Metal Finish. When preservation of work metal finish is a primary objective, the dies or punches or both are sometimes chromium plated. Other means of preserving the work metal finish include the use of oil-impregnated paper between the tools and the work metal, or spraying the tools with a plastic of the type used to coat metal sheets for deep drawing.

Procedures for Specific Shapes

Procedures and tooling for press-brake operations vary widely and are mainly influenced by workpiece shape. The following examples describe the procedures used for producing several different shapes, including simple boxlike parts, panels, flanged parts, architectural columns, fully closed parts, and semicircular parts.

Example 2: Four-Stroke Forming of Closed-Bottom Boxes From Notched Blanks.

Closed-bottom boxes were produced from 1010 steel blanks sheared to 508 × 610 mm (20 × 24 in.) in a press brake. The four corners were notched to a depth of 102 mm (4 in.) in a standard notching die in the same press brake as that used to make the blanks. The box was then formed in four strokes of the press brake, using a standard 90° V-die but with a deeper-than-normal punch (Fig. 18). By bending the 305 mm (12 in.) sides first, all four bends could be made with a die 406 mm (16 in.) long. Hourly production rates for the three operations were:

Operation	Pieces
Shear blanks	**55**
Notch corners (four strokes)	**57**
Bend from sides (four strokes)	**44**

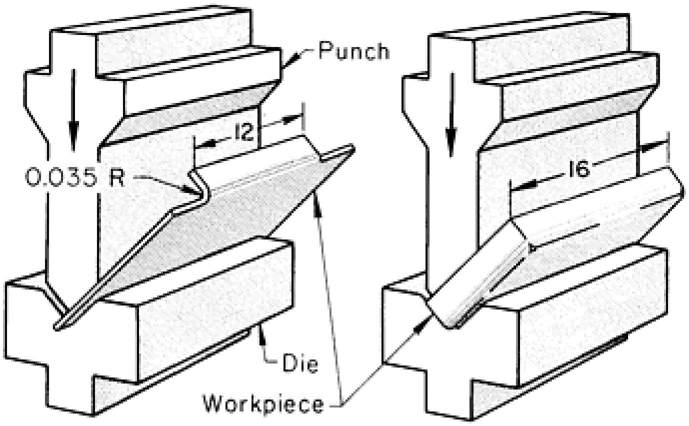


Fig. 18 Die and punch setup for bending sides in the production of a closed-bottom box. 1010 steel, 55-65 HRB. Dimensions given in inches.

Example 3: Six-Operation Forming of an Architectural Column.

An architectural column 3 m (10 ft) long was produced in six operations in a press brake. Figure 19 shows the sequence of shapes produced. Channel dies were used for operations 1 and 2. Operation 3 required a special punch and die for producing the large-radius bends. A simple channel die was used for operation 4, and a V-die for operation 5. Operation 6 was performed with a gooseneck punch that was necked-in sufficiently to clear the edge flanges as the part closed in.

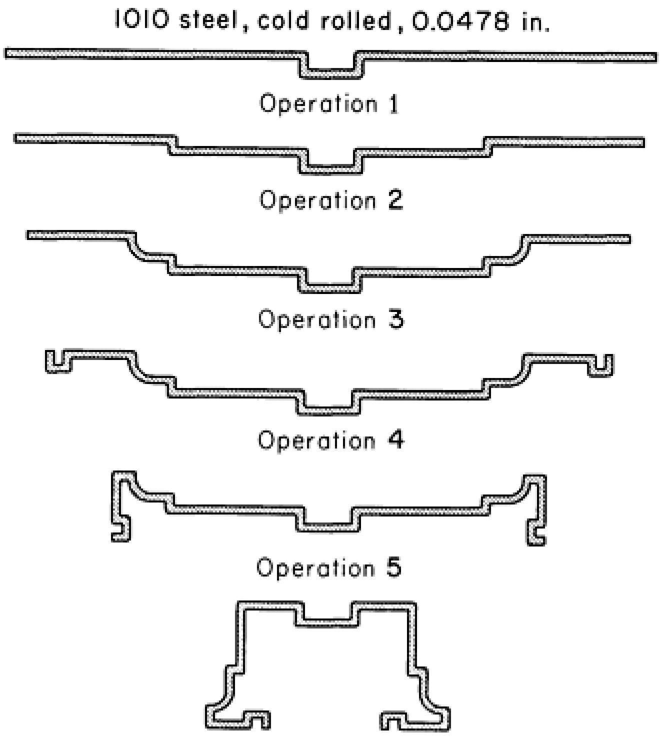


Fig. 19 Shapes progressively produced in six-operation forming of an architectural column in a press brake using 1.21 mm (0.0478 in.) thick steel sheet.

The major problem in forming this shape was to obtain sharp 90° bends at all corners and to keep the flanges in the same plane. Because the part was 3 m (10 ft) long, considerable shimming of the tools was required to produce satisfactory parts.

Correct shimming is a major factor in maintaining accuracy when producing shapes in a press brake such as that shown in Fig. 19. Shims are required to adjust for the discrepancies between bed plate and bolster. Also, deflections produced by the punch bottoming on all hits will be greater in the center of the die than at the edges, and shimming is required to equalize the pressure along the entire length of the bend. Optimal shimming is accomplished mainly by trial and error, because of the variations among machines, tools, and workpieces.

Example 4: Producing a Completely Closed Triangular Shape.

Figure 20 shows the four separate setups that were used to produce an 864 mm (34 in.) long completely closed triangular part in a press brake. The 541 × 864 mm ($21\frac{9}{32}$ × 34 in.) blanks were prepared by shearing on separate equipment. As shown in Fig. 20, the first pressbrake operation produced a 90° bend, and the second operation produced a 68° bend; simple straight-sided punches were used for both bends. In the third operation, a special punch 29 mm ($1\frac{1}{8}$ in.) thick and having an offset nose was used to produce a 32° bend. By bending only to 32°, sufficient space was allowed for withdrawal of the punch. The punch had an offset nose because of the off-center seam location (a design requirement); if the seam had been centered, the punch would have been symmetrical. In the fourth operation, the part was closed. The part just before the fourth operation is shown at the upper right-hand corner in Fig. 20. Time to complete the four operations was 0.9916 min. Total time per part was 1.0412 min (production rate: 57.7 parts per hour).

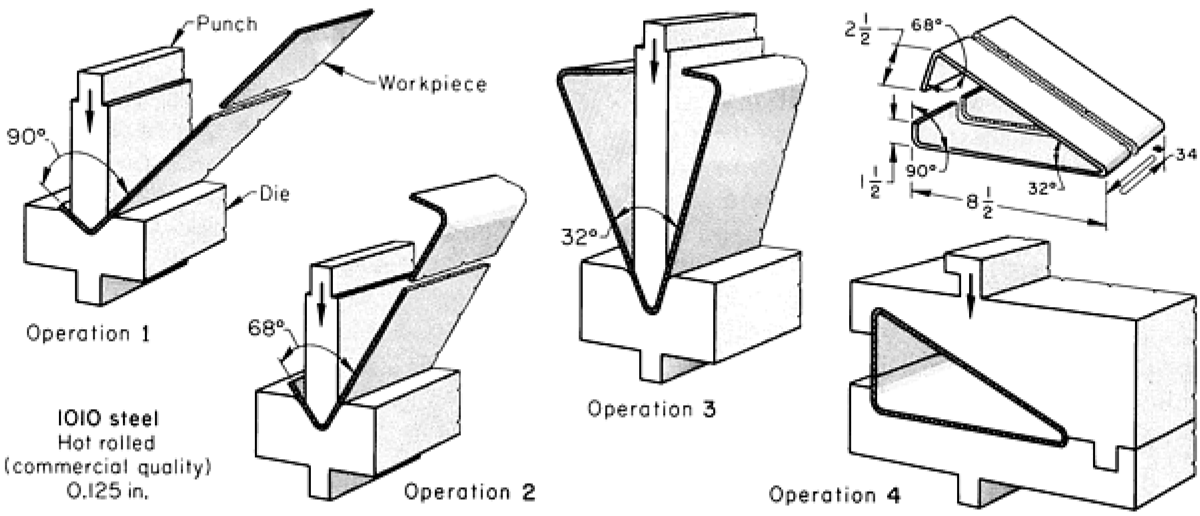


Fig. 20 Sequence of operations for forming a closed triangle in a press brake. Dimensions given in inches.

Semicircular Shapes and U-Bends. Flat stock can be formed into semicircular shapes and U-bends in a press brake. If the press capacity is adequate for the work metal thickness and the dimensions required, forming can be done in one operation, as in 90° V-bending. As shown in Fig. 5(b), the radius of the punch nose forms the inside radius of the workpiece.

Air bending is used to form semicircular shapes and U-bends when the work metal thickness and the dimensions exceed press capacity. A typical setup is shown in Fig. 21. Starting from the end of the workpiece at the right, and a distance of half the span of the die opening, pairs of equally spaced center-punch marks are made near each side of the workpiece, progressing to the left in two straight lines at 90° to the bend axis. These two rows of punch marks guide the operator in maintaining the alignment of the bend.

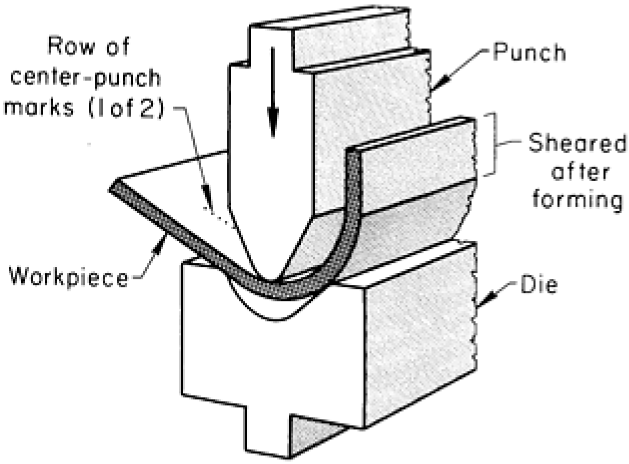


Fig. 21 Air bending to form a semicircular part by progressive strokes of the punch.

To form the part, the blank is placed across the die opening with the center-punch marks facing up, so that the punch will contact the blank at the first pair of punch marks. A bend is made at each pair of punch marks progressively toward the center of the blank (Fig. 21). When the center is reached, a quarter circle will have been formed. The blank is rotated 180°, and the procedure is repeated until a semicircle is formed. The radius of the semicircle will depend on the amount of bending done with each blow of the press and on the distance between the punch marks.

Bending should always proceed from the end of the blank toward the center in order to avoid interference between the ram and the formed workpiece. After forming, the straight section at each end of the workpiece is sheared off. The following example describes an application of this procedure for producing semicircular parts by air bending.

Example 5: Forming Semicircular Parts by Air Bending.

Two semicircular parts were formed from 19 mm ($\frac{3}{4}$ in.) thick low-carbon steel plate, and the parts were welded together to produce a 254 mm (10 in.) OD hollow cylinder 254 mm (10 in.) long. Blank size for each semicircular part was 254 × 470 mm (10 × 18$\frac{1}{2}$ in.). The bend length required for each part was 368 mm (14$\frac{1}{2}$ in.), but the blanks were cut to 470 mm (18$\frac{1}{2}$ in.) to allow for trimming after forming (Fig. 21).

Before forming, two rows of center-punch marks were made on the blanks (Fig. 21). The first mark was 50 mm (2 in.) from the blank edge, with subsequent marks every 13 mm ($\frac{1}{2}$ in.) of the 368 mm (14$\frac{1}{2}$ in.) bend length.

The parts were formed in an 1800 kN (200 tonf) mechanical press brake using a standard V-die and round-nose punch. Thirty bends were required to form each semicircular part. After several bends, the curve was checked with a template to determine the accuracy of the bend. After forming a quarter of the circle, the workpiece was rotated 180°, and the operation was repeated to complete the half circle. The final bend was made at the center of the workpiece. The 50 mm (2 in.) allowance at each end of the workpiece was then sheared off.

Corrugated Sheet. Special procedures allow bends to be fabricated in corrugated metal that are perpendicular to the corrugations, without flattening the corrugations. This can be achieved by using cast-on plastic blankets.

Effect of Work Metal Variables on Results

Thickness variations, yield strength, and rolling direction are the work metal variables that have the greatest effect on results in press-brake operations. Whenever possible, any metal to be formed should be purchased only to commercial tolerances; special tolerances increase cost. However, when workpiece tolerances are close, it is sometimes necessary to purchase metal with special thickness tolerances, because normal variations can use up a substantial amount of the assigned final tolerance (see the section "Dimensional Accuracy" in this article).

Yield Strength. As the yield strength of the work metal increases, so does the difficulty in bending. This difficulty occurs as cracking at the bends, increased power requirements, or an increase in springback.

For example, bending of stainless steel requires about 50 to 60% more power than bending a comparable thickness of low-carbon steel. Because of its resistance to bending, stainless steel often causes difficulty in obtaining acceptable results. Additional information is available in the article "Forming of Stainless Steel" in this Volume.

Springback. In a wiping type of bending operation, in which the metal is bent to position but the corner is not coined to set the bend, the metal attempts to return to its original position. This movement, known as springback, is evident to some extent in all metals, and it increases with the yield strength of the metal. The amount of springback is usually negligible for a soft metal such as 1100 aluminum alloy. However, for aluminum alloys such as 2024, the amount of springback can be significant. In general low-carbon steels exhibit more springback than aluminum or copper alloys do, and still more springback can be expected for stainless steel.

A common technique for overcoming springback is to overbend by approximately the number of degrees of springback. Several trials in tool development may be needed to obtain the proper angle, because of variations in mechanical properties, work-hardening rate, metal thickness, and die clearances. Springback from one bend can sometimes be used to offset that from another. Tables and graphs for springback have been developed for specific metals. Detailed information on the subject of springback is provided in the Section "Forming of Nonferrous Sheet Materials" in this Volume.

Another technique for overcoming springback is the use of specially designed bottoming dies that strike the workpiece severely at the radius of the bend. This action stresses the metal in the bend area beyond the yield point through almost the entire thickness and thus eliminates springback. Bottoming must be carefully controlled, particularly if it is done in a mechanical press brake, because the force developed by this machine can be very high.

Restriking in the original dies or special fixtures will reduce springback to a low level. It requires an additional operation, but may entail little or no additional equipment. In the following example, a second stroke was used.

Example 6: Correcting Springback in the Forming of a Complex Shape.

The shape shown at the lower right-hand corner in Fig. 22 was produced from 0.91 mm (0.036 in.) thick 1010 steel in lengths ranging from 0.9 to 2.4 m (3 to 8 ft). The five operations used in producing the part are shown in Fig. 22(a) to (e). The box section was formed by a wiping action (Fig. 22d) with no force on the outermost portion of the box. Consequently, springback occurred and required another step to correct by overforming (Fig. 22e). The shut height of the die was adjusted to provide the correction.

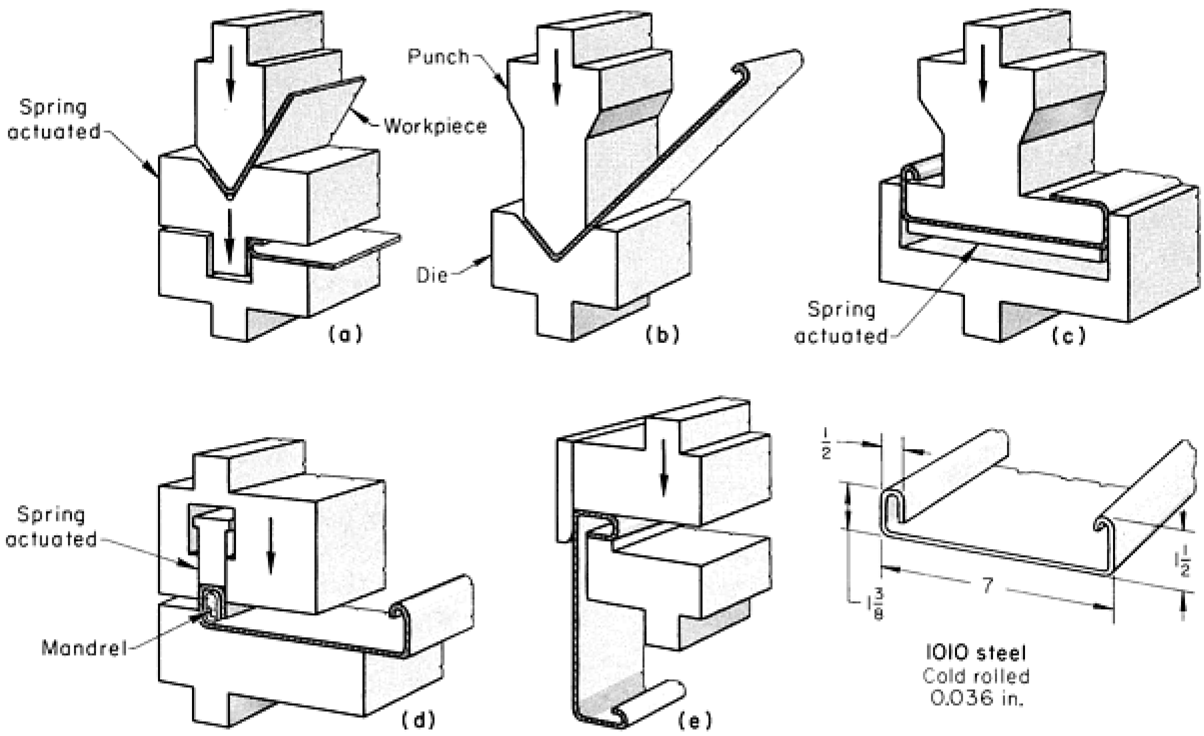


Fig. 22 Setups and sequence of operations for forming a complex shape in a press brake showing use of a restriking operation to eliminate springback. (a) Forming hem in two strokes. (b) Forming of first 90° angle for box section. (c) Forming channel. (d) Closing of box section over a mandrel. Part was moved by sliding it off mandrel. (e) Restriking of box section to eliminate springback. Dimensions given in inches.

Rolling Direction. In the press-brake forming of steel, the effect of rolling direction is often a greater problem than in other methods, because long members are usually bent in a press brake and bends are made with axes parallel to the rolling direction, which is the least favorable orientation. However, it is sometimes possible to take advantage of directionality. The most severe bends can be made perpendicular to the direction of rolling, or if several bends are required along axes that are not parallel with each other, the layout can be planned so that all bends run diagonally to the direction of rolling. The difference in behavior of the same steel bent in both directions in a press brake is demonstrated in the following example.

Example 7: Effect of Rolling Direction on Bending.

An axle bearing support was produced in four bends (Fig. 23) in a press brake using standard 90° V-dies. Cracks could not be tolerated. No cracks appeared on the flanges formed on the short dimensions, which were bent 90° to the direction of rolling to a 6.4 mm ($\frac{1}{4}$ in.) radius. However, in bending the flanges on the long dimensions, parallel with the direction of rolling, open breaks appeared along the length of the bend. To prevent this cracking, it was necessary to increase the bend radius on the long dimensions to 13 mm ($\frac{1}{2}$ in.) and to prepare the blanks so that the long flanges were formed at a slight angle to the direction of rolling.

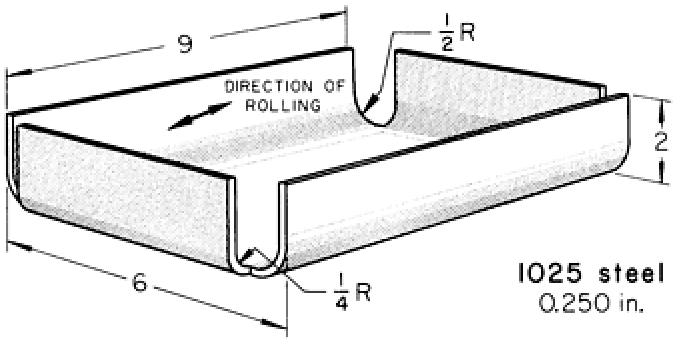


Fig. 23 Axle bearing support for which blank was prepared so that long flanges were formed at a slight angle to direction of rolling to prevent cracking. Dimensions given in inches.

Relation Between Bend Angle and Rolling Direction. As the thickness and yield strength of the work metal increase, the relationship between bend angle and grain direction becomes more important. For example, when stock thickness reaches about 25 mm (1 in.) and the yield strength is relatively high, as in high-strength low-alloy steels, the bend radius should be at least twice (and preferably three times) the stock thickness, even for bends of no more than 45°, when the bend axis is parallel with the direction of rolling.

In the press-brake forming of long, narrow workpieces, bending at an angle to the direction of rolling is seldom practical. For such work, the use of steel sheet that has been cross rolled or subjected to a pinch pass is a simple but relatively expensive means of minimizing the adverse effects from grain direction.

Press-Brake Forming

Dimensional Accuracy

The generally accepted tolerance for dimensions resulting from bending is ±0.41 mm (±0.016 in.) for metals up to and including 3.2 mm (0.125 in.) thick. For thicker metals, the tolerance is increased proportionately. As in many other mechanical operations, obtainable tolerances are influenced by design, stock tolerances, blank preparation, and condition of the machine and tooling. In some cases, close control of variables can provide closer dimensions at no additional cost; in others, cost will be increased.

Design. Bends or holes too close to the workpiece edges make it difficult to maintain an accurate bend line. Notches and cutouts on the bend line make it difficult to hold accurate bend location. Offset bends will shift unless the distance between bends in the offset is at least six times the thickness of the work metal.

Stock tolerances affect the dimensional accuracy of the finished part because they use up a portion of the assigned final tolerance. Commercial tolerances, particularly on thickness to which the specified metal is furnished, should be ascertained. For aluminum, there are minor differences in thickness limits between clad and unclad alloys. For steels, there are significant differences both in thickness tolerances and in cost among hot-rolled sheet, hot-rolled strip, cold-rolled sheet, and cold-rolled strip.

Cold-rolled steel sheet is produced to closer tolerances than hot-rolled sheet, but its cost is higher. Tolerances on steel strip, either hot rolled or cold rolled, are closer than those for corresponding sheet. Established tolerances are closer as the product becomes narrower or thinner.

Thickness tolerances for steel plate are considerably wider than those for hot-rolled steel sheet and strip. When ordered to thickness, the allowable minimum is 0.25 mm (0.010 in.) less than that specified, regardless of thickness, and the allowable maximum for an individual plate is $1\frac{1}{3}$ times the values that are published by the mills and expressed as a percentage of the nominal weight. Therefore, when tolerance requirements are stringent, it should be determined whether the metal can be obtained as strip or sheet rather than as plate.

Blank preparation can have an important effect on the tolerances and the cost of the finished part. If a blank is prepared by merely cutting to length from purchased stock, it will be low in cost, but the width tolerance will be that of the mill product. This may be greater than the tolerance obtainable by shearing. If it is necessary to shear all sides of a blank, the cost will increase, but good shearing can result in greater accuracy.

The stock from which blanks are cut must be flat enough for the blanks to be properly inserted into tooling and to remain in position during forming. Stretcher-leveled and resquared sheet costs a little more, but it is usually necessary when tolerances are close.

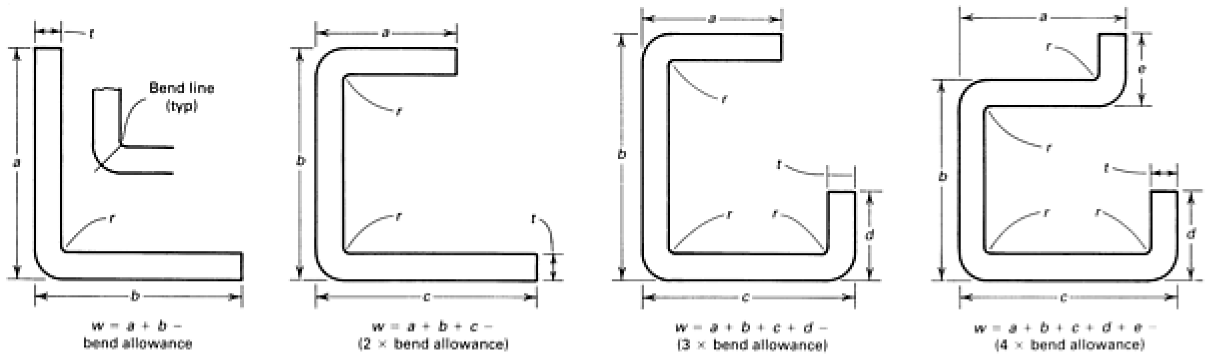
Blank Size. To determine the size of the blank needed to produce a specified bent part, the blank dimension (usually, the blank width) at 90° to the bend axis can be developed on the basis of the dimension along the neutral axis.

For 90° bends, as shown in Table 2, the developed blank width can be obtained by deducting bend allowances from the theoretical distance along the outside mold line. These allowances take into account the type and thickness of the work metal and the bend radius--each of which can affect the location of the neutral axis and therefore the developed width. The application of these allowances, which are based on shop practice with low-carbon steel and aluminum alloy 5052, is shown in the illustration in Table 2, for parts having one, two, three, or four bends.

Table 2 Bond allowances for 90° bends in low-carbon steel and aluminum alloy 5052

Metal thickness (*t*)		Bend allowance mm (in.), for bends with inside radius (*r*) of:									
		0.8 mm ($\frac{1}{32}$ in.)		**1.6 mm ($\frac{1}{16}$ in.)**		**2.4 mm ($\frac{3}{32}$ in.)**		**3.2 mm ($\frac{1}{8}$ in.)**		**6.4 mm ($\frac{1}{4}$ in.) steel**	13 mm ($\frac{1}{2}$ in.) steel
mm	**in.**	**Steel**	**Aluminum**	**Steel**	**Aluminum**	**Steel**	**Aluminum**	**Steel**	**Aluminum**		
0.81	0.032	1.50 (0.059)	1.45 (0.057)	1.68 (0.066)	1.73 (0.068)	2.01 (0.079)	2.08 (0.082)	2.36 (0.093)	2.41 (0.095)	3.71 (0.146)	**6.45 (0.254)**
1.27	0.050	2.21 (0.087)	1.98 (0.078)	2.57 (0.101)	2.31 (0.091)	2.90 (0.114)	2.67 (0.105)	3.28 (0.129)	3.00 (0.118)	4.27 (0.168)	**7.01 (0.276)**
1.57	0.062	2.67 (0.105)	2.41 (0.095)	3.00 (0.118)	2.74 (0.108)	3.35 (0.132)	3.05 (0.120)	3.68 (0.145)	3.38 (0.133)	4.65 (0.183)	**7.37 (0.290)**

1.98	0.078	3.25 (0.128)	2.95 (0.116)	3.61 (0.142)	3.33 (0.131)	3.94 (0.155)	3.66 (0.144)	4.29 (0.169)	3.99 (0.157)	5.13 (0.202)	**7.87 (0.310)**
2.29	0.090	3.71 (0.146)	3.30 (0.130)	4.06 (0.160)	3.66 (0.144)	4.39 (0.173)	3.99 (0.157)	4.75 (0.187)	4.32 (0.170)	5.52 (0.217)	**8.23 (0.324)**
3.18	0.125	5.03 (0.198)	4.44 (0.175)	5.36 (0.211)	4.80 (0.189)	5.69 (0.224)	5.16 (0.203)	6.17 (0.243)	5.49 (0.216)	6.61 (0.260)	**9.32 (0.367)**
4.78	0.188	7.34 (0.289)	6.50 (0.256)	7.67 (0.302)	5.51 (0.217)	8.02 (0.316)	7.19 (0.283)	8.36 (0.329)	7.54 (0.297)	9.73 (0.383)	**11.3 (0.443)**
6.35	0.250	9.71 (0.382)	8.59 (0.338)	10.0 (0.395)	8.92 (0.351)	10.4 (0.409)	9.27 (0.365)	10.8 (0.424)	9.60 (0.378)	12.1 (0.476)	**13.2 (0.519)**
7.95	0.313	12.0 (0.474)	. . .	12.4 (0.488)	. . .	12.7 (0.501)	. . .	13.1 (0.515)	. . .	14.5 (0.569)	**17.2 (0.676)**
9.52	0.375	14.4 (0.566)	. . .	14.7 (0.580)	. . .	15.1 (0.593)	. . .	15.4 (0.607)	. . .	16.8 (0.661)	**19.5 (0.768)**
11.1	0.437	16.7 (0.658)	. . .	17.1 (0.672)	. . .	17.4 (0.685)	. . .	17.8 (0.699)	. . .	19.1 (0.752)	**21.8 (0.860)**
12.7	0.500	19.0 (0.750)	. . .	19.4 (0.764)	. . .	19.7 (0.777)	. . .	20.1 (0.791)	. . .	21.5 (0.845)	**24.2 (0.952)**



Note: w, developed width of blank; t, metal thickness; r, inside radius of bend

For setting the stock stops from the centerline of the punch and die, the distance from the edge of the workpiece to the bend line at the neutral axis (Table 2) must be determined. To establish this value for 90° bends, subtract one-half the bend allowance from the outside flange width.

For bend angles other than 90° or radii other than those listed in Table 2, the width of a strip needed to produce a given shape can be calculated by dividing the shape into its component straight and curved segments and totaling the developed width along the neutral axis. Equation 3 can be used to determine the developed width, *w*, of a curved segment:

$$w = 0.01745\,\alpha(r + kt) \qquad \textbf{(Eq 3)}$$

where 0.01745 is a factor to convert degrees to radians; α is the included angle to which the metal is bent (in degrees); *r* is the inside radius of the bend (in inches); and *k* is the distance of neutral plane or axis from the inside surface at the bend expressed as the fraction of the metal thickness, *t,* at the bend. Empirically determined values of *k* are: $\frac{1}{3}$, for bends of radius less than 2*t;* and $\frac{1}{2}$, for bends of greater radius. Sample calculations showing the use of Eq 3 are presented in the article "Contour Roll Forming" in this Volume.

Permissible bend radii depend mainly on the properties of the work metal and on tool design. For most metals, the ratio of minimum bend radius to thickness is approximately constant, because ductility is the primary limitation on minimum bend radius. Another complicating factor is the effect of work hardening during bending, which will vary with metal and heat treatment.

Condition of Machines and Tools. Machines and tools must be kept in the best possible condition for maintaining close dimensions in the finished product. General-purpose tooling is seldom built for precision work and is frequently given hard use, which contributes further to inaccuracy through wear. Uneven wear aggravates the condition. If the press brake has been allowed to become loose and out-of-square and if ram guides and pitman bearings are worn, accurate work cannot be produced. Good maintenance is as essential in successful press-brake operation as in any other mechanical process.

Press-Brake Forming Versus Alternative Processes

For many applications, press-brake forming is the only practical method of producing a given shape. In one case, for example, press-brake forming was used for a massive workpiece 3 to 3.7 m (10 to 12 ft) long that required several bends spaced at least 152 mm (6 in.) apart.

Under certain conditions, either a punch press or a contour roll former will compete with a press brake in performance and economy. When a workpiece can be produced by two or all of these methods, the choice will depend mainly on the quantity to be produced and the availability of the equipment.

Press Brake Versus Punch Press. When a given workpiece can be made to an equal degree of acceptability in either a press brake or a punch press, the punch press is usually more economical, and it is more efficient than the press brake in terms of power requirements for a given force on the ram and number of strokes per unit of time. In addition, air ejection is more readily adapted to a punch press than to a press brake; this is a factor when air is required for ejecting either the workpiece or scrap.

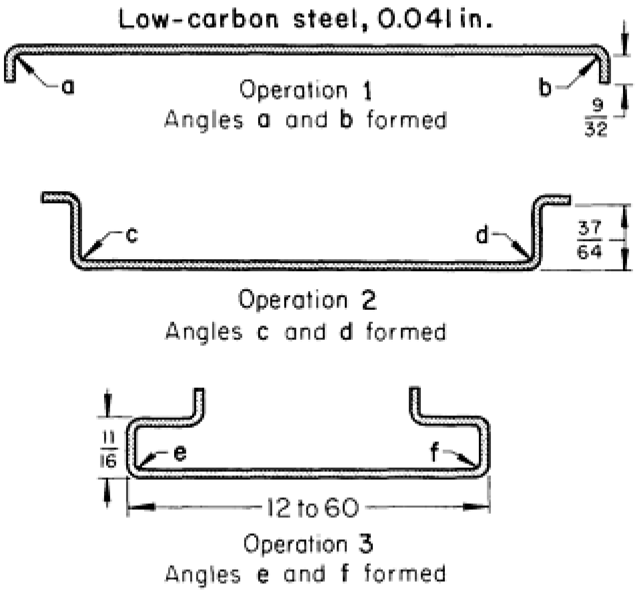
The advantages of a punch press over a press brake are generally greater when production quantities are large and workpieces are relatively small. As workpiece size increases, the advantages of a punch press diminish.

Tooling for a press brake is usually simpler and less costly than counterpart tooling for a punch press--an important consideration for small production quantities. One disadvantage of punch presses is that they are more sensitive to thickness variations of the work metal because they operate at a faster rate.

Press-Brake Versus Contour Roll Forming. For many parts usually formed in a press brake, contour roll forming is an acceptable alternative method of production, and the choice between the two processes depends mainly on the quantity to be formed. Press-brake forming is adaptable for quantities ranging from a single piece to a medium-size production run, while contour roll forming is usually restricted to large-quantity production because of higher tooling costs. An advantage of contour roll forming is that coil stock can be used, while cut-to-length stock must be used in a press brake (see the article "Contour Roll Forming" in this Volume). The following example compares the efficiency of press-brake forming and contour roll forming.

Example 8: Press-Brake Forming Versus Contour Roll Forming.

Parts were produced to the shape shown in Fig. 24 in lengths up to 3.7 m (12 ft) and widths varying from 0.30 to 1.5m (12 to 60 in.). The six bends were originally made in a press brake in three operations. When quantity requirements increased, production was changed to contour roll forming in a tenstation machine, from sheared-to-size sheets. Contour roll forming not only decreased the production time (Table , Fig. 24) but also resulted in improved surface finish, because less handling of the work metal was required.



Item	Time, h	
	Press brake	Roll former
Setup	2.1[(a)]	**9.2**[(b)]

(a) Total for all operations, including dies and gages.

(b) Includes dismantling.

(c) Pieces are 2.5 m (100 in.) long and 1.2 m (48 in.) wide.

Fig. 24 Workpiece formed in six bends in either a press brake or a ten-station contour roll former. Dimensions given in inches.

Safety

Press-brake operations involve the hazards of other press operations. Proper feeding devices are vital in order to ensure the safety of the press operator. Because more than one operator is often needed, added precautions are necessary to prevent the operation of a press brake without the direct consent of each man.

The article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume contains information and literature references on safe operation. Some of the precautions noted are discussed below.

Barrier guards should be used wherever possible. Hand feeding devices such as vacuum lifters, special pliers, or magnetic pick-ups should always be used to keep operators' hands clear of dies.

When a large workpiece extends in front of the die, the operator often must use his hands to support the workpiece during forming. If a barrier guard cannot be used, because of the arc of travel of the front leg during forming or because the workpiece is of such shape that a guard would prevent loading or unloading of the workpiece, the sheet should be inserted against back gages. These gages, or stops, are adjusted so that the workpiece cannot slide over them.

The workpiece is supported by hand only if there is no other way to support it, and even then only if the operator's hands are not within reach of the die or any pinch point. A die apron or table should be provided to aid in loading large sheets into the die and to act as a support for sheets that do not require hand support. The formed sheet should be removed from the front of the press; parts that cannot be unloaded from the front of the press are moved to the end for removal. End supports may be required to prevent the workpieces from falling.

For versatility, a press brake is provided with a foot pedal to operate the machine. The foot pedal must have a cover guard so the press cannot be tripped accidentally. A foot-operated press brake should incorporate a single-stroke mechanism and be used as a single-operator machine.

When a press brake is used as a power press for stamping, shearing, and notching operations, the foot pedal should not be used. Instead, the press brake should be equipped with electropneumatic clutch and brake controls and should be provided with a single-stroke device. The foot pedal is replaced with two-hand palm switches, which are spaced so that the operator must use both hands to hold the switches until the die is closed. If a press brake is used exclusively for press work, the foot pedal should be permanently removed.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Introduction

PRESS FORMING is a metalworking process in which the workpiece takes the shape imposed by the punch and die. The applied forces may be tensile, compressive, bending, shearing, or various combinations of these. In some applications, the metal requires appreciable stretching in order to retain the shape of the formed part.

Although some of the applications described in this article include cutting operations, such as blanking, trimming, and piercing, these operations are discussed in more detail in the articles "Blanking of Low-Carbon Steel" and "Piercing of Low-Carbon Steel" in this Volume. The production of hollow shells from flat blanks is covered in the article "Deep Drawing" in this Volume. Forming that involves only bending is treated in the article "Press Bending of Low-Carbon Steel" in this Volume. The selection of low-carbon steel sheet for formability is discussed in the article "Formability Testing of Sheet Metals" in this Volume and in the article "Sheet Formability of Steels" in *Properties and Selection: Irons, Steels, and High-Performance Alloys*, Volume 1 of the *ASM Handbook.*

Presses

The characteristics of the various types of presses used in forming sheet metal parts are discussed in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume. Restriking, coining, and embossing are usually done in presses with more available force capacity than that needed for the simple forming of similarly sized areas, because in these operations the metal is confined while being forced into plastic flow. Progressive dies are used in presses with enough force capacity to meet the total demands of the various stations and with enough dimensional capacity for the long multiple-station dies. Although some progressive dies are hand fed, most have auxiliary equipment, such as stock feeders, scrap choppers, coil reels, and chutes, to carry the finished parts to containers.

Whether or not a press has a die cushion has some effect on die design and construction costs. Single- and double-action presses are available in about the same ranges of bed size and force capacity. Shallow forming can be done in single-action presses using die cushions or springs to provide the blankholder pressure. Deeper forming and the forming of large irregular shapes generally must be done in double-action presses with die cushions.

Springs, cams, fluid pressure, or press knockouts are used for piece ejection. The use of blank feeders and piece ejectors or extractors depends on the production rate and safety requirements.

Transfer Presses. In transfer machines (eyelet machines), the mechanism for moving the workpiece from station to station is a part of the machine to which suitable transfer fingers are attached. Transfer presses are generally long-bed straight-side presses. The transfer mechanism as a part of the press is actuated by the main press drive or is powered separately. A dial feed is a type of transfer mechanism that moves the workpiece from die to die in a circular path rather than in a straight line. Transfer press technology has progressed to the point at which large automotive outer body panels can be formed in transfer presses.

Multiple-slide machines are designed for automatic, complete production of a variety of small formed parts. Flat stock is fed into a straightener, into a feed mechanism, and then through one or more presses incorporated in the multiple-slide machine for operations such as piercing, notching, and bending--often in a progressive die. The feed mechanism then moves the metal into the multiple-slide forming area, where it is first severed by a cutoff mechanism to predetermined lengths. The piece is usually formed around a center post by four sets of tools mounted 90° apart around the forming post. Finally, the part is stripped off the center post and dropped through a hole in the bed. More information on the forming of steel in these machines is available in the article "Forming of Steel Strip in Multiple-Slide Machines" in this Volume.

Press Forming of Low-Carbon Steel

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Speed of Forming

Speed of forming has little effect on the formability of steels used for simple bending or flanging or for moderate stretching. The maximum velocity of the punch when it contacts the blank in such conventional press forming is usually not greater than about 1 m/s (200 ft/min). However, the steels used for most parts that involve local stretching of more than 20% in forming move considerably over the face of the punch or flow appreciably over the blank-holder. The flow of the metal in such operations is controlled by frictional forces so sensitive to speed that the steel often stretches to failure before moving against the frictional forces, provided the punch velocity exceeds a critical value, which differs for each steel and die combination. A maximum punch velocity of 0.2 m/s (40 ft/min) is recommended; high punch speeds also shorten tool life.

Lubrication

The type of lubricant used usually has little effect on the grade of steel selected to form a given part. The main purposes of a lubricant are to prevent die galling and die wear and to reduce the friction over critical areas, thus allowing proper flow of metal and possibly a reduction in severity class. The selection of the optimal lubricant for a given part is a complex problem that depends on part geometry and the forming process used.

In progressive dies, a light oil sprayed on the strip as it enters the die is often enough to keep the stock lubricated through all stages. The oil is generally applied to the stock between the feeding device and the die. Applying oil to the stock ahead of the feeder may cause variation in the feed length, depending on the type of feeder.

For some applications, residual mill oil or the residue from emulsion cleaning provides enough lubrication for forming. When this is not adequate, a spray or mist lubricant can be applied to the work metal as it enters the die. More information on lubricants is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Dies

Dies for the press forming of low-carbon steel are made from a wide range of materials, including plastics, cast irons, tool steels, and cemented carbides. Severity of forming, number of parts to be produced, workpiece shape, work metal hardness, specified surface condition, and tolerances affect selection of the die material. These factors are discussed in the article "Selection of Materials for Press-Forming Dies" in this Volume.

Low-carbon steel can be formed by any of the several types of dies described in the following paragraphs. Workpiece size and shape, production volume, tolerances, and available presses are the major factors that determine the most suitable type of die for a specific application.

Single-operation dies perform one operation at a time and are individually loaded and unloaded. They are usually set up in a press, and the operation is performed on a specific lot size. The die is then removed from the press, and the next die in the sequence is set up. For continuous production, a line of presses, each operating a single die, can produce finished pieces from raw stock without interruption for change in setup. Occasionally, more than one die is set up in a press at a time, and the parts are moved manually from one die to the next. With this type of tooling, more than one operation is done in each stroke of the press. Single-operation dies are used when:

- The operations are so interrelated that they cannot be done in a compound die
- The amount of work done on a part is approaching press capacity, and more work would overload the press
- Production quantity is low, and two or more single-operation dies would be less costly than a die combining operations

Single-operation dies do not necessarily have a low production rate. Coil stock can be fed automatically into blanking dies at a high rate. Blanks can be fed into, and workpieces ejected from, forming dies either manually or mechanically. Presses with inclined beds permit high-speed loading and unloading.

Single-operation dies can often be run at high speed. When a higher rate of production is needed, it is sometimes more practical to increase the speed of the press than to use an additional die, provided the flywheel, bearings, gibs, and gears can withstand the additional speed.

Compound dies are one-station dies in which more than one operation is done on a workpiece in one press stroke without relocating the workpiece in the die. The operations must be such that their inclusion does not weaken the die elements or restrict other operations. The operations are generally done in succession in the course of the press stroke, rather than simultaneously.

Typical combinations of operations include:

- Cutting a blank from a strip and then forming
- Lancing and forming a tab or louver
- Forming a flange and embossing stiffening bead

When a die is used for blanking and forming a part, holes can often be pierced in the bottom with the same die. When pierced holes are required in a flange, piercing should be done after the flange has been formed; otherwise, the hole (and perhaps the edge of the flange) can be distorted. The combination of lancing and forming is common. Continued travel of the lancing punch does the forming. Flanging can be combined with forming or embossing if no metal flow is necessary after the flange has been formed.

A compound die may or may not cost more than a set of single-operation dies. Loading and unloading can be automated or manual. Compound dies are generally operated at slower speed than single-operation dies. In the automotive industry, single-operation and compound dies are both set up in a press line. Coil stock or blanks are automatically fed into the first press, and the workpiece is automatically removed and transferred to the next press, where the cycle is repeated until the workpiece is completed. Typical parts are front grills, hoods, roof panels, and deck lids.

Several operations can be performed successively on a workpiece in a press, using two or more compound or single-operation dies. The parts can be manually transferred from die to die, eliminating storage and transfer between presses. The capacity of a large-bed press may be more fully utilized by performing several operations during each press stroke.

Progressive dies perform a series of operations at two or more die stations during each press stroke as the stock is moved through the die. One or more operations are done on the workpiece at each die station. As the outline of the workpiece is developed in the trimming or forming stations, connecting tabs link the workpiece to the strip until the workpiece reaches the last station, where it is cut off and ejected from the die. Pilot holes that are engaged by pilot pins in the die keep the workpieces aligned and properly spaced as they progress through the die.

The initial cost of a progressive die is generally greater than that of a series of individual dies for the same workpiece. However, unless the production quantity is low, the lower setup, maintenance, and direct-labor costs for the progressive die will often outweigh its higher initial cost.

A set of individual dies is sometimes used for making a complex part prior to the designing and building of a progressive die. This is done for two reasons. First, a set of individual dies can usually be made in less time than a progressive die, thus permitting earlier production startup, and second, the experience gained in producing the parts in individual dies can be used in designing the progressive die. From this experience, it can be determined:

- How the metal flows and reacts in the die
- How much work can be done in each operation
- What is the best sequence of operations
- What the size and shape of the developed blank should be

Although a progressive die runs more slowly than a single-operation or a compound die for similar work, overall production is usually higher because the die is operated more continuously. Progressive dies are used to perform an almost endless variety of operations on one piece. Operations that can be combined in a progressive die include notching, piercing, coining, embossing, lancing, forming, cupping, drawing, and trimming.

Transfer dies are similar to progressive dies except that the workpieces being processed are not attached to a strip but are mechanically moved from station to station. A blank is automatically fed into the first station and moved to the next at each press stroke. The first station can be a blanking die, which cuts a blank from manually or automatically fed stock during each press stroke.

Selection of Steel for Forming

Hot-rolled commercial-quality rimmed steel is suitable for many forming applications and has the advantage of minimum cost. Cold-rolled, drawing-quality, special-killed, and temper-passed steel has maximum formability and yields parts with the best appearance and finishing characteristics, but it is more expensive.

In selecting steel for forming, attention must be paid to deoxidation practice. Killed steel is preferred where sheets must be free of significant changes in mechanical properties (strain aging) for a long time, where neither stretcher strains (Lüders lines) nor roller leveling is permitted, or where better mechanical properties are desired for severe forming applications.

Generally, killed steel has mechanical properties that are superior to those of drawing-quality rimmed steel (particularly, it has low yield point); killed steel also has better formability and performance, less tendency to form buckles, and is usually free from aging. However, inferior surface properties and more surface defects can be expected from killed steel than from rimmed steel, with consequent higher scrap or repair loss because of these defects. In addition, panels produced from killed steel are usually less resistant to handling damage and oil canning because of the lower yield strength of this steel.

Most major producers of stampings restrict the use of killed steel to the most severe draws, to low-volume parts when the steel inventory cannot be used before aging begins in rimmed steel, and to small or irregularly shaped parts for which sheet cannot be roller leveled successfully. To ensure optimal performance, killed steel should have a fine, flat, elongated grain; ASTM grain size 7 to 8 is preferred. Stretcher strains may often be removed by roller leveling if the size and shape of the blank permit. However, this is the responsibility of the supplier because killed steel is expected to be usable without roller leveling.

Adding a die operation may reduce the severity of forming enough so that a rimmed steel can be used instead of a killed steel. The overall cost of producing a part is usually the criterion for determining whether to use a more expensive grade of steel or a more expensive die system. Depending on blank size, die complexity, and number of pieces to be produced, the savings in material may offset the additional die costs.

Press Forming of Low-Carbon Steel

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Stretcher Strains

Stretcher strains, or Lüders lines, and the Piobert effect, also known as worms, are characteristic markings that appear on the surface of low-carbon steel that has been annealed as a final mill operation. These lines appear during the early stages of stretching and almost disappear as the stretch exceeds 5 to 10%. In tension, the lines are depressions in the surface; in compression, they are raised; and in bending, the same phenomenon causes flutes or kinks. Stretcher strains have no harmful effect on strength. In stampings that are visible in service, stretcher strains are generally unacceptable, because they show clearly through paint.

Stretcher strains can be avoided by a temper pass of about 1% cold reduction after final annealing. The correction is normally permanent in killed steel, but stretcher strains frequently recur in rimmed steel unless it is formed in 1 week or less, depending on the amount of temper pass, the temperature, steelmaking practice, and the amount of forming in the stamping.

The probability of stretcher straining can be eliminated from temper-passed rimmed steel and from insufficiently temper-passed killed steel by roller leveling through a machine that flexes the sheet sufficiently in bending to remove the sharp yield point and the yield point elongation that cause stretcher strains. This amount of cold work does not reduce drawing quality (in some steels, the quality may be improved by reducing the yield point), but additional strain aging is induced, which reduces formability if the steel is stored after roller leveling. Roller-leveled steel should be used within 24 to 72 h after leveling. The sheet should be passed through the roller leveler once in each direction because about 455 mm (18 in.) of the entering end of the sheet is not flexed.

Occasionally, a lift, or even a shipment, of steel does not respond to roller leveling. If such material is unsatisfactory after two passes through the roller leveler, it should not be used for parts that will be exposed in service. However, the performance of annealed steel used for a very difficult unexposed part can be improved by a single pass through the roller leveler.

Annealed sheet cannot be roller leveled for an exposed part, because the flex roll kinks the sheet so severely that, after forming, the deformation will not disappear. In addition, small stretcher strains will occur between the kinks.

Coil breaks and stickers have the appearance of stretcher strains, but both are distinctly different. Coil breaks are regularly spaced, and stickers are spotty. Roller leveling has no effect on these defects.

Strain Aging

The effect of aging of rimmed steel on formability is variable and may be impossible to predict on the basis of tests. One rimmed steel may not age at all, while another may make the most difficult draws when received and, after aging 30 days, may not make minimum draws.

After an operation such as blanking, forming, or finishing, strain aging is more pronounced than for unworked steel. It is therefore advisable to complete the sequence of operations on a part without intervening storage unless artificial aging tests positively indicate the absence of aging.

Artificial aging tests give an approximate measure of the strain-aging characteristics of the steel, but do not predict the time at which definite changes in mechanical properties will occur. Artificial aging does not change the tensile strength appreciably; however, yield strength and hardness will always increase, while elongation and uniform elongation will always decrease.

Press Forming of Low-Carbon Steel

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Surface Finish

The surface roughness of sheet steel has an effect on the finishing cost and the appearance of the formed product as well as on processing in dies and on other operations. Dull or slightly roughened surfaces are used especially in parts with the deepest draws in order to retain lubricant through the operations for minimum scoring of the dies and for better flow of metal over pressure pads. Sheet with a surface roughness of about 0.75 to 1.3 μm (30 to 50 μin.) draws well and is smooth enough for most painted parts, such as hood tops and fenders, that require average paint finish.

Single-dip or painted parts intended for trim and interior moldings require a smoother surface of about 0.25 to 0.5 μm (10 to 20 μin.). Sheet for average decorative chromium-plated parts should have surface roughness no greater than 0.25 μm (10 μin.) where the surface is to have no preparation except a light polishing to remove die marks. Parts with surface roughness as high as 0.4 μm (15 μin.) require additional surface preparation--for example, buffed copper plate applied before another plating.

The selection of fine-grain steel (ASTM grain size of 9) with minimum surface roughness for forming usually sacrifices some ductility and latitude in die design. With fine grain, the steel will be somewhat harder, higher in yield point and elastic ratio (yield point/tensile strength), lower in elongation and uniform elongation, and more likely to strain age.

Surface defects in unexposed parts may be acceptable if the function or the strength of the part is not affected. The following example describes the use of two different grades of steel for forming a concealed panel on which surface defects were acceptable and for forming a panel that required a smooth surface for painting.

Example 1: Effect of Grade of Steel on Surface Finish of Severely Formed Parts.

Figure 1 shows an inner panel for a glove-compartment door that was made of 0.81 mm (0.032 in.) thick cold-rolled drawing-quality rimmed 1008 steel. On some parts, stretcher strains appeared in the severely stretched flat surface, as shown in Fig. 1. When the stock strained, the parts were used for a part number where the surface was covered by another detail. Parts that had severe stretcher strains also cracked in the areas shown in Fig. 1 and were not acceptable.

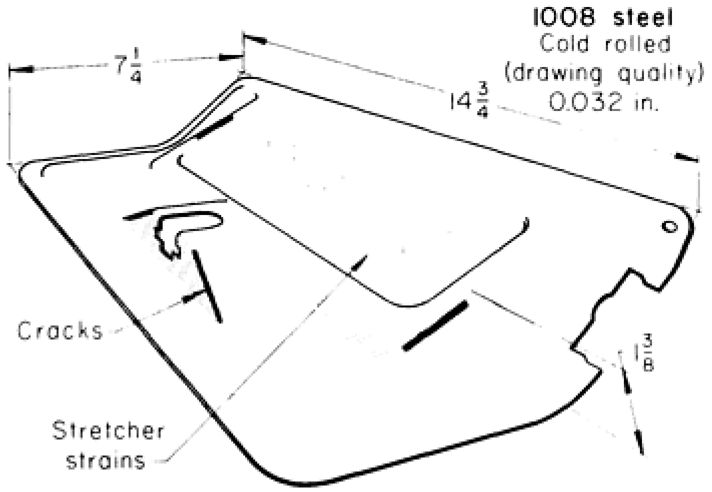

Fig. 1 Formed panel on which stretcher strains and cracks sometimes occurred. Some of the strained parts could be used in applications in which they were concealed in service; cracked parts were unacceptable. Dimensions given in inches.

The outer panel for the same door, however, was visible and had to have a maximum surface roughness of 1.15 μm (45 μin.) before it was painted. Killed or flex-rolled steel strip was used for the outer panel to minimize the stretcher strains.

The transfer die for the door panel was set up in a 7 MN (800 tonf) straight-side mechanical press operating at 500 strokes per hour. The die was cleaned after each shift, and it was resharpened after making 40,000 pieces. Lubrication was a chlorinated oil applied to the stock by rollers.

Revised by John Siekirk, General Motors Technical Center

Process Development

Users should control the type of tryout steel furnished for process and tool development and tryout runs. Steels that are below the average quality expected in regular production shipments should be selected. For tentative severity classification of a part, a steel near maximum in hardness and near minimum in formability should be used.

Tools developed with steel of below-average quality are seldom troublesome and run with minimum tool breakage and steel rejection when the production run begins. They are also less sensitive to pressure adjustments, variations in sheet thickness, or normal variations in steel properties, and maintenance costs are usually less. Conversely, tools developed with steel of above-average quality are often unsatisfactory when forming regular production shipments of steel. In the following example, rimmed steel was replaced by killed steel after initial forming experience.

Example 2: Use of Killed Steel to Avoid Annealing.

The steel originally used to form the cross-suspension member shown in Fig. 2 was 2.44 mm (0.096 in.) thick hot-rolled, drawing-quality rimmed 1010 steel. The blank edges strain-age hardened sufficiently during the time (up to several days) between blanking and forming to cause the edge in the hump area (Fig. 2) to fracture during forming.

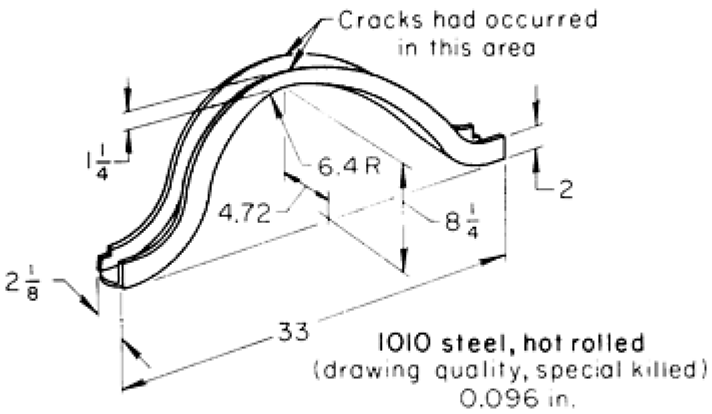

Fig. 2 Cross-suspension member that was made of killed steel rather than rimmed steel to avoid strain-age hardening that caused cracking during forming. Dimensions given in inches.

The part was produced in the following sequence of operations:

- Blank
- Coat with dry soap film lubricant
- Preform to start the center hump
- Form
- Pierce holes in the side of the channel section (not shown in Fig. 2)

In the hump area, the stretch along the edge was 25 to 33%. The blanking operation was separate from the forming operation, and 1 day or more usually elapsed between blanking and forming. Strain aging occurred during this period. The time between blanking and forming included application of the lubricant, which involved heating the blanks to 100 °C (212 °F) to dry the film. The strain aging occurred as a combined result of the baking temperature and time lapse.

In the first effort to correct this condition, the rimmed steel blanks were annealed before forming. The use of special-killed 1010 steel (also hot rolled and drawing quality) was then considered. The killed steel cost more than the rimmed steel, but the elimination of annealing and handling costs yielded a cost savings.

The parts were run in a 4.5 MN (500 tonf) double-action toggle press at 12 strokes per minute. Annual production was 300,000 pieces.

Annealing

Descriptions of procedures for full annealing, in-process annealing, spheroidizing, and normalizing are given in *Heat Treating*, Volume 4 of the *ASM Handbook.* Pieces formed from fully annealed sheet have a tendency toward stretcher straining and fluting; therefore, fully annealed sheet is used most often for unexposed parts, for which these conditions are not objectionable. Workpieces that must have a specified hardness after forming are made of annealed, spheroidize-annealed, or pretempered stock, depending on the severity of the forming operation.

Where only a spot or local anneal is needed for further forming, the area can be heated with torches to 870 to 925 °C (1600 to 1700 °F). The disadvantages of torch annealing are the lack of close control and the formation of scale. Areas that have been heated must be cleaned by pickling, abrasive blasting, or polishing.

In-process annealing is done after some press-forming operations to remove the effects of cold work and to increase formability for subsequent operations. To prevent the formation of scale, a protective atmosphere is used during the heating and cooling of the workpiece. An inexpensive exothermic atmosphere is sufficient for low-carbon steel. If a protective atmosphere is not available, the annealed work usually must be pickled or abrasive blasted to remove scale.

Process annealing often causes excessive grain growth. If the pieces have been subjected to a considerable amount of cold work before annealing, they may crack during subsequent forming and may have a rough surface appearance in the formed area.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Cleaning

The procedure for cleaning steel sheet before forming depends on the type and amount of soil present and on the finish specified for the formed surface. Removing soil before forming improves the surface finish, prevents marking of the formed piece, and prolongs die life. Large particles can be removed by wiping, which allows oil to remain and act as a lubricant. Coil stock can be cleaned by feeding it between wiping pads at the press. Another method is to feed the coil through a vat of emulsion cleaner or a light-duty drawing lubricant before it passes between the wiper pads. This procedure is an economical means of simultaneously removing foreign material and providing lubrication.

Cleaning the steel in this manner normally does not completely remove the smudge from the surface. This condition is generally desirable because the smudge acts as a filler in the lubricant. Many parts are formed using only mill oil with its smudge for lubrication. If the smudge must be removed, a rotating brush can be used between the emulsion cleaner and the press to scrub the work metal surfaces.

Scale is often removed by abrasive blasting or pickling before the hot-rolled sheet is formed. Both methods remove the residual lubricant. Workpieces that have been in-process annealed without a protective atmosphere are usually cleaned by abrasive blasting before final forming. The need to clean parts after forming is determined by the subsequent operations and by the necessary finish. The necessity for cleaning immediately after forming can be minimized by choosing lubricants that are compatible with welding, painting, plating cycles, and handling. More information on the cleaning of parts is available in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

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Deburring of Blanks

The deburring of blanks prior to forming depends on the operations performed on a blank and on the end use of the formed part. Burrs on blanks that will be severely formed can reduce formability and increase breakage. Burrs are unsafe and unsightly on the exposed portions of consumer products. Burrs on blanked or pierced parts cannot be avoided, but their formation can be minimized by the use of proper clearance between punch and die and by keeping tools sharp (see the articles "Blanking of Low-Carbon Steel" and "Piercing of Low-Carbon Steel" in this Volume).

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Work Metal Thickness

Thickness variations in sheet steel can cause parts made on the same tooling to be of different shapes. This is because of springback or because the pressure applied is either insufficient or excessive at sharp corners or at sides that are to be held at a predetermined angle.

If the sheet is too thick, a die or roll adjusted to a certain thickness may pinch the steel and localize the stretching, thus causing fracturing; or it may work harden the steel and cause excessive springback in a subsequent operation. Thickness greater than the die clearance may cause undesirable marring of the surface of the part or galling and scoring on the surface of the tools, and in some cases, it may be the reason for tool breakage.

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Workpiece Shape

The size and shape of the workpiece and the number of operations needed to make it must be considered in determining press capacity (both force capacity and bed size) and the type of tooling used. Open-end parts, or parts with one or more open edges, can be formed two or more at a time from a single blank. Sheet metal elbows that cannot be made from tubing are formed four halves at a time, then separated and assembled into two elbows. Small flanged parts with a low ratio of flange width to stock thickness are often difficult to form because of slippage and unbalanced forces. Forming two parts at a time can balance the forces and reduce scrap. More information on the factors that affect press selection is available in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume.

Recessed parts require special precautions in forming to avoid wrinkling in the flat area surrounding the recesses and to prevent cracking the corners of the recess. In the forming of a deep recess in a large panel, for example, adjustment of the blank-holder pressure may be necessary to prevent such failures. In this application, the deep recess is formed first, with the material being drawn into the recess from the end and two sides of the blank; stretching done in the final forming removes wrinkles.

When the cross section of the recess is a circular arc, acceptable percentages of stretch for recesses with various height-to-diameter, h/d, ratios are:

h/*d* ratio	0.10	0.15	0.20	0.25	**0.30**
Stretch, %	**3**	**6**	**10**	**15**	**22**

Dish-shaped parts having only one recess, of regular or irregular shape, are commonly formed by stretching the metal over a punch. The punch nose should be smoothly contoured so as not to trap the metal and should have as large a radius as possible. Parts with a straight sloping surface can be free formed by stretching between a clamp ring and a small-diameter punch. On large parts, stretch is most severe near the punch, and the work metal elongates and drapes from its own weight, forming an undesirable concavity in the wall. Both of these defects, uneven stretch and concavity, can be minimized by using a stepped punch.

Cone-shaped parts that are formed by a combined stretch-and-draw operation can be made in a progressive die without first cutting the contour. This reduces the number of die stations that are required, and more than one part can often be formed at a time.

Shapes With Locked-In Metal. In the forming of some shapes, metal may become locked-in (formed so that metal flow is stopped) before enough of it has been drawn into the cavity to form the part completely, and the metal sometimes fractures before the punch has reached the bottom of the stroke. In some cases, the strain that causes fracturing can be relieved by piercing a hole or lancing the metal in a noncritical area.

Severely Formed Shapes. Developed blanks are sometimes used to provide sufficient metal in critical areas of severely formed parts. Preforming helps to distribute the metal before final forming and restriking operations, thus reducing the severity of these operations. Edge condition and ductility greatly influence the success of severe forming.

Offset Parts (Edge Bending). Scrap loss can sometimes be reduced when blanks can be produced in simple rectangular form and subsequently edge bent in the final shape (see the article "Press Bending of Low-Carbon Steel" in this Volume). Whether or not the severity of the edge-bending operation will adversely affect subsequent forming operations must be considered. A potential savings of work metal can be quickly lost if edge bending work hardens the pieces to the extent that many are broken in forming. In some applications, the tensile and compressive stresses set up in edge-bent blanks can be counteracted by forming stretch flanges around the inner contours of the bends and compression flanges on the outer contours. The same flanges made on a cut blank instead of an edge-bent blank could fracture during flanging. On the other hand, reverse flanges made on an edge-bent blank may cause more severe stress than they would on a cut blank and may possibly lead to a high scrap rate.

The edge-bending operation and its tooling are an added expense in the fabrication of a part. This expense must be compared to the savings of work metal when one method is to be selected over the other.

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Large Irregular Shapes

Large parts are usually formed in single-operation or compound dies mounted in large-bed presses. If the part is irregular in shape, care must be taken in shaping the blankholder surfaces, in planning the orientation of the workpiece in the die, and in planning the die operation to obtain approximately even stressing of all areas of the workpiece and to form the piece in the fewest possible operations.

To make shallow draws, sometimes the metal must be locked in place by the blankholder in order to stretch the sheet metal over the punch enough to set the contours. Draw beads or selective local applications of drawing compound are used to restrict and control metal flow.

Large, irregularly shaped parts are often made by preshaping blanks before forming. This method reduces scrap when making the blank and neutralizes the tensile and compressive stresses in the flanges. The blanks for automobile fenders and other parts are preshaped by bending and tack welding them into a cone shape.

Use of Shaped Blankholders. Some parts that are made from preformed blanks are formed with the aid of shaped blankholders to avoid distortion of the previously formed contours. In addition, parts that are not completely formed in one operation may have to be clamped in a shaped blankholder in a second-operation die. The part in the following example had a peripheral offset that served as a locating surface in the second-operation die, but the part had to be clamped to prevent distortion during final forming.

Example 3: Holding a Previously Formed Workpiece in a Shaped Blankholder to Prevent Distortion During Subsequent Forming.

After the first forming operation, the blank for the fan shown in Fig. 3 had a 4.78 mm (0.188 in.) offset outer rim and a 22 mm ($\frac{7}{8}$ in.) deep recessed inner web. The surfaces were used as locators in a shaped blankholder for the subsequent trimming and forming operations.

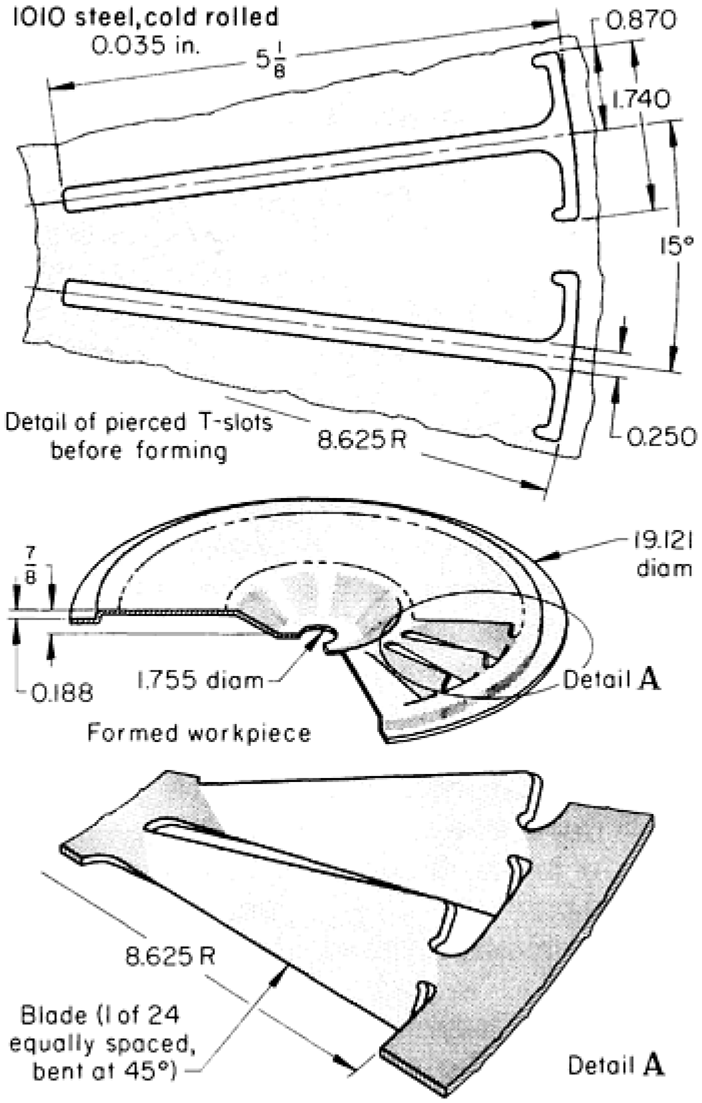

Fig. 3 Large sheet metal fan that was clamped in a shaped blankholder during the second forming operation. Dimensions given in inches.

Spring-loaded pressure pads were used to hold the rim and the web while the blades were twisted. The pressure pads bottomed at the end of the stroke so that any deformation in the rim or the web could be flattened. The 4.78 mm (0.188 in.) offset was placed over a close-fitting plug to prevent radial distortion of the rim.

Three operations were needed to produce the part. In the first operation, the 4.78 mm (0.188 in.) offset was formed to 471 mm (18.550 in.) in diameter, and the 22 mm ($\frac{7}{8}$ in.) deep recess was formed at the same time. In the second operation, the 485.7 mm (19.121 in.) outside diameter was trimmed, and the 44.6 mm (1.755 in.) diam hole and 24 T-shape cutouts at 15° spacing were pierced. The T-slots gave shape to the fan blades and necked the ends of the blades prior to twisting at 45°, which was done in the third operation.

The cutting elements in the dies were made of A2 tool steel. The forming dies for the offset rim and recessed web were of low-carbon high-strength alloy cast iron. The blade formers were made of prehardened low-alloy tool steel. The workpiece was made of a 0.89 mm (0.035 in.) thick cold-rolled 1010 steel blank 510 mm (20$\frac{1}{16}$ in.) square.

Recesses in Large Panels. The forming of recesses in large panels at some distance from the edge can be difficult. Because the sheet is large, the recesses appear to be shallow, but forming may actually be severe in terms of localized deformation.

If the recess is fairly small and far enough from the edges of the blank so that there is a large resistance to metal flow, the metal can be overstressed at the punch nose and can fracture or tear. To prevent tearing, blankholder pressure is kept as low as possible to allow almost unrestricted metal flow, but this may produce wrinkles radiating from the lip of the recess. These wrinkles can be removed by stretching the sidewalls of the recess in a second forming operation.

Large cup-shaped parts that must be stronger in the bottom surface than in the wall are often designed with tapered wall sections. A tapered section can be made by machining the part before or after drawing.

High-strength low-carbon steels of thinner gage than conventional low-carbon steels can be used without tapering, but they are more difficult to draw and are more expensive. The use of a tapered blank for a cup-shaped part is described in the following example.

Example 4: Forming a Large Disk From a Tapered Circular Blank.

The truck-wheel disk illustrated in Fig. 4 was formed from a blank 610 mm (24 in.) in diameter that had been roller tapered from a 500 mm ($19\frac{11}{16}$ in.) diam blank with a 90.5 mm ($3\frac{9}{16}$ in.) diam center hole. Taper was about 0.04 mm/mm (0.04 in./in.) of radius. The material was annealed hot-rolled 1012 or 1015 steel, 9.52 mm (0.375 in.) thick, abrasive blasted to removed scale and oxide.

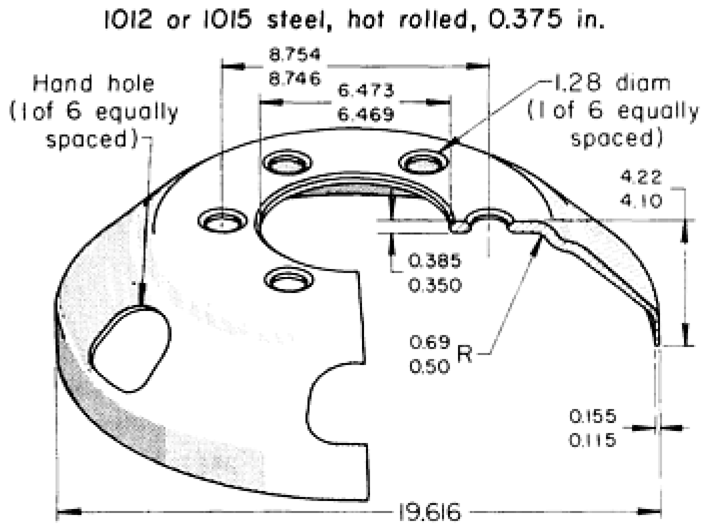

Fig. 4 Truck-wheel disk that was formed from a tapered blank. Dimensions given in inches.

The blanks were tapered back to back, two at a time, giving each disk only one rolled surface. The tapering rolls were stopped before reaching the edge of the blank in order to maintain an even taper. Otherwise, lack of resisting stock at the edge would have caused a torn edge. Tapering started at a diameter of 260 mm ($10\frac{1}{4}$ in.), just beyond the circle describing the outer edges of the six round holes; this gave a stock thickness of 8.38 mm (0.330 in.) at the 17.5/12.7 mm (0.69/0.50 in.) radius.

The combined forming and trimming operation was done in a 13.3 MN (1500 tonf) hydraulic press with the rolled surface of the work face down in the die. The blank was trimmed to 584 mm (23 in.) in diameter in a compound blank-and-draw die at a rate of 375 per hour. The die was made of O1 tool steel and was reworked after making 40,000 pieces.

A second operation sized the 498.2 mm (19.616 in.) outside diameter, enlarged the center hole to 164.4 ± 0.05 mm (6.471 ± 0.002 in.) in diameter, and pierced six 32.5 mm (1.28 in.) diam holes, using a 7.1 MN (800 tonf) mechanical press. In the third operation, six hand holes were punched, one at a time. To attach a rim to a disk, 16 rivet holes evenly spaced around the circumference of the flange were pierced in both the disk and the rim at the same time.

The optional 1015 steel had the highest carbon content that could be used in this part. Work hardening increased its strength and hardness. If the hardness of the workpieces exceeded 91 HRB, hairline cracks radiated from the rivet holes, causing rejection, as observed when 1020 steel was used.

Localized severe forming is encountered in making many large irregular shapes by press forming. This imposes stringent demands on process planning; quality of work metal; lubrication; and the design, material, and maintenance of dies--as demonstrated in the following example.

Example 5: Severe Embossing and Hole Flanging in Press Forming a Control Arm.

The severe embossing and hole flanging demanded on the automobile control arm shown in Fig. 5 required the use of drawing-quality steel for the workpiece and high-quality tool steel for the dies, close attention to tool maintenance, and the use of a heavy-duty lubricant. The stock was hot-rolled drawing-quality rimmed 1008 or 1010 steel, pickled and oiled, with a hardness of 55 HRB. Commercial-quality steel had been tried, but was unsatisfactory for the severe forming. Stock thickness was 3.96 or 4.17 mm (0.156 or 0.164 in.). Wall thickness of the hole flanges had to be at least 2.67 mm (0.105 in.), and flange width was 7.62 mm (0.30 in.). The parts were formed with dies of hardened W2 tool steel. A developed blank was used, making a final trimming operation unnecessary. The operations were as follows:

- Blank to developed outline, pierce two locating holes, form center boss--done in a 1.8 MN (200 tonf) mechanical press with W2 tool steel die inserts hardened to 57 to 60 HRC
- Form in a 5.3 MN (600 tonf) mechanical press using W2 tool steel dies hardened to 61 to 64 HRC
- Pierce two holes in flanges, enlarge one locating hole, pierce oval hole in center boss--done in a 620 kN (70 tonf) inclined-bed mechanical press
- Restrike to size and sharpen radii, form dimple, flange round hole and oval hole in a 5.3 MN (600 tonf) mechanical press
- Pierce remaining holes in a 620 kN (70 tonf) inclined-bed mechanical press
- Outward flange two side holes to 35.5/35.4 mm (1.398/1.394 in.) in diameter, 2.67 mm (0.105 in.) minimum wall thickness, and 7.62 mm (0.30 in.) minimum flange height, using hydraulic equipment designed for this part

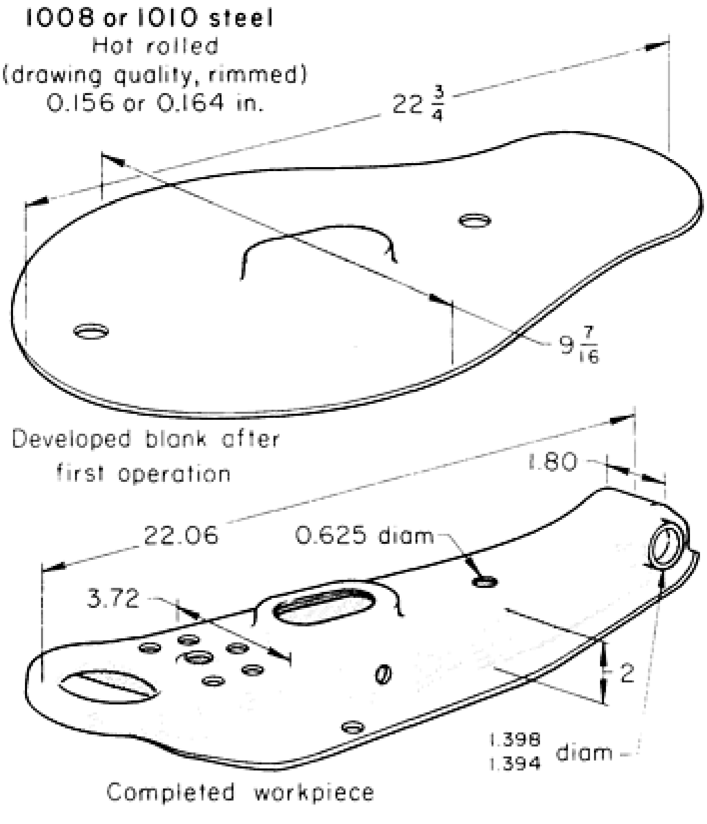

Fig. 5 Press-formed control arm on which embossing and hole flanging were of near-maximum severity. Dimensions given in inches.

A dry-film lubricant consisting of soap and borax was applied to the blank. Where further lubrication was needed, an oil-base compound was added. Production rate ranged from 200 to 325 pieces per hour, production-lot size was 10,000 pieces, and annual demand was more than 200,000 pieces. Except for minor repairs, the dies were good for one year of production.

Blanks That Cannot Be Nested. Irregularly shaped parts frequently must be made from developed blanks with a contour that makes close nesting of the blanks impossible. Channel-shaped parts with flanges or web of varying width use blanks that are cut with more scrap than parts with flanges or web of unvarying width, and an excessive amount of scrap may be generated in producing them.

In some applications, material that would otherwise be wasted can be moved into a useful location after notching or lancing. This was done in the following example, in which the web was notched at the end and then spread into a V-shape to increase flange height at the ends and to reduce blanking scrap.

Example 6: Use of a Notched Blank to Increase Flange Width and Reduce Scrap in Forming an Irregular Channel-Shaped Part.

A flange of varying width was needed on the channel-shaped truck-frame member shown in Fig. 6. Instead of using a contoured blank, which would have meant considerable waste in blanking, the additional flange width was gained by notching each end of a nearly rectangular blank and spreading the notch into a V-shape during the forming operation. The end of each notch was radiused to minimize cracking during forming. The stock saved by notching the blank was 1.4 kg (3 lb) per piece.

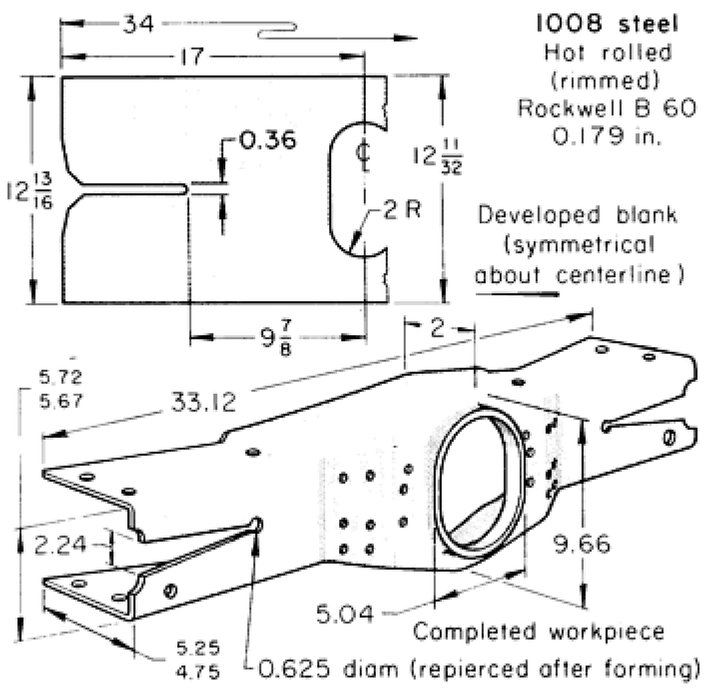

Fig. 6 Press-formed truck-frame member that was notched and spread to increase flange width locally and therefore save material in blanking. Dimensions given in inches.

The workpiece was made of 4.54 mm (0.179 in.) thick hot-rolled, rimmed 1008 steel, pickled and oiled. Each of the two notches in the blank was 9.1 mm (0.36 in.) wide by 181 mm ($7\frac{1}{8}$ in.) long. The notches were spread to make V-shape openings 56.9 mm (2.24 in.) wide at the ends. Flange widths varied from 50 mm (2 in.) at the center of the part to 127 mm (5 in.) at each end of the channel. The operations were:

- Cut blank to developed outline, and pierce large center hole (Fig. 6). A 3.1 MN (350 tonf) mechanical press with a mechanical unloader produced 300 to 400 blanks per hour
- Form completely, including flange around center hole, in a 16 MN (1800 tonf) hydraulic press
- Pierce all holes in the web, and trim 18.8 mm (0.74 in.) radius at two places on each end in a 2.9 MN (325 tonf) mechanical press
- Pierce holes in top and bottom flanges, and restrike flange ends in a 2.9 MN (325 tonf) mechanical press

Presses for the second, third, and fourth operations listed above, operating at 250 to 310 strokes per hour, were set up in a line with transfer equipment between them. In the second operation, a spreader was incorporated into the die to assist in opening the notch to a V-shape. The 181 mm ($7\frac{1}{8}$ in.) length of the notch had been carefully developed so that the notch would spread to the required maximum width without causing the work metal to split. After forming, a 15.87 mm (0.625 in.) diam hole was pierced at the end of each notch to remove any fractured material or other stress raisers aggravated by the severe edge forming.

The forming punches were made of 1045 steel. The wear surfaces on the punch and die were W2 tool steel hardened to 61 to 64 HRC. As a lubricant, a soap solution was dried on the stock. Some heats of steel were difficult to form; for these, an oil-base compound was used as additional lubricant.

The truck-frame member was made in lots of 27,000 pieces for an annual production of 270,000 pieces. Except for minor repairs, such as replacing small punches, the dies were good for 1 year of production.

Use of Draw Beads. A draw bead in a blankholder controls the movement of metal into the cavity by providing additional resistance to metal flow. The location of the beads is usually determined in die tryout; dies for producing similar parts can be used as a guide.

A single bead is generally placed around the cavity, and additional beads are placed in areas where more control is needed. Conditions may indicate that the bead size should be reduced or that the whole bead omitted in some places. Short beads can be placed at an angle to deflect metal into or away from local areas.

Whether the bead is placed in the draw ring or in the blankholder is determined by the die construction. Placing the groove in the upper member has the advantage that it will not catch dirt. However, the groove should be put in the member that is to be altered during spotting for mating of opposing surfaces. For convenience in making alterations, this is usually the lower member.

Unless they are part of the product design, draw beads are placed outside the trim line, as shown in Fig. 7(a). The trim line can be on the punch or the blankholder. A locking draw bead, such as that shown in Fig. 7(b), is used to provide maximum restriction to metal flow. Locking beads are used when forming to shape is done primarily by stretching the metal under the punch, rather than by moving metal into the cavity.

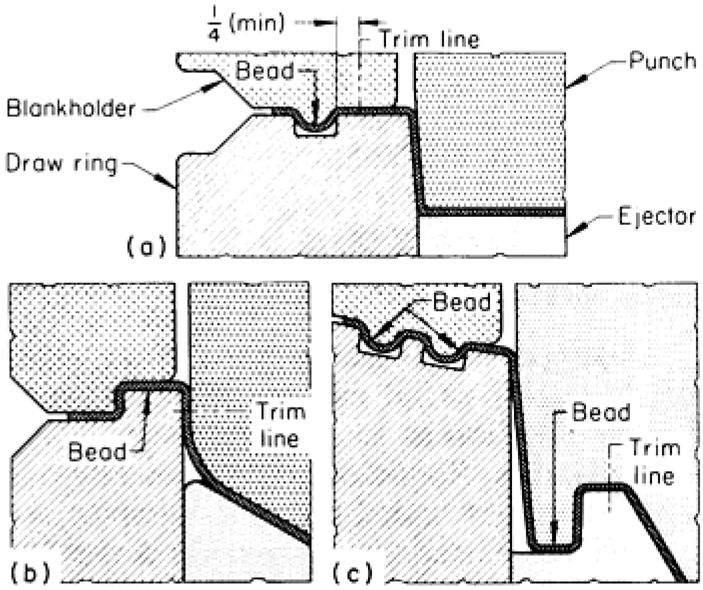

Fig. 7 Use of draw beads. (a) Conventional. (b) Locking. (c) Combined conventional and locking. Dimensions given in inches.

Figure 7(c) shows the use of conventional and locking beads. Here, conventional beads control metal flow into the die cavity until the last portion of the punch movement, when the locking bead gradually engages the metal to restrict its flow. The last fraction of a millimeter of punch travel causes stretch in the metal under the punch.

Beads in the concave surfaces of a blankholder are usually 3 mm (0.12 in.) deeper than beads on the top or straight surfaces. This eliminates locking on the top surfaces during preforming of the blank to the shape of the concave blankholder surface.

Sometimes, draw beads need not be used for the full depth of draw, or need to be used only in certain locations, such as the corners of regular or irregular polygon-shaped parts. In such cases, some of the material may be allowed to slip through the draw bead to be retained at the end of the stroke only by blankholder pressure. More information on the design and construction of draw beads is available in the article "Deep Drawing" in this Volume.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Forming of Ribs, Beads, and Bosses

Unsupported sheet metal surfaces that might buckle or oil can are often stiffened by the addition of long, thin bosses called beads or ribs. Round or nearly round bosses are sometimes called buttons. Dimples are occasionally used as a recess for a rivet or screw head. The forming of ribs, beads, and bosses is a combination of bending, stretching, and drawing and involves high shear forces.

Design of Bosses. Bosses are usually about one stock thickness in depth. The radius of curvature on the inside of bends is about one stock thickness. A typical bend appears to be about four stock thicknesses wide on the convex side and about three stock thicknesses wide on the concave side. Some recesses for screw heads have sloping sides with very little curvature.

Bosses are typically produced at the same time that other forming operations are done and in the same dies, although they can be formed at separate stations in a progressive die. Die clearances can be critical in the forming of decorative bosses and embossed lettering, especially when definition must be sharp and detail must be accurate. On the other hand, reinforcing beads usually do not require great accuracy and can be produced in dies with the clearances typical of ordinary forming.

Use of Bosses. Bosses are often used to provide flatness, stiffness, and reinforcement to formed parts. They can also serve as locators for subsequent operations. In Example 5 in this article, an oval boss was used as a hole-locating surface and as the hole flange. The most frequent use of embossed beads is to lighten the weight of products by making it possible to use thinner metal than would be feasible without bosses, as described in the following example.

Example 7: Use of a Bead for Stiffening a Corner Bracket.

The corner bracket shown in Fig. 8 had a bead on the flat surface to produce stiffness. The work material was 1.09 mm (0.043 in.) thick cold-rolled 1008 or 1010 steel. The bead made the use of heavier stock unnecessary. A short flange around the periphery of the bracket also added to the stiffness.

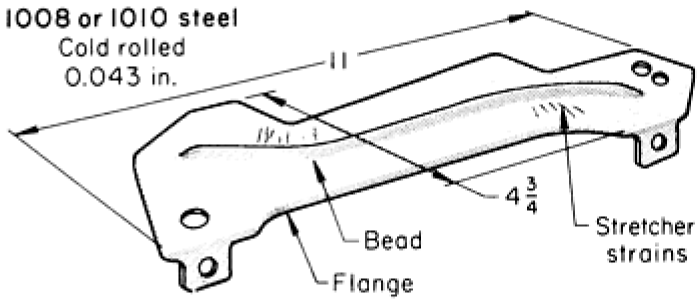

Fig. 8 Corner bracket that was stiffened by beading and flanging. Dimensions given in inches.

The bracket was produced in a progressive die in the following operations: trim to shape, pierce, form bead, form flange, and pinch trim from the carrier strip. The die was run in a 4.45 MN (500 tonf) press operating at 600 strokes per hour. Forming of the bead caused some distortion of the pilot holes and resulted in stretcher strains that extended from the inside radius of the bead to the edge.

The progressive die was made of D2 tool steel hardened to 62 to 64 HRC. One hour per week was needed to clean the die and to make minor repairs. Die lubricant was chlorinated oil.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Accuracy

Tolerances that can be maintained in the press forming of steel depend on press condition, accuracy of the tools, workpiece shape, and hardness and variation in the thickness of the work metal.

Flange Width. In forming 90° flanges, if the break lines in the plan view and side elevation are straight or nearly straight, there should be no difficulty in holding a tolerance of ±0.76 mm (±0.030 in.) on flange width. When flange break lines are curved significantly, tolerance usually must be increased to ±1.52 mm (±0.060 in.).

The overall length and width of large formed parts such as automobile hoods and deck lids can usually be held to a tolerance of ±0.76 mm (±0.030 in.). For small formed parts, closer tolerances can be met in production.

Bend Angle. Tolerances that can be held on bend angles depend greatly on the thickness and hardness of the work metal and on the flange height, because these factors affect springback. A 90° bend in low-carbon steel will spring back about 3°; therefore, either the dies are built to overform by this amount or the bend region is compressed to set the corner. To provide enough metal for gripping in the die, the minimum inside flange height should be $2\frac{1}{2}$ times the stock thickness, plus the bend radius.

Distance Between Holes. Distances between the centerlines of pierced holes that lie in a common surface of a formed part are commonly held within a total tolerance of 0.25 mm (0.010 in.) or less (see the article "Piercing of Low-Carbon Steel" in this Volume). The relation of hole positions in parallel or in-line surfaces of formed parts can be held to ±0.38 mm (±0.015 in.); holes with a smaller tolerance on the centerline distance should be pierced after forming (Fig. 9).

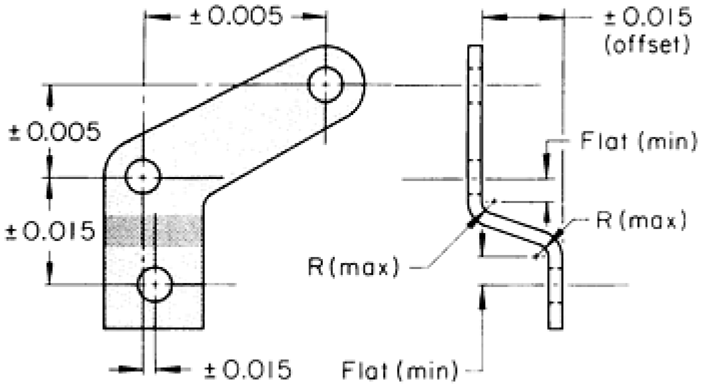

Fig. 9 Tolerances on hole position and distance between offset parallel surfaces on formed parts, and recommended dimensions to be specified for formed offsets. The tolerances shown apply to holes pierced before forming; holes in offset parallel surfaces with position tolerances less than ±0.38 mm (±0.015 in.) should be pierced after forming. Dimensions given in inches.

Distance Between Offset Surfaces. In ordinary commercial practice, the distance between two offset flat surfaces in parallel planes can be maintained within ±0.38 mm (±0.015 in.). For closer tolerances, extreme accuracy must be built into the tools, and the stock thickness must be held closer than normal mill tolerances. The following example describes the techniques used to maintain a tolerance of ±0.25 mm (±0.010 in.) on the distance between surfaces.

Example 8: Variation of a Critical Distance Between Offset Parallel Planes.

On the brake-assembly backing plate shown in Fig. 10, the distance between the tops of the six pads (A to F, Fig. 10) and the bottom of the center mounting face had to be within ±0.25 mm (±0.010 in.). The ranges of dimensions for all six pads as measured on 22 pieces are plotted in the graph in Fig. 10. Of the 132 individual measurements, only 7 exceeded the limits. The spread in dimensions was the result of a combination of variables, including stock thickness (±0.18 mm, or ±0.007 in.), springback after forming and coining, and condition of the press.

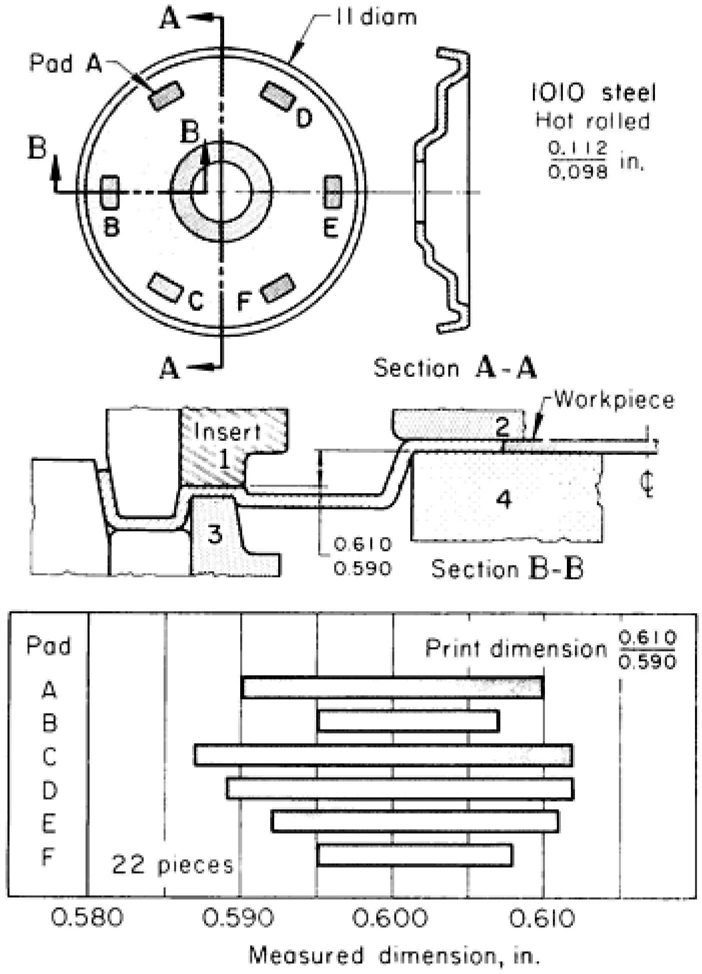

Fig. 10 Variation of offset distance in a formed and coined backing plate for a broke assembly. Dimensions given in inches.

Springback was unpredictable and varied among different lots of steel. Therefore, the die was made extra rigid, and the inserts for coining the pads (inserts 1 and 3, section B-B, Fig. 10) were made adjustable to compensate for variations in stock thickness. Coining reduced stock thickness at the 1600 mm^2 (2.5 in.^{2}) pads to 2 to 2.3 mm (0.080 to 0.090 in.)--a 15% reduction.

The backing plate was made in two operations, both in a 7.1 MN (800-tonf) mechanical knuckle press with a 127 mm (5 in.) stroke, which was operated at 20 strokes per minute. In the first operation, coil stock was fed into a progressive die, where the inner portions of the part were formed, the six pads were partly completed, the center hole was pierced, and the outside diameter was blanked. The outside flange was formed, and the six pads were coined to height and parallelism in a second die (section B-B, Fig. 10).

The press slide was parallel to the bolster within 0.25 mm/m (0.003 in./ft). The slide and the press bed were large enough to keep the die set centered, thus minimizing the possibility of error. The die set had four heavy guideposts in long bushings. The main parts of the punch and die were made of 6145 steel hardened to 45 to 50 HRC and ground after hardening, and the inserts for coining (inserts 1 through 4, section B-B, Fig. 10) were made of O1 tool steel hardened to 60 to 61 HRC and ground.

Holes Close to a Bend. Whether a hole that is close to a bend is pierced before or after forming depends on the function of the hole, its closeness to the bend, the bend radius, and the stock thickness. When the distance from the edge of the hole to the inside surface of the other leg of the bend is less than $1\frac{1}{2}$ times the stock thickness, plus the bend radius, the outer portion of the hole is likely to deform as a result of stretching of the metal. If the deformation is acceptable, the hole can be pierced before forming; otherwise, it must be pierced after forming. When the offset angle on formed parts is unimportant, stating the minimum flat and maximum radius dimensions as in Fig. 9 allows the maximum practical tolerance in producing acceptable parts.

Trimmed edges can be held to close tolerances when they are trimmed by one punch and when trimming is done after any forming that would cause distortion, as in the following example.

Example 9: Forming and Accurate Trimming in a Progressive Die.

Figure 11 shows an eggbeater side frame that was produced in a four-station progressive die from 1 mm (0.040 in.) thick cold-rolled 1008 to 1010 steel strip 19 mm ($\frac{3}{4}$ in.) wide. For accurate fit in assembly, the end of the part was trimmed to the close tolerances indicated in Fig. 11 after the U-shaped bead flanked by the vertical tabs had been formed. The bead was formed by drawing metal from the surrounding area and by stretching.

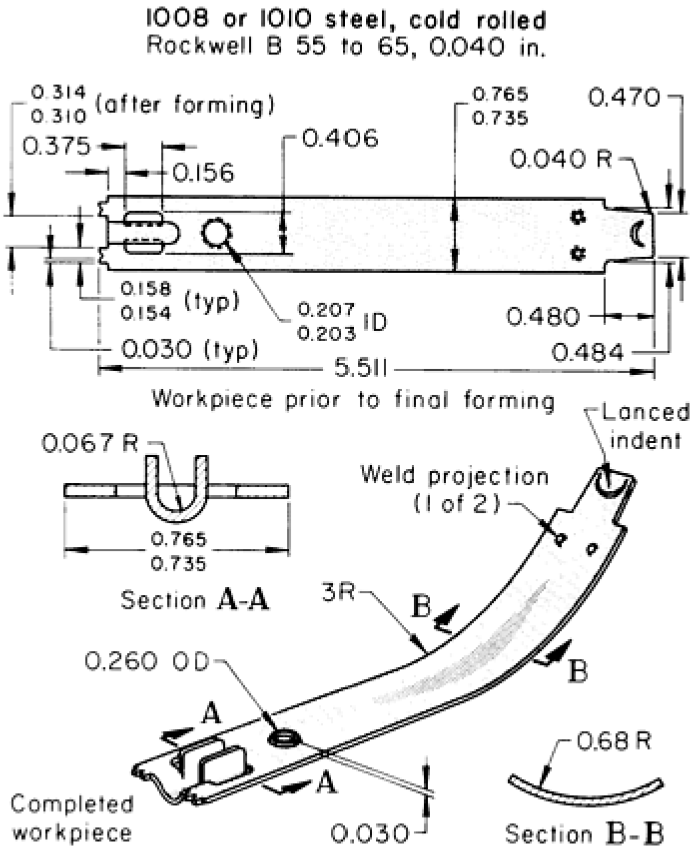

Fig. 11 Eggbeater side frame that was formed and accurately trimmed in a progressive die. Dimensions given in inches.

The stock had a No. 6 edge, a No. 3 finish, and a hardness of 55 to 65 HRB. The No. 6 edge (a square edge produced by edge rolling the natural edge of hot-rolled strip or slit-edge strip) was relatively burr free. Because the edges of the strip were also the edges of the completed part, the use of a No. 6 edge eliminated a deburring operation. The No. 3 finish was particularly suited to bright nickel plating without prior polishing or buffing. Stock was purchased to average in the lower part of the standard thickness range of ±0.05 mm (±0.002 in.) and within the standard width tolerance of ±0.38 mm (±0.015 in.). The sequence of operations in the four die stations was as follows:

- Lance and form semicircular indent, and notch-trim
- Pierce rectangular slot at one end, pierce round hole, and emboss weld projections
- Lance tabs, form bead and form flange around round hole

- Cut off (trim) and form contour

The die, made of D2 tool steel, was used in a 665 kN (75 tonf) press; die life between regrinds was 65,000 pieces. Mineral oil was used as the lubricant. The production rate was 360 pieces per hour; annual production quantity was 600,000 pieces.

Close Tolerances. Closer-than-conventional accuracy can be attained in sheet metal parts with accurate dies, precise location of the parts in the dies, and handling equipment designed to avoid damage to semifinished or finished workpieces. Press condition is also an important factor. The following example describes the techniques used for close-tolerance forming in progressive dies.

Example 10: Use of Progressive Die to Meet Close Tolerances on a Lamp Bracket.

The lamp bracket shown in Fig. 12 had several tolerances that were closer than normal. Three holes had to be pierced to a tolerance of +0.13, -0 mm (+0.005, -0 in.) on diameter, and two of the holes had to be in line within 0.25 mm (0.010 in.) after forming. In addition, when the 104° bend was made with the preformed flanges out, the radius of bend on the flanges had to be held within 0.38 mm (0.015 in.) total indicator reading, and the 10° angle of the flanges had to be held within ±5°. The bracket was produced in a seven-station progressive die with two piloting stations that could have been used for auxiliary operations such as shaving or restriking, if necessary. The operations were as follows:

- Notch for stop, pierce one 10 mm (0.395 in.) hole, pierce two 6.7 mm (0.265 in.) holes
- Blank the contour of the ears
- Pilot
- Blank the partial contour
- Form the two long flanges
- Pilot
- Form the 104° angle and cut off

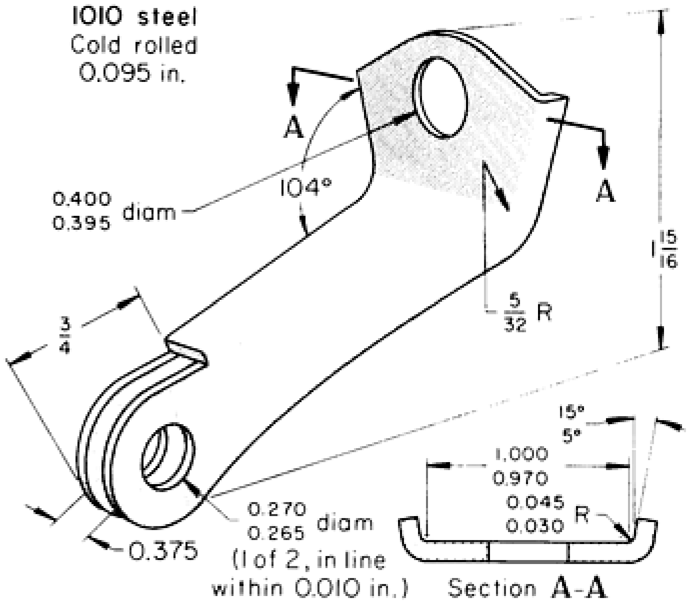

Fig. 12 Lamp bracket that was press formed to close tolerances in a progressive die. Dimensions given in inches.

The material was cold-rolled 1010 steel strip in No. 4 temper. The strip was 2.4 mm (0.095 in.) thick by 86 mm ($3\frac{3}{8}$ in.) wide. The progressive die was mounted in a 665 kN (75 tonf) straight-side mechanical press with a flywheel drive, a 152 mm (6 in.) stroke, and a maximum rate of 60 strokes per minute. The press was equipped with an air clutch. Ball-lock punches, die bushings, and easily reproducible die sections were used throughout.

The die was made of D2 tool steel hardened to 58 to 60 HRC and had a life of 55,000 pieces per regrind. Production was discontinued after 2.5 million lamp brackets had been made.

Flatness. The longitudinal and transverse stresses set up during the forming of a part can cause it to warp--particularly if the part is made of steel that is not uniform. Depending on the size and shape of the part, either smooth flattening or embossing can be used to maintain flatness.

Press Condition. The accuracy and condition of presses must be maintained within close limits when tolerances on formed parts are critical, regardless of the type of operation. Press slides that are not parallel with the bed at the bottom of the stroke can cause uneven stock thicknesses when the punch bottoms against the die surface. Unbalanced forming forces can shift the punch, producing out-of-tolerance workpieces. In some forming applications, shifting of the punch can be minimized by tipping the workpiece to balance the forces. Heel blocks and other means of positively maintaining the punch-to-die relationship are used to overcome unbalanced forming forces and shifting of the punch slide.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Hot Forming

In hot forming, the work metal is heated above its transformation temperature. Less press capacity is needed to hot form a given shape than to cold form it. However, the press, dies, and related equipment must be designed to withstand high temperature. Meeting this requirement is sometimes more difficult than obtaining a higher-capacity press for cold forming.

In some applications, a steel workpiece can be quenched directly from the forming temperature. In the following example, 25 mm (1 in.) thick high-strength low-alloy steel blanks heated to 815 °C (1500 °F) were press formed and then allowed to cool in air.

Example 11: Hot Forming of High-Strength Low-Alloy Steel.

A 25 mm (1 in.) thick blank of high-strength low-alloy steel was hot formed into a bucket blade for earthmoving equipment (Fig. 13). The blank was cut by oxy-fuel gas to 305 × 1700 mm (12 × 67 in.) from a flat plate, then furnace heated to 815 °C (1500 °F). The hot workpiece was formed with one stroke of a 1.8 MN (200 tonf) hydraulic press. No lubricant was used, because production quantities were small and the surface condition of the part was not critical. After being formed, the part was quickly removed from the die and allowed to cool on the floor. Time from furnace to press to floor was 4 min per piece.

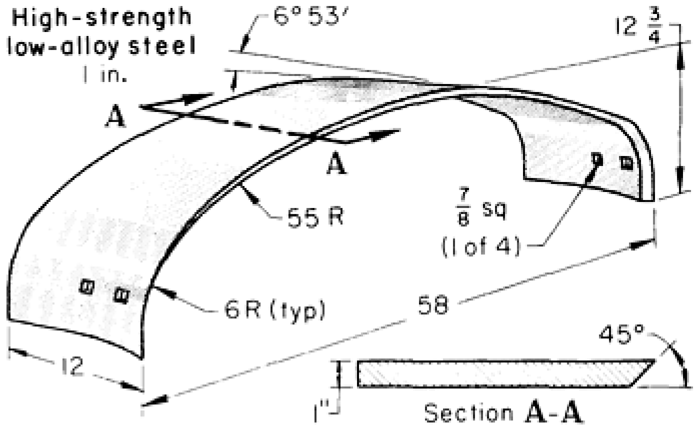

Fig. 13 Digger-bucket blade that was hot formed in a hydraulic press. Dimensions given in inches.

Heating the blank to 815 °C (1500 °F) permitted forming in a much smaller press and with greater accuracy than could have been done if the blank had been formed at room temperature. Tolerances of ±1.6 mm (± $\frac{1}{16}$ in.) were maintained on the overall dimensions of the formed part.

The forming die was a weldment made of hot-rolled 1045 steel, flame hardened at the critical wear points. Production was 400 buckets per year in lots of 60. Die life was 2400 pieces before reworking.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Auxiliary Operations in Presses

Presses are used to fasten parts together by rivets or by plastically deforming mating areas in either or both of the parts. In operations such as staking, folding, crimping, curling, or press assembly, projections of various sizes and shapes are deformed so that the assembly is reasonably strong. In the following example, a press was used to curl the lip of a hub over a disk for a tight torque-resistant joint.

Example 12: Assembly of Hub to Center Disk by Curling in a Hydraulic Press.

Center disks of large blower wheels were assembled to hubs by curling, as shown in Fig. 14. The hub was an automatic bar machine product made of cold-drawn 1018 steel tubing. The length of the hub was 28.6 mm (1 $\frac{1}{8}$ in.) before curling, and the curled lip was 3.2 mm ($\frac{1}{8}$ in.) high.

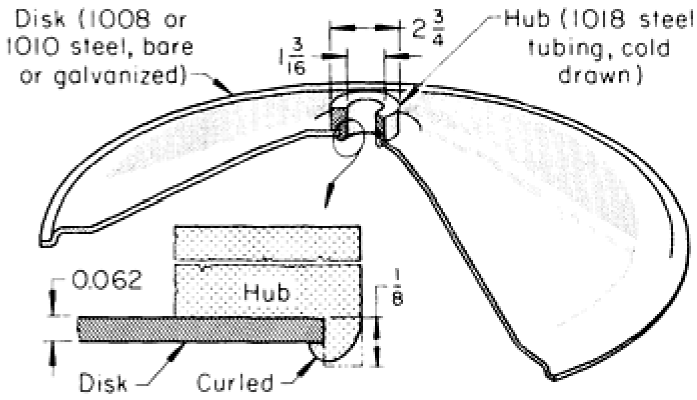

Fig. 14 Hub and disk that were assembled by curling in a press. Dimensions given in inches.

The work was done in a 490 kN (55 tonf) hydraulic press with no lubricant. Because the disks and hubs were assembled and positioned manually, the production rate was only 30 pieces per hour. The joint could have been made much faster by spinning, but spinning equipment that could accommodate the large disks was not readily available.

The curling tool was made of A5 tool steel and had a life of several years. The blower-disk assemblies were made in quantities of 20,000 per year.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Press Forming Versus Alternative Methods

If a part can be made by forming sheet metal in a press, this is ordinarily the least expensive method of manufacture, except when only a few pieces are needed. The low cost per piece results from the high production rates and the efficient use of metal usually obtainable in the press forming of sheet metal.

The accuracy inherent in press forming is satisfactory for most requirements. Greater accuracy can be achieved by the use of precision tooling and by maintaining close control over press conditions.

The disadvantages of relatively high tool cost and long tooling time can often be offset, except for the production of very small quantities, by the use of short-run tooling. The design of a part can sometimes be modified to permit the use of press forming instead of alternative methods. An application in which cost was reduced when forming replaced forging is described in the following example.

Example 13: Press Forming Versus Forging.

The bicycle handlebar stem shown in Fig. 15 was formed from hot-rolled commercial-quality 1008 or 1010 steel sheet 4.17 mm (0.164 in.) thick, pickled and oiled. The formed part cost much less than the machined forging it replaced. More than 1 year was required to perfect the forming process.

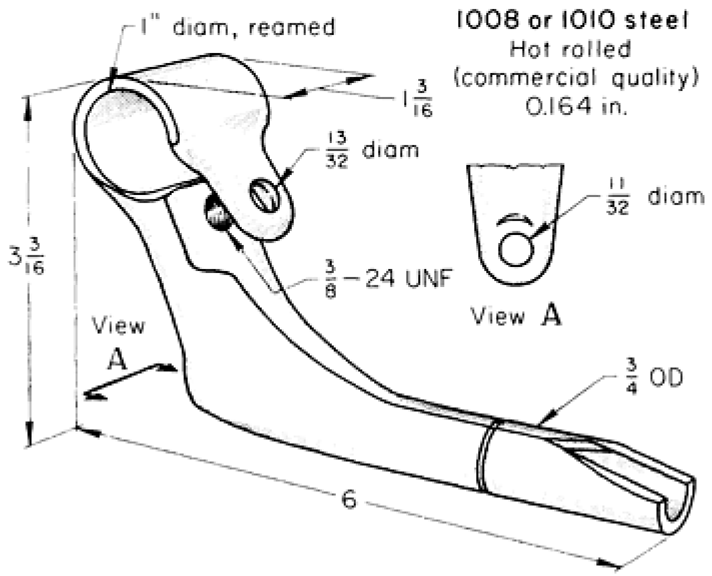

Fig. 15 Bicycle handlebar stem that cost less when formed from sheet steel than when produced as a machined forging. Dimensions given in inches.

The pieces were blanked two at a time in a 2.2 MN (250 tonf) press operating at 40 strokes per minute. They were formed in a nine-station transfer die that ran at 20 to 26 strokes per minute in a 2.67 MN (300 tonf) press loaded at 50% of its force-capacity rating, as shown by a meter. The blanks were fed into the first station from a magazine feeder by a slide. After forming, one hole was reamed and another was drilled and tapped in a rotary indexing machine.

The severity of forming require the use of a drawing compound. The production rate of 1 million pieces per year caused no unusual problems except those that resulted from aging of the stock. If the stock was not used shortly after receipt from the mill, there was some splitting at the most severely formed areas, such as the shoulders of the handlebar stem, and some surfaces showed an orange-peel effect. After forming, the part was plated for appearance and corrosion resistance.

The blanking die was of D2 tool steel hardened to 56 to 58 HRC. After runs of 50,000 parts, the die was resharpened by removing 0.3 mm (0.012 in.) from the punch and die. The forming punches were of O1 and A2 tool steels, hardened to 52 to 54 HRC. The forming-die sections were of D2 tool steel hardened to 54 to 56 HRC. The high-wear areas were coated with a deposit of cemented carbide. The dies made over 300,000 acceptable parts.

Workpieces that are very similar can sometimes be made equally well, and at about the same cost, by two or more different methods. Under these conditions, the choice of method may depend on the availability of equipment, as in the following example.

Example 14: Press Forming in V-Dies Versus Rotary Draw Bending.

The two types of rectangular frames shown in Fig. 16 were made by two different forming methods. Each type of frame could have been made by either method; but because of the type of equipment that was available, the corners of the frame shown in Fig. 16(a) were press formed in a V-die, and those shown in Fig. 16(b) were made by rotary draw bending. Each type of frame was made in a number of sizes by making individual bends in succession on precut lengths of contour-rolled or press-brake-formed steel strip and welding the ends together.

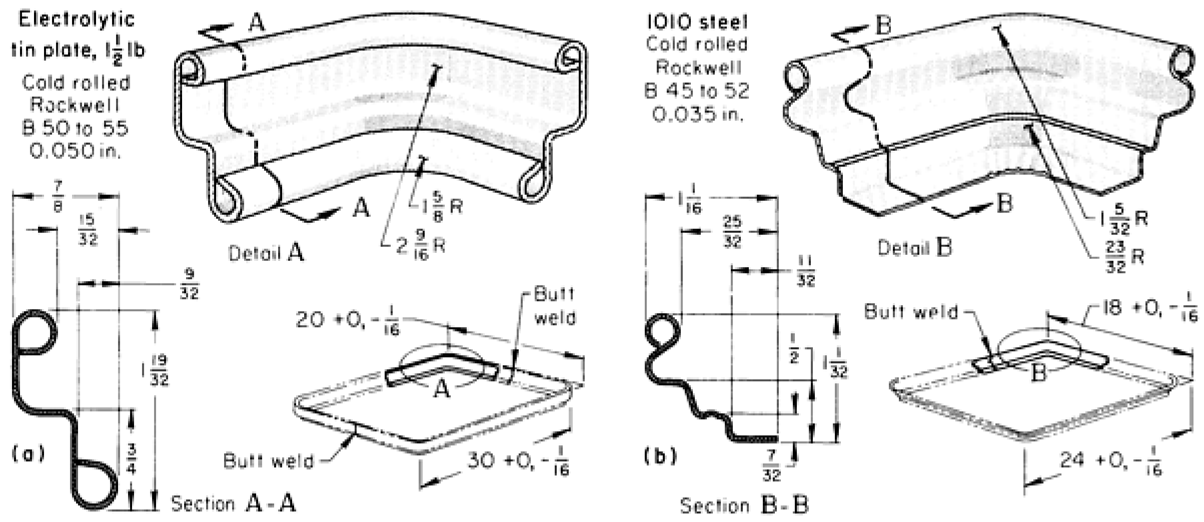

Fig. 16 Rectangular frames that were made by two different forming methods. (a) Press forming in a V-die. (b) Bending in a rotary draw bender. Dimensions given in inches.

The corners for frames made from 1.27 mm (0.050 in.) thick electrolytic tin plate (Fig. 16a) were formed one at a time in a 152 mm (6 in.) stroke, 500 kN (56 tonf) mechanical press, using a developed V-die made of D2 tool steel. Each corner was restruck twice for accuracy of bend angle and contour. Because of the tin coating, no lubricant was needed. The production rate was 420 corners per hour. Die life was indefinite. Lot size averaged about 1000 frames, for a yearly production of about 25,000 frames of various sizes.

The corners for a second line of frames (Fig. 16b) were bent from contour-roll-formed 54.8 × 0.89 mm ($2\frac{5}{32}$ × 0.035 in.) 1010 steel strip in a rotary draw bending machine equipped with conforming die and clamp blocks. The die and clamp blocks were made of D2 tool steel; residual mineral oil from the preceding contour roll forming operation provided sufficient lubrication for the draw bending. The production rate was 600 bends per hour for a total yearly production of about 1 million frames in lots of 1000 to 10,000 frames. These frames were phosphate coated and electrostatically spray painted after welding.

Press Forming of Low-Carbon Steel

Revised by John Siekirk, General Motors Technical Center

Safety

Press forming, like other press operations, involves potential hazards to operators, maintenance personnel, and other persons in the work area. No press, die, or auxiliary equipment can be considered ready for operation until hazards are eliminated by the installation of necessary safety devices. Operators and all persons working around the operation should be properly instructed in safe operation of the equipment. The article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume contains additional information on safe operation.

Press Forming of High-Carbon Steel

Introduction

HIGH-CARBON STEEL strip (including spring steel and tool steel) is blanked, pierced, and formed to make a variety of parts. The practices, precautions, presses, and tools used in making high-carbon steel parts are comparable to those used for producing similar parts of low-carbon steel (see the preceding articles concerning the blanking, piercing, press bending, and press forming of low-carbon steel in this Volume). Differences that must be considered in blanking, piercing, and forming high-carbon rather than low-carbon steel include the following:

- More force is required for high-carbon steel because of its higher strength
- Greater clearance between the punch and die is necessary in blanking and piercing
- A more wear-resistant tool material may be required before acceptable tool life can be obtained

Blanking and Piercing

The most important difference between the blanking and piercing of high-carbon and of low-carbon steel is that greater clearance between punch and die is required for high-carbon steels. The clearances needed for producing each of the five edge types in blanking and piercing high-carbon and low-carbon steels are compared in Table 1 in the article "Piercing of Low-Carbon Steel" in this Volume. As shown in that table and in the accompanying Fig. 2, at equal clearance, rollover depth will be smaller and burnish depth greater for high-carbon than for low-carbon steel. For example, 12% clearance per side will produce a type 4 edge on high-carbon steel, and a type 2 edge on low-carbon steel.

This relationship of cut-edge characteristics to carbon content is different from the relationship of cut-edge characteristics to temper or hardness of low-carbon steel. Figure 4 in the article "Piercing of Low-Carbon Steel" shows that rollover depth and burnish depth are both smaller for harder tempers of low-carbon steel than for softer tempers.

Tool materials recommended for blanking and piercing high-carbon steel are listed in the article "Selection of Material for Blanking and Piercing Dies" in this Volume. Selection tables in that article show that although the complexity of the workpiece and the total quantity to be blanked are more significant factors than work metal composition in the selection of tool material, the hardness of the work metal is nevertheless significant. Carbide dies are often used for large quantities (see Example 1).

Die life in the blanking and piercing of high-carbon steel varies with different applications, depending greatly on the dimensional accuracy that must be maintained and the burr height that can be tolerated on the blanked parts. The following example describes an application in which a change in tool material increased die life by a factor of about 28.

Example 1: Change From Tool Steel to Carbide Punches and Dies.

The part shown in Fig. 1 was blanked and pierced from 48 mm ($1\frac{7}{8}$ in.) wide coils of bright-finish 1045 steel having a hardness of 70 to 75 HR15N (equivalent to 20 to 30 HRC). Thickness ranged from 0.48 to 0.986 mm (0.019 to 0.0388 in.); tolerance for all thicknesses was +0.000, -0.003 mm (+0.000, -0.001 in.).

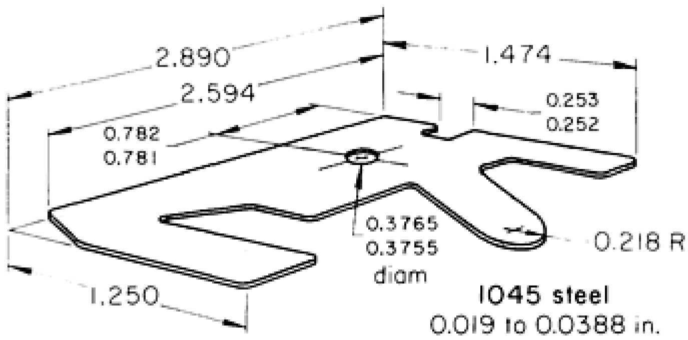

Fig. 1 Blanked and pierced textile machine part for which carbide compound dies had 28 times the total life of those made of D2 tool steel. Dimensions given in inches.

The punch and the die were both originally made of D2 tool steel at 62 to 63 HRC. To keep burr height on the parts at or below the maximum of 0.08 mm (0.003 in.), it was necessary to grind the die after each 25,000 pieces. The steel die had a usable depth of 16 mm ($\frac{5}{8}$ in.) and 0.20 mm (0.008 in.) was removed with each grinding. Therefore, with 78 grindings, the total die life was 1,950,000 pieces.

To improve die life, the tool material for both the punch and the die was changed to carbide. The carbide tools cost three times as much as the steel punch and die, but production between grinds increased to 350,000 pieces and only 0.10 mm (0.004 in.) of stock was removed per grind. Therefore, with the same amount of usable die, the total die life would be 54,600,000 pieces.

The die was a compound-type unit. It was operated in a 445 kN (50 tonf) open-back inclinable mechanical press having a 75 mm (3 in.) stroke and mechanical feed.

Tooling for Greatest Efficiency. Along with the increase in punch-to-die clearance, other tool changes are required for efficiency in the blanking and piercing of high-carbon steel. For example, with high-carbon steel more so than with low-carbon steel, the scrap skeleton from blanking and piercing operations is likely, in springing back, to adhere to the punch, sometimes causing spalling of the punch edges. To strip this scrap from the punch, it is common practice to include in the punch mechanism a second, chisel-pointed punch placed as close as possible to the blanking edge. This second punch serves to spread the scrap skeleton, minimizing its adherence to the blanking punch (see Example 2).

The following example deals with changes in tooling that reduced cost. In the example, blanking replaced machining.

Example 2: Cost Reduction When Blanking Replaced Milling.

The blanked workpiece shown in Fig. 2 replaced a machined part of slightly different dimensions. For the machined part, starting blanks 75 mm (3 in.) long by 17 mm ($\frac{11}{16}$ in.) wide by 4.0 mm ($\frac{5}{32}$ in.) thick were sawed from ground flat stock of A2 tool steel spheroidize-annealed to 14 to 18 HRC. The long edges of the blanks were ground to reduce the width of the blank from 17 to 15.9 mm ($\frac{11}{16}$ to 0.625 in.). Grinding was followed by four separate milling operations. This change in production method from milling to blanking reduced the machining time for 100 pieces from 28 to 0.23 h and decreased cost per piece by a factor of 122.

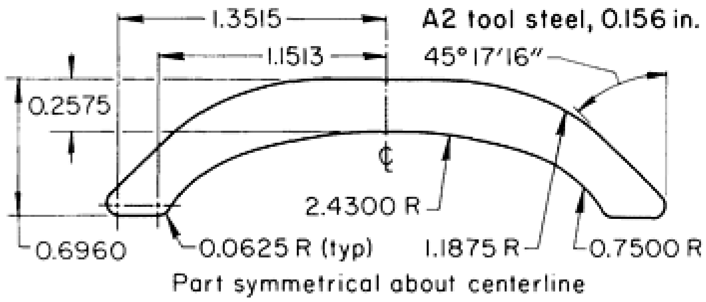

Fig. 2 Part produced by blanking for less than 1% of the per-piece cost of producing it by milling. The punch was modified to solve a stripping problem. Dimensions given in inches.

The blanking was done in a 280 kN (32 tonf) open-back inclinable mechanical press with a punch and die made from A2 tool steel at 60 to 62 HRC. To obtain acceptable edges on the workpiece, a punch-to-die clearance of 8 to 10% of stock thickness per side was used. Because of the force required for stripping, it was necessary to add a chisel-edge punch to spread the scrap skeleton and to prevent damage to the blanking punch. In addition, a minimum corner radius of 1.6 mm (0.062 in.) was necessary for efficient stripping, and the overall width of the part was increased to 17.7 mm (0.696 in.).

Dimensional accuracy in the blanking and piercing of high-carbon steel depends largely on the accuracy of the tooling. Initially, the practical accuracy is the same as that for the blanking and piercing of other metals. However, because the rate of tool wear is usually higher in the blanking and piercing of high-carbon steel (especially if pretempered) than for many other work metals, maintenance of tolerances can be more difficult and may require more frequent reconditioning of the tools.

The following example describes a spring for which it was impossible to attain the specified tolerances on one dimension without subsequently grinding. Variations in two dimensions, measured on 124 pieces during the production run, are shown in the bar graphs in Fig. 3.

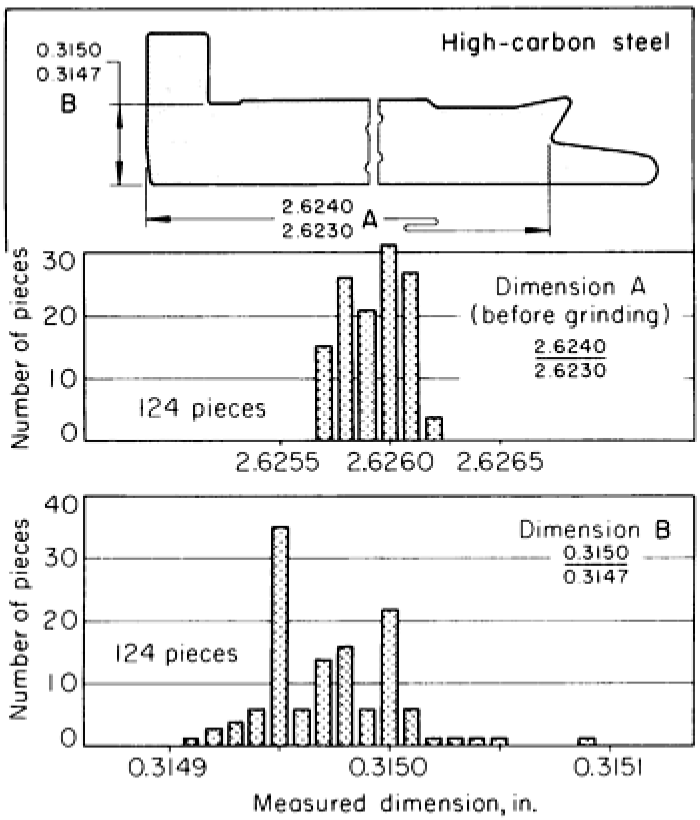

Fig. 3 Variations in two dimensions of blanked springs. Dimensions given in inches.

Example 3: Variations in Dimensions of Blanked Springs.

The data in Fig. 3 show variations in two dimensions measured on 124 blanked springs randomly selected from a production run of 50,000 pieces. The total tolerance on the length (dimension A) was 0.03 mm (0.001 in.), which was not practical for blanking. Therefore, the springs were blanked slightly oversize, and about 0.05 mm (0.002 in.) was removed by grinding the square end. The total tolerance on dimension B was 0.008 mm (0.0003 in.). As shown in Fig. 3, all but 11 of the springs conformed to this tolerance, and these 11 samples were all on the high side of 8.001 mm (0.3150 in.).

The springs were blanked from 25 mm (1 in.) wide, bright-finish, pretempered high-carbon steel (1.15 to 1.35% C, 84 to 87.5 HR15N). Thicknesses ranged, in steps of 0.013 mm (0.0005 in.) from 0.102 to 0.165 mm, ±0.005 mm (0.004 to 0.0065 in., ±0.0002 in.). The die was made of T1 high-speed steel at 64 to 65 HRC and operated in a 270 kN (30 tonf) open-back inclinable mechanical press having a 38 mm ($1\frac{1}{2}$ in.) stroke and an automatic feed.

The tolerances that can be held and the size and spacing of holes and slots that are practical in press dies are illustrated in the following example.

Example 4: Blanking and Piercing an Intricate Pattern.

The comb of an electric shaver (Fig. 4), made of pretempered spring steel, had an intricate pattern of slots and holes. Because more than 7 million of these combs were produced yearly, it was economical to construct the die needed to make them by blanking and piercing. Most of the dimensions on the part were held to a tolerance of ±0.025 mm (±0.001 in.). Similar parts of stainless steel were made by chemical machining.

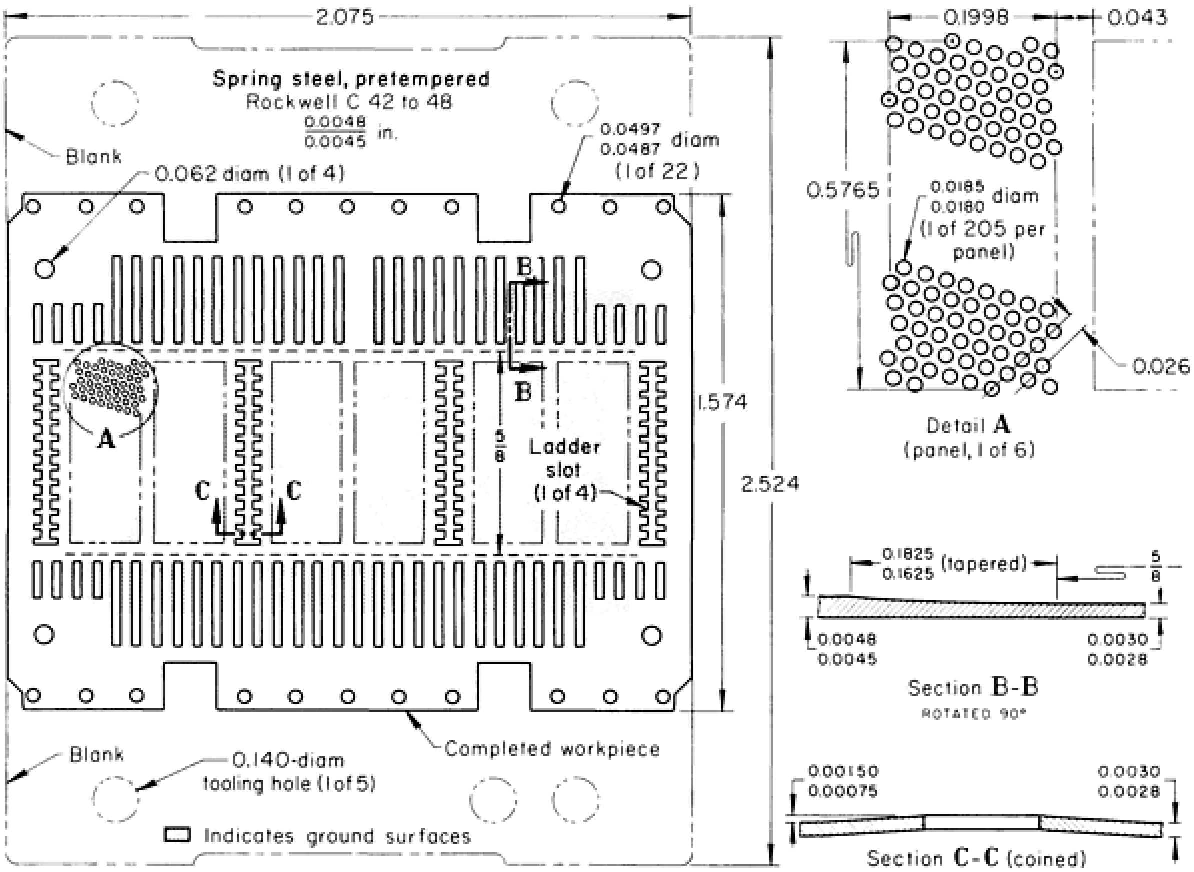

Fig. 4 Comb of an electric shaver made by blanking and piercing. Dimensions given in inches.

The stock was 56 mm ($2\frac{3}{16}$ in.) wide high-carbon spring steel, pretempered to 42 to 48 HRC. Thickness was 0.11 to 0.12 mm (0.0045 to 0.0048 in.). Blanks sheared from the stock were 52.70 mm (2.075 in.) wide by 64.11 mm (2.524 in.) long and included, at each end, 12.1 mm (0.475 in.) that was later trimmed. This trimmed stock contained five tooling holes, one of which was a foolproof hole that prevented incorrect placement of the blank in the die.

In the first piercing operation, the rectangular slots at each end of the cutting areas were pierced, along with the five 3.56 mm (0.140 in.) diam tooling holes in the trim area and four ladder slots that bordered the three cutting areas. After this operation, the three cutting areas were ground to a thickness of 0.071 to 0.076 mm (0.0028 to 0.0030 in.). This ground surface, 15.9 × 12.6 mm (0.625 × 0.495 in.), tapered to the original stock surface in 4.11 to 4.62 mm (0.162 to 0.182 in.).

In the third operation, 0.46 mm (0.018 in.) diam holes were pierced in the cutting areas at a centerline spacing of 0.572 mm (0.0225 in.) in one direction and 0.66 mm (0.026 in.) in the other direction. Each of the three cutting areas had 410 holes arranged in two panels of 205 holes each.

After the third operation, workpieces were deburred, and the area around each of the four ladder slots was coined to a depth of 0.019 to 0.038 mm (0.00075 to 0.0015 in.). The surface was buffed before plating, removing 0.008 mm (0.0003 in.) of stock. Finally, the comb was trimmed from the rough blank and four 1.6 mm (0.062 in.) diam holes for locating studs and 22 rivet holes 1.24 to 1.26 mm (0.0487 to 0.0497 in.) in diameter were pierced. The piece was plated after assembly with the comb support.

Dies were made of D2 tool steel and hardened to 58 to 60 HRC. Some additional wear resistance that could have been attained by hardening to 60 to 62 HRC was sacrificed in order to make the delicate punches more resistant to shock. The presses were 620 and 670 kN (70 and 75 tonf) mechanical presses operated at 5 or 6 strokes per minute to accommodate meticulous hand feeding of the workpieces. Production lots consisted of 50,000 pieces each.

Forming Pretempered Steel

The mold forming of high-carbon steel in the quenched-and-tempered (pretempered) condition (usually 47 to 55 HRC) is common practice. The severity of forming that can be done without cracking of the work metal depends mainly on thickness. When metal thickness is no more than about 0.38 mm (0.015 in.), it is possible to make relatively severe bends without fracturing the work metal. However, as metal thickness increases, the amount of forming that can be done on pretempered steel decreases rapidly.

The example that follows describes the production of parts by blanking, piercing, and forming in progressive dies. The hardness and abrasiveness of the work metal reduced die life well below that experienced with low-carbon steel or annealed high-carbon steel. Tabs were coined to a spherical radius after being bent to a 45° angle. This required resetting the die to overcome variations in each coil of stock.

Example 5: Lockwashers From 1074 Steel.

The lockwasher shown in Fig. 5 was made from 1074 pretempered steel with a hardness of 64 to 67 HR30N. Coil stock was 64 mm ($2\frac{1}{2}$ in.) wide and 0.38 mm (0.015 in.) thick. A progressive die with three working stations was used to cut the tabs to shape, bend, and coin the tabs to a spherical radius, and blank the part from the strip. Idle stations were placed between the working stations for added die strength. D2 tool steel was used for the cutting and forming die elements. Die life between sharpenings was about 25,000 pieces.

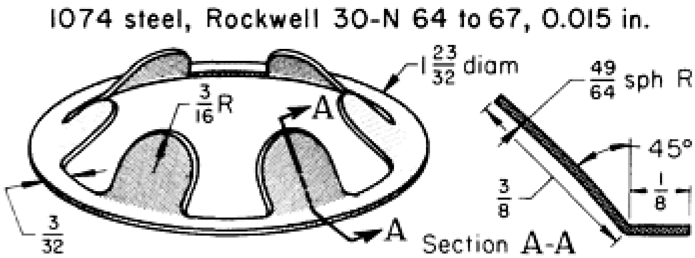

Fig. 5 Lockwasher that was blanked and formed of pretempered steel. To control tab dimensions, the dies had to be reset for each new coil of stock. Three progressive-die stations cut the tabs to shape and coined them to spherical radius. Dimensions given in inches.

Adjustment of the die setting was necessary for each coil of stock so that the tabs would be coined and bent to the proper angle. A 580 kN (65 tonf) mechanical press operating at 110 strokes per minute was used.

Forming Annealed Steel

Moderately severe forming can be done on cold-rolled stock that has not been quenched and tempered and on high-carbon steel that has been spheroidize-annealed. Such materials are usually hardened and tempered after forming to improve spring properties. Table 1 shows the effect of the carbon content of steel on bendability and demonstrates the major importance of sheet thickness.

Table 1 Typical effects of carbon content and sheet thickness on minimum bend radius of annealed steels

Thickness of sheet	Minimum bend radius		
	Steels 1020 to 1025	**Steels 4130 and 8630**	Steels 1070 and 1095

mm	in.	mm	in.	mm	in.	mm	in.
0.41	0.016	0.8	0.03	0.8	0.03	1.5	**0.06**
0.51	0.020	0.8	0.03	0.8	0.03	1.5	**0.06**
0.64	0.025	0.8	0.03	0.8	0.03	1.5	**0.06**
0.76	0.030	0.8	0.03	1.5	0.06	2.3	**0.09**
0.89	0.035	1.5	0.06	1.5	0.06	2.3	**0.09**
1.07	0.042	1.5	0.06	1.5	0.06	3.3	**0.13**
1.27	0.050	1.5	0.06	2.3	0.09	3.3	**0.13**
1.57	0.062	1.5	0.06	2.3	0.09	4.1	**0.16**
1.98	0.078	2.3	0.09	3.3	0.13	4.8	**0.19**
2.36	0.093	2.3	0.09	4.1	0.16	6.4	**0.25**
2.77	0.109	3.3	0.13	4.1	0.16	7.9	**0.31**
3.18	0.125	3.3	0.13	4.8	0.19	7.9	**0.31**
3.96	0.156	4.1	0.16	6.4	0.25	9.7	**0.38**
4.75	**0.187**	**4.8**	**0.19**	**7.9**	**0.31**	**12.7**	**0.50**

Source: *Die Design Handbook,* 2nd ed., McGraw-Hill, 1964

The following two examples describe the forming of parts from annealed stock or from cold-rolled stock that had not been hardened. The first example deals with parts that were redesigned to improve producibility in the press; the other example, with parts for which the forming severity required annealed or unhardened stock. (Annealed stock was also used in Example 8.)

Example 6: Bracket That Was Redesigned to Prevent Punch Breakage.

The bracket shown in Fig. 6 was made of 0.11 mm (0.042 in.) thick spheroidize-annealed 1070 spring steel at 70 to 82 HRB. It was blanked, pierced, and formed by a progressive die in a 530 kN (60 tonf) double-eccentric straight-side press at the rate of 210 pieces per minute.

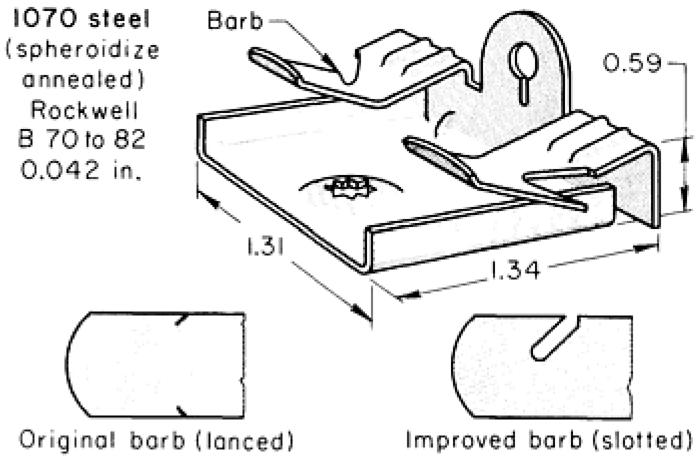

Fig. 6 Bracket produced by blanking, piercing, and forming in a progressive die. Improved design of the barb reduced die maintenance cost and press downtime and gave the barb more holding power in service. Dimensions given in inches.

The bracket was used to attach electric conduit and other building components to the flanges of beams and other structural members. In the original design of the bracket, the lanced barbs (Fig. 6) caused frequent breaking of a punch. The improved design, as shown in Fig. 6, produced a barb with better holding power and permitted the use of a stronger punch, reducing the cost of die maintenance and press downtime. After forming, the parts were heat treated to 44 to 48 HRC and phosphate coated.

Example 7: Use of a Progressive Die to Outline, Pierce, Form, and Blank.

The washer-wingnut shown in Fig. 7 replaced two parts--a washer and a wingnut. To produce the unit as one piece, spheroidize-annealed 1070 steel was used. The part was quenched and tempered after forming.

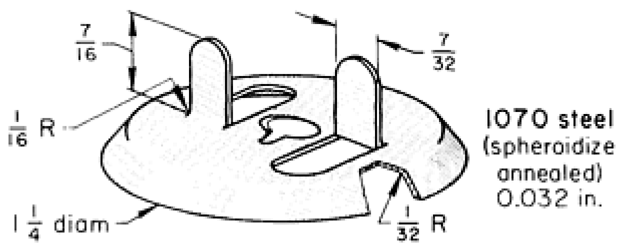

Fig. 7 Washer-wingnut that was made in a progressive die. The edges of the center-hole slot were offset to the pitch of the screw thread so that the part could serve as both a nut and a washer. Dimensions given in inches.

A progressive die was used to outline, pierce, form, and blank the part from coil stock. The metal around the center hole was slotted and spiral formed, as in making a speed nut, to engage threads. A 530 kN (60 tonf) double-eccentric straight-side press operating at 180 strokes per minute produced three wingnuts per stroke, or 540 per minute.

The progressive die was made of D2 tool steel and hardened to 58 to 60 HRC. Die life was 200,000 strokes between regrinds. An extreme-pressure lubricant was used on the strip. Annual production was 3.4 million pieces.

Hole Flanging

Flanges are formed around holes to increase bearing surface or to increase the number or threads that will fit in a tapped hole. The relationship among stock thickness, hole size, and flange height is discussed in the article "Press Bending of Low-Carbon Steel" in this Volume. In the following example, flanges were formed around holes that were large compared to the flange width in order to provide a bearing surface.

Example 8: Swivel Washer With Flanged Holes.

Annealed 1070 spring steel 0.25 mm (0.010 in.) thick was used to make the swivel washer shown in Fig. 8. After forming, the parts were heat treated to 46 to 48 HRC.

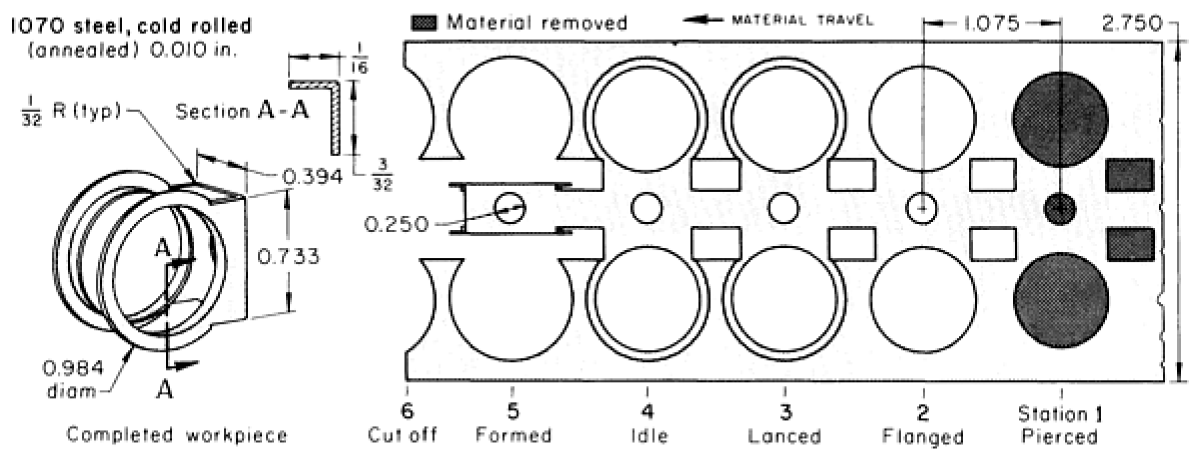

Fig. 8 Swivel washer, with flanged holes, that was made in a progressive die. Dimensions given in inches.

The strip layout for the six-station progressive die is also shown in Fig. 8. In the first station, two rectangular holes, one 6.35 mm (0.250 in.) diam pilot hole, and two 18.0 mm (0.710 in.) diam holes were pierced. The larger round holes were flanged to 20.3 mm (0.798 in.) in diameter by 1.6 mm ($\frac{1}{16}$ in.) in depth in the second station. The washer was lanced in the third station and formed in the fifth. The part was cut off in the sixth station. Station 4 was idle.

The die was made of A2 tool steel and was hardened to 60 to 61 HRC. It ran in a 130 kN (15 tonf) open-back inclinable press at 2000 strokes per hour. To maintain a minimum burr height, the die was sharpened after making 60,000 pieces. Total die life was more than 3 million pieces.

Multiple-Slide Forming

Small parts that are used in large quantities and require considerable forming are often produced on multiple-slide machines. In general, more severe forming can be done in the forming station of a multiple-slide machine than in a progressive die. Additional information on multiple-slide forming is available in the article "Forming of Steel Strip in Multiple-Slide Machines" in this Volume. The following example illustrates high-carbon steel parts produced in a multiple-slide forming machine.

Example 9: Blanking and Bending a Bracket in a Multiple-Slide Machine.

The mounting bracket shown in Fig. 9 was embossed, pierced, and notched in a progressive die in the press station of a multiple-slide machine. It was then cut off and bent. Production was at the rate of 100 pieces per minute. The work metal was coiled cold-rolled 1050 steel strip, 0.91 mm (0.036 in.) thick by 32 mm ($1\frac{1}{4}$ in.) wide.

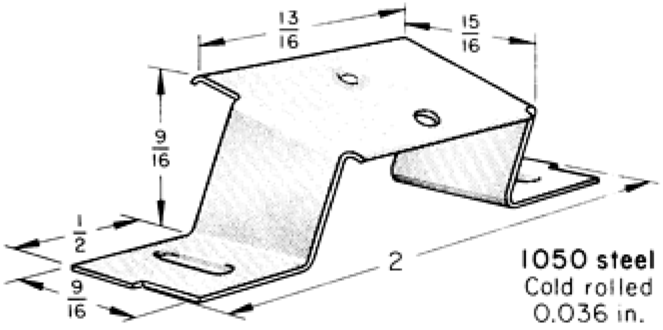

Fig. 9 Mounting bracket that was blanked and bent in a multiple-slide machine. Dimensions given in inches.

In the press station, two holes were pierced, two weld projections were embossed, one hole was flanged, the outline was notched, and the top surface was flanged to improve stiffness. The strip then moved to the forming station, where the part was cut off and bent to the final form shown in Fig. 9. All slides in this station moved in a horizontal plane. A front slide tool held the part against the center post and preformed the flanges. A cam action on the right and left slides formed the flanges against the post and the rear slide tool.

Vertical-Plane Machines. The machines that formed the parts described in the preceding example have slides that operate in the horizontal plane. There are also machines in which the center post is horizontal and the slides operate in a vertical plane normal to the center post.

Introduction

COATED STEEL sheet or strip is formed in the same presses as those used for forming uncoated steel. Forming procedures, however, must sometimes be modified, depending on the type of coating. During processing, scratching or breaking of the coating (sometimes, only marring the surface) must be avoided because these defects could cause rejection of the finished part.

Coated steels have formability characteristics that are different from those of uncoated steels. This article will discuss the effects of coatings on the formability of sheet steels and will provide some general guidelines for the forming of coated steels. The forming of bare (uncoated) steels is discussed in the articles "Press Forming of Low-Carbon Steel" and "Press Forming of High-Carbon Steel" in this Volume.

Press Forming of Coated Steel

Impact of Coating Processes on Substrate Formability

Coated steels can be categorized according to the formability of the substrate. It must be kept in mind, however, that the method used to apply the coating (whether it is zinc (galvanized), aluminum, tin, or terne or an organic coating) can affect the metallurgical structure of the substrate and therefore its properties.

The most prevalent coated steel that is currently in use is zinc-coated steel (galvanized). Steel can be galvanized by hot dipping or electroplating. Although applications for electrogalvanized steel are increasing, most of the galvanized steel currently being used is produced by hot dipping.

Hot-Dip Galvanizing. Hot-dip galvanized steel is produced by one of two basic processes, depending on the properties required. A low-temperature process (~455 to 480 °C, or 850 to 900 °F) is used when the steel is preannealed to achieve a soft, ductile structure and good formability.

Higher temperatures (675 to 870 °C, or 1250 to 1600 °F) are used for in-line annealing (that is, the steel substrate is annealed as the hot dip coating is applied). Because of the short duration of the in-line annealing process, steels processed by this method are less formable than steels that are coated at lower temperature. Depending on carbon content, steels coated by the high-temperature hot-dip process may or may not require postannealing to enhance formability. Low-carbon steels often require postannealing to restore full formability; extralow-carbon steels (0.01% C) normally do not require postannealing.

Electrogalvanizing is conducted at temperatures at or near ambient; therefore, the properties of electrogalvanized steel sheet are nearly identical to those of uncoated cold-rolled steel.

Other types of coatings discussed in this article include aluminum, tin, terne (lead-tin alloy), nickel and chromium, and organic coatings. Because aluminum is applied by hot dipping, aluminum-coated (aluminized) steel is subject to the same types of problems as hot-dip galvanized steel. Tin coatings can be applied by hot-dipping or electroplating; the latter is much more common. Terne is a hot-dip product. Nickel plating and chromium plating are applied by electroplating. The methods used to apply paints and other organic coatings vary, although most require curing at moderate temperature to achieve final properties. These coatings will be discussed in more detail in the following sections.

Press Forming of Coated Steel

Galvanized Steels

The formability of galvanized steels is reduced to some extent by the brittle iron-zinc alloy layer that is produced between the metallic zinc and the steel base during hot-dip galvanizing. The thickness of the alloy layer depends on the temperature-time cycle in galvanizing, but it is also affected by the percentage of other metals, especially lead and aluminum, in the molten-zinc bath. The decrease in formability is usually in direct proportion to the thickness of the iron-zinc alloy layer. Modern hot-dip galvanizing processes use special thermal cycles and low-lead low-aluminum coating materials to minimize formation of the iron-zinc alloy layer.

In deep drawing, the beneficial effects of the free-zinc layer on the surface of the work metal outweigh the adverse effect of the alloy layer, often permitting greater reductions and greater draw depths than with similar uncoated steel. The layer of soft metallic zinc prevents galling during forming by eliminating direct contact between the steel substrate and the punch.

Formability is also influenced by other factors; chief among these are the initial properties of the steel base; the amount of mechanical work before or after galvanizing; and the response of the steel to the heating cycle for galvanizing, to supplementary heat treatments, and to aging. These factors often have a greater effect on the formability of galvanized steel than the galvanized coating does.

Mill Products. Galvanized steel sheet for use in forming is generally purchased in one of four AISI grades:

- Commercial quality (CQ)
- Drawing quality (DQ)
- Drawing quality, special killed (DQSK)
- Drawing-quality special-killed extralow carbon

Drawing-quality special-killed extralow-carbon steel is available in both stabilized (interstitial-free) and nonstabilized grades. The grades listed above are available in either the dead-soft or the temper-rolled condition.

The thickness of the zinc coating per side, including the iron-zinc alloy layer, ranges from about 55 μm (2.15 mils) for the heaviest coating to 16.5 μm (0.65 mils) in eight coating classes. It is more common, however, to specify the desired coating by weight per unit of surface area. This is particularly true in the automotive industry; automakers are requesting closer weight control tolerances for more consistent welding and forming characteristics. Coating weight is commonly specified in the auto industry in terms of grams per square meter (g/m^2) per side.

One-side galvanized steels have also been developed for the automotive industry. These materials are used with the galvanized side facing inward; this gives the protection of galvanizing while maintaining the paintability and weldability of uncoated low-carbon steel on the outside of the panel.

Tool Design. Tools for forming zinc-coated steel parts are of conventional design and are made of cast iron and standard tool steels. However, parts formed of CQ continuous-annealed steel or of steel over 1.52 mm (0.060 in.) thick require more compensation for springback than conventional box-annealed steel and uncoated steel, which have lower yield strengths and hardnesses. Postheat-treated continuous-annealed and box-annealed DQ galvanized steels require the same tooling as uncoated steels in most forming operations.

Forming Applications. Galvanized steel is used for parts that differ widely in forming severity; typical formed parts include automobile frame and body parts, roofing, siding, gutters and downspouts, ductwork, signs, awnings, outdoor hardware, highway guardrails, and culverts. Information on the hot-dip galvanizing process is available in the article "Batch Hot Dip Galvanized Coatings" in *Surface Engineering*, Volume 5 of the *ASM Handbook*; the corrosion resistance of hot-dip galvanized steels is the subject of the article "Hot Dip Coatings" in *Corrosion*, Volume 13 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

Electrogalvanized Steels. Because the intermediate layer of iron-zinc alloy is either absent or very thin on electrogalvanized steels, the formability of these materials is essentially the same as that of equivalent uncoated steels. There is no measurable thickness of iron-zinc alloy on the plated coil stock even after long storage at room temperature; the amount of alloy produced in heat treating the plated stock before forming is usually too small to affect formability. The plating ($\sim$2.5 to 7.5 μm, or 0.1 to 0.3 mil thick) is tightly adherent even under the most severe deformation. Table 1 lists the typical mechanical properties of galvanized and uncoated CQ, DQ, DQSK, and DQSK extralow-carbon steels.

Table 1 Typical substrate mechanical properties of four types of galvanized and uncoated steels

Type of steel	**Yield strength**		**Ultimate tensile strength**		**Elongation, in 50 mm (2 in.), %**	**Average normal plastic anisotropy, $\bar{r}$**	Average strain-hardening exponent, *n*
	MPa	**ksi**	**MPa**	**ksi**			
CQ steels							
Hot-dip galvanized	276	40	352	51	34	1.1	**0.18**
Electrogalvanized	234	34	331	48	38	1.2	**0.20**
Uncoated cold rolled	234	34	331	48	38	1.2	**0.20**
DQ steels							
Hot-dip galvanized	255	37	345	50	37	1.1	**0.20**
Electrogalvanized	207	30	324	47	39	1.2	**0.21**
Uncoated cold rolled	207	30	324	47	39	1.2	**0.21**
DQSK steels							
Hot-dip galvanized							

Conventional in-line anneal plus post heat treat	228	33	338	49	39	1.2	**0.20**
Preanneal plus post heat treat	207	30	317	46	41	1.6	**0.21**
Electrogalvanized	187	27	303	44	43	1.6	**0.22**
Uncoated cold rolled	187	27	303	44	43	1.6	**0.22**
DQSK extralow-carbon steels							
Hot-dip galvanized							
Extralow carbon: stabilized							
(interstitial free) In-line annealed	193	28	331	48	42	1.6	**0.21**
Prebox annealed	172	25	331	48	46	2.0	**0.24**
Extralow carbon: nonstabilized							
Extended postheat treatment							
Electrogalvanized							
Interstitial free	172	25	331	48	44	2.0	**0.23**
Uncoated cold rolled							
Interstitial free	**165**	**24**	**331**	**48**	**47**	**2.0**	**0.24**

Source: Ref 1

Galvannealed steels are hot-dip products that use a zinc-iron alloy coating on both sides of the sheet. The alloy coating is produced by hot-dipping and then annealing or wiping the sheet.

An investigation of the formability of two different galvannealed materials found that the annealing treatment used to produce galvannealed steel resulted in averaging of the substrate, with resulting lower yield strength and higher elongation values (Ref 2). Ductility as measured by the strain-hardening coefficient *n* also increased. However, deep drawability as measured by the plastic-strain ratio *r* decreased. A higher *r* value indicates good resistance to thinning and therefore good deep-drawing properties. Figure 1 illustrates the effect of the galvannealing treatment on mechanical properties.

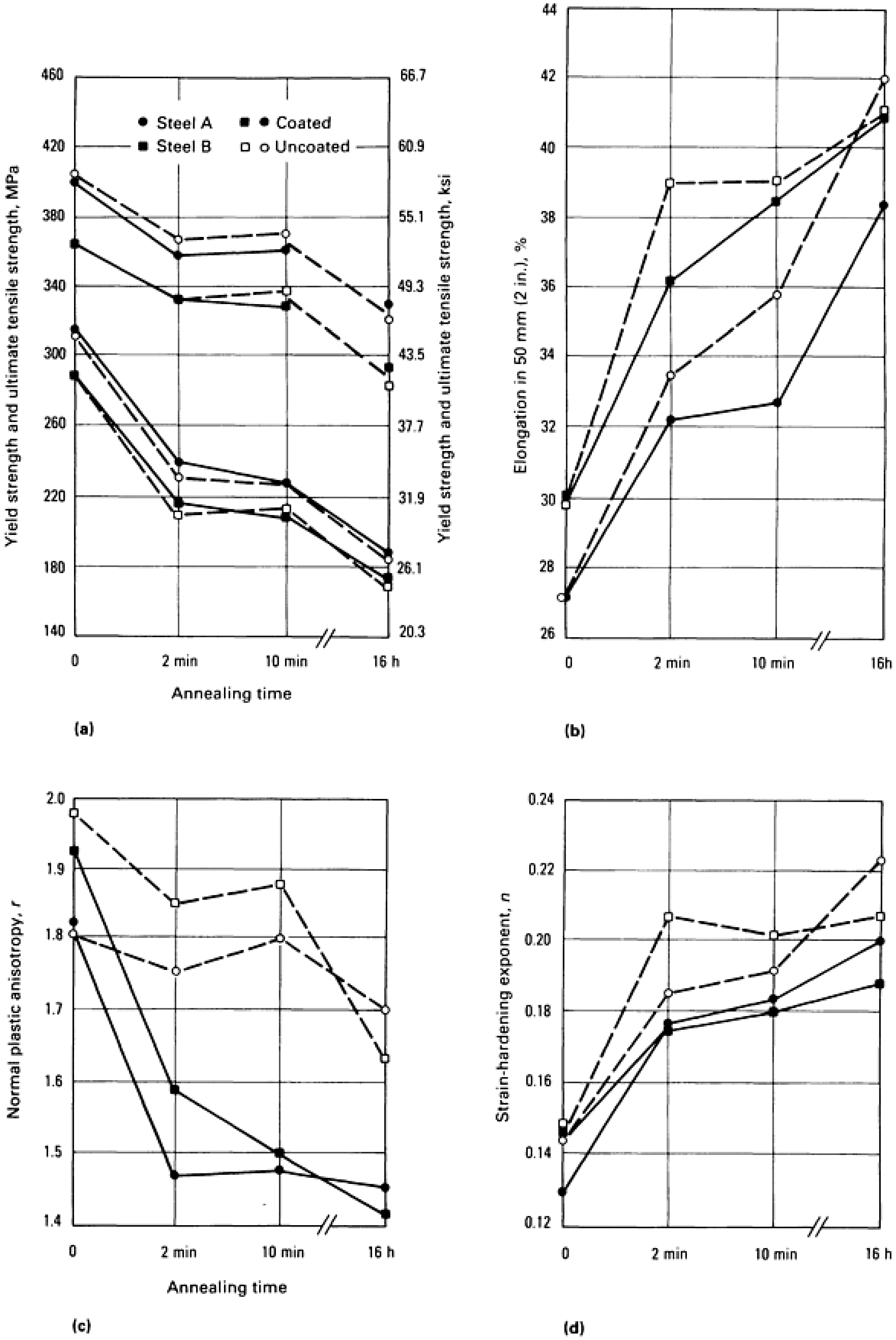

Fig. 1 Effect of the zinc-iron alloy layer on the mechanical properties of galvannealed steels. Data for hot-dip galvanized steel are shown for comparison with galvannealed steels after three annealing times. (a) Yield strength and ultimate tensile strength. (b) Elongation. (c) Normal plastic anisotropy *r*. (d) Strain-hardening

coefficient *n*. Short-term anneals (2 and 10 min) were at a temperature of 550 °C (1020 °F); 16 h anneal was at 420 °C (790 °F). Source: Ref 2.

Galfan-Coated Steels. A relatively recent development in hot-dip coatings is a Zn-5Al-mischmetal coating known as Galfan. Bend and deep-drawing tests have shown that this coating material is less susceptible to cracking upon forming than other hot-dip coatings (Ref 3). Figure 2 compares forming limit diagrams for hot-dip galvanized and Galfan-coated steels. The forming limit diagram shows the maximum strain a material can withstand during forming without necking.

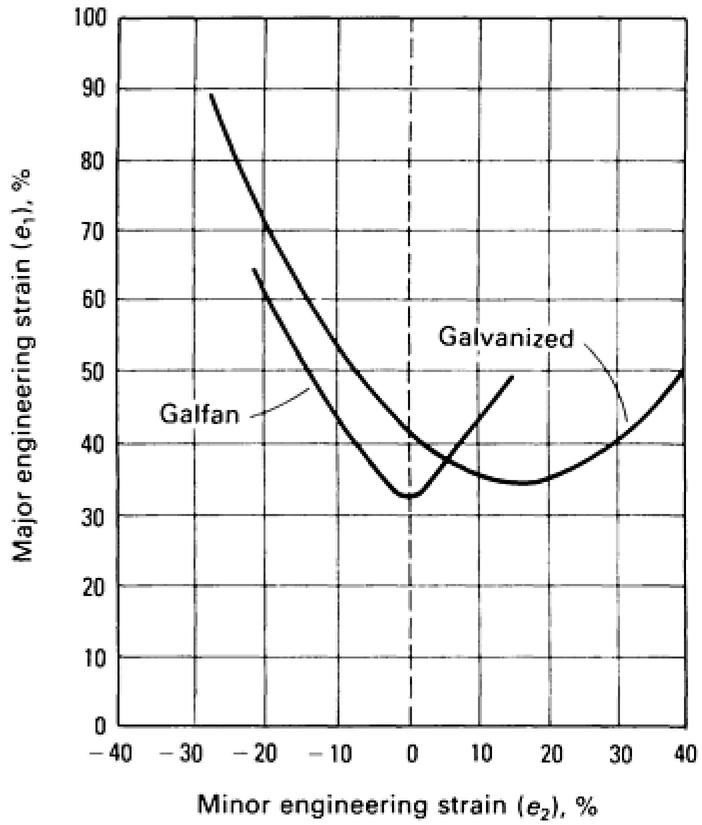

Fig. 2 Forming limit diagrams for hot-dip galvanized and Galfan-coated steel. Source: Ref 3.

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2. W. Warnecke and W. Muschenborn, "Formability Aspects of Galvannealed Steel Sheet," Paper 16, presented at IDDRG '85, Amsterdam, The Netherlands, International Deep Drawing Research Group, May 1985
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Aluminum-Coated Steels

Moderately severe forming is done on hot-dip aluminum-coated (aluminized) steel. The same dies and pressworking practices used for uncoated steel are applicable to aluminum-coated stock. Drawing compounds are recommended for forming and drawing operations (see the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume).

There are two types of aluminum coatings that are commercially significant. Type 2 uses commercially pure aluminum for the coating, and Type 1 uses an aluminum alloy containing 5 to 11% Si. The microstructure of the Type 2 coating has a layer of aluminum, often with scattered iron-aluminum intermetallic particles, bonded to the steel substrate by an iron-aluminum intermetallic layer. This intermetallic layer forms a distinctive serrated boundary with the steel and is generally identified as Fe_2Al_5, although some investigations have found additional iron-aluminum compounds.

A different microstructure is produced when silicon is added to form a Type 1 coating. The intermetallic layer becomes narrower and smoother, and this results in increased formability of the coated product. The coating bath temperature can also be lowered with increasing silicon additions, and the growth of the intermetallic layer is further inhibited.

Alloy additions of beryllium, copper, and certain other elements have also been found to impede the growth of the intermetallic layer. Additions of these and other elements to the steel itself can also retard the growth of the alloy layer. The way in which these elements reduce the growth is not understood, although silicon appears to act after being incorporated into the intermetallic layer itself.

Formability of Mill Products. Forming operations of moderate severity can be done on CQ steel with a Type 1 or 2 coating. Sheet stock withstands bending 180° flat on itself in any direction, without fracture of the steel base, and bending 180° over two thicknesses of the material without flaking or peeling on the outside of the bend. When greater ductility is needed, DQ or DQSK steels with Type 1 coating are used, and they are supplied in a quality suitable for forming a specific part.

Requirements for corrosion resistance in service often limit the permissible severity of forming to less than that described in the preceding paragraph. Hairline cracks that develop in the aluminum coating lead to lower service life at high temperature or in atmospheric exposure. Table 2 gives minimum diameters for 180° bends for 25 cycles of exposure for 30 min at 595 °C (1100 °F) and cooling for 30 min (Type 1 coating) or for a service life of 1 year in a mild industrial atmosphere (Type 2 coating).

Table 2 Minimum bend radii for corrosion-resistant 180° bends in various thicknesses of aluminized steel sheet

Steel sheet thickness, *t*		Minimum bend radius	
mm	**in.**	**Type 1 coating**[(a)]	Type 2 coating[(b)]
1.61	0.0635	1.5*t*	**2.5*t***
1.31	0.0516	1.5*t*	**2*t***
1.00	0.0396	1.5*t*	**1.5*t***
0.85	0.0336	. . .	**1*t***
0.70	$\leq$**0.0276**	**0.5*t***	**0.5*t***

(a) Coating containing about 9% Si and weighing 150 g/m^2 (0.5 oz/ft^2). Minimum radii are for no rust on the outside of the bend after exposure in air for 25 cycles consisting of 30 min at 595 °C (1100 °F) and 30 min of cooling.

(b) Coating of commercially pure aluminum weighing 350 g/m^2 (1.15 oz/ft^2). Minimum bend radii are for no rusting at the outside of the bend after 1 year of exposure to a mild industrial atmosphere.

Adherence testing by reverse bending has shown good correlation in predicting the suitability for forming and drawing steel sheet with aluminum hot-dip coatings of different composition, structure, and thickness or coating weight. Compressive forces are more destructive than tensile forces to the adherence of the coating. In a 180° bend test, the coating always fails first on the compression side of the bend, if it fails at all. Similarly, in a coating failure occurring during a drawing operation, peeling always develops first on the compression side.

In applications involving tension only, as in an elongated tensile specimen, the alloy layer is fractured, but the fractures do not show at the outer surface of the coating. Microscopic examination has verified the ability of the more ductile outer layer to elongate and provide continuous coverage of the cracked alloy particles.

An investigation of the adhesion of Type 1 coatings of various thicknesses in severe press forming found that adhesion decreased as coating thickness increased (Ref 4). In three-station direct redrawing (Fig. 3), the aluminized coating peeled as coating weight exceeded 50 g/m^2 (0.16 oz/ft^2). The thickness of the intermetallic alloy layer was determined to be the primary cause of peeling at higher coating thicknesses (Ref 4).

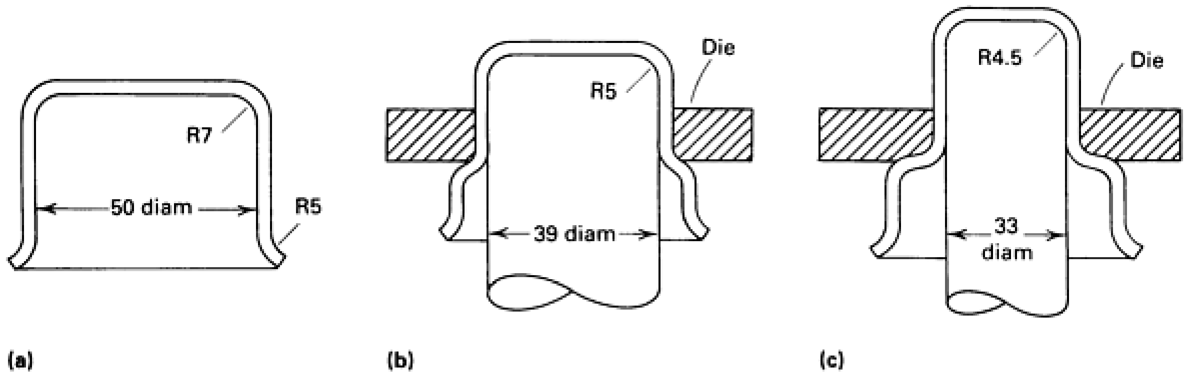

Fig. 3 Three-station direct redrawing used to investigate the formability of aluminized steels in Ref 4. (a) First draw. (b) First redraw. (c) Second redraw. Blank diameter was 90 mm (3.54 in.); blankholder force was 600 kgf (1320 lbf). Dimensions given in millimeters (1 in. = 25.4 mm). Source: Ref 4.

Examples of Application. Steels coated with Type 1 aluminum are used in applications in which resistance to heat and oxidation is important, such as in automotive exhaust systems or heat-treating equipment. Type 2 coatings are used for their resistance to atmospheric corrosion.

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4. T. Yamada and H. Kawase, Formability of Aluminum Coated Steel Sheet, in *Efficiency in Sheet Metal Forming,* Proceedings of the IDDRG 13th Biennial Congress, Melbourne, Australia, International Deep Drawing Research Group, 1984, p 329-338

Tin-Coated and Terne-Coated Steels

The steels to which tin and terne coatings are applied vary in composition for different products and among different manufacturers, but are generally low-carbon grades similar to 1008 or 1010. Nearly all of the common forming methods are used on tin-coated and terne-coated low-carbon steel. Spinning is not ordinarily done on these materials, because of the likelihood of excessively thinning or fusing the coatings.

More than 95% of the steel sheet and strip that is coated with tin at the mill has an electroplated coating of tin; the remainder has a hot-dip coating. The properties of these products are determined by the requirements of the end use.

The thickness of tin on electroplated mill products ordinarily ranges from 0.4 to 2.3 μm (15 to 90 μin.) per side. Hot-dip products have coating thicknesses of 1.7 to 3.8 μm (66 to 150 μin.) per side.

Terne-coated steel products are hot-dip coated with a lead-tin alloy that contains 10 to 25% Sn by weight. Terne coatings range from the thinnest coating that gives complete coverage of the steel to about 1 to 1.8 μm (40 to 70 μin.) per side.

Effect of Coating on Formability. The thin, ductile surface layer of pure tin (or lead-tin alloy) increases die life and reduces lubrication requirements in forming, as do zinc and aluminum coatings. However, unlike the hot-dip zinc and aluminum coatings, the tin and lead-tin coatings have too thin an alloy layer to reduce the formability of the steel base noticeably. Therefore, in general, the formability of tin- or terne-coated steels is the same as that of uncoated steels.

The formability of electrolytic tin-coated sheets and long terne sheets is related to the quality designation of the steel. Commercial-quality sheets are suitable for moderate deformation and can be bent flat on themselves in any direction without fracture of the base steel or the coating. Drawing-quality, drawing-quality special-killed, and, less frequently, physical-quality sheets are used to meet specific deep-drawing and severe forming requirements.

Deep Drawing. A tin coating on steel produces a substantial improvement in the drawability of the base material. The effect of electrolytic tin plate of various thicknesses on the limiting drawing ratio of steel has been investigated for DQ and DQSK steels 0.71 mm (0.028 in.) thick. The results of lubricated Swift cup tests showed that the thinnest tin coating (0.076 μm, or 3 μin., per side) produced a considerable increase in the limiting drawing ratio, that is, an increase in drawability. The increase was greater for thicker coatings (Fig. 4). Unlubricated testing showed the same results, except that the improvement in drawability was not as pronounced (Fig. 4).

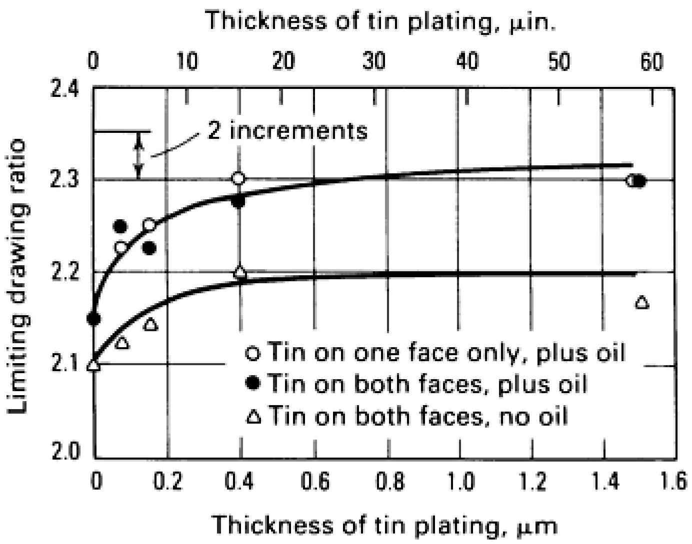

Fig. 4 Effect of tin coating thickness on limiting drawing ratio of steel sheet with and without lubrication.

Applications. Tin plate containers for various items are made at high speed by blanking, forming, rolling, lockseaming, and crimping. Other applications for which tin plate is formed include household utensils and appliances, commercial baking pans, automotive parts, toys, and hardware.

The behavior of terneplate in forming is generally the same as that of tin plate. Because of its high lead content, terneplate is toxic and therefore not suitable for food containers, but it is formed into containers for gasoline and paint. Terneplate is used in formed products such as roofing, door frames, and automotive parts because of its excellent resistance to atmospheric corrosion and its low cost.

Nickel-Plated and Chromium-Plated Steels

Press forming and roll forming are sometimes done on steel that has been electroplated in the coil with decorative copper-nickel or copper-nickel-chromium. More often, however, parts are formed to final shape before electroplating with these materials.

Conventional lubricants can be used in the press forming of this material, particularly in high-volume production. Sometimes, however, no lubricant is used in making decorative parts. Instead, surface contact between the work metal and tools is prevented by the use of strippable plastic coatings or adhesive-backed paper on the work metal, or of loose paper between the work metal and the punch or the die. These materials protect the decorative finish on the preplated steel, prevent galling, and provide a controlled amount of friction for forming. They can also be removed from the completed parts without harming the finish.

Polished die surfaces, or dies or die inserts made of rubber or plastic, also protect parts during forming. Coated blanks or parts can be interleaved with paper, cardboard, or plastic sheet material, or they can be placed in containers with separate compartments to protect the decorative surfaces from damage during storage or handling between forming operations.

Conventional lubricants are used in roll forming. Volatile materials that evaporate completely can be used where cleaning after forming presents problems. Mineral seal oil is preferred in some applications because it leaves only a light residue that may not require removal.

Standard Products. The substrate for these decorative preplated materials is usually 1008 or 1010 cold-rolled steel. Rimmed steel is used for forming applications in which there is only mild or moderate deformation. Aluminum-killed steel is used in applications in which strain lines or aging presents a problem. Drawing-quality steel may be needed where the utmost in severity of deformation is encountered, although its use entails some sacrifice in surface finish in the formed areas.

Stock thickness is usually from 0.20 to 1.27 mm (0.008 to 0.050 in.) for coils and up to 1.57 mm (0.062 in.) for cut sheets, which are usually used in the annealed condition. One manufacturer of preplated strip specifies a hardness of 50 to 60 HR30T on the raw material. Harder tempers can be used to meet special requirements.

Copper plating thickness and nickel plating thickness are usually 2.5 to 7.5 μm (0.1 to 0.3 mil) each, and the chromium plating is 0.075 to 0.25 μm (3 to 10 μin.) thick. A nickel deposit thicker than 7.5 μm (0.3 mil) gives greater corrosion resistance, but usually cannot be produced on coil stock.

Plating conditions are controlled to give a ductile deposit with minimum residual stress. The copper is buffed, and the nickel is buffed or given a satin finish.

Formability. The ductility of the plated steel may be different from that of the unplated coil stock, depending on the metal or metals deposited, plating bath and plating conditions, and the effects of aging or buffing. It may also be affected to some extent by work hardening from the extra coiling and uncoiling of stock during plating.

The plating procedure is usually selected and controlled to yield ductile electrodeposits and a plated product that (after buffing or aging for several days) has formability at least as good as the unplated raw material. Formability of this quality is obtained with buffed, ductile electrodeposits of copper or nickel, or a composite of the two, up to a total plating thickness of 64 μm (2.5 mils), with the normal bright chromium plate over these. Ductility is reduced by the use of nonductile electrodeposits from bright or contaminated plating baths or by the use of unfavorable plating conditions in depositing a heavier-than-normal chromium coating. The severity of forming permitted is usually limited by the need to avoid objectionable visible roughening of the plating or lowering of corrosion resistance in the regions of severe deformation, not by the ability to deform the preplated stock without cracking or rupturing.

Organic-Coated Steels

Steels that have been painted or plastic coated in the coil are formed by the commonly used press and roll methods, using the same equipment as for uncoated stock. The thickness of the steel base is usually 0.25 to 1.57 mm (0.010 to 0.062 in.) but may range from less than 0.20 to 1.9 mm (0.008 to 0.075 in.).

The tools used on uncoated steel can usually be used for painted or plastic-coated steel. However, die materials lower in strength, shock resistance, and wear resistance than those generally used in forming uncoated steel are satisfactory in many applications. Dies may have inserts of rubber or plastic to help protect the coating during forming.

Organic coatings promote long tool life because they generally provide a less abrasive surface than bare steel. This effect varies with the composition of the coating The abrasiveness of an organic coating increases with decreasing gloss.

To avoid scoring or marring of the organic coatings, die surfaces must be highly polished, and workpieces must be handled with care. For forming metals that have coatings only a few tenths of a mil thick, little or no increase in clearance is needed. However, the resilience and compressibility of the thicker dispersion coatings and vinyl-film laminates must be taken into consideration in the design of tooling to ensure that dimensional tolerances are maintained during forming.

Zincrometal is an organic-coated sheet steel that is widely used in the automotive industry. It consists of a mixed-oxide underlayer containing metallic zinc particles and a zinc-rich organic (epoxy) topcoat. The total coating thickness of Zincrometal is about 15 μm (0.6 mils). A solid lubricant has recently been incorporated into the coating to improve formability, which is similar to that of electrogalvanized steel (Ref 5).

Organic composite-coated sheet steels for the automotive industry are currently under development (Ref 6, 7, 8). These coil-coated products generally have an electroplated zinc alloy base layer and a chemical conversion coating under a thin organic topcoat containing a high percentage of metal powder. The thinness of the organic topcoat allows for good formability without damage to the coating (Ref 6, 7, 8).

Lubricants and Handling. Lubricants must be selected that do not affect the appearance or properties of the coating, and they must be easy to remove without damaging the coating. Adhesive-backed paper or strippable plastic films are sometimes used in place of or in conjunction with a lubricant.

Formability of organic-coated metals is generally limited to a severity that will not damage the appearance of the coating, reduce its protective value, or impair its adhesion (although some coatings are flexible enough to withstand deformation that fractures the underlying metal). The effect of the coating on formability, tooling, and forming procedure depends primarily on the type and thickness of the coating and the pretreatment of the base metal.

Table 3 lists a number of types of organic coatings and gives the normal thickness range and adhesion rating for each coating, with the minimum bend radius and formability rating for the coated sheet metal.

Table 3 Forming characteristics of organic precoated sheet steels

For coiled bare, hot-dip or electrogalvanized, and aluminized steels. Applied also to aluminum, copper, and brass substrates. For tin-coated steel, ratings apply only for epoxy coatings; other coatings have lower ratings. For copper and brass, ratings apply only when primers based on epoxy or phenolic resins are used. Data are based on the use of suitable chemical conversion treatments and primers; results may vary for different substrates. Conversion coatings and primers improve results with most coating-substrate combinations.

Type of coating	**Coating thickness**		**Coating adhesion**[a]	**Minimum 180° bend radius**[b]	Suitability for severe forming[a]
	mm	**mils**			
Solution paints					
Alkyd-amino	0.0025-0.03	0.1-1.2	G	3*t*	**F**
Vinyl-alkyd	0.0025-0.03	0.1-1.2	G	2*t*	**F**
Silicone-polyester	0.018-0.03	0.7-1.2	G	2*t*-3*t*	**F**

Thermoset acrylic	0.023-0.03	0.9-1.2	E	1*t*-3*t*	**F-G**[c][d]
Solution epoxy	0.0025-0.025	0.1-1.0	E	0*t*	**E**
Ester epoxy	0.0025-0.025	0.1-1.0	E	0*t*	**E**
Polyester	0.0025-0.03	0.1-1.2	G	2*t*	**G**
Solution vinyl	0.0025-0.03	0.1-1.2	E	0*t*	**E**
Dispersion paints					
Organosol vinyl	0.018-0.10	0.7-4.0	E	0*t*	**E**
Plastisol vinyl	0.10-0.50	4.0-20.0	E	0*t*	**E**[e]
Polyvinyl fluoride	0.013-0.05	0.5-2.0	G	0*t*	**G**
Polyvinylidene fluoride	0.013-0.05	0.5-2.0	G	0*t*	**G**
Laminated plastics					
Polyvinyl fluoride	0.04-0.05	1.5-2.0	G	3.2 mm ($\frac{1}{8}$ in.)	**G**[f]
Polyvinyl chloride	0.10-0.64	4.0-25.0	E	0*t*	**E**[g]
Polyester	0.013-0.36	0.5-14.0	F	0*t*	**F**[c][h]
Tetrafluoroethylene	0.025-0.50	1.0-20.0	G	0*t*	**E**[i]
Acrylic	**0.075-0.15**	**3.0-6.0**	**G**	**0*t***	**G**

(a) Ratings: E, excellent; G, good; F, fair.

(b) *t*, thickness of sheet.

(c) Results are greatly affected by coating thickness.

(d) Coating of medium thickness is good for deep drawing.

(e) Coating can bridge cracks in the metal produced by severe forming; compressibility of the coating must be considered in forming to close tolerances.

(f) Results are greatly affected by substrate material and thickness.

(g) Bonds may be destroyed in extreme draws or sharp bends.

(h) Bond strength may be seriously reduced after slight deformation.

(i) Particularly susceptible to damage by scoring of coating during forming

The ranges of coating thickness shown in Table 3 represent the extreme limits that are technically feasible; most production coatings have much narrower limits of thickness. Primer thickness is not included in the values for coating thickness.

The surface condition of the metal base can influence the permissible severity of forming, particularly with thin (~25 μm, or 1 mil) coatings. Highly polished metal surfaces give a more uniform distribution of the surface stresses induced by tools during forming. This more effectively preserves the appearance and texture of the coating and would allow greater severity of deformation if it were not counteracted by the generation of heat over the entire surface of the coating that is in contact with the forming die.

A metal base with a relatively coarse surface finish subjects the high points of the coating (which for thin coatings are a reproduction of those of the metal beneath) to higher stresses and greater wear during forming. However, the smaller area of contact results in generation of less heat from friction.

Adhesion and Flexibility. The adhesion of commonly used coatings is rated in Table 3. The minimum bend radii and formability ratings in Table 3 are a measure of the combined effect of coating adhesion and flexibility. Vinyl plastisol coatings have such outstanding flexibility that they can bridge cracks produced in the metal base by severe forming.

To provide the adhesion generally required for forming, chemical conversion coatings, selected for the metal surface to be painted or plastic coated, are applied to coil stock or blanks by spray or immersion treatment. This treatment is usually followed by the application of a prime coat compatible with the final coating material. Bare steel is given an iron phosphate coating with a weight of 375 to 480 mg/m^2 (0.013 to 0.016 oz/ft^2). Zinc-coated steel receives a zinc phosphate coating--1.6 to 2.7 g/m^2 (0.005 to 0.009 oz/ft^2) on hot-dip coatings and 1.08 to 1.9 g/m^2 (0.0035 to 0.006 oz/ft^2) on electroplated coatings. Aluminum-coated steel is treated with a chromate coating weighing 215 to 270 mg/m^2 (0.0007 to 0.0008 oz/ft^2). These coating weights refer to area of stock and must be doubled when treatment of both sides of the stock is considered.

Organic coatings that are applied to untreated and unprimed metal surfaces have better flexibility than those applied to pretreated surfaces. However, without pretreatment or the use of a suitable primer, the degree of adhesion necessary to withstand forming forces or to have adequate service life and corrosion resistance usually cannot be assured. As indicated in Table 3, forming characteristics are generally improved by the use of a primer. An exception is tin-coated steel.

The thicker films of organosols, plastisols, and vinyl-film laminates have better flexibility than films of solution coatings in the 0.025 mm (1 mil) dry-film range. Because of their greater thickness, the reduction in thickness from elongation in bending is a smaller percentage than that for the thinner films of solution coatings. The stress of forming is absorbed within the thicker films and is not transferred to the interface with the metal, at which adhesion is established.

Solution coatings, because of increasing film strength, have less flexibility and adhesion near the upper thickness limit. The most severe forming can be done near the lower thickness limit, but such thin coatings may not fulfill service requirements. Although solution coatings of silicone-polyesters and thermosetting acrylics can be applied as thin as 2.5 μm (0.1 mil), as exterior coatings they are used almost exclusively in the heavier thicknesses shown in Table 3. Laminated plastic coatings may show excessive film strength and therefore less flexibility and adhesion at the upper end of the thickness range, and low tensile strength at the lower end. The color and gloss of an organic coating also affect coating flexibility, which decreases with increased pigment loading.

Hardness of organic coatings is in the range of HB to 3H pencil hardness for most of the commonly used paints and about Durometer A 85 to 90 for plastisol coatings and laminated plastics. Softer coatings are more likely to be damaged by scoring in the forming die. Die pressure transmitted through an organic coating to the interface can destroy adhesion. In heavy films and vinyl-film laminates, the elastic, compressible finish coating normally yields under die pressure, but the relatively brittle adhesive primer layer can be damaged by localized high die pressure.

Shearing, Blanking, and Piercing. Sharpness of cutting tools and direction and speed of cut affect the performance of coatings in the cut area. Dull cutting tools or high impact speeds cause high-energy impact on the coating surface and may shatter the bond in the surrounding areas, particularly in coatings of borderline adhesion strength. Flaking or lifting of the coating may result.

Bending. Minimum bend radii for organic-coated metals are given in Table 3. Slow bending will prevent breakage of the coating more effectively than rapid bending. When bending with contour forming rolls, the finish of the organic coating will be preserved, and less stress will be imposed on the steel base if the radii are bent over several rolls instead of one or two rolls.

Bending short flanges close to a cut edge or where the coating is scored by the bending tool at the peak of the bend can cause the coating to lift off the steel base. In both cases, the cohesive strength of the film has been weakened, and coatings with high film strength will attempt to return to the shape in which they were applied.

Deep Drawing. Suitability of the organic-coated metals for deep drawing (or severe forming) is rated in Table 3. The effect of speed of drawing or forming is generally the same as that described for bending.

The more steps used, the more severely a part can be drawn or formed without damaging the organic coating. However, the ductility and work-hardening behavior of the steel, as well as the flexibility of the coating system, must be considered in the design of a forming or drawing die.

Forming Temperature. Depending on the effect of temperature on the properties of the organic coating, heating up to 50 °C (120 °F) before forming will reduce the likelihood of coating fracture. Some coatings, such as silicone-polyester coatings with a high silicone content, can be formed at a temperature as high as 65 °C (150 °F). Heating can be done with infrared radiant heaters, hot air, or an open gas flame or by storing the coil stock in a heated room until fabrication.

Overheating must be avoided. Organic coatings, especially the thermosetting types, can be softened enough to make them subject to surface damage from die action and handling.

The design and surface finish of tools and the selection of handling procedures and equipment are important in forming organic-coated metals. Depending on the accuracy requirements and the coating thickness, allowance may have to be made for the coating thickness in dimensioning the forming tools. When accuracy requirements and coating thicknesses permit, the same dies can often be used for painted and plated metals.

To prevent scratching and scoring of prepainted surfaces, the die surfaces must be polished and sharp corners rounded off. In addition, damage during ejection and subsequent handling of the parts must be prevented.

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Coated High-Strength Steels

The demand is increasing for high-strength steel sheet for stamped parts, particularly in the automotive industry. Although most current applications are for uncoated high-strength steels, the use of coated high-strength steels is expected to increase; corrosion resistance is of even greater concern for the thinner (and therefore lighter) high-strength steel sections used. Coated high-strength steels can be manufactured by any of the conventional coating processes and can be coated with any of the coating materials already described in this article. Table 4 lists typical substrate mechanical properties of galvanized and uncoated high-strength steels.

Table 4 Typical substrate mechanical properties of high-strength steels

Type of steel	**Yield strength**		**Ultimate tensile strength**		**Elongation in 50 mm (2 in.), %**	**Average normal plastic anisotropy, $\overline{r}_m$**	Average strain-hardening exponent, *n*
	MPa	**ksi**	**MPa**	**ksi**			
Hot-dip galvanized							
50	386	56	476	69	28	1.1	**0.15**
60	455	66	545	79	24	1.0	**0.13**
Electrogalvanized							
50	379	55	469	68	28	1.1	**0.16**
60	448	65	538	78	24	1.0	**0.14**
Uncoated cold rolled							
50	379	55	469	68	28	1.1	**0.16**
60	**448**	**65**	**538**	**78**	**24**	**1.0**	**0.14**

The galvanizing of high-strength steels appears to have much the same effect as the zinc coating of carbon steels; that is, the free zinc at the surface acts as a lubricant during forming. Figure 5 compares the limiting drawing ratios of coated and uncoated high-strength steel sheets. The results of a comprehensive comparison of the formability of coated and uncoated high-strength steels are reported in Ref 9.

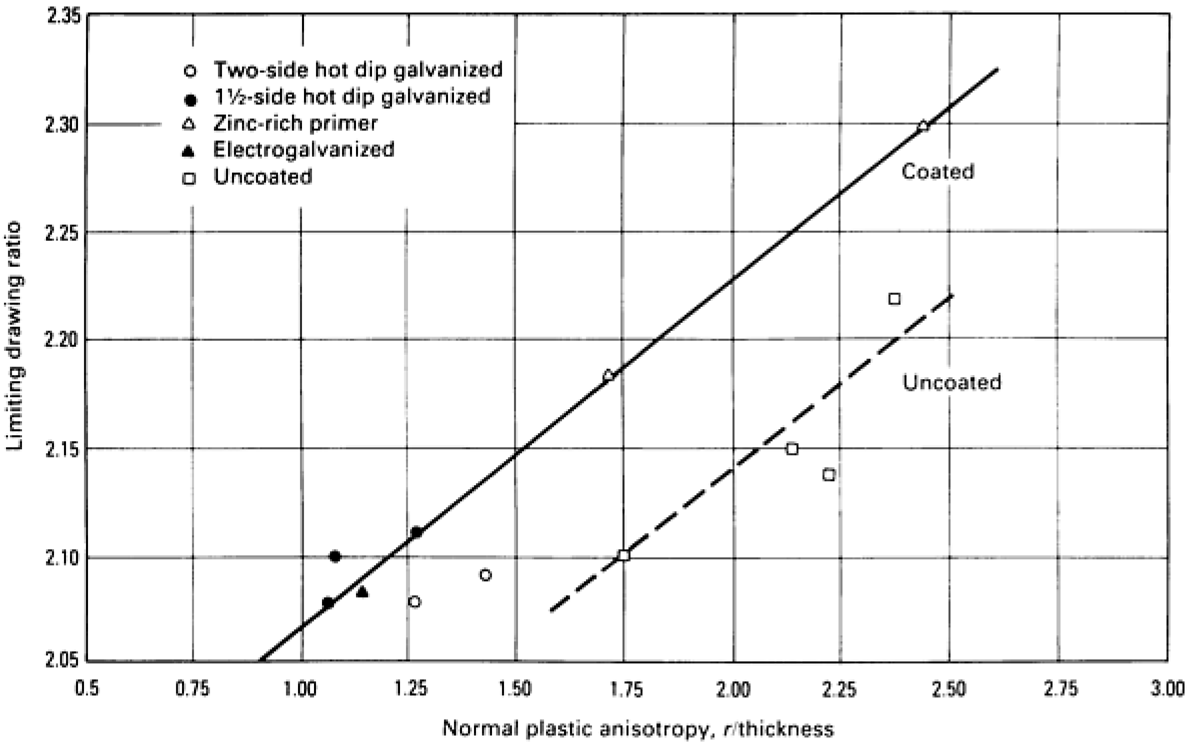

Fig. 5 Limiting drawing ratio for various coated and uncoated high-strength steels. One and a half side galvanized steel is produced by hot dipping, then wiping one side of the sheet to produce a zinc-iron alloy coating. Source: Ref 9.

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Forming of Steel Strip in Multiple-Slide Machines

Revised by Deborah A. Blaisdell, The U.S. Baird Corporation

Introduction

MULTIPLE-SLIDE FORMING is a process in which the workpiece is progressively formed in a combination of units that can be used in various ways for the automated fabrication of a large variety of simple and intricately shaped parts from coil stock or wire. Operations such as straightening, feeding, trimming, blanking, embossing, coining, lettering, forming to shape, and ejecting can all be done in one cycle of a multiple-slide machine. Forming is generally limited to bending operations, but the four slides and center post permit the fabrication of very complex parts. Deep drawing is generally not done in the forming or press stations of a multiple-slide machine.

Acknowledgement

Portions of this article were adapted with permission from T. Hanson, *The Multiform Manual,* Heenan & Froude Ltd.

Applicability

Multiple-slide forming is used to produce shapes from coiled strip or wire. The maximum size of workpiece that can be formed from strip metal in a multiple-slide machine is 203 mm (8 in.) wide by 685 mm (27 in.) long. Parts made from wire up to 1015 mm (40 in.) long (or longer if a special machine is used) and up to 9.5 mm ($\frac{3}{8}$ in.) in diameter can be formed automatically from coil stock. This article deals with the forming of strip stock; the fabrication of wire forms is discussed in the article "Forming of Wire" in this Volume.

If the work metal is comparatively thin and the bending is not severe, tempered strip material can be formed. Plated or coated materials can be formed, but it is usually better to coat after forming because it is difficult to avoid marring coated surfaces during forming. However, nonmetallic inserts at appropriate points in the straightener, feeder, and forming tools can be used to reduce tool marks.

Springback must be considered in bending such materials as stainless steel, phosphor bronze, certain grades of brass and beryllium copper, or high-carbon steel. Adjustments can be made in the forming tools to provide the amount of overbending required for the accuracy of the finished work.

More than one piece can be made in each cycle of a multiple-slide machine. For example, a part that had been made in seven conventional press operations was replanned for the multiple-slide production of four pieces per cycle at 200 cycles per minute.

Forming of Steel Strip in Multiple-Slide Machines

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Multiple-Slide Machines

Multiple-slide machines are made in a range of sizes, all similar in construction and principle. The larger machines have a longer die space, which enables more die stations to be used for the manufacture of complicated components. Generally, the number of strokes per minute decreases and the horsepower increases as the machine size increases.

The four forming slides of a typical multiple-slide machine are generally sufficient for ordinary part-forming needs. However, complex parts can be formed at two or three levels around the center post, thus doubling or tripling the number of forming positions available.

Figure 1 shows a plan view of the main units of a medium-size multiple-slide machine that uses a floor space of 3.7 × 1.5 m (12 × 5 ft), including the stock reel. Four shafts (A, B, C, and D), mounted to a flat-top bedplate, are driven at equal speed through spur gearing (E) by an electric motor. Each of the four shafts is fitted with a positive-action cam (F) that drives a slide (G) (only two of four are identified) on which the forming tools can be secured. In the center of the machine is a vertical post (H) into which the center post or former is fixed and around which the work material is bent. The formed workpiece is removed from the center post by a stripper mechanism, which usually consists of a hardened steel plate surrounding the center post and secured to a vertical rod operated by a cam (F) on the right-hand shaft to give up-and-down motion to the stripper. All of these parts constitute the forming station of the machine.

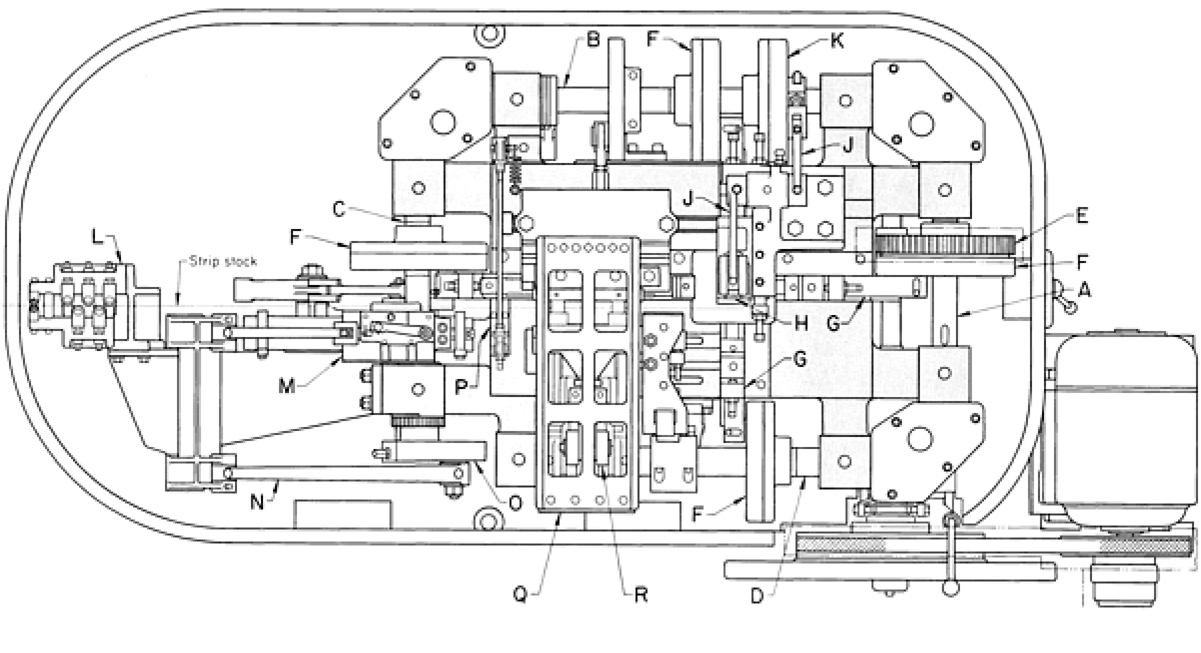

Fig. 1 Plan view of a multiple-slide machine showing major components. A to D, integrated shafts; E, spur gearing; F, positive-action cam; G, slide; H, vertical post; J, bell crank; K and R, cams; L, stock straightener; M, automatic gripper in feed slide; N, links; O, adjustable crank; P, stationary gripper with cam-operated jaws;

Q, horizontal press with dies; R, cam. See text for description of operation.

To the left of the machine proper is a stock straightener (L), shown in working position with strip stock passing through it. Intermittent feeding of the work metal is accomplished by an automatic gripper in the feed slide (M) and an adjustable crank (O), which is attached to a shaft (C). A separate gripper (P) is provided with cam-operated jaws, which grip the strip on the return stroke of the feed slide to prevent backward motion of the strip.

The work metal strip, fed through the machine in a vertical plane (on edge), passes horizontally through dies in a horizontal press (Q). A short, powerful stroke is given to the horizontal press slide by a cam on the front shaft (D) (see Fig. 2).

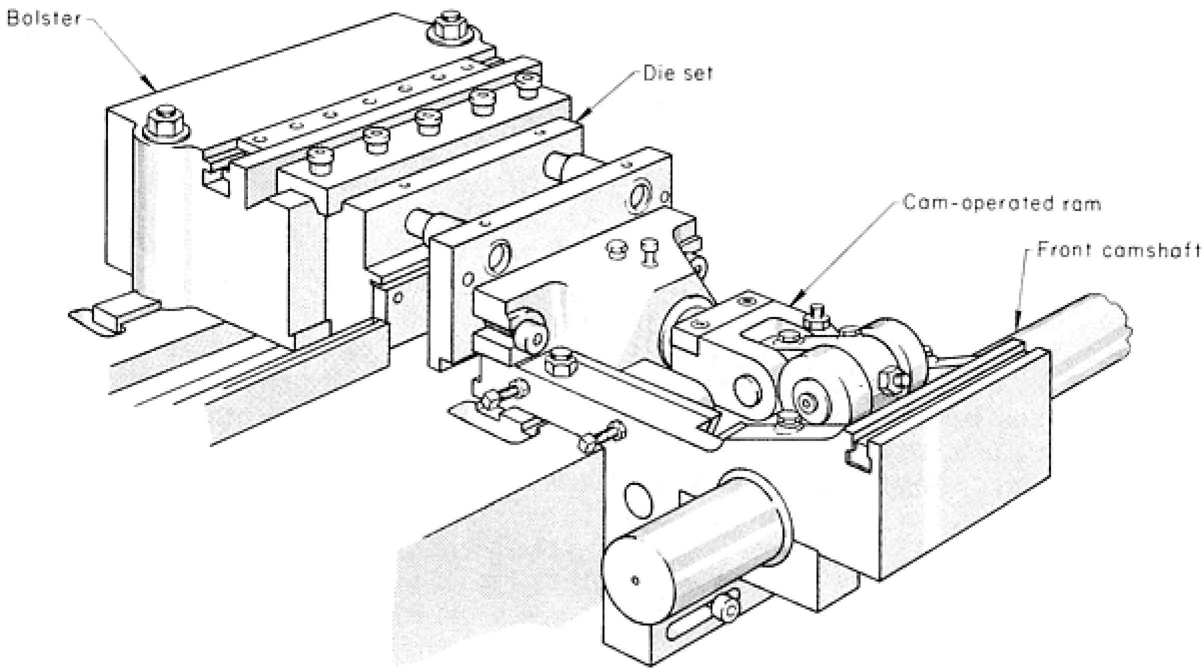

Fig. 2 Press station of a multiple-slide forming machine. See text for details.

Stock straighteners used on multiple-slide machines are similar to those described in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume. The primary difference is that in a multiple-slide machine the rolls are mounted vertically to straighten the work metal as it passes through the machine on edge, instead of horizontally as in a conventional press.

The stock-feed mechanism of a multiple-slide machine is made of two separate units constituting the forward gripping and transporting device (M, Fig. 1) and a stationary gripping unit (P) to hold the strip when it is released on the return stroke of the feed. The stock-feed slide is reciprocated through a system of links from a crank disk keyed to the left-hand camshaft of the machine (M, O, and C, Fig. 1).

Press Station. The die used for piercing, trimming, embossing, and minor forming of the stock is mounted in the press station, which consists essentially of a horizontal press operated by a cam on the front shaft (Q and D, Fig. 1). It may consist of a single unit, as shown in Fig. 2, or additional units can be placed side by side, especially in the larger machines.

Die head units can also be operated from front, rear, or both in machines that are constructed symmetrically. Burr direction can be controlled at designer option.

Cams provide a positive movement to the press slide in both directions, so that the tools are withdrawn from the strip at the end of the working stroke. A dwell at completion of the in-stroke provides time for opposing motions. This permits working cleanly from each side of the stock. A means of adjusting the shut height of the die is provided. The entire unit can also be moved longitudinally along the bedplate to the desired position established by the feed length.

A bolster provides support for the die shoe, and a cam-actuated ram provides support and motion for the punch holder.

The cutoff unit, placed between the press and the front forming slide, is used for cutting work metal into blanks before they are bent to shape. The ends can be cut off straight, or the cut can be curved.

The cutoff unit consists basically of a horizontal slide that is operated through a lever from a cam on the front shaft. The unit can be adjusted along the bedplate in order to cut off the blank at the required distance from the center post. A positive cam action returns the slide so that the cutoff tool will not interfere with forming. Generally, the cutting die and stripper, through which the work metal is fed, are stationary, while the punch moves to cut off the blank.

It is sometimes desirable to install a second cutoff unit on the right-hand side of the center post. The two units trim the two ends of the blank to an accurate length. Because both ends of the blank are trimmed, slightly more stock is required when this method is used, but the inaccuracies of a long feed length are corrected.

The forming station consists of four slides, a center post, and a stripper mechanism. Shaped tools in the four slides progressively bend the workpiece around the center post. In most applications, the center post controls the shape of the workpiece. The first forming tool, usually on the front slide, holds the blank against the center post during cutoff.

Each of the four forming slides runs in its own slideway machined in the bedplate, and each slide is operated positively in both directions by cams. The front, rear, and right-hand slides are all similar in design; the left-hand slide is different because it must pass underneath the press unit.

The tool holder and slide have a mating key and keyway machined parallel to the slide motion so that the forming tool can be adjusted in this direction. A keyway in the top of the tool holder at right angles to the slide movement and a mating key on the end of the forming tool allow sidewise adjustment. The cam that operates each forming slide is made in two halves and has radially disposed slots so that it can be easily exchanged or timing can be adjusted.

The unit shown in Fig. 3 holds the center post rigidly in position for the forming tools to operate around and provides the means for stripping the completed parts off the center post. The center post is fitted and rigidly secured to a king post, which generally consists of a square or rectangular steel bar. The king post fits into a recess in an overhead horizontal slide and is clamped in position by a steel plate and screws. This arrangement holds the king post and center tool rigid and allows for easy adjustment of the vertical position of the center post. The center tool is held square with the forming tools at all times.

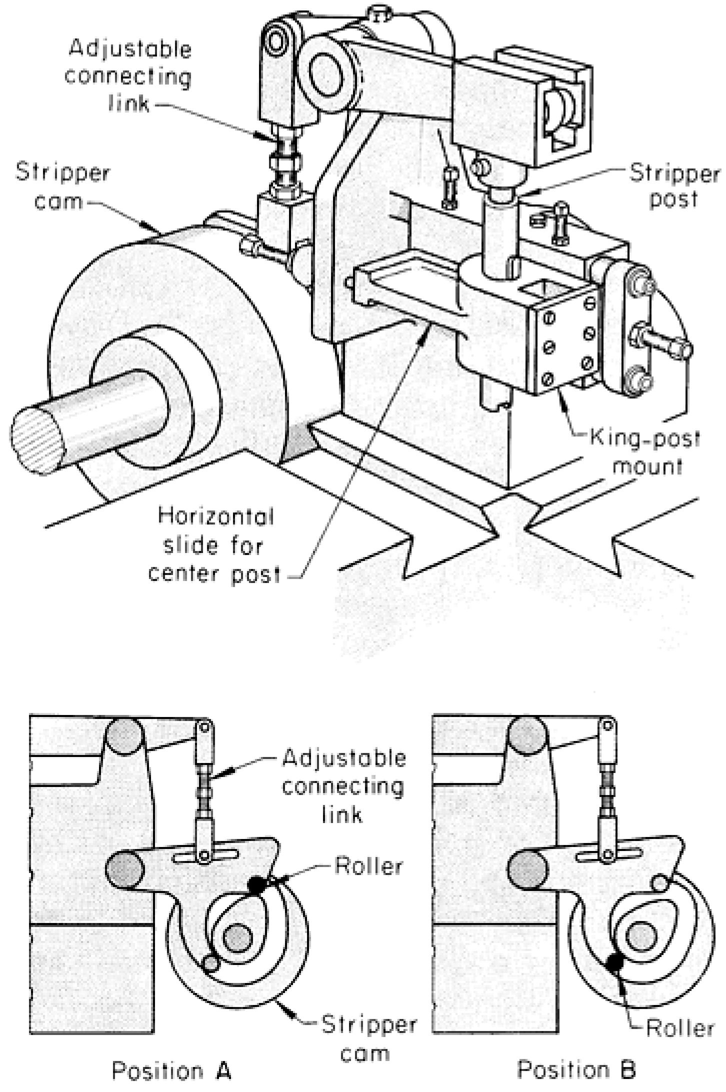

Fig. 3 Unit containing cam-operated stripping mechanism and horizontal slide for mounting of the center post assembly. See text for details.

The overhead slide, carrying the center post, moves parallel with the front and rear slides and is pushed backward by the action of the front tool holding the strip against the center post until the slide reaches a stop, when the metal is bent to shape. When the front tool retracts, the slide containing the center post is returned by spring pressure until a stop is reached. If desired, the slide can be held in a fixed position by adjustment of the two stops. The sliding center post arrangement minimizes interference between the cutoff die and the left-hand slide tools.

Workpieces are ejected from the center post by the downward movement of a suitably shaped stripper plate. The stripper plate is held on a vertical rod, which derives its motion through a system of levers from a positive-action cam on the rear shaft. During forming, the stripper plate is positioned above the forming tools. The vertical rod is guided by a bushing in the overhead slide.

The stripper-cam lever has provision for the roller to be in one of two positions. With the roller as shown in position A, Fig. 3, the cam acts as a normal ejector, moving quickly down and returning the stripper rod almost immediately. With the roller in position B, the rod is held down for a longer period and remains in the upward position only for a short time. This arrangement is used when a retractable mandrel is attached to the mechanism in place of a stripper plate. The mandrel can be held down for a long time while the forming takes place and can then be quickly retracted, as may be required in a further forming or closing operation.

Revised by Deborah A. Blaisdell, The U.S. Baird Corporation

Blanking

The blank that is bent to shape in the forming station has all of the prior operations done in the press station. Trimming the blank outline; piercing holes; embossing ribs, weld projections, and hole flanges; and stamping letters and numerals are done before the blank is cut off and formed. The blank is severed from the strip by cutting off, parting, or blanking methods.

Dies used in multiple-slide machines are either single-stage or progressive, depending on the complexity of the part being formed. Generally, a progressive die is used, even though it may be only a simple two-station pierce-and-pilot die.

The dies for producing the blank must be made as accurately for a multiple-slide machine as they are for a conventional press. However, the multiple-slide dies are of a simpler design and are less expensive than conventional press dies because most of the forming is done in a separate station and around the center post.

The strip layout and die construction are similar to those for progressive dies used in conventional presses. However, as noted previously in this article, the bending operations are usually done in the forming station and cutting off is usually done in the cutoff unit, unless a transfer unit is used to transport the blank from the press station to the forming station. Air jets are used where possible to eject the pierced slugs from the die.

The forming of lanced detents and flanged holes toward the punch side of the blank can be done by actuating the punches with cams on the rear camshaft. Additional movements of die units can be obtained from any of the three other camshafts.

Extended dies are progressive dies that are mounted in the press station and extend into the forming station. After the usual piercing, notching, and piloting operations, bending or forming is done by a combination of elements in the progressive die and those actuated by the front and rear camshafts. The stripper mechanism can be used to advance and retract a mandrel around which the part is bent. The moving or positioning of punches and dies by the camshafts can make it possible to use tools that are much simpler in design than those made for operations in conventional presses. The parts made in extended dies are generally those that can be retained on the strip until all operations are complete before the part is cut off.

Cutting off the blank is usually done with a blade and die mounted in the cutoff unit. The die is fixed to the housing across which the strip of work metal passes. The blade is secured to the slide so that when it is given a forward movement, the blank is sheared from the strip.

Basically, the cutoff tool consists of two flat pieces of metal: the cutoff die (A) and the blade (B), as shown in Fig. 4(a). A fixed stripper (D) holds the work metal (C) against the die, and when the blade moves across the face of the die, the metal is severed. Most of the parts illustrated in this article were cut from the strip by this method.

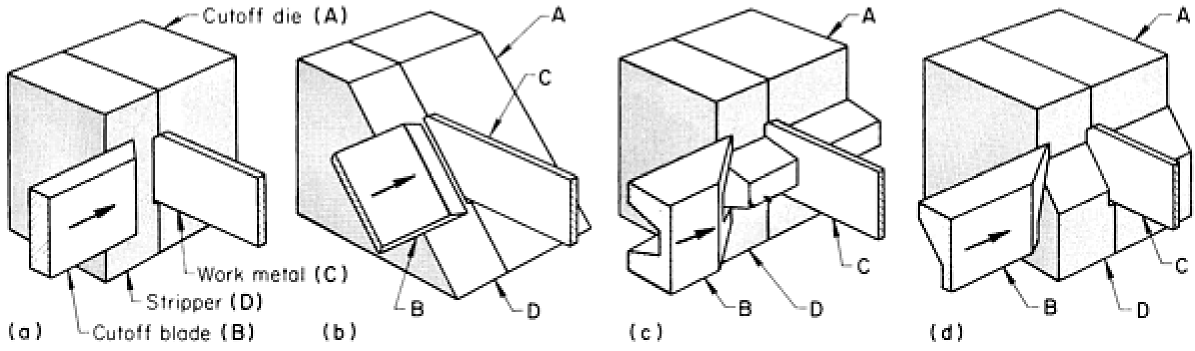

Fig. 4 Four arrangements of cutoff blades and dies for multiple-slide forming machines. See text for details.

The die and blade can be modified to give a shaped cut, as shown in Fig. 4(b) to (d). The shapes illustrated can be used when forming ringlike parts that have a smooth-fitting joint.

Shaped ends on a workpiece can be formed by two methods. The first consists of cutting the full contour of the ends of two adjacent parts in one machine stroke, as shown in Fig. 5(a). The second method consists of partly developing the contour in the press station and then using the cutoff station to complete the contour by parting the blank from the strip by shearing the connecting tab, as shown in Fig. 5(b).

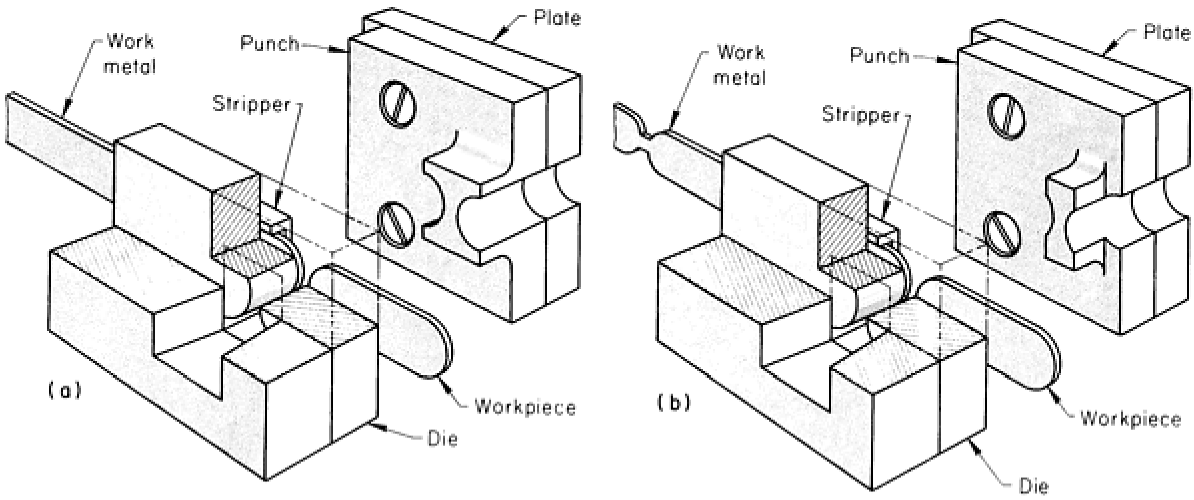

Fig. 5 Two types of parting dies used to shape the ends of blanks before forming. (a) Die for cutting the contour in one stroke. (b) Die for completing the contour by parting a partially developed blank from the strip.

Blending of the parting punch with the previously cut contour may necessitate the use of a punch with sharp corners, which could shorten punch life. If not properly blended, flats or other evidence of mismatch will appear on the severed workpiece.

As shown in Fig. 5, the parting punch is mounted on the plate at the rear of the strip. The die is fitted with a stripper into which the metal is guided across the front of the die. The die and the stripper are both mounted on the slide of the cutoff unit and move horizontally toward the punch, carrying the strip metal with them. When the work metal contacts the punch, the ends are sheared. The scrap is ejected through the die.

Normal press practice is for the die to remain stationary and the punch to move. This is reversed when parting in the cutoff unit so that the end of the metal can swing clear, which would not be possible if the die were at the back of the strip.

Another way in which shaped ends can be produced is to provide a cutoff unit at each side of the front forming tool, each unit being fitted with a die to cut one end. When pilots cannot be used in the die or when a long blank is being cut, the second unit helps in solving tooling problems.

Short Blanks. When the length of the blank is short and the end is very close to the forming slides, the cutoff blade can be fastened to the front tool. The blade is fitted and secured to the front tool; an adjustment screw provides for positioning of the blade. The cutoff die is positioned to mate with the blade.

Transfer of blanks from the cutoff unit to the forming station is generally unnecessary, because the blank can be moved into the forming station before it is cut from the strip. However, it is sometimes desirable to cut the blank from the strip at the last station in the progressive die, as in ordinary press blanking. In such a cycle, a transfer unit such as that shown in Fig. 6(a) is used, which transfers the blank to the forming station. The transfer head is actuated by a stub shaft located below the left-hand camshaft (C, Fig. 1).

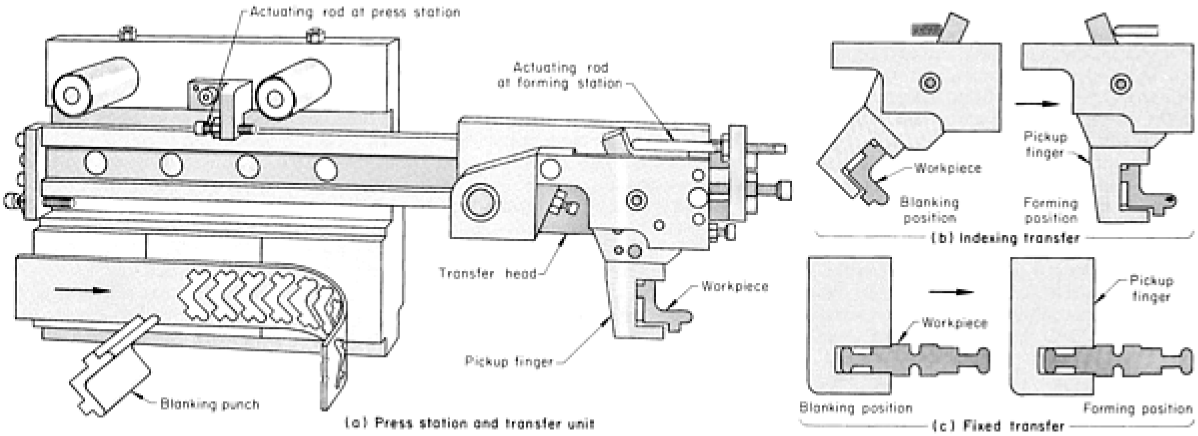

Fig. 6 Mechanism for indexing and fixed transfer of blanks from the press station to the forming station of a multiple-slide machine.

After the blanking punch has retracted, the blank is held in the die cavity. The blank is then pushed out of the die cavity with a plunger that is moved forward by a cam fixed to the rear shaft and into the waiting pickup finger. The blank is transported horizontally to the forming station, where the first forming tool pushes the blank out of the pickup finger and against the center post. While the forming tools are bending the blank around the center post, the transfer head moves back to the press station for another blank.

A blank can be picked up in the blanking station in one position and indexed to the forming position during the transfer motion (Fig. 6b). This permits the strip layout to be designed for optimal stock use. Bends can also be positioned favorably with respect to the rolling direction.

The transfer of blanks from the blanking position to the forming position without reorientation is shown in Fig. 6(c). The pickup finger is mounted in the transfer head so that no swiveling or indexing takes place.

Timing of the transfer-head motion is important. The finger must be in position for loading or unloading, but must not interfere with the blanking punch or the forming slides.

Forming of Steel Strip in Multiple-Slide Machines

Revised by Deborah A. Blaisdell, The U.S. Baird Corporation

Forming

As the work metal strip leaves the press station, it passes through the cutoff unit and between the center post and the first forming tool (usually the front tool). Simultaneously with the cutting off of the blank, the front forming tool moves forward and holds the blank firmly against the center post. By continued movement of the front tool, the blank is bent around the center post. Tools on the two side slides move in to make further bends, and these can be followed by a fourth tool on the rear slide to complete the forming. The sequence in which the slides move is not fixed, nor is it always necessary to use all four slides when forming a part.

Holding blanks firmly in position against the center post is necessary while they are being severed from the stock and also during the initial forming operation in order to prevent slipping and to ensure against premature bending or kinking across a weak section. The work metal is likely to bulge when a U-shape part is being formed. Such a bulge is not removed when the tools are fully closed, because the surplus metal cannot flow while the blank is held firmly by the two corners of the bending tool. In some applications, a spring-loaded plunger in the first forming tool is positioned slightly in advance of the bending portions and prevents the blank from bulging when the bends are started. A positive blankholder is essential for holding forms having a weak section between the bends or for preventing slipping or dragging of a blank around the corner when bending takes place. A partly formed blank also must be held while being moved to the lower level and until a form tool can grip it.

There are two general types of mechanically operated blankholders. One type makes use of the standard stripping mechanism, and the other is operated by a cam and lever from one of the shafts.

Part Ejection. After all tools are retracted, the formed part is ready for ejection by the stripper plate. During forming, the stripper plate is positioned at the top and clear of the forming tools. After forming, the stripper plate moves down and ejects the part from the center post. The stripper can also move a partly formed component into a second, or even a third, forming level on the center post.

Multiple-Part Forming. Forming of more than one part per cycle of the machine should be considered when planning the tools for production, but the increased loading must be within the capacity of the equipment. The way in which the outline of the part is developed depends largely on its finished shape. If a part can be made of strip stock by piercing, cutting off, and forming, then more than one part can be made in each cycle by slitting the strip into two or more ribbons. For parts having shaped ends (for example, semicircular), the strip can be slotted and the blank then cut off. Slotting the strip may eliminate the need for sharp corners on the punch and die, thus increasing their service lives. Parts with more complex outlines require more complex trimming punches. The blank is severed from the strip by either cutoff or parting methods.

Some parts, such as those having the basic shape of an L, are easier to form into a U-shape and then part after forming. The bottom forming level of a multiple-slide machine can be used for parting. The parting punch is positioned on the rear tool slide, and the die on the front tool. The center post has a clearance hole for the punch.

Forming Level. Forming can be done around the center post at the same level at which the blank enters the forming station (single-level forming) or at one or two positions below that level (two- or three-level forming). Parts can be finish formed at the lower level, resistance welded into ringlike parts, or finish formed and then cut into two or more pieces. The partly completed workpieces are usually moved to the lower level by the stripper mechanism.

Single-level forming is used when all of the bends can be made with one set of forming tools, usually with one forward stroke per machine cycle. Sometimes, a bend can be made by partly forming, retracting the tool, doing some work by another tool, and then advancing the first tool to finish the bend. Auxiliary forming tools actuated by separate cams or lever arrangements can be used to do more work on a piece. Wide blanks are usually formed at one level because of the length of center post needed to support more than one part and because of the limited space available mounting tools.

During two-level forming, two components are on the center post--one at the top level and the other at the bottom level. Work is done simultaneously on each piece by the forming tools. Forming is often done at both levels simultaneously by providing each slide with a tool that is shaped to perform an operation on each piece. The workpiece at the lower level is pushed off of the center post by the downward movement of the partly completed piece from the top level.

One of the simplest applications of two-level forming is the manufacture of bushings and ferrules by forming the center and the ends of the blank at the top level and finish forming to final diameter at the bottom level. A more complicated application of two-level forming is described in the following example.

Example 1: Two-Level Forming of a Hose Clamp.

The hose clamp shown in Fig. 7 was made from cadmium-plated steel 9.5 mm ($\frac{3}{8}$ in.) wide at two levels on the center post in a multiple-slide machine. At the press station, the three holes were pierced and a score mark or slight kink was formed between two holes to ensure that the two holes would coincide after the metal had been folded back on itself. Pilots in the press station ensured accurate location of the strip for cutoff and forming. The forming tools used for the two levels are shown at left in Fig. 7.

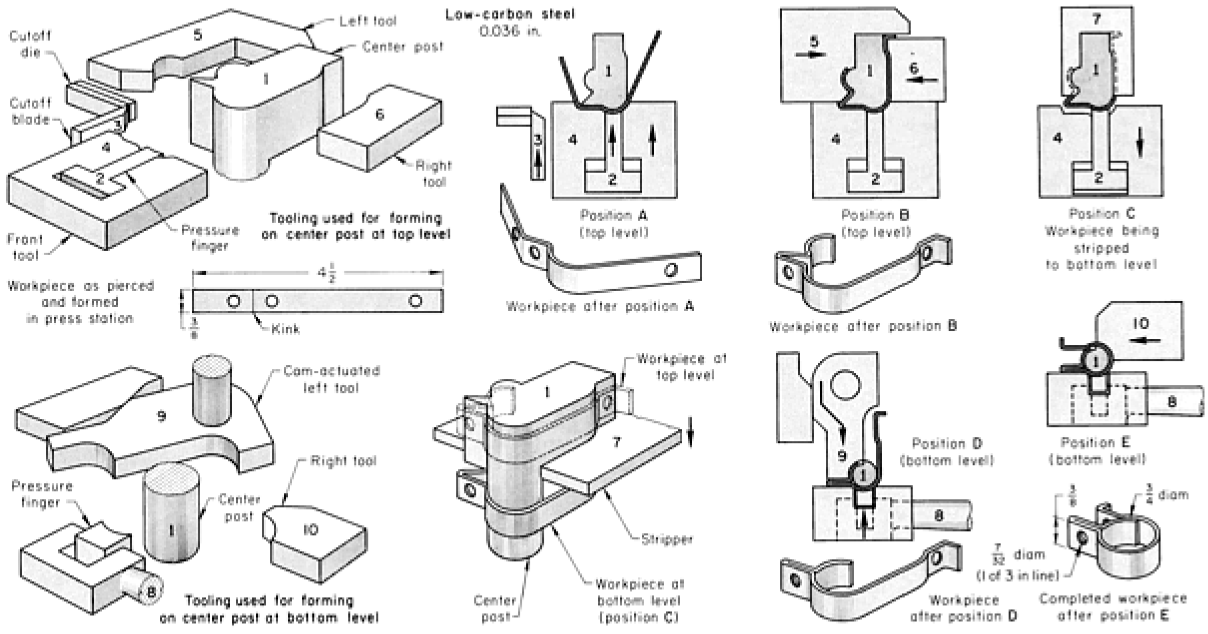

Fig. 7 Forming tools used and sequence of operation in the forming of a low-carbon steel hose clamp. Dimensions given in inches.

At position A in Fig. 7, the cutoff blade (3) sheared the blank from the strip while it was held against the center post (1) by the pressure finger (2). The front tool (4) formed the blank as shown. The side tools (5 and 6) are shown pressing the stock around the center post in position B. With the front and side tools retracted in position C, the pressure finger held the formed part against the center post while the stripper (7) moved the part to the lower level, where the spring-loaded lever arm and pressure finger (8) held the part.

In position D at the bottom level, the pivoting rear tool (9) formed the part around the center post (circular in shape at this level). The rear tool also set the folded-over metal against an extension of the front tool (4). The final forming operation by the right tool (10) was as shown in position E.

In the next cycle, the stripper pushed a new workpiece down to the bottom level, as the completed hose clamp was pushed off of the center post. The cycle time was about 60 components per minute.

Lockseaming. Metal strip is used to make large quantities and varieties of small open-end boxes, tubes, and cylinders to be used as ferrules for paint brushes, radio shields, and many other common products. All of these are made with some locking method for joining the ends of the metal.

In lockseaming or can seaming, one end of the metal is formed over the other and flattened into a tight joint. Other methods of locking two ends of metal together in multiple-slide machine include interlocking dovetails, raised tabs inserted through slots and then flattened or twisted, a tongue inserted into a lanced slot and then swaged, and lancing and forming through two stock thicknesses.

Lockseaming can be done at either one or two levels around the center post. Single-level forming is used where the tubes are long or where projecting lugs would be too weak to push the finished part off of the center post. In this method, the rear and side tools must be advanced two or three times during each cycle of the machine.

Two-level forming with an attachment that gives a positive movement to the seaming tools is a more efficient method of making lockseams. The sliding head containing the center post can be used to make several sizes of lockseam tubes by setting the rear and side tools a fixed distance from the strip line, then changing only the front tool and the center post. The following example describes a two-level forming operation for external lockseaming to make a short cylindrical tube.

Example 2: Two-Level External Lockseaming to Produce a Round Tube.

The external lockseam of the tube shown in Fig. 8 was formed at two levels in a multiple-slide machine. At position A, the strip had been fed between the center post (1) and the pressure finger (2) and sheared by the cutoff die (3) and blade (4). At the same time, the right-hand end of the blank had a small flange formed by the fixed tool (5) and the blade (6). Position B shows the blank formed into a U-shape by the front tool (7). Forming of the cylinder by the side tools (8 and 9) is shown in position C. The two ends extend at right angles to the surface, one end being longer than the other. The right tool (10), moved by the cam (11), supported the previously bent flange as the left tool (8) formed the longer end. At position D, the left tool (12), actuated by the cam (13), bent the long end over the shorter end.

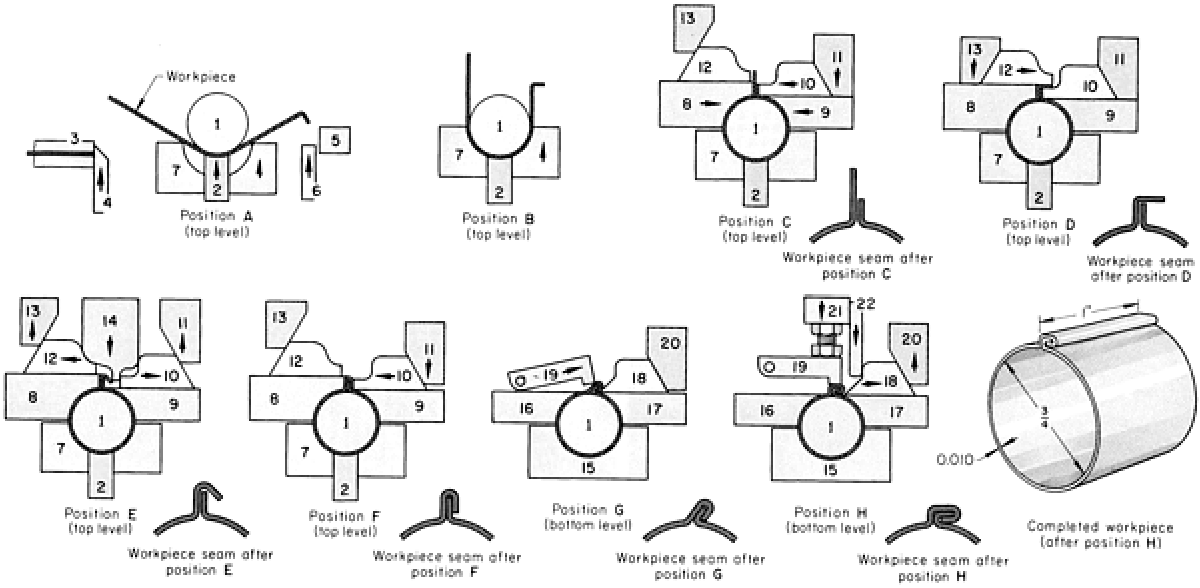

Fig. 8 Two-level forming of an external lockseam to produce a cylindrical part. Dimensions given in inches.

At position E, the cams (11 and 13) retracted, allowing the side tools (10 and 12) to be pushed aside by the advancing rear tool (14), thus permitting the end of the seam to be bent. The rear tool retracted, and the cam (11) advanced to move the side tool (10) to flatten the seam against the left tool (8), as shown in position F. The part then was transferred to the lower level by the stripper (not shown).

At the bottom level, the front tool (15) and the side tools (16 and 17) held the cylinder on the center post. The front tool also backed up the center post during the final forming operations. As shown in position G, the anvil (18) supported the seam while the swinging tool (19) moved forward to bend the seam at an angle. The anvil is moved by the cam (20). Final flattening of the seam is shown in position H. As the rear tool (21) advanced, the cam projection (22) contacted the anvil, pushing it clear as the swinging tool (19) closed the seam.

Two workpieces were on the center post at this stage, and both were formed at the same time. One, formed through position F, was at the top level; the other was at the bottom level. Both were held tightly on the center post. The completed piece was pushed off of the center post and into a container.

Internal lockseams can be made by forming an external seam, as shown in Fig. 8, and then re-forming the part to make the seam flush with the outer surface. The part is moved to the lower level, where a vertical slot in the center post provides space for the seam as it is pushed inward.

Forming of Steel Strip in Multiple-Slide Machines

Revised by Deborah A. Blaisdell, The U.S. Baird Corporation

Assembly Operations

By means of auxiliary feeding attachments, one or more components can be assembled to a part being formed during the same machine cycle. Hopper feeding units can sort and orient machined or previously formed parts for feeding into the part being formed in the multiple-slide machine. These devices can be arranged to feed horizontally adjacent to any forming slide, or can be operated vertically.

Hopper feeding involves the automatic sorting and transfer of components placed randomly in a hopper of suitable design and shape for delivery by gravity to a track, correctly positioned and in ordered sequence to a machine.

Production Practice. Figure 9 shows the assembly of a flat steel spring into a small steel bracket at the lower level of a two-level multiple-slide machine operation. The bracket was made from 12.7 mm ($\frac{1}{2}$ in.) wide stock 0.76 mm (0.030 in.) thick. In the press station, the two holes were pierced, and the lug was slit and raised. The bracket was bent to shape around the center post at the top level by the action of the front, rear, and right forming tools. It was then moved to the bottom forming level by the stripper and held in position against the center post by a spring-loaded retainer. A sliding pin mounted in the retainer was used to flatten the lug against the spring steel strip.

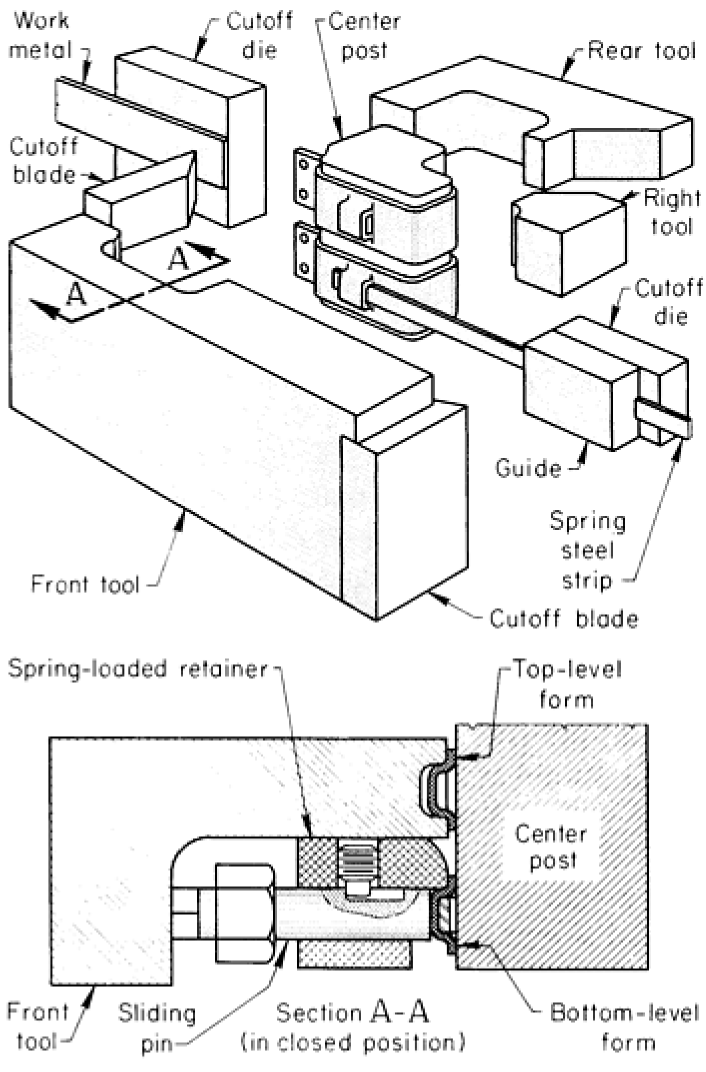

Fig. 9 Assembly of spring steel strip into a bracket at the bottom level of a two-level multiple-slide forming operation.

The spring steel strip was fed into the recess formed by the lug. The front tool then moved in, contacted the sliding pin, flattened the lug, secured the strip to the bracket, and at the same time severed the spring steel strip by a blade mounted to the front tool.

On the next cycle, the assembly was ejected from the center post by the downward movement of the succeeding component. The lower part of the spring-loaded retainer was cut away to clear the strip as it was fed into the lug and to permit ejection.

Forming of Steel Strip in Multiple-Slide Machines

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Multiple-Slide Rotary Forming Machines

The multiple-slide rotary forming machine with open planetary gearing is simple in concept and design. In this type of machine, the slides are driven by a central gear wheel, and the central forming motions are controlled by cams.

Figure 10 shows a schematic of a multiple-slide rotary forming machine. The material is first uncoiled from the reel, then pulled through the straightening station (A) by the material feeder (B). It is then fed to the press (C) and forming slides (D), where the workpieces are formed and ejected. Recessed rails guide the material between the feeder and the stamping station, as well as between the cutting and forming stations. Here, the blanks are either completely stamped out in the press and then pushed into the bending station by the following material, or they remain attached to the strip by means of a web that is not completely separated until it reaches the final forming station. Material feed, stamping presses, forming slides, and operation of the central tool are synchronously coupled to each other.

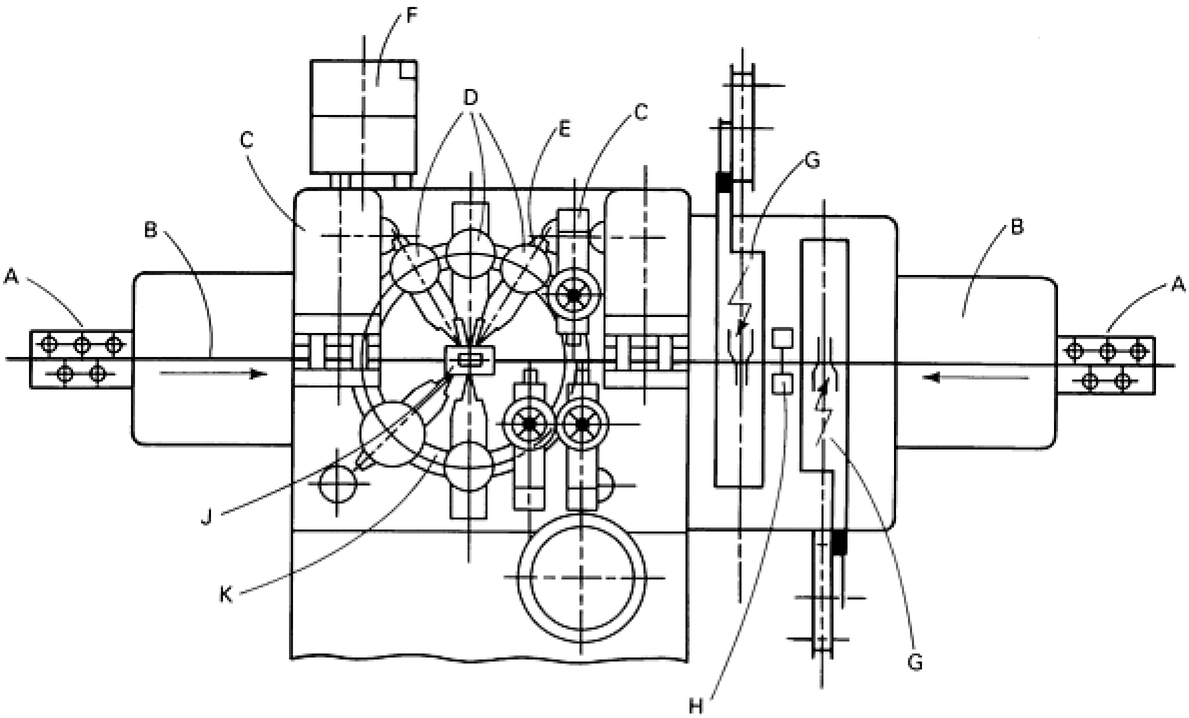


Fig. 10 Schematic of a multiple-slide rotary forming machine. A, straightening stations; B, feed mechanism; C, stamping stations; D, forming tools; E, thread tapping unit; F, station for feeding and assembling; G, welding stations; H, sizing unit; J, standard forming tool guide; K, central gear wheel. See text for description of operation.

Auxiliary Equipment. A second material feed and press, a part-feeding mechanism, three thread-tapping units, and two welding units can be added to a multiple-slide rotary forming machine (Fig. 10). It is possible to have additional material feeds from the front and rear sides. Additional cam motions allow forming slides and standardized tools to be mounted on the rear side of the bed in the forming area. Machined assembly surfaces on the bed and cover plate offer further possibilities for fastening attachments.

Standardized Tooling. The forming tool guide (the basic tool) consists of the tool plate with a central slider for one or more tools (Fig. 11). Forming tools are suspended in the tool holders mounted on the forming slides, which can be easily inserted or removed. In operation, the tool guide is enclosed by a cover plate, with an opening at its center for ejection of the working piece. Control cams drive the forming slides and consist of two complementary disk cams.

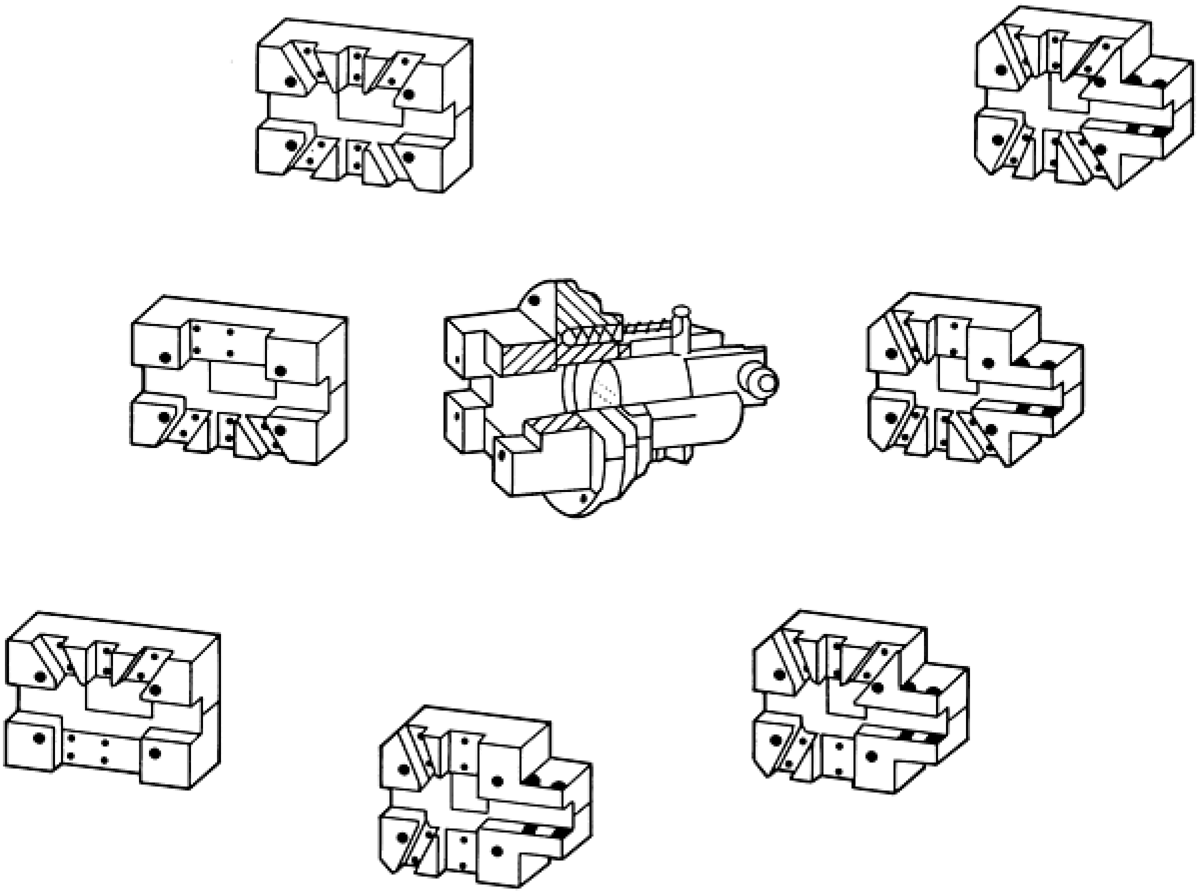


Fig. 11 Standard forming tool guides for multiple-slide rotary forming machines.

Deep Drawing

Introduction

DEEP DRAWING of metal sheet is used to form containers by a process in which a flat blank is constrained while the central portion of the sheet is pressed into a die opening to draw the metal into the desired shape without folding of the corners. This generally requires the use of presses having a double action for hold-down force and punch force. The process is capable of forming circular shapes, such as cooking pans, box shapes, or shell-like containers. The term deep drawing implies that some drawing-in of the flange metal occurs and that the formed parts are deeper than could be obtained by simply stretching the metal over a die. Clearance between the mate punch and the female die is closely controlled to minimize the free span so that there is no wrinkling of the sidewall. This clearance is sufficient to prevent ironing of the metal being drawn into the sidewall in the deep-drawing process. If ironing of the walls is to be part of the process, it is done in operations subsequent to deep drawing.

Suitable radii in the punch bottom to side edge, as well as the approach to the die opening, are necessary to allow the metal sheet to be formed without tearing. In most deep-drawing operations, the part has a solid bottom to form a container and a retained flange that is trimmed later in the processing. In some cases, the cup shape is fully drawn into the female die cavity, and a straight-wall cup shape is ejected through the die opening. To control the flange area and to prevent wrinkling, a hold-down force is applied to the blank to keep it in contact with the upper surface of the die. A suitable subpress or a double-action press is required. Presses can be either hydraulic or mechanical devices, but hydraulic presses are preferred because of better control of the rate of punch travel.

Any metal that can be processed into sheet form by a cold-rolling process should be sufficiently ductile to be capable of deep drawing. Both hot- and cold-rolled sheet products are used in deep-drawing processes. The cold-work effects introduced during processing of the sheet products for deep-drawing applications must be removed (by annealing, for example), and the as-delivered coils should be free of any aging. This would imply that an aluminum-killed drawing-quality steel, for example, would be preferred over a rimmed steel. After the deep-drawing operation, ductility can be returned to that of the original sheet by in-process annealing, if necessary. In many cases, however, metal that has been deep drawn in a first operation can be further reduced in cup diameter by additional drawing operations, without the need for intermediate annealing.

The properties considered to be important in sheet products designed for deep drawing include:

- Composition, with a minimum amount of inclusions and residual elements contributing to better drawability
- Mechanical properties, of which the elongation as measured in a tension test, the plastic-strain ratio *r* (see the section "Drawability" in this article), and the strain-hardening exponent *n* are of primary importance. The strength of the final part as measured by yield strength must also be considered, but this is more a function of the application than forming by deep drawing
- Physical properties, including dimensions, modulus of elasticity, and any special requirements for maintaining shape after forming

Once a metal has been deep drawn into a suitable form, it can be further processed to develop additional shape. The first shape is usually a round cylinder, or a modification of this--a square box with rounded corners, for example. This latter shape is related to the cylinder in that the four corners are essentially quarter segments with straight walls between each segment.

For small cylinders, a relationship between the diameter of a circular blank and the bottom diameter of the cup shape to be formed is sometimes used to measure deep drawability. The most commonly referred to cup test for deep drawing is the Swift cup test, which uses a 2 in. (50 mm) diam flat-bottomed punch to form test blanks. For ductile low-carbon steel, aluminum, and brass sheets, a 102 mm (4 in.) circular blank can be formed in a single draw. Increased plastic strain ratio *r* and ductility allow larger blanks to be drawn successfully; the limit is reached when the bottom punches out, rather than forming a cup shape. The blank diameter divided by the punch diameter gives the limiting draw ratio (LDR), which for the above examples would be 2. More information on the Swift cup test is available in the article "Formability Testing of Sheet Metals" in this Volume.

Of the commonly formed metals, brass and austenitic stainless steels show high limiting draw ratios (up to 2.25). A few samples have reached a value of 2.5 to produce a cup with a sidewall height of nearly 64 mm (2.5 in.). This is possible, although the total length of the cup cross section would be 179 mm (7 in.), which is more than the blank diameter because of the deep-drawing forces. The interstitial-free steels, which have average *r* values of 2.5 or higher, can be deep drawn to limiting draw ratios near 2.5. With these extremely deep draws, sidewall delayed splitting must be prevented by such means as stress-relief annealing immediately following the drawing operation.

The thickness of the work metal does not change appreciably in deep drawing; therefore, the surface area of the final part is about the same as that of the initial blank. As the metal in the flange area is drawn into the die opening over the approach radius, it is subjected to radial tension and, concurrently, to circumferential compression. This explains why a 127 mm (5 in.) diam blank with a surface area of 126 cm^2 (19.6 $in.^2$) can form a cup shape 64 mm (2.5 in.) deep that has a total surface area of about 121 cm^2 (18.8 $in.^2$).

With proper balance among the punch force, the hold-down force, and the strength of the metal sheet being formed, a cup shape can be developed. At the start of this process, the metal in the free area between the punch bottom and the flange hold-down is stretched and wrapped over the nose of the punch and the approach radius of the die. During this stretching, strain hardening strengthens the metal. If it is not capable of such strengthening or if its strength at any location is exceeded at any time during the forming, the bottom of the cup shape will break out. Contributing to this strengthening is a high *r* value, which is a measure of resistance to through-thickness changes in the metal sheet. If the metal has a high resistance to thinning and thickening, the bottom radius and the upper sidewall areas retain close to their original thickness, and the radial and circumferential strains obtainable in the drawn-in flange are increased to accommodate the deep-drawing process.

After the bottom has been stretch formed, the clearance between the punch and die is such that the metal in the cup side is free to move without excessive rubbing on the die walls. It has been found that slight roughening of the punch radius and minimizing the lubrication of this area contribute to deeper drawability; however, the die opening should be smooth and well lubricated with a suitable drawing compound.

Fundamentals of Drawing

A flat blank is formed into a cup by forcing a punch against the center portion of a blank that rests on the die ring. The progressive stages of metal flow in drawing a cup from a flat blank are shown schematically in Fig. 1. During the first stage, the punch contacts the blank (Fig. 1a), and metal section 1 is bent and wrapped around the punch nose (Fig. 1b). Simultaneously and in sequence, the outer sections of the blank (2 and 3, Fig. 1) move radially toward the center of the blank until the remainder of the blank has bent around the punch nose and a straight-wall cup is formed (Fig. 1c and d). During drawing, the center of the blank (punch area, Fig. 1a) is essentially unchanged as it forms the bottom of the drawn cup. The areas that become the sidewall of the cup (1, 2, and 3, Fig. 1) change from the shape of annular segments to longer parallel-side cylindrical elements as they are drawn over the die radius. Metal flow can occur until all the metal has been drawn over the die radius, or a flange can be retained.

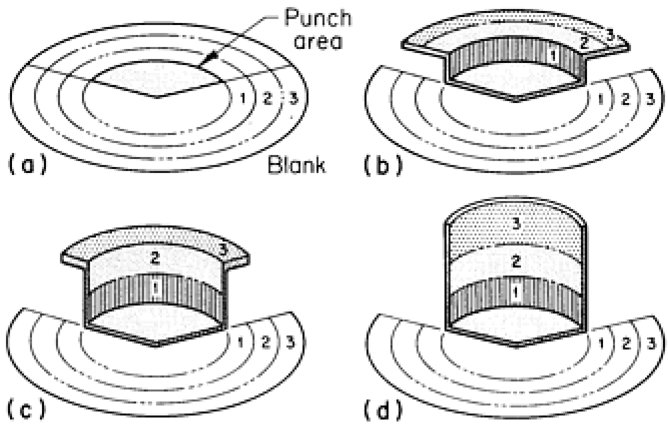


Fig. 1 Progression of metal flow in drawing a cup from a flat blank.

A blankholder is used in a draw die to prevent the formation of wrinkles as compressive action rearranges the metal from flange to sidewall. Wrinkling starts because of some lack of uniformity in the movement or because of the resistance to movement in the cross section of the metal. A blankholder force sufficient to resist or compensate for this nonuniform movement prevents wrinkling. Once a wrinkle starts, the blankholder is raised from the surface of the metal so that other wrinkles can form easily. The force needed to hold the blank flat during drawing of cylindrical shells varies from practically zero for relatively thick blanks to about one-third of the drawing load for a blank 0.76 mm (0.030 in.) thick. Thinner blanks often require proportionally greater blankholder force.

Conditions for drawing without a blankholder depend on the ratio of the supported length of the blank to its thickness, the amount of reduction from blank diameter to cup diameter, and the ratio of blank diameter to stock thickness. For thick sheets, the maximum reduction of blank diameter to cup diameter in drawing without a blankholder is about 25%. This ratio approaches zero for thin foil-like sheet. If a blankholder is employed, the maximum reduction is increased to about 50% for metals of maximum drawability and 25 to 30% for metals of marginal drawability in the same equipment.

Drawability

In an idealized forming operation--that is, one in which drawing is the only deformation process that occurs--the blankholder force is just sufficient to permit the work material to flow radially into the die cavity without wrinkling.

Deformation takes place in the flange and over the lip of the die. No deformation occurs over the nose of the punch. The deep-drawing process can be thought of as analogous to wire drawing in that a large cross section is drawn into a smaller cross section of greater length.

The drawability of a metal depends on two factors:

- The ability of the material in the flange region to flow easily in the plane of the sheet under shear
- The ability of the sidewall material to resist deformation in the thickness direction

The punch prevents sidewall material from changing dimensions in the circumferential direction; therefore, the only way the sidewall material can flow is by elongation and thinning. Thus, the ability of the sidewall material to withstand the load imposed by drawing down the flange is determined by its resistance to thinning, and high flow strength in the thickness direction of the sheet is desirable.

Taking both of these factors into account, it is desirable in drawing operations to maximize material flow in the plane of the sheet and to maximize resistance to material flow in a direction perpendicular to the plane of the sheet. Low flow strength in the plane of the sheet is of little value if the work material also has low flow strength in the thickness direction.

The flow strength of sheet metal in the thickness direction is difficult to measure, but the plastic-strain ratio r compares strengths in the plane and thickness directions by determining true strains in these directions in a tension test. For a given metal strained in a particular direction, r is a constant expressed as:

$$r = \frac{\epsilon_w}{\epsilon_t} \qquad \textbf{(Eq 1)}$$

where ϵ_w is the true strain in the width direction and ϵ_t is the true strain in the thickness direction.

Sheet metal is anisotropic, that is, the properties of the sheet are different in different directions. It is therefore necessary to use the average of the strain ratios measured parallel to, transverse to, and 45° to the rolling direction of the sheet to obtain an average strain ratio $\bar{r}$, which is expressed as:

$$\bar{r} = \frac{r_L + 2r_{45} + r_T}{4} \qquad \textbf{(Eq 2)}$$

where r_L is the strain ratio in the longitudinal direction, r_{45} is the strain ratio measured at 45° to the rolling direction, and r_T the strain ratio in the transverse direction.

If flow strength is equal in the plane and thickness directions of the sheet, $\bar{r}$= 1. If strength in the thickness direction is greater than average strength in the directions in the plane of the sheet, $\bar{r}$> 1. In this latter case, the material resists uniform thinning. Generally, the higher the $\bar{r}$value, the deeper the draw that can be achieved (Fig. 2).

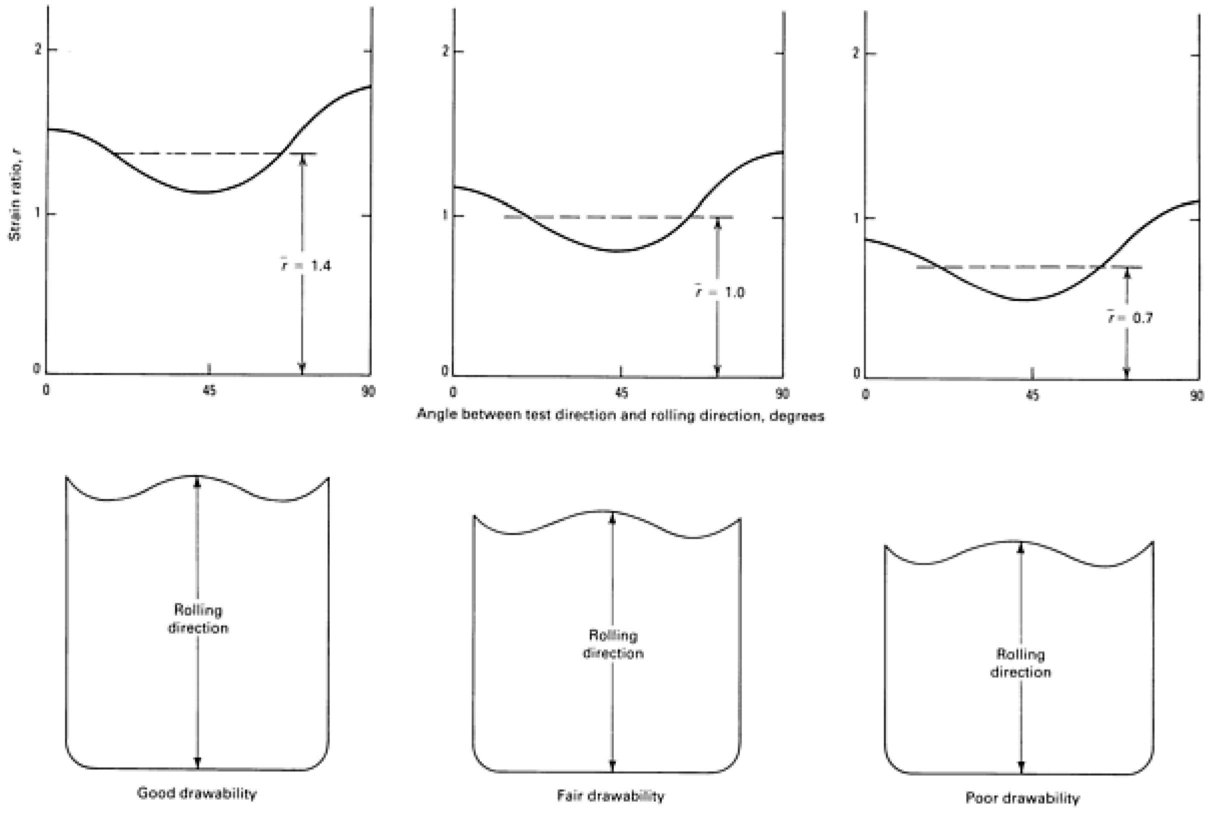


Fig. 2 Variation of strain ratio *r* with direction in low-carbon steel (top curves) and effect of average strain ratio $\bar{r}$ on drawability of cylindrical cups (bottom). Each cup represents the deepest cup that can be drawn from material with the indicated $\bar{r}$.

Because the average strain ratio $\bar{r}$ gives the ratio of average flow strength in the plane of the sheet to average flow strength normal to the plane of the sheet, it is a measure of normal anisotropy. Variations of flow strength in the plane of the sheet are termed planar anisotropy. The variation in strain ratio in different directions in the plane of the sheet, Δr, is a measure of planar anisotropy, and Δr can be expressed as:

$$\Delta r = \frac{r_L + r_T - 2r_{45}}{2} \quad \textbf{(Eq 3)}$$

where Δr is the variation in strain ratio and the other terms are as defined in Eq 2.

A completely isotropic material would have $\bar{r} = 1$ and $\Delta r = 0$. These two parameters are convenient measures of plastic anisotropy in sheet materials. More information on formability is available in the article "Formability Testing of Sheet Metals" in this Volume.

Earing in deep-drawn parts is related to planar anisotropy. The sheet metal therefore may be stronger in one direction than in other directions in the plane of the sheet. This causes the formation of ears on the drawn part even when a circular blank is used. In practice, enough extra metal is left on the drawn cup so that the ears can be trimmed. More information on the effects of anisotropy is available in the section "Effects of Material Variables" in this article.

Draw Ratios. Drawability can also be expressed in terms of a limiting draw ratio or percentage of reduction based on results of Swift cup testing (see the article "Formability Testing of Sheet Metals" in this Volume). The limiting draw ratio is the ratio of the diameter *D* of the largest blank that can be successfully drawn to the diameter of the punch *d:*

$$\mathrm{LDR} = \frac{D}{d} \quad \textbf{(Eq 4)}$$

Percentage of reduction would then be defined as:

$$\text{Percentage of reduction} = \frac{100\,(D - d)}{D} \quad \textbf{(Eq 5)}$$

Additional information on formability testing and other measures of formability is available in the article "Formability Testing of Sheet Metals" in this Volume.

Deep Drawing

Presses

Sheet metal is drawn in either hydraulic or mechanical presses. Descriptions of these machines are given in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume.

Double-action presses are required for most deep drawing because a more uniform blankholding force can be maintained for the entire stroke than is possible with a spring-loaded blankholder. Double-action hydraulic presses with a die cushion are often preferred for deep drawing because of their constant drawing speed, stroke adjustment, and uniformity of clamping pressure. Regardless of the source of power for the slides, double-action straight-side presses with die cushions are best for deep drawing. Straight-side presses provide a wide choice of tonnage capacity, bed size, stroke, and shut height.

Factors in Press Selection

Drawing force requirements, die space, and length of stroke are the most important considerations in selecting a press for deep drawing. The condition of the crankshaft, connection bearings, and gibs is also a factor in press selection.

Drawing Force. The required drawing force, as well as its variation along the punch stroke, can be calculated from theoretical equations based on plasticity theory or from empirical equations. The maximum drawing force $F_{d,max}$ required to form a round cup can be expressed by the following empirical relation:

$$F_{d,max} = n\pi d t s_u \quad \textbf{(Eq 6)}$$

where s_u is the tensile strength of the blank material (in pounds per square inch or megapascals), d is the punch diameter (in inches or millimeters), t is the sheet thickness (in inches or millimeters), and $n = \sigma_D/s_u$, the ratio of drawing stress to tensile strength of the work material. Equation 6 would yield $F_{d,max}$ in either pounds or kilonewtons, depending on the other units used.

The drawing force required to form a round shell can be estimated using Fig. 3. The nomograph shown in Fig. 3 is based on, first, a free draw with sufficient clearance so that there is no ironing and, second, on a maximum reduction of about 50% (note also that only English units of measure are used). Figure 3 gives the load required to fracture the cup or the tensile strength of the work metal near the bottom of the shell. An example of its use is the determination of the force required for deep drawing 0.125 in. thick steel stock with a tensile strength of 50,000 psi into a shell 10 in. in diameter:

- Using Line 1, connect point 10 on scale 2 to point 0.125 on scale 4
- Line 1 intersects scale 3 at 4.0, which is the approximate cross-sectional area (in.^{2}) of the shell wall
- Connect this point using line 2 to point 50,000 on scale 1
- Project a line to the right to intersect scale 5 at 98 tons, which is the required drawing force

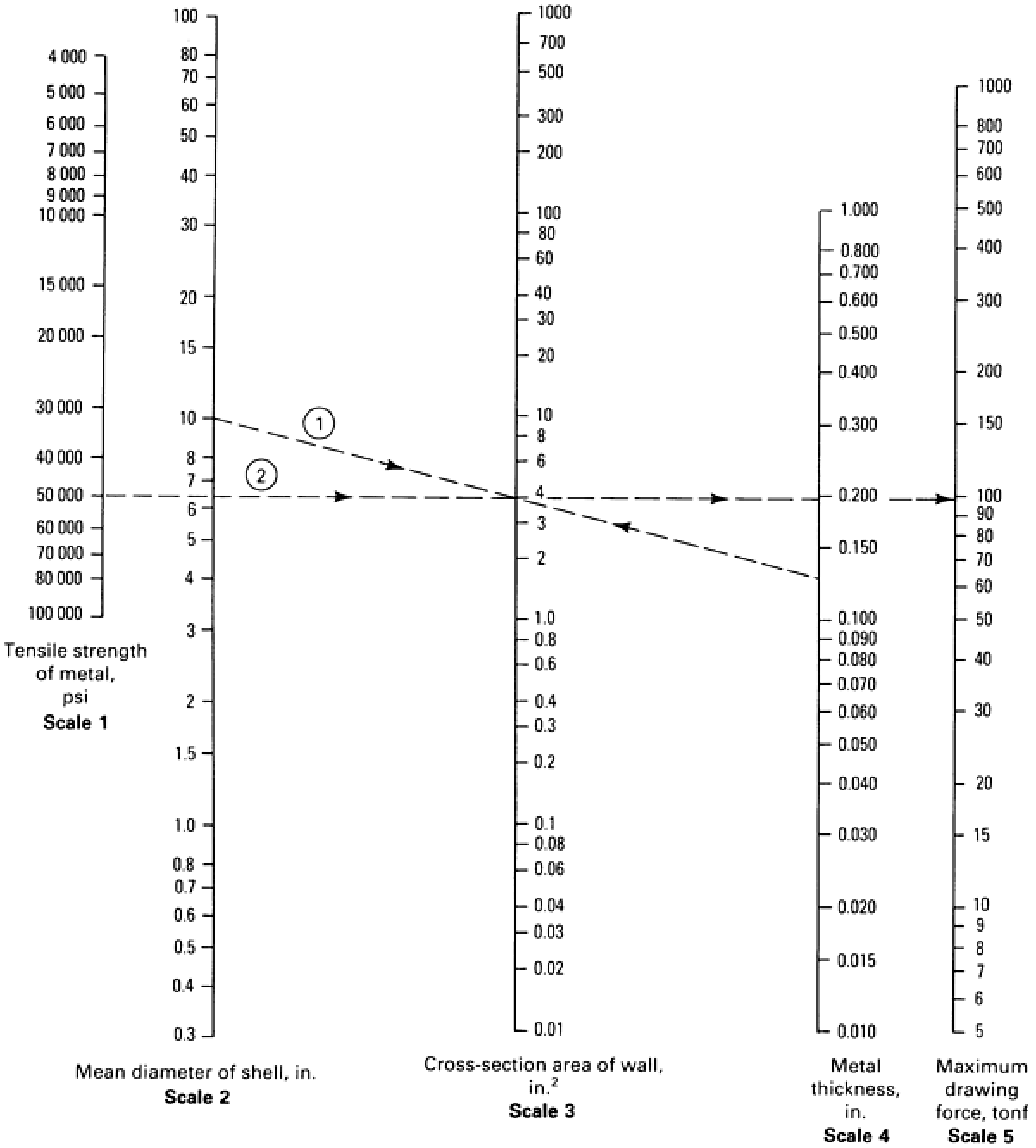


Fig. 3 Nomograph used for estimating drawing force based on several parameters. See text for description of use.

The force required to draw a rectangular cup can be calculated using Eq 7:

$$F_{d,max} = ts_u\,(2\pi R k_a + L k_b) \qquad \textbf{(Eq 7)}$$

where R is the corner radius of the cup (in inches), L is the sum of the lengths of straight sections of the sides (in inches), k_a and k_b are constants, and the other quantities are as defined in Eq 6. Values for k_a range from 0.5 for a shallow cup to 2.0 for a cup with a depth five to six times the corner radius; k_b values range from 0.2 (for easy draw radius, ample clearance, and no blankholder force) to a maximum of 1.0 (for metal clamped too tightly to flow).

When blankholder cylinders are mounted on the main slide of the press, the blankholder force must be added to the calculated drawing force. When a die cushion is used to eject workpieces, the main slide works against this force; therefore, such setups require more drawing force than would be calculated using Eq 6 or 7.

In toggle draw presses, the blankholder force is taken on the rocker shaft bearings in the press frame, so that the crankshaft bearings sustain only the drawing load. In other types of presses, both the drawing and blankholding loads are on the crankshaft, and allowances are made when computing press capacity. For round work, the allowance for blankholding should be 30 to 40% of the drawing force. For large rectangular work, the drawing force is relatively lower than that for round work, but the blankholding force may be equal to the drawing force. Where stretching is involved and the blank must be gripped tightly around the edge (and a draw bead is not permissible), the blankholding force may be two or three times the drawing force.

Blank size governs the size of the blankholder surfaces. Some presses with sufficient force cannot be considered for deep drawing, because the bed size and shut height are inadequate.

Depth of Draw. The length of stroke and the force required at the beginning of the working portion of the stroke are both important considerations. Parts that have straight walls can often be drawn through the die cavity and then stripped from the punch and ejected from the bottom of the press. Even under these ideal conditions, the minimum stroke will be equal to the sum of the length of the drawn part, the radius of the draw die, the stock thickness, and the depth of the die to the stripping point, in addition to some clearance for placing the blank in the die.

Workpieces with flanges or tapered walls must be removed from the top of the die. In drawing these workpieces, the minimum press stroke is twice the length of the drawn workpiece, plus clearance for loading the die. In an automatic operation using progressive dies or transfer mechanisms, at least one-half the stroke must be reserved for stock feed because the tooling must clear the part before feeding begins for the next stroke. For automatic operation, it is common practice to allow a press stroke of four times the length of the drawn workpiece. Therefore, some equipment is not suited to automatic operation, or it is necessary to use manual feed with an automatic unloader, or conversely, because of a shortage of suitable presses.

Slide Velocity. When selecting a press, it is also necessary to check slide velocity through the working portion of the stroke (see the section "Effect of Press Speed" in this article).

Means of Holding the Blank. Double-action presses with a punch slide and a blankholder slide are preferred for deep drawing. Single-action presses with die cushions (pneumatic or hydraulic) can be used, but are less suitable for drawing complex parts. Draw beads are incorporated into the blankholder for drawing parts requiring greater restraint of metal flow than can be obtained by using a plain blankholder or for diverting metal flow into or away from specific areas of the part (see the section "Restraint of Metal Flow" in this article).

Selection Versus Availability. The ideal press equipment for a specific job is often not available. This makes it necessary to design tools and to choose product forms of work metal in accordance with available presses and supplementary equipment. For example, if available presses are not adequate for drawing large workpieces, the manufacturing sequence must be completely changed. It may be necessary to draw two sections and weld them together. In addition, operations that could otherwise be combined, such as blanking, piercing, drawing, and trimming, may have to be performed singly in separate presses.

On the other hand, some manufacturers have placed more than one die in a single press because of the availability of a large press and the shortage of smaller presses. This procedure can cause lower production because all blanks must be positioned before the press can be operated. However, storage of partly formed workpieces and additional handling between press operations are eliminated. Where several small dies are used to reduce overall tool cost, there is economic justification for the use of small-capacity presses. If small presses are not available, it is often more economical to use compound dies. This is particularly true if overall part production is likely to exceed original estimates.

The availability of auxiliary equipment may also influence the type of press and tooling used. For example, if equipment is available for handling coils, plans will be made accordingly. However, if coil-handling equipment is not available and straight lengths of sheet or strip are to be processed, a compatible tooling procedure must be used, even though it might not be the most economical procedure.

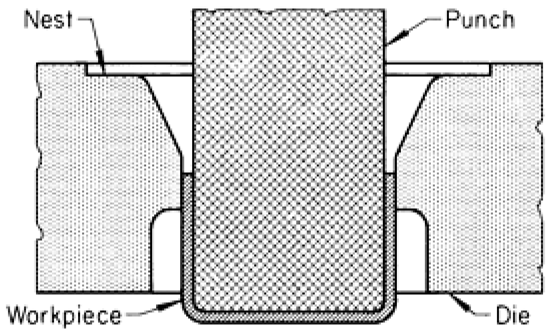
Dies

Dies used for drawing sheet metal are usually one of the following basic types or some modification of these types:

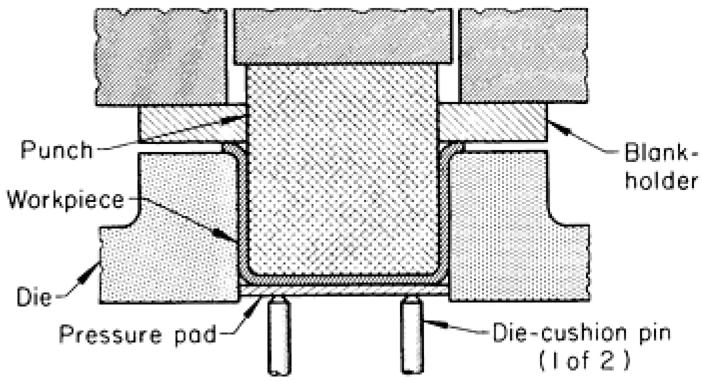
- Single-action dies
- Double-action dies
- Compound dies
- Progressive dies
- Multiple dies with transfer mechanism

Selection of the die depends largely on part size, severity of draw, and quantity of parts to be produced.

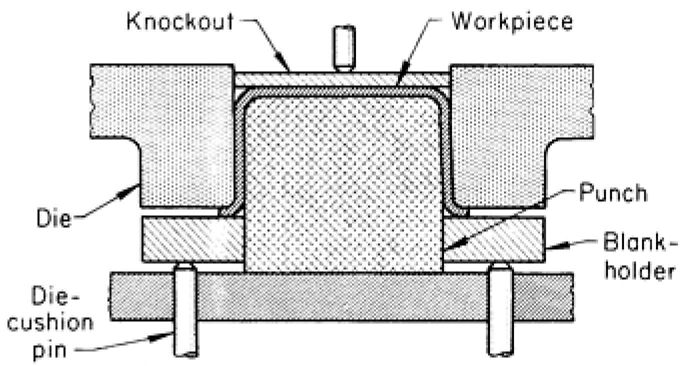
Single-action dies (Fig. 4a) are the simplest of all drawing dies and have only a punch and a die. A nest or locator is provided to position the blank. The drawn part is pushed through the die and is stripped from the punch by the counterbore in the bottom of the die. The rim of the cup expands slightly to make this possible. Single-action dies can be used only when the forming limit permits cupping without the use of a blankholder.



(a) Single-action die



(b) Double-action die



(c) Double-action die, inverted type

Fig. 4 Components of three types of simple dies shown in a setup used for drawing a round cup. See text for discussion.

Double-action dies have a blankholder. This permits greater reductions and the drawing of flanged parts. Figure 4(b) shows a double-action die of the type used in a double-action press. In this design, the die is mounted on the lower shoe; the punch is attached to the inner, or punch slide; and the blankholder is attached to the outer slide. The pressure pad is used to hold the blank firmly against the punch nose during the drawing operation and to lift the drawn cup from the die. If a die cushion is not available, springs or air or hydraulic cylinders can be used; however, they are less effective than a die cushion, especially for deep draws.

Figure 4(c) shows an inverted type of double-action die, which is used in single-action presses. In this design, the punch is mounted on the lower shoe; the die on the upper shoe. A die cushion can supply the blankholding force, or springs or air or hydraulic cylinders are incorporated into the die to supply the necessary blankholding force. The drawn cup is removed from the die on the upstroke of the ram, when the pinlike extension of the knockout strikes a stationary knockout bar attached to the press frame.

Compound Dies. When the initial cost is warranted by production demands, it is practical to combine several operations in a single die. Blanking and drawing are two operations commonly placed in compound dies. With compound dies, workpieces can be produced several times as fast as by the simple dies shown in Fig. 4.

Progressive Dies. The initial cost and length of bed needed for progressive dies usually limit their application to relatively small workpieces. Figure 5 shows a typical six-station progression for making small shell-like workpieces on a mass-production basis. However, larger parts, such as liners for automobile headlights, have been drawn in progressive dies.

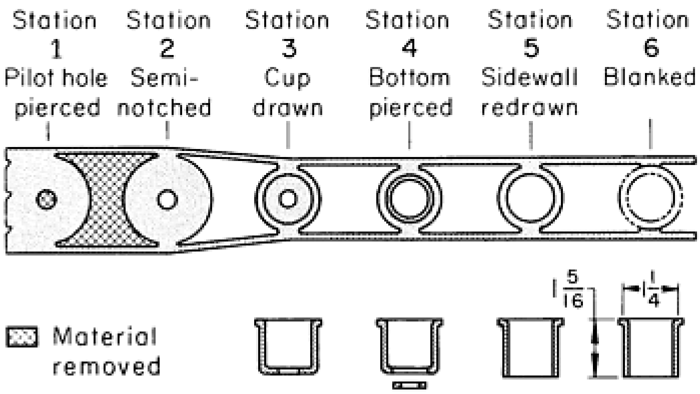


Fig. 5 Production of a small ferrule in a six-station progressive die. Dimensions given in inches.

The total number of parts to be produced and the production rate often determine whether or not a progressive die will be used when two or more operations are required. There are, however, some practical considerations that may rule against a progressive die, regardless of quantity:

- The workpiece must remain attached to the scrap skeleton until the final station, without hindering the drawing operations
- Drawing operations must be completed before the final station is reached
- In deep drawing, it is sometimes difficult to move the workpiece to the next station
- If the draw is relatively deep, stripping is often a problem
- The length of press stroke must be more than twice the depth of draw

Assuming that a progressive die can be used to make acceptable drawn parts, cost per piece is usually the final consideration. Progressive-die drawing is generally considered to be economical if savings in material and labor can pay for the die in 1 year. Ordinarily, the savings achieved by the use of a progressive die results from decreased labor.

Multiple dies, in conjunction with transfer mechanisms, are often used instead of progressive dies for the mass production of larger parts. Multiple dies and transfer mechanisms are practical for a wider range of workpiece sizes than progressive dies are. Although the eyelet-type transfer method is the most widely used for making parts less than 25 mm (1 in.) in diameter, transfer dies are practical for much larger workpieces. The seven-station operation for making the 165 mm ($6\frac{1}{2}$ in.) outside diameter cylindrical shell shown in Fig. 6 represents a typical sequence for the transfer-die method. The workpiece is mechanically transferred from one die to the next. One advantage of the transfer-die method, as opposed to the progressive-die method, is the greater flexibility permitted in processing procedure, mainly because in transfer dies the workpiece does not remain attached to the scrap skeleton during forming. Because of this, precut blanks can be drawn by the transfer method.

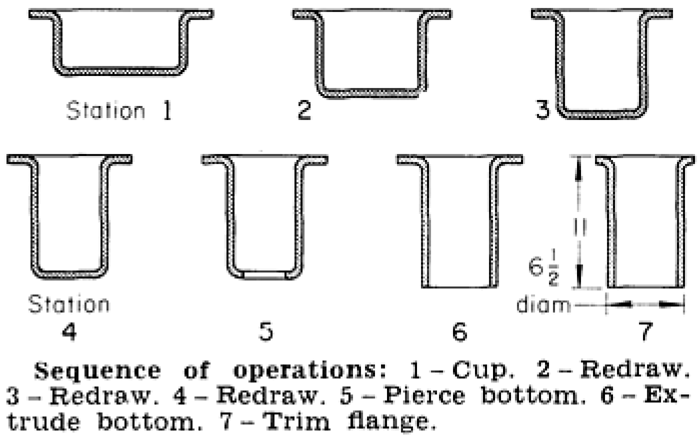


Fig. 6 Seven-station drawing and piercing of a cylindrical part in a multiple die and transfer mechanism. Dimensions given in inches.

Preforms can also be used as blanks. For example, oil pans for automobiles are blanked and partly drawn in a compound die, then finish formed, pierced, and trimmed by the transfer method.

Dies for producing a given part usually cost more for the transfer-die method than for a separate-die operation, but about the same as for a progressive-die operation. The cost of adapting the transfer unit to the part is not included in the die cost. Similarly, the production rate for the transfer method is usually greater than that for a single-die operation, but 10 to 25% less than that for drawing in a progressive die. Many parts can be produced equally well by all of these methods. Under these conditions, tool cost, rate of production, and total quantity of parts to be drawn determine the choice of procedure.

Die and Punch Materials. The selection of material for dies and punches for drawing sheet metal depends on work metal composition, workpiece size, severity of the draw, quantity of parts to be drawn, and tolerances and surface finish specified for the drawn workpieces. To meet the wide range of requirements, punch and die materials ranging from polyester, epoxy, phenolic, or nylon resins to highly alloyed tool steels with nitrided surfaces, and even carbide, are used. Detailed information on tool materials is available in the articles "Selection of Material for Press-Forming Dies" and "Selection of Material for Deep-Drawing Dies" in this Volume.

Effects of Process Variables

The process parameters that affect the success or failure of a deep-drawing operation include punch and die radii, punch-to-die clearance, press speed, lubrication, and type of restraint of metal flow used (if any). Material variables, such as sheet thickness and anisotropy, also affect deep drawing. These are discussed in the section "Effects of Material Variables" in this article.

Effect of Punch and Die Radii

As the blank is struck by the punch at the start of drawing, it is wrapped around the punch and die radii; the stress and strain that develop in the workpiece are similar to those developed in bending, with an added stretching component. The bends, once formed, have the radii of the punch and die corners. The bend over the punch is stationary with reference to both punch and shell wall. The bend over the die radius, however, is continuously displaced with reference to both the punch radius and the blank, and it also undergoes a gradual thickening as the shell is drawn. The force required to draw the shell at the intermediate position has a minimum of three components:

- The force required for bending and unbending the metal flowing from the flange into the sidewall
- The force required for overcoming the frictional resistance of the metal passing under the blankholder and over the die radius
- The force required for circumferential compression and radial stretching of the metal in the flange

Because of the variation in metal volume and in resistance to metal flow, the punch force increases rapidly, passes through a maximum, and gradually decreases to zero as the edges of the flange approach and enter the die opening and pass into the shell wall. With the cup diameter remaining constant, the maximum press load and the length of stroke required to draw the cup depend on the size of the blank. The punch force-stroke relations for drawing blanks of various diameters from brass sheet 1.5 mm (0.060 in.) thick, using a 50 mm (2 in.) diam punch, are shown in Fig. 7.

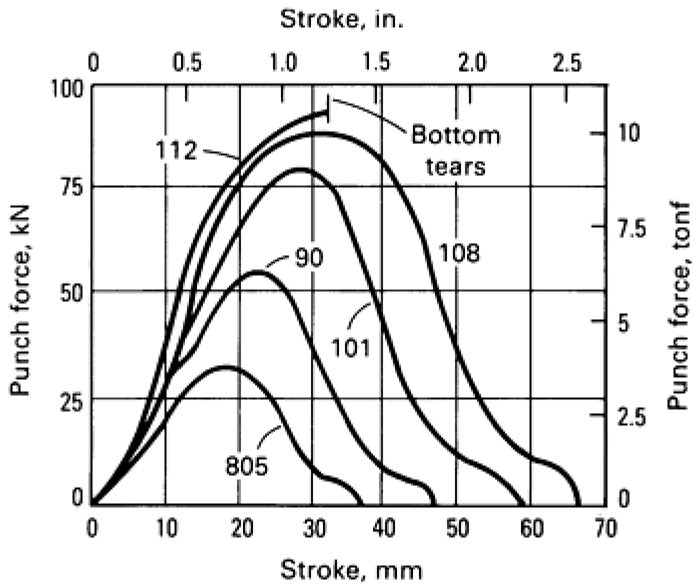


Fig. 7 Force-stroke relations for drawing blanks of various diameters from 1.5 mm (0.060 in.) thick alloy C27400 (yellow brass, 63%) sheet using a 50 mm (2 in.) diam punch. Numbers indicate blank diameter in millimeters.

Under the conditions shown in Fig. 7, during cupping, the shell bottom is subjected to tensile stress in all directions, while the lower portions of the shell wall, particularly the radiused portion connecting the bottom with the wall, are primarily subjected to longitudinal tension. The stress in the metal being drawn into the shell wall consists of combined compressive and tensile stresses. Separation of the shell bottom from the wall is likely if a reduction is made that requires a force greater than the strength of the shell wall near the bottom (Fig. 7).

The punch and die radii and percentage of reduction determine the load at which the bottom of the shell is torn out. Drawing is promoted by increasing punch and die radii. For a given drawing condition, the punch force needed to move the metal into the die decreases as the die radius increases, as shown in Fig. 8.

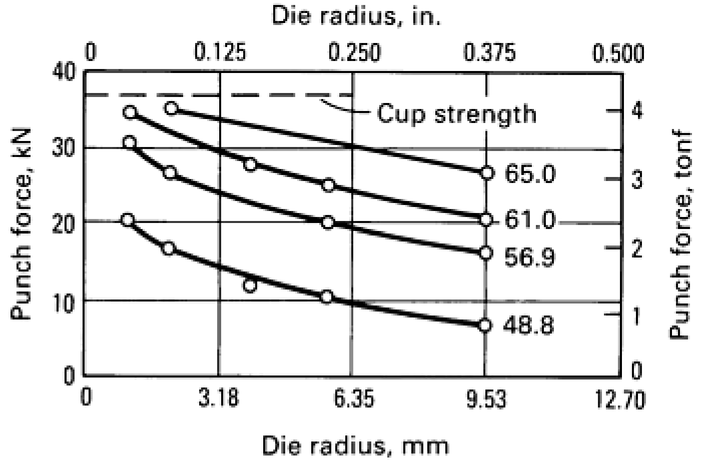


Fig. 8 Effect of die radius on punch force required for cupping various diameters of 1 mm (0.040 in.) thick alloy C27400 (yellow brass, 63%) blanks using a 30.5 mm (1.2 in.) diam punch with a nose radius of 0.61 mm (0.024 in.). Numbers indicate blank diameter in millimeters.

The reduction of drawing force in a double-action die by modification of the effective die radius can be accomplished in two convenient ways, as shown in Fig. 9. In the conical lead-in die (Fig. 9a), the cutout is effective in reducing frictional loads by removal of the portions of the die surface that are usually heavily loaded and increase friction. In Fig. 9(b), the sheet metal is formed into a conical shape before appreciable drawing begins. This has the effect of reducing the area of contact over the die radius by an amount proportional to α/90° (where $\alpha$is the angle to declination of the hold-down surface to the horizontal, as shown in Fig. 9b).

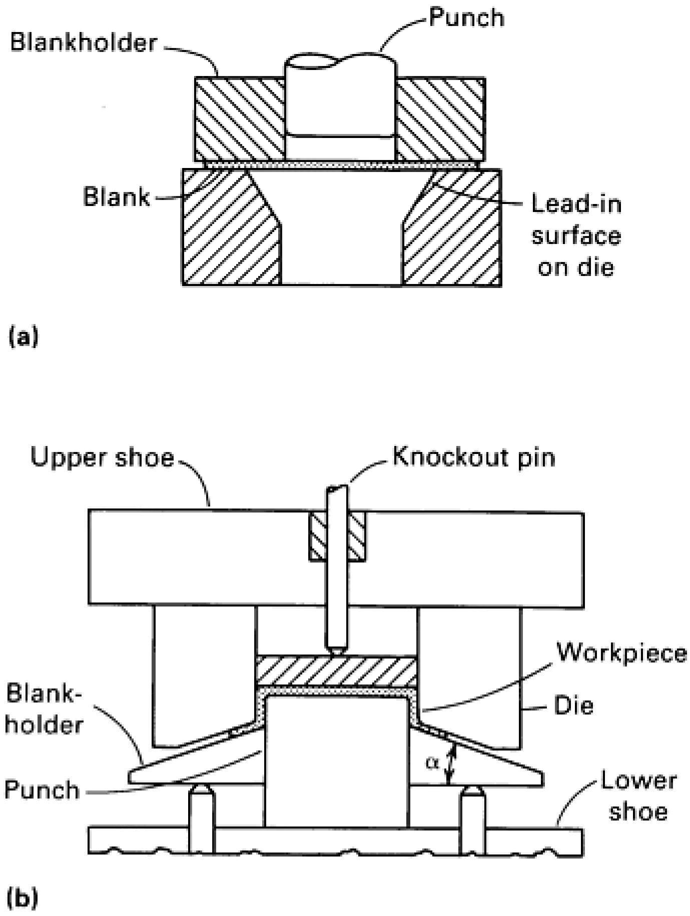


Fig. 9 Two ways of reducing the required drawing forces. (a) Conical lead-in die. (b) Conical blankholder. See text for details.

If the punch nose radius can be increased from one to five times metal thickness, the load in the sidewall of the shell will decrease so that the reduction in blank diameter will increase from 35% to about 50% (for steel). The shell can therefore be drawn deeper before the sidewall tears.

If the shell bottom radius is less than four times the sheet thickness, it is usually desirable to form it with a larger-radius punch and then to restrike to develop the specified radius. This will minimize bottom failures. However, the bottom corner radius usually cannot be increased beyond ten times the sheet thickness without the likelihood of wrinkling. The metal in dome-shaped parts is likely to pucker in the unconfined area between the punch nose and die radius. High blankholding forces or draw beads are often used to induce combined stretching and drawing of the metal when forming dome shapes.

The deep drawing of stainless steel or high-strength alloy boxes with sides longer than 50 times stock thickness may result in a stability problem called oil canning. The deflection of the sides by snap action can be eliminated by drawing the part in two operations with slightly different punches and an intermediate anneal. The first-draw punch will have a larger nose radius than the second; therefore, in the second drawing operation, the metal can be stretched to eliminate the oil-canning effect. Stretching of the metal in parts with long sidewalls can be improved by gradually increasing the punch nose radius from the corner toward the center. A constant nose radius is used on the second-draw punch.

Effect of Punch-to-Die Clearance

The selection of punch-to-die clearance depends on the requirements of the drawn part and on the work metal. Because there is a decrease and then a gradual increase in the thickness of the metal as it is drawn over the die radius, clearance per side of 7 to 15% greater than stock thickness (1.07 to 1.15t) helps prevent burnishing of the sidewall and punching out of the cup bottom.

The drawing force is minimal when the clearance per side is 15 to 20% greater than stock thickness (1.15 to 1.20t) and the cupped portions of the part are not in contact with the walls of the punch and die. The force increases as the clearance decreases, and a secondary peak occurs on the force-stroke curve where the metal thickness is slightly greater than the clearance and where ironing starts.

Redrawing operations require greater clearance, in relation to blank thickness, than the first draw in order to compensate for the increase in metal thickness during cupping. A sizing redraw is used where the diameter or wall thickness is important or where it is necessary to improve surface finish to reduce finishing costs. The clearance used is less than that for the first draw.

Table 1 lists clearances for cupping, redrawing, and sizing draws of cylindrical parts from metal of various thicknesses. As the tensile strength of the stock decreases, the clearance must be increased.

Table 1 Punch-to-die clearance for drawing operations

Metal thickness, *t*		Clearance-to-metal-thickness relationship for:		
mm	**in.**	**Cupping**	**Redrawing**	Sizing draws
Up to 0.38	Up to 0.015	1.07-1.09t	1.08-1.10t	**1.04-1.05*t***
0.41-1.27	0.016-0.050	1.08-1.10t	1.09-1.12t	**1.05-1.06*t***

1.29-3.18	0.051-0.125	1.10-1.12*t*	1.12-1.14*t*	**1.07-1.09*t***
3.2 and up	**0.126 and up**	**1.12-1.14*t***	**1.15-1.20*t***	**1.08-1.10*t***

Clearance between the punch and die for a rectangular shell, at the sidewalls and ends, is about the same as, or slightly less than, that for a circular shell. Clearance at the corners may be as much as 50% greater than stock thickness to avoid ironing in these areas and to increase drawability.

Restraint of Metal Flow

Even in the simplest drawing operation, as shown in Fig. 4(a), the thickness of the work metal and the die radius offer some restraint to the flow of metal into the die. For drawing all but the simplest of shapes, some added restraint is generally required in order to control the flow of metal. This additional restraint is usually obtained by the use of a blankholder, as illustrated in Fig. 4(b) and 4(c). The purpose of the blank-holder is to suppress wrinkling and puckering and to control the flow of the work metal into the die.

Drawing Without a Blankholder. A blank is not susceptible to wrinkling, and a blankholder need not be used, if the ratio of supported length to sheet thickness is within certain limits. In Fig. 10, the supported length *l* is the length from the edge of the blank to the die cavity (point of tangency). The sheet thickness is denoted as *t*. The *l*/*t* ratio is influenced little by other geometrical conditions, and it differs little for the various metals commonly drawn. When the *l*/*t* ratio does not exceed 3 to 1, a cup can be drawn from annealed brass, aluminum (to half-hard), and low-carbon steel without a blankholder. For slightly harder work metals, such as hard copper or half-hard brass, this ratio should not exceed 2.5 to 1.

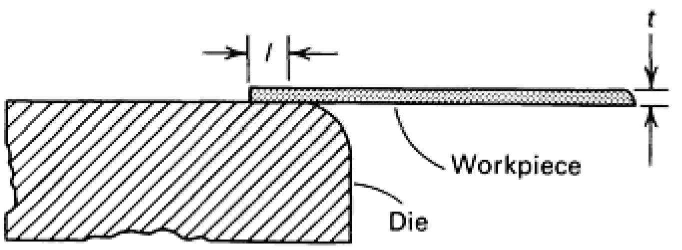


Fig. 10 The ratio of supported length *l* to sheet thickness *t* determines whether or not a blankholder is required for deep drawing.

An elliptical or conical die opening, such as that shown in Fig. 9(a), can be used where the die radius required to draw the part reduces the length of the blank-supporting surface to less than three times stock thickness. The distance between the die opening and the punch should not exceed ten times stock thickness.

A 30° elliptical radius derived from a circle created by a given draw radius increases the strain on the metal being drawn by 4.2%, but it decreases the metal out of control by 47% of the length of the original draw radius. This shape has been helpful in the drawing of tapered shells from a flat blank. For these draws, it is desirable to increase the strain slightly to prevent puckers and to reduce the metal out of control for the same reason.

A 45° elliptical radius derived as above reduces the strain on the metal being drawn by 1.03% and reduces the metal out of control by 33% of the length of the original draw radius. The 45° ellipse is useful only when a large radius will draw the part, but produces wrinkles. A smaller radius will not permit the draw.

A 60° elliptical radius does not measurably reduce drawing strain and accounts for only a 9% reduction of metal out of control. Its use on draw dies is not economically feasible when the small gains derived are considered in relation to the cost of producing the contour. The drawing of thick metal without a blankholder is frequently done when the blank diameter is no greater than 20 times stock thickness.

Blankholders. The purpose of a blankholder is to prevent wrinkles from forming in the flange of a part during drawing. The formation of wrinkles interferes with, or prevents, the compressive action that rear-ranges the metal from flange to sidewall. Much greater reductions are possible when a blankholder is used.

Blankholders can be used in double- and single-action presses. In a double-action press, the blankholder advances slightly ahead of the punch and dwells at the bottom of its stroke throughout the drawing phase of the punch cycle. The blankholder dwell usually extends to a point on the punch upstroke at which positive stripping of the shell is ensured. By using a die cushion and an inverted die, similar action can be obtained in a single-action press. A die cushion in a double-action press supports the blank and holds it against the punch during the drawing operation; it then lifts the finished part out of the die.

A blankholder must allow the work metal to thicken as the edge of the blank moves inward toward the working edge of the die. The amount of thickening is expressed by:

$$\frac{t_1}{t} = \sqrt{\frac{D}{D_1}} \qquad \textbf{(Eq 8)}$$

where t is the blank thickness, t_1 is the thickness of the flange at any instant during the drawing operation, D is the blank diameter, and D_1 is the diameter of the flange at any instant during the drawing operation (or the mean diameter of the workpiece without the flange). As the metal flows, paths of least resistance are taken; therefore, the actual value of t_1, will be less than that calculated from the formula.

Types of Blankholders. The simplest type of blankholder is fixed to the die block and has a flat hold-down surface, as shown in Fig. 11(a). A disadvantage of this type of blankholder is that maintenance of the optimal gap between the die surface and the flat hold-down surface requires careful adjustment. As shown in Fig. 11, the blankholder does not quite contact the work metal as drawing begins; restraint begins and increases as the flange portion thickens. A gap that is either too small or too large increases force and reduces drawability. For optimal results, the gap should be slightly smaller than the flange thickness, allowing 50 to 75% of the final thickening before the work metal contacts the blankholder.

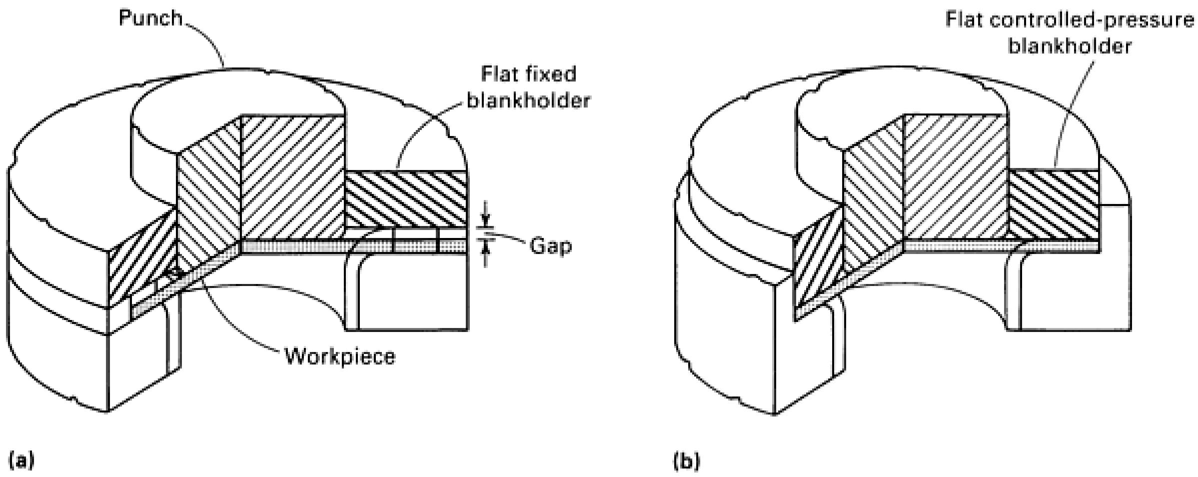


Fig. 11 Setups showing the use of two types of blankholders. See text for details.

The flat controlled-pressure blankholder shown in Fig. 11(b) is generally preferred in production operations because it can be adjusted to a predetermined and closely controlled value by hydraulic or pneumatic pressure. Springs, unless extremely long, are not suitable for supplying pressure to a blankholder during deep drawing, because the force exerted by a spring increases rapidly as it is compressed. The force on hydraulic or pneumatic die cushions will increase about 20% when compressed the full stroke length. Some hydraulic systems have pressure control valves that supply a more nearly constant pressure during the entire stroke.

The fixed-type blankholder (Fig. 11a) draws a cup without a flange and ejects it through the bottom of the die. The blankholder shown in Fig. 4(b) and 4(c) and in Fig. 11(b) can be used for drawing a cup with or without a flange. Cups without a flange can be pushed through the die if a pressure pad is not needed to support the blank.

Blankholder Force. Compressive forces on the metal in the area beyond the edge of the die cause the work metal to buckle. If this buckled or wrinkled metal is pulled into the die during the drawing operation, it will increase the strain in the area of the punch nose to the point at which the work metal would fracture soon after the beginning of the draw. Blankholder force is used to prevent this buckling and subsequent failure. The amount of blankholder force required is usually about one-third that required for drawing. Thickness of the work metal must also be considered when simple shapes are being drawn; the thinner the work metal, the more blankholder force that is required.

There are no absolute rules for calculating blankholder force for a given drawing operation; most blankholder force values are found empirically. Blankholder force should be just sufficient to prevent wrinkling, and it depends on draw reduction, work metal thickness and properties, the type of lubrication used, and other factors. For a particular application, blankholder force is best determined experimentally.

Draw beads help prevent wrinkles and control the flow of metal in the drawing of shells. The use of draw beads increases the cost of tools, product development, and tool maintenance. However, they are often the only means of controlling metal flow in the drawing of odd shapes. Draw beads are ordinarily used for the first draw only; therefore, production rates are the same as when conventional blankholders are used. For low production, draw beads are often made by laying a weld bead on the die after the optimal location has been determined.

Restraint of the metal flow, to the extreme of locking the flange of the blank to prevent motion, is needed for some draws. A deep shell with sloping walls can be made by drawing, followed by several redraws. This results in a stepped workpiece. The final sizing draw is a stretching operation that is done with the flange secured by a locking bead in the blankholder. This kind of blankholder is also used in making shallow drawn panels. Additional information on the design and use of draw beads is available in the article "Press Forming of Low-Carbon Steel" in this Volume.

Effect of Press Speed

Drawing speed is usually expressed in meters per minute (m/min) or linear feet per minute (ft/min). Under ideal conditions, press speeds as high as 23 m/min (75 ft/min) are used for the deep drawing of low-carbon steel. However, 6 to 17 m/min (20 to 55 ft/min) is the usual range--up to 17 m/min (55 ft/min) for single-action presses and 11 to 15 m/min (35 to 50 ft/min) for double-action presses. Ideal conditions include:

- Use of a drawing-quality work metal
- Symmetrical workpieces of relatively mild severity
- Adequate lubrication
- Precision carbide tools
- Carefully controlled blankholding pressure
- Presses that are maintained to a high level of accuracy

When one or more of the above conditions is less than ideal, some reduction in press speed is required. If all, or nearly all, are substantially less than ideal, press speed may have to be reduced to 6 m/min (20 ft/min). When the operation includes ironing, the drawing speed is usually reduced to about 7.6 m/min (25 ft/min) regardless of other factors.

The punch speed in hydraulic presses is relatively constant throughout the stroke. In mechanical presses, punch speed is that at mid-stroke because the velocity changes in a characteristic manner throughout the drawing stroke from maximum velocity to zero. The only adjustment in speed that can be made is to decrease flywheel speed or to use a press with a shorter stroke that operates at the same number of strokes per minute. This proportionately decreases maximum punch speed.

Speed is of greater significance in drawing stainless steels and heat-resistant alloys than in drawing softer, more ductile metals. Excessive press speeds have caused cracking and excessive wall thinning in drawing these stronger, less ductile metals. Nominal speeds for drawing various metals are given in Table 2.

Table 2 Typical drawing speeds for various materials

Material	Drawing speed	
	m/min	ft/min
Aluminum	45.7-53.3	**150-175**
Brass	53.3-61	**175-200**
Copper	38.1-45.7	**125-150**
Steel	5.5-15.2	**18-50**
Stainless steel	9.2-12.2	**30-40**

Effect of Lubrication

When two metals are in sliding contact under pressure, as with the dies and the work metal in drawing, galling (pressure welding) of the tools and the work metal is likely. When extreme galling occurs, drawing force increases and becomes unevenly distributed, causing fracture of the workpiece.

The likelihood of pressure welding depends on the amount of force and the work metal composition. Some work metals are more "sticky" than others. For example, austenitic stainless steel is more likely to adhere to steel tools than low-carbon steel is.

Lubricants are used in most drawing operations. They range from ordinary machine oil to pigmented compounds.

Selection of lubricant is primarily based on the ability to prevent galling, wrinkling, or tearing during deep drawing. It is also influenced by ease of application and removal, corrosivity, and other factors, as described in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

If a lubricant cannot be applied uniformly by ordinary shop methods, its purpose is defeated, regardless of its ability to prevent pressure welding. In general, as the effectiveness of a lubricant increases, the difficulty of removing it also increases. For example, grease or oil can be easily removed, but special procedures (frequently including some hand scrubbing) are required for removing lubricants that contain zinc oxide, lithopone, white lead, molybdenum disulfide, or graphite.

A lubricant is sometimes too corrosive for use on certain metals. For example, copper alloys are susceptible to staining by lubricants that contain large amounts of sulfur or chlorine compounds. Lubricants containing lead or zinc compounds are not recommended for drawing stainless steel or heat-resistant alloys, because the compounds, if not thoroughly removed, can cause intergranular attack when the work-pieces are heat treated or placed in high-temperature service. Suitable safety precautions are necessary with toxic or flammable lubricants.

Some metals, such as magnesium and titanium, are drawn at elevated temperature, which complicates selection of the lubricant. Most oil-base and soap-base lubricants can be successfully used to 120 °C (250 °F), but above this temperature, the choice narrows rapidly. Some special soap-base lubricants can be used on work metals to 230 °C (450 °F). Molybdenum disulfide and graphite can be used at higher temperatures.

Any lubricant must remain stable, without becoming rancid, when stored for a period of several months at various temperatures. The cost of application and removal of the lubricant, as well as its initial cost, must be considered because all of these items can add substantially to the cost of the drawn workpieces.

In some plants, when a new application is started, a heavily pigmented drawing lubricant is used, regardless of the difficulty of applying and removing it. Lubricant is then downgraded as much as possible to simplify the operation and to reduce costs. In other plants, the reverse of this practice is used; that is, a simple lubricant, such as machine oil, is used at first, and lubricant is then upgraded when necessary.

The difficulty of removing drawing lubricants is an important consideration in production operations. In a number of applications, changes in drawing techniques (such as increasing the number of draws) or in workpiece design (larger radii, for example) have been made solely to permit the use of an easier-to-remove drawing lubricant. Methods of removing lubricants are discussed in the section "Cleaning of Workpieces" in this article.

Typical lubricants used in drawing steel are given in Table 3 according to severity of draw or the percentage of reduction from blank to cup diameter. Zinc phosphate conversion coating of the steel to be drawn is helpful for any drawing operation, and the importance of phosphate coating increases as the severity of the draw increases. Methods of application and other details on the use of phosphate conversion coatings are discussed in the article "Phosphate Coatings" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Table 3 Lubricants commonly used for the drawing of low-carbon steel

Severity is indicated by the percentage of reduction in diameter in drawing a cylindrical shell.

Type or composition of lubricant	**Ease of removal by:**		Protection against rusting
	Water-base cleaners	**Degreasers or solvents**	
Water-base lubricants			
Low severity (10% or less)			
Water emulsion of 5-20% general-purpose soluble oil or wax	Very good	Good	**Fair**
Moderate severity (11-20%)			
Water solution of 5-20% soap	Very good	Very poor	**Fair**
Water emulsion of heavy-duty soluble oil (contains sulfurized or chlorinated additives)	Very good	Good	**Fair**
High severity (21-40%)			
Soap-fat paste, diluted with water (may contain wax)	Fair	Poor	**Fair**
Water emulsion of heavy-duty soluble oil (contains a high concentration of sulfurized or chlorinated additives)	Very good	Good	**Fair to poor**

Maximum severity (>40%)			
Pigmented soap-fat paste, diluted with water	Poor	Very poor	**Good**
Dry soap or wax (applied from water solution or dispersion); may contain soluble filler such as borax	Good	Very poor	**Good**
Oil-base lubricants			
Low severity (10% or less)			
Mill oil, residual	Good	Very good	**Fair**
Mineral oil	Good	Very good	**Fair**
Vanishing oil	Removal not required		**. . .**
Moderate severity (11-20%)			
Mineral oil plus 10-30% fatty oil	Good	Very good	**Fair**
Mineral oil plus 2-20% sulfurized or chlorinated oil (extreme-pressure oil)	Good to fair	Good	**Fair to poor**
High severity (21-40%)			
Fatty oil	Fair	Fair	**Fair**
Mineral oil plus 5-50% of:			
(a) Nonemulsifiable chlorinated oil	Poor	Good	**Very poor**
(b) Emulsifiable chlorinated oil	Good	Good	**Very poor**
Concentrated phosphated oil	Fair	Fair	**Fair**
Maximum severity (>40%)			
Blend of pigmented soap-fat paste with mineral oil	Poor	Poor	**Fair**
Concentrated sulfochlorinated oil (may contain some fatty oil):			

(a) Nonemulsifiable	Very poor	Fair	**Poor**
(b) Emulsifiable	Good	Fair	**Poor**
Concentrated chlorinated oil:			
(a) Nonemulsifiable	Very poor	Fair	**Very poor**
(b) Emulsifiable	**Good**	**Fair**	**Very poor**

Materials for Deep Drawing

Sheet steels and other sheet metals with higher strengths and better formability have recently become available. Developments such as vacuum processing and inclusion shape control have been especially beneficial in increasing the drawability of steels. Other metals and alloys that can be deep drawn include aluminum and aluminum alloys, copper and alloys, some stainless steels, and titanium.

Low-carbon sheet steels are the materials that are most commonly deep drawn and are commonly used, for example, in the automotive industry. Materials such as 1006 and 1008 steel have typical yield strengths in the range of 172 to 241 MPa (25 to 35 ksi) and elongations of 35 to 45% in 50 mm (2 in.). These materials have excellent formability and are available cold or hot finished in various quality levels and a wide range of thicknesses. Table 4 lists mechanical properties of the various qualities of carbon steel sheet.

Table 4 Typical mechanical properties of low-carbon sheet steels

Quality level	**Tensile strength**		**Yield strength**		**Elongation, % in 50 mm (2 in.)**	**Plastic-strain ratio, *r***	**Strain-hardening exponent, *n***	Hardness, HRB
	MPa	**ksi**	**MPa**	**ksi**				
Hot rolled								
Commercial quality	358	52	234	34	35	1.0	0.18	**58**
Drawing quality	345	50	220	32	39	1.0	0.19	**52**
Drawing quality, aluminum killed	358	52	234	34	38	1.0	0.19	**54**
Cold rolled, box annealed								
Commercial quality	331	48	234	34	36	1.2	0.20	**50**
Drawing quality	317	46	207	30	40	1.2	0.21	**42**

Drawing quality, aluminum killed	303	44	193	28	42	1.5	0.22	**42**
Interstitial-free	**310**	**45**	**179**	**26**	**45**	**2.0**	**0.23**	**44**

Other low-carbon steels that are commonly deep drawn are grades 1010 and 1012. These materials are slightly stronger than 1006 and 1008 and are slightly less formable. They are often specified when drawing is not severe and strength of the finished part is of some concern.

Grain size affects the drawability of these materials, and it may affect the selection of a grade. Grain sizes of ASTM 5 or coarser may result in excessive surface roughness as well as reduced drawability.

Surface finish also influences drawability. The dull finish normally supplied on drawing steels is designed to hold lubricants and to improve drawability. Brighter finishes may be required if, for example, parts are to be electroplated.

Effects of Materials Variables

Anisotropy. As mentioned earlier in this article, there are two types of anisotropy that must be considered: planar anisotropy, in which properties vary in the plane of the sheet, and normal anisotropy, in which the properties of the material in the thickness direction differ from those in the plane of the sheet.

Planar anisotropy (variations in normal anisotropy in the plane of the sheet) causes undesirable earing of the work material during drawing. Between the ears of the cup are valleys in which the material has thickened under compressive hoop stress rather than elongating under radial tensile stress. This thicker metal sometimes forces the die open against the blankholder pressure, allowing the metal in the relatively thin areas near the ears to wrinkle. Die design, draw reduction, and type of lubricant used all affect earing (see the section "Effects of Process Variables" in this article).

Sheet Thickness. In deep drawing, the pressure on the dies increases proportionally to the square of sheet thickness. The pressure involved is concentrated on the draw radius, and increasing sheet thickness will localize wear in this area without similar effect on other surfaces of the die.

Thick stock has less tendency to wrinkle than thin stock. As a result, blankholder pressures used for the drawing of thick sheet may be no greater, and may even be less, than those used for thinner blanks.

Direct Redrawing

In direct redrawing in a single-action die, the drawn cup is slipped over the punch and is loaded in the die, as shown in Fig. 12. At first, the bottom of the cup is wrapped around the punch nose without reducing the diameter of the cylindrical section. The sidewall section then enters the die and is gradually reduced to its final diameter. Metal flow takes place as the cup is drawn into the die so that the wall of the redrawn shell is parallel to, and deeper than, the wall of the cup at the start of the redraw. At the beginning of redrawing, the cup must be supported and guided by a recess in the die or by a blankholder to prevent it from tipping, because tipping would result in an uneven shell.

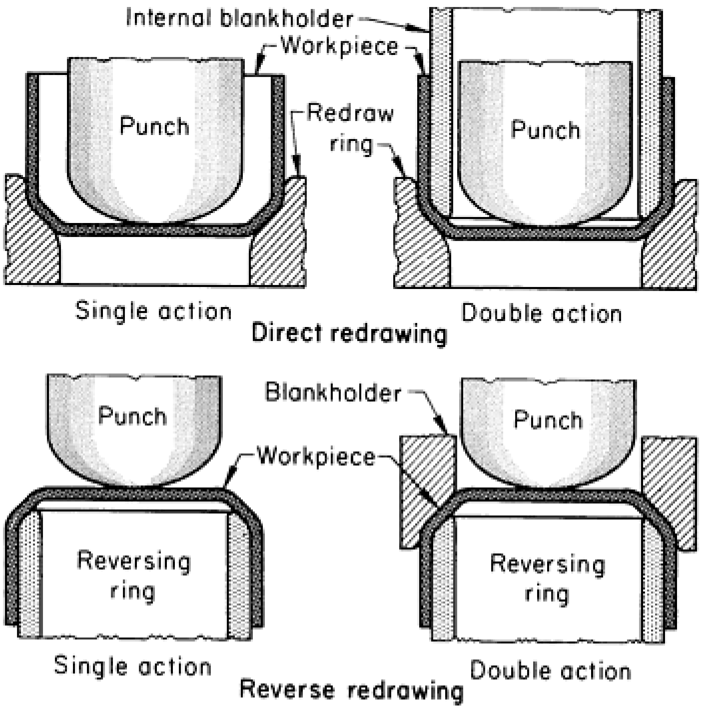


Fig. 12 Direct and reverse redrawing in single-action and double-action dies

In a single-action redraw, the metal must be thick enough to withstand the compressive forces set up in reducing the cup diameter without wrinkling. Wrinkling can be prevented by the use of an internal blankholder and a double-action press (upper right, Fig. 12), which usually permits a shell to be formed in fewer operations than by single-action drawing without the use of a blankholder.

Internal blankholders (Fig. 13) are slip fitted into drawn shells to provide support and to prevent wrinkling during direct redrawing. The blankholder presses on the drawn shell at the working edge of the die before the punch contacts the bottom of the shell and begins the redraw. It dwells against the shell as the metal is drawn into the die by the punch, preventing wrinkles.

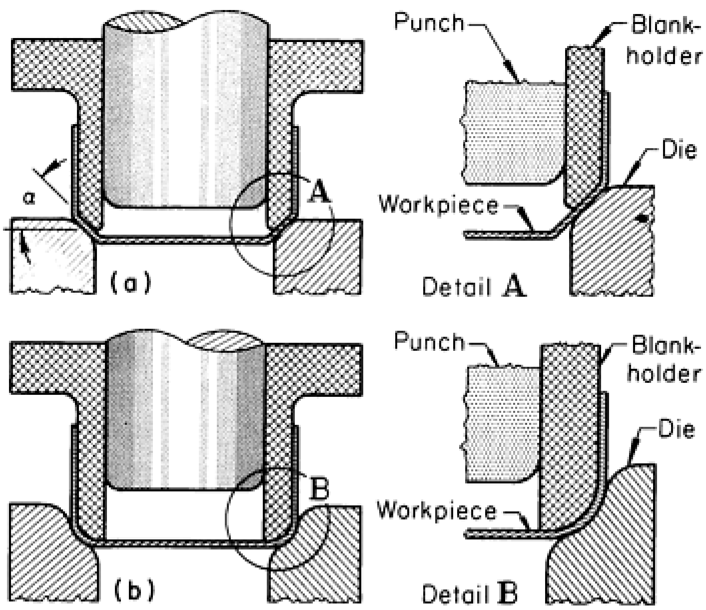


Fig. 13 Setups using internal blankholders for restraint of work metal in redrawing shells. See text for details.

The bottom of the cup to be redrawn can be tapered (Fig. 13a) or radiused (Fig. 13b), with the tip of the blankholder and the mouth of the die designed accordingly. An angle of 30° is used for metal thinner than 0.8 mm ($\frac{1}{32}$ in.), and 45° is used for thicker work metal. A modification of the above is a blankholder fitted against an S-curve die (Fig. 13b). The main disadvantage of an S-curve die is that it is more expensive to make and maintain. Near the bottom of a redrawn shell, there is usually a narrow ring, caused by the bottom radius of the preceding shell, that is thinner and harder than the adjacent metal. Redrawing may be required for reasons other than the severity of the drawn shape--for example, to prevent thinning and bulging.

Redrawing can also be done in a progressive die while the part is still attached to the strip. Where space permits the extra stations, the amount of work done in each station will be less than that done in a single die. This reduces the severity of the draw and promotes high-speed operation.

Reverse Redrawing

In reverse redrawing, the cupped workpiece is placed over a reversing ring and redrawn in the direction opposite to that used for drawing the initial cup. As shown in the two lower views in Fig. 12, reverse redrawing can be done with or without a blankholder. The blankholder serves the same purposes as in direct redrawing.

The advantages of reverse redrawing as compared with direct redrawing include:

- Drawing and redrawing can be accomplished in one stroke of a triple-action hydraulic press, or of a double-action mechanical press with a die cushion, which can eliminate the need for a second press
- Greater reductions per redraw are possible with reverse redrawing
- One or more intermediate annealing operations can often be eliminated by using the reverse technique
- Better distribution of metal can be obtained in a complex shape

In borderline applications, annealing is required between redraws in direct redrawing, but is not needed in reverse redrawing.

The disadvantages of reverse redrawing are:

- The technique is not practical for work metal thicker than 6.4 mm ($\frac{1}{4}$ in.)
- Reverse redrawing requires a longer stroke than direct redrawing

Usually, metals that can be direct redrawn can be reverse redrawn. All of the carbon and low-alloy steels, austenitic and ferritic stainless steels, aluminum alloys, and copper alloys can be reverse redrawn.

Reverse redrawing requires more closely controlled processing than direct redrawing does. This control must begin with the blanks, which should be free from nicks and scratches, especially at the edges.

The restraint in reverse redrawing must be uniform and low. For low friction, polished dies and effective lubrication of the work are needed. Friction is also affected by hold-down pressure and by the shape of the reversing ring. Radii of tools should be as large as practical--ten times the thickness of the work metal if possible. Reverse redrawing can be done in a progressive die as well as in single-stage dies if the operations are divided to distribute the work and to reduce the severity of each stage.

Tooling for Redrawing

Tooling for redrawing depends mainly on the number of parts to be redrawn and on available equipment. In continuous high production, a complete die is used for each redraw; the workpieces are conveyed from press to press until completed. In low or medium production, it is common practice to use a die with replaceable draw rings and punches. A die of this type used for three redrawing operations is shown in Fig. 14; the three redraws were made by changing to successively smaller draw rings and punches. The cup was drawn in a compound blank-and-draw die from a blank 1.7 mm (0.067 in.) thick and 171 mm ($6\frac{3}{4}$ in.) in diameter.

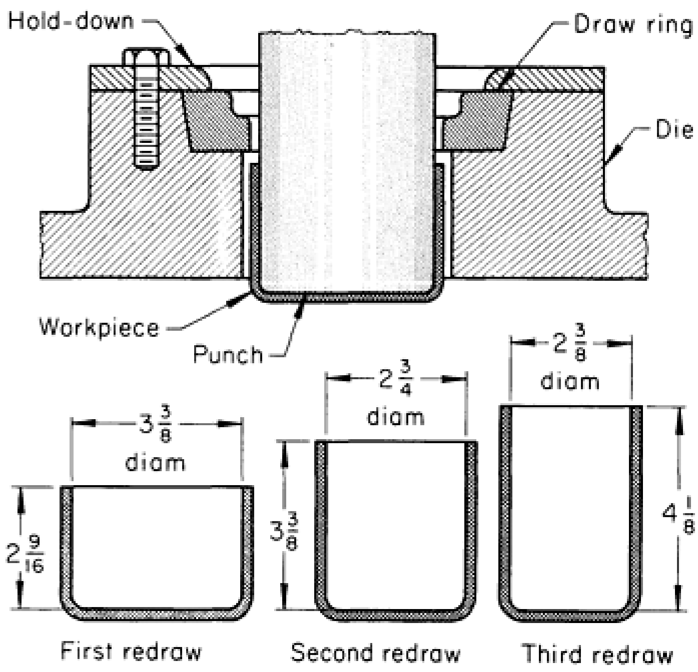


Fig. 14 Die in which draw rings and punches can be replaced for producing three successive redraws. Dimensions given in inches

Drawing of Boxlike Shells

Square or rectangular shells can be formed by redrawing circular shells when there is no flange. When flanges are required, the difficulty of producing acceptable boxlike shapes by drawing is increased. For deep-drawn square or rectangular shells (for example, where the depth is greater than either length or width), the best approach for forming a narrow flange is to allow sufficient stock and to form the flange after redrawing from a cylindrical shell.

Shallower boxlike shapes (for example, with proportions similar to the box illustrated in Fig. 15) can be drawn with a flange, which is then trimmed to the desired width. Calculations for the area of a blank used for a circular workpiece cannot be used for a square or rectangular box. These require metal in the bottom, ends, sides, and flange, as shown when a box is unfolded (flat pattern, Fig. 15). The excess metal at the corners (shaded areas, Fig. 15) is a problem.

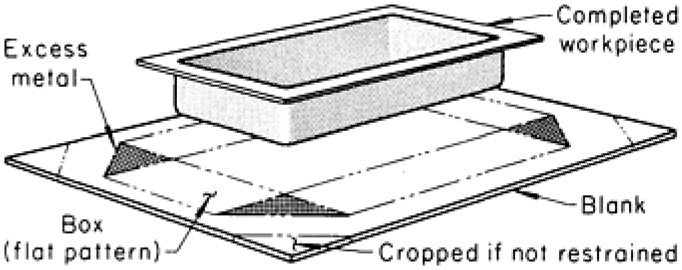


Fig. 15 Flanged rectangular box drawn from a blank with restraint at the corners. See text for details.

A seamless square or rectangular shell is made by drawing metal into the corners. The metal not needed for the corners is pushed into the walls adjacent to the corner radius and into earlike extensions of the corners. The compressive stresses set up when the metal in the corners is rearranged cause the metal to be thicker in the corners than in the sidewall or in the original blank.

The more difficult draws are made more easily by using a carefully developed blank. There are methods of developing the shape at the corners of a blank for a square or rectangular shell so that there is a minimum of excess metal. However, by cropping the corners as shown in Fig. 15 and by using a blankholder, satisfactory parts can generally be made. Draw beads in the blankholding surface surrounding the die are frequently used.

Drawing of Workpieces With Flanges

Regardless of whether the drawn workpiece is circular, rectangular, or asymmetrical, producing acceptable small-width flanges on workpieces is seldom a problem. Flanged workpieces are usually drawn in two or more operations, frequently with restriking as a final operation.

Cylindrical workpieces with wide flanges are troublesome to draw because of excessive wrinkling or fracturing in the sidewall due to lack of metal flow. Even though the metal is restrained by a blankholder, it is difficult to obtain acceptable flatness without special procedures.

Wide flanges on relatively large workpieces can be made flat by coining after drawing. Another means of dealing with wrinkling, when design permits, is to provide ribs in the flange. This controls the wrinkling by allowing space for the excess metal. Ribs are usually spaced radially around the flange, although circular, concentric ribs also are effective.

Rectangular, boxlike workpieces that have flanges are difficult to redraw in such a manner that the flange is unaffected in redrawing operations. Therefore, it is common practice to draw the part first to a shallower depth and with larger bottom radii than needed for shaping the final contour. The part is then re-formed in a final operation.

Asymmetrical workpieces that have flanges are often difficult to draw, particularly when neither draw beads in the die nor ribs in the workpiece can be permitted. Under these conditions, considerable development is usually required to determine the blankholder pressure that will result in the desired metal flow without using a larger blank than necessary.

Drawing of Hemispheres

In the drawing of a hemisphere, metal flow must be closely controlled for balance between excessive thinning in one area and wrinkling in another. In the top illustration in Fig. 16, the punch has begun to stretch the round blank, which is restrained by the blankholder, and the crown section of the hemisphere is being formed. At this stage, the crown section is subjected to biaxial tension, which results in metal thinning. With correct pressure on the blankholder, thinning is in the range of 10 to 15%. More than 15% thinning is likely to result in fracture of the crown section. In the top illustration in Fig. 16, the portion of the blank under the blankholder has not begun to move.

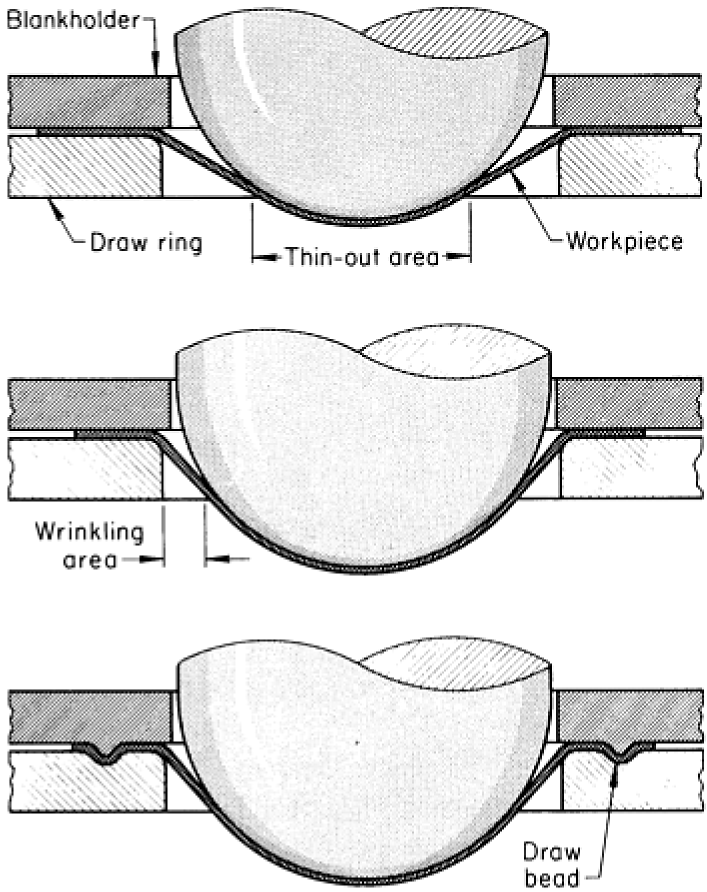


Fig. 16 Drawing of a hemisphere with and without a draw bead. See text for more information.

As the drawing operation continues, the metal begins to move from the blankholder, and a different problem develops (center illustration, Fig. 16). Here the metal has been drawn into a partial hemisphere with unsupported metal in a tangential slope between the punch and the clamped surface. Unlike the drawing of straight-sided shapes, the wide gap (wrinkling area, Fig. 16) prevents the use of the draw ring bore as the means of forcing the metal against the punch surface; therefore, the probability of wrinkling increases. Because the metal cannot be confined between the punch and die, wrinkling is likely to occur in this area.

To prevent wrinkles, the metal must flow from the flange area and, at the same time, must be securely held in tension. This requires an additional stretching force, derived from the portion of the blank that remains clamped. The area of metal between the clamping surfaces is gradually reduced as the punch advances, but the draw radius offers some resistance because the metal must follow a sharper bend as it moves into the die.

One means of controlling wrinkling is by the use of draw beads, as shown in the bottom illustration in Fig. 16. Another means is by a sharp draw radius. Small radii are susceptible to metal pickup and, depending on sharpness, can produce undesirable circumferential grooves in the hemisphere if the punch does not move at a steady rate.

Reducing Drawn Shells

Necking and nosing are used for reducing the diameter of a drawn cup or shell for a part of its height.

Necking. By the die reduction method, the work metal is forced into compression, resulting in an increase in length and wall thickness. The thicknesses of a shell before and after necking are related by:

$$t_2 = t_1 \sqrt{\frac{d_1}{d_2}} \quad \textbf{(Eq 9)}$$

and heights before and after necking by the formula:

$$h_2 = h_1 \sqrt{\frac{d_1}{d_2}} \quad \textbf{(Eq 10)}$$

where t_1 is the shell thickness before necking, t_2 is the shell thickness in the necked area after necking, d_1 is the mean diameter of the shell before necking, d_2 is the mean diameter after necking, h_1 is the unit of height before necking, and h_2 is the unit of height after necking.

Figure 17 shows the flow of metal in a necking operation. As the metal flows, paths of least resistance are taken. Therefore, the actual values for t_2 will be less, and for h_2 greater, than those calculated from Eq 9 and 10.

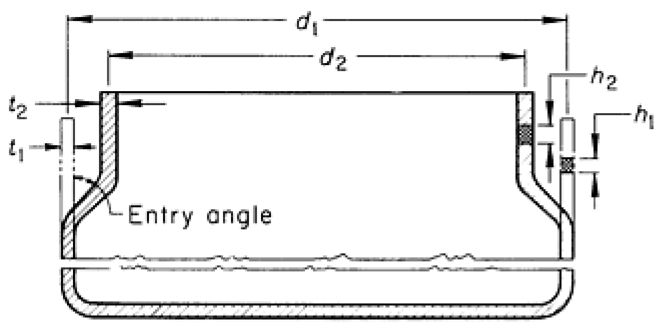


Fig. 17 Flow of metal in the reduction of a drawn shell by necking. See text for details.

Necking results are uniformly better if the workpiece has been slightly cold worked. This provides added strength to resist bulging in the column section and buckling in the section being reduced. The entry angle on the necking die is important because the probability that the metal will collapse is decreased as the angle with the vertical becomes smaller. This angle should be less than 45° (Fig. 17). If the angle is greater than 45°, a series of reductions may be necessary with localized annealing between reducing operations. With a die entry angle less than 45°, thin-wall tubes can be reduced as much as 15% in diameter; thick-wall tubes can be reduced as much as 20%.

Nosing reduces the open end of a shell by tapering or rounding the end (usually by cold reduction) and is primarily used in making ammunition. Shells are often machined before, instead of after, nosing. Shells are usually cold reduced as much as 30% of their original diameter by nosing.

Ironing is the intentional reduction in wall thickness of a shell by confining the metal between the punch and the die wall. When ironing occurs, the force needed to displace the punch often increases to a secondary maximum in the force-displacement curve. The second force maximum can be of such magnitude that the shell will break. However, after ironing has started and metal has been wrapped around the punch, the force is uniform and frequently less than that for redrawing operations.

Ironing is seldom used with redrawing operations unless the amount of wall thinning is relatively small, because it results in excessive die wear, causes workpiece breakage, and increases press tonnage requirements. If a shell with constant wall thickness is needed, however, it can be obtained only by ironing.

Deep Drawing

Expanding Drawn Workpieces

There are several methods for expanding portions of drawn workpieces in a press. Because the wall thickness is reduced during expansion, it is not advisable to increase the diameter for ductile metal shells (such as low-carbon steel or copper) more than 30%. If a diameter increase of more than 30% is required, the operation should be done in two or more stages, with annealing between stages.

Expanding With a Punch. In expanding with a punch, as in Fig. 18, the portion to be expanded is first annealed. Localized annealing, instead of annealing the entire cup, helps retain strength in the remainder of the cup. Regardless of whether or not the strength is required in the finished part, maximum column strength is desirable to prevent buckling as the punch enters the cup.

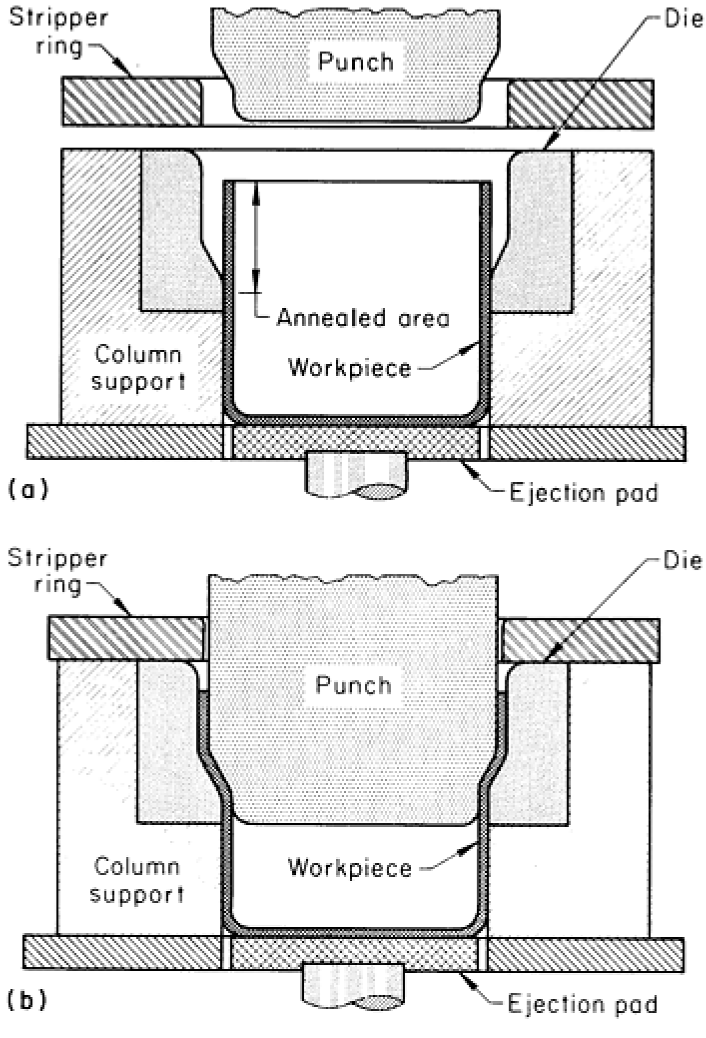


Fig. 18 Expansion of the mouth of a drawn shell with a punch.

After the cup has been placed in the die (Fig. 18a), the punch moves downward and expands the top of the cup (Fig. 18b). During the return stroke, the workpiece is stripped from the punch by the stripper ring and is ejected from the die by the ejection pad.

In an expanding operation of this kind, die dimensions are predetermined within reasonably close limits during the design stage. However, the possibility of later design changes must always be considered. Depending on the shape and location of the expanded section, a height reduction of the cup may occur that will require some modification of the die and punch after tryout.

Expanding with segmented dies is often used for forming sidewalls of drawn shells or sections of tubing. The forming segments are contracted by compression springs and expanded radially by a tapered punch. The die is made of two or more segments held apart by compression springs. As the press ram descends, cams move the die segments together. The punch then moves the inner segments outward, thus forming the contours in the sidewall. The presence of gaps between the forming segments is one of the disadvantages of this method and is the reason an alternative method, such as rubber-pad forming, is sometimes selected.

Deep Drawing of Pressure Vessels

Various grades of steel--many of them high-strength alloys--are deep drawn to make cylinders for compressed gases. Joints (when they are made) are around the girth of the vessel, rather than longitudinal. The integrity of the vessel is critical. Commercial-quality hot-rolled steels in the as-rolled condition are generally used. The work metal is usually induction heated or induction annealed to minimize scale.

For propane gas, pressure tanks must have high strength at minimum weight. In one application, the weight of such a tank was reduced from 59 to 32 kg (130 to 70 lb) by changing from 1025 steel to a high-manganese deep-drawing steel (Fe-0.2C-1.6Mn-0.025P-0.3S). Before drawing, the high-manganese steel had a minimum yield strength of 345 MPa (50 ksi) and a minimum tensile strength of 483 MPa (70 ksi).

Bottles for dispensing small quantities of liquefied gases or gases under high pressure are commonly made of drawing quality low-carbon steel to take advantage of the improved mechanical properties produced by deep drawing. The bottles range in size from 12.7 mm ($\frac{1}{2}$ in.) in diameter by 32 mm ($1\frac{1}{4}$ in.) long to 38 mm ($1\frac{1}{2}$ in.) in diameter by 152 mm (6 in.) long.

Deep Drawing Using Fluid-Forming Presses

Fluid forming (also termed hydroforming) is a deep drawing process that uses only one solid die half. Forming pressure is applied by the action of hydraulic fluid against a flexible membrane, which forces the blank to assume the shape of the rigid tool.

Fluid forming can be used for deep drawing and in fact offers advantages over other forming methods. One of these is that the draw radius can be varied by changing the pressure of the hydraulic fluid during the forming operation. This makes it possible to have, for example, a large draw radius at the start of the operation that decreases as the draw continues. Thus, reductions of up to 70% in a single pass are possible when drawing cylindrical cups; for rectangular shaped parts, a height of six to eight times the corner radius can be obtained in a single operation.

Presses for fluid forming sometimes use a telescoping ram system. Figure 19 shows a schematic of one type of press used for fluid forming. Figure 20 illustrates the deep drawing process on a press of this type. More information on fluid-forming equipment and processes is available in the article "Rubber-Pad Forming" in this Volume.

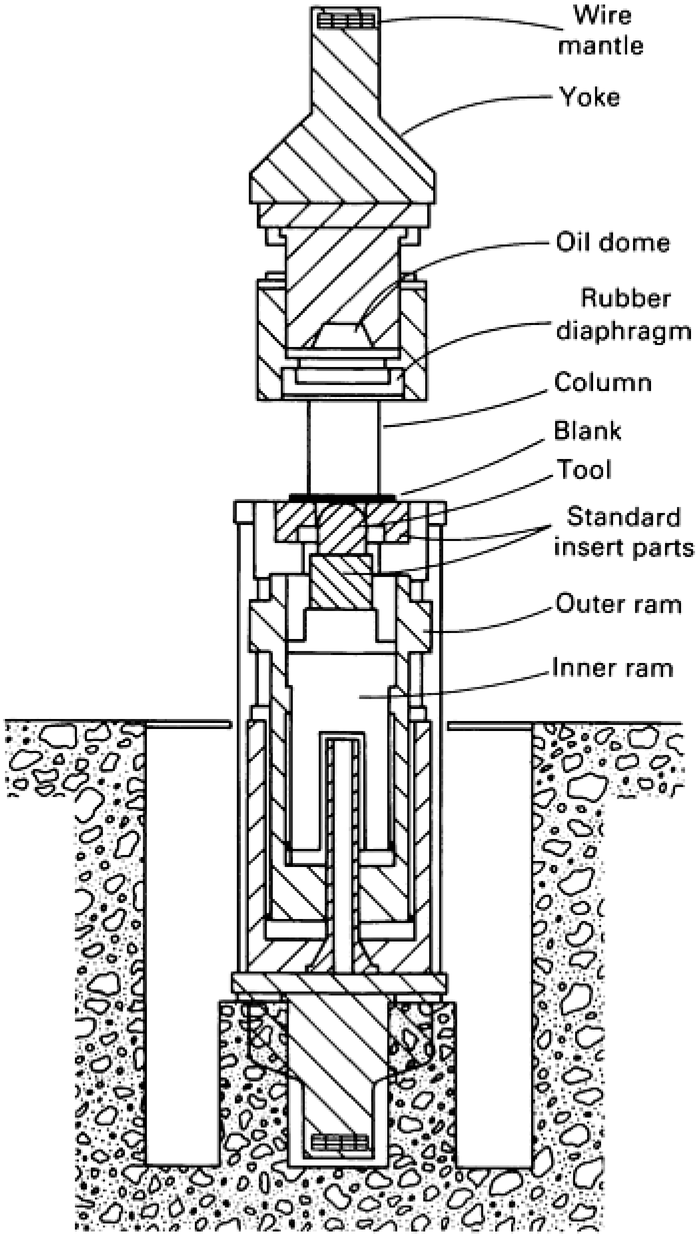


Fig. 19 Schematic of one type of fluid-forming press used for deep drawing.

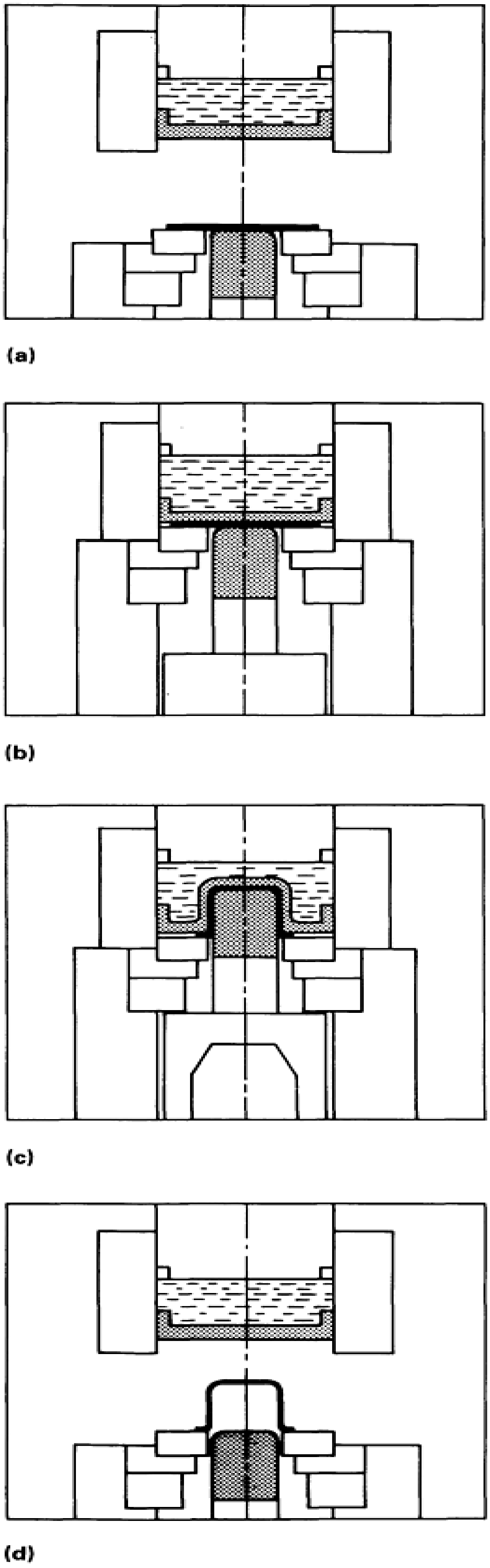


Fig. 20 Deep-drawing process using the fluid-forming press shown in Fig. 19. (a) The blank is placed on the blankholder. (b) The outer ram moves upward, carrying the blank. (c) Oil is pumped into the inner ram system, pressing the punch upward. (d) Outer ram is returned to its initial position, and the punch is retracted to allow removal of the formed part.

Ejection of Workpieces

In drawing operations, the drawn workpiece may adhere to either the punch or the die. Adherence is increased by depth of draw, straightness of workpiece walls, and viscosity of lubricant. The simplest means of ejecting a small workpiece is by compressed air through jets in the punch or the die. Timed air blast is widely used for ejecting relatively small workpieces--for example, where cup diameter is no greater than 102 to 127 mm (4 to 5 in.). In some production drawing operations, the workpiece is ejected by compressed air, and another timed blast of air from the side removes the piece by sending it down a chute or into a container. However, for larger workpieces or for those that are deep, some other means of ejection is required.

Mechanical methods of ejection include:

- Edge stripping by means of a lip on the draw ring (Fig. 21a) or by a spring-actuated stripper (Fig. 21b)
- The use of a blankholder in combination with an upper ejector (Fig. 22)
- The use of a lower ejector in combination with an upper stripper ring (Fig. 23)

Numerous other mechanical methods using cams or links have been devised to meet specific requirements. These methods are usually modifications of those described above. For example, thin shells are sometimes stripped from punches near the top of the press stroke by a cam-actuated rod that extends through the punch. This method is often used to avoid damage to the open end of the shell, which can occur when the piece is ejected by other methods. The major factors influencing the method of ejection are workpiece design (especially the presence or absence of a flange), work metal composition and thickness, and the type of equipment available.

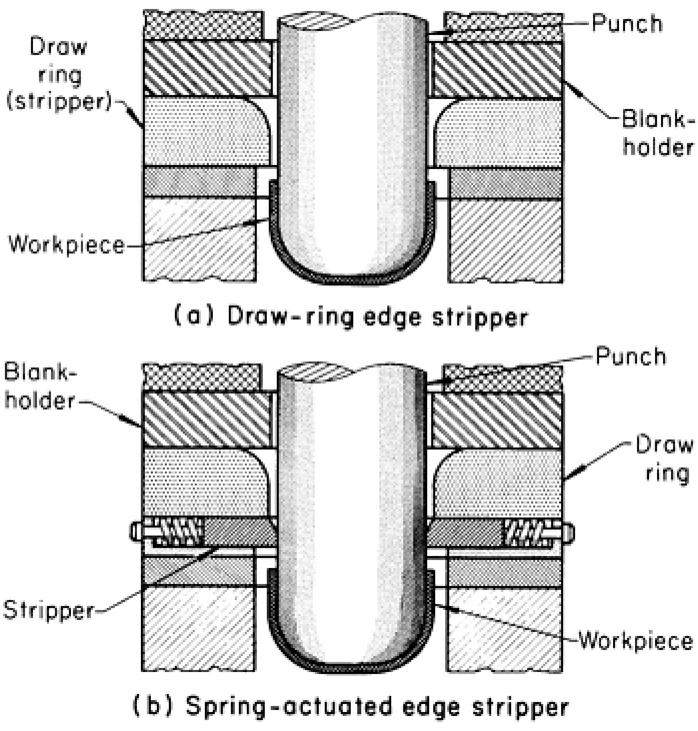


Fig. 21 Setups showing two methods of ejecting a drawn workpiece through the bottom of the press by edge stripping.

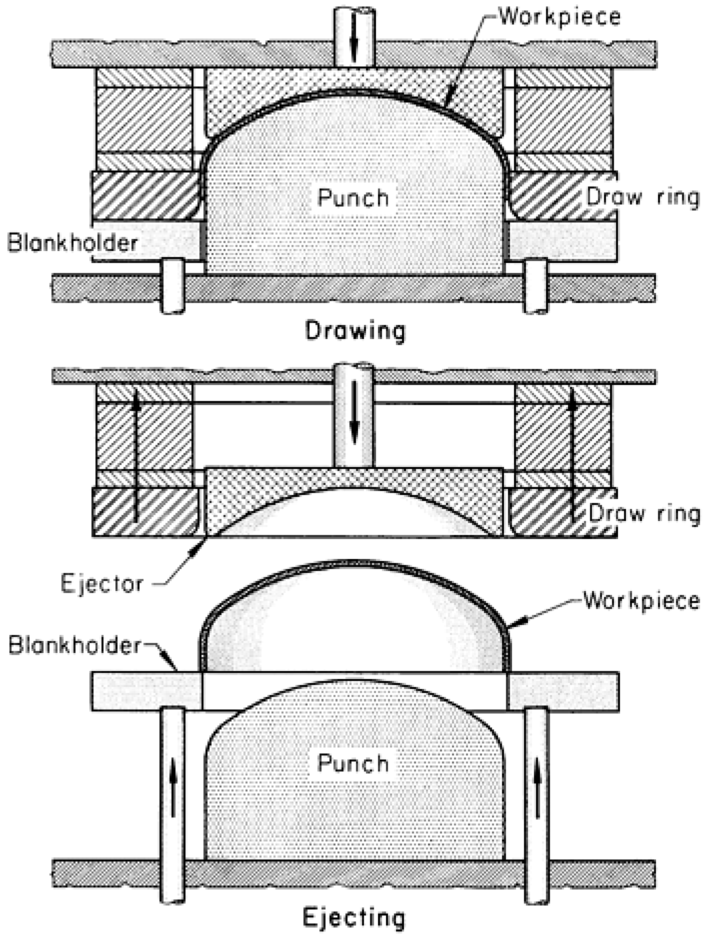


Fig. 22 Use of a blankholder and ejector for stripping of a drawn workpiece from inverted dies.

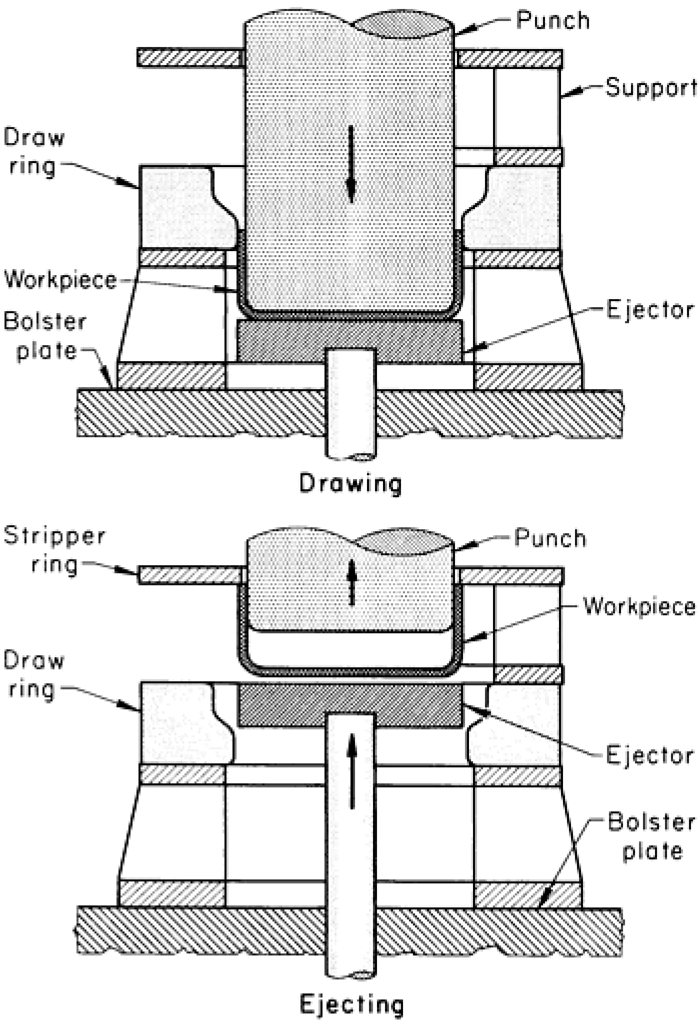


Fig. 23 Setup in which drawn workpiece is lifted from the die by an ejector and is stripped off the punch by a fixed stripper ring.

Trimming

Trimming in a lathe (using a cutting tool), roll trimming in a lathe, rotary shearing, die trimming (regular and pinch), and trimming on special machines are the methods most commonly used for trimming drawn workpieces.

Methods for Specific Shapes. Cylindrical workpieces such as the one shown in Fig. 24(a) can be trimmed by at least four different methods:

- In a lathe, with a cutting tool, but production cost is high
- By roll trimming in a lathe or in a rotary shear. Production cost is lower than that for trimming with a cutting tool, but the finish of a rolled edge is poor and maintenance cost of the rolls is high
- By pinch trimming in the press at the bottom of the drawing stroke. This involves almost no increase in production cost, but requires a more expensive die. This method produces a thinned edge at the trim line, which may be unacceptable
- In a shimmy die or trimming machine, but production quantities must be high to warrant the investment

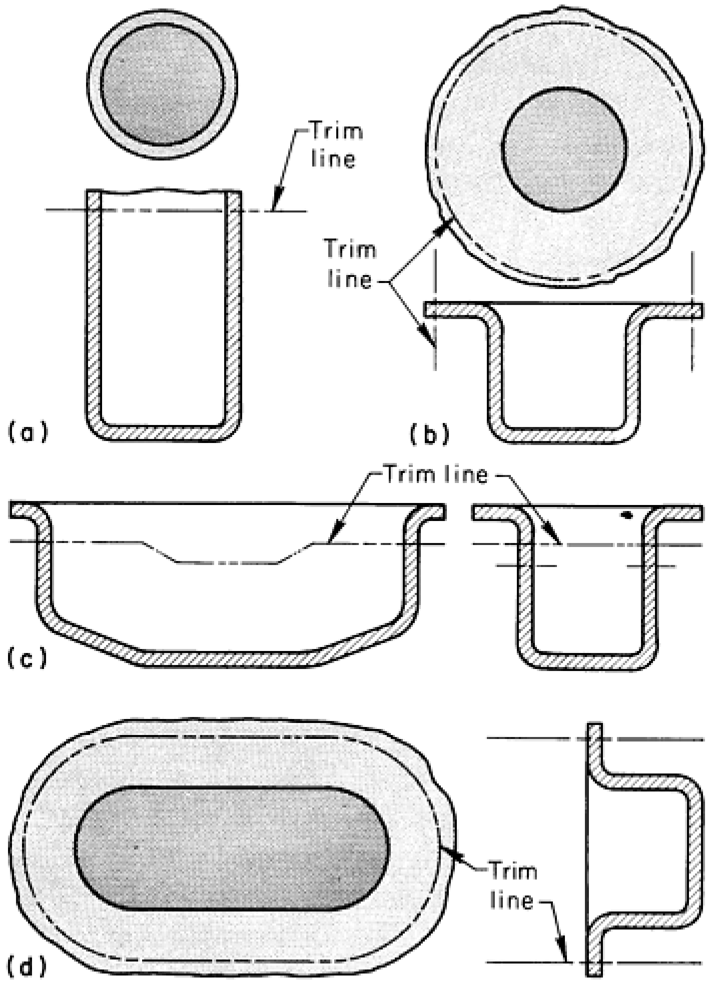


Fig. 24 Typical trim lines on drawn parts. See text for details.

Cylindrical workpieces with flanges, such as the one shown in Fig. 24(b), can also be trimmed in a lathe, although shapes such as this are ideal for trimming in a die and can be die trimmed for about 5% of the cost of trimming in a lathe. Rotary shearing can also be used for trimming circular drawn parts with flanges if the dimensional tolerance is 0.76 mm (0.030 in.) or more.

Drawn workpieces with an irregular trim line, as in Fig. 24(c), can be trimmed in a die for low-production requirements, or with a shimmy die or trimming machine for high-production requirements. The cost of a trimming die is about half that for a special trimmer (excluding the cost of the original machine). However, the trimming cost per piece with the special trimmer will be only about half the per-piece cost with multiple dies, and the trimmed edges will be better.

Flanged workpieces such as the one shown in Fig. 24(d) can be trimmed in a die for 5% of the cost of trimming in a rotary shear. In low production, drawn workpieces of such a shape are frequently trimmed in a nibbler and filed to conform to a template. This means of trimming costs up to 60 times as much as die trimming.

Developed Blanks Versus Final Trimming

The most important factor that influences a choice between using a developed blank or a final trimming operation is whether or not the shape of the drawn edge is acceptable. A semideveloped blank is sometimes necessary to draw an acceptable part, and the edge must be trimmed to meet dimensional tolerances.

The next consideration is the cost of blanking versus final trimming. This would include the adaptability of the process to the available equipment, based on expected production requirements. The principal advantage of a developed blank is that strip or coiled work metal can be used. The use of strip eliminates the need for shearing the work metal to a rough-blank shape, as is sometimes required when final trimming is used. The developed-blank approach is usually more economical than final trimming because a blanking die is frequently less expensive than a final-trimming die.

When using developed blanks, the draw dies are made, and several blanks are drawn to select the optimal developed shape before the blanking die is made. This causes a delay in placing the draw die into production. However, with proper planning and scheduling, this should not be a problem.

Another disadvantage of developed blanks occurs when variations in work metal properties and thickness are sufficient to affect the uniformity of the drawn workpiece. Under these conditions, closer tolerances are obtained by final trimming. It is possible to develop blank contours accurately enough so that the outline of the drawn part is within tolerance, thus avoiding a final-trimming operation.

Cleaning of Workpieces

In general, the more effective the lubricant, the more difficult it is to remove. Therefore, an overly effective drawing lubricant should be avoided.

The cleaning method depends on the work metal composition, the lubricant, the degree of cleanliness required, workpiece shape, and sometimes the length of time between application of lubricant and its removal. Some metals will be attacked by cleaners that are not harmful to others. For example, strong alkaline cleaners are suitable for cleaning steel and many other metals, but they are likely to attack aluminum alloys. Detailed information is available in the articles on the surface engineering of specific metals in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Unpigmented oils and greases can be removed from steel workpieces by several simple shop methods, including alkaline dipping, emulsion cleaning, and cold solvent dipping. These methods are usually sufficient for in-process cleaning. However, if the workpieces are to be painted, a more thorough cleaning by emulsion spray or vapor degreasing is required. For plating, electrolytic cleaning plus etching in acid (immediately prior to plating) is required. These latter methods usually follow a rough cleaning operation.

Pigmented drawing lubricants and waxes greatly increase cleaning problems. At a minimum, in-process cleaning usually requires slushing in a hot emulsion or vapor degreasing. If the lubricant is not removed for several days after application, soaking in a hot alkaline cleaner or an emulsion cleaner may be required. Particularly for complex workpiece shapes, some hand or power brush scrubbing may be needed. If the workpieces are to be painted or plated, additional cleaning will be required, as described above. Detailed information on the choice of cleaning method is available in the article "Classification and Selection of Cleaning Processes" in *Surface Engineering*, Volume 5 of the *ASM Handbook.*

Dimensional Accuracy

Dimensional accuracy in deep drawing is affected by the variation in work metal thickness, variation in work metal condition (chiefly hardness), drawing technique (particularly the number of operations), accuracy of the tools, rate of tool wear, and press condition. Control of dimensions begins with the purchase of sheet to closer-than-commercial thickness tolerance, which adds substantially to the cost. Close control of sheet hardness also costs more. In-process annealing may be required to minimize springback or warpage; it will not be needed if tolerances are more liberal. Annealing, handling, and cleaning operations are costly.

As tolerances become closer, it is often necessary to add more die stations to minimize the amount of drawing in any one station. Close tolerances may demand restriking operations that would not be necessary for parts with more liberal tolerances. Additional operations increase tool costs and decrease productivity, thus increasing the cost per piece.

The initial cost of tools increases as tolerances become closer because of greater cost for precision machining and grinding or more costly tool materials. In addition, tool life before reconditioning and total tool life decrease as tolerances become closer. Maintenance costs and downtime of presses are also greater.

When required, extremely close tolerances can be maintained on some parts (Fig. 25). In most deep drawing, the accuracy shown in Fig. 25 is either impossible or impractical. The more usual practice when dimensional accuracy is important is to check critical dimensions at specified intervals during a production run and to plot the variation. Data from this method of quality control show the capabilities of the process under shop conditions and the magnitude of drift during a production run. When results (either initially or during a run) are unacceptable, one or more of the controls discussed at the beginning of this section can be applied.

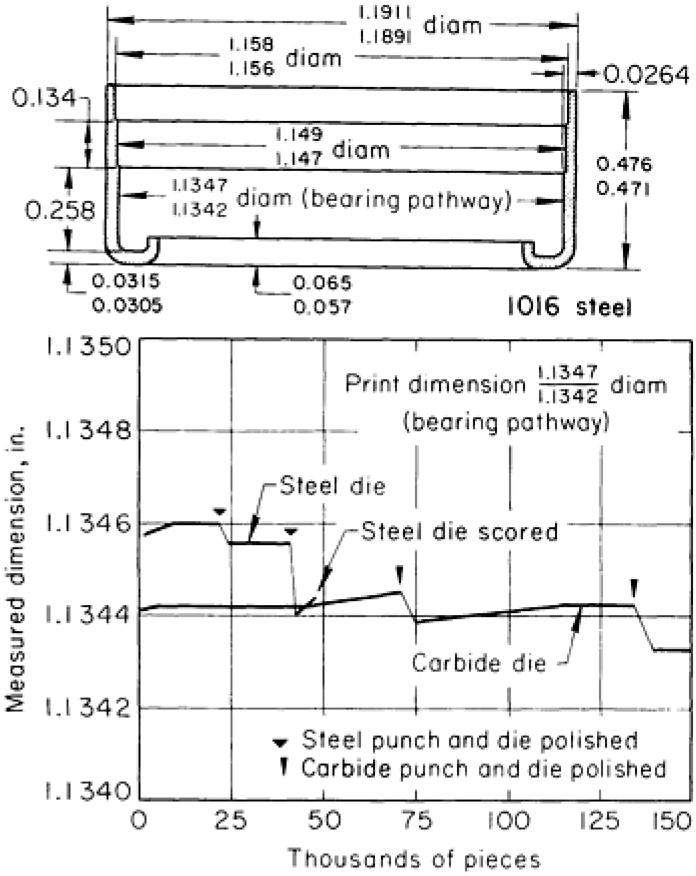


Fig. 25 Variation in bearing pathway diameter of a needle bearing cup drawn using high-speed tool steel and carbide dies.

Safety

Deep drawing, like other press operations, involves potential hazards to operators and other personnel in the work area. No press, die, or auxiliary equipment can be considered ready for operation until these hazards are eliminated by the installation of necessary safety devices. Operators should be properly instructed in safe operation of equipment.

Introduction

STRETCH FORMING is the forming of sheet, bars, and rolled or extruded sections over a form block of the required shape while the workpiece is held in tension. The work metal is often stretched just beyond its yield point (generally 2 to 4% total elongation) to retain permanently the contour of the form block.

The four methods of stretch forming are:

- Stretch draw forming (Fig. 1a and b)
- Stretch wrapping, also called rotary stretch forming (Fig. 1c)
- Compression forming (Fig. 1d)
- Radial draw forming (Fig. 1e)

These methods are discussed separately in subsequent sections of this article.

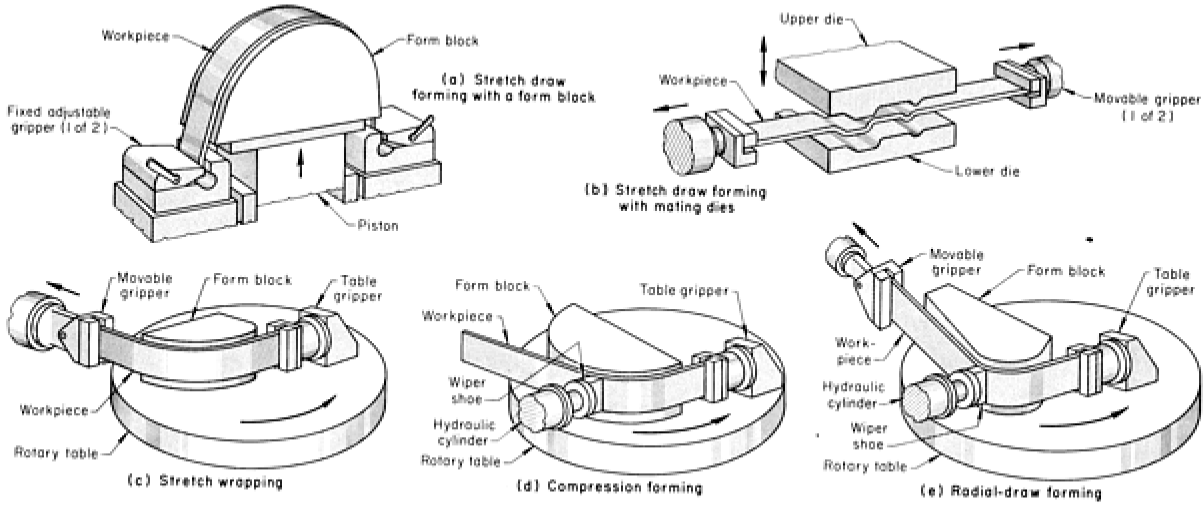


Fig. 1 Fundamentals of the techniques involved in the four methods of stretch forming

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Applicability

Almost any shape that can be produced by other sheet-forming methods can be produced by stretch forming. Drawn shapes that involve metal flow, particularly straight cylindrical shells, and details that result from such compression operations as coining and embossing cannot be made. However, some embossing is done by the mating-die method of stretch draw forming (Fig. 1b).

Stretch forming is used to form aerospace parts from steel, nickel, aluminum, and titanium alloys and other heat-resistant and refractory metals. Some of these parts are difficult or impossible to form by other methods--for example, the titanium alloy gas-turbine ring shown in Fig. 2. The procedure for making such a ring is described in Example 5 in this article.

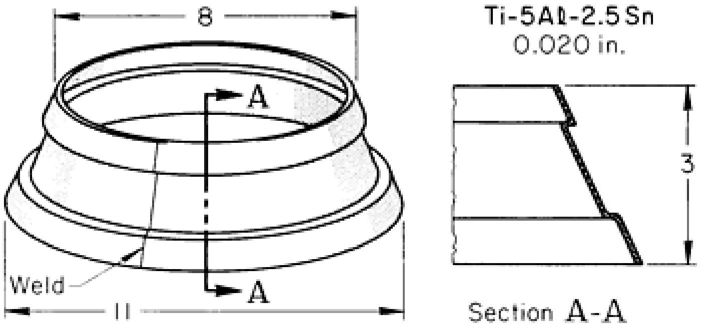

Fig. 2 Titanium alloy gas-turbine ring that was produced by compression forming. Dimensions given in inches

Stretch forming is also used to shape automotive body panels, both inner and outer, and frame members that could be formed by other processes but at higher cost. An example is the automobile roof shown in Fig. 3, which was stretch draw formed using a blank that weighed 2.9 kg (6.4 lb) less than would have been needed for a conventional press-forming process. Architectural shapes and aerospace forms that call for compound curves, reverse bends, twists, and bends in two or more planes are also produced by stretch forming.

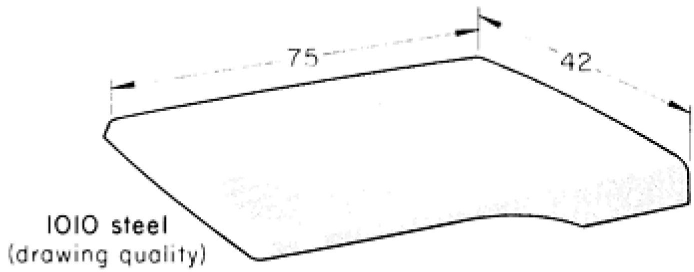

Fig. 3 Stretch draw formed automobile roof. Dimensions given in inches

Advantages. Stretch forming has the following advantages over conventional press-forming methods:

- About 70% less force is needed than that required for conventional press forming
- Stretch forming can reduce material costs by as much as 15%. Although allowance must be made on the stock for gripping, it is gripped on two ends only. The allowance for trimming is usually less than that in conventional press forming
- Because stretch forming is done on the entire area of the workpiece, there is little likelihood of buckles and wrinkles. Tensile strength is increased uniformly by about 10%
- Hardness is increased by approximately 2%
- Springback is greatly reduced. There is some springback, but it is easily controlled by overforming
- Residual stresses are low in stretch-formed parts
- Form blocks are made of inexpensive materials, such as wood, plastic, cast iron, or low-carbon steel, and are about one-third the cost of conventional forming dies. If the workpiece is formed hot, the dies must be able to withstand the forming temperature. However, most stretch forming is done at room temperature
- Changeover is simple. Only one form block and two sets of grippers are involved. To make the same part from a different metal or another stock thickness, the same form block and grippers are used, but the tension of the stretch mechanism is adjusted

Limitations. Stretch forming is subject to the following limitations:

- It is seldom suited to progressive or transfer operations
- It is limited in its ability to form sharp contours and reentrant angles. It is at its best in forming shallow or nearly flat contours
- If the piece is not pinched between mating dies, there is no opportunity to coin out or iron out slight irregularities in the surface of the metal
- In some applications, especially in stretch wrapping, the process is slower than competitive processes, and it is not suited to high-volume production. However, stretch draw forming with mating dies can be done as rapidly and automatically as conventional press operations. In fact, punch presses are used with dies incorporating draw beads or other means of gripping the blank in order to perform some stretch-forming operations
- Metals with yield strength and tensile strength very nearly the same, such as titanium, necessitate the use of automatic equipment for determining the amount of strain for uniform results
- Optimal results are achieved with rectangular blanks. The aircraft industry uses trapezoidal blanks, but gives greater attention to each piece than is warranted in high-volume production
- Deep forming in the direction of the free edges is not practical

Machines and Accessories

Stretch wrapping, compression forming, and radial draw forming use rotary tables (some with sliding leaves) for mounting the form blocks, a ram gripping and tensioning or wiping device, and a mechanically or hydraulically actuated table gripper (Fig. 4). Machines used for these operations have capacities to 8900 kN (1000 tonf).

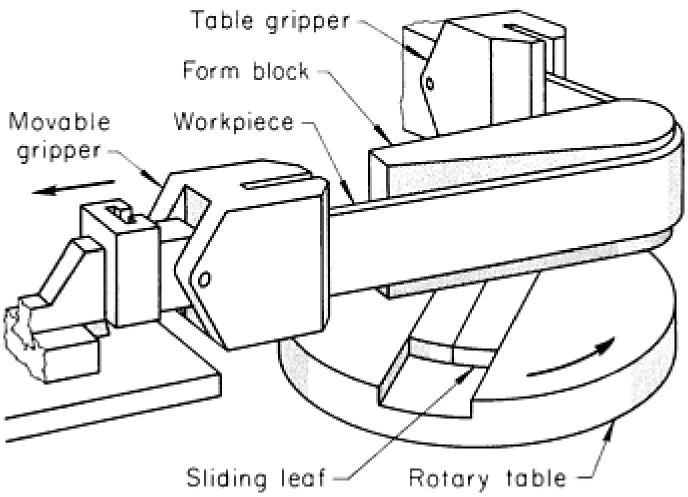

Fig. 4 Stretch-forming machine with rotary table and sliding center leaf

Stretch draw forming is done in three types of machines. In one type, the form block mounted on a hydraulic cylinder is pushed into the blank, which is held in tension by a pair of pivoting grippers. In another type, the form block is fixed to the table, and the blank is drawn around it by a pair of grippers actuated by slides or a hydraulic cylinder. The third type of machine is a single-action hydraulic press equipped with a means of closing and moving a pair of grippers (see Fig. 7); a mating die is used instead of a form block. The hydraulic presses ordinarily used in stretch draw forming have capacities of 1800 to 7100 kN (200 to 800 tonf).

Accessory Equipment. Grippers and wiping shoes or rollers are made to conform to the rolled or extruded shape that is to be stretch formed. Jaws used for gripping sheet in stretch draw forming can be segmented or contoured to apply equal stretch to all parts of the sheet as it is formed.

The vertical adapter shown in Fig. 5 is used with a rotary table; it is fastened to the hydraulic cylinder used for applying tension to the blank. The adapter allows wiper shoes, rollers, and grippers to move up or down as needed in order to accommodate work with bends in two or more planes. Lead screws or hydraulic cylinders position the grippers or wiping devices at the correct position for the forming operation (see Examples 5 and 6).

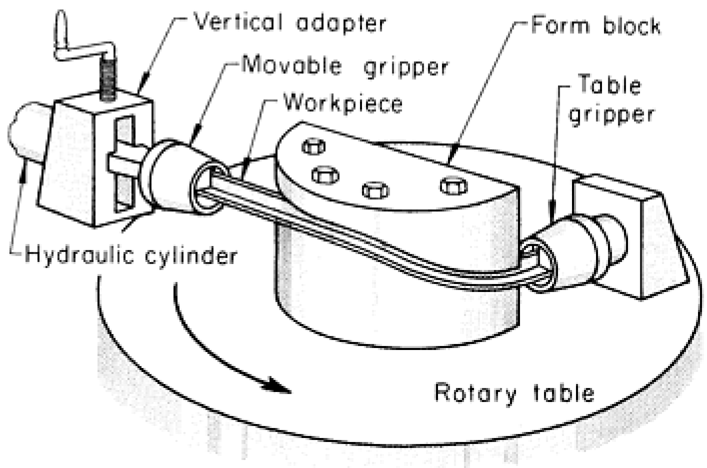

Fig. 5 Use of a vertical adapter on the tension unit to accommodate work with bends in two or more planes

A yield detector and tension-control device (Fig. 6) provide a means of automatically applying the same amount of stress to every workpiece in a production lot. This is important with metals (for example, titanium) that have yield strength and tensile strength too close for ordinary control of tension for stretch forming (see the article "Forming of Titanium and Titanium Alloys" in this Volume). With this type of control, scrap in the stretch forming of titanium can be reduced to 2%.

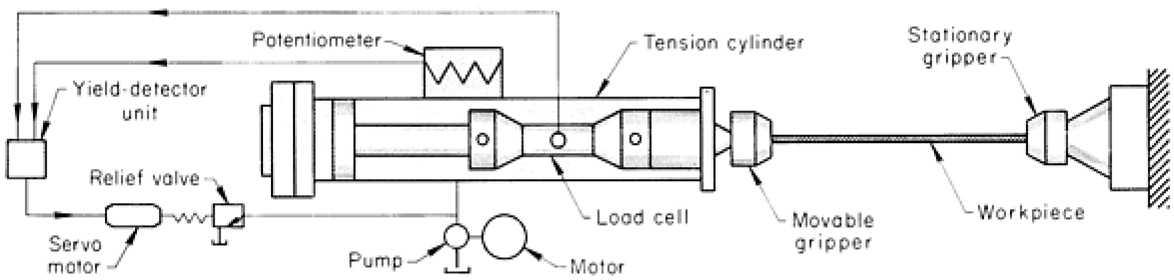

Fig. 6 Components and signal-flow diagram of an automatic tension-control system used in stretch forming

The tension control uses two inputs in a null system for its output signal. One input comes from a load cell that gives a signal proportional to the stretch force on the workpiece. The other signal comes from a potentiometer that measures the elongation of the workpiece. As long as the signals are proportional, the metal is not stretched beyond its yield point, and the two inputs balance. When the yield point is reached, the input from the load cell stops increasing or increases at a much lower rate, while the potentiometer input continues to rise. This upsets the null balance, and an output signal is given, which can be interpreted as percentage of strain beyond the yield point.

Table restretch units are small short-stroke hydraulic cylinders and clamps that can be bolted to the rotary table to give a final stretch set to workpieces that need to be stretched from both ends or restretched after heat treatment. The capacity of a table restretch unit is usually equal to that of the main tensioning gripper.

Stretch Draw Forming

Stretch draw forming is done with either a form block or a mating die.

The Form-Block Method. Bars and structural shapes, although usually radial draw formed, can be stretch draw formed by the form-block method. Also known as drape forming, the form-block method uses either a fixed or a moving form block. A fixed form block is attached to the machine base. Each end of the blank is held by a gripper attached to a hydraulic cylinder. The grippers move to stretch the blank over the form block. Alternatively, the moving form block is attached to a hydraulic piston. A blank is held by grippers while tension is applied to it, and the form block then moves to form the part, as shown in Fig. 1(a).

The mating-die method uses a two-piece die mounted in a single-action hydraulic press (Fig. 7). This method combines the advantages of stretch forming and conventional press forming. The stretch forming sets the contours of moderately formed workpieces, and the press forming gives definition to sharply formed contours, such as beads or feature lines on automobile body parts.

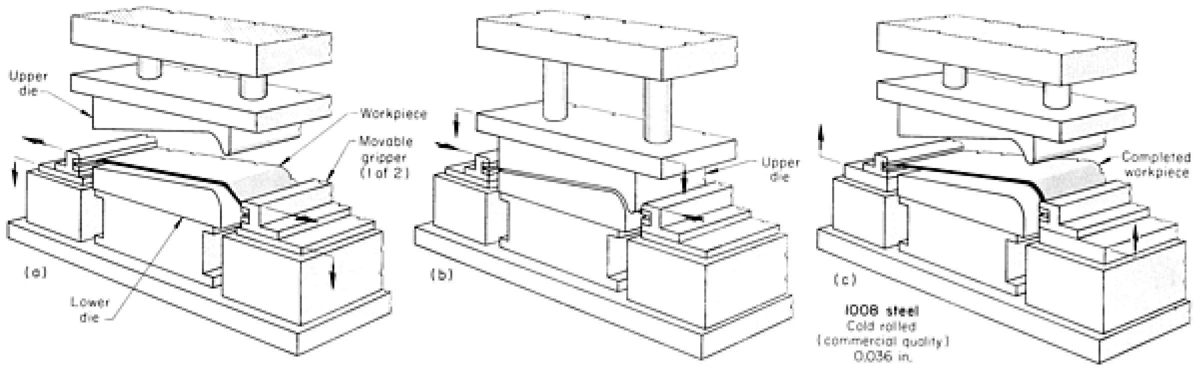

Fig. 7 Production of an automobile rear-deck lid in a stretch draw forming press. (a) Sheet metal blank is tensioned by grippers moving apart. Grippers move down, stretching the workpiece over the lower die. (b) Upper die descends onto the workpiece, pressing the metal into both dies to form the part. (c) After forming, the press opens, and the part is released from the grippers.

Grippers preform the blank over the lower die to the curvature of the part (Fig. 7a). There is very little metal flow; the stretching action and the die form the general outline of the part. The upper die then descends to produce the details and to set the contours (Fig. 7b).

Automatic material-handling equipment can be adapted to the machine for production runs. Production rates are comparable to those obtainable for drawing in conventional single- and double-action presses. Stretch draw press tooling for large parts, such as automobile roof panels, weighs only one-third that for a conventional double-action press, as indicated in Table 1.

Table 1 Comparison of conventional and stretch presses

Press	**Capacity**				**Tooling weight**		Press height	
	Punch		**Gripper (blankholder)**					
	kN	**tonf**	**kN**	**tonf**	**kg**	**tons**	**mm**	in.
Conventional	8000	900	5300	600	20,000	22	7320	**288**

Stretch	**2200**	**250**	**760**	**85**	**6300**	**7**	**5100**	**200**

Exposed parts, such as automobile outer body panels, frequently have a maximum surface roughness specification of 1.1 μm (45 μin.), and stretcher strain marks and other defects, which are still noticeable after painting, must be prevented. One method of avoiding strain marks is the use of segmented or curved grippers, which equalize the amount of stretching. The following example describes the production of a rear-deck lid by stretch draw forming with a mating die.

Example 1: Stretch Draw Forming of an Automobile Rear-Deck Lid.

Automobile rear-deck lids were produced in a stretch draw press using mating dies, as shown in Fig. 7. The blanks were commercial-quality cold-rolled 1008 steel 0.91 mm (0.036 in.) thick, 1450 mm (57 in.) wide, and 1600 mm (63 in.) long. Residual mill oil was the only lubricant. The production rate was 360 pieces per hour, and annual production was 400,000 deck lids.

Tension was applied to the sheet by the grippers as they moved apart. (Generally, hydraulic cylinders are used to apply the force in this operation.) The tensioned sheet (still held by the grippers) was then lowered to stretch over the lower die. Finally, the upper die was lowered, pressing the sheet into both dies to form the lid.

The cycle time was 7 s. The finished parts showed uniformly good quality without wrinkles or buckles. Approximately 0.9 kg (2 lb) more sheet steel would have been needed to produce this part in a conventional double-action press.

Lancing. If stretch drawing is used to form severe contours, the stretch limits of the metal may be exceeded in the zones of deep forming, resulting in fracture of the metal. This can be avoided by lancing the metal in areas to be discarded later so that the metal can flow in the severely formed zones. Single-operation production of a truck-cab roof having a combination of gradual curves and sharp contours is described in the following example.

Example 2: Stretch Draw Forming of a Truck-Cab Roof With Reinforcing Beads.

By using stretch draw forming with mating dies, the truck-cab roof panel shown in Fig. 8 was produced in one operation. Panels were formed from cold-rolled drawing-quality aluminum-killed 1008 steel. The blanks were 1520 × 813 × 0.89 mm (60 × 32 × 0.035 in.).

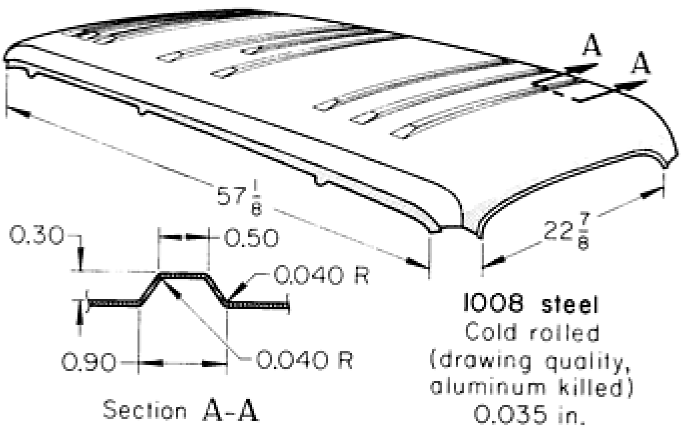

Fig. 8 Beaded truck-cap roof that was stretch draw formed with mating dies. Dimensions given in inches

With automatic material-handling equipment to load and unload the machine, the production rate was 100 to 150 pieces per hour. Annual production was 25,000 pieces.

The forming dies were of cast iron, with flame-hardened surfaces where severe forming occurred. Design changes that would make the dies obsolete were not expected for 4 or 5 years. Radii on the roof beads were 1.0 mm (0.040 in.).

Other examples of stretch draw forming using the mating-die and form-block methods are described in the article "Forming of Aluminum Alloys" in this Volume.

Stretch Wrapping

In stretch wrapping, just enough tension is applied to one end of a workpiece to exceed the yield strength of the material, while the form block revolves into the workpiece with the turning of the table, as shown in Fig. 1(c). The other end of the workpiece is held in a table gripper or clamped to the end of the form block. The hydraulic cylinder applying tension to the workpiece is free to swivel so that the tension is always tangential to the last point of contact. Thus, the work metal wraps in tension around the form block without the scuffing or friction that occurs with other forming methods. The result is an accurately formed piece with little springback; therefore, form blocks can be made to accurate size.

Because there is no scuffing, form blocks can be made of soft metal, wood, or plastic, although common die materials such as cast iron are often used. Form blocks made of hardwood, masonite, and epoxy have also been used. The contour of the form block can vary throughout the bend, and the workpiece will follow it accurately if there are no concave surfaces on the form block.

Form blocks for the stretch wrapping of rolled and extruded sections are machined to the shape of the section as well as the contour of the finished part. Thus, the shaped form block supports the section during forming. Additional support is sometimes needed for open or hollow sections. A segmented filler, a filler made of low-melting alloy, or a strip of easy-to-form metal can provide this support. Return bends can be made by using additional form blocks on sliding leaves of the turntable and by reversing the table direction to produce the part, as shown in Fig. 9.

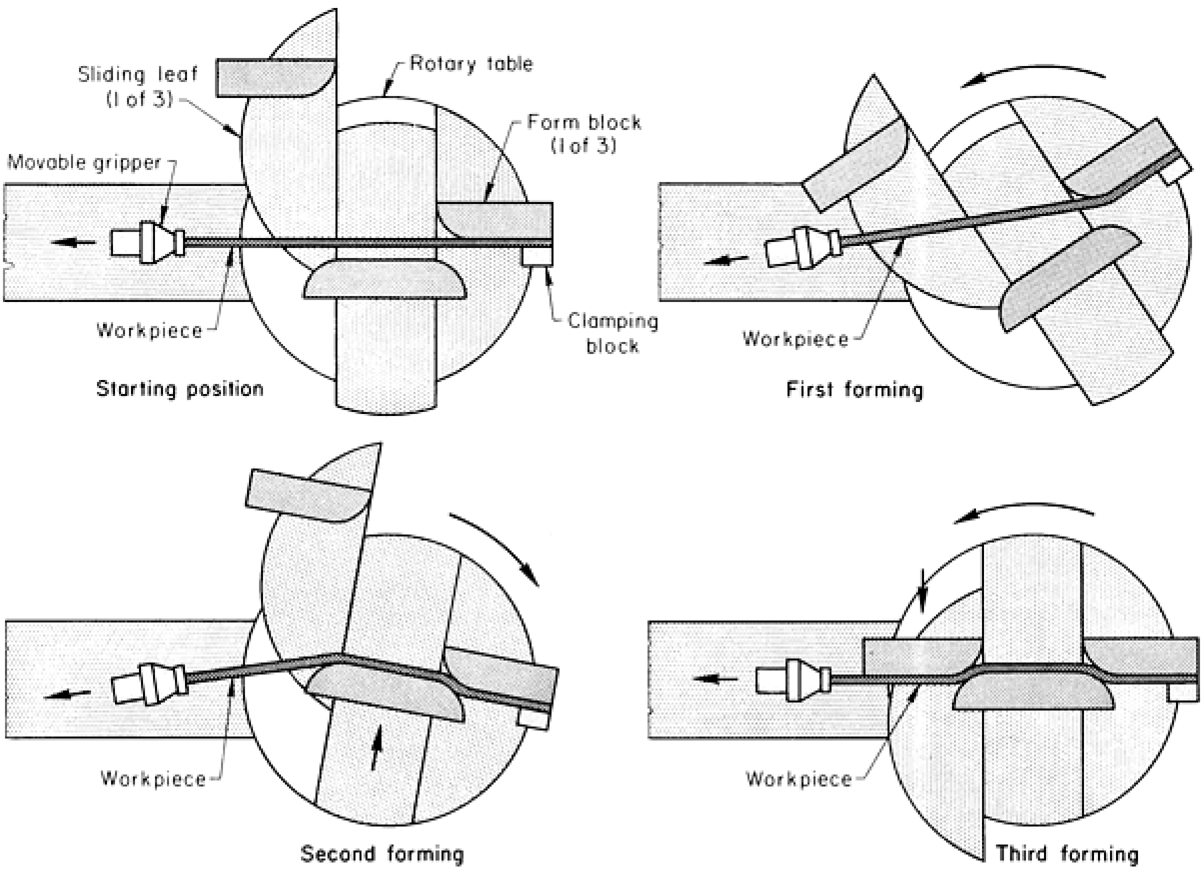

Fig. 9 Stretch wrapping of a part around three form blocks to make two reverse bends

Machines for stretch wrapping consist basically of a variable-speed power-driven rotary table and a double-action pressure-controlled hydraulic cylinder. The form block is bolted to the table. Grippers are connected to the hydraulic cylinder so that tension can be applied to the workpiece, as in Fig. 1(c). The fabrication of a typical part by stretch wrapping is described in the following example.

Example 3: Forming of an Aircraft Leading-Edge Wing Panel by Stretch Wrapping.

A corrugated leading-edge wing panel of aluminum alloy 6061-O was stretch wrapped in a stretch-forming machine with a vertical-axis turntable. The sheet, with corrugations in the direction of airflow, was gripped at each end with grippers shaped to fit the corrugations. The tension applied was slightly above the yield strength of the work metal. The form block, bolted to the turntable, rotated slowly into the workpiece, causing it to form smoothly into the shape of the wing without flattening the corrugations. While the form block was moving in the sheet, the hydraulically restrained gripper maintained tension slightly above the yield point. The form block was made to the required final shape without allowance for springback because only a small amount of springback occurred.

Compression Forming

In compression forming, the workpiece is pressed against the rotating form block instead of being wrapped around it. The process is typically used for maintaining or controlling workpiece cross-sectional dimensions throughout the contour, for bending to radii small enough to exceed the elongation limits of the metal if formed by stretch wrapping, and for bending sections too heavy for the capacity of the available stretch wrap machinery.

Compression forming can generally be done in the same machine as stretch wrapping, but the hydraulic cylinder is used to apply pressure instead of tension to the workpiece. The cylinder is locked in place to keep it from swiveling, and the ram head is furnished with a roller or a shoe to press the workpiece against the form block. A clamp or table gripper holds the end of the workpiece against the form block, and as the table rotates, the shoe or roller on the hydraulic cylinder presses the workpiece into the contour of the block, as shown in Fig. 1(d).

Compression forming can often make bends to a smaller radius than stretch wrapping in a part that has a deep cross section. If the same bend were produced by stretch wrapping, fracturing or overstressing of the outer fibers would result. The total load needed to form large-section pieces, such as crossrails and bumpers, can be as little as 2% of that needed to form them in a punch press. The total energy applied to the workpiece would of course be the same (neglecting efficiency); the smaller compression-forming force is applied for a longer period of time. The wiping shoe or roller can hold the cross-sectional size and shape to close tolerance throughout the contour. Parts that are too heavy in cross section for stretch wrapping can often be compression formed.

Blanks for stretch forming are usually made longer than the finished part so that the surface damaged by the gripper jaws can be trimmed off. However, end details, locating surfaces, and other considerations occasionally necessitate the use of a blank cut to the length of the finished part, and dimensional tolerances still must be met, as in the following example.

Example 4: Use of an Adjustable Form Block to Compression Form a Developed Blank.

Because both ends of the piece shown in Fig. 10 had previously produced details, the part could not be trimmed after forming. Instead of a table gripper or clamp, the blank was fastened to the form block by bolts through two 21 mm ($\frac{13}{16}$ in.) diam holes pierced in one end. The blank was cut slightly shorter than the required length because the length increased from 3.602 to 3.613 m (141.81 to 142.25 in.) during forming.

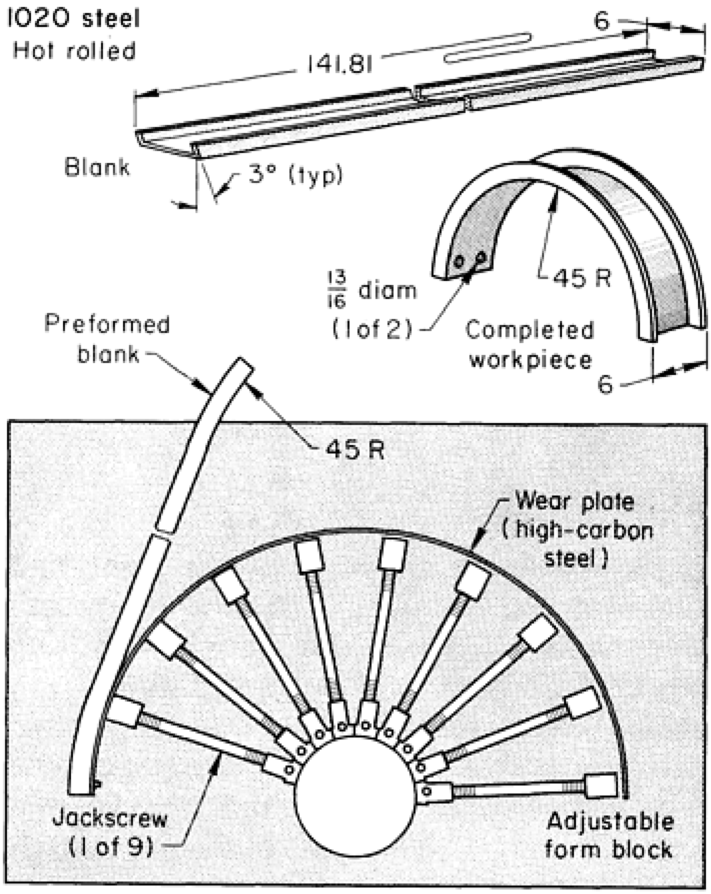

Fig. 10 Steel channel that was compression formed without trim allowance using an adjustable form block. Dimensions given in inches

The 1020 steel structural shape that was being compression formed had considerable springback, which varied with each heat of steel. To obtain uniform results, the form block was made adjustable. As shown in Fig. 10, the wear plate, made of 19 mm ($\frac{3}{4}$ in.) thick by 203 mm (8 in.) wide high-carbon steel, was backed up with jackscrews that could be adjusted to change the effective radius. One end of the wear plate was fastened to the base plate, and the other end was free to move in and out of any position, supported by the jackscrews. When a new lot of steel was delivered, an experimental piece was run to determine springback, and the jackscrews were adjusted accordingly.

The work material was a hot-rolled channel 152 mm (6 in.) wide, weighing 15.6 kg/m (10.5 lb/ft), approximating a 1020 steel in composition. The piece was compression formed on a radial draw former into a half circle with a 1145 mm (45 in.) radius. The two holes in the end of the piece were used to connect this section with a fishplate on one end of a mating piece to form an assembled ring. The sequence of operations was as follows:

- Saw ends with a 3° bevel to developed length (3.602 m, or 141.81 in.)
- Deburr
- Pierce two 21 mm ($\frac{13}{16}$ in.) diam holes
- Form both ends to 1145 mm (45 in.) radius for 152 to 203 mm (6 to 8 in.) of length on a press brake
- Bolt the workpiece to the form block by the two pierced holes. Compression form to 1145 mm (45 in.) radius
- Galvanize after forming
- Flatten as necessary (galvanizing sometimes causes warpage)

A straight mineral oil was used as the lubricant. The overall tolerance on the formed curve was 1.5 mm (0.060 in.) total indicator reading.

Production time was 3 min per piece with two operators, and setup time was 1 h. The production-lot size was 250 pieces.

Two-plane curvature. When a part must have curvature in two or more planes, a vertical adapter, either hydraulic powered or screw actuated, is used to permit the ram gripper to move up and down as the work requires. Thus, the work material can be fed into a spiral or other form involving rising and failing curvatures. In the following example, a vertical adapter with a wiper shoe was used to form a low-angle helix that was later welded into a ring.

Example 5: Producing a Ring From a Helix to Counteract Springback.

Because springback in the forming of a titanium alloy engine ring made it difficult to weld the workpiece into a true circle after forming, the stock was compression formed into a low-angle helix by using a vertical adapter with a wiper shoe. The form block was smaller in diameter than the finished ring, and when the workpiece was removed from the form block, springback was just sufficient to permit welding into a true ring. The setup used for forming the ring is shown in Fig. 11.

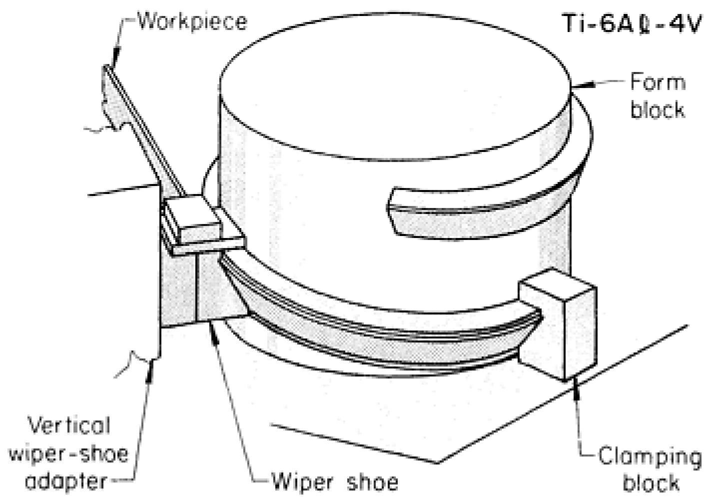

Fig. 11 Use of a vertical wiper-shoe adapter to form a helical shape. After forming, springback of the titanium alloy used brought the formed piece into a circular shape, which could be welded into a ring.

Stretch Forming

Radial Draw Forming

Radial draw forming is a combination of stretch wrapping and compression forming, as shown in Fig. 1(e). As in stretch wrapping, one end of the workpiece is gripped by stationary jaws attached to the rotary table. The other end is gripped by jaws on the hydraulic cylinder. The cylinder exerts tension on the workpiece as the form block on the rotary table revolves into it. A second hydraulic unit, fitted with a wiper shoe or roller, presses the workpiece into the contour of the form block at the point of tangency. The hydraulic unit applying the compression force can be moved as necessary to keep the wiper shoe in contact with the workpiece. On large machines, an operator sometimes rides a platform on the second unit to observe the point of contact.

Joggles in rolled or extruded sections can be formed after the part has been radial draw formed, without removing the part from the form block. When contour forming is completed, the part is held in tension while the compression unit is repositioned, and the joggle is formed by the wiper shoe. The wiper shoe is sometimes used to apply pressure to a loose joggle block (Fig. 12) if the wiper shoe will not provide the correct shape for the joggle. In either case, the shape of the joggle is also machined into the form block. The vertical adapter shown in Fig. 5 can be used in the radial draw forming of bends in two or more vertical planes.

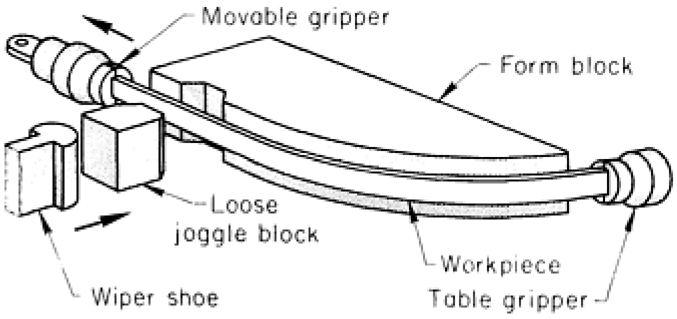

Fig. 12 Forming a joggle with a loose block and a wiper shoe after radial draw forming

Architectural sections, extruded shapes, and other sections sometimes have to twist on themselves if they are contour formed through any plane other than the plane of symmetry. In radial draw forming, this can be done by permitting the workpiece to rotate axially as the part follows the twisting contour of the form block. Rotation is obtained by slightly loosening the lock ring on the body of the gripper head, allowing the head to rotate about its own centerline. The following example illustrates the forming of an angle section by this method.

Example 6: Twisting of an Angle Section During Contour Forming.

An L-shaped section for the gunwale of an air-sea rescue craft (Fig. 13) had to be twisted as it was radial draw formed. It was made of aluminum alloy 2024-T4. Forming had to be done in several planes. The locking ring of the gripper was loosened, permitting the head to rotate as the part was formed.

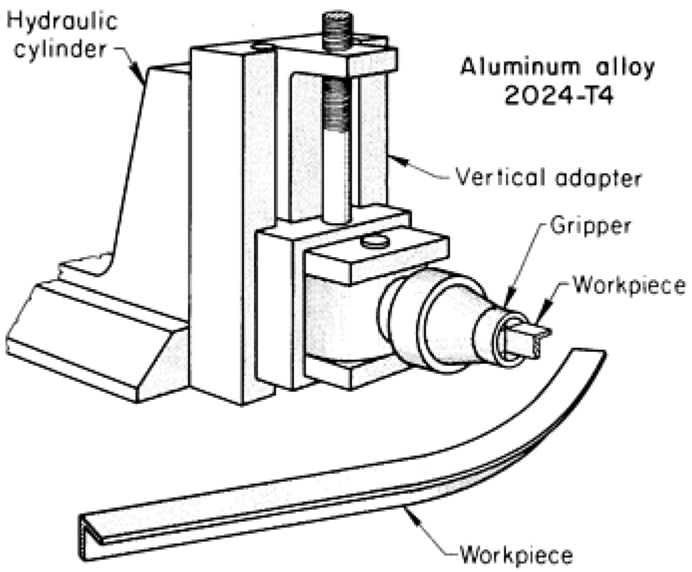

Fig. 13 Gunwale section that was produced from an L-section by a combination of twisting, stretching, and forming in several planes

The usual production-lot size was 500 pieces. Parts were formed at the rate of 10 per hour.

Radial draw forming operations on an extruded section and on a sheet are illustrated in the article "Forming of Aluminum Alloys" in this Volume.

Stretch Forming

Stretch-Forming Machines for Large Parts

Drape-forming machines are designed to be used on relatively small lengths and widths. The stretching and bending of greater lengths and widths require the increased capacity of stretch-forming machines. Stretch-forming machines bend the workpiece around the die to elongate the material fibers while simultaneously preventing wrinkles and minimizing springback.

These machines are available in two basic types:

- Moving jaws only, to stretch the blanks around a stationary form block
- Moving jaws combined with a moving-die (form-block) table

Transverse or longitudinal models and combination transverse-longitudinal models are available. Selection of the appropriate machine (jaw width, distance between jaws, and force capacity are key specifications) is determined by the configuration and dimensions of the workpiece.

Transverse Machines. The jaws of transverse machines, on which the workpieces are stretched and bent transversely, must be as long as the workpiece to provide substantial area for gripping. The transverse machine shown in Fig. 14 has a movable die table that can be tilted 15° above or below the horizontal. The jaws can be swiveled 30° in a horizontal plane and 90° in a vertical plane so that the direction of stretching can be aligned with the contour of the die. Rated at 6700 kN (750 tonf), the machine has a 2080 mm (82 in.) stroke, a 25 to 3660 mm (1 to 144 in.) jaw distance, and a 25 to 457 mm/min (1 to 18 in./min) forming speed. This equipment is primarily used for forming large sheets, such as fuselage skins and the leading edges of airplane wings. Large, cumbersome extrusions, such as wing spars, can be formed by changing or adapting the jaws.

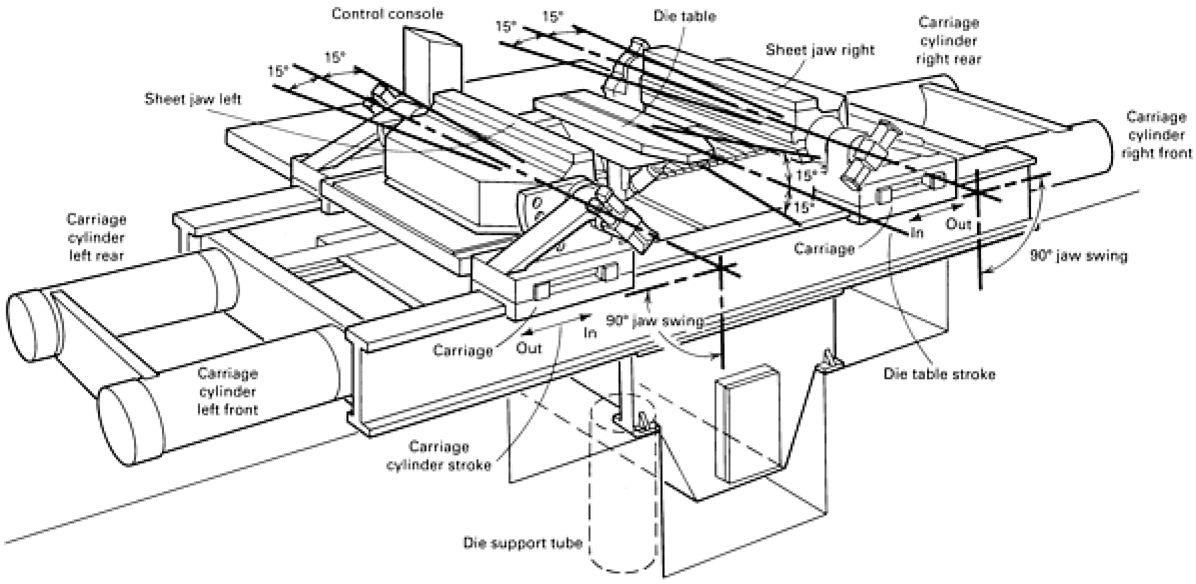

Fig. 14 Transverse stretch-forming machine having both movable and tiltable die table and swiveling, movable jaws

Longitudinal Machines. The longitudinal stretch-forming machine shown in Fig. 15 can be swiveled 90°, but cannot be raised or lowered. The 2540 mm (100 in.) wide, hydraulically powered, leadscrew-actuated jaws are made in sections for curvature to various radii. These jaws can be swiveled both horizontally and vertically. Each individual jaw develops a 6700 kN (750 tonf) tensile force, and the jaws in tandem are capable of forming 12 × 2.4 in (40 × 8 ft) sheet metal skins. The addition of adapter jaws bolted to the standard jaws allows the machine to form long extruded frame members weighing up to 450 kg (1000 lb).

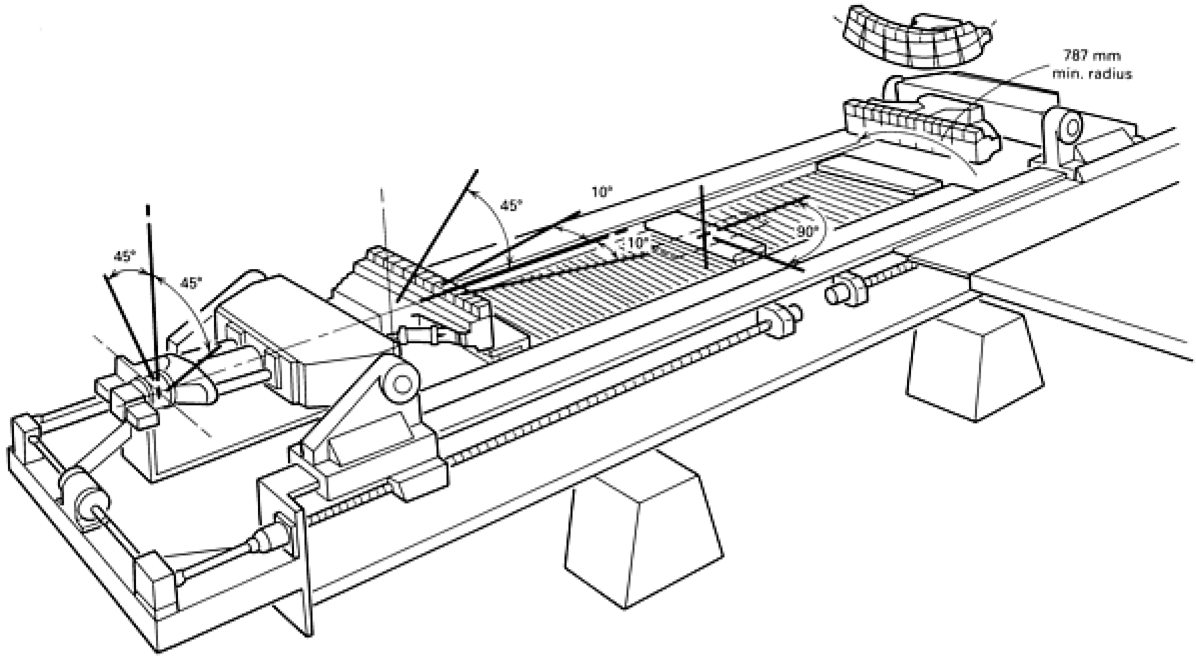

Fig. 15 Longitudinal stretch-forming machine with leadscrew-actuated jaws that can be curved and swiveled both horizontally and vertically

Accuracy

High-strength alloys, stainless steel, and titanium can be stretch formed to overall tolerances of ±0.25 mm (±0.010 in.) on workpieces bent to about a 178 mm (7 in.) radius. With springback allowance, bends can be controlled to $\pm\frac{1}{2}$°. Cross-sectional dimensions have been held to ±0.05 mm (±0.002 in.) by close control of raw material, as in the following example.

Example 7: Maintaining Close Tolerances on Grooves While Forming a Guide-Vane Shroud.

Because a cover strip had to slide easily, but without play, into the grooves of a stainless steel guide-vane shroud after the vanes were assembled, the width of the grooves had to be held within 0.10 mm (0.004 in.), as shown in Fig. 16. Strip was selected that had thickness variation within ±0.013 mm (±0.005 in.) because of the close groove tolerances that had to be met. The two U-bends forming the grooves were made in a press brake, and the guide slots were pierced in another operation. The shrouds were then contoured to a 190 mm ($7\frac{1}{2}$ in.) radius by stretch forming. The width of the work strip and the width of the grooves were held within the specified tolerances without using a filler for support.

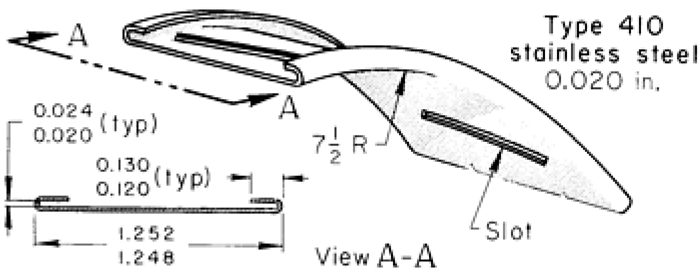

Fig. 16 Guide-vane shroud that was stretch formed without noticeable distortion to the 0.51/0.61 mm (0.020/0.024 in.) groove dimension. Dimensions given in inches

The shrouds were produced at a rate of 10 per hour. A typical production run was between 200 and 1000 pieces.

Tolerance Specifications. As shown in the preceding example, it may be necessary to control stock to a small thickness variation if close tolerances on the workpiece must be met. Ordinarily, it is good practice to allow about 25% of the finished-part tolerance as the stock thickness tolerance or the tolerance on any preformed dimension that could affect the accuracy of the stretch-formed dimension. In Example 7, the smallest tolerance was ±0.05 mm (±0.002 in.), and the stock thickness variation was controlled to ±0.013 mm (±0.0005 in.) (25% of workpiece tolerance).

Titanium is stretch formed both hot and cold (see the article "Forming of Titanium and Titanium Alloys" in this Volume). In cold stretch forming, shrinkage at right angles to the stretch is ordinarily controlled to ±0.79 mm ($\pm\frac{1}{32}$ in.) on bends with 229 mm (9 in.) radii. Angular variation on stretch bends in all materials is held to $\pm\frac{1}{2}$°.

In one plant, large rectangular tubing of copper alloy is stretch formed with a filler or with a flexible mandrel similar to those described in the article "Bending and Forming of Tubing" in this Volume. Tubes as large as 102 to 203 mm (4 to 8 in.) square and up to 4.9 mm (16 ft) long with 3.2 to 9.5 mm ($\frac{1}{8}$ to $\frac{3}{8}$ in.) walls can be formed. When mandrels are not used, distortion appears as concavity in the face away from the form block and some tapering toward the concave face, as shown in Fig. 17. These tubes are stretch formed to large radii with a ±0.81 mm ($\pm\frac{1}{32}$ in.) tolerance on the radius of the bend.

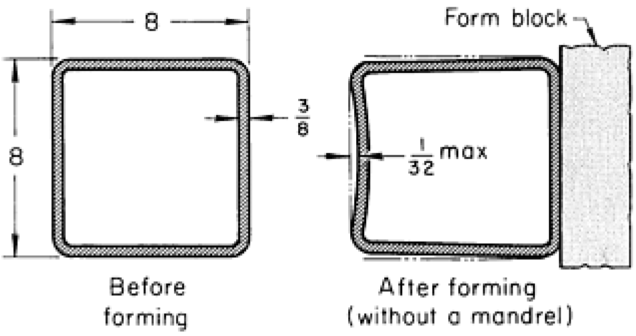

Fig. 17 Typical distortion of square copper alloy tubes in stretch forming. Dimensions given in inches

In the bending of large tubing, the bend is usually so shallow that the elastic limit of the metal is not reached without gross overbending, unless stretch-forming methods are used. As with conventional methods, overbending leads to unpredictable results. Tolerances on shallow bends can seem deceptively large. To hold a 3.0 m (10 ft) radius within ±0.79 mm ($\pm\frac{1}{32}$ in.) in a 10° arc means holding an overall tolerance of more than ±0.13 mm (±0.005 in.).

Surface Finish

Little can be done in stretch forming to improve surface finish because tool contact with the surface is incidental. However, some practices can be implemented to help preserve the original finish:

- Avoid overstretching. With most materials, 2 to 4% stretch is sufficient to achieve the results desired in stretch forming. The overstretching of some metals, such as aluminum, simply because they are ductile is a common mistake. This leads to the appearance of stretcher strains or other surface relief effects

- Plastic wiper shoes can be used in the compression forming or radial draw forming of aluminum alloys to avoid marring the surface. With stainless steel workpieces, well-finished plastic wiper shoes are used with drawing compounds similar to those used for severe deep drawing

An extra-fine finish is necessary in order to protect the surfaces of aluminum alloys directly in contact with the form block. Special practices used to preserve the finish include cleaning to eliminate abrasive dust particles, the use of polyvinyl chloride instead of a lubricant, and the use of special carrier sheets for protection of the surface during forming.

Stretch Forming Versus Alternative Methods

The following example compares stretch draw forming with alternative forming or drawing processes for a part manufactured by an automotive supplier. Stretch forming was competitive for the product considered.

Example 8: Stretch Draw Forming Versus Conventional Drawing.

An automotive plant that produced quarter-pillar lock panels from 0.89 mm (0.035 in.) thick commercial-quality 1008 steel by conventional drawing investigated the relative merits of stretch draw forming for this product. In drawing, an 8900 kN (1000 tonf) double-action press with conventional draw dies produced 525 pairs of panels per hour. In stretch draw forming, a 7200 kN (800 tonf) 2.74 × 1.52 m (108 × 60 in.), straight-side, single-action mechanical press with 1.22 m (48 in.) long stretch grippers was used. The production rate was the same as that for the conventional press when automatic loading and unloading were used, and the production cost was less. The process was changed to stretch draw forming.

In high-production forming, the principal disadvantage of stretch forming is the slowness of the hydraulic units used on the grippers, unless pumps of excessively high capacity are used. Mechanical units are available that have rapid response.

Operating Parameters

Size and configuration of the workpiece, material composition, type of forming operation used, machine and tooling used, and production requirements are among the variables that influence stretch forming. Operating parameters, such as force requirements, and the lubricant used must be determined prior to forming.

Force Requirements. The application of excessive tension in stretch forming can cause breakage of the workpiece. On the other hand, too little tension can result in poor contouring, wrinkling, or springback of the formed part. The force capacity of the machine required for stretch forming a part can be calculated by:

$$F = \left(\frac{Y_s + UTS}{2}\right) A \quad \textbf{(Eq 1)}$$

where F is the stretch-forming force (in pounds of force), Y_s is the yield strength of the material (in pounds per square inch), UTS is the ultimate tensile strength of the material (in pounds per square inch), and A is the cross-sectional area of the workpiece (in square inches). To convert from English units (pounds of force) to metric units (newtons), the force in pounds is multiplied by 4.448.

The estimate of the force required for stretch forming obtained with Eq 1 is generally an average. To compensate for work hardening, friction, more complex contours, and other variables, the force obtained mathematically should be increased by an additional 25% for some applications.

Lubrication. In most stretch forming, little or no lubrication is needed, because the movement between the work metal and the form block is minimal. On sheet steel, residual mill oil is usually sufficient, although some operators spray the stock with a light lubricating oil as it enters the forming area. Lubricants are sometimes intentionally avoided because they attract and retain dust particles that could mar the workpiece surface.

In the compression forming of copper alloys, low-carbon steel, and stainless steel, in which a shoe rubs hard against the part or there is considerable movement against the form block, white lead thinned with SAE 30 engine oil can be brushed on the workpiece before forming. In some shops, molybdenum disulfide is similarly used on low-carbon steel. Both lubricants resist heat and pressure and reduce friction. Polyvinyl chloride sheet can be used in place of a lubricant (and to embed dust particles) in the forming of microwave reflectors.

Introduction

SPINNING is a method of forming sheet metal into seamless, axisymmetric shapes by a combination of rotation and force. On the basis of techniques used, applications, and results obtainable, the method can be divided into two categories: manual spinning and power spinning. This article will discuss the spinning of sheet; tube spinning is covered in the article "Tube Spinning" in this Volume.

Manual Spinning

Manual spinning involves no appreciable thinning of the work metal. The operation is accomplished with the use of a lathe, and it consists of pressing a tool against a circular metal blank that is rotated by the head-stock. The blank is usually forced over a mandrel of a predetermined shape, but simple shapes can be spun without a mandrel. Various mechanical devices are used to increase the force that can be applied to the workpiece.

Any metal that is ductile enough to be cold formed by other methods can be spun. Most spinning is done without applying heat to the workpiece; the metal is sometimes preheated to increase ductility or to allow thicker sections to be spun.

Applicability

Manual spinning is used to form flanges, rolled rims, cups, cones, and double-curved surfaces of revolution (such as bells). Several typical shapes formed by manual spinning are shown in Fig. 1. Products include light reflectors, tank ends, covers, housings, shields, and components for musical instruments. Manual spinning is also extensively used for the production of aircraft and aerospace components, often with mechanical assistance for increased force.

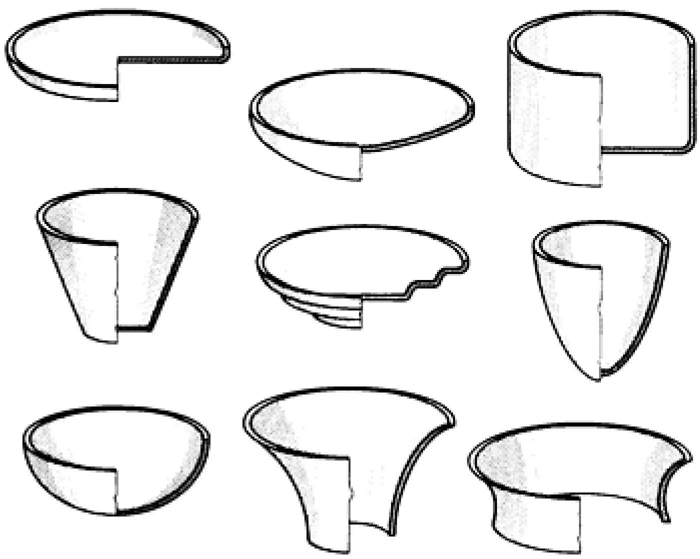

Fig. 1 Typical conical, cylindrical, and dome shapes that can be formed by manual spinning.

The practical maximum thickness of low-carbon steel that can be spun without mechanical assistance is 3.2 mm ($\frac{1}{8}$ in.). In this thickness, the diameter can be as great as 1.8 m (72 in.). Diameters can be greater when the sheet steel is thinner, but the maximum practical diameter is often limited by the availability of equipment. The upper limit of thickness increases as work metal ductility increases or as strength decreases. For example, the manual spinning of aluminum as thick as 6.4 mm ($\frac{1}{4}$ in.) is feasible.

Advantages and Disadvantages

Manual spinning has several advantages over a competitive process such as press forming:

- Tooling costs less, and investment in capital equipment is relatively small
- Setup time is shorter
- Design changes in the workpiece can be made at minimum expense
- Changes in work metal composition or thickness require a minimum of tool changes

The disadvantages of manual spinning include:

- Skilled operators are required, because uniformity of results depends greatly on operator skill
- Manual spinning is usually slower than press forming
- Available force is more likely to be inadequate in manual spinning than in press forming

Equipment

A simple tool and workpiece setup for manual spinning is shown in Fig. 2(a). The mandrel is mounted on the headstock of a lathe. The circular blank (workpiece) is clamped to the mandrel by the follower block. An antifriction center is used between the follower and the tailstock spindle, and pressure is applied at the tailstock by means of a screw or by air or hydraulic pressure, depending on the size and type of lathe. The tool rest and pedestal permit the support pin (fulcrum) to be moved to various positions by swinging the tool rest and moving the support pin from one hole to another as needed. Spinning is done by manually applying the friction-type spinning tool as a pry bar.

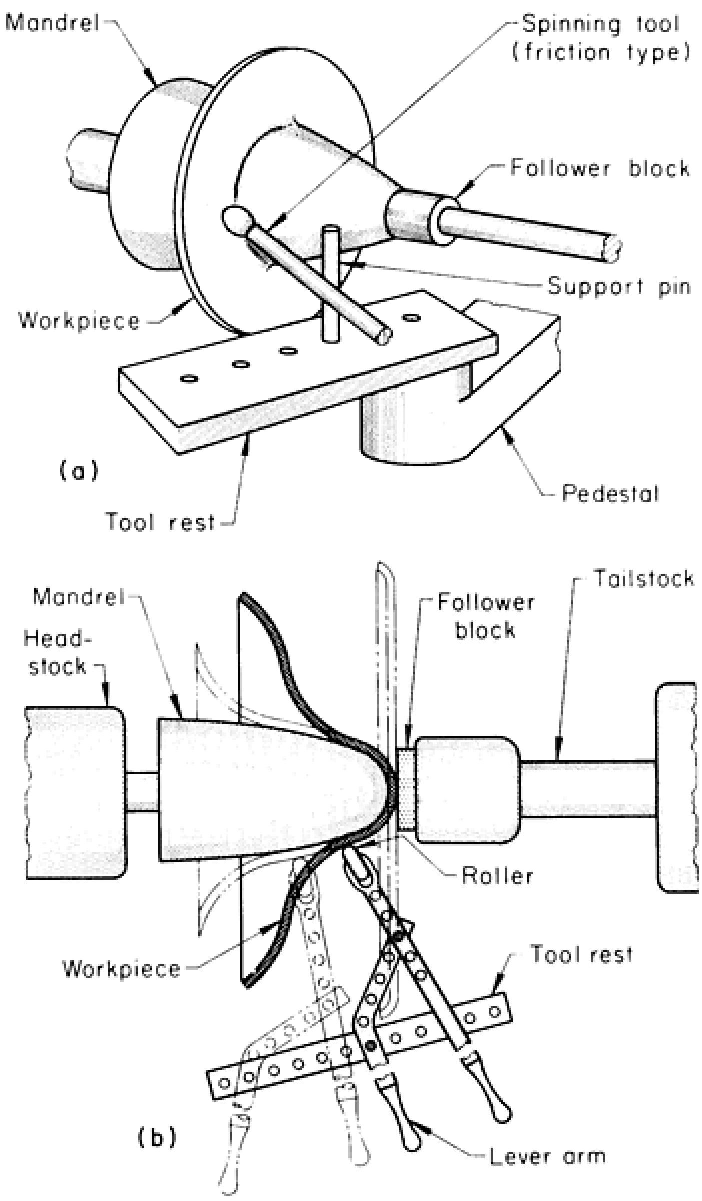

Fig. 2 Manual spinning using a lathe. (a) Simple setup using a hand tool applied as a pry bar. (b) Setup using scissorlike levers and roller spinning tool.

Figure 2(b) shows a more complex setup for manual spinning. In this configuration, the spinning tools (rollers) are mounted in the fork sections of long levers, and the tool support has a series of holes for the rapid changing of tool position. The tool is manipulated by pulling, pushing, or pivoting the two scissorlike handles, with the roller against the workpiece.

Lathes. Several sizes of standard horizontal spinning lathes are available that can spin blanks ranging from 6.4 mm to 1.8 m ($\frac{1}{4}$ to 72 in.) in diameter. Special pit lathes permit the spinning of blanks as large as 4.9 m (192 in.) in diameter. Standard lathes can be fitted with special chucks for making oval parts. Lathes should be equipped with variable-speed drives to permit quick changes of speed as judged necessary by the operator.

Mandrels, also known as form blocks or spin blocks, are usually made of seasoned hard-maple wood. Most hardwood mandrels are constructed by gluing strips of 25 to 50 mm (1 to 2 in.) thick maple into the main block to create a cross-laminated structure, then turning the glued structure to the desired shape. Such mandrels are stronger and more durable than mandrels turned from a solid block. Some wooden mandrels are steel reinforced at the ends and at small radii to ensure maintenance of radii in the spun workpieces. Sharp corners can be produced in workpieces by spinning them over mandrels cornered with steel; but minimum inside radii of 1.6 mm ($\frac{1}{16}$ in.) are more common than sharp corners, and 3.2 mm ($\frac{1}{8}$ in.) minimum radii are preferred where possible.

Some mandrels are constructed of alternating wood and steel plates or rings in order to obtain a more economical yet durable mandrel. Other materials include fiber compositions, steel, cast iron, aluminum, magnesium, and plastic-coated wood. Few mandrels are made entirely of heavy metals such as steel and cast iron, except for close-tolerance work. Cored castings of these metals are then preferred, because of the weight savings. Solid steel or cast iron mandrels must be statically balanced, and for use at high speed, they should also be dynamically balanced.

Spinning Tools. Simple spinning tools are usually made by forging carbon or low-alloy tool steels (such as W1 or O1) to the desired shape, hardening the working ends to about 60 HRC, and polishing them. Several typical shapes are illustrated in Fig. 3. Tools of shaped aluminum bronze are also satisfactory, especially for the spinning of steel. Hardwood tools have performed satisfactorily in spinning thin-gage ductile metals.

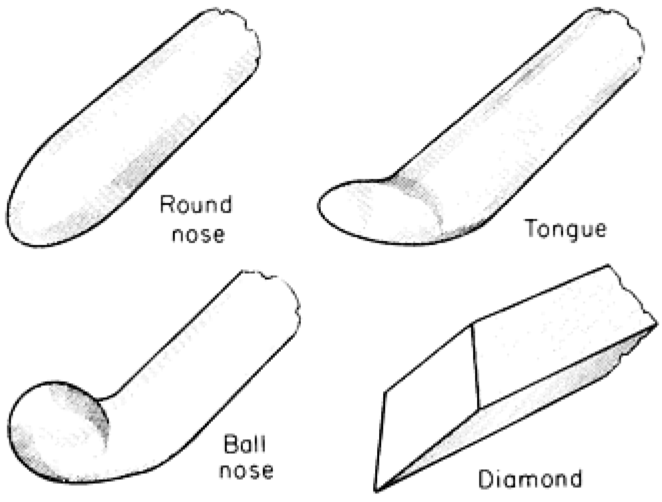

Fig. 3 Typical shapes of working ends of tools used for manual spinning. Round nose, tongue, and ball nose tools are for spinning; the diamond tip is for trimming.

With the lever arrangement (Fig. 2b), the tools usually consist of rollers (sometimes called tool rings) mounted in forks. Most rollers are made of hardened tool steel or of aluminum bronze.

Manual Spinning Practice

Because of the low tooling cost, manual spinning is extensively used for prototypes and for production runs of 1000 pieces or fewer. Larger lots can usually be produced at lower cost by power spinning or press forming.

For example, the part in the middle of the second row in Fig. 1 is a stainless steel cover for a food-processing machine, produced in one plant at the rate of 100 per year. The parts were produced satisfactorily by manual spinning with only two hardwood mandrels, the cost of which was only a fraction of the tooling cost for the press forming of the same shape.

Conical parts (such as the shape on the left in the middle row in Fig. 1) are ideal for spinning because only one tool is required; drawing in dies would require four or five operations. Many such cones, depending on their included angle, can be spun in one operation at a moderate production rate. Therefore, manual spinning is often used for quantities up to medium production (<1000 units). For large-quantity production, power spinning is generally less expensive than manual spinning.

Control of quality, including freedom from wrinkles and scratches and maintenance of dimensional accuracy, is largely a function of operator skill. Dimensional tolerances increase as the diameter of the blank increases, as indicated in Table 1. These tolerances are typical of demands for commercial products and parts for aerospace applications.

Table 1 Typical dimensional tolerances for manual spinning

Diameter of blank		Tolerance			
		Commercial		Aerospace	
m	in.	mm	in.	mm	in.
Up to 0.305	Up to 12	±0.4	$\pm\frac{1}{64}$	±0.20	±0.008
0.33-0.90	13-36	±0.8	$\pm\frac{1}{32}$	±0.38	±0.15
0.94-1.37	37-54	±1.6	$\pm\frac{1}{16}$	±0.51	±0.020
1.4-2.4	55-96	±3.2	$\pm\frac{1}{8}$	±0.76	±0.030

Speeds that are best suited to manual spinning depend mainly on work metal composition and thickness. For example, a given blank of stainless steel is successfully spun at 60 m/min (200 surface feet per minute, or sfm), and speed is determined by "operator feel" to be maximum for the conditions. Under otherwise identical conditions, changing to an aluminum blank will permit speeds of 120 to 180 m/min (400 to 600 sfm). Similarly, if the thickness of the stainless steel blank were decreased to one-half the original thickness (no other changes), speed could be safely doubled or tripled.

Selection of optimal speed depends largely on "operator feel." In many spinning operations, speed is changed (usually increased) during the operation by means of a variable-speed drive on the headstock.

Lubricants should be used in all room-temperature spinning operations, regardless of work metal composition, workpiece shape, or type of spinning tools used. The usual practice is to apply the lubricant to the blank with a swab or brush before loading the blank into the machine. In some cases, additional lubricant is added during operation as judged necessary by the operator. The need for additional lubricant depends on the tenacity of the lubricant used and on blank-rotation speed.

The most important property of a lubricant used for spinning is its ability to adhere to the rotating blank. Ordinary cup grease is often used. It can be heated to reduce its viscosity, thus making it easier to apply to the blank. Upon application to the cold blank, the viscosity of the grease increases. In addition, cup grease can be easily removed.

Other lubricants used for spinning include soaps, waxes and tallows (and proprietary mixtures of two or more of these materials), and pigmented drawing compounds. All of these, however, are more difficult to remove than simple grease. Therefore, the more tenacious lubricants are not used if an easier-to-remove lubricant will provide acceptable results.

Power Spinning

Power spinning is also known as shear spinning, because in this method metal is intentionally thinned by shear forces as high as 3.5 MN (400 tonf). Power spinning is used in two broad areas of application: cone spinning and tube spinning. In cone spinning, the deformation of the metal from the flat blank is in accordance with the sine law (see the section "Mechanics of Cone Spinning" in this article).

Virtually all ductile metals can be processed by power spinning. Products range from small hardware items made in large quantities (metal tumblers, for example) to large components for aerospace applications in unit or low-volume production.

Blanks as large as 6 m (240 in.) in diameter have been successfully power spun. Plate stock up to 25 mm (1 in.) thick can be power spun without applying heat. When heated, blanks as thick as 140 mm ($5\frac{1}{2}$ in.) have been successfully spun.

Conical and curvilinear shapes are those most commonly produced from flat (or preformed) blanks by power spinning. The mechanics of the process should be known and the rules followed when planning manufacturing processes that include power spinning.

Mechanics of Cone Spinning

The application of shear spinning to conical shapes is shown schematically in Fig. 4. The metal deformation is such that forming is in accordance with the sine law, which states that the wall thickness of the starting blank, t_1, and that of the finished workpiece, t_2, are related as follows:

$$t_2 = t_1 (\sin \alpha)$$

where α is one-half the apex angle of the cone. When spinning in accordance with the sine law, the axial thickness is the same as the thickness of the starting blank (Fig. 4).

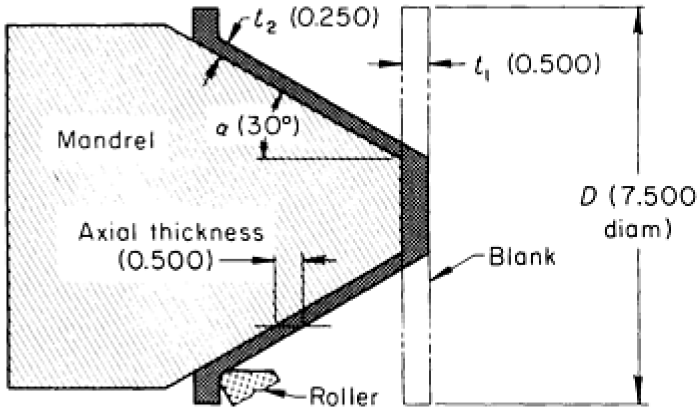

Fig. 4 Setup and dimensional relationships for the one-operation power spinning of a cone. *D*, mandrel diameter; t_1, original (blank) thickness; t_2, spun thickness; α, included angle. Dimensions given in inches.

When spinning cones to small angles (<35° included angle), the best practice is to use more than one spinning pass with a different cone angle for each pass, as illustrated in Fig. 5. When using this technique, the workpiece is annealed or stress relieved between passes.

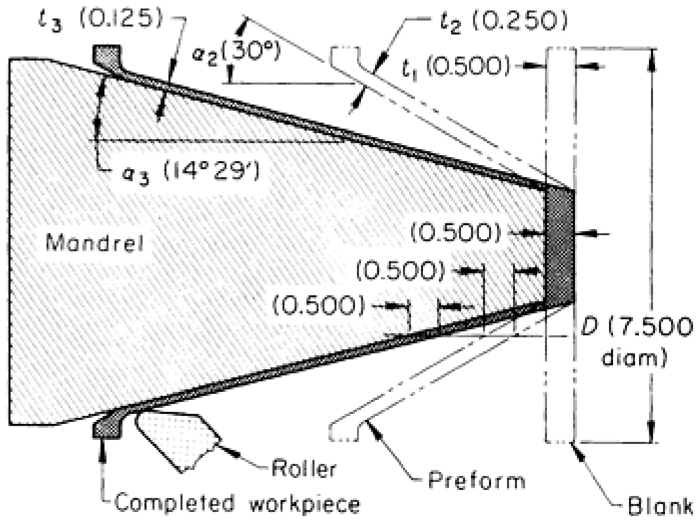

Fig. 5 Setup and dimensional relationships for the two-operation spinning of a cone to a small angle (35° included angle). Dimensions given in inches.

This practice permits a high total reduction while maintaining a practical limit of 50 to 75% between process anneals. The reduction between successive annealing operations is determined by the maximum acceptable limits of deformation for the metal being spun (Table 2); this value is obtained by multiplying t_1 by a factor (0.5 for 50%, 0.25 for 75%, and so on) and then dividing the result by t_1 to obtain the sine of the required half angle.

Table 2 Recommended maximum reductions for the single-pass power spinning of various metals

Metal	Maximum reduction, %	
	Cone	Hemisphere

Aluminum alloys		
2014	50	**40**
2024	50	**. . .**
3000	60	**50**
5086	65	**50**
5256	50	**35**
6061	75	**50**
7075	65	**50**
Pure beryllium[(a)]	35	**. . .**
Copper	75	**. . .**
Molybdenum[(a)]	60	**45**
Nickel-base alloys		
Waspaloy	40	**35**
René 41	40	**35**
Steels		
4130	75	**50**
4340	70	**50**
6434	70	**50**
D6ac	70	**50**
H11	50	**35**

Stainless steels		
Type 321	75	**50**
Type 347	75	**50**
Type 410	60	**50**
17-7PH	65	**45**
A286	70	**55**
Titanium[(a)]		
Commercially pure	45	...
Ti-6Al-4V	55	...
Ti-3Al-13V-11Cr	30	...
Ti-6Al-6V-2.5Sn	50	...
Tungsten[(a)]	**45**	...

(a) Hot spun

Even in multiple-pass spinning, the original blank diameter is retained, and the exact volume of material is used in the final part. At any diameter of either the preform or the completed workpiece, the axial thickness equals the thickness of the original blank. For example, if a flat plate has a diameter of 190 mm ($7\frac{1}{2}$ in.) and a thickness of 12.5 mm ($\frac{1}{2}$ in.), the spun preform has this same 12.5 mm ($\frac{1}{2}$ in.) axial thickness, but the wall thickness is only 6.4 mm ($\frac{1}{4}$ in.) (t_1, Fig. 5), thus satisfying the sine law. Similarly, the final workpiece has an axial thickness of 12.5 mm, ($\frac{1}{2}$ in.), but in accordance with the sine law, it has a wall thickness of only 3.2 mm ($\frac{1}{8}$ in.) (t_3, Fig. 5).

Effects of Deviation From the Sine Law. Deviation from the sine law is usually expressed in terms of overreduction or underreduction. In overreduction, the final thickness of the workpiece is less than that dictated by the sine law; in underreduction, the thickness is greater. In overreduction, the flange will lean forward; in underreduction, the flange will lean backward. If a thin blank is spun with severe underreduction, the flange will wrinkle. This phenomenon corresponds to a deep-drawing operation in which the blankholder pressure is insufficient.

In power spinning, overreduction has an additional effect on the shape of the workpiece. As the workpiece is overreduced, back extrusion can occur (Fig. 6). For a given amount of reduction, the likelihood of back extrusion increases with increasing mandrel angle (Fig. 6).

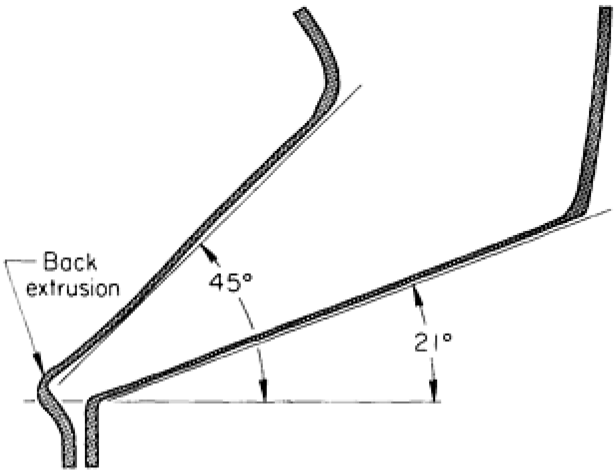

Fig. 6 Back extrusion as a result of overreduction in the power spinning of low-carbon steel.

The phenomenon of back extrusion in spinning is explained in terms of the compressive stress in the spun workpiece that pushes the spun section backward. If the tailstock of the machine is removed, it is possible to obtain curvilinear shapes on a conical mandrel by varying the amount of overreduction during spinning.

Machines for Power Spinning

Most power spinning is done in machines specially built for the purpose. The significant components of such a machine are shown in Fig. 7. Although Fig. 7 illustrates the power spinning of a conical shape, similar machines are used for the spinning of tubes (see the article "Tube Spinning" in this Volume).

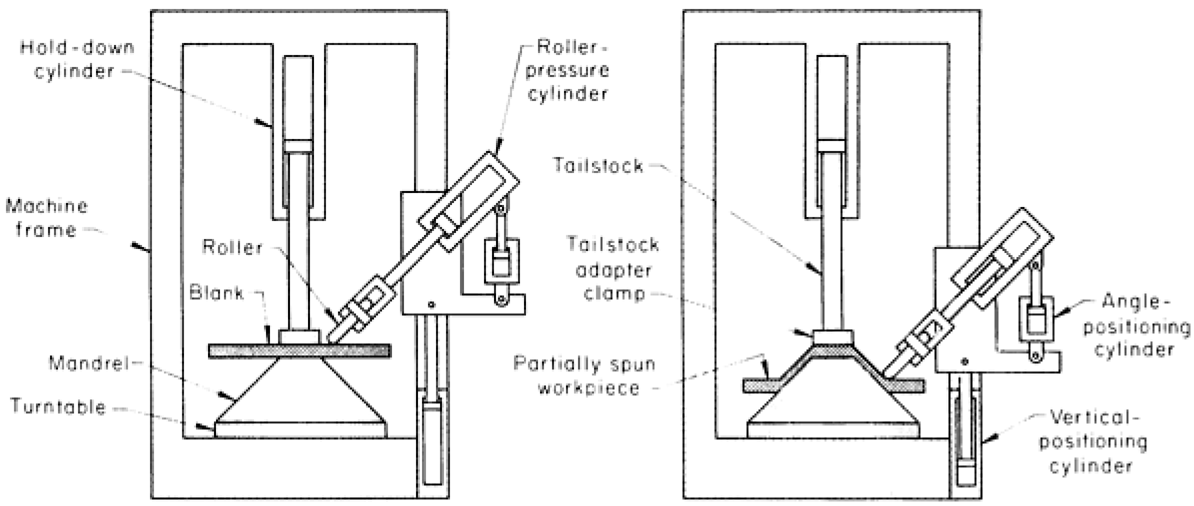

Fig. 7 Schematic of power spinning in a vertical machine.

Machines for power spinning are usually described by specifying the diameter and length (in inches) of the largest workpiece that can be spun and the amount of force that can be applied to the work. It is also common practice to specify that the machine can spin a given thickness of metal at a 50% reduction in thickness in one pass.

The capacity of spinning machines ranges from 455 × 380 mm (18 × 15 in.) at 18 kN (4000 lbf) to machines capable of spinning workpieces as large as 6 m (240 in.) in diameter × 6 m (240 in.) long. Force on the work can be as great as 3.5 MN (800,000 lbf). Machines have been built that spin steel 140 mm ($5\frac{1}{2}$ in.) thick.

Spinning machines can be vertical or horizontal. Machines used for spinning workpieces 1.8 m (70 in.) or more in diameter are usually vertical because they are better suited to handling large work.

Machines for power spinning can be automated to various degrees. Most spinning machines use template guides that control the shape and accuracy of the workpiece. Most machines used for production spinning are semiautomatic; that is, they are loaded and unloaded by the operator, but the entire spinning cycle is controlled automatically. Machines can also be equipped with automatic loading and unloading devices, thus making them fully automatic.

Tools for Power Spinning of Cones

Mandrels, rollers, and other tools are subjected to more rigorous service in power spinning than in manual spinning; therefore, more careful consideration must be given to design and materials of construction.

Mandrels. A typical mandrel profile is illustrated in Fig. 8. Dimensions *A* and *B* and angle α can vary as required. Usual practice is to have, first, an integral flange to permit the mandrel to be bolted to the headstock and, second, a boss of suitable diameter and at least 16 mm ($\frac{5}{8}$ in.) in thickness that fits into the headstock of the machine (Fig. 8). Radius *R* can vary from a minimum of 0.8mm ($\frac{1}{32}$ in.) to a round nose.

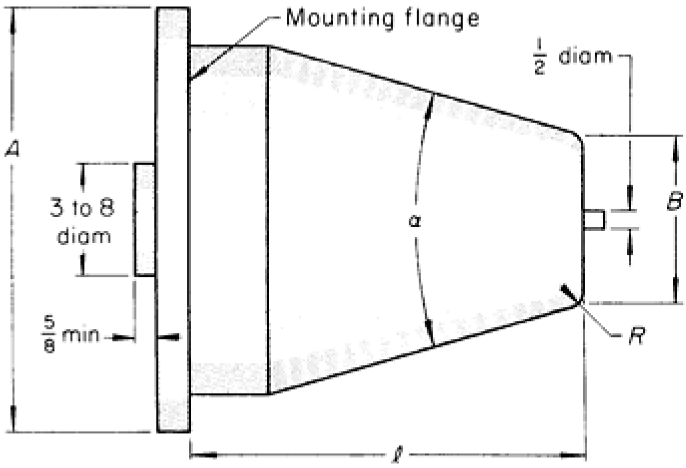

Fig. 8 Typical profile of a mandrel for the power spinning of cones. Dimensions given in inches.

Mandrel wear or failure is frequently a problem in the power spinning of conical shapes. The mandrels used in production spinning must be hard in order to resist wear, and they must resist the fatigue resulting from normal eccentric loading. Failure is often caused by spalling (flaking off). Mandrels can also be damaged by the rollers plunging into the workpiece at the start of metal flow. The need for plunging can sometimes be eliminated by machining a ring on the preform to a depth equal to the depth the rollers would otherwise be plunged. This technique permits the rollers to enter the machined space before they start moving along the mandrel, thus eliminating the severe stress on the mandrel as spinning is begun.

Materials selection for the mandrels used in cone spinning depends primarily on the number of identical workpieces to be spun. Based on quantity, the most commonly used materials are:

- Gray iron (as-cast) for the low-production spinning of soft metals (10 to 100 pieces)
- Alloy cast iron (sometimes flame hardened in areas susceptible to high wear), for spinning 100 to 250 pieces
- 4150 or 52100 steel hardened to about 60 HRC, for spinning 250 to 750 pieces
- Tool steels such as O6, A2, D2, or D4 hardened to 60 HRC or slightly higher, for high production

The finish of the mandrels should be no rougher than 1.5 μm (60 μin.). The various diameters should be within ±0.025 mm (±0.001 in.) concentric with each other within approximately 0.05 mm (0.002 in.) total indicator reading.

Rollers. Three types of rollers are shown in Fig. 9. Rollers are usually 305 to 510 mm (12 to 20 in.) in outside diameter, depending on the type and size of the spinning machine. Roller widths are usually 50 to 75 mm (2 to 3 in.), and inside diameters range from 255 to 380 mm (10 to 15 in.). The shape of the rollers depends largely on the shape of the workpiece to be spun. Full-radius rollers (Fig. 9a) are usually used to produce curvilinear shapes, while those illustrated in Fig. 9(b) and 9(c) are preferred for the spinning of cones.

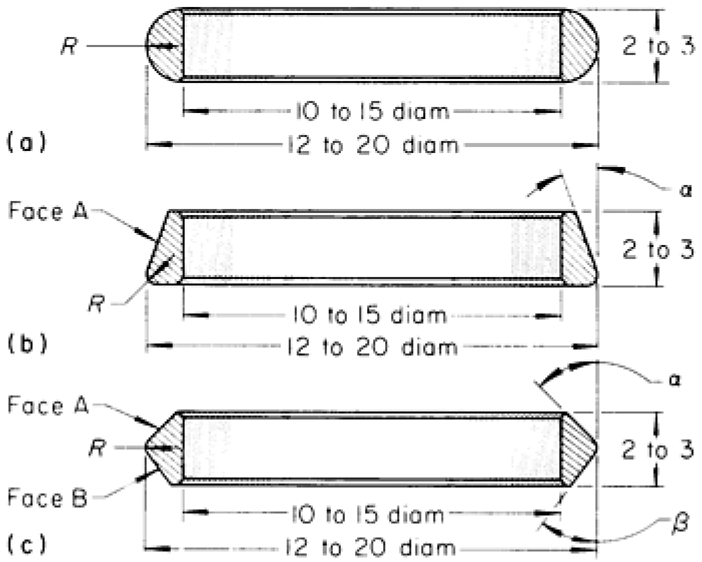

Fig. 9 Typical rollers used in the spinning of cones and hemispheres. (a) Full-radius roller. (b) Corner roller. (c) Thinning roller. Dimensions given in inches.

Angle α shown in Fig. 9(b) and 9(c) is necessarily varied to suit the work being spun (particularly the angle of the cone). This angle is intended for clearance and should be such that the work metal does not touch face A (Fig. 9b) or either face A or face B (Fig. 9c). The radius *R* should not be less than the final wall thickness.

The type of roller illustrated in Fig. 9(b) is widely used in cone spinning. A typical setup, using two of these rollers opposed, is illustrated in Fig. 10. When two rollers are used to spin a part from flat plate, the rollers are set the same. However, when spinning is done from a preformed shape, common practice is to make one the lead roller and to set it ahead of the other by 1.6 to 3.2 mm ($\frac{1}{16}$ to $\frac{1}{8}$ in.). If more than two rollers are used, this increment is continued between successive rollers. The angle between the axis around which the rollers revolve and the workpiece (angle α, Fig. 10) is usually about 10°, while the angle between the same roller axis and the peripheral face of the roller (angle β, Fig. 10) may vary and is shown in Fig. 10 as approximately 30°.

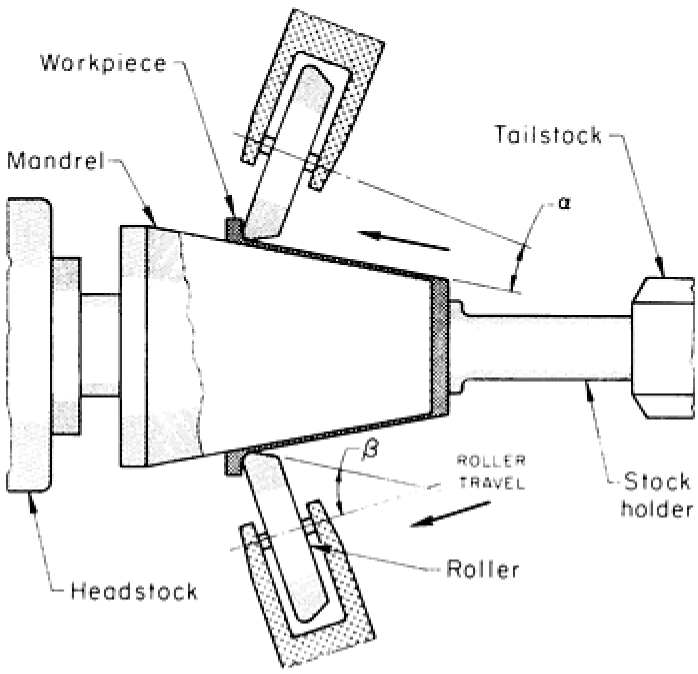

Fig. 10 Relative positions of rollers and workpiece in the spinning of a cone.

Rollers are made from a variety of hard materials. The five materials most widely used for the power spinning of conical shapes, in order of ascending wearability and cost, are W2 tool steel, O6 tool steel, D2 tool steel, D4 tool steel, and carbide. Choosing among these materials is usually done on the basis of quantity of workpieces to be spun. The less costly W2 and O6 tool steels are generally suitable for low-to-medium production quantities. Tool steels D2 and D4 are preferred to high production quantities. Carbide is seldom used except for specialized applications in which the need has been proved and the high cost can be justified.

Rollers made from any of the above tool steels should be hardened to 60 to 65 HRC. All rollers should be polished to a maximum surface roughness of 0.25 μm (10 μin.).

Auxiliary tools for cone spinning include tailstock adapters, tracer templates, tracer followers, and stripping devices. A tailstock adapter clamps the work to the mandrel (Fig. 7) and is made of carbon or alloy steels or of tool steel. The clamping face of the tailstock adapter must be ground square to the spindle axis.

Tracers are used to spin workpieces that vary in wall thickness or shape. Tracer templates are made of low-carbon steel, such as 1020, that is, 3.2 to 4.8 mm ($\frac{1}{8}$ to $\frac{3}{16}$ in.) thick. Large templates have lightening holes for easier handling. Tracer templates are made to the same standards of accuracy as dies and similar tools. Tracer followers can be ball bearings or hardened tool steel fingers, depending on cone shape.

Stripping devices can be full rings or fork-type fingers, attached to the roller carrier. The need for stripping devices depends on the size and shape of the workpiece.

The use of preforms is common in cone spinning when the included angle of the cone is less than 35° or when the percentage of wall reduction is high. Preforms are usually prepared by cold forming in a die, although hot forging or machining or a combination of both can be used. Some preforms are made by spinning.

Speeds and Feeds for Cone Spinning

Most metals spin best at high speeds. The minimum speed considered to be practical is about 120 m/min (400 sfm), but speeds this low are seldom used except for the spinning of small-diameter workpieces. Machine spindles sometimes cannot rotate fast enough with such workpieces to achieve the desired surface speed. Speeds of 305 to 610 m/min (1000 to 2000 sfm) are most widely used, regardless of work metal composition, workpiece shape, or reduction per pass.

Feed. Most cone-spinning operations are done at feeds of 0.25 to 2 mm/rev (0.01 to 0.08 inches per revolution, or ipr). In practice, however, feeds are usually calculated in millimeters per minute (mm/min) or inches per minute (ipm). Most machines used in cone spinning are equipped with electronic or hydraulic devices that steplessly change the rate of feed as the diameter on which the rollers are working changes continuously. The rate of feed usually ranges from 38 to 380 mm/min ($1\frac{1}{2}$ to 15 ipm).

Feed rate is important, because it controls the workpiece finish and the fit of the workpiece to the mandrel. With all other factors constant, an increase in feed rate will make the workpiece fit tighter on the mandrel, and the finish of the workpiece will coarsen. On the other hand, a decrease in feed rate will cause a loose fit, and workpiece finish will improve. The diameter of the mandrel should be the same as the inside diameter required on the workpiece (no allowance for springback), and the workpiece should be spun to fit the mandrel. The fit may be loose, snug, or tight.

To find the optimal combination of speed, feed, and pressure, a few pieces should be spun experimentally when a new job is set up. During continuous operation, the temperature of the mandrels and spinning tools changes; therefore, after the first hour or so, it is often necessary to adjust the pressure, speed, and feed for uniform results.

Power Spinning of Hemispheres

The use of preforms to control percentage of reduction has enabled power spinning to be applied to the forming of hemispheres, ellipses, ogives, and in general, any curvilinear surface of revolution. However, the design of the preform for curvilinear shapes is more complicated than that for conical shapes. In the spinning of conical shapes, it is possible to find an axial thickness of the spun part that corresponds to the thickness of the blank (Fig. 5). No such relationship exists for a curvilinear surface. In the path from the pole to the equator, the axial thickness of the metal on a hemisphere changes from stock thickness at the pole to infinity at the equator (the inverse of sin 0° being infinity). The blank thickness must be back tapered to compensate for the change in thickness that will take place during spinning. This is shown in Fig. 11; the machined taper started at 3.8 mm (0.150 in.) in thickness (in the center of the blank) and ended at 7.6 mm (0.300 in.) in thickness at the circle where the 30° radial line of the sphere was projected to the blank. At the corresponding 45° line, the blank thickness was 5.38 mm (0.212 in.); at the 15° line, 14.73 mm (0.580 in.). Below the 30° line, however, the reduction was greater than permissible for the material, and the operation was planned as if spinning a cylinder. The blank for this portion had a flange with a thickness proportional to the percentage of reduction.

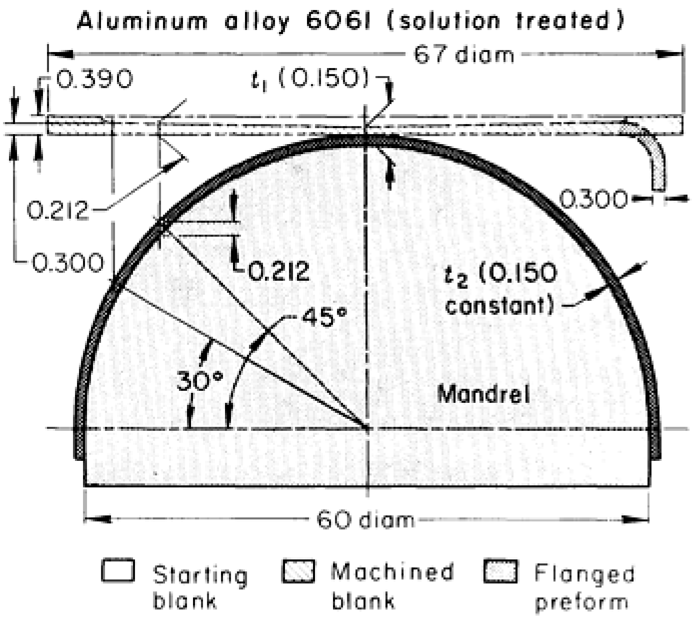

Fig. 11 Hemisphere spun from a machined and preformed blank. Dimensions given in inches.

A usable blank can be designed by first finding in Table 2 the allowable reduction for the work metal to be used. A beginning stock thickness should be selected that, with the maximum reduction, will give the thickness desired on the sphere. The ratio of finished stock thickness to original stock thickness is then taken as the sine of an angle, which will be the angle of the surface at the latitude at which preforming must start. Beyond this point, the reduction required to make the hemisphere will be greater than is permissible for the work metal. There will be no reduction at the pole, because at that point blank thickness and final thickness will be the same. At 45° from the pole, final part thickness will be 0.707 times the original thickness (sin 45° = 0.707). At a corresponding circle on the blank, therefore, the original thickness must be 1.414 times the final part thickness. Other latitudes can be similarly chosen, and necessary stock thickness at a corresponding circle on the blank determined. Preforming must start at the circle corresponding to the limiting latitude (the point where the maximum permissible reduction has taken place). In a cross-section view, the circles resulting from the above method will appear as points, and the thickness of the stock at these points can be determined. When the points are laid out, a dozen or more points are connected to yield the final part shape.

In modern shops, such calculations are usually performed with the aid of a computer. It is common practice in such systems to use 1200 to 1500 points on a shape such as a large hemisphere.

The thickness of the starting blank can be obtained by multiplying the known thickness of the finished part by an appropriate factor. Similarly, by dividing the starting blank thickness by the appropriate factor, the thickness of the finished part is obtained. The factors are related to the percentage of reduction and are the reciprocal of the difference between the percentage reduction (expressed as a decimal) and one, as follows:

- For 50% reduction, use a factor of 2
- For 66 $\frac{2}{3}$% reduction, use a factor of 3
- For 75% reduction, use a factor of 4

- For 80% reduction, use a factor of 5

Use of this system is illustrated in the following example.

Example 1: Forming a 1.5 m (60 in.) Diam Hemisphere by Power Spinning.

Large hemispheres (Fig. 11) were power spun from solution-treated aluminum alloy 6061 using the following calculations. From Table 2, it was determined that a 50% reduction could be made with this alloy. Preliminary calculations for thickness of the starting blank were as follows:

$$
\begin{aligned}
t &= \text{final wall thickness} \cdot \text{factor for percentage reduction} \\
&= 3.81 \text{ mm } (0.15 \text{ in.}) \cdot 2 \\
&= 7.62 \text{ mm } (0.300 \text{ in.})
\end{aligned}
$$

In calculating the blank thickness at various points on the sphere, it was found that at the pole, or 90° point, the thickness had to be reduced to 3.81 mm (0.150 in.) and that some reduction was required out to a point directly above the 30° tangency on the hemisphere, where the thickness of the starting blank had to be 7.62 mm (0.300 in.). Beyond this point, a flange would be preformed by spinning, and an additional thickness would be required. It was estimated that an increase in blank thickness of 30% would be enough, and initial blank thickness established at 9.91 mm (0.390 in.).

Machining of the blank to graded thickness was done in a tracer-controlled vertical boring mill, with the blank held on a vacuum chuck. After machining, the flange was preformed to the desired contour by conventional power spinning, accomplishing a reduction in wall thickness that provided a uniform 7.62 mm (0.300 in.) wall.

Final spinning was accomplished in one pass of the rollers after the alloy was given a controlled amount of room-temperature aging (usually 13 to 18 h). During final spinning one roller led the other by a vertical offset of 1.6 to 3.2 mm ($\frac{1}{16}$ to $\frac{1}{8}$ in.), using 19 mm ($\frac{3}{4}$ in.) radius tool rings at a feed of about 2.3 mm/rev (0.09 ipr). Speed varied from 300 rpm maximum down to 40 rpm at the flange.

The procedure described in Example 1 has also been successfully applied to the forming of hemispheres and ellipses 152 mm to 1.8 in (6 to 70 in.) in diameter from 17-7 PH and type 410 stainless steels, from alloy steels such as 4130 and 4140, and from aluminum alloys 5086, 2014, and 2024 (as well as 6061). In one case, the procedure was used for the hot spinning of an ogive 762 mm (30 in.) in diameter and length from molybdenum.

Hot Spinning of Hemispheres. The use of heat for decreasing the strength and increasing the ductility of the work metal is sometimes required because the machine capacity is insufficient for cold forming the thickness to be spun or because the room-temperature ductility of the work metal is too low. Hot spinning is done only when necessary, because heating, subsequent cleaning, and increased tool deterioration all contribute to increased cost.

Lubricants and Coolants for Power Spinning

Power spinning requires the use of a fluid that serves as both a lubricant and a coolant. Because of the large amount of heat generated, a water-base fluid is most commonly used. Usually, a colloidal suspension of zinc in lithium soap or molybdenum disulfide paste is mixed with water to function as the lubricant. These lubricant-coolant combinations are satisfactory for most metals, although zinc-free lubricants and coolants should be used for the spinning of stainless steel to avoid surface contamination.

Various oils and oil mixtures, such as 10% lard oil in kerosene, have also been successfully used for power spinning. Regardless of composition, the fluid must be free flowing and applied by pumps in copious amounts, or both workpieces and tools will be damaged from heat.

When spinning aluminum or stainless steel, the workpieces or mandrels or both are sometimes coated with the lubricant before spinning. During spinning, workpieces and tools are flooded with a coolant, such as an emulsion of soluble oil in water.

Effects of Spinning on Work Metal Properties

Power spinning is a severe cold-working operation and therefore has a marked effect on the mechanical properties of the work metal. A well-defined and directional grain flow pattern is produced by power spinning. The surface finish of a spun workpiece is usually good enough so that no additional machining is required after spinning. Spun finishes are commonly about 1.5 μm (60 μin.), although finishes as smooth as 0.5 μm (20 μin.) have been produced by power spinning.

Strength and Hardness. In spinning, tensile and yield strengths increase, and ductility decreases. The magnitude of effect depends on the amount of wall reduction and on the susceptibility of the metal to work hardening.

In many applications, the increase in strength caused by spinning is highly desirable because it eliminates the need for heat treating. In other applications, the change in mechanical properties is not desired, and the workpieces must be annealed after spinning.

To measure the work hardening in the deformation zone, Rockwell F (HRF) readings were taken on the cross section of a spun copper workpiece that was reduced 43%. The results are shown in Fig. 12. It is evident that the area near the roller contact has higher hardness than the area at the mandrel side.

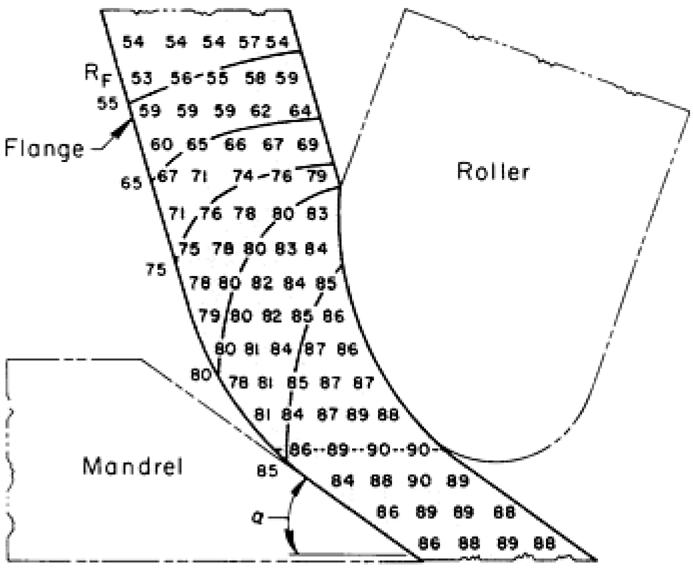

Fig. 12 Hardness distribution (HRF) in a copper workpiece reduced 43% by spinning.

Assembly by Spinning

Spinning is frequently used for less conventional applications than those described earlier in this article and in the article "Tube Spinning" in this Volume. It is often the least expensive means of joining two or more parts to form an assembly. For example, a tube can be inserted through a hole in a plate, and the protruding end of the tube can then be spun to secure it to the plate. Small parts are assembled by this technique with a special tool rotated by a drill press.

Introduction

RUBBER-PAD FORMING, also known as flexible-die forming, employs a rubber pad or a flexible diaphragm as one tool half, requiring only one solid tool half to form a part to final shape. The solid tool half is usually similar to the punch in a conventional die, but it can be the die cavity. The rubber acts somewhat like hydraulic fluid in exerting nearly equal pressure on all workpiece surfaces as it is pressed around the form block.

Rubber-pad forming is designed to be used on moderately shallow, recessed parts having simple flanges and relatively simple configurations. Form block height is usually less than 100 mm (3.9 in.). The production rates are relatively high, with cycle times averaging 1 min or less.

The advantages of the rubber-pad forming processes compared to conventional forming processes are:

- Only a single rigid tool half is required to form a part
- One rubber pad or diaphragm takes the place of many different die shapes, returning to its original shape when the pressure is released
- Tools can be made of low cost, easy-to-machine materials due to the hydrostatic pressure exerted on the tools
- The forming radius decreases progressively during the forming stroke, unlike the fixed radius on conventional dies
- Thinning of the work metal, as occurs in conventional deep drawing, is reduced considerably
- Different metals and thicknesses can be formed in the same tool
- Parts with excellent surface finish can be formed as no tool marks are created
- Set-up time is considerably shorter as no lining-up of tools is necessary

The disadvantages are:

- The pad or diaphragm has a limited lifetime that depends on the severity of the forming in combination with the pressure level
- Lack of sufficient forming pressure results in parts with less sharpness or with wrinkles, which may require subsequent hand work
- The production rate is relatively slow, making the process suitable primarily for prototype and low-volume production work

Equipment. The hydraulic presses used in most flexible-die forming are similar to those described in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume. Some processes use special machines, which are described in this article in the discussions of the specific processes. In most applications, only one solid tool half is specially made. The tool half can be made of epoxy resin, zinc alloys, hardwood, or other inexpensive material, as well as aluminum, cast iron, or steel.

Equipment is available with cycling rates as high as 1500 per hour. Some flexible-die forming methods have been applied to high-volume production, such as the forming of deeply recessed taillight reflectors for automobiles, and the deep drawing of toaster shells (Example 3).

The application of rubber pads in press-brake dies is discussed in the article "Press-Brake Forming" in this Volume. In the past, flexible-die forming methods were designated by specific processes: Guerin process, Verson-Wheelon process, trapped-rubber process, Marform process, Hydroform process, SAAB process, and Demarest process. Modern technology has reduced this list, categorizing the methods into three basic groups: rubber pad, fluid cell, and fluid forming. Detailed applications of these rubber-die forming processes to specific metals are available in the articles "Forming of Stainless Steel," "Forming of Aluminum Alloys," "Forming of Copper and Copper Alloys," and "Forming of Titanium and Titanium Alloys" in this Volume.

Rubber-Pad Forming

The Guerin process is synonymous with the term rubber-pad forming. An improvement over the Guerin process is the Marform process, which features the addition of a blankholder and die cushion to make this process suitable for deeper draws and to alleviate the wrinkling problems common to the Guerin process. Another variation of the Guerin process is the trapped-rubber process, in which the forming force is provided by a hammer instead of a hydraulic press. Like the Marform process, the trapped-rubber process can be used for deeper draws and results in less scrap due to wrinkling than the basic Guerin process. The design and construction of the ASEA Quintus rubber-pad presses are further refinements of the Guerin and Marform processes.

Guerin Process

The Guerin process is the oldest and most basic of the production rubber-pad forming processes. Its advantages are simplicity of equipment, adaptation to small-lot production, and ease of changeover.

Some metals that are commonly formed by the Guerin process are listed in Table 1. Titanium can be formed only if the workpiece and the form block are both heated. The resulting deterioration of the rubber pad often makes the process too costly, as compared to forming by conventional dies.

Table 1 Metals commonly formed by the Guerin process

Metal	Maximum thickness[(a)]	
	mm	in.
Mild forming		
Aluminum alloys		
2024-O, 7075-W	4.7	**0.187**
2024-T4	1.6	**0.064**
Austenitic stainless steels		
Annealed	1.3	**0.050[(b)]**
Quarter hard	0.8	**0.032[(c)]**
Titanium alloys	1.0	**0.040[(d)]**
Stretch flanging		
Aluminum alloy 2024-T4	1.6	**0.064**
Austenitic stainless steels		

Quarter hard	**0.8**	**0.030**

(a) Typical; varies with type of equipment and part design.

(b) Up to 2.0 mm (0.078 in.) when compression dams are used (see the section "Accessory Equipment" and Fig. 2 in this article).

(c) Only very mild forming.

(d) When heated to 315 °C (600 °F)

Presses. For maximum forming capability, the force capacity of the press and the area of the rubber pad must be suitable for the operation under consideration. The rubber pad is generally about the same size as the press ram, but it can be smaller (Example 2).

Tools. The principal tools are the rubber pad and the form block, or punch (Fig. 1). The rubber pad is relatively soft (about Durometer A 60 to 75) and is usually three times as deep as the part to be formed. The pad can consist of a solid block of rubber, or of laminated slabs cemented together and held in a retainer, as shown in Fig. 1. The slabs can also be held loose in a flanged retainer. The retainer is generally made of steel or cast iron, and it is approximately 25 mm (1 in.) deeper than the rubber pad. It is also strong enough to withstand the forming pressures generated (up to 140 MPa, or 20 ksi, in some applications, although an upper limit of 14 MPa, or 2 ksi, is more common).

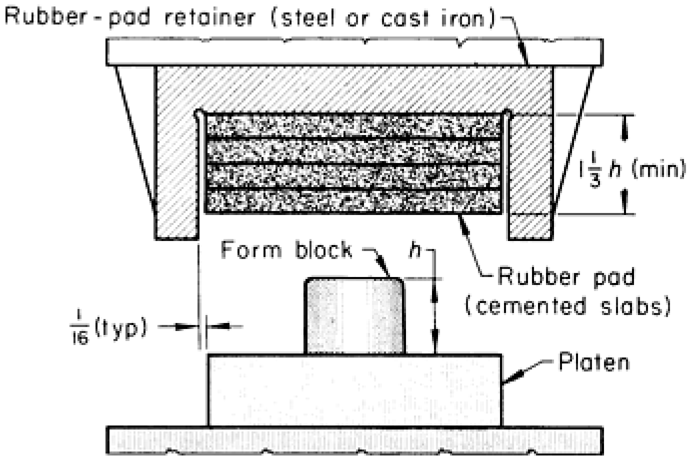

Fig. 1 Tooling and setup for rubber-pad forming by the Guerin process. Dimensions given in inches.

The minimum pad thickness is $1\frac{1}{3}$ times the height of the form block, as shown in Fig. 1. Pad thicknesses generally vary from 152 to 305 mm (6 to 12 in.), and the most commonly used thickness is 203 to 229 mm (8 or 9 in.).

Form blocks are made of wood, plastic, masonite, cast iron, steel, or alloys of aluminum, magnesium, zinc, or bismuth. The softer materials are used in making prototypes or experimental models or in small production runs. The life of a wood, plastic, or soft-metal form block can be extended by facing it with steel. Form blocks are fitted with locating pins to hold the blanks in position while they are being formed.

The form block is loosely mounted on a platen, or pressing block (Fig. 1), which fits closely into the rubber-pad retainer to avoid extrusion of the rubber during the forming process. Several form blocks are often mounted on one platen so that several parts can be formed simultaneously with one stroke of the press. Two or three platens can be used with each press; they can be slid or rotated from under the press ram for loading and unloading.

Accessory equipment includes draw clips, cover plates, wiping plates, forming rings and forming bars, and dams and wedge blocks. These are used to increase the pressure on the workpiece in specific locations and to aid in the forming of difficult shapes.

Draw clips are fastened to the edge of a blank to equalize the drawing force on a flange and to keep it from wrinkling. Wiping plates, usually hinged to the pressing block, mechanically transfer the pressure of the rubber pad to hard-to-form flanges. Forming rings and forming bars work in the same way, except that they encircle the part and therefore are not hinged. Dams are shaped and positioned so that with the sidewall of the form block they form a trap. The dam has a face sloping toward this trap so that rubber is cammed against the sidewall as the rubber pad moves down, thus increasing the pressure in that area. Wedge blocks use the same kind of camming action to apply mechanical pressure to the side of a workpiece. Examples 1 and 4 in this article illustrate the use of dams. Cover plates are used to hold blanks flat during forming or to protect previously formed areas from distortion.

Procedure. The rubber-pad retainer is fixed to the upper ram of the press, and the platen, containing the form block, is placed on the bed of the press. A blank is placed on the form block and is held in position by two or more locating pins. The pins must be rigidly mounted in the form block so that the rubber will not drive them down into the pinhole or push them out of position; and they must be no higher than necessary to hold the blank, or they will puncture the rubber pad. In some applications, nests can be used to locate the blank during forming.

As the ram descends, the rubber presses the blank around the form block, thus forming the workpiece. The rubber-pad retainer fits closely around the platen, forming an enclosure that traps the rubber as pressure is applied. The pressure produced in the Guerin process is ordinarily between 6.9 and 48 MPa (1 and 7 ksi). The pressure can be increased by reducing the size of the platen. Pressures as high as 140 MPa (20 ksi) have been developed through the use of small platens in high-capacity presses (see Example 1).

The pressure is not a function of the number of parts being formed, but of the platen area. To obtain maximum production with each stroke of the press, therefore, as many form blocks as possible are mounted on a single platen. The depth of the finished parts formed by this process seldom exceeds 38 mm ($1\frac{1}{2}$ in.). However, deeper parts can be formed by using a press with a high force capacity and a rubber pad with a small surface area. In one application, such a setup produced 140 MPa (20 ksi) of pressure and was able to form a flange 70 mm ($2\frac{3}{4}$ in.) deep.

Straight flanges can be easily bent by the Guerin process if they are wide enough to develop adequate forming force. If the flanges are not wide enough, accessory tools must be used.

Minimum widths for flanges of stainless steel and aluminum alloys that can be bent by rubber-pad forming are listed in Table 2. Angles on flanges in soft metal can generally be held to a maximum variation of ±1°. In hard metals, such as half-hard stainless steels, which have more springback than annealed stainless steels, a ±5° tolerance can be met only with special care. An envelope (all-around) tolerance of ±0.38 mm (±0.015 in.) is possible on the contour of soft-metal pieces, but on hard metal, the tolerance must be increased to ±0.51 mm (±0.020 in.).

Table 2 Minimum formable flange widths for the rubber-pad forming of stainless steels and aluminum alloys

Alloy and/or temper	Minimum flange width[(a)]	
	mm	in.
Stainless steels		
Annealed	4.8 + 4.5t	$\frac{3}{16}$ **+ 4.5*t*[(b)]**

Quarter hard	16	$\frac{5}{8}$
Aluminum alloys		
2024-O, 7075-O	1.6 + 2.5*t*	$\frac{1}{16}$ **+ 2.5*t*(b)**
2024-T3, 2024-T4	**3.2 + 4*t***	$\frac{1}{8}$ **+ 4*t*(b)**

(a) Using minimum permissible bend radius; a larger bend radius requires a wider flange.

(b) *t*, sheet thickness

Stretch flanges and shrink flanges can be formed around curves and holes if the deformation is slight to moderate. If forming is severe, auxiliary tools must be used to support the work and to prevent wrinkling.

Shallow Drawing. Cover plates are often used to hold webs flat while flanges are being formed. In the following example, a pressure of 140 MPa (20 ksi) was used to form a part so well that only minimal hand reworking was required. To obtain the 140 MPa (20 ksi) pressure, a rubber pad 508 mm (20 in.) in diameter (surface area: ~0.19 m^2, or 300 in.^{2}) was mounted in a 27 MN (3000 tonf) hydraulic press. The bed size of the press was 1520 mm (60 in.) front-to-back and 1570 (62 in.) left-to-right. Stroke length was 457 mm (18 in.), and shut height was 1270 mm (50 in.). The press had a turntable that held two form blocks; therefore, one block could be unloaded and loaded while the other was under the press ram. The rubber pad was 203 mm (8 in.) thick and made in two pieces. One piece was 178 mm (7 in.) thick and had a hardness of Durometer A 80 to 85. The second piece, which was replaceable, was 25 mm (1 in.) thick and had a hardness of Durometer A 70 to 75.

Example 1: Shallow Drawing of a Fuselage Tail Cap by the Guerin Process.

The fuselage tail cap shown in Fig. 2 was rubber-pad formed at 140 MPa (20 ksi) using the 27 MN (3000 tonf) hydraulic press and the 508 mm (20 in.) diam pad described above. The cap was originally made by spinning, but at a rate of only one piece per hour. Changing to high-pressure rubber-pad forming by the Guerin process increased the production rate to 12 pieces per hour.

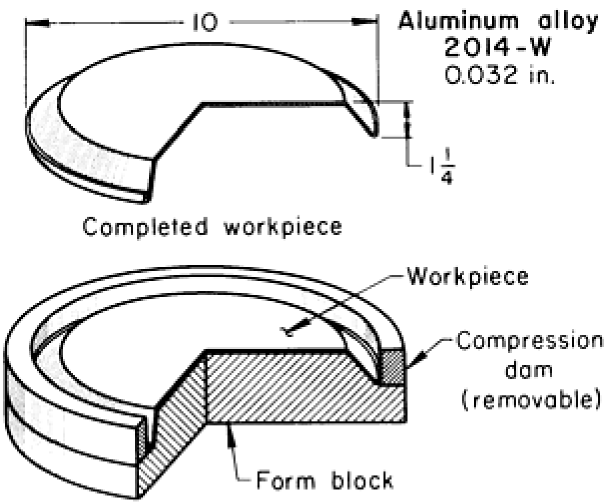

Fig. 2 Fuselage tail cap that was formed by the Guerin process in a high-pressure setup. Dimensions given in inches.

A blank of aluminum alloy 2014-O, 0.81 mm (0.032 in.) thick, was solution heat treated to the W temper and was lubricated with heavy-duty floor wax. The part was formed before age hardening was complete. A compression dam surrounded the form block (Fig. 2) and was used to concentrate pressure on the flange.

Matched Laminations. In the following example, accurately matched laminations were made by forming one over the other on a form block by the Guerin process.

Example 2: Forming of a Two-Piece Cockpit Rail on a Single Form Block.

By using a rubber pad instead of a conventional die, it was possible to form the two mating parts of a cockpit rail section on a single form block. One of the two parts is shown in Fig. 3. The second part was formed over the first after it had been formed.

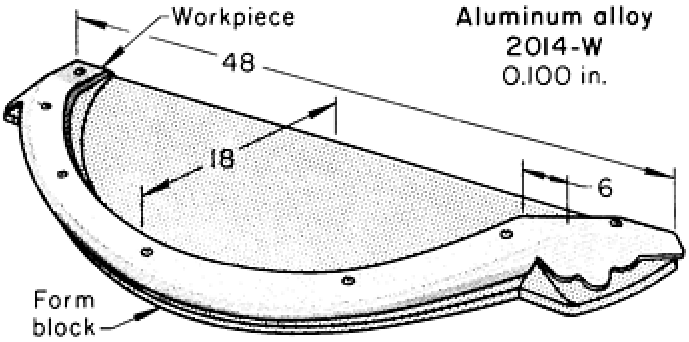

Fig. 3 Cockpit rail section that was formed on a single form block by the Guerin process. Dimensions given in inches.

A fully developed blank, 2.54 mm (0.100 in.) thick, was cut from aluminum alloy 2014-O and solution heat treated. Forming was done by the Guerin process with a minimum pressure of 52 MPa (7.5 ksi). No lubricant was used. The form block (Fig. 3) was made of masonite and was plastic faced. The production rate was 20 pieces per hour.

Blanking. With the Guerin process, rubber pads can be used for blanking and piercing as well as for forming. Rubber pads produce better edges on the workpiece than band sawing, and almost as good as those made by routing. An edge radius up to the thickness of the metal can be produced on some heavy-gage metals. The rubber-pad method can blank aluminum alloy 2024-O up to 0.81 mm (0.032 in.) thick and, for some shapes, up to 1.0 mm (0.040 in.) thick. The minimum hole diameter or width of cutout is 50 mm (2 in.). A minimum of 38 mm ($1\frac{1}{2}$ in.) trim is needed for external cuts.

The form block has a sharp cutting edge where the blank is to be sheared. In hard-metal blocks, this edge can be cut into the form block, as shown in Fig. 4(a) and 4(b). Form blocks of soft metal, plastic, or wood need a steel shear plate for the cutting edge (Fig. 4c); the shearing edge should be undercut 3 to 6°.

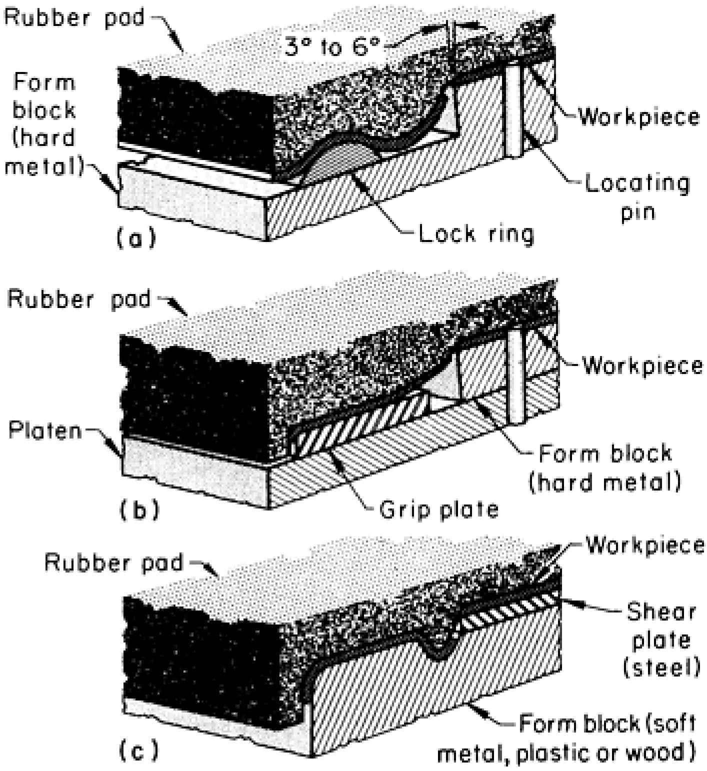

Fig. 4 Three techniques for blanking by the Guerin process categorized by clamping method. (a) Lock ring. (b) Grip plate. (c) Raised extension of the form block.

The trim metal beyond the line of shear must be clamped firmly so that the work metal will break over the sharp edge instead of forming around it. This clamping is done by a lock ring (Fig. 4a), a grip plate (Fig. 4b), or a raised extension of the form block (Fig. 4c).

These clamping devices also localize pressure at or near the cutting line. A rounded edge on the finished blank can be produced by locating the lock ring or grip plate a small distance from the shear edge (Fig. 4a and 4b). The metal droops in the unsupported area and forms around the sharp corner before it shears. The result is a smooth, rounded edge.

Drawing of shallow parts is often done by a modification of the Guerin process in which the contour is recessed (for example, a die cavity) into the form block rather than being raised on it. The blank is securely clamped between the rubber pad and the surface around the recess before forming begins.

Clamping the work metal before drawing and the amount of pressure used are both important for successful drawing. The work metal must be securely clamped to prevent it from flowing irregularly and subsequently forming wrinkles, but not so tightly that the metal cannot flow at all, which will cause thinning, or even tearing, of the work metal. To avoid this, either the edges can be lubricated or a protecting block with an undercut slot to accommodate the flange (Fig. 5) can be placed over the edges of the workpiece. The width of the block and undercut must provide the correct balance between clamping force and drawing force. The undercut should be 0.08 to 0.15 mm (0.003 to 0.006 in.) higher than the thickness of the work metal.

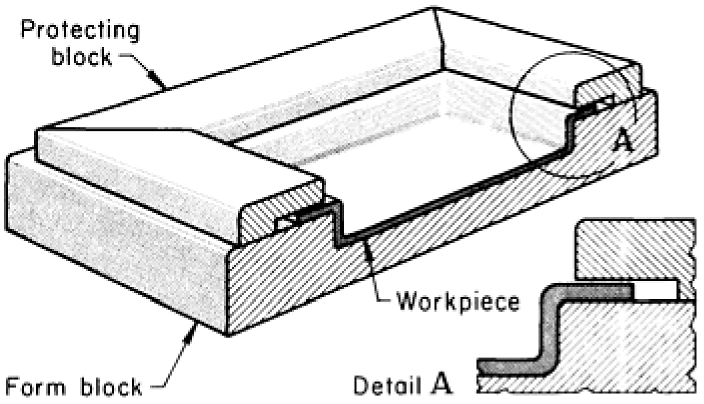

Fig. 5 Use of a protecting block to prevent work metal irregularities in shallow drawing by the Guerin process.

Marform Process

The Marform process was developed to apply the inexpensive tooling of the Guerin and Verson-Wheelon processes (see the section "Verson-Wheelon Process" in this article) to the deep drawing and forming of wrinkle-free shrink flanges. A blankholder plate and a hydraulic cylinder with a pressure-regulating valve are used with a thick rubber pad and a form block similar to those used in the Guerin process. The blank is gripped between the blankholder and the rubber pad. The pressure-regulating valve controls the pressure applied to the blank while it is being drawn over the form block.

While forming a soft aluminum alloy blank, the diameter can usually be reduced 57%, and reductions as high as 72% have been obtained. A shell depth equal to the shell diameter is normal when the minimum stock thickness is 1% of the cup diameter. Depths up to three times shell diameter have been reached with multiple-operation forming. The minimum cup diameter is 38 mm (1½ in.).

Foil as thin as 0.038 mm (0.0015 in.) can be formed by placing the blank between two aluminum blanks about 0.76 mm (0.030 in.) thick and forming the three pieces as a unit. The inner and outer shells are discarded.

Presses. The Marform process is best suited to a single-action hydraulic press in which pressure and speed of operation can be varied and controlled. A Marform unit comes as a package that can be installed in a hydraulic press having ample stroke length and shut height. However, a press that incorporates a hydraulic cushion system into its bed has been designed specifically for Marforming.

The rubber pressures used depend on the force capacity of the press and the surface area of the rubber pad. Recent installations range from 34 to 69 MPa (5 to 10 ksi).

Tools. The rubber pad used in Marforming is similar to that used in the Guerin process. It is normally 1½ to 2 times as thick as the total depth of the part, including trim allowance. The rubber pad can be protected from scoring by the use of a throw sheet, which is either cemented to the pad or thrown over the blank.

Well-polished steel form blocks are used for long runs and deep draws. Aluminum alloy form blocks must be hard coated to prevent galling for draws deeper than 38 mm (1½ in.). Masonite form blocks can be used if they can withstand the abuse and wear of forming a particular part in a given quantity. When a cast shape is more economical, aluminum or zinc alloy form blocks can be used.

Blankholder plates are usually made of low-carbon steel. The contact surface is ground flat and polished to avoid scratching of the blank. Clearance between the form block and the mating hole in the blankholder is 0.76 to 1.52 mm (0.030 to 0.060 in.) per side. The edge should have a 1.6 mm ($\frac{1}{16}$ in.) radius.

A radius plate is necessary when the machine pressure is insufficient for forming the flange radius within tolerance. The part is drawn first without the plate, then redrawn using the plate to form the exact radius. The radius plate is usually 13 mm (½ in.) thick and 25 mm (1 in.) wider than the workpiece. A sealing ring is used to prevent the rubber pad from extruding out of the container.

Procedure. The blank rests on the blankholder plate above the form block. The rods supporting the seal ring and blankholder plate (Fig. 6) are supported on a variable-pressure hydraulic cushion. As the press ram is lowered, the blank is clamped between the rubber pad and the blankholder before forming begins. As the rubber pad continues to descend, the blank is drawn over the form block while the pressure control valve in the hydraulic cushion releases fluid at a controlled rate. The pressure in the hydraulic cushion must be adjusted to prevent wrinkles from forming in the flange but to permit the blank to be drawn into a smooth shell. The part is stripped from the form block by the blankholder. The following example describes an application of the process.

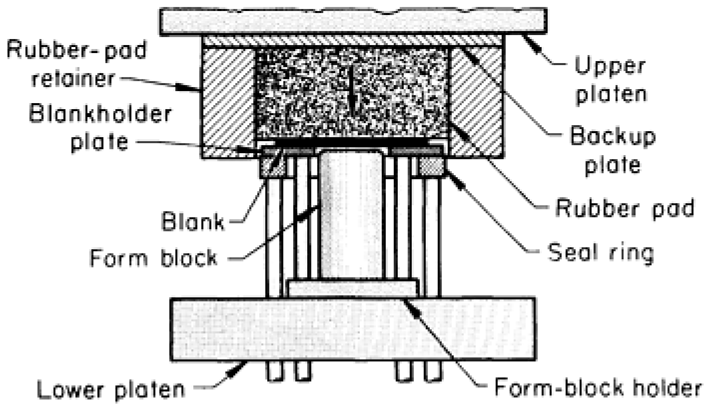

Fig. 6 Tooling and setup for rubber-pad forming by the Marform process.

Example 3: Deep Drawing of Toaster Shells by the Marform Process.

The toaster shell shown in Fig. 7 was deep drawn in large quantities (80,000 pieces) from 0.76 mm (0.030 in.) thick deep-drawing-quality 1010 steel. The blanks were lubricated by brushing with a soap compound. Available pressure was 41 MPa (6 ksi). The depth of the trimmed shell was 127 mm (5 in.). The reduction time per piece was 22 s.

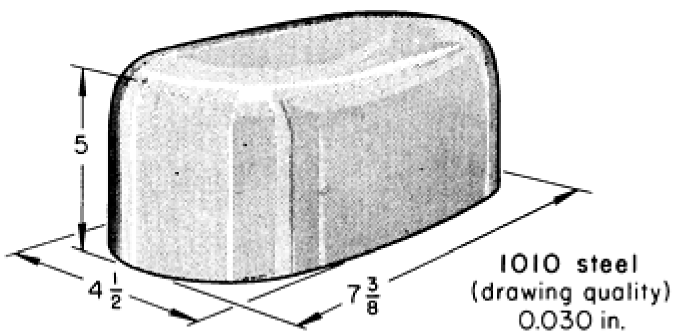

Fig. 7 Toaster shell that was deep drawn by the Marform process. Dimensions given in inches.

Drop Hammer Forming With Trapped Rubber

A process similar to the Guerin process, for forming shallow workpieces, is a trapped-rubber process, which uses a drop hammer in place of the hydraulic press; the primary differences are the faster forming speed and the impact force of the hammer. The use of rubber pads in drop hammer forming is illustrated in the article "Drop Hammer Forming" in this Volume.

Figure 8 shows the effects of forming flanges on aluminum alloys 5052-O and 2024-O by the drop hammer (trapped-rubber) and Guerin processes. When flanges deeper than 32 mm (1¼ in.) are made by the Guerin process, stretch flanges can tear and shrink flanges can wrinkle. However, when the drop hammer process is used, fewer deformities occur (Fig. 8).

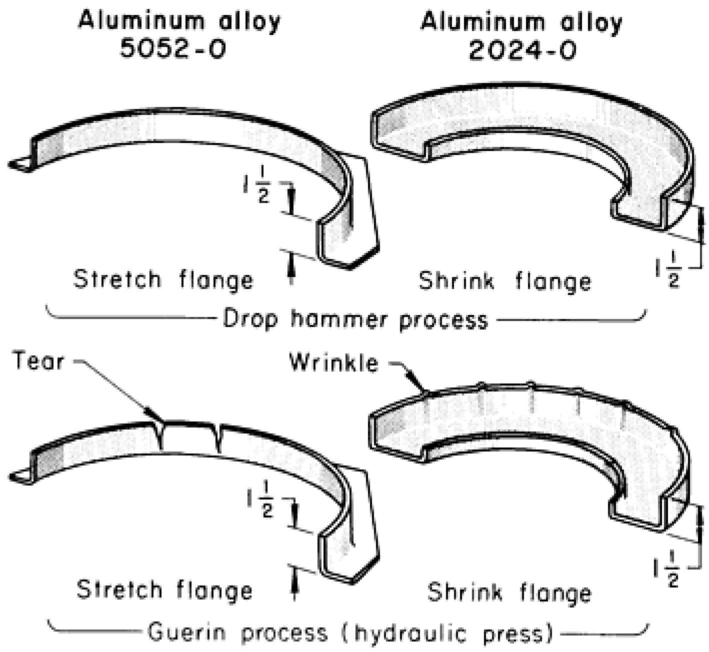

Fig. 8 Effect of impact in forming stretch and shrink flanges by the drop hammer (or trapped-rubber) and Guerin processes. Dimensions given in inches.

ASEA Quintus Rubber-Pad Press

ASEA presses, generally designed with force capacities of 50 to 500 MN (5600 to 56,000 tonf), are constructed of wire-wound frames and have separate guiding columns (Fig. 9). By winding the press frames with prestressed wire, only compressional stresses are present in the large castings or forgings of the yokes and columns, even when subjected to maximum forming pressure. Therefore, when the press is loaded, the frame remains in slight compression, and the major structural components never operate in the tensile mode.

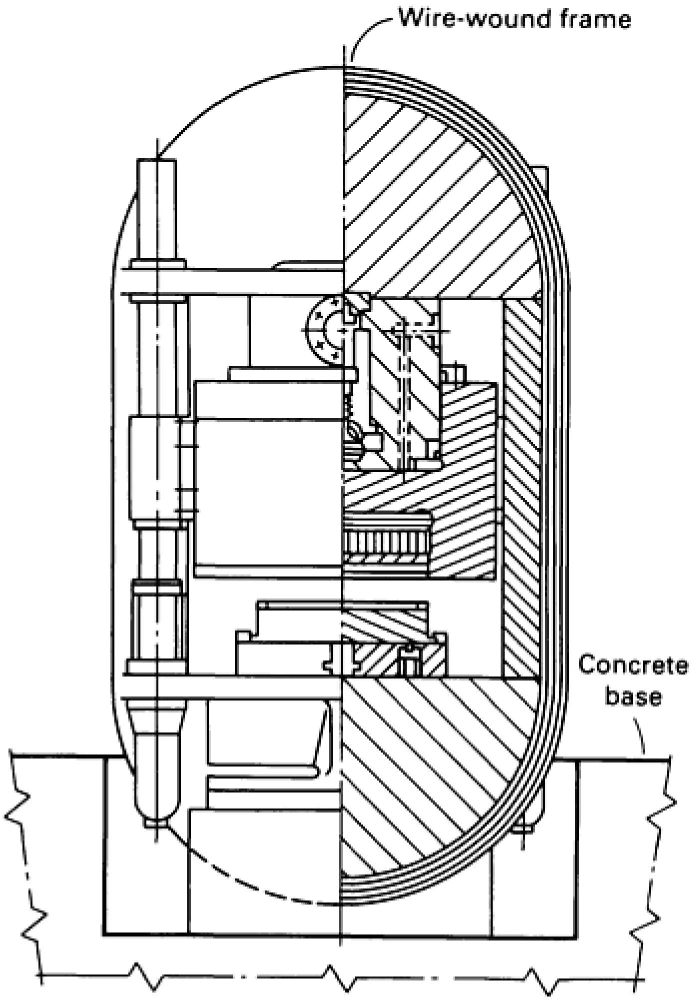

Fig. 9 Schematic of ASEA Quintus rubber-pad hydraulic press with wire-wound frame.

The press is equipped with a forged-steel rubber-pad retainer that has a replaceable insert to allow for forming at even higher pressures. Although the maximum tool height is sacrificed by using these high-pressure inserts, cutting the work area in half doubles the maximum forming pressure of the press when needed.

Standard table sizes range from 0.7 × 1.0 m (28 × 39 in.) to 2 × 3 in (79 × 118 in.). These presses provide forming pressures to 100 MPa (15 ksi). Hard and brittle materials such as titanium, along with the die, can be heated outside the press by an infrared heater; blanks can also be heated by conduction from the table through the heat transferred through the die (Fig. 10).

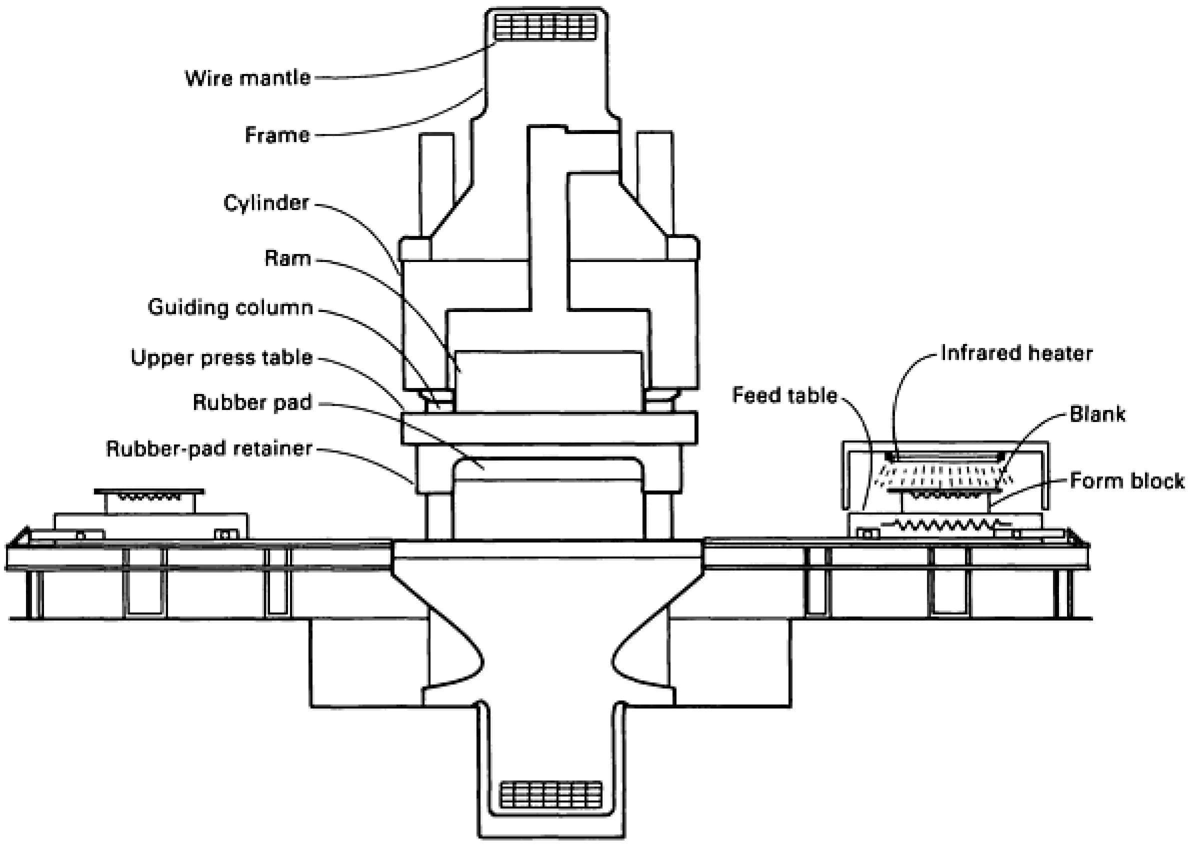

Fig. 10 Schematic of ASEA Quintus rubber-pad press with provision for heating hard and brittle materials using infrared heater or heating elements contained in feed table.

Fluid-Cell Forming

Initially developed as the Verson-Wheelon process, the fluid-cell process uses a fluid cell (a flexible bladder) backed up by hydraulic fluid to exert a uniform pressure directly on the form block positioned on the press table. This process can be classified in terms of the presses used (Verson-Wheelon and ASEA Quintus) as well as a specialized method (Demarest process) for producing cylindrical and conical parts. The Verson-Wheelon press has cylindrical press housings of laminated, prestressed steel that serve as pressure chambers. The ASEA Quintus press has a forged steel cylinder that is wound with high strength steel wire to create a prestressed press frame with extremely good fatigue properties.

Fluid-cell forming can be used for recessed parts that are beyond the capabilities of rubber-pad forming, for all flange configurations (including C-shaped flanges), and for complex parts with reentrant features and intricate joggles. Maximum form block height is 425 mm (16.7 in.), and typical cycle time is 1 to 2 min.

Verson-Wheelon Process

The Verson-Wheelon process was developed from the Guerin process. It uses higher pressure and is primarily designed for forming shallow parts, using a rubber pad as either the die or punch. A flexible hydraulic fluid cell forces an auxiliary rubber pad to follow the contour of the form block and to exert a nearly uniform pressure at all points.

The distribution of pressure on the sides of the form block permits the forming of wider flanges than with the Guerin process. In addition, shrink flanges, joggles, and beads and ribs in flanges and web surfaces can be formed in one operation to rather sharp detail in aluminum, low-carbon steel, stainless steel, heat-resistant alloys, and titanium.

Presses. The Verson-Wheelon press has a horizontal cylindrical steel housing, the roof of which contains a hydraulic fluid cell (Fig. 11). Fluid-cell bladders can be of neoprene or polyurethane composition. Hydraulic fluid is pumped into the cell, causing it to inflate or expand. The expansion creates the force needed to flow the rubber of the work pad downward, over and around the form block and the metal to be formed.

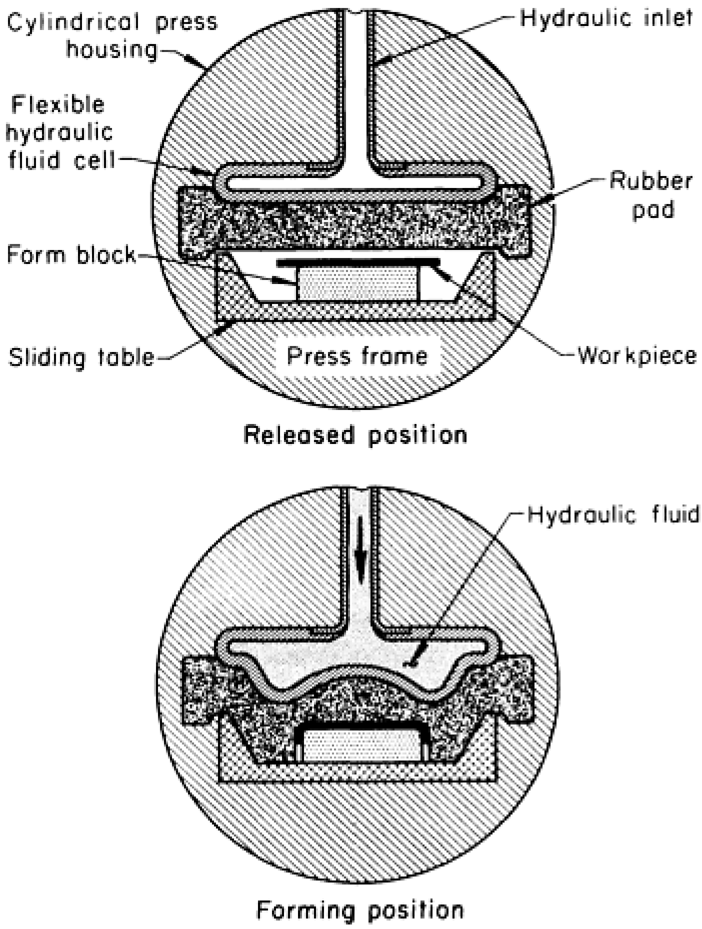

Fig. 11 Principal components of the Verson-Wheelon process.

Below the chamber containing the rubber pad and the hydraulic fluid cell is a passage, extending the length of the press, that is wide and high enough to accommodate a sliding table containing form blocks. At each end of the passage is a sliding table that is moved into position for forming.

The rubber pad used in the Verson-Wheelon process has a hardness of about Durometer A 35. It is usually protected from sharp corners on the form block and blank by a throw sheet or work pad that is harder and tougher than the pad itself. The throw sheet is much less costly to replace than the rubber pad below the fluid cell.

Verson-Wheelon presses are available with forming pressures ranging from 35 to 140 MPa (5 to 20 ksi) and force capacities of 22 to 730 MN (2500 to 82,000 tonf). Sliding tables range in size from 508 × 1270 mm (20 × 50 in.) to 1270 × 4170 mm (50 × 164 in.). The larger machine can form parts having flange widths to 238 mm ($9\frac{3}{8}$ in.).

Heating elements can be used with the sliding tables for producing parts made of magnesium. The maximum temperature is 315 °C (600 °F), and special heat-resistant throw pads are used to protect the hydraulic fluid cell.

Tools. The form blocks for the Verson-Wheelon process are made in much the same way as those for the Guerin process. Compression dams or deflector bars can be used to direct pressure into local areas for forming shrink flanges or return bends, as described in Example 4.

Aluminum alloy or zinc alloy form blocks are recommended. Because of the high pressures, masonite or wood form blocks may break down from repeated use. More than one form block can be used at a time; the quantity depends on the size and shape of the form block.

Damage to the rubber pad can be reduced by removing all burrs and sharp edges from the blank. Form blocks should be smooth; all sharp corners and projecting edges should be well rounded; high tooling pins should be eliminated; deep narrow crevices or gaps between parts of form blocks should be eliminated; holes in blocks should be plugged during forming.

Procedure. The sliding table containing the form blocks is loaded and slid into the press. Hydraulic fluid is pumped into the cell, expanding it and driving the rubber pad down against the workpiece and around the form blocks. The pressure is released, and the table of formed pieces is slid out, unloaded, and reloaded for another cycle.

Repositioning the form blocks after a few cycles will distribute the wear on the rubber pad and lengthen its life. The use of a hard-rubber (or occasionally leather) pad slightly larger than the blank assists in the uniform forming of flanges and prevents wrinkles.

The cycle time for the Verson-Wheelon process is longer than that of conventional presses, such as those used with the Guerin process. To reduce cell filling and draining time, it is good practice to load the table to capacity or to have dummy blocks on the sliding table when only one part is being formed.

In the following example, the time to form a part in a Verson-Wheelon press was less than when the part was made by the Guerin process. The higher forming pressure completely formed the part in one operation (whereas the Guerin process had required two operations) and reduced hand work after machine forming.

Example 4: Verson-Wheelon Versus Guerin Process for the Forming of a Complex Part.

The complex part shown in Fig. 12 was originally formed by the Guerin process in a 40 MN (4500 tonf) hydraulic press from 1.0 mm (0.040 in.) thick alclad aluminum alloy 7075-W. The improved method used a Verson-Wheelon press that could exert a pressure of 69 MPa (10 ksi) and had a capacity of 360 MN (41,000 tonf). A pressure of 48 MPa (7 ksi) was needed to form the part. The same tool was used for both processes.

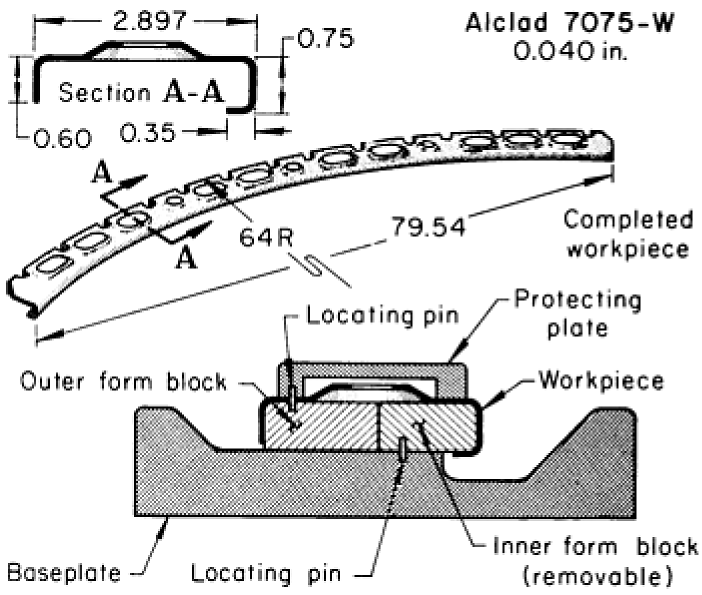

Fig. 12 Complex part that was formed in a Verson-Wheelon press. Dimensions given in inches.

In the Guerin process, forming was done in two operations. Joggles and stringer tabs were set by hand after forming. In the first press operation, the outer flange was formed, and the inner and return flanges were partly formed. In the second press operation, the forming was completed with rubber strips confined by the dams.

In the Verson-Wheelon process, the outer, inner, and return flanges and the joggles were formed in one operation. This translated into a 30% labor-cost advantage for the Verson-Wheelon process over the Guerin process for this workpiece.

The heat-treated aluminum alloy 6061 form block was mounted to a baseplate, with the inner and outer rims acting as dams. Because of the return flange on the inside radius of the part, the aluminum alloy form block was split longitudinally, and the outer half was fastened to the baseplate (see tooling setup in Fig. 12). The inner half, bushed and located on pins projecting from the baseplate, was removed from the base with the finished part. Locating holes in the blank and the outer form block matched locating pins in the cover plate.

The 127 × 2240 mm (5 × 88 in.) blank was routed, and lightening holes were individually pierced and flanged, in a punch press in both processes before rubber-pad forming. After forming, the part was aged to the T6 temper. The production lot was 20 pieces. Several thousand pieces were produced on the form blocks.

Secondary Operations. Even though higher forming pressures are used, many pieces made by the Verson-Wheelon process (as well as by the Guerin process) need hand work to remove wrinkles and to add definition to details. A further refinement in the use of throw pads is a shaped rubber pad. Pad laminations are built up around a cavity that approximates the shape of the part, so that flow of the rubber is less severe and forming pressure is more evenly distributed than with the conventional flat rubber pad. The shape of the cavity is only approximate and can be used for similar parts.

Demarest Process

Cylindrical and conical parts can also be formed by a modified rubber bulging punch. The punch, equipped with a hydraulic cell, is placed inside the workpiece, which is in turn placed inside the die. Hydraulic pressure expands the punch. Forming with an expanding punch using the Demarest process is described in the following example.

Example 5: Use of Expanding Punches to Form Aircraft Fuel-Tank Sections.

Aluminum alloy workpieces, rolled and welded into cones (Fig. 13a), were formed into aircraft fuel-tank sections with expanding rubber punches (Fig. 13b). The cones were lowered into cast iron dies, which weighed 1600 kg (3500 lb) each and were designed to withstand 10 MPa (1.5 ksi) of forming pressure.

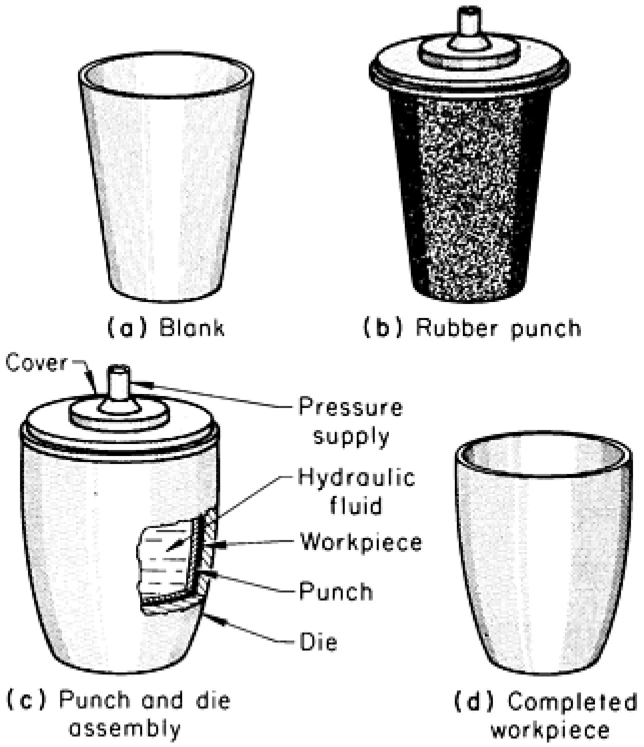

Fig. 13 Forming of a fuel-tank section from a blank using the Demarest process.

The rubber punch was lowered into the workpiece, and a steel cover was clamped over the whole assembly (Fig. 13c). The punch was expanded under 2800 kPa (400 psi) of hydraulic pressure, which formed the work metal into the curved shape of the die (Fig. 13d).

The time taken for the entire process, including dismantling of the die and unloading of the workpiece, was 3 min. In contrast, spinning requires 15 to 20 min.

ASEA Quintus Fluid Cell Process

The ASEA Quintus fluid cell process is a further development of the Guerin process, allowing deeper and more complex parts to be formed. It uses a flexible rubber diaphragm backed up by oil as either the male or female tool half. The pressurized diaphragm forces the blanks to assume the shape of the solid-tool halves. The high uniform hydrostatic pressure permits the forming of shallow- to medium-depth parts with complex shapes to final shape, practically eliminating the subsequent hand forming usually required to apply the Guerin process.

Presses. The ASEA Quintus fluid cell press (Fig. 14) consists of a horizontally placed cylindrical press frame, two rectangular, independently operated press tables on each side of the press, and all electric and hydraulic equipment necessary to operate the press placed on top of and alongside the press. The press frame is a forged steel cylinder that has been wound under pre-stress with high strength steel wire, so that only compressional stresses can occur in the steel forgings, even at maximum pressure. Therefore, the fatigue properties of the Quintus wire-wound frame are excellent and the risk of fracture virtually eliminated.

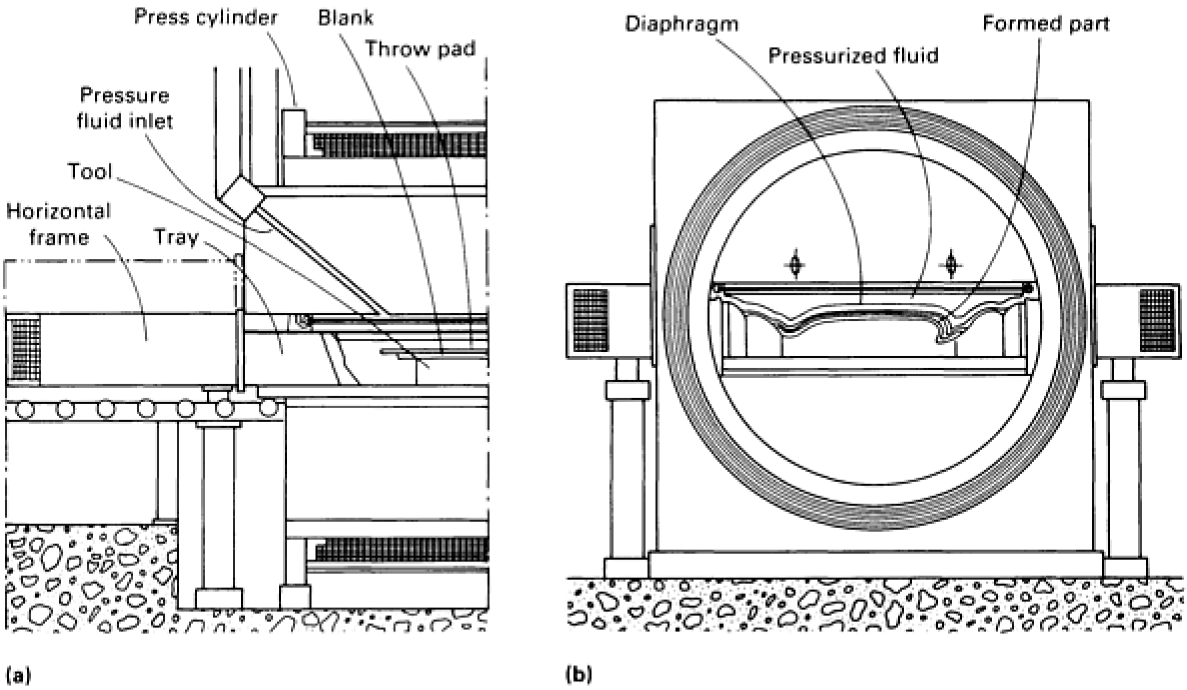

Fig. 14 Illustration of the principal components of a new generation ASEA Quintus fluid cell press. (a) Tray containing blanks inserted into press prior to pressurizing. (b) Pressurized fluid cell diaphragm forming a blank.

A diaphragm (bladder) of natural rubber is located inside the press and covers the entire surface of the press table. Hydraulic fluid is pumped into the cell created by the frame and the diaphragm, and the diaphragm is forced by the increased pressure to expand into the table, which forces the blank to assume the shape of the form block, thus forming the part. Once forming is done, the table is shuttled out to one side of the cylindrical press frame, and the table on the other side can be shuttled inside the press to allow forming of parts placed on that table.

In the new generation of ASEA Quintus fluid cell presses, the flexible diaphragm can be changed in less than 30 min. No rubber pad is required, and the diaphragm, which is made of natural rubber, can be repaired on-site should a sharp corner of a tool or blank penetrate the roll-up pad that rolls out to cover the press table when it is shuttled into the press.

ASEA Quintus fluid cell presses are available with maximum forming pressures ranging from 100 to 200 MPa (14 to 29 ksi) and force capacities up to 1400 MN (157,000 tonf). The forming tables range in size from 700 × 2000 mm (27.5 × 78.7 in.) to 2000 × 5000 mm (78.7 × 196.8 in.). The large presses can accommodate tools as high as 425 mm (16.7 in.) and consequently form parts as deep or flanges as wide as 425 mm (16.7 in.).

Tools. Because of the uniform hydrostatic pressure exerted by the press on the tools, they can be made of low-strength and low-cost tool materials such as hardwood, bakelite, epoxy, zinc-alloy, and so on, as well as of stronger materials such as aluminum, cast iron, and steel. Depending on the shape of the part, and tolerance and surface finish requirements, parts can be formed over a male die (form block), in a female die, or in an expansion die. The side of a part requiring a high surface finish should face the diaphragm, while the side of a part requiring close tolerances should face the tool. Parts with complex shapes and tight radii or made of high strength materials, demanding high pressures, and parts required in large quantities may require tools of zinc or aluminum alloys.

Procedure. The ASEA Quintus fluid cell press has two independently operated press tables. This allows parts to be unloaded and new blanks loaded on tools on one press table while parts are being formed on the other table inside the press. A press table can be loaded with one large single tool half occupying the full size of the table or several smaller tool halves; the number is restricted only by the size of the table.

Once blanks are loaded on the tools, the table is shuttled into the cylindrical press frame to a position where the flexible diaphragm, located in the upper half of the press, covers the entire table area. Oil is then pumped into the cell, causing the diaphragm to expand, which forces the blanks to assume the shapes of the tools. When the parts are formed, the pressure is relieved and the oil is evacuated from the cell allowing the table to be shuttled out from the press so that parts can be unloaded from the tools. The cycle time for a Quintus fluid cell is usually 1 to 3 min, depending on the press size and forming pressure selected.

Rubber-Pad Forming

Fluid Forming

In contrast to conventional two-die forming, which produces local stress concentrations in a workpiece, fluid forming (previously classified as rubber-diaphragm forming) employs a flexible-die technique that inhibits thinning and crack initiation due to the uniformly distributed pressure. In the fluid forming process, a rubber diaphragm serves as both the blankholder and a flexible-die member. Fluid forming differs from the rubber-pad and fluid-cell processes in that the forming pressure can be controlled as a function of the draw depth of the part.

Fluid forming was initially known as the Hydroform process. The process, as originally conceived, is incorporated into the Verson Hydroform press. After a hydraulic pump delivers fluid under pressure into the pressure-dome cavity, the punch containing the die is driven upward into the cavity against the resistance provided by the fluid, and the workpiece is formed.

In the SAAB rubber-diaphragm method, hydraulic fluid being compressed by the press piston alone (no moving die is involved) forces the workpiece to assume the contour of the die. Air vents incorporated into the die facilitate the removal of trapped air and eliminate blisters on the surface of the workpiece. The ASEA Quintus fluid forming press was developed through slight modification of the SAAB process and optimization of press specifications with easily changeable pressure domes.

Another adaption of the fluid forming method is the ASEA Quintus technique used in the ASEA Quintus deep-drawing fluid forming press. Dome pressure and punch draw are both controlled from below the blank by two concentric rams, each of which governs one of these two variables. The units have interchangeable pressure domes.

Fluid forming is intended for punch, cavity, hydroblock, or expansion forming of deep-recessed parts (Fig. 15). Cycle time is 15 to 20 s for most parts.

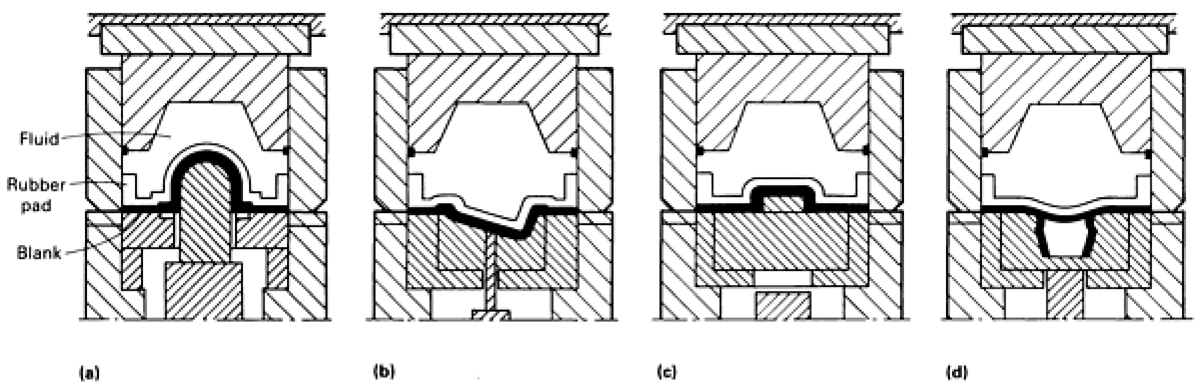

Fig. 15 Four forming techniques that can be used in a fluid forming press. (a) Punch draw. (b) Cavity draw. (c) Hydroblock draw (male-die forming). (d) Expansion draw.

Verson Hydroform Process

This process differs from those previously described in that the die cavity is not completely filled with rubber, but with hydraulic fluid retained by a 64 mm (2½ in.) thick cup-shaped rubber diaphragm. This cavity is termed the pressure dome (Fig. 16). A replaceable wear sheet is cemented to the lower surface of the diaphragm, as shown in Fig. 16. More severe draws can be made by this method than in conventional draw dies because the oil pressure against the diaphragm causes the metal to be held tightly against the sides as well as against the top of the punch.

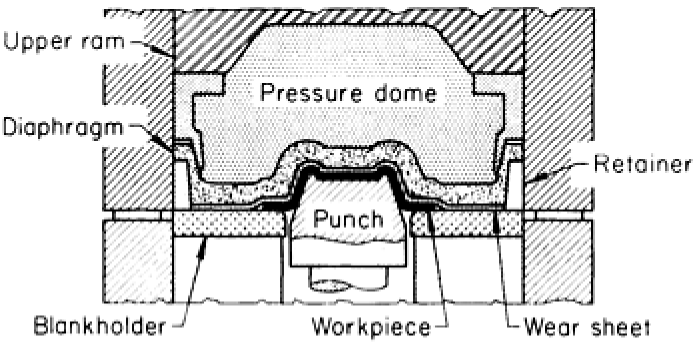

Fig. 16 Fluid-cell forming in a Hydroform press.

Reductions in blank diameter of 60 to 70% are common for a first draw. When redrawing is necessary, reductions can reach 40%. Low-carbon steel, stainless steel, and aluminum in thicknesses from 0.25 to 1.65 mm (0.010 to 0.065 in.) are commonly formed. Parts made of heat-resistant alloys and copper alloys are also formed by this process.

Presses. A special press, called a Hydroform press, is used for this process. A lower hydraulic ram drives the punch upward; the upper ram is basically a positioning device. A hydraulic pump delivers fluid under pressure to the pressure dome. The blankholder is supported by a solid bolster and does not move during the operation.

Dome pressures range from 41 to 103 MPa (6 to 15 ksi), and punch force capacities vary from 3.2 to 19 MN (368 to 2089 tonf). Special guide pins and a platen adaptor convert a standard Hydroform press into a single-action conventional hydraulic press with a force capacity of 6.2 to 40 MN (700 to 4470 tonf). This variation of the process has the punch stationary and the blankholder actuated by the die cushion of a single-action hydraulic press, as shown in Fig. 20.

Maximum blank diameters are 305 to 1020 mm (12 to 40 in.), maximum punch diameters are 254 to 864 mm (10 to 34 in.), and maximum draw depths are 178 to 305 mm (7 to 12 in.). The maximum rating is 1500 cycles per hour. The practical production rate in cycles per hour is usually about two-thirds the machine rating. However, the operation often takes the place of two or three conventional press operations.

Tools. Punches can be made of tool steel, cold-rolled steel, cast iron, zinc alloy, plastic, brass, aluminum, or hardwood. Choice of material depends largely on the work metal to be formed, number of parts to be made, shape of the part, and severity of the draw.

Blankholders are usually made of cast iron or steel and are hardened if necessary. Clearance between punch and blankholder is not critical; it may be 50% or more of the thickness of the metal being drawn.

For short runs, an auxiliary blankholding plate can be placed on a blankholder that is already in place. The auxiliary blankholder plate should not overhang in the punch clearance more than its thickness, and it should not be larger than the blankholder.

Rubber strips are placed on the blank to break the vacuum caused by dome action during drawing. Blankholders can be contoured to match the shape of a preformed blank or to preform the blank as an aid in forming.

Procedure. The blank to be formed is placed on the blankholder. The pressure dome, filled with the hydraulic fluid and covered by the rubber diaphragm, is lowered over the blank, and preliminary pressure is applied through a pump in the hydraulic supply line. The preliminary pressure can range from 14 to 69 MPa (2 to 10 ksi), depending on the part to be formed.

The punch is raised and pushed into the blank from underneath. As the form in the blank rises into the hydraulic chamber, the pressure in the chamber increases sharply, reaching as high as 103 MPa (15 ksi). A pressure control valve keeps the pressure within programmed limits. When parts are formed of thin metal, a vacuum release valve can be built into the punch to aid stripping after forming.

Three cams are programmed to control the operation of the machine. The first controls the height of rise of the punch, the second controls edging or sharpening of the corner radii, and the third returns the punch at the end of the stroke while the blankholder strips the finished part from the punch. The forming of a complex part by the rubber-diaphragm process is described in the following example.

Example 6: Rubber-Diaphragm Forming of a Complex Jet-Engine Part.

Fuel-nozzle swirl cups for high-performance turbojet engines were originally produced by welding six press-formed sections of type 310 stainless steel, AMS 5521 (Fig. 17a). Forming the six sections was difficult, and the finished parts were expensive. The rejection rate was also high.

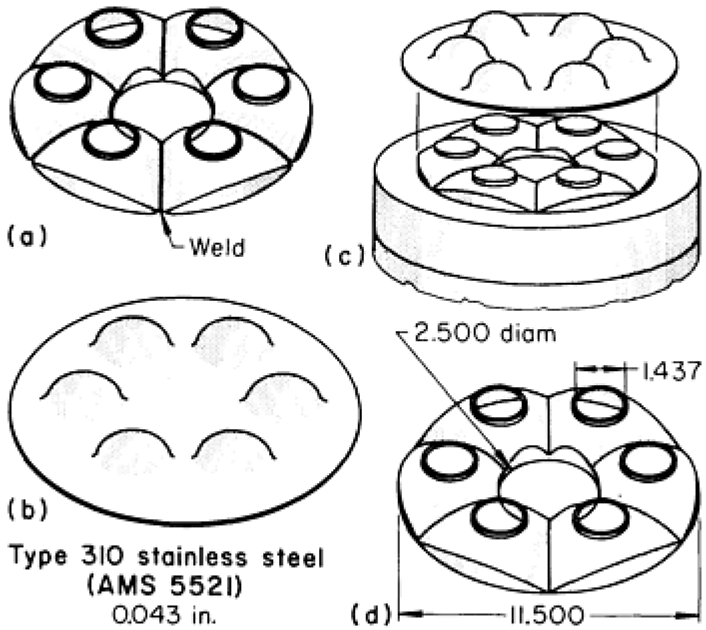

Fig. 17 Original and improved methods of forming a fuel-nozzle swirl cup for a turbojet engine. (a) Part formed by original method; six press-formed sections welded together. (b) Partly drawn blank ready for rubber-diaphragm forming. (c) Punch of six similar wedge-shaped segments doweled into bottom plate, used for rubber-diaphragm forming. (d) Swirl cup as formed by the rubber-diaphragm method and subsequently pierced and trimmed. Dimensions given in inches.

Rubber-diaphragm forming in a Hydroform press was tried. This press formed the part from one blank 1.1 mm (0.043 in.) thick by 324 mm ($12\frac{3}{4}$ in.) in diameter. Less press force was used, and costs were reduced 50%.

Before forming, the blank was rough drawn (Fig. 17b) in a 1330 kN (150 tonf) hydraulic press to a depth of 35.6 mm (1.40 in.), and its thickness was reduced to 0.99 mm (0.039 in.). After degreasing and annealing, the partly formed blank was drawn in a 305 mm (12 in.) Hydroform press, using the punch shown in Fig. 17(c). The blank rested on a blankholder mounted on a subbolster. Diametral clearance between punch and blankholder was a minimum of 50% of the work metal thickness. The production rate was 30 pieces per hour.

After forming, six equally spaced 36.50 mm (1.437 in.) diam holes and a 63.50 mm (2.500 in.) diam center hole were pierced in a 490 kN (55 tonf) mechanical press. The outside diameter was trimmed in a lathe after the part had been pierced, annealed, and restruck. The completed workpiece is shown in Fig. 17(d).

Lubricants. The following example shows the importance of the lubricant and its application when the depth of draw is near the limit for the rubber-diaphragm process.

Example 7: Use of Lubricant to Eliminate Tearing and Wrinkling in Severe Rubber-Diaphragm Drawing.

The stepped cover shown in Fig. 18 represented the limit of forming severity for the rubber-diaphragm equipment that was available. The material was 1.0 mm (0.040 in.) thick cold-rolled drawing-quality 1008 steel. The shell was 102 mm (4 in.) deep and had a step in its outer contour. Attempts to draw the stepped shell in one operation in a Hydroform press were not successful. Subsequently, two Hydroforming operations were developed in which the larger width of the cover was drawn first, and then the narrower portion above the step was produced in a redrawing operation to complete the part.

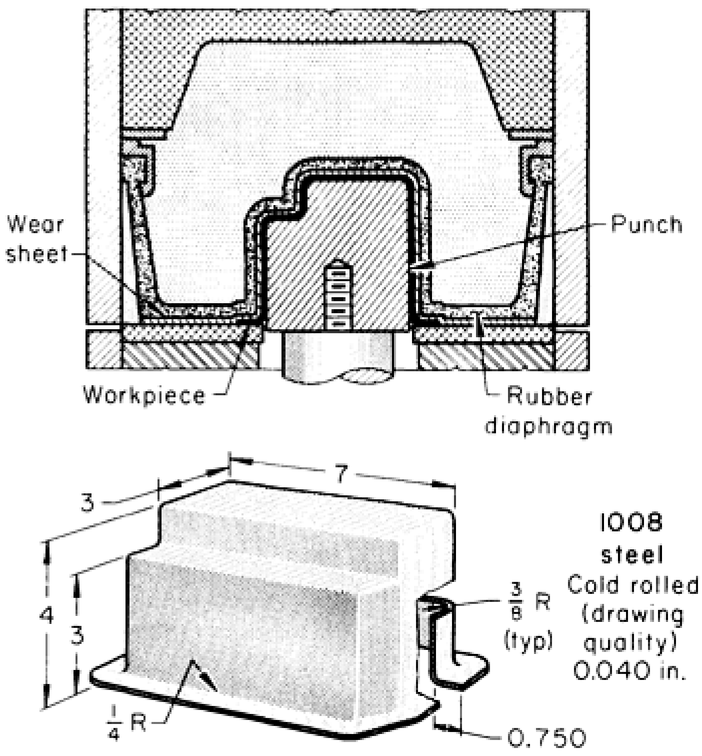

Fig. 18 Drawing of a stepped cover by the fluid-cell process. Dimensions given in inches.

In the first operation, the blankholding pressure had to be carefully adjusted. When the pressure was too low, the metal moved freely and wrinkles appeared at the corners. Too high a blankholding pressure caused tears along the narrow end. Tears and wrinkles damaged the wear sheet and, in extreme cases, the diaphragm itself.

A lubrication program was developed that prevented wrinkling or tearing. After the first draw, the workpiece was cleaned, annealed, and phosphate coated. The phosphate made it possible to use a lighter oil and to apply it more effectively, with heavy applications in some areas and little or none in others. With experience, the operators became expert at judging the location and thickness of the lubricant. Mechanical application of lubricant could not be made selective enough or controlled closely enough for consistent results.

Because the part was nearly impossible to produce by conventional deep-drawing techniques, rubber-diaphragm forming was used. The tools, consisting of two punches and a blankholder, cost considerably less than the several sets of draw dies that would otherwise have been needed, and with the lubricating technique that was developed, there was less danger of tearing or wrinkling than by other processes.

Surface Finish. A major reason for using any rubber-pad process is to preserve the surface finish of the work metal, which would be scuffed or marked by ordinary press-forming tools. In the following example, appearance was an important consideration. The part was to be plated with copper-nickel-chromium. Forming by the rubber-diaphragm method prevented marks that would have been difficult to buff out before plating.

Example 8: Use of a Rubber-Diaphragm Process to Preserve Surface Finish on a Flatiron Shell.

Because a mechanical draw press caused an impact line on the workpiece that was difficult to remove by buffing, production of the flatiron shell shown in Fig. 19 was changed to a rubber-diaphragm process, using a 3.6 MN (400 tonf) Hydroform press. A rubber draw ring of Durometer A 92 hardness helped adjust hold-down pressure so that wrinkles were avoided in the finished product. Two rubber pads were used on the rubber diaphragm. One covered the diaphragm as a reinforcement and protector; the other was a 9.5 mm ($\frac{3}{8}$ in.) thick ring molded to the shell outline. The blank was located in a nest on the blankholder.

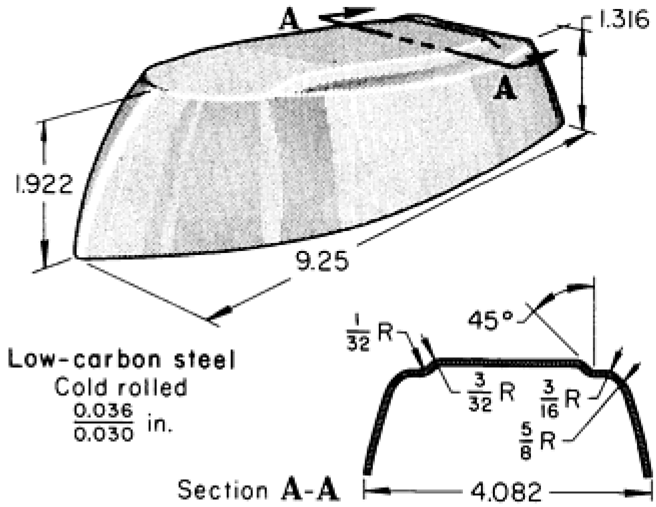

Fig. 19 Flatiron shell that was formed by the fluid-cell process in a Hydroform press to preserve the surface finish. When this shell was drawn in conventional dies, an impact line was caused below the radius that was difficult to remove by buffing. Dimensions given in inches.

Previously, the part had been drawn on a single-action mechanical press of 890 kN (100 tonf) capacity. In this press, the tools had been made of D2 tool steel. The stock was treated with soap and wiped with hydraulic oil near the point of the shell to minimize tearing.

The Hydroform press cycled at 450 strokes per minute. The production-lot size was 50,000 pieces, and yearly production was 850,000 pieces. Life of the rubber pads was as high as 20,000 pieces, and the finish of the part was good enough for subsequent plating with a minimum of buffing.

The sequence of operations was as follows: cut off blank, draw in Hydroform press, trim, pierce, copper plate, buff, nickel-chromium plate. The stock was 0.84 ± 0.08 mm (0.033 ± 0.003 in.) thick cold-rolled low-carbon steel sheet slit to width. Two different qualities of steel were used:

- Aluminum-killed drawing-quality special-surface steel with a commercial finish, dry, maximum hardness HRB 60
- Cold-rolled aluminum-killed steel strip with a No. 2 finish, dry, dead soft, maximum hardness HRB 55

The tolerance on important dimensions was ±0.08 mm (±0.003 in.); on angles, ±½°.

Single-Draw Operation. In the following example, a pressure dome was mounted on the ram of a single-action hydraulic press. The punch was fixed to a shoe mounted on the bolster plate. A die cushion provided the blankholding force. This setup functioned much like a conventional draw die except that the oil-filled pressure dome and rubber diaphragm replaced the draw ring and die cavity.

Example 9: Forming of an Automotive Tail-Lamp Housing in One Drawing Operation in a Rubber-Diaphragm Press.

An automotive tail-lamp housing was drawn in one operation from an aluminum alloy 5457-O blank 1.2 mm (0.048 in.) thick and 311 mm (12¼ in.) in diameter, in a rubber-diaphragm press rated at 69 MPa (10 ksi), as shown in Fig. 20. A water-soluble low-foaming lubricant was used. The production rate was 425 to 450 pieces per hour.

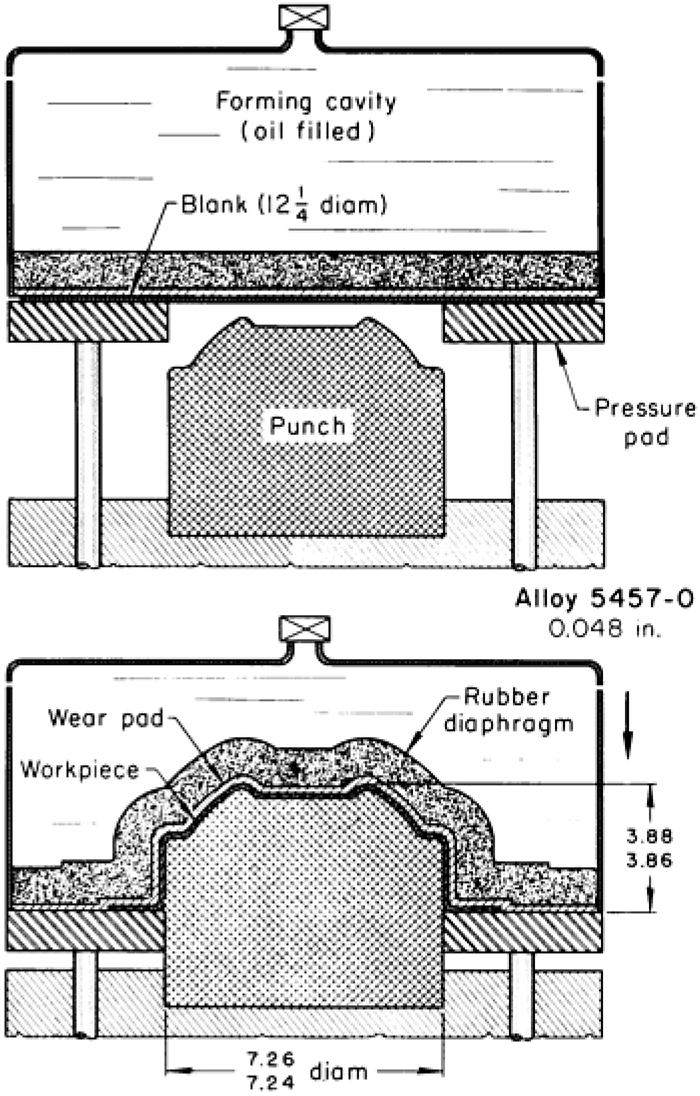

Fig. 20 Forming of an automotive tail-lamp housing in one draw in a fluid forming press. Dimensions given in inches.

To produce this part with conventional tooling, two drawing operations would have been needed to form the sharp radii at the top and bottom of the part. Tooling costs for the rubber-diaphragm press were less than one-third the cost for conventional press tooling.

The blanks were moved from a stack adjacent to the press to an automatic feeder by a pneumatic suction transfer device. A photoelectric cell prevented more than one blank being transferred. The blank passed between lubricating rollers before being fed automatically into the die. In subsequent operations, the housing was trimmed, flanged, and pierced in a mechanical press, using two conventional dies.

SAAB Rubber-Diaphragm Method

For some applications, the male member of a die set is made of rubber, and the female member is made of a hard material. In the Guerin process, shallow draws are made by recessing the form block and using the rubber pad as a punch to form the part (see the section "Shallow Drawing" in this article). The advantage of this method is that the flange is clamped before drawing, thus preventing wrinkling.

In the SAAB rubber-diaphragm method, hydraulic fluid is used behind a comparatively thin rubber pad or diaphragm. A hydraulic piston compresses the fluid against the rubber and forces the blank into the die, as shown in Fig. 21.

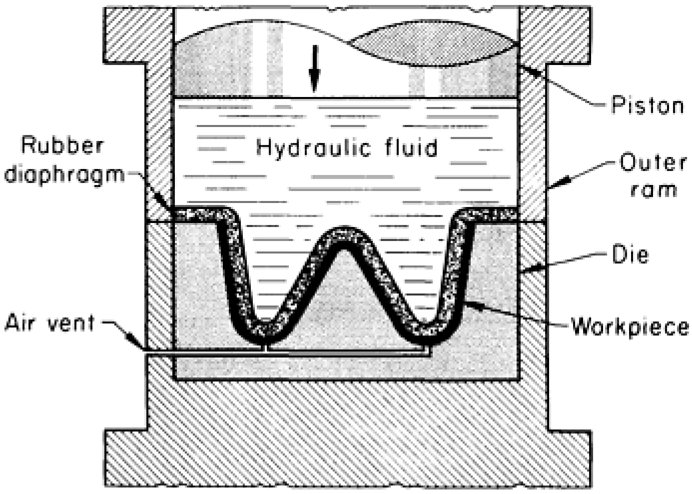

Fig. 21 Principals of SAAB rubber-diaphragm (fluid forming) method. The air vents keep trapped air from causing blisters on the workpiece.

In all rubber-punch forming processes, air vents are provided in the die to allow the air trapped between workpiece and die to escape (Fig. 21). Without air vents, the trapped air would prevent the workpiece from reaching the full contours of the die, and the workpiece would have to be removed after partial forming to release the compressed air and then replaced in the same die to complete the forming.

Bulging Punches

Rubber punches can be used to make tubular parts that must be expanded or beaded somewhere along their lengths. If such parts were made with solid punches, the punches would have to be collapsible so that they could be withdrawn.

Hollow shapes can be bulged into suitable mating dies by applying a vertical force to the punch. The dies must be segmented so that the resulting bulged product can be removed, as shown in the following example.

Example 10: Forming of a Mushroom Shape in a Segmented Bulging Die.

Figure 22 shows the process used in forming a mushroom-shaped frying-pan cover. The workpiece was a rectangular drawn shell of stainless steel, which was placed over a rubber punch of the same shape.

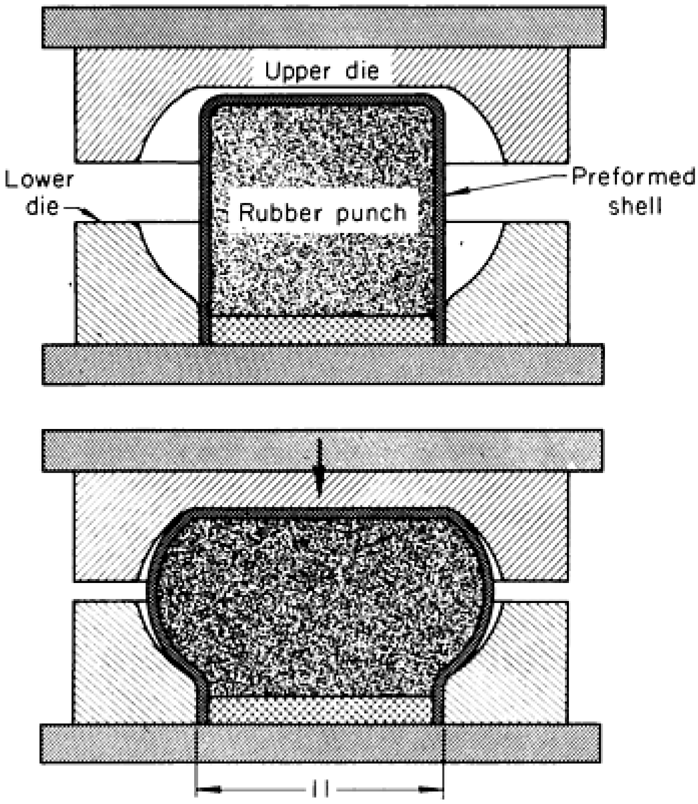

Fig. 22 Bulging of a mushroom shape from a preformed shell in a two-piece die with a rubber punch.

The two dies, which contained between them a cavity of the shape required, were closed until the rubber punch bulged the workpiece. The amount of bulge was determined by the depth of stroke. When the dies were opened, the punch returned to its original shape and was easily extracted from the finished part.

ASEA Quintus Fluid Forming Press

The ASEA Quintus fluid forming press is a vertical press having a circular fluid form unit that contains the rubber diaphragm and pressure medium (Fig. 23). These modular fluid form units serve the same function as the units used in the ASEA Quintus deep-drawing fluid forming press (see the section "ASEA Quintus Deep-Drawing Technique" in this article). The rigid tool half may be a male block, a cavity die, or an expansion die that is situated in a movable tool holder. Blanks are loaded onto the tool holder prior to shuttling the holder into the press for the 10 to 50 s forming cycle required to produce the part.

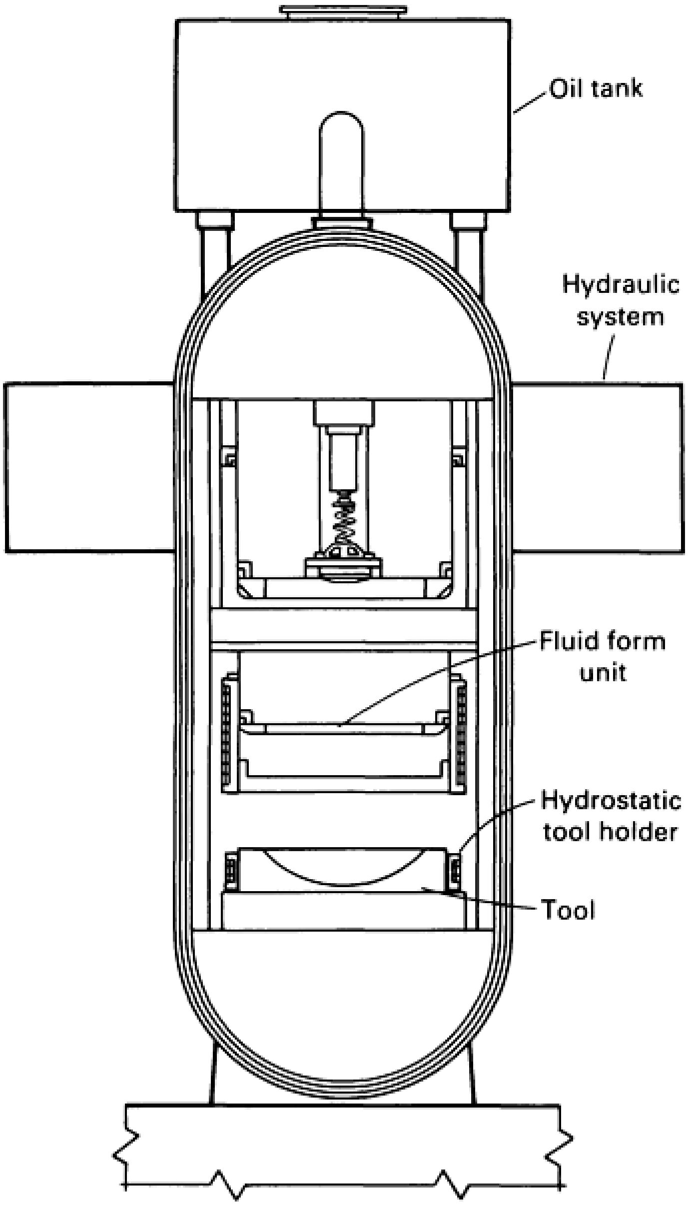

Fig. 23 Schematic of ASEA Quintus fluid forming press showing self-contained fluid form units.

For a 56 MN (6300 tonf) fluid forming press, two fluid form units designed for maximum 315 mm (12.4 in.) draw depth can be used. One unit, with a 63 MPa (9100 psi) forming pressure, has a 1090 mm (43 in.) blank diameter capacity. The other unit, providing a 160 MPa (23.2 ksi) forming pressure, has a 690 mm (27 in.) blank diameter capacity.

ASEA Quintus Deep-Drawing Technique

Fluid forming has been optimized using the ASEA Quintus deep-drawing fluid forming technique, a variation of the SAAB rubber-diaphragm method. The ASEA Quintus fluid forming method incorporates two telescopic rams, an outer ram to control dome pressure, and an inner ram to regulate the length of the punch draw (Fig. 24). Interchangeable domes, which require 20 min to change, allow the user to select a dome of optimal size and to avoid uneconomical use of an oversize dome when manufacturing small parts. Maximum forging pressure can also be increased by installing a smaller dome.

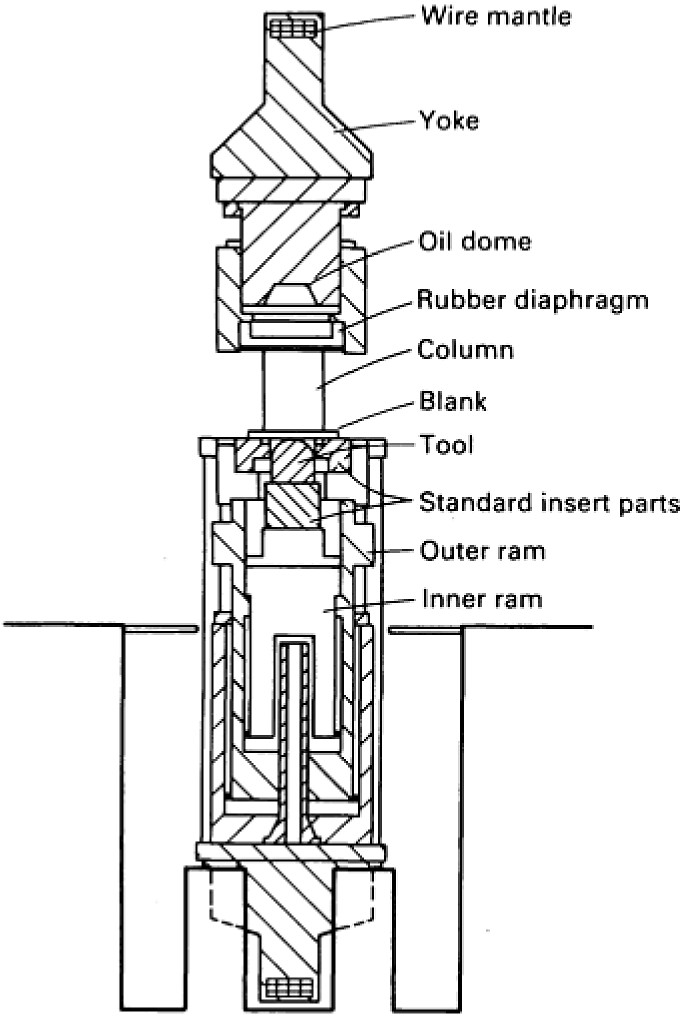

Fig. 24 Schematic of ASEA Quintus deep-drawing press, a fluid forming press with a telescopic ram system.

Each dome is a completely self-contained unit, with rubber diaphragms sealing off the pressure medium. Fluid form units for various pressure levels between 50 and 200 MPa (7.3 and 29 ksi) are available for each press size. Two or three such units should enable the user to make the most cost-effective use of the deep-drawing press. As an example, a set of interchangeable fluid form units for a 27 MN (3000 tonf) press ranges from an 800 mm (31.5 in.) diam unit with a maximum 60 MPa (8.7 ksi) forming pressure to a 450 mm (17.7 in.) diam unit with a maximum 190 MPa (27 ksi) forming pressure. Cycle time ranges from 10 to 60 s, depending on draw depth, part configuration, installed power, and selected pressure.

Complex shapes require accurate press control. As a result of 96 photocells that monitor the continuously varying pressure in the dome, greater accuracy and reliability are attained using a computer program to regulate the position and velocity of the draw depth. A paper cam cut with scissors gives dome pressure versus draw depth. The network of photocells reads the cam and governs an electrohydraulic control valve that controls the oil pressure in the outer cylinder, which is proportional to the dome pressure but much lower. By controlling the low counteracting pressure instead of the dome pressure, increased accuracy and reliability are achieved.

Diameters to 2000 mm (79 in.) can be formed from blanks ranging from 0.1 to 16 mm (0.004 to 0.63 in.) thick. Draw ratios to 3:1 can be produced, making it possible to form a complex part in one operation.

Rubber-Pad Forming

Failures in Rubber-Die Flanging

The rubber-pad forming of flanges can be performed within certain limits. The flange must be wide enough to develop sufficient bending force (Table 2), but not so wide as to exceed the permissible depth of the part. Figure 25 shows some typical flanging failures.

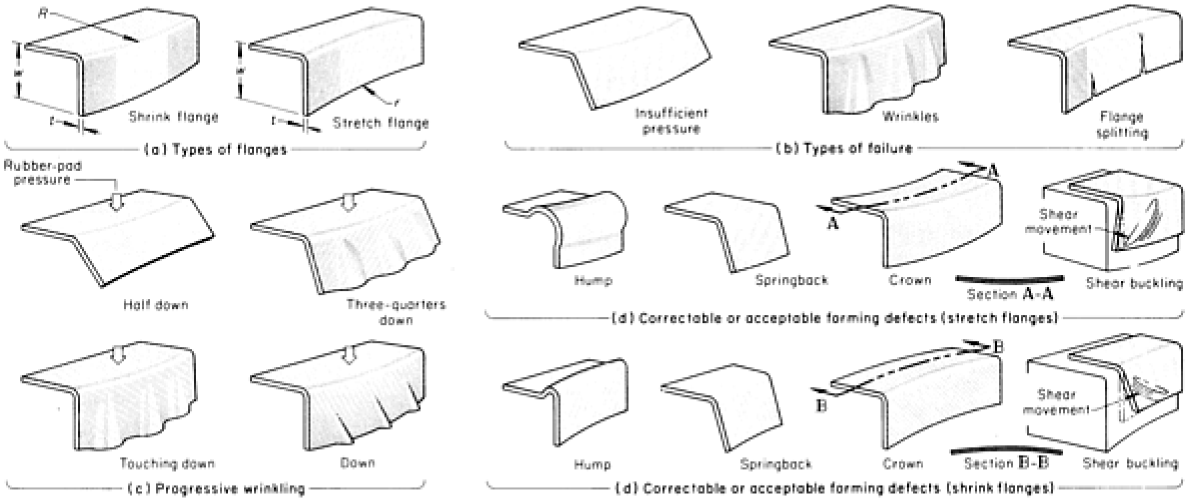

Fig. 25 Principal types of failure in curved flanges made by rubber-die forming.

Three-Roll Forming

Introduction

THREE-ROLL FORMING is a process for forming plate, sheet, bars, beams, angles, or pipe into various shapes by passing the work metal between three properly spaced rolls. This article will discuss sheet and plate, the mill products most often formed by the three-roll process.

Shapes Produced. Figure 1 illustrates some of the shapes commonly produced from flat stock by three-roll forming.

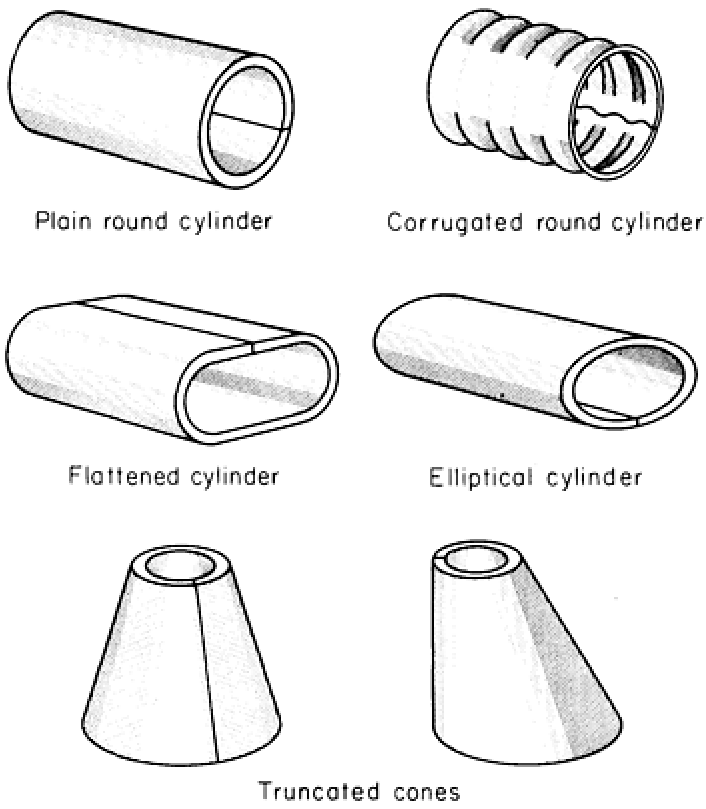

Fig. 1 Typical shapes produced from flat stock by three-roll forming.

The plain round cylinder shown in Fig. 1 is used for pressure tanks, boilers, and related containers, and it represents a large portion of the shapes produced. The corrugated cylinder is produced in quantity for culvert pipe and is formed from flat stock corrugated at the mill. To retain the corrugations in the workpiece, the forming rolls also must be corrugated.

The flattened cylinder (obround) is primarily used for oil-supply tanks for heating systems and transformer cases. The elliptical cylinder is used for tank trucks hauling liquid food products, petroleum products, and chemicals.

Symmetrical and asymmetrical cones are both used in a wide variety of hoppers, bins, vertical storage tanks, concrete mixers, and vessels for chemical and food processing, as well as in piping and ductwork. In addition to the shapes produced for commercial use, three-roll forming is also used to produce various regular and irregular shapes for structural sections of submarines, aircraft, and nuclear reactors.

Metals Formed. Any metal ductile enough to be cold formed by other processes can be formed in a three-roll machine. Steels with a maximum carbon content of 0.25% constitute a major portion of the total tonnage used in three-roll forming. Steel sheet or plate in the 1010 to 1020 category is sometimes used, but most of the steels formed by this process conform to one of the plate specifications: either plain carbon or low-alloy steels, such as ASTM A515 grade 60, A515 grade 70, A516 grade 70, A285, A441, A283, A306, and A36. For the most successful three-roll forming, steels with a minimum elongation of 18% are preferred. Stainless steels, heat-resistant alloys, and aluminum and copper alloys can also be successfully formed by the three-roll process.

Metal thicknesses commonly used range from 1.52 mm (0.0598 in.) sheet (16 gage) to 254 mm (10 in.) plate. In a few applications, 305 mm (12 in.) plate has been successfully formed. The principal factors limiting maximum thickness are the size and power of the rolling machine. Minimum thickness is typically limited only by handling equipment. Any sheet that can be handled without damage can usually be rolled.

It is impractical to roll thicknesses ranging from 1.52 to 254 mm (0.0598 to 10 in.) on the same machine, although any machine can handle a relatively wide range of work metal thicknesses. For example, a machine capable of rolling 9.5 mm ($\frac{3}{8}$ in.) plate (maximum or near maximum) can generally roll sheet as thin as 1.52 mm (0.0598 in.), while a machine with a maximum capability for rolling 1.52 mm (6 in.) plate can successfully roll plate as thin as 13 mm (½ in.)--even less on some machines.

Diameter and Width. The minimum diameter of a workpiece that can be successfully formed in a given machine is governed by the diameter of the top roll on either of the two types of machines used in three-roll forming--pinch type or pyramid type. In general, the smallest cylinder that can be rolled under optimal conditions is 50 mm (2 in.) larger in diameter than the top roll of a pinch-type machine. On a pyramid-type machine, the minimum workpiece diameter is rarely less than 152 mm (6 in.) greater than the top roll. However, more power is required to form sheet or plate into cylinders of minimum diameter than to form cylinders substantially larger than the top roll.

The maximum workpiece diameter that can be rolled is primarily limited by the space available above the machine to accommodate extremely large circles. Thin-gage metal rolled to a large diameter on horizontal rolls becomes less self-supporting as the workpiece diameter increases, and out-of-round cylinders will result if supports are not used. However, by using supports, almost any diameter can be rolled from thin metal. In general, 1.52 mm (0.0598 in.) thick low-carbon steel sheet can be formed into cylinders as large as 1.2 m (48 in.) in diameter without support, while 6.4 mm (¼ in.) thick low-carbon steel can be formed into cylinders as large as 2.1 m (84 in.) in diameter without support.

The width (dimension of the work metal parallel with the axes of the rolls, designated as length in the formed cylinder) of sheet or plate that can be rolled is limited by the size of the equipment; machines with rolls as long as 12.5 m (41 ft) have been built. The width-to-diameter relationship for workpieces that are extremely large in both directions is limited by problems in handling.

Three-Roll Forming

Machines

There are two basic types of three-roll forming machines: the pinch-roll type and the pyramid-roll type. The rolls on most three-roll machines are positioned horizontally; a few vertical machines are used, primarily in shipyards. Vertical machines have one advantage over horizontal machines in forming scaly plate: Loose scale is less likely to become embedded in the work metal. With vertical rolls, however, it is difficult to handle wide sections that require careful support to avoid skewness in rolling. Most vertical machines have short rolls for fast unloading and are used for bending narrow plate, bars, and structural sections.

Conventional pinch-type machines have the roll arrangement shown in Fig. 2. For rolling flat stock up to about 25 mm (1 in.) thick, each roll is of the same diameter. However, on larger machines, the top rolls are sometimes smaller in diameter to maintain approximately the same surface speed on both the inside and outside surfaces of the plate being formed. These heavier machines are also supplied with a slip-friction drive on the front roll to permit slip, because of the differential in surface speed of the rolls. Therefore, as work metal thickness increases, the diameter of the top roll is decreased in relation to the diameter of the lower rolls.

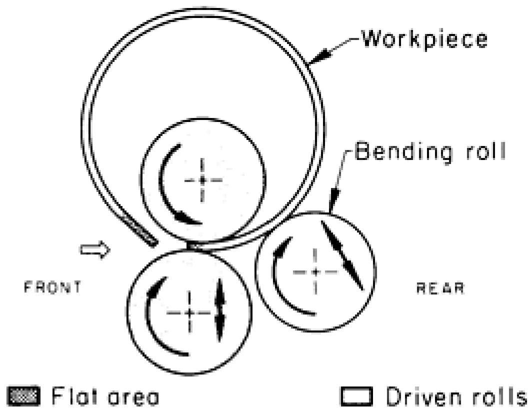

Fig. 2 End view of a cylindrical workpiece being rolled in a conventional pinch-type machine. Note the large flat

area on the leading end and the smaller flat area on the trailing end.

The position of the top roll is fixed, while the lower front roll is adjustable vertically to suit the thickness of the blank. Optimal adjustment of the lower roll is important for gripping the stock and for minimizing the length of the flat areas on the workpiece. The rear, or bending, roll is adjustable angularly (usually 30° off vertical), as shown in Fig. 2. Angular movement of this roll determines the diameter of the cylinder to be formed.

All of the rolls are powered in most pinch-type machines. On some machines, however, only the two front rolls are powered and the bending roll is rotated by friction between the roll and the work metal (Fig. 2). This arrangement is usually satisfactory in forming medium-to-heavy stock to large diameters. However, when forming sheet or plate that is thin or soft (or both) or when the diameter is large, the amount of friction is sometimes insufficient to rotate the bending roll. This condition can result in a marred surface if the work metal is soft or has a bright mill finish (aluminum sheet, for example).

A pinch-type machine can produce a more nearly true cylindrical shape than a pyramid-type machine because the work metal is held more firmly. This results in smaller flat areas on the leading and trailing ends of the workpiece.

As shown in Fig. 2, the work metal is fed to the powered pinch rolls (front), which grip the plate and move it through the machine. Forming begins when the work metal contacts the bending roll (rear) and is forced upward. As the forward motion of the workpiece continues, a cylindrical shape is produced, except for the unformed flat area along the leading end and a small flat area at the trailing end of the workpiece (Fig. 2). The width of the flat area on the trailing end usually ranges from ½*t* to 2*t* (*t*, work metal thickness), depending on the design of the machine.

In most pinch-roll forming, one of two procedures is used to minimize flat areas. The most common method is to preform both ends of the work metal in the machine. This is done by reversing the rotation of the rolls and feeding a short section of the work metal from the rear, thus preforming one end. The work metal is then removed from the machine; the formed section can be fed into the machine from the front, or it can be turned to the opposite unformed end and fed through from the rear of the machine. This procedure eliminates most of the flat areas.

Another method is to preform the leading and trailing ends of the work metal in a press brake, hydraulic press, or joggling press. However, this technique is seldom used, because it is usually more convenient to preform in the pinch-roll machine.

On the other hand, preforming in a press brake or in hydraulic or joggling presses can sometimes save time in the rolling machine, thus increasing the productivity of the machine. Additional advantages of a pinch-roll machine, as compared to a pyramid-roll machine, are:

- When all rolls are power driven, thinner sheets can be rolled, and cylinders can be formed to within about 50 mm (2 in.) of the diameter of the top roll
- A given size of a pinch-type machine can roll a greater range of metal thicknesses because of the method of feed
- Greater dimensional accuracy can be obtained in one pass in a pinch-type machine than in a pyramid-type machine

The principal disadvantage of a pinch-roll machine is its unsuitability for rolling workpieces from angles, channels, and other structural forms.

Shoe-Type Pinch-Roll Machines. One important modification of the conventional three-roll pinch-type machine is the shoe-type machine, which uses the pinch principle and incorporates a forming shoe, as shown in Fig. 3. Because of the relationship of the two front rolls and the forming shoe to the workpiece, the flat area becomes barely discernible compared with the length of flat area obtained when rolling in a conventional machine (without preforming).

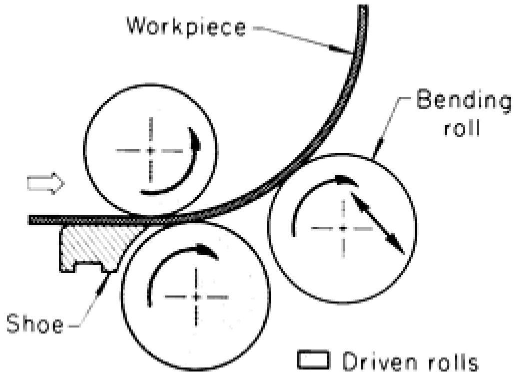

Fig. 3 End view of a cylindrical workpiece being rolled in a shoe-type machine with two powered rolls.

The shoe-type machine is often used to manufacture transformer cases and small tanks, such as jackets for hot-water tanks. This type of machine can be completely automated; therefore, the work metal can be positioned on the table and fed into the machine automatically. During the work cycle, the cylinder is formed and ejected by means of an ejector mechanism and an automatically controlled drop end. Thus, a shoe-type machine is primarily a production machine that is used where large quantities of identical workpieces are to be rolled. For this reason and because of the limitations listed below, shoe-type machines seldom compete directly with conventional pinch-type machines:

- Thickness of the work metal is limited to 12 gage (2.657 mm, or 0. 1046 in.)
- Width of the sheet is limited to 1.83 m (72 in.)
- Shoe-type machines are best adapted to the rolling of round cylinders; the rolling of ovals or obrounds is impractical
- Shoe-type machines are applicable only to cold forming

Within their range of applicability, however, shoe-type machines can produce a rolled cylinder in about half the time required in a conventional machine, primarily because preforming is not required with a shoe-type machine.

Pyramid-Type Machines. Figure 4 illustrates the arrangement of the rolls in a pyramid-type machine. The bottom rolls are of equal diameter, but are about 50% smaller in diameter than the top roll. The bottom rolls are gear-driven and are normally fixed; each roll is supported by two smaller rolls (Fig. 4). The top roll is adjustable vertically to control the diameter of the cylinder formed. The top roll, which rotates freely, depends on friction with the work metal for rotation. Backup rolls are not used on the top roll.

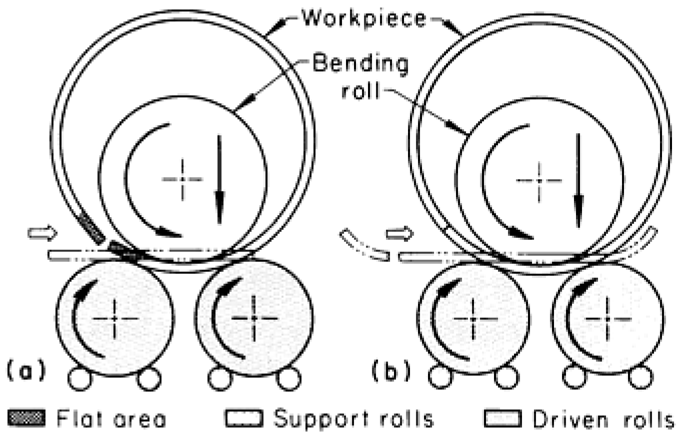

Fig. 4 Arrangement of rolls in a pyramid-type machine. (a) Entrance of flat workpiece and shape of a nearly finished workpiece, including the flat areas on the leading and trailing ends. (b) Similar, except that the

workpiece was prebent to minimize the flat areas on the ends.

As shown in Fig. 4, the work metal is placed on the bottom rolls while the top roll is in a raised position. The top roll is then lowered to contact and bend the work metal a predetermined amount, depending on the diameter of the workpiece to be formed. Machines are usually equipped with a device that indicates the amount of initial bend. Some machines use an ammeter, which shows the amount of current used in forcing the roll downward. However, this device measures force only; variables in the work metal can cause differences in the amount of bend for a given force.

As required bending force increases, machines are designed with rolls of larger diameter, and the distance between centers of the bottom rolls increases. Bending forces are applied midway between the bottom rolls; therefore, less force is needed for a given deflection, but less curvature is produced.

Because the top roll is adjustable, pyramid rolls can be used for forming irregular shapes by bolting dies to the top roll--a technique that is not adaptable to pinch-type machines. In addition, plate, beams, angles, and other structural forms can be straightened with greater ease because the bottom rolls are on the same elevation.

The top roll is an idler; therefore, there are definite limitations on the minimum thickness of work metal that can be rolled (especially when forming large diameters). Adequate stiffness in the work metal is essential to provide enough friction to rotate the top roll. The minimum thickness that can be rolled varies, depending on the specific machine and the work metal composition.

Another disadvantage of the pyramid machine is the large flat areas that remain on both the leading and trailing ends of the work metal. Because the workpiece must remain supported by the bottom rolls at all times, the ends of the work can never get closer to the top roll than the distance between the points of tangency of the workpiece and the rolls. Therefore, it is impossible to eliminate these flat areas by rolling (Fig. 4a).

To minimize flat areas when using pyramid machines, the usual procedure is to preform the ends to the desired radius in a press brake or to roll an oversize blank, then trim the flat ends after. The shell can sometimes be returned to the rolls for truing after the seam has been joined. Occasionally, a narrow shim is placed at the ends to increase the bend radius, but care must be taken to avoid machine overload. The techniques used in forming with pyramid rolls make it more difficult to achieve the accuracy that is obtainable with pinch-type rolls.

Capacity. Three-roll forming machines are rated by the manufacturer according to the maximum thickness and width of low-carbon steel plate the machine can form at room temperature. Values are usually given for single-pass rolling, and allowances are then made for multiple-pass rolling. For example, a machine rated at 19 × 3660 mm (¾ × 144 in.) (thickness and width of plate, respectively) for work metal with a maximum tensile strength of 414 MPa (60 ksi) and capable of rolling plate to a diameter of 2.44 m (96 in.) in a single pass can roll to a final diameter of 584 mm (23 in.) in multiple passes if the top roll is no larger than about 368 mm (14½ in.) in diameter.

If plate thickness is increased to 25 mm (1 in.), the same diameter restrictions would apply, but the allowable plate width would be reduced from 3.66 to 1.42 m (144 to 56 in.) because of the additional power required for the increased thickness of work metal. At this point, another limitation may be encountered because the load imposed on the shorter surface area can become excessive as the plate becomes narrower and thicker. On the other hand, assuming all other factors remain constant, if plate thickness is reduced to 16 mm ($\frac{5}{8}$ in.), the allowable width would revert to full capacity of the machine (3.66 m, or 144 in.), but the rolled diameter could be reduced to 419 mm (16½ in.).

The maximum plate thickness that can be handled by this machine depends on the pinch opening and is rated by the manufacturer of the machine. For example, some machines rated as described above can accommodate work 38 mm (1½ in.) thick, but for forming this thickness in a machine having the indicated capacity, the allowable plate width would be reduced to 533 mm (21 in.) because of the above-mentioned factors. All of the above calculations also take into consideration the limiting factor of roll deflection.

With all other conditions constant, power requirements increase according to the square of metal thickness. Therefore, the power required for forming plate 50 mm (2 in.) thick is four times as great as that required for forming 25 mm (1 in.) thick plate of the same width.

Selection of Machine

Selection between pinch-type and pyramid-type machines depends mainly on the shape of the starting form and of the finished workpiece, the number of formed parts to be produced, accuracy requirements, and the cost. The pinch-type machine produces more accurate workpieces, and it can be loaded and unloaded much faster than the pyramid-type machine. Although both machines can produce shapes other than plain cylinders, the pinch type is capable of rolling a wider range of thicknesses. However, the pyramid-type machine is often preferred for small quantities of varied work, as in a job shop. Because of the wide space that can be obtained between the upper roll and the two lower rolls in a pyramid machine, various types of dies and fixtures can be fastened to the upper roll, thus permitting channels, angles, and various other structural shapes to be rolled or bent, either hot or cold.

Rolls

Rolls used in three-roll forming machines are machined from steel forgings having a carbon content of 0.40 to 0.50% and a hardness of 160 to 210 HB. Plain carbon steel such as 1045 has often been used; when greater strength is needed, rolls are forged from an alloy steel such as 4340. Because the modulus of elasticity is the same for all carbon and low-alloy steels of medium carbon content, roll deflection for a given force will be the same.

Although the hardness range of 160 to 210 HB can be obtained by annealing, rolls with a microstructure obtained by quenching and tempering or by normalizing and tempering are less subject to surface deterioration from spalling. Therefore, the forged rolls are heat treated before being machined.

Roll diameter varies with the length and thickness of plate to be rolled. A typical top roll in a pinch-type machine, rated for forming steel plate up to 64 mm ($2\frac{1}{2}$ in.) thick and 3.66 m (144 in.) wide, would have a minimum diameter of 762 mm (30 in.). Journals for rolls of this diameter are approximately 432 mm (17 in.) in diameter.

Crowning of rolls to compensate for deflection is common practice. The amount of crowning is not necessarily the same for all rolls in a given machine. For example, in some machines, the rolls are not all of the same diameter; under these conditions, a roll that is smaller in diameter requires more crowning than a larger roll because the stress on all rolls is the same. When a machine is used for both light and heavy work, it is usual to crown the rolls for average conditions and then to use strips either at the center of the rolls to compensate for extreme deflection or at the ends to compensate for a lack of deflection (see the section "Roll Deflection" in this article).

Roll Maintenance. The extreme pressures to which rolls are subjected cause them to work harden. Rolls used in continuous production under high pressure sometimes elongate and reduce slightly in diameter. The amount of elongation or reduction in diameter is seldom significant, although the ends of rolls may require trimming after long periods of use.

There is no standard practice for reconditioning rolls. In some plants, rolls that have been subjected to long periods of severe service are trued by removing some or all of the work-hardened layer by turning. When required, the diameter is built up by welding an overlay on the rolls and then finish turning them. On the other hand, some manufacturers recommend that roll surfaces should never be turned. If the surfaces are spalled or otherwise damaged, any protruding metal should be removed by grinding. Although indentations in the rolls are less likely to be harmful, they may mark polished or clad surfaces. When rolling scaly plate, blowing away loose scale with an air lance is helpful in preventing scale from indenting the rolls or the work metal.

Bearings and Lubricants. Bronze has been successfully used for main bearings and is sometimes specified by the user. However, tin-base babbitt is superior to bronze for most applications and is used in most machines. Tin-base bearings are more compatible with the relatively soft steel journals at the pressures and speeds involved, and their ability to absorb particles of scale minimizes the possibility of scoring journals or bearings.

Extreme-pressure lubricants are recommended for the main bearings on all rolls, and a grade containing molybdenum disulfide is especially desirable. Because environmental conditions are likely to vary considerably where three-roll forming is done, the lubricant should have good pumpability over a range of temperatures. Extreme-pressure lubricants are satisfactory for both cold and hot forming.

Three-Roll Forming

Preparation of Blanks

Blanks are usually cut to the desired size before forming. The length of plate (dimension of the work metal perpendicular to the axes of the rolls) required to form a given shape is determined by measuring the mean circumference (or perimeter, if the shape is other than a cylinder), which is the circumference taken at one half the distance between the inside diameter and the outside diameter of the shape to be formed. This method of calculation is the one most generally used in both the cold and hot forming of plate.

Allowance for Shift in Neutral Axis. When greater accuracy is required, the more exact location of the neutral axis is considered when computing the blank, particularly if heavy plate thicknesses are involved. The neutral axis is the boundary between metal in tension and in compression and is usually one-quarter to one-half the thickness of the metal being bent, as measured from the inside of the bend. The exact location of this axis varies to some extent with the bend radius and the mechanical properties of the metal.

During cold forming, the neutral axis shifts inward from the mean by about 26% of the plate thickness. Therefore, for a 457 mm (18 in.) ID cylinder, rolled from 13 mm (½ in.) thick plate, the mean circumference is about 147.6 mm (58.12 in.), and for a 26% shift, the circumference at the neutral axis becomes 145.5 mm (57.30 in.). For a 457 mm (18 in.) ID cylinder of 6.4 mm (¼ in.) thick plate, the mean circumference is 145.6 mm (57.33 in.), and with a 26% shift, it changes to 144.6 mm (56.93 in.); the amount of shift is about one-half that for the 13 mm (½ in.) thick plate. Therefore, the shift of neutral axis is usually disregarded for plate thicknesses less than 13 mm (½ in.) except where greater accuracy is required.

In cold forming, the length of the blank is calculated by using a radius that is determined by subtracting 26% of the plate thickness from the mean radius or by adding 24% to the inside radius. When dimensional requirements are stringent, similar allowances are made for shift of the neutral axis and for thermal expansion in the hot forming of thick plates (75 to 152 mm, or 3 to 6 in.) at 870 °C (1600 °F).

Cutting of blanks can be done by shearing, if shearing equipment is available for the width and thickness of the work metal. Gas cutting is commonly used for preparing blanks that are too thick for shearing. Additional information is available in the articles "Shearing of Plate and Flat Sheet" and "Thermal Cutting" in this Volume.

Edge Preparation. The cut edges of any plate (high-strength steel, in particular) can be a serious problem because of cracking during cold forming, which is cause for rejection of the workpiece. When plate is sheared, the edges are rough and often have surface cracks. Gas cutting usually produces smoother edges, but the edges of gas-cut steel plate will frequently be hardened in cooling from the cutting temperature. Therefore, nucleation sites for cracks are likely to be present from either method of cutting.

The danger from cracking caused by rough edges increases as plate thickness increases and the finished diameter of the cylinder decreases. Because the plate surface that forms the outside diameter of the cylinder is in tension during forming, cracks propagate from edges, which are in tension.

On plate 25 mm (1 in.) or more in thickness, the edges indicated in Fig. 5 should be removed before cold forming. This is not required before hot forming. Usual practice is to employ a chipping hammer and then a portable grinder to smooth the edges. The amount of metal removed is usually negligible, and a slight bevel on the critical edges is sufficient. If a substantial amount of metal is to be removed, allowance must be made for it when calculating the dimensions of the blank.

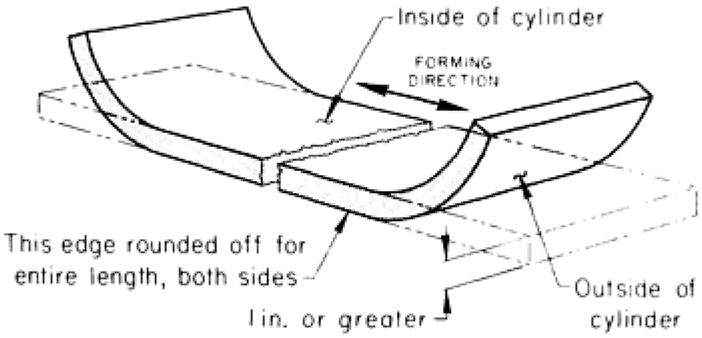

Fig. 5 Sheared or gas-cut blank, showing where metal should be removed from edges before cold forming, to reduce susceptibility to cracking.

Three-Roll Forming

Cold Versus Hot Forming

Because cold forming involves fewer problems and is less costly than hot forming, it is preferred practice to form workpieces at room temperature. In hot forming, dimensional accuracy is more difficult to control, and cost is significantly increased by:

- Heating the blank
- Handling both the blank and the workpiece while the metal is hot
- The necessity for restoring acceptable surfaces by pickling, blast cleaning, or other surface treatment
- The accelerated rate of deterioration of rolls and other equipment because of contact with hot metal

Forming Capacity. When carbon or low-alloy steel is heated, tensile strength decreases and formability increases. Heating the work metal therefore extends the usefulness of a roll-forming machine. For example, a 3 m (10 ft) long pinch-type machine with 495 mm (19½ in.) diam rolls can form a 3.66 m (144 in.) diam cylinder from 64 mm (2½ in.) thick by 622 mm (24½ in.) wide plate at room temperature in one pass (assuming the work metal has tensile strength of 410 MPa, or 60 ksi, at room temperature). With all other conditions remaining constant, by heating the work metal to a temperature high enough to reduce the tensile strength to 70 MPa (10 ksi) or lower, the width of the plate (measured parallel to the roll axes) can be increased to 2.1 m (82 in.) and rolled in one pass, using the same amount of power needed for rolling plate 622 mm (24½ in.) wide at room temperature. To reduce the tensile strength of low-carbon steel and to obtain optimal formability, the usual practice is to heat the steel to 870 °C (1600 °F).

Similarly, the size of machine described above is capable of cold forming a 3.66 m (144 in.) diam cylinder from 44 mm (1¾ in.) thick by 1.5 m (60 in.) wide low-carbon steel plate in one pass. Under the same conditions, except for heating to 870 °C (1600 °F), the thickness of the plate can be increased to 70 mm (2¾ in.).

One method of evaluating the difference in formability between cold and hot rolling is to measure the force required to form a given plate. This is done on pyramid-type rolls by measuring, in terms of amperage, the downward force on the upper roll and the force required to rotate the powered rolls (Fig. 6). The following example demonstrates the difference in current flow, number of passes, and time needed for the cold and hot forming of similar cylinders.

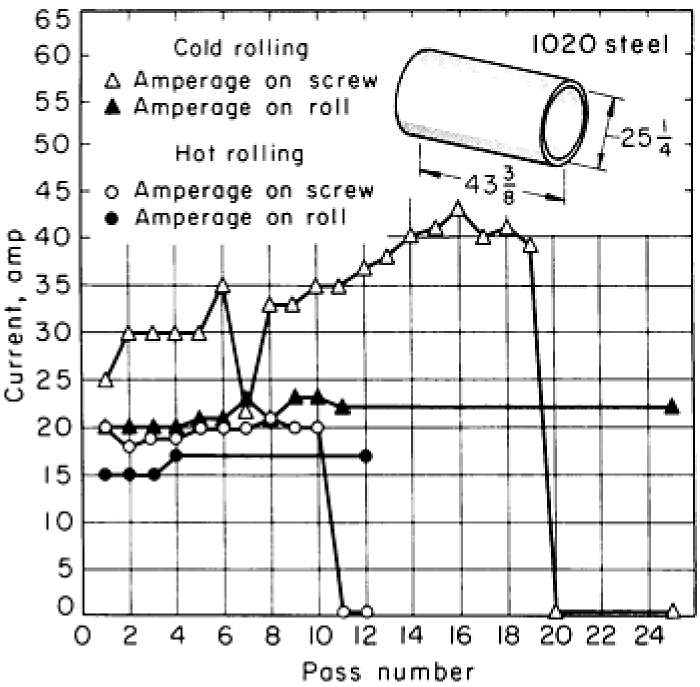

Fig. 6 Comparison of current flow (proportional to force) measured on screw and rolls during cold rolling and during the rolling of 44 mm (1¾ in.) thick plate preheated to 870 °C (1600 °F). Dimensions given in inches.

Example 1: Cold Versus Hot Forming of 44 mm (1¾ in.) Thick Steel Plate.

A pyramid-type machine was used to produce 641 mm (25¼ in.) ID by 1.1 m (43⅜ in.) long cylinders from 1020 steel blanks 44 mm (1¾ in.) thick by 1100 mm (43⅜ in.) wide by 2145 mm (84½ in.) long. When forming was done at room temperature, 25 passes were required with current flow on the upper roll (screw) and power rolls as shown in Fig. 6. Rolling time was 40 min per cylinder. When blanks were heated to 870 °C (1600 °F) and finished at 565 °C (1050 °F), the number of passes was reduced to 12 and rolling time to 11 min per cylinder. Current flow was also reduced, as shown in Fig. 6.

Hot Forming Formability Problems. The technique described in the preceding example for increasing the effective capacity of equipment by hot forming does not apply to all metals, and the amount of decrease in tensile strength varies considerably among carbon and low-alloy steels. For example, heat-resistant alloys, by definition, resist the softening effect of heat, and many of these alloys precipitation harden in the temperature range that would be used for the hot forming of carbon steel (see the article "Forming of Heat-Resistant Alloys" in this Volume). Magnesium and titanium alloys are usually formed at the same elevated temperature used to form the same alloy by other methods (see the articles "Forming of Magnesium Alloys" and "Forming of Titanium and Titanium Alloys" in this Volume). Copper and aluminum alloys are usually formed at room temperature. However, alloys 7075 and 7079, as well as some other precipitation-hardening alloys, must be formed within 24 h after solution treatment. If forming cannot be done within this length of time, the work metal must be stored at -12 °C (-10 °F) to prevent precipitation hardening (see the articles "Forming of Aluminum Alloys" and "Forming of Copper and Copper Alloys" in this Volume).

Maximum Elongation. In many applications with steel, hot forming is mandatory regardless of the capacity of available forming equipment. Common practice is to compute a maximum elongation in the outer surface for cold forming, as determined by:

$$E = \left(\frac{t}{d + t}\right)100 \quad \textbf{(Eq 1)}$$

where E is the percentage of elongation in the outer surface of the cylinder, d is the inside diameter of the cylinder (in inches), and t is the plate thickness (in inches). For example, for a cylinder having an inside diameter of 1.45 m (57 in.) to be formed from 75 mm (3 in.) thick steel plate:

$$E = \left(\frac{3}{57 + 3}\right)100 = 5\% \quad \textbf{(Eq 2)}$$

Elongation of more than 5% (determined by Eq 2) is seldom permitted for cold forming, and the maximum is often 3.5%. If the maximum permissible elongation was 3.5% in the above example, either the minimum cylinder diameter would be near 2.11 m (83 in.) or plate thickness would have to be reduced before cold forming would be permitted. Maximum elongation is established by the user of the formed product, and hot forming is used when specifications cannot be met by cold forming.

Combination Hot and Cold Forming. A combination of hot and cold forming is sometimes advantageous and permits elongation requirements to be met. For example, in forming the 1.45 m (57 in.) diam cylinder described above, one procedure is first to hot form the 75 mm (3 in.) thick plate to a circular segment of about 2.29 m (90 in.), allow the workpiece to cool to room temperature, and then clean and finish form at room temperature. This procedure makes it possible to meet more severe elongation requirements and to retain some of the advantages of cold forming, such as greater accuracy.

Hot-Forming Temperatures for Steel

Carbon or low-alloy steel plate is commonly heated to 870 °C (1600 °F) for hot forming after normalizing at the mill. However, plate in the as-rolled condition is less costly. The as-rolled steel is normalized while it is being heated for forming and cooled during forming. In such an operation, the steel is heated to 900 to 925 °C (1650 to 1700 °F), instead of to 870 °C (1600 °F), before forming.

Finishing temperature is critical for some steels, especially the plain carbon grades, because of the blue-brittle temperature range. It is generally recommended that the finish-rolling temperature should be 565 °C (1050 °F) or higher. If the workpiece cannot be completely formed before it cools to 565 °C (1050 °F), it should be removed from the machine and reheated.

Warm forming is often used when forming requirements are too severe for room temperature and when heating to the conventional hot-forming temperature cannot be permitted because the mechanical properties of the steel would be impaired. A notable example is the forming of quenched-and-tempered grades of high-strength low-alloy steel. Common practice is to heat these steels no higher than the temperatures at which they were tempered and then form at once.

Three-Roll Forming

Power Requirements

The power required to form a given cylinder on three-roll equipment depends on the strength of the work metal, plate thickness, plate width, finished diameter of the cylinder, number of passes used, and temperature (hot or cold forming).

Strength of the work metal, plate thickness, and plate width (measured parallel to the roll axes) determine the diameter of a cylinder that can be formed in a given machine. The curves in Fig. 7, for a 3.1 m (10 ft) long pinch-type roll machine with 483 mm (19½ in.) diam rolls, represent combinations of maximum plate thickness and width that can be rolled at room temperature into cylinders of 3.66 m (144 in.) in diameter or larger in a single pass. The significant influence of the strength of the steel is evident from Fig. 7. For example, 38 mm (1.5 in.) thick low-carbon steel plate of 410 MPa (60 ksi) tensile strength can be rolled into 3.66 m (144 in.) diam cylinders in one pass in widths up to 3.05 m (120 in.) in this machine. Under similar conditions, the width of 38 mm (1.5 in.) thick high-strength low-alloy steel that can be formed to the same cylinder diameter is restricted to about 56 mm (22 in.) (Fig. 7). The machine used was rated at 67 mm ($2\frac{5}{8}$ in.) by 3.05 m (120 in.).

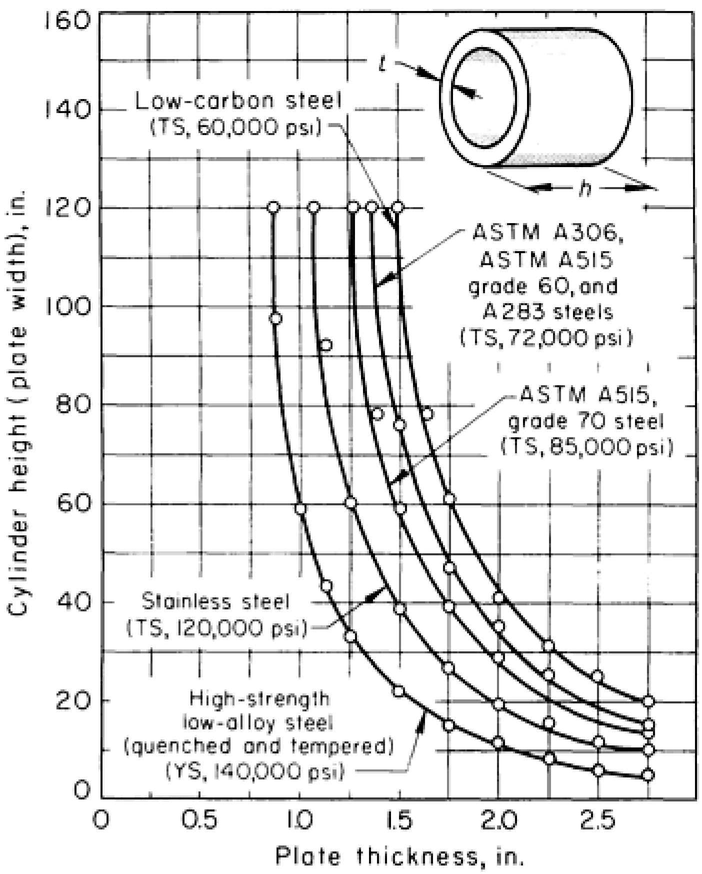

Fig. 7 Interrelationship of cylinder height (*h*), thickness (*t*), and work metal strength for forming in a single pass in a pinch-type roll machine at room temperature.

Work Hardening. Most metals are susceptible to strengthening by cold work (work hardening), although the extent to which metals are affected varies widely among the various compositions. Of the steels commonly processed by three-roll forming, those of low carbon content, such as 1010, are least susceptible to work hardening and seldom present any serious problems. As carbon or alloy content increases, the rate of work hardening increases.

For metals that work harden rapidly, power consumption increases as forming proceeds. Eventually, the machine is overloaded, or the work metal fractures.

Intermediate annealing must be used when work-hardenable metal is severely formed. For steel, full annealing is usually recommended. Process (subcritical) annealing is sometimes used. However, process annealing alternated with cold work is likely to result in excessive grain growth and subsequent poor formability, despite the hardness.

Cylinder Diameter. With other conditions constant, power requirements increase as cylinder diameter decreases when the cylinder is completely formed in one pass. However, the use of two or more passes (up to 12 is not uncommon) permits the rolling of smaller-diameter cylinders without increasing machine size. For example, Fig. 7 shows that 38 mm (1.5 in.) thick by 3.05 m (120 in.) wide plate of low-carbon steel can be formed into a cylinder 3.66 m (144 in.) in diameter in one pass on a given machine. By using 10 or 12 passes, cylinders as small as 65 mm (25½ in.) in diameter have been formed from the maximum thicknesses shown in Fig. 7. For plate thickness of 30 mm ($1\frac{3}{16}$ in.) or less, cylinders as small as 55 mm (21½ in.) in diameter can be formed in multiple passes.

Similarly, for carbon steel having a tensile strength to 590 MPa (85 ksi) and stainless steel to 830 MPa (120 ksi), any of the plate thicknesses shown in Fig. 7 can be rolled to cylinders as small as 70 mm (27½ in.) in diameter. For steels having a tensile strength of about 500 MPa (72 ksi), plate thicknesses of 28 mm ($1\frac{3}{32}$ in.) or less can be rolled to cylinders 56 mm (22 in.) in diameter, using the multiple-pass procedure. The plate thickness limitations for rolling 56 mm (22 in.) diam cylinders then decrease to 26 mm ($1\frac{1}{64}$ in.) (maximum) for steel with a tensile strength of 590 MPa (85 ksi) and to 21 mm ($\frac{13}{16}$ in.) for stainless steel with a tensile strength of about 830 MPa (120 ksi).

Power requirements for quenched-and-tempered high-strength low-alloy steel (970 MPa, or 140 ksi, in Fig. 7) are high, and there is an increased probability of cracking. Suggested limits for maximum plate thickness and minimum cylinder diameter for multiple-pass rolling, regardless of power, are:

Maximum plate thickness		Minimum cylinder diameter	
mm	**in.**	**mm**	in.
25	1	1170	**46**
19	$\frac{3}{4}$	889	**35**
13	$\frac{1}{2}$	826	**32** $\frac{1}{2}$
9.5	$\frac{3}{8}$	775	**30** $\frac{1}{2}$
6.4	$\frac{1}{4}$	**711**	**28**

Temperature. The limitations imposed by steel composition and other factors shown in Fig. 7 are markedly changed when the work metal is heated (see the section "Cold Versus Hot Forming" and Example 1 in this article). However, it is not always possible to use hot forming--for example, for quenched-and-tempered high-strength low-alloy steel (see the section "Warm Forming" in this article).

Three-Roll Forming

Forming Small Cylinders

The cold forming of small cylinders by the three-roll process requires extra care, especially when the diameter of the cylinder to be formed is near that of the rolls. The rolling of cylinders having an inside diameter of less than 50 mm (2 in.) more than the outside diameter of the roll is not generally recommended. However, a skilled operator, using special care, can form cylinders within 38 mm (1½ in.) in diameter in a pinch-type machine.

Forming Large Cylinders

Cylinders that are large in diameter or in length can be formed by the three-roll process. Some special procedures may be required, especially when the sheet is so thin that the cylinder cannot retain a round shape without support. Under these conditions, overhead cranes or temporary braces or both can be used for support.

When flat blanks of the required length are unavailable, two or more sections can be welded together to obtain the required length. The following example describes this practice in the forming of large cylinders.

Example 2: Forming 4.72 m (186 in.) OD Cylinders in a Pinch-Type Machine.

Cylinders 4.72 m (186 in.) in diameter and 2.84 m (112 in.) long were formed from blanks prepared by welding together three sections of copper-clad low-carbon steel. The fabricated blanks were 14.8 m (580¾ in.) long by 2.84 m (112 in.) wide by 14 mm ($\frac{9}{16}$ in.) thick. The cylinders were formed cold on a 19 mm (¾ in.) by 3.7 m (12 ft) pinch-type machine. The major problem in this operation was support because the work was not thick enough to provide natural support. Overhead cranes were used to hold the formed section of the plate during rolling. The ends of the cylinder were tack welded before the workpiece was removed from the machine. Temporary braces were then used to hold the cylinders in a near-round condition while the seam was welded. After welding, outside rounding rings made from 38 × 127 mm (1½ × 5 in.) rectangular steel bars were used to hold the cylinder shape for subsequent attachment to other cylinders. In use, the welded internal parts stiffened the assembly.

Forming Truncated Cones

Pyramid-type machines are generally used in the forming of truncated cones. There are two basic limitations on the shape of conical configurations that can be formed by the three-roll process. First, the smaller diameter (A, Fig. 8) must be large enough to maintain the established workpiece-to-roll relationship on minimum obtainable diameter, and second, the plate must be thick enough to be formed by the holdback method shown in Fig. 8 so that the pressure buildup on the holdback pin does not upset the plate edge or damage the holdback attachment or both.

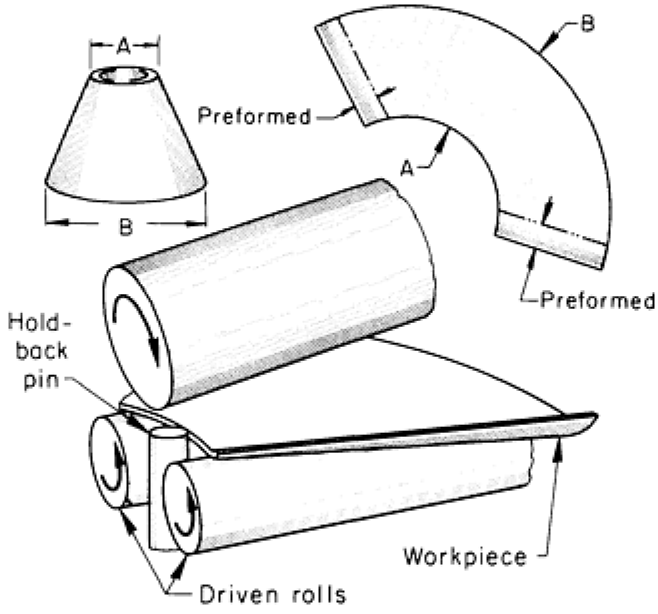

Fig. 8 Rolling a truncated cone in a three-roll pyramid-type machine from a blank with preformed ends.

The size of cone that can be formed depends largely on the pitch, the spacing and length of rolls, and the diameter of the upper roll. As the cone height increases, difficulties in getting the large diameter to follow the small diameter accurately also increase. If the lower roll spacing is too wide, the size of the smaller diameter will be severely limited, because as the work metal passes the holdback pin (Fig. 8), it will curve into the housing that supports the rolls and rolling will be impossible.

Procedure. There are several significant differences between forming conical shapes and forming cylinders by the pyramid-type three-roll process. In rolling cylinders, the blank is rectangular and is rolled in a direction perpendicular to the rolls. In contrast, curved blanks are used for conical shapes, and they are rolled on a curve. In addition, no pin is needed in rolling cylinders, and the top roll is pitched for forming cones but is straight and level when forming cylinders.

Before the developed blank is placed in the pyramid-type three-roll machine for forming a cone, the ends of the blank are preformed in a pinch-type roll from the rear or in a press brake. For forming the cone, the top roll is pitched as shown in Fig. 8. As the rolls drive the blank, its edge drags around the pin, and the various diameters of the cone are formed. The blank is then rolled in multiple passes, dragging around the pin until the ends of the blank are closed. If the pitch of the rolls matches the pitch of the entire length of the cone, a nearly true cone will result.

Truncated cones are sometimes produced by forming two semicircular half-cones and welding them together; the completed workpiece has two longitudinal seams instead of one. In another method, two or more circular tapered sections are formed and welded together. When produced by this method, the finished cone has one longitudinal seam and one or more circumferential seams.

Three-Roll Forming

Forming Bars and Shapes

Pyramid rolls and machines with over-hanging rolls are used to form bars, bar sections, and structural shapes into circles. Shapes that can be processed by this method include rounds, squares, flats (on the edge or on the flat), I-beams, L-shaped structurals, and channels. Hardened roll sections that are adjustable to the thickness or cross section of the shape are used (Fig. 9). Some bars or shapes can be formed with plain, flat rolls, but more often rolls conforming to the shape of the unformed workpiece are required.

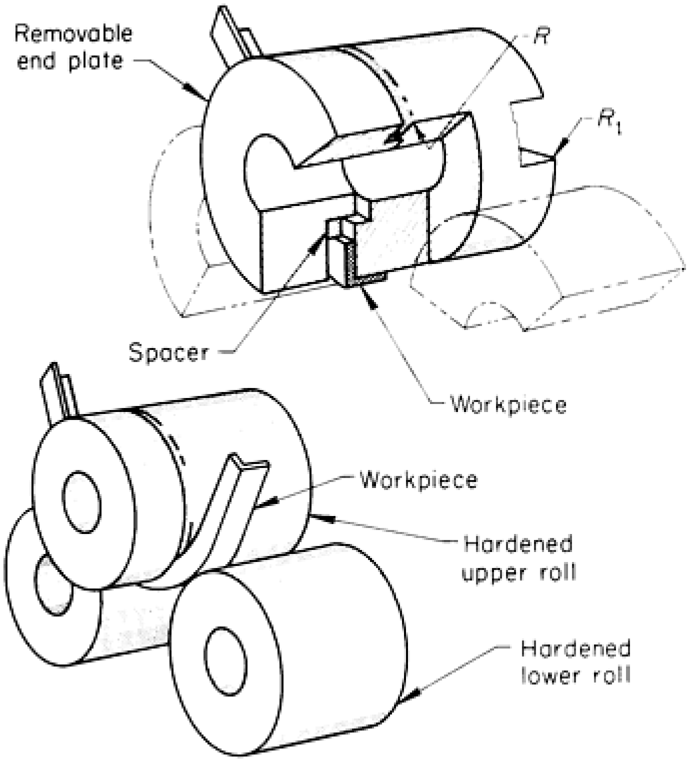

Fig. 9 Roll setup for forming an angle section into a circle. Guide rolls and guide fingers are not required for this application.

The rolls are adjusted to produce the required workpiece diameter in the same manner as in rolling cylinders from sheet or plate. Prebending the ends and rolling the workpiece back and forth are also employed in the three-roll forming of bars and shapes. A significant difference in the rolling of bars and shapes is the frequent use of guide rollers or guide fingers or both, mounted so as to contact the sides of the workpiece and prevent it from twisting during forming.

One of the most difficult shapes to form by the three-roll process is an angle with one leg inside the circle (Fig. 9). When forming this shape, spiraling, twisting, buckling of the inside leg, and reduction of the angle between the two legs are likely to occur. These difficulties can be minimized by using a hardened upper roll having a removable end plate and a spacer that is not more than 0.81 mm ($\frac{1}{32}$ in.) thicker than one leg of the angle (Fig. 9). The radius on the upper roll (R, Fig. 9) must conform to the fillet radius of the angle to be rolled. The other end of this roll (R_1, Fig. 9) can have the same radius or a radius conforming to another angle to be rolled. This end of the upper roll can be used by reversing the roll end for end. In production rolling of the same angle, common practice is to have the same values for R and R_1 (Fig. 9), thus permitting double the roll life by reversing the roll. Guide rollers and fingers (not required for the section shown in Fig. 9) also help to produce accurate circles from bar sections.

Out-of-Roundness

Out-of-roundness, or ovality, of cylinders produced by three-roll forming is caused by one or more of the following variables:

- Varying thickness of the flat blank
- Varying hardness within the blank
- Overforming or underforming of the ends in the preforming operation

- Springback of the work metal
- Temperature of the metal being formed
- Number of passes
- Condition of the equipment
- Operator skill

The most important of the above factors is operator skill; condition of the equipment is also a major factor.

Variations in the mill product (thickness and hardness within a single sheet or plate) are seldom great enough to warrant the extra cost that would be necessary for closer-than-normal control of the work metal. Out-of-roundness and other dimensional variations in the product increase as workpiece diameter increases. Plate thickness above or below the actual crown thickness of the rolls can also cause dimensional variations. For large or small workpieces, much out-of-roundness is caused by variations in preforming the ends, regardless of whether pinch rolls or press brakes are used in preforming.

Springback is overcome by forming to a circle smaller than that required for the finished cylinder. However, the overforming of high-springback material must be done with caution; as the elastic limit is exceeded, metals will take a permanent set and too much overforming can result.

Hot forming may contribute to out-of-roundness because considerable plastic flow can take place when a steel workpiece is heated to 870 °C (1600 °F) or higher. The amount of plastic flow varies as the bending load is applied during forming, and variations are increased by uneven cooling of the work metal. Resulting variations in plate thickness and curvature contribute to out-of-roundness.

Production Example. Without the use of special techniques or secondary operations, there is likely to be considerable variation in out-of-roundness among workpieces that are intended to be identical and are produced under the same conditions. This is demonstrated in the following example.

Example 3: Out-of-Roundness Variations in 44 Cylinders, 432 mm (17 in.) in Diameter.

Cylinders 432 mm (17 in.) in diameter were produced on a pinch-type machine in one pass. Leading edges of the blanks were prebent 15° for a distance of 50 mm (2 in.) with a 102 mm (4 in.) radius. After the cylinders were formed and tack welded, measurements were taken. Out-of-roundness variation in 44 cylinders is plotted in Fig. 10.

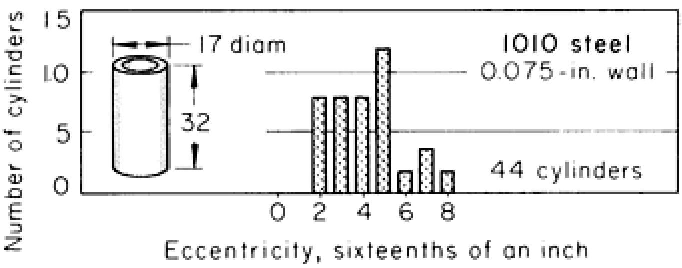

Fig. 10 Out-of-roundness variations in 44 cylinders of the same diameter produced in a pinch-type roll machine. Dimensions given in inches.

The number of passes used to form a given cylinder can have a significant effect on out-of-roundness. In most applications, two or more passes (sometimes as many as 12) will produce cylinders that are more nearly true round than those formed in a single pass.

Methods of Correction. The most effective method for correcting out-of-roundness is to reroll the cylinder carefully before welding. This operation causes a one-third greater load on the machine than the original rolling.

For cylinders having wall thickness no greater than about 9.5 mm ($\frac{3}{8}$ in.), a draw-down type of expander applied after rolling and welding is an effective means of correcting out-of-roundness.

If design permits, beads or flanges can be rolled into the cylinder wall to reinforce it and to help maintain roundness. Oil drums are examples of the effective use of this technique. When dimensional requirements are plus or minus a few thousandths of an inch, stock must be allowed for machine boring the cylinder to the specified diameter.

Three-Roll Forming

Forming Speed

The speed of forming is a critical factor in product quality. Low-carbon steel plate up to 7.9 mm ($\frac{5}{16}$ in.) thick is sometimes rolled at speeds to 18 m/min (60 ft/min). For a speed this high, however, workpiece diameter is necessarily medium to large, because it is impractical to control the machine for rolling small diameters at high speed.

The most commonly used speeds for cold forming (particularly for thick plate) range from 3.7 to 6.1 m/min (12 to 20 ft/min). This range is usually maintained in both cold and hot forming; however, to complete hot forming with a minimum decrease in temperature of the work metal, it is sometimes necessary to increase the speed of the bending roll.

Three-Roll Forming

Roll Deflection

Roll deflection can be calculated by standard formulas, considering the roll as a simple beam supported at both ends. On pyramid rolls, deflection is often minimized by support rollers applied to the lower rolls. These rolls act as backup rolls.

In forming heavy plate, pressures are high and all three rolls are crowned (made larger at the centers than at the ends). Crowning is necessary because the rolls deflect under the bending load; if they were straight, all formed cylinders would bulge somewhat at the center. Because the amount of deflection depends on the bending load, usual practice is to crown the rolls enough to compensate for the average job in the plant. When forming plate thicker than the actual crown deflection, the rolls are shimmed by running strips of thin metal (16, 14, 12, or 10 gage) between the rolls and the inside diameter of the workpiece at the center of the rolls. This shimming compensates for excessive deflection.

When forming metal that is too thin to cause deflection of the rolls, crowning will cause the formed cylinder to be larger in diameter at the ends than in the center. Correction can be made by shimming the ends of the rolls in a manner similar to that described above for shimming the centers.

Three-Roll Forming

Alternative Processes

Three-roll forming is the most practical method of producing large cylinders and truncated cones from heavy plate.

Deep drawing is often the most economical method of producing small cylinders from sheet no thicker than 3.18 mm (0.125 in.). Seamless cylinders or cones can be produced by deep drawing, piercing, and trimming (see the article "Deep Drawing" in this Volume). However, as cylinder size or wall thickness increases, forming by deep drawing becomes impracticable. In some applications, three-roll forming and welding are preferred for producing a hollow shape from stock that is substantially thinner than 3.18 mm (0.125 in.).

Contour Roll Forming. Theoretically, the diameter and length of straight cylinders producible by contour roll forming are almost unlimited. In practice, however, diameter and wall thickness are limited by the size of available equipment. Contour roll forming is rarely used for rolling metal thicker than 6.35 mm (0.250 in.) and is most often used for thicknesses less than 3.18 mm (0.125 in.). Therefore, contour roll forming is impractical for producing large heavy-wall cylinders (see the article "Contour Roll Forming" in this Volume).

Forming two halves (semicircles) between dies in a press and then welding the two half-cylinders is sometimes practical. However, even when presses of sufficient size are available for forming large plate, die cost is likely to be prohibitive. For limited production, a press brake is often used to produce semicircles that can subsequently be joined by welding into cylinders.

Safety

Forming rolls move relatively slowly, but require protection for the operator. The most positive method of protection is to cover the nip point between the feed rolls. One effective guarding device is a solid metal plate covering the nip point between the feed table and the rolls for the full length of the rolls. This plate, with a stud welded to each end, is attached to slotted vertical brackets by nuts and washers so that it is adjustable vertically. The brackets are securely fastened to the feed table.

The height of the feed table can be made adjustable by welding a nut to the lower end of each tubular leg. A long bolt, with a large washer welded to the top of the head, is screwed into the nut to achieve the desired height. A locknut can be used to prevent the bolt from turning because of vibration.

Emergency tripping bars connected to electric cutoff switches or, preferably, to reverse electric switches can be used to stop the rolls. The bars may be at knee level in front of the operator, or directly in front of the bottom feed roll and far enough below the feed point to avoid accidental tripping.

Feeding guides for narrow workpieces can be made of bar stock or angles that are bolted to the feed table. The guides should be slotted for ease of adjustment to various widths of workpieces.

Introduction

CONTOUR ROLL FORMING (also known as roll forming or cold roll forming) is a continuous process for forming metal from sheet, strip, or coiled stock into desired shapes of uniform cross section by feeding the stock through a series of roll stations equipped with contoured rolls (sometimes called roller dies). There are two or more rolls per station. Most contour roll forming is done by working the stock progressively in two or more stations until the finished shape is produced.

Only bending takes place in contour roll forming; the stock thickness is unchanged except for a slight thinning at bend radii. The process is particularly suited to the production of large quantities and long lengths to close tolerances and involves a minimum of handling. Auxiliary operations, such as notching, slotting, punching, embossing, curving, and coiling, can easily be combined with contour roll forming.

Contour roll forming is used in many diverse industries to produce a variety of shapes and products. The process is also used for parts that were previously manufactured by extrusion processes. This use is limited, however, to parts that can be redesigned to have a constant wall thickness. Industries that use roll-formed products include the automotive; building; office furniture; home appliance and home product; medical; railcar; aircraft; and heating, ventilation, and air conditioning (HVAC) industries.

Contour roll forming can be divided into two broad categories: a process using precut lengths of work metal (precut or cut-to-length method), and a process that uses coil stock that is trimmed to size after forming (postcut method).

In precut operations, the work metal is cut to length before entering the forming machine. The precut process usually employs a stacking and feeding system to move blanks into the machine, a contour roll forming machine operating at a fixed speed of about 15 to 75 m/min (50 to 250 ft/min), an exit conveyor, and a stacking system. The precut method is primarily used for low-volume operations and when notching cannot be easily accomplished in a postcut line. Often, the material is run from a coil to a shear or blanking press and then fed mechanically to the contour roll former.

Tooling for the precut method is relatively inexpensive, because cutting requires only a flat shear die or an end notch die. End flare is more pronounced than it is with the postcut method, however, and side roll tooling is required to obtain a good finished shape.

The most efficient, productive, and consistent contour roll forming process is the postcut method. This method requires an uncoiler, a roll-forming machine, a cutoff machine, and a run-out table (see Fig. 1). Postcut contour roll forming can be augmented by various auxiliary operations, such as prenotching, punching, embossing, marking, trimming, welding, curving, coiling, and die forming. These auxiliary operations can be used to eliminate the need for subsequent operations, resulting in production of a finished product.

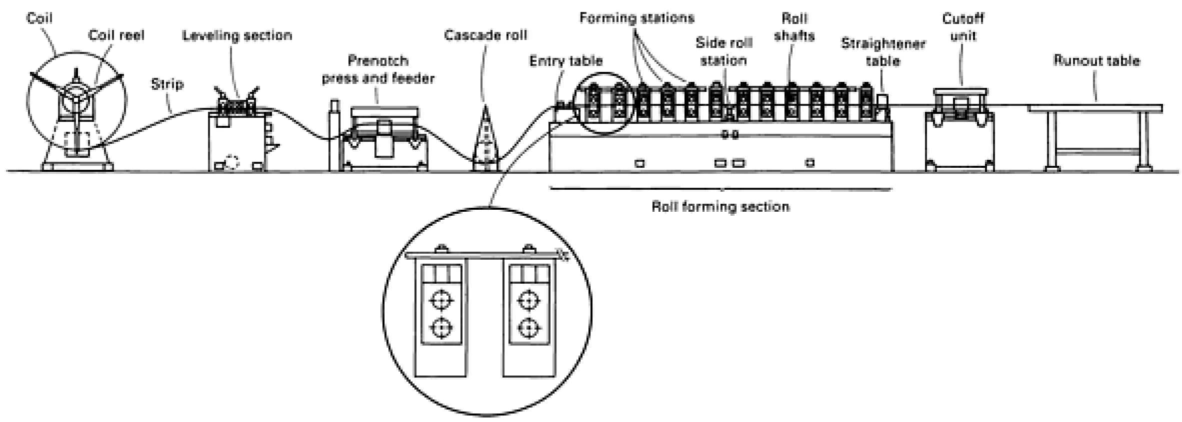

Fig. 1 Setup for contour roll forming of coiled stock (postcut method).

Tooling cost and tooling changeover time are greater for the postcut method than is the case with precut operations, but the increased efficiency of the postcut process balances this limitation.

Contour Roll Forming

Materials

Any material that can withstand bending to the desired radius can be contour roll formed. Thicknesses of 0.13 to 19 mm (0.005 to ¾in.) and material widths of 3.2 to 1830 mm (⅛ to 72 in.) can be used. Length of the formed part is limited only by the length that can be conveniently handled after forming.

In some cases, multiple sections can be formed from a single strip; in other cases, several strips can be simultaneously fed into the machine and combined after forming to produce a composite section. Contour roll forming is almost always performed at room temperature; however, some materials, such as certain titanium alloys, must be formed at elevated temperatures. This is done on specially designed machines.

Influence of Work Metal Composition and Condition. The effect of work metal formability on procedures and results in contour roll forming is generally the same as it is in other forming methods.

Initial yield strength and rate of strain hardening of the work metal affect contour roll forming. One measure for predicting the formability of a work metal is the Olsen cup test (see the article "Formability Testing of Sheet Metals" in this Volume).

Work metals of equal thickness, with differing compositions, initial yield strengths, and rates of strain hardening, could require alterations in the contour rolling operation. Factors to be considered are: power requirements, number of stations, roll material, lubrication, and speed.

In contour roll forming, it is not uncommon to change the work metal but retain the same shape. In such instances, changes may be necessary in equipment and tooling.

When work metal thickness and tooling remain unchanged, it is seldom a problem to change to a more formable work metal. For example, in changing from low-carbon steel to aluminum, major changes would be unlikely. However, it may be necessary to regrind the finish-station rolls to avoid overbending the section; this would usually be determined by a trial run.

When the change is to a metal of higher strength, one or more changes in procedure may be required to achieve desired results. In forming higher-strength metals such as stainless steels, some overforming is usually required to allow for springback. Residual stress in highly cold-worked metal often causes straightening problems, particularly when forming asymmetrical shapes. A common remedy is to add roll stations to decrease the amount of forming in a given station.

In most instances, difficulty in maintaining size and angle tolerance increases as the yield strength of the work metal increases. Optimum rolling speed decreases as yield strength or hardness increases. For example, in changing from carbon steel to stainless, speed is usually decreased 10 to 25%, mainly to prevent rolls from galling when there is an appreciable amount of roll sweep. One method of combating roll galling without greatly reducing speed is to use EP (extreme-pressure) additives in the lubricant; for extreme conditions, pigmented drawing compounds can be added to the lubricant. The main disadvantage of special lubricants is the difficulty and expense involved in removing them from finished workpieces.

Aluminum bronze rolls are often an advantage in shaping the difficult-to-form metals because they resist galling; however, bronze rolls are softer and wear faster than do tool steel rolls. Rolls made from D2 tool steel, hardened and chromium plated, are usually best for contour roll forming of high-strength metals (see the section "Roll Materials" in this article).

Process Variables

In contour roll forming, material is progressively formed as it passes from one station to another. The variable parameters in a roll forming operation include power requirement, forming speed, and type of lubricant. These parameters are determined by width, thickness, and type of material; complexity of the cross section to be formed; coating (if any) on the material; and accuracy required.

The power required by a roll forming machine depends on the torque loss through the drive gearing and the friction between the material and the rolls as the material is being formed. The particular alloy and its thickness must be taken into consideration when looking at the effect of material on power requirements. Generally, contour roll forming machines have motors ranging from 10 to 50 hp on small machines and from 50 to 125 hp on larger machines.

Forming Speed. Speeds used in contour roll forming can range from 0.5 to 245 m/min (1.5 to 800 ft/min), although this speed range represents unusual extremes. Speeds between 25 and 30 m/min (80 and 100 ft/min) are most widely used. One or more of the following can influence optimum forming speed:

- Composition of the work metal
- Yield strength or hardness of the work metal
- Thickness of the work metal

- Severity of the forming operation
- Cutting of finished shapes to length
- Number of roll stations
- Required auxiliary operations
- Use of lubricant (coolant)

Lower speeds in the range indicated above (near 0.5 m/min, or 1½ ft/min) are required for contour roll forming titanium into a relatively complex shape. At the other extreme, a speed of 245 m/min (800 ft/min) has been used in production operations in which conditions were nearly ideal, that is, for contour rolling low yield strength metal, such as aluminum or annealed low-carbon steel, in thicknesses less than 0.91 mm (0.0359 in.), in an operation having mild forming severity and requiring cutoff into relatively long lengths (about 25 m, or 80 ft). To use such high speeds effectively, even though forming is not severe, more stations are usually required, to minimize the amount of forming in any one station. High forming speed usually precludes auxiliary operations, such as punching, notching, or welding, and requires a flood of lubricant at each station.

The first four factors listed above are closely related and influence permissible forming speed. In addition, one or more of the last four factors may dictate a lower speed regardless of the otherwise permissible speed.

Lubricants prevent metal pickup by the rolls (thus improving finish on the work metal and prolonging roll life) and also prevent overheating of rolls and work metal. When rolls become overheated, their life is shortened. If work metal is overheated, it may warp and require straightening. When lubricants can be tolerated, rolling efficiency is usually increased by their use.

Soluble oils (in a 1-to-12 mixture with water) are the most commonly used lubricants. They are usually applied by a pumping action from a self-contained sump in the machine base through a manifold having flexible tubes and nozzles that direct the fluid to the required locations. Gutters are arranged around the top of the machine to catch the fluid and return it to the sump.

Other lubricants have been used satisfactorily for specific applications. In addition to lubricating and cooling, however, a lubricant must be nontoxic, noncorrosive to the metal being formed (as well as to rolls and other machine components), and removable by available shop cleaning facilities. For instance, some silicone-base fluids are excellent lubricants for roll forming, but they are extremely difficult to remove from metal surfaces. This poses problems in obtaining satisfactory plating or adherence of organic coatings or adhesives. Extreme-pressure (EP) lubricants are sometimes used in severe roll forming.

For some applications, no lubricant is permitted (for example, for the forming of painted or otherwise coated metals or for the forming of complex shapes that would entrap lubricants). The result could be a reduction of rolling speed or lower-quality finish, or both. However, in some instances, even though flooding with lubricant cannot be tolerated, other means can be used to supply some lubricant to the rolls. One method is to mount cellulose sponges in constant contact with the rolls, and to keep them wetted with lubricant by hand or by drip applicators.

Despite the fact that lubrication is helpful and often necessary in contour rolling, application and subsequent removal of lubricants are significant cost items. When roll forming steel, however, the selection of hot-rolled, pickled, and oiled grades of work metal has often eliminated the need for an additional lubricant. More information on the role of lubrication and the types of lubricants used in sheet forming is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Contour Roll Forming

Machines

The contour roll forming machine most commonly used has a number of individual units, each of which is actually a dual-spindle roll forming machine, mounted on a suitable baseplate to make a multiple-unit machine. The flexibility of this construction permits the user to purchase enough units for immediate needs only. The purchase of additional length of baseplate on the machine allows the addition of units at any time for future needs. Some of these machines are provided with machined ends on the baseplates, making it possible to couple several machines together, in tandem, to provide additional units as required.

Screws for making vertical adjustments of the top rolls are designed with dials and scales to provide micrometer adjustment and a means of recording the position of the top shaft for each roll pass and each shape being formed. Shaft diameter on most machines ranges from 25 to 102 mm (1 to 4 in.).

Several types of roll forming machines or roll formers are used. They can be classified according to spindle support, station configuration, and drive system.

Spindle Support. Roll forming machines can be classified according to the method by which the spindles are supported in the unit. Generally, two types exist: inboard or overhung spindle machines and outboard machines.

Inboard-type machines (Fig. 2 top) have spindle shafts supported on one end that are 25 to 38 mm (1 to 1½ in.) in diameter and up to 102 mm (4 in.) in length. They are used for forming light-gage moldings, weather strips, and other simple shapes. Material thickness is limited to about 1 mm (0.040 in.), and the top roll shaft is generally geared directly to the bottom shaft. This direct-mesh gearing permits only a small amount of roll redressing (no more than the thickness of the material being formed) on the top and bottom rolls. Tooling changeover is faster on this machine than on the outboard type of machine.

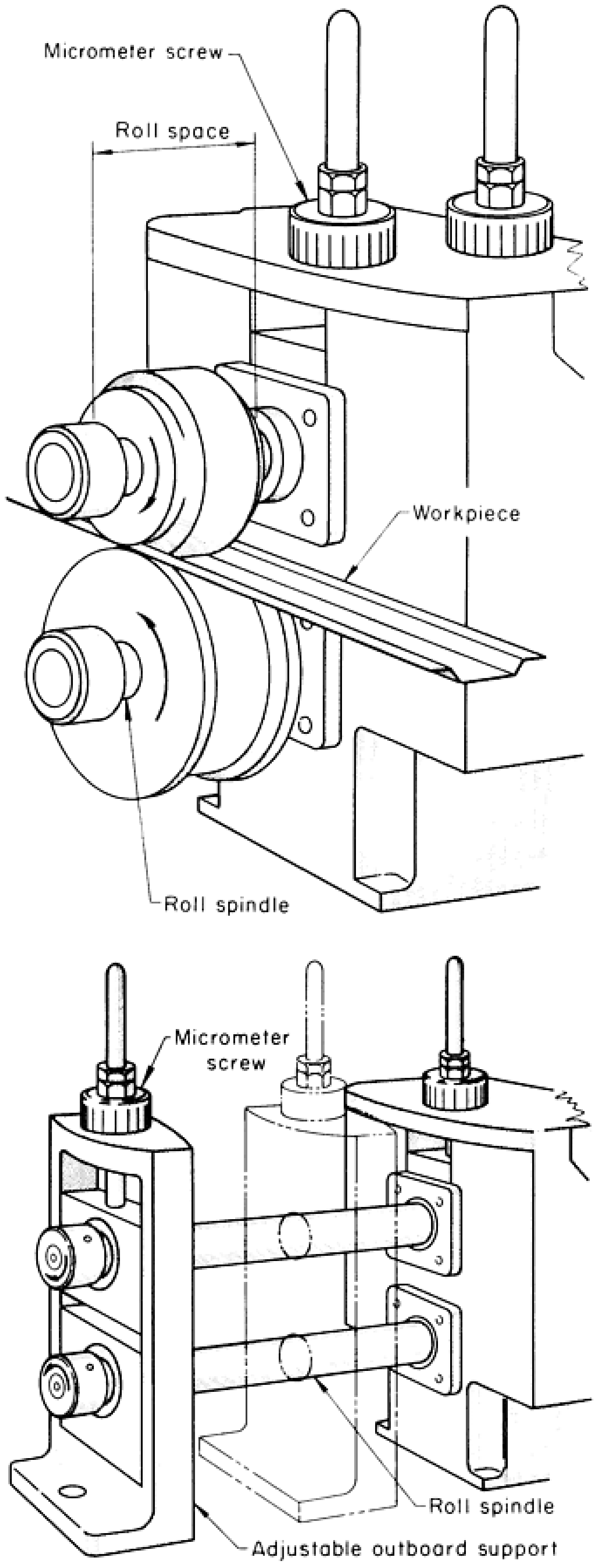

Fig. 2 Two basic machine concepts for contour roll forming. (top) Inboard-type machine. (bottom) Outboard-

type machine.

Outboard machines (Fig. 2 bottom) have housings supporting both ends of the spindle shafts. The outboard housing is generally adjustable along the spindles, permitting shortening of the distance between the supports to accommodate the roll forming of small shapes of heavy-gage material. This adjustment also permits the machine to be used as an inboard type of machine when desired. Outboard machines can be readily designed to accommodate any width of material by making the spindle lengths suit the material width and then mounting the individual units and spindles on a baseplate of suitable width. This type of machine is built with spindle sizes ranging from 38 to 102 mm (1½ to 4 in.) diam and with width capacities up to 1830 mm (72 in.).

Generally, for roll forming material more than 5 mm ($\frac{3}{16}$ in.) thick, machines are constructed so that both top and bottom shafts can be removed by lifting them vertically from the housings after the housing caps have been removed. This permits rolls to be mounted on the shafts away from the machine, an important consideration when heavy rolls are being handled. This type of machine is built in spindle sizes ranging from 50 to 380 mm (2 to 15 in.) in diameter.

Station Configuration. As was previously mentioned, a typical contour roll forming machine consists of several individual forming units mounted on a common baseplate. The manner in which the forming units are mounted determines to a great extent the type of shapes that are formed on the machine.

Single-duty machines are built and designed for a one-purpose profile or for one particular set of roll tooling, and are not normally designed for convenient roll changing. This machine is generally used for long production runs, and its cost is low in comparison to the other styles.

Conventional (standard) machines are more versatile than single-duty machines because the outboard supports are easily removed. This facilitates roll changes, making conventional machines suitable for a variety of production requirements.

To change the tooling, the top and bottom spindle lock nuts are removed and the out-board housing is pulled off the spindles. The tooling can then be removed and replaced with the desired profile.

Side-by-side machines (Fig. 3) are designed for multiple-profiled tooling and provide the flexibility of having more than one set of roll tooling mounted on the spindle shaft at the same time. Generally, this type of machine is limited to two sets of rolls at a given time, but there can be up to three or four sets of rolls when small profiles are being run in production. Changeover from one production profile to another is accomplished by shifting the machine bed to the desired profile. The main advantages of the side-by-side configuration are low initial investments, fast tooling change, and reduced floor space requirement. Roll wear, however, can create problems because one set cannot be reground without regrinding the others at the same time. Adjusting for material variations can also be a problem.

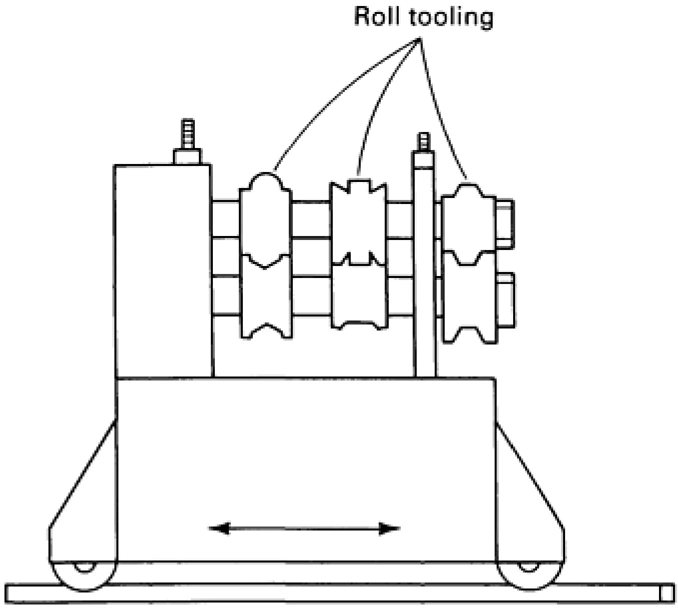

Fig. 3 A side-by-side contour roll forming machine, which allows forming of several different profiles on the same machine.

The double-high machine configuration consists of one set of roll tooling mounted on its own roll shafts and housings at one level on the bed frame, and a second complete set of roll tooling and housings mounted at a different level on the same frame. This particular type of machine is used in the metal building industry for forming building panels up to 1520 mm (60 in.) wide.

The rafted machine configuration resembles the single-duty and conventional configurations, because each configuration has housings and spindle shafts with one particular set of roll tooling mounted on it. However, the rafted configuration has several roll-forming units mounted on rafts or subplates that are removable from the roll-former base. During tool changeover, the individual rafts are removed from the base, and the replacement rafts with the roll-forming units and tooling are installed. On a typical 16-stand roll-forming machine, there are four sets of rafts containing four forming units each.

Double-head machines are designed and constructed with two separate sets of housings and roll shafts mounted so that they face one another. Each housing is mounted on an adjustable plate mechanism to allow the housing to be shifted for a change in overall width while at the same time maintaining the same profile for the edge formation.

This type of machine is very popular in the shelving industry, in which large, flat panels are rolled on a production basis and the panel widths change regularly. A disadvantage of this type of machine is that it does not lend itself to forming the center of the panel. Two of these machines, connected by an automatic transfer mechanism, are used to form the four edges of a shelf; the first machine forms the two long edges, and the second machine forms the two end configurations.

Drive Systems. The five basic methods used to drive roll forming units are chain drive, spur gear drive, worm gear drive, square gearing, and universal drive.

A chain drive consists of a sprocket attached to the individual roll forming unit and connected to the main drive by means of a roller chain. This is accomplished using a continuous roller chain, with one long chain driving each unit, or a shorter chain connected to each individual unit. This drive system is inexpensive and allows flexibility in the construction of the machine.

A spur gear drive consists of a continuous train of spur gears mounted at the rear end of each spindle shaft. Idler gears are positioned between each unit to transfer the drive equally to all the units.

A worm gear drive is very similar to the spur gear drive. However, instead of using the idler gear to transfer the drive to each unit, an individual worm gear box is mounted on the bottom spindle of each unit. The worm gear boxes are coupled in line, which permits the machine designer to spread out the horizontal centers of each roll forming station without being concerned about properly meshing the gear train to the idler gear.

Square gearing also incorporates both spur gears and a worm gear. This type of gearing permits a vertical adjustment of the upper spindle and allows use of a wide range of roll diameters.

Universal drive eliminates the need for any spur gearing or roller chain and sprocket drives. It consists of a series of worm-driven gear boxes with top and bottom outputs that transfer the power source to the individual shafts through a double-jointed universal coupling. On certain applications, only the bottom spindle is driven. This drive system is generally used with rafted-style machines to permit quick tool changeover. Simplicity of design and minimal maintenance are two important advantages of this drive system.

Machine Selection. Several factors must be taken into consideration when selecting a machine to be used in a roll forming operation. These include load capacity, section size and shape, and roll changeover.

Load Capacity. The type and thickness of the material being formed determine to a large extent the load capacity that a given forming machine is required to produce. If material type and thickness change, it is best to select a machine that can provide the additional capacity.

In forming material up to 1.5 mm (0.060 in.) thick, a machine with 38 mm (1½ in.) diam spindles should be used as long as the part is not too wide. A machine with 50 mm (2 in.) diam spindles can be used for forming material thicknesses up to 2 mm (0.080 in.). As the material thickness increases, the diameter of the spindles must also increase to provide strength to create the pressure required to do the forming. Center distances must be increased as the size of the part shape and the movement of material between forming stations increases. These distances are usually determined by the machine builder.

Section Size and Shape. Wide roll-formed sections require wide roll spaces. To support the pressure of the rolls, the spindle shafts must be large enough in diameter to prevent shaft deflection during forming. The distance between the centerline of the bottom spindle and the machine bed determines the maximum roll diameter and hence the maximum section depth.

The more complex the shape of the section being formed, the greater the number of passes (pairs of rolls) required to roll form the section. It is best to select a machine that provides the flexibility of adding or subtracting pairs of rolls in accordance with the part design.

Roll changeover can be costly and time consuming, because material variations may require different roll pressure settings. When several part configurations must be run on one machine, it is best to select a machine that permits the tooling to be changed quickly. If only two or three profiles are run, the side-by-side and the double-high machines are possible selections; the changeover can be performed quickly without losing valuable production time. Another machine to consider is the rafted machine.

Contour Roll Forming

Auxiliary Equipment

In addition to the machines that do the roll forming, several other pieces of equipment are usually required for production operation. Stock for roll forming is usually received in coils; thus, an electric hoist on an overhead track is needed to lift coils from skids and transfer them to a cradle or reel (another piece of auxiliary equipment). Also equipment for welding the end of an expended coil to the lead of the next one, an entrance guide, intermediate guides, a straightening device, and cutoff equipment may be needed.

Stock reels should be equipped with an expandable arbor to fit the inside diameter of the coil, and with a friction drag. Stock reels incorporating these features are commercially available in a wide range of coil capacities (see the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume).

The friction drag is necessary to prevent the coil stock from overrunning onto the floor in the event of a sudden stoppage of the roll forming equipment. In the simplest type of motor-driven stock reel, a dancer arm and roll ride the stock in a loop-detector arrangement, which starts and stops the motor as required, supplying stock at the average rate used by the roll former. Stock speed is matched approximately by adjusting a variable-pitch sheave to prevent too-frequent stopping and starting of the alternating current drive motor. This type of control on the stock reel provides acceptable results for most applications. More elaborate controls can be used, such as a direct current motor drive with feedback control to match stock speed with machine speed. Elaborate controls are expensive and should not be considered unless they are needed to meet special workpiece requirements.

Stock reels are available with a swivel base and two arbors. A coil may be positioned on one arbor while the first coil is being used, thus reducing change time. This arrangement is advantageous when coils are relatively small and production requirements are high, because time consumed in changing coils can become a substantial portion of the total production time.

Welding Equipment. Thread-up time can be eliminated by manually welding the end of each expended coil to the leading end of the next one. For stock thickness of 1.6 mm ($\frac{1}{16}$ in.) or more, a semiautomatic welder can be placed in the line. Regardless of the welding method used, any appreciable flash must be removed before the welded joint reaches the first roll station. Provision can be made to remove flash by installing a grinder similar to a band saw blade grinder. More information on combined roll forming and welding operations is available in the section "Tube and Pipe Rolling" in this article.

Entrance guides positioned in front of the first forming station ensure correct alignment of the work metal entering the starting rolls. This is particularly desirable when the part being formed is asymmetrical in the first station because the stock could climb or shift to one side without guides. The simplest form of entrance guide consists of a flat plate with a channel milled to the proper width and depth to accept the strip at its maximum tolerance, plus a simple, removable lid to hold the stock in place. The mounting for this guide should permit adjustment vertically and laterally.

When wide variations in stock width are encountered, a self-centering, parallel-rule entrance guide is preferred. This guide is constructed like a navigator's parallel rule, with the crossbars pivoted and mounted at their centers with a spring, causing the side bars or rules to close on the stock under spring load.

Stock drags are occasionally used to place a slight tension on the stock and to cause it to feed more uniformly through the first few stations. The simplest form consists of two pieces of hardwood. The stock is clamped between the wooden members, which butt against the entrance guide, thus providing enough friction to keep the stock under tension. The amount of tension can be regulated by the clamping force on the wooden members.

Guides between roll stations facilitate entrance of the partly formed stock into the next station. In theory, if rolls are properly designed, guides between stations are unnecessary, because each set of rolls should accept the cross section from the preceding set of rolls. In practice, however, because of such factors as cost, lead time, and availability of space or equipment, the number of roll stations is often fewer than the ideal number; this necessitates more forming in each station than is consistent with the best practice. Therefore, the use of guides between stations helps to compensate for this lack of additional stations, and to minimize springback.

Generally, guides between stations are only required to contact the critical points of the work metal, not the entire contour. Regardless of their shape, guides should be designed with removable top portions to facilitate threading.

Various metals may be used for guides, depending on the end-use of the workpiece. For the areas contacting the moving workpiece, hardened steel (usually, case-hardened low-carbon steel) is preferred from the standpoint of guide life. However, when workpiece finish is critical and hardened steel guides are likely to scratch the surface, bronze or aluminum guides are used. For some work, hard chromium plating of guides minimizes damage to workpiece surfaces and still provides acceptable guide life. Guides can be replated when the plating becomes worn.

Straightening Equipment. Usually it is necessary to straighten the workpiece after it leaves the final roll station. This is done by standard straightening guides attached to the machine beyond the last set of rolls, or by special devices designed for individual applications.

Straightening guides are usually adjustable vertically and laterally; the most versatile types can be swiveled in either elevation or azimuth and can also be rotated about an axis. Most straighteners employed for contour roll forming are either of the roll type or the shoe type.

A roll straightener consists of multiple rolls (individually adjustable) arranged to contact the stock in selected areas. A shoe straightener consists of one or more shoes, usually made of bronze, properly fitted to the contour and adjustable in at least one direction that will crimp the stock to correct for sweep or twist.

There are also applications in which a sweep (curve) is deliberate and desired. A sweep guide is similar to a straightening guide, and a straightening guide often can be adjusted to give the required sweep in a constant radius. For more detailed information on straightening equipment, see the articles "Straightening of Bars, Shapes, and Long Parts" and "Straightening of Tubing" in this Volume.

Cutoff Equipment. Because most contour rolled products are made from coil stock, a system of cutting the formed shapes to length must be provided. There are several types and sizes of flying-shear cutoff machines.

The sliding-die cutoff machine is most commonly used. The action of this machine is similar to that of a punch press, although construction of the machine differs. The flywheel and clutch are placed below the bed, with the ram posts passing through the bed. Gibs are provided in the bed and ram to accept a gibbed die and a punch holder that permits linear movement of the die to match work metal speed during the cutoff cycle.

Tooling

Tooling used in roll forming includes the forming rolls and the dies for punching and cutting off the material. Tube mills require some additional tooling to weld, size, and straighten the tubes as they are produced on the machine; the needed tooling is discussed in the section "Tube and Pipe Rolling" in this article.

Forming Rolls

The rolls are the tools that do the actual forming of the material as it moves through the roll forming machine. Several factors must be considered when designing the rolls to form a particular part. These include the number of required passes, the material width, the "flower" design, the roll design parameters, and the roll material. Flower is the name given to the progressive section contours, starting with the flat material and ending with the desired section profile.

Number of Passes. The roll forming of material into a desired final shape is a progressive operation in which small amounts of forming are performed at each pass or pair of rolls. The amount of change of shape or contour in each pass must be restricted so that the required bends can be formed without elongating the material. Too few passes can cause distortion and loss of tolerances; too many passes increase the initial tooling cost.

Generally, the number of passes depends upon the properties of the material and the complexity of the shape. Other areas to consider are part width, horizontal center distance between the individual stations, and part tolerances. The number of passes must be increased as the tolerances of the shape become tighter.

Material. Material thickness, hardness, and composition all affect the number of passes required to achieve a desired shape. As the thickness of the metal increases, the number of passes required to form the material increases. Steel that has a high yield strength should be overformed approximately 2° and then brought back to finish size on the final pass. Overforming compensates for springback that is encountered when materials having high yield strengths are formed. Material that is coated or that has a polished surface generally requires more passes than does uncoated material. Precut material may also require more passes so that the rolls can pick up the leading end of each section.

Shape complexity is determined by the number of bends and the total number of degrees that the formed part must be bent. It is also influenced by the symmetry of the part design. The forming angle method is a rule of thumb which roll designers use to determine the approximate number of passes.

On simple shapes, a forming angle of 1 to 2° is recommended. This forming angle is based on the amount of bending performed for every inch of distance between station centers (horizontal center distances). The minimum forming length for a single bend is determined by multiplying the height of the desired section by the cotangent of the forming angle. This length is then divided by the distance between station centers on a given machine, to determine the approximate number of passes. For multiple bends, the number of passes must be determined for each bend and then, after the formation of bends have been combined where possible, the approximate number of passes can be determined.

Horizontal Center Distance. If the machine on which the section must be run is predetermined, the specifications and limitations of the machine will have a bearing on the number of passes required. The distance between stations (horizontal center distance) may dictate more stations if that distance is too short. The total distance from flat material to finished section is more critical than the number of stations, because undue stresses are created by forming too fast.

Strip Width. The width of strip required to produce a given shape is determined by making a large-scale layout, dividing it into its component straight and curved segments, and totaling the developed width along the neutral axis. The outside profile and the neutral axis of each curved segment can usually be treated as circular arcs. Also, for bends having an inside radius of up to about twice the stock thickness in low-carbon steel, the neutral plane or axis is located approximately one-third of the distance from the inside surface to the outside surface at the bend.

Developed width, sometimes termed bend allowance, is the amount of material required to form a curved section of a particular shape properly. The two methods for calculating developed width described in this section use general equations and can be employed for all shapes. The equations given are applicable when low-carbon steel is formed; for less-formable materials, values should be increased.

Method One. Using this method, developed width w is calculated as follows:

$$w = r\frac{\alpha}{57.3} \quad \textbf{(Eq 1)}$$

If the inside bend radius is less than two times the material thickness, then:

$$r = r_i + 0.4t \quad \textbf{(Eq 2)}$$

where w is in millimeters (inches), r is the bend radius in millimeters (inches), α is the angle (in degrees) through which the material is bent, r_i is the inside bend radius in millimeters (inches), and t is the metal thickness in millimeters (inches).

If the inside bend radius is greater than $2t$, then:

$$r = r_i + 0.5t \quad \textbf{(Eq 3)}$$

If the material is bent through a 90° zero radius or a 180° zero radius, w is $\frac{1}{3}t$ or $\frac{2}{3}t$, respectively.

Method Two. Another method used to determine developed width for a roll-formed part is with the empirical equation:

$$w = (t \times p + r_i)0.01745\alpha \quad \textbf{(Eq 4)}$$

where w is in millimeters (inches), p is a bend factor based on the ratio of inside bend radius to material thickness expressed as a percentage, and the other quantities are as previously described.

The bend factor p is obtained by first dividing the inside bend radius by the material thickness. After this ratio is obtained p may be determined using either the nomograph shown in Fig. 4 or the following calculations: For a ratio less than one:

$$p = r_A \times 0.04 + 0.3 \quad \textbf{(Eq 5)}$$

For a ratio greater than one or equal to one:

$$p = (r_A - 1.0)0.6 + 0.34 \qquad \textbf{(Eq 6)}$$

where r_A is the inside bend radius divided by the material thickness, r_i/t. If p is calculated to be greater than 45%, the value is 0.45.

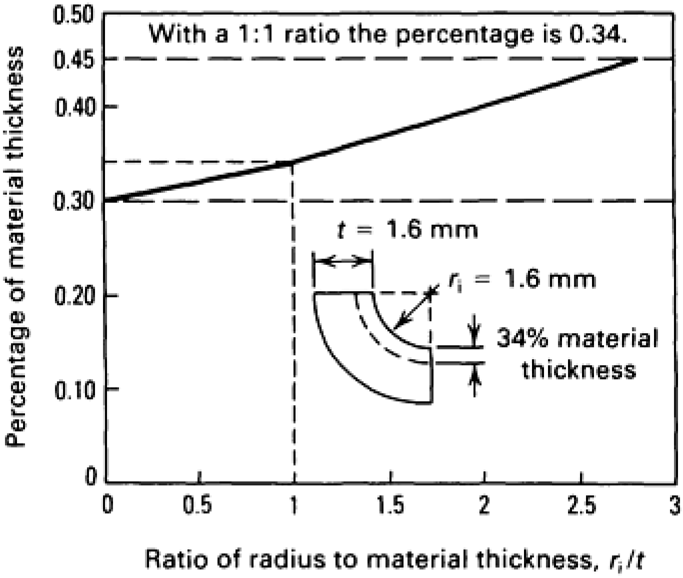

Fig. 4 Nomograph used to determine percentage of material thickness required when calculating bend allowance.

Flower Design. The development of the flower--the station-by-station overlay of progressive section contours, starting with the flat strip width before forming and ending with the final desired section profile--is the first step in the design of tooling for contour roll forming. The intermediate profiles between flat material and finished profile are graduated at a rate that enables the section to be completed in the fewest number of stations of passes without compromising general roll forming parameters. The flower graphically shows the number of passes required to roll form the given profile (Fig. 5).

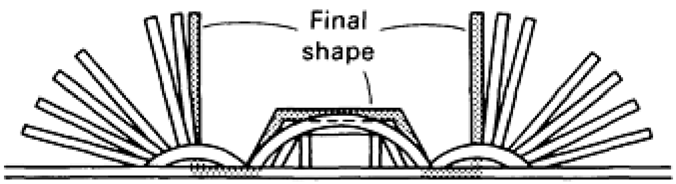

Fig. 5 Development of a flower for design of roll forming rolls. See text for details.

The two prime considerations in designing the flower are: a smooth flow of material from first to last pass and maximum control over fixed dimensions while roll forming. Other factors to be considered include forming position (a section is usually formed in an upward position), vertical reference line with respect to the number and severity of bends, and drive line (the optimal placement in the rolls for equal top and bottom surface speeds).

Roll Design Parameters. After the flower is completed to the satisfaction of the designer, rolls may be drawn around each overlay. For small sections, the roll material should contact the section material as much as possible. It is possible, however, to go too far and overdesign rolls, thereby creating too much roll contact, which can be detrimental. Each roll pass must be examined not only by itself but as part of the total job to determine where to make contact with the section material, where to exaggerate pressures or dimensions, where to clear out rolls so that material flows without restriction from one pass to another, and how to accept the section material from the previous configuration.

Rolls are usually made progressively larger in diameter from one pass to the next to permit the surface speed of each succeeding station to increase. The diameter increase is called step-up. The speed differential between passes creates a tension in the section material and eliminates the possibility of an overfeed between passes. Overfeed is created by an excessive amount of work being done in a single pass, which stretches the section material. Normal step-up is approximately 0.8 mm ($\frac{1}{32}$ in.) per pass on diameter, but it varies depending upon the gage of the section and the particular amount of forming being done in those passes.

Rolls may be solid or split (segmented) depending on the complexity of the section. Simple rolls are usually of a one-piece design, but as complexity of the workpiece increases, the use of split rolls should be considered. There are seldom any marked disadvantages in using split rolls, and one or more of the following advantages can often be gained:

- Turning, grinding, or other machining operations are usually easier to perform on the separate sections of split rolls than on one-piece rolls having a complex contour
- Sections of split rolls are less susceptible to cracking in heat treatment than are single complex rolls
- Handling problems are simplified, particularly for large roll sections
- Because sections of rolls subject to excessive wear or breakage can be replaced separately, split rolls can be more economical than one-piece rolls
- Split rolls permit the use of different roll materials, as needed, for areas of high and low wear
- Split rolls allow flexibility in making different widths of the same section by the use of spacers or additional roll sections
- Split rolls allow for minor adjustments that cannot be made with one-piece rolls

Figure 6 illustrates some of the above advantages of split rolls. In Fig. 6(a), the upper roll is composed of five separate sections. In addition to being easier to manufacture, split rolls of this type can accommodate minor adjustments (by shims or similar means) after the initial trial and before the first production run.

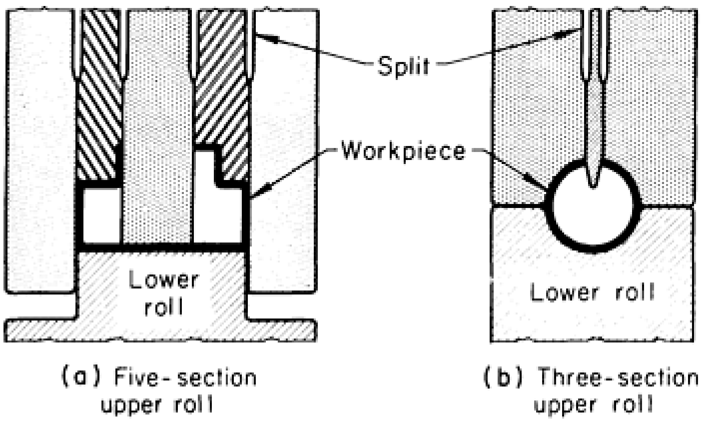

Fig. 6 Two types of split rolls used in contour roll forming. (a) Upper roll constructed in five sections allows for minor adjustments. (b) Three-section upper roll allows replacement of center section.

Figure 6(b) shows an upper forming roll made up of three sections--a narrow center section flanked by two wider sections. In use, the center section is subjected to a higher rate of wear than are the adjacent flanking sections. The center section of this forming roll can be replaced without changing the flanking sections, or can be made from a more wear-resistant metal.

Roll Materials. The materials that are most commonly used for contour rolls are:

- Low-carbon steel, turned and polished but not hardened
- Gray iron (such as class 30), turned and polished but not hardened
- Low-alloy tool steel (such as O1 or L6), hardened to 60 to 63 HRC and sometimes chromium plated
- High-carbon high-chromium tool steel (such as D2), hardened to 60 to 63 HRC and sometimes chromium plated
- Bronze (usually aluminum bronze)

The quantity of parts to be rolled is usually the major factor in choosing the most appropriate roll material, although other factors, as noted below, also affect selection to some extent, and one or more of them may become definitive in particular applications.

Short-Run Production. For rolling small quantities of a specific shape, or when repeat orders are not expected, rolls made of either low-carbon steel or gray iron are commonly used. When the work metal is soft and corner radii are generous, low-carbon steel or gray iron rolls can be used for medium- or high-production runs because rolls are easily made from these materials and are not expensive.

Medium-Run Production. As the size of the production run increases, rolls made of hardened, ground, polished tool steel, such as O1 or L6, are usually more economical and are widely used. Not only are these materials relatively inexpensive, they machine more easily than more highly alloyed tool steels and they can be heat treated by simple procedures. Rolls made of these steels can be plated with up to 25 μm (1 mil) of chromium to reduce galling or scratching of the work metal, or to extend tool life between grinds by decreasing roll wear or minimizing corrosion or pitting.

Long-Run Production. For long production runs (>5 million ft) or continuous high production, it is usually more economical to make rolls from one of the high-carbon high-chromium tool steels, such as D2. These highly alloyed grades cost nearly twice as much as O1, are more difficult to machine and grind, and require more complex heat treatments, but because of their longer life between regrinds, are usually more economical for long runs.

Factors Other Than Quantity. Three other conditions, under any one of which rolls made of steel such as D2 are often preferred (quantity becoming a secondary consideration), are: excessively hard work metal (low formability), excessively sharp radii or other severe forming conditions, and work metal surfaces that are abrasive (such as unpickled hot-rolled steel) and cause excessive roll wear. Rolls made of highly alloyed tool steel may also be plated with chromium for the reasons mentioned in the section "Medium-Run Production" above.

Special Finish Requirements. In many applications, such as the rolling of light-gage stainless steel, aluminum alloys, or coated stock, preservation of surface finish is of primary concern. With these work metals, softer rolls are used to avoid damaging the work metal surface, even though there may be a substantial reduction in roll life.

As workpiece surface finish requirements become more rigorous, softer rolls are required. Rolls made of bronze are often used. In some applications, the plating of hardened steel rolls is sufficient to prevent the marring of work metal surface finish.

Contour Roll Forming

Tube and Pipe Rolling

Welded-seam pipe and tube are contour roll formed by three methods: edge forming, center forming, and true-radius forming. Edge and center forming (Fig. 7a and b) require complete sets of rolls for each size of tube, because the stations that precede welding have the final radii in the roll contours. In true-radius forming (Fig. 7c), the breakdown rolls can be used for a range of sizes, which reduces tooling cost and setup time.

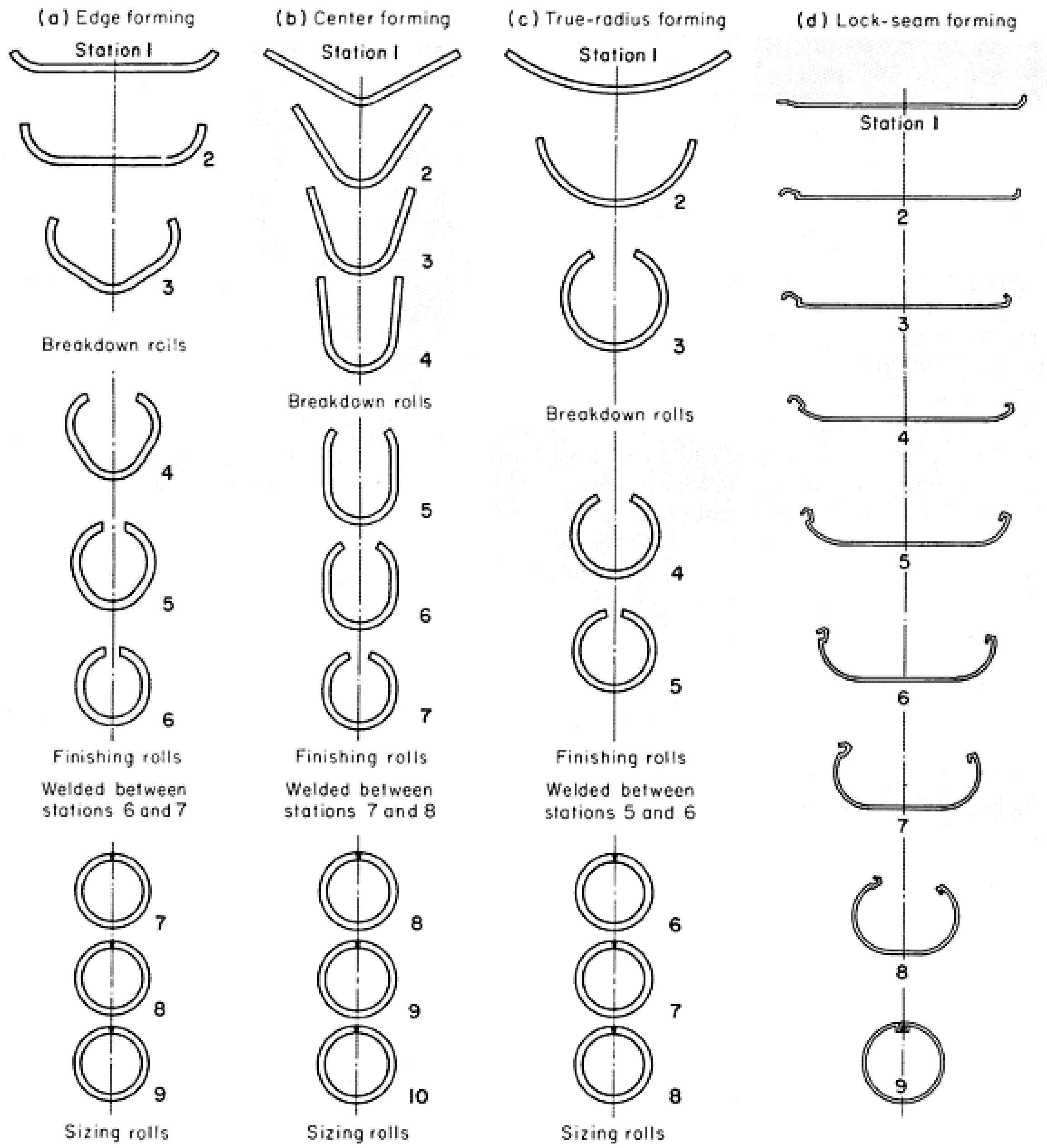

Fig. 7 Typical sequences for forming round pipes and tubes. (a to c) With a butt-welded longitudinal seam. (d) With a lock-seam joint.

For all three methods of producing welded tubing, the forming rolls are termed as breakdown and finishing rolls. Breakdown is usually accomplished in the first three or four roll stations, as indicated in Fig. 7.

Optimum distance between the edges of the stock in the final finishing station (stations 6, 7, and 5 in Fig. 7a, b, and c, respectively) is affected by the method of welding and tube size. For resistance welding, however, the following relations of tube size to distance are typical:

Tube diameter	Distance

mm	in.	mm	in.
9.5	$\frac{3}{8}$	1.57	**0.062**
25	1	3.15	**0.124**
51	2	4.37	**0.172**
102	**4**	**6.35**	**0.250**

Machine size is determined by material thickness and tubing size. Large pipe sometimes requires a machine having spindles 305 to 355 mm (12 to 14 in.) in diameter.

Speed of production is controlled by thickness and type of work metal. Aluminum can be formed and welded as fast as 75 m/min (250 ft/min), whereas titanium tubing is produced at a rate of only 455 mm/min (18 in./min). The welding operation is often the main limitation on the speed at which tubing can be produced by roll forming.

Soluble oils are the most practical lubricant for forming tube, and should be used to prevent galling. A mixture of 25 to 40 parts water to 1 part oil is commonly used.

Long roll life can be expected in producing round tubing. Three to four million feet between regrinds is considered normal.

Lock-seam tube (Fig. 7d) has two edges folded over to form a lock. Production of lock-seam tubing is restricted to relatively thin material (usually less than 0.91 mm, or 0.0359 in.) because heavier stock is too difficult to lock. The lock-seam method is used extensively for thicknesses that are impractical for welding because they are too thin. Maximum thickness is also restricted by tube diameter. As a general rule, stock thickness should not be greater than 3% of the tube diameter. For instance, 0.76 mm (0.030 in.) is about the maximum thickness of strip that should be used to produce 25 mm (1 in.) OD tubing.

Minimum width of the lock should be five times the material thickness. The lock-seam method is also applicable to square tubing.

The various stages of lock-seam forming are shown in Fig. 7(d). Idlers are used between stations 4 to 8. Between stations 8 and 9, a lock housing unit that is equivalent to two stations is used. Both side rolls and vertical rolls are used in the locking stand, the main purpose of which is to lock the two edges of work metal in the groove. A mandrel is placed inside the tube to help form the small lock. This mandrel is in the locking stand and extends beyond station 9. In the last station, two small rolls ride opposite each other on the mandrel, pushing the work metal to form a tight lock between the top and bottom rolls in station 9. It is necessary to have close control over the stock thickness, because a 0.025 mm (0.001 in.) difference in thickness will result in a difference of about 0.13 mm (0.005 in.) in the outside diameter of the tube.

Roll Design for Tube Rolling

A number of designs and methods are used for forming strip into a tubular shape suitable for welding, with many factors involved in choosing the proper roll design for producing a particular tube.

Figure 8 illustrates one of the most commonly used designs for rolls used for forming the tube before welding. The rolls are designed with a single forming radius in each roll pass. This radius decreases progressively in each roll pass until the final pass. The radius of the final roll pass is slightly larger than the finished tube size to permit the insertion of a thin fin in the top roll to act as a guide for the two edges of the material. Generally, for the last two or three roll passes, there are fin rolls in the top rolls to guide the two edges of the strip, prevent twisting of the tube and ensure accurate positioning of the seam entering the welder. Idler rolls mounted on vertical spindles between the driven-roll passes are positioned to prevent excessive rubbing and scuffing of the side of the tube as it passes through the succeeding driven-roll pass. The number of driven-roll passes can vary, increasing as the tube diameter increases, but five driven passes are considered a minimum.

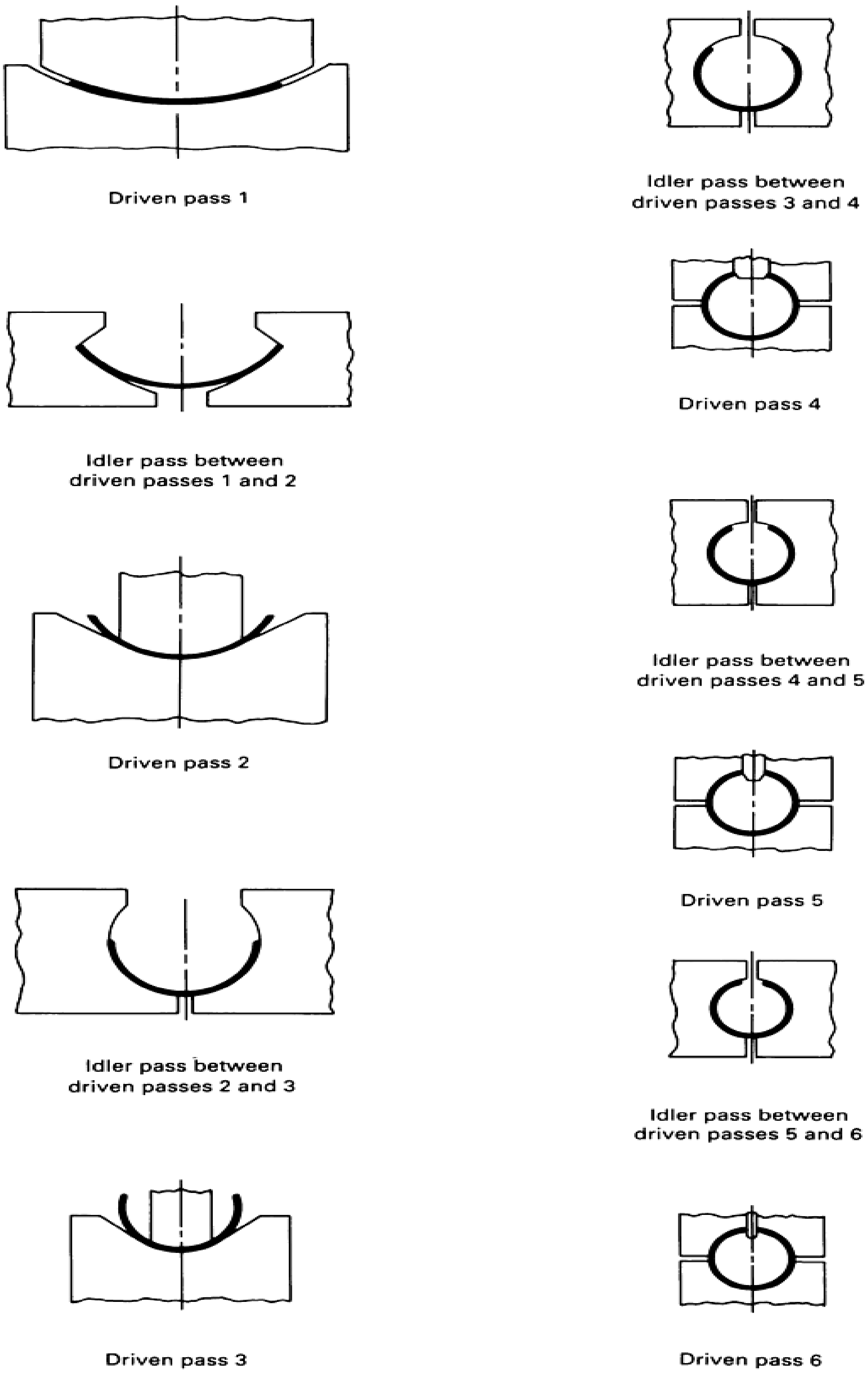

Fig. 8 Typical sequences for roll forming tubing from strip material.

For forming tube with either a very thin wall or a very thick wall (<3% of the outside diameter of the tube, or >10% of the outside diameter of the tube), a modification of the design shown in Fig. 8 is used. This modification is obtained by forming the portion of the strip adjacent to the edges to the finished radius of the tube in the first forming pass, instead of depending on the fin passes to finish form at the edges. This finished form at the edges of the strip helps to prevent buckles and waviness at the edges of the strip as it passes through the forming rolls when very thin material is being formed, and it helps to avoid the necessity of extreme pressures at the fin rolls when extra heavy-gage material is being formed. Figure 9 illustrates the first four sets of rolls used in this method of tube forming. The remaining rolls are similar to those shown in Fig. 8.

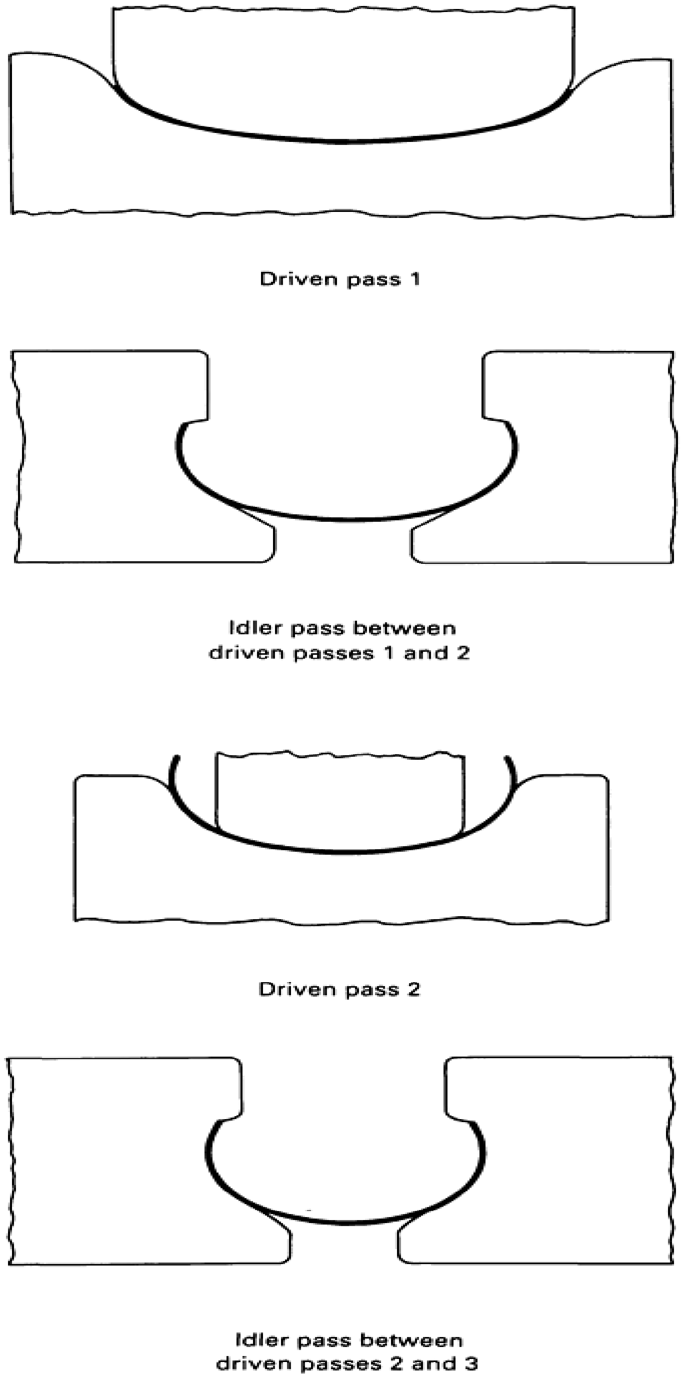

Fig. 9 First group of rolls for forming tubing from thin strip material from the edges inward.

In forming material of high tensile strength when springback of the metal is a factor, a third method of forming is sometimes used. In this design a part of the strip is formed to the finished tube radius in each roll pass, progressing from the two edges toward the middle until the fin passes are reached. The forming radius on the rolls is less than the finished radius of the tube to compensate for the springback of the material.

Welding

Seam welding of pipe and tubing is generally performed using either low-frequency rotary-electrode welding or high-frequency welding; laser welding is beginning to be used (see the section "Laser Welding" below). High-frequency resistance welding and high-frequency induction welding for tube diameters less than 25 mm (1 in.) Fig. 10 has become more predominant in recent years in the production of welded tubing (see the article "High Frequency Welding" in *Welding, Brazing, and Soldering*, Volume 6 of the *ASM Handbook*). With rotary-electrode welding, the maximum wall thickness that is economically feasible is 4.5 mm (0.180 in.). Using high-frequency welders, wall thicknesses as thick as 19 mm (0.75 in.) and as thin as 0.13 mm (0.005 in.) are obtainable. After the welding operation, the piece is usually sized and then straightened before being cut to length.

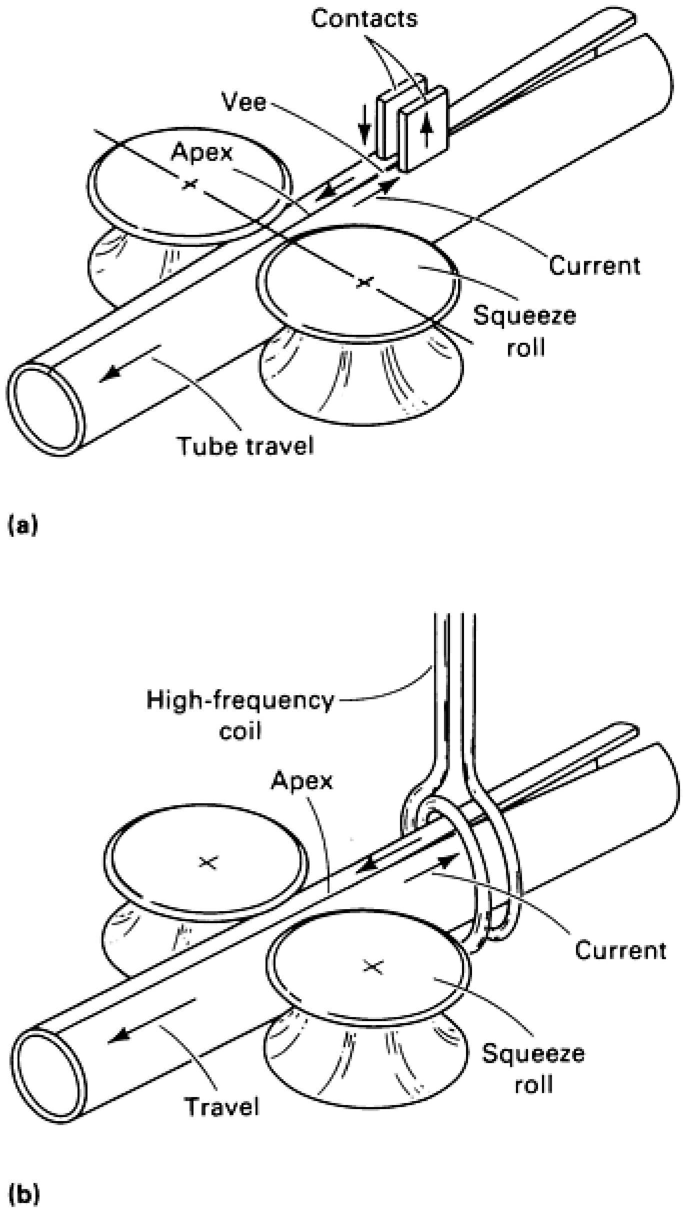

Fig. 10 Two methods of high-frequency welding of longitudinal seams in tubing. (a) Sliding contacts introduce current to the tube edges. (b) Multiturn induction coil induces current to the tube edges.

Laser Welding. Powerful carbon dioxide (CO_2) lasers also have been used to make longitudinal welds in contour roll formed stainless steel tube. In this process autogenous (no filler metal) welds were made in type 304 and type 430 stainless steel tubes using a 5 kW CO_2 laser at a speed of 5 m/min (16.5 ft/min). Tubes had a wall thickness of 1.5 mm (0.060 in.) and a diameter of 48.5 mm (1.91 in.). Laser welding resulted in joints that were tougher and more ductile than joints made by other welding processes.

Sizing and Straightening. Tube and pipe are welded to an outside diameter slightly larger than the finished diameter. The sizing rolls can then produce round, accurately dimensioned, straight, finished tube. A typical set of sizing and straightening rolls consists of three driven-roll passes, vertically mounted idler rolls between each driven pass, and finally a set of idle cluster rolls that are adjustable both vertically and horizontally for final straightening of the tube. The roll radius for each driven roll successively decreases to size the tube down to its proper diameter.

Reshaping of Round Tubing

Several cross sections that can be feasibly produced by reshaping round tubing are shown in Fig. 11. Reshaping can be done either continuously in sequence with the production of round tubing or in a separate operation.

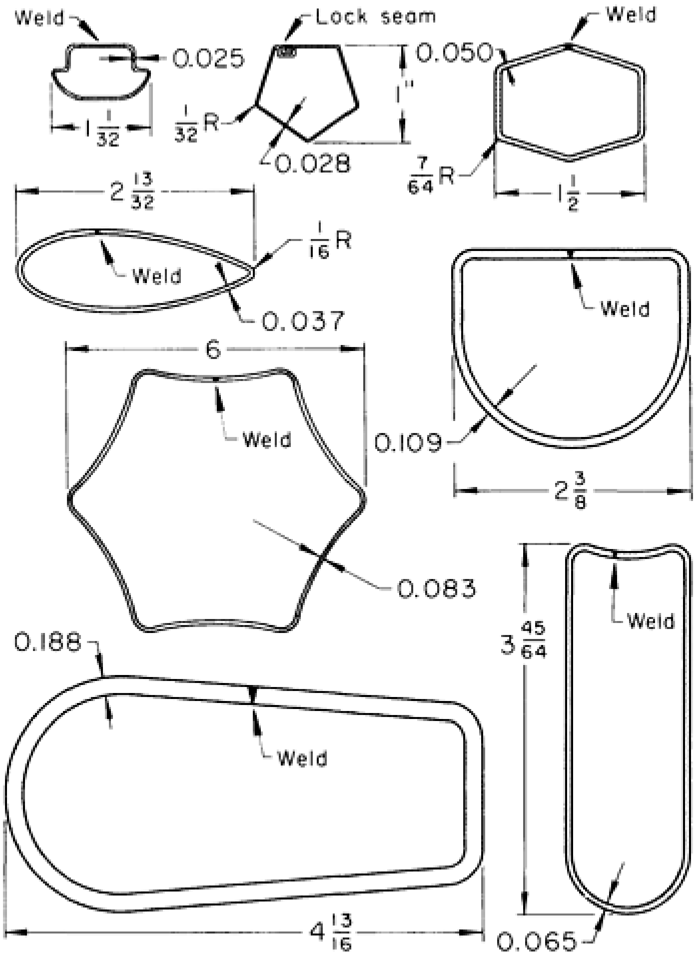

Fig. 11 Cross sections showing typical contours that can be produced by reshaping welded or lock-seam round tubing. In addition to these contours, square, triangular, and rectangular shapes can be formed from round tubing in one or more roll stations. Dimensions given in inches.

On square and rectangular tubing, the flatness of the sides will vary with work metal thickness and hardness. On light gages and in the harder tempers, springback results in a crown effect on the sides. This condition can be corrected by overforming in the final station. In forming rectangles, the longer sides may become convex, and the short sides may become concave. Flatness can be controlled to a minor extent by roll adjustments in the finish station, but a major correction must be made by using concave or convex contours in final Turk's-head rolls.

Rolling speeds used in reshaping tubing of light-gage metal are often equal to those used in producing the basic round tube. For reshaping heavier-gage tubing (for example, over 0.89 mm, or 0.035 in.), speed should be reduced by as much as 25% of that used for forming the round tube, depending mainly on the capacity of the equipment.

Tooling requirements for reshaping round tubing vary with the gage, size, and complexity of the final shape. It is often possible to form simple squares from thin-gage round tubing in one roll station and one Turk's-head station. However, as stock thickness or complexity of shape, or both, increase, more stations are required.

Contour Roll Forming

Tolerances

Cross-sectional tolerances on part dimensions are a result of variations in material width and thickness, physical properties of the material, quality of the tooling, conditions of the machine, and operator skill. Dimensional cross-sectional tolerances of ±0.25 to ±0.78 mm (0.010 to 0.030 in.) and angular tolerances of ±1° are common. Tolerances are slightly greater when wide building panels and deep sections are being formed. If a closer tolerance is required, material with a controlled-thickness tolerance of ±0.05 mm (0.002 in.) should be used.

Length tolerances are dependent on material thickness, part length, line speed, equipment quality and condition, and type of measuring and cutoff system used. For thin material (0.38 to 0.64 mm, or 0.015 to 0.025 in. thick), tolerances of ±0.51 to ±2.36 mm (0.020 to 0.093 in.) are obtainable. For material more than 0.64 mm (0.025 in.) thick, tolerances of ±0.38 to ±1.52 mm (0.015 to 0.060 in.) are obtainable. The minimum tolerances are based on part lengths up to 915 mm (36 in.), and the maximum tolerances are based on lengths up to 3.66 m (12 ft). Tolerances would generally be greater on parts longer than those specified.

In roll forming, it is generally advisable to order the material to be formed with somewhat tighter than commercial quality tolerances. If this is done, a great many dimensional problems can be eliminated. Failure to consider material quality results in needless problems and frustrations.

Straightness

In addition to cross-sectional, angular, and length tolerances, another tolerance to consider is the straightness of the material and the formed section. Some of the parameters that determine straightness include camber, curve or sweep, bow, and twist. The terms camber, curve, and bow are often used synonymously when describing straightness. The horizontal and vertical planes of the formed part are determined by the position in which the part is formed.

Camber (Fig. 12a) is the deviation of a side edge from a straight line. Measured prior to roll forming, the maximum allowable camber is 3.2 mm/m ($\frac{3}{8}$ in. in 10 ft.). Excessive camber contributes to curve, bow, and twist in the finished part.

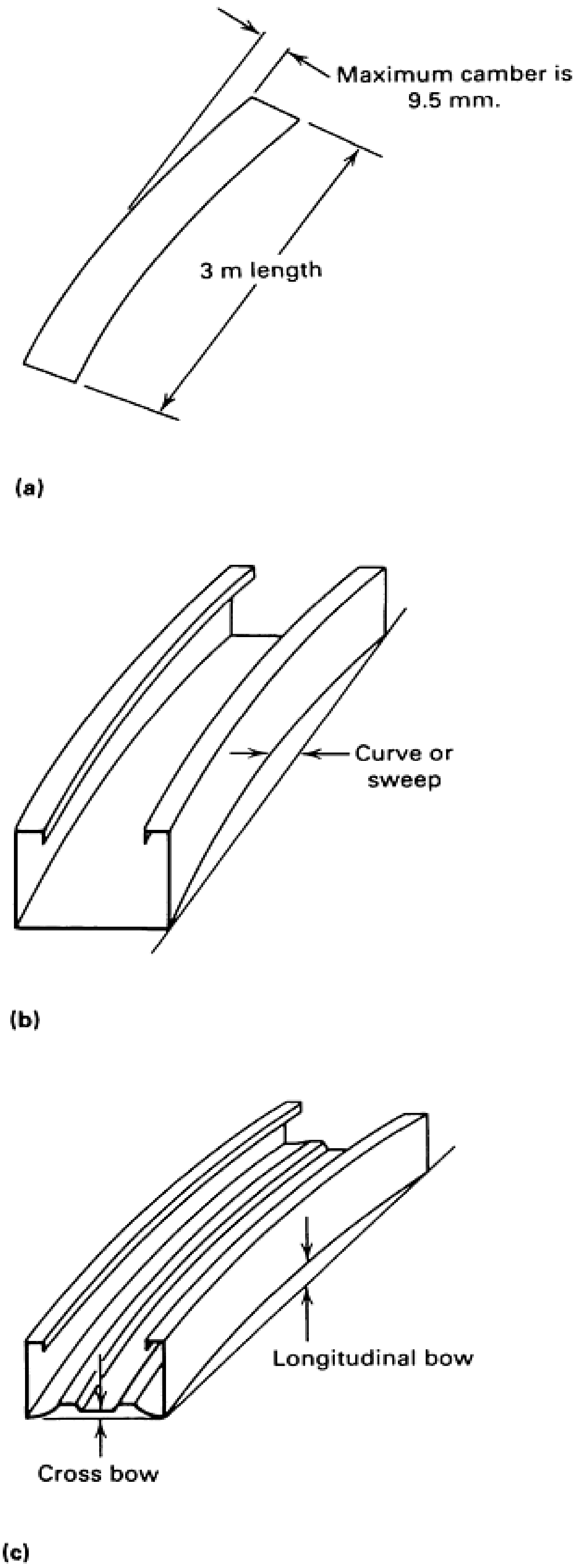

Fig. 12 Straightness parameters for roll formed parts. (a) Camber. (b) Curve or sweep. (c) Bow. See text for details.

Curve or sweep (Fig. 12b) is the deviation from a straight line in the horizontal plane measured after the part has formed. The curve in a formed part can be held to within ±1 mm/m (±1⁄8 in. in 10 ft). Curve or sweep can result from incorrect horizontal roll alignment and uneven forming pressure in a pair of rolls.

Bow (Fig. 12c) is the deviation from a straight line in the vertical plane and can be either cross bow or longitudinal bow. Bow results from uneven vertical gaps on symmetrical sections and from uneven forming areas on unsymmetrical sections. Generally, bow can be held to within ±1 mm/m (±1⁄8 in. in 10 ft).

Twist in a formed part resembles a corkscrew effect and often results from excessive forming pressure. Twist is generally held to less than 5° in 3 m (10 ft).

Quality and Accuracy

Two factors that can affect the quality and accuracy of a roll-formed section are springback and end flare.

Springback is a distortion that becomes evident after the straining of the part has been discontinued. The amount of springback varies with different metal properties such as yield and elastic modulus. Springback can be compensated for in the tool design by overforming. Overforming forms the material past its expected final shape.

End flare is the distortion that appears at the ends of a roll-formed part. The internal stresses incurred in roll forming are much more complex than in other types of bending. These stresses are usually higher in the edges of the material being formed and are released when the part is cut off.

Control of End Flare. End flare occurs to some degree in nearly all roll-formed shapes. It can extend back from the end as little as 50 or 75 mm (2 or 3 in.) or as much as 305 mm (12 in.). End flare can range from a few thousandths of an inch (this small amount is usually ignored) to 12.7 mm (½ in.) or more. The possibilities for flare can pose difficult problems. Both ends may flare outward, or one end may flare outward and the other inward. Excessive flare is caused by hard work metal, workpiece shape, too few roll stations, unsuitable roll design, or a combination of all of these.

There are several means of keeping end flare within acceptable limits. Replacing a hard work metal with one that is more formable will lessen, but not always eliminate, end flare. Minor changes in shape or design of the workpiece are often feasible and should be considered when end flare is a problem. It is much better to recognize the possibility of excessive flare before designing the rolls than to attempt correction after the rolls are made. The best way to minimize end flare is to provide enough roll stations to prevent the sides or edges of the work metal strip from elongating.

Surface Finish

Contour roll forming seldom improves the initial finish of the work metal. One exception is the roll forming of unpickled hot-finished steel, from which much scale is removed. It is usually possible to preserve the existing finish on unformed areas of the work metal. Sheet and strip ranging from unpickled hot-rolled steel to highly polished stainless steel are contour roll formed with a minimum of damage to the surface finish. In addition, work metals having almost every known type of coating are contour roll formed in high production with no damage to the coatings.

This does not mean that no damage to the work metal will occur if the specific applications are not carefully considered in planning the processing technique. In addition to normal precautionary measures, such as keeping the work metal clean before rolling and maintaining the equipment properly, one or more of the following must be considered and possibly adjusted when minimum damage to the surface finish is a primary requirement:

- Roll design or number of rolls in a given station
- Number of stations
- Roll material and finish
- Lubricant
- Rolling speed

As severity of forming increases, the possibility of damage to the work metal surface increases and may require alteration of the rolls within a station. For example, in forming deep channels, the shape can be produced using top and bottom rolls.

Side rolls can be added to improve dimensional tolerance. When maintenance of surface finish is a problem, the use of side rolls is helpful, because it minimizes roll sweep, which is inevitable when the shape is produced solely by top and bottom rolls. Excessive roll sweep is likely to damage both the work metal and the rolls.

Although sliding friction caused by roll sweep may damage surface finish, there is even greater likelihood of damage from excessive forming pressure in a given roll station. Therefore, as severity of forming increases, the possibility of damage to the work metal finish can be lessened by adding stations, thus decreasing the amount of forming done by a given set of rolls and reducing forming pressures.

Roll material and roll finish also contribute to the surface finish obtained in contour roll forming. Chromium-plated steel rolls or aluminum bronze rolls are best for preserving work metal finish.

Lubrication is preferred in contour roll forming and has a significant effect on work metal finish. When lubricants cannot be tolerated, as in the roll forming of coated metals, more attention must be given to roll design, additional stations, roll materials, and possibly lower rolling speeds than would be used if copious amounts of lubricant were permitted.

Each metal presents a different problem in maintaining surface finish.

Hot-rolled unpickled steel seldom offers any problem in maintaining surface finish. Rolling removes much of the scale and usually improves the finish, provided a flood of lubricant is used to flush away the scale. Otherwise, this scale will be trapped between work metal and rolls, resulting in damage to both surfaces. Rolls made from abrasion-resistant tool steel such as D2 are especially recommended for roll forming of hot-rolled unpickled steel.

Cold-finished carbon steel, aluminum, and brass are usually rolled with a minimum of damage to work metal finish. One or more of the conditions listed above may require special attention, depending mainly on severity of forming. A flood of lubricant is desirable in roll forming cold-finished metals.

Highly polished stainless steel or aluminum can also be contour roll formed without damage to surfaces. However, each step of the procedure becomes more critical than is the case with roll forming lower-quality finishes. Greater attention must be given to roll design, fitting, and maintenance. Chromium-plated rolls are usually preferred when work metal finishes are critical. Maximum cleanliness in all phases of the operation (including the use of freshly cleaned work metal) is mandatory for achieving desired results. Special lubricants are preferred for roll forming stainless steel and may be essential when forming is severe and quality of finish is critical.

Galvanized Steel. Success in roll forming hot dip galvanized steel depends mainly on the quality of the zinc coating, maintenance of the rolls, and lubrication. Inferior galvanizing or severe bends, or both, cause the coating to loosen and stick to the rolls. Wipers that contact the working surfaces of the rolls will aid in preventing surface damage. Chromium-plated rolls are also helpful in minimizing damage to galvanized work metal.

Precoated metals (vinyl and other organic coatings) must be rolled without lubricant and sometimes pose problems, although by paying careful attention to the conditions listed above, precoated metals can be contour roll formed without damage to the coating surface. One of the most common applications is the forming of aluminum siding for buildings. Complete preservation of finish depends mainly on severity of forming. Sometimes it is necessary to increase radii if the particular operation is to be successful.

Embossed metals are also roll formed without lubricants. Shapes are designed to avoid excessive forming pressure, and bend radii not less than twice the metal thickness are used to prevent distortion of the embossing. Forming of aluminum eaves troughs is an example of this operation.

Use of Computers

Computers are becoming an important aid in the design of roll-forming tooling. Consistency, accuracy, and speed enable the designer to determine the optimum design for each roll pass in less time than is required when the calculations are performed by hand. The capability to display the profile of the part enables the designer to see how the material flows through each pass. This profile enables the designer to determine whether too much work is being performed at a particular pass.

The numerical information compiled by the computer can be employed in numerically controlled machining operations to ensure that rolls are accurately produced. The computer also aids in the setup of the rolls on the machine by specifying the size and locations of the required shims and spacers. All this information and data can be stored for future use and reproduced whenever necessary.

To design the rolls, information about the roll forming machine, the cross section of the final shape, and the initial forming sequence are entered in the computer program. The computer numerically defines the coordinates of each corner and displays on the computer terminal the profile of the part at each pass as well as various perspectives of the flower diagram. Input changes can be made to vary the material flow through the roll forming machine so that optimum flow is achieved. Computer output includes flower diagrams, drawings of the cross-sectional shape, drawings of the rolls, and tabular data defining the material and rolls. A computer can also produce the tapes used in the manufacturing of the rolls on numerically controlled machines.

Explosive Forming

Revised by A.E. Doherty, Explosive Fabricators, Inc.

Introduction

EXPLOSIVE FORMING changes the shape of a metal blank or preform by the instantaneous high pressure that results from the detonation of an explosive. This article is concerned only with the explosives generally termed high explosives, and not with so-called low explosives.

Metal tubing up to 1.4 m (54 in.) in diameter in lengths up to 4.6 m (15 ft) have been formed using the explosive forming process. Diameters of 1.4 m (54 in.) or less can have lengths of up to 9.1 m (30 ft).

Typical domes constructed of 6- to 12-piece gore sections fabricated from explosively formed metal can measure up to 6.1 m (20 ft) in diameter. Russian engineers have used the process to fabricate gore sections for a 12 m (40 ft) diam dome.

Systems used for explosive-forming operations are generally classified as either confined or unconfined. This article will primarily deal with unconfined systems.

Confined systems (Fig. 1) use a die, in two or more pieces, that completely encloses the workpiece. The closed system has distinct advantages for the forming of thin stock to close tolerances, and it has been used for the close-tolerance sizing of thin-wall tubing. However, confined systems are generally used only for the forming of comparatively small workpieces because economic feasibility decreases as the size of the workpiece increases.

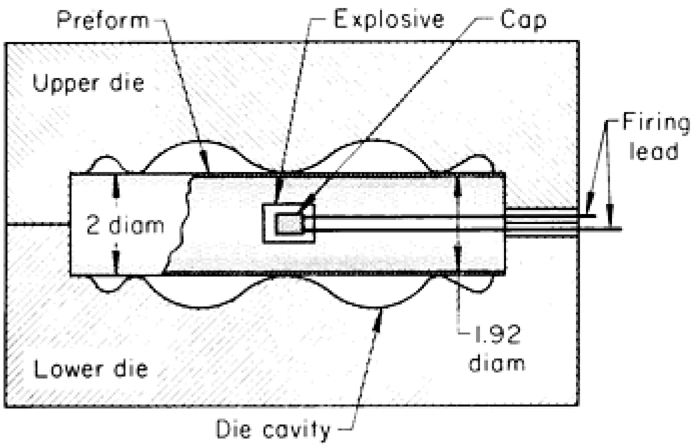

Fig. 1 Confined system for explosive forming. Dimensions given in inches.

Unconfined Systems. In an unconfined system (Fig. 2), the shock wave from the explosive charge takes the place of the punch in conventional forming. A single-element die is used with a blank held over it, and the explosive charge is suspended over the blank at a predetermined distance (the standoff distance). The complete assembly can be immersed in a tank of water, as shown in Fig. 2, or a plastic bag filled with water can be placed over the blank.

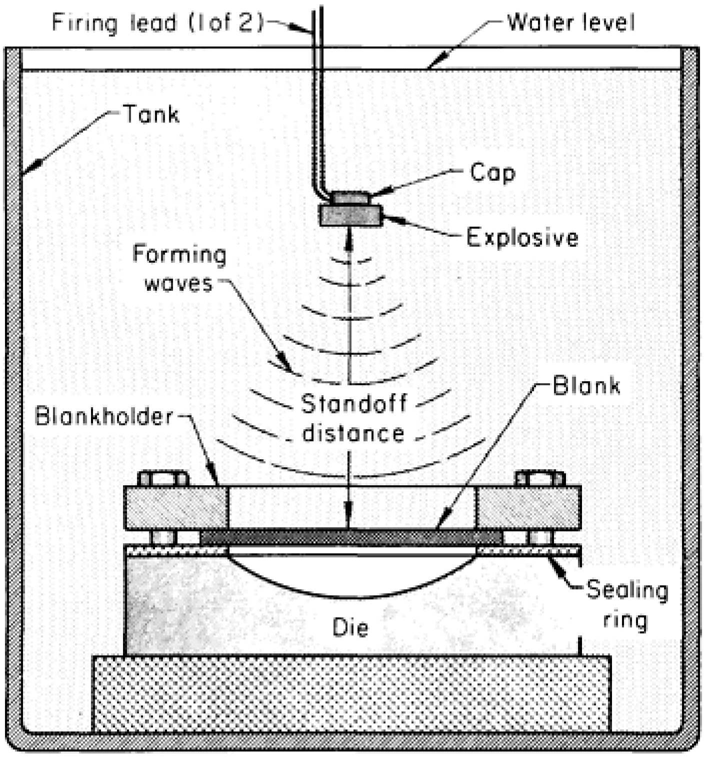

Fig. 2 Unconfined system for explosive forming.

The unconfined system is inherently inefficient, because only a small part of the total energy released by the explosion is effective as forming energy. The medium in which the explosion occurs plays an important role in determining the efficiency of the system. Efficiency increases with the density of the medium. Therefore, most explosive forming of large pieces is done in a medium more dense than air. Water is the most commonly used medium for ambient-temperature explosive forming. Molten aluminum has been used as the medium in explosive forming at elevated temperature.

Under normal operating conditions, it is best to detonate the explosive charge as far below the surface of the water as possible. This reduces the amount of water that is thrown by the explosion, and it reduces the amount of energy lost by venting to the atmosphere the gas bubble that results from the detonated charge. However, there is a depth for a given size of charge below which additional efficiency will not occur.

The amount of energy or peak pressure delivered can be calculated from standard formulas. A cylindrical charge (or point charge) is generally located near the centerline of the part and at a standoff distance that is related to the span of the workpiece over the die cavity. For large parts, it is generally impractical to use a point charge. For example, in forming large hemispheres or end closures for rocket motors, Primacord--either shaped in a large loop and located close to the outer periphery of the part or strung in a net form (using a fish net as a suspension device)--is ordinarily used instead of a point charge. Primacord is a cordlike detonating fuse that consists of a filament of explosive material covered by a protective water-repellent coating.

The variations in energy level delivered from various shapes of charge are small when the charge is fired in a water medium if standoff distance is 305 mm (12 in.) or more. When charges are placed close to a blank (within 25 to 50 mm, or 1 to 2 in.), energy transfer mechanisms from the explosive to the workpiece change. Figure 3 shows an example of these effects.

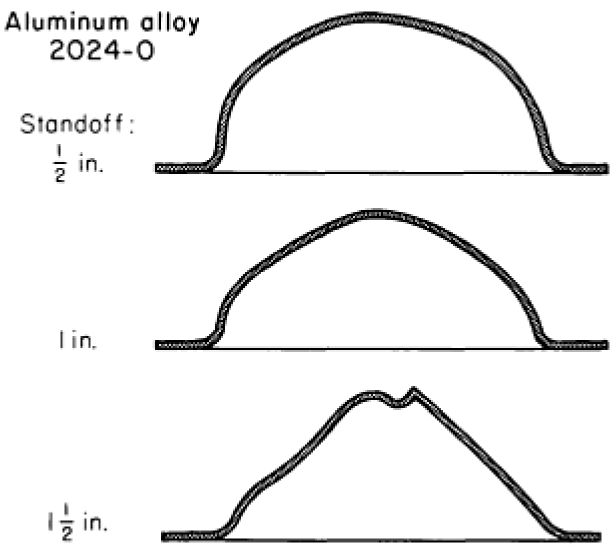

Fig. 3 Cross sections of explosive-formed workpieces showing the effect of slight changes in standoff distance at close range.

Explosive Forming

Revised by A.E. Doherty, Explosive Fabricators, Inc.

Equipment

The primary equipment for explosive forming in an unconfined system consists of a water tank, a crane, a vacuum pump, and a detonator control (firing) box.

The water tank must withstand the repeated impacts of the explosive shock without rupturing. Many tanks are designed to be large enough so that the shocks reaching the walls from centrally placed charges are considerably reduced. Figure 4 shows the stress in a tank wall as a function of the radius of the tank for a 0.5 kg (1 lb) charge of TNT and a constant wall thickness of 25 mm (1 in.). These data show that an increase in the diameter of the tank from 1.2 to 12 m (4 to 40 ft) lowers the stress level in the tank wall by only about 26%--from 11.4 to 8.45 MPa (1650 to 1225 psi). At the same time, the weight of tank-wall material increase tenfold.

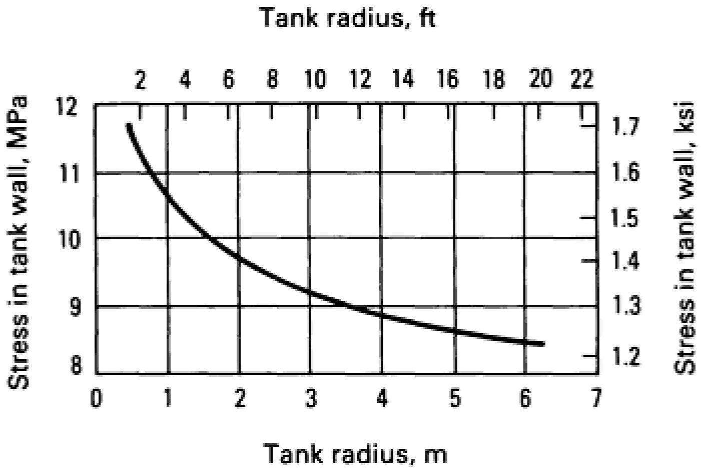

Fig. 4 Effect of tank radius on stress produced in a 25 mm (1 in.) thick tank wall by the detonation of 0.45 kg (1 lb) TNT at the tank center.

One of the best approaches to the reduction of tank-wall thickness is to moderate the pressure of the shock wave before it strikes the tank wall. Figure 5(a) shows the use of inflated rubber tubing for the reduction of stress in the tank walls. The rubber tubing acts as a cushion; in one application, it provided an 83% reduction in stress. A considerable amount of tubing would be needed to use this technique in a large tank, and it would be difficult to maintain the position of the tubing and to prevent damage to the tubing during explosions. The use of an air-bubble curtain as shown in Fig. 5(b) appears to be one of the best solutions for low-cost water tank construction. To be effective, a uniform and closely spaced curtain of bubbles must be maintained along the walls of the water tank. The bubble curtain is controlled by the size and spacing of holes in the air line at the base of the tank and by the air flow and pressure maintained in the line. A maximum charge size of 0.5 kg (1.1 lb) of TNT has been fired in a 2.4 m (8 ft) diam tank having 12.5 mm (½ in.) thick steel wall, 25 mm (1 in.) thick steel bottom, and a curtain of air bubbles with no visible signs of damage to the tank wall when the bubble curtain was operating.

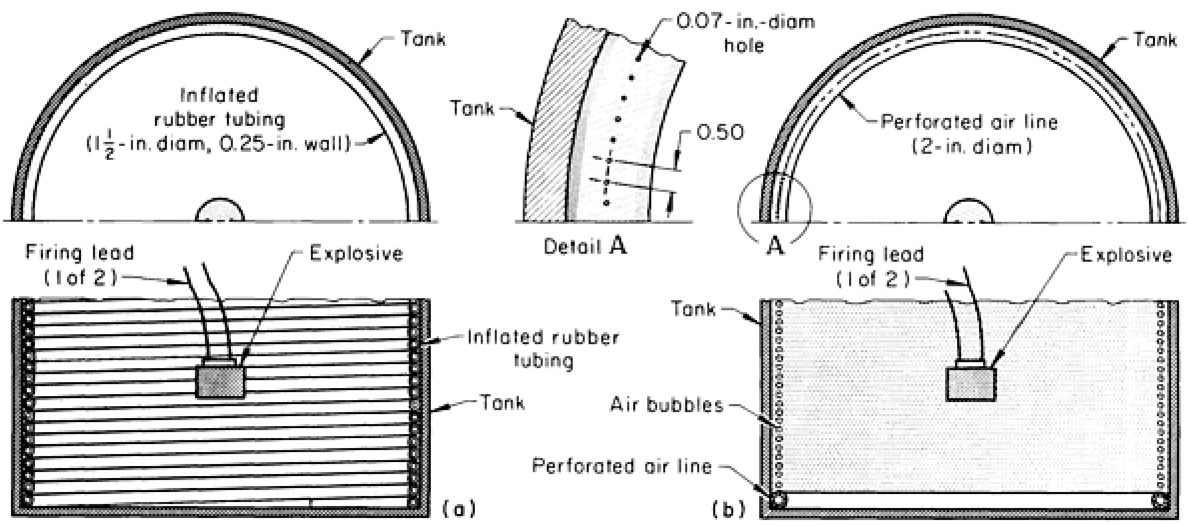

Fig. 5 Use of inflated rubber tubing (a) and an air-bubble curtain (b) for reducing the stress in the tank walls induced by explosion. Dimensions given in inches.

The base of the tank must withstand at least as much impact as the tank walls. It is advantageous to place the tank in solid rock, where possible. A heavy base of reinforced concrete can be used if it is covered by a heavy steel plate to distribute stress in the concrete evenly, preventing localized high stresses. Shock-absorbing material can be placed between the concrete and the steel baseplate; a sheet of rubber will provide some stress reduction. The use of a closely coiled, inflated rubber hose between the baseplate and the base of the tank is also effective in reducing stress; stress in the base can be reduced by as much as 80% with this technique. The hose should be inflated with only enough air to support the baseplate and the water head above it; overinflation will increase stress-wave transmission and may result in damage to the hose.

Difficulties in sealing the tank base to the tank wall must also be overcome. In some metal tanks, the joint has been welded, but not with complete success. Seals of resilient plastic have provided satisfactory results, and they are easy to repair if a leak develops.

A crane is usually needed to move material around the facility, as well as in and out of the water tank. Ideally, the crane should be air operated to avoid having electric power lines within the firing area. The required capacity of the crane depends on the size and weight of the dies to be handled. A jib crane is one of the best systems for handling large dies. One of the largest portable dies used in an explosive-forming operation weighed 9500 kg (21,000 lb).

A vacuum pump will probably be needed for most explosive-forming operations when parts are formed under water. If the firing area is to be maintained with a minimum of electric lines, an entire pump operating on water pressure will work satisfactorily. A mechanical pump driven by an electric motor can be used; the vacuum lines are brought into the firing area from a remote pumping site. An electrically driven mechanical pump is preferred because it has a considerably greater capacity than a venturi pump and is more economical to operate. A pump that will not be affected or damaged by water intake is the best type. These are sometimes called liquid ring pumps. If a conventional vane-type mechanical pump is used, it is important to eliminate any possibility of water entering the pump. In a high-production facility, where the application of a vacuum might be unacceptably time consuming, a storage tank in the vacuum line will greatly assist the operation. If it is deemed necessary to operate an electrically driven vacuum pump in the firing area, it should have shielded wiring and a sealed motor.

Detonation Circuit. Under ideal conditions, the firing box for the electric blasting caps is the only electric device that should be permitted in the area where explosives are handled. A firing box should be constructed on the fail-safe principle so that any malfunction will immediately cause the circuit to be disarmed. The following characteristics are desirable in the design of a firing box for use in explosive-forming applications:

- The device should be operable only with a key that is carried by the individual setting the charges
- When the device is not armed with the key, this fact should be visually discernible from the work area
- The lead wires to the cap should always be shorted when the circuit is not armed
- When the circuit is armed with the key, both a visual and an audible warning of its armed condition should be activated automatically
- A method of checking the continuity of the blasting circuit should be an integral part of the firing box

A schematic wiring diagram for a detonator that meets these requirements is shown in Fig. 6.

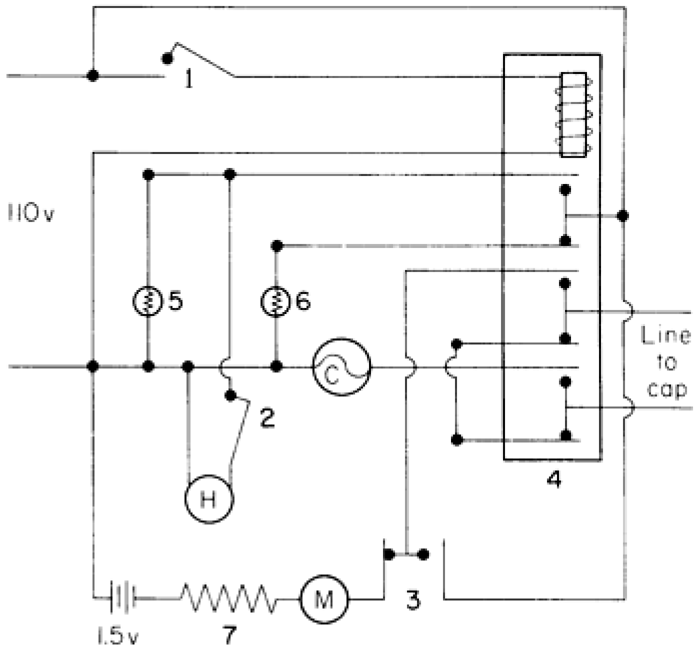

Fig. 6 Wiring diagrams for a safe detonator for explosive forming. 1, key switch (single pole, single throw); 2, horn switch (single pole, single throw; momentary-off operation); 3, continuity and firing switch (single pole, double throw); 4, relay (triple pole, double throw); 5, "armed" warning light; 6, "safe" warning light; 7, 10,000 Ωresistor. C, 5 A circuit breaker; H, horn; M, microammeter.

The current for firing the blasting caps can come from a 6 V battery or directly from a 110 V line. If a 110 V line is used, it is advisable to use a circuit breaker to protect the lines if a short occurs during firing.

Figure 6 shows the circuit for a firing box in the unarmed condition, the condition being demonstrated by current flowing through the uppermost of the three poles in the relay (4) to the "safe" warning light (6). Switch 1 is actuated by a key carried by the responsible person. It operates the relay. The upper pole disconnects the "safe" warning light and connects the "armed" warning light (5) and the horn (H). The two other poles shift to disconnect the short circuit of the line to the caps and connect its two leads to the firing circuit. Switch 3 is the detonating switch. Before it is thrown, the firing circuit is completed through the battery and the microammeter for a continuity check of the circuit. When switch 3 is thrown, the 110 V line is connected to the firing circuit and the cap detonates. Thus, the conditions listed above are met.

Explosive substances used in the explosive process have detonation velocities ranging from 1400 to 8380 m/s (4600 to 27,500 ft/s). Additional properties of selected explosives used in explosive forming are listed in Table 1. The conversion from solid explosive to gaseous products takes fractions of a second and creates temperatures of 3000 to 5500 °C (5432 to 9900 °F) and pressures of 6.9 to 28 GPa (1000 to 4000 ksi) (Ref 2).

Table 1 Properties of selected high explosives

Explosive	**Relative power, % TNT**	**Form of charge**	**Detonation velocity**		**Energy**		**Detonator required**	**Storage life**	Maximum pressure	
			m/s	**ft/s**	**kJ/kg**	**ft · lbf/lb**			**GPa**	ksi
Trinitrotoluene (TNT)	100	Cast	7010	23,000	780	262,000	J-2[(a)]	Moderate	16.5	**2400**
Cyclotrimethylene	170	Pressed	8380	27,500	1270	425,000	No. 6	Very good	23.4	**3400**

trinitramine (RDX)		granules								
Pentaerythritol tetranitrate (PETN)	170	Pressed granules	8290	27,200	1300	435,000	No. 6	Excellent	22.1	**3200**
Pentolite (50/50)	140	Cast	7620	25,000	950	317,000	No. 8	Good	19.3	**2800**
Tetryl	129	Pressed granules	7835	25,700	. . .	. . .	Special[b]	Excellent	. . .	**. . .**
Composition C-3	115	Hand-shaped putty	8045	26,400	. . .	. . .	No. 6	Good	. . .	**. . .**
40% straight dynamite	94	Cartridge granules	4725	15,500	605	202,000	No. 8	Fair	6.7	**970**
50% straight ditching dynamite	103	Cartridge granules	5305	17,400	660	220,000	No. 6	Fair	. . .	**. . .**
60% extra dynamite	109	Cartridge granules	3810	12,500	715	240,000	No. 6	Fair	4.3	**620**
Blasting gelatin	99	Cartridge plastic	7985	26,200	1220	408,000	J-2	Fair	17.9	**2600**
Bituminous coal D permissible explosive	. . .	Cartridge granules	1400	4,600	. . .	. . .	No. 8	Fair	. . .	**. . .**
Primacord, 8.5 g/m (40 gr/ft)	. . .	Plastic or cotton cord	6340	20,800	. . .	. . .	No. 6	Excellent	. . .	**. . .**
Mild detonating cord, 2.1 g/m (10 gr PETN/ft)	. . .	Metal-coated cord	7315	24,000	. . .	. . .	Special	Excellent	. . .	**. . .**
Detasheet	. . .	Cut to shape	7225	23,700	. . .	. . .	No. 8	Very good	. . .	**. . .**
Cyadyn 3	90	Cartridge granules	2135	7,000	. . .	. . .	No. 6	Fair-good	. . .	**. . .**
IRECO DBA-10HV	**20**	**Slurry (two parts)**	**3505**	**11,500**	**. . .**	**. . .**	**Special**	**Excellent (unmixed components)**	**. . .**	**. . .**

Source: Ref 1

(a) With booster.

(b) Special engineer's blasting cap.

Auxiliary Equipment. During the operation of an explosive-forming facility, debris will probably collect on the surface of the water or be distributed throughout the water tank. Skimming equipment, such as that used for swimming pools, serves well for the removal of material floating on the surface. Material distributed throughout the water must be removed by filtration. However, any explosive material collected in the water tank will be pumped through the filtration lines and possibly into the pump, where it may collect and later cause an accident.

In locations where water is plentiful, the tanks are emptied by gravity and refilled frequently; in other areas, the same water must be reused. The dumping of tank water into sewer lines should be avoided because any explosive suspended in the water may be trapped by an obstacle in the sewer and accumulate into a concentration that could cause a serious accident. When an incomplete detonation of the explosive material is detected, attempts should be made to recover as much of the undetonated explosive as possible for later destruction. The filter material should be handled as explosive material when it is emptied.

The amount of explosive stored within the facility should always be kept to a minimum and preferably should not exceed the supply necessary for one day's operation. For the temporary storage of explosives within the facility, small storage containers can be made from discarded refrigerators. The caps should be stored in one and the explosives in another. The main storage of explosives for the operation should be at some distance from the facility. Local ordinances regarding explosive storage and storage containers should always be checked.

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2. J.S. Rinehart and J. Pearson, *Explosive Working of Metals,* Macmillan, 1963, p 19-20, 263-266

Explosive Forming

Revised by A.E. Doherty, Explosive Fabricators, Inc.

Die Systems and Materials

Basic differences between tooling for explosive forming and for conventional forming arise from the type of loading that the die material must withstand. In explosive forming, high-impact loads transmit shock waves through the metal that cause unusual stress patterns within the die material; therefore, corners should be eliminated where possible. Shock loading causes the die to fracture along lines from the corners, rather than through the thinnest section as in static fracture. Figure 7 shows the modes of fracture for conditions of static and dynamic (shock) loading.

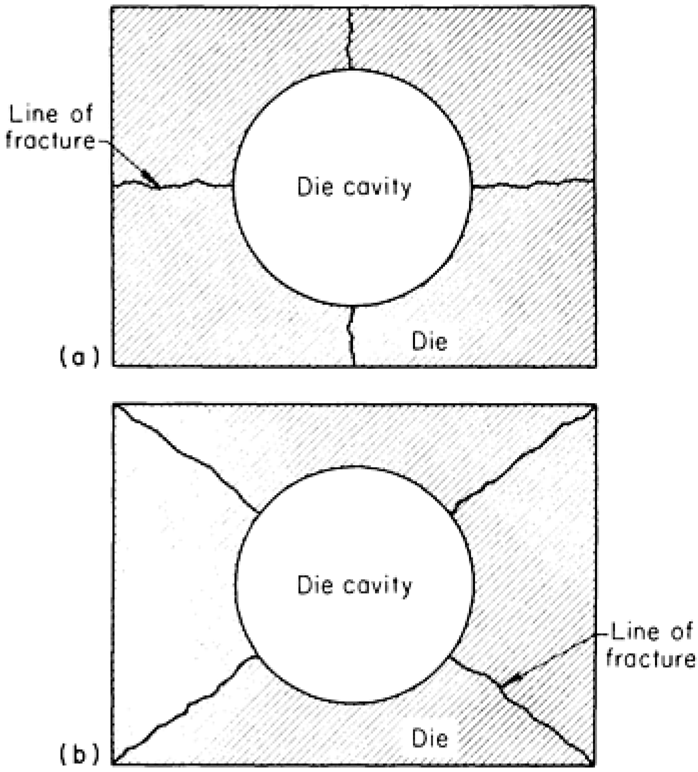

Fig. 7 Modes of fracture of a rectangular die under static (a) and dynamic (b) loading.

Evacuating Ports and Seals. In most explosive forming, a vacuum is applied between the blank and the die cavity. If possible, some method of sealing should be made an integral part of the die. In many applications, the use of rubber projecting about 1.6 mm ($\frac{1}{16}$ in.) above the die surface has provided the necessary seal. Because explosive forming causes the metal to pick up detail from the die, the vacuum port in the die should be kept small and should be located in a part of the die where its impression can be removed from the workpiece in a subsequent trimming operation. If this is not possible, the vent hole should be located so that the dimple that is formed will not be objectionable.

Blankholder Design. A blankholder is generally necessary to prevent wrinkling of the metal as the blank is drawn into the die. The size of the blankholder and the clamping pressure needed depend on the type of metal being formed. With a soft metal, such as annealed aluminum, very little blankholder pressure is needed; hard metals, such as stainless steel, need high pressures to prevent wrinkling.

Bolts or heavy-duty C-clamps are often used to clamp the blankholder to the die. Hydraulic clamping jacks are usually efficient in the forming of materials such as aluminum that do not need high clamping forces for the blankholder.

In applications in which the parts to be made are concentric and the forming is to be done in air, it may be possible to eliminate the blankholder. A taper-wall die has been used for forming cones, where the blank is situated on a taper leading into the die. This technique can provide savings when above-ground operations are used.

Die Design Calculations. Typical design calculations for cylindrical dies are as follows (Ref 2). To determine the explosive load, the properties of the material to be formed are used. It is first necessary to determine the yield pressure:

$$P_y = \frac{2 \cdot YS \cdot t_m}{D}$$

where P_y, is the yield pressure, YS is the yield strength, t_m is the material thickness, and D is the diameter. Next, the equivalent static pressure is found:

$$P_{ex} = KP_y$$

where P_{ex} is the equivalent static pressure to size explosively, K is the forming factor (average K value is 3 to 20, depending on the complexity of the part being formed), and P_y is the yield pressure.

To find the die wall thickness, the design stress is first determined:

$$\sigma_t = \frac{YS}{X}$$

where σ_t is the design stress, YS is the yield strength, and X is the safety factor (X value is 4 to 6). Then, using Lame's formula:

$$R = r\left[\frac{\sigma_t + P_{ex}}{\sigma_t - P_{ex}}\right]^{1/2}$$

where R is the outside radius of the die, r is the final part radius, σ_t is the design stress, and P_{ex} is the equivalent static pressure to size explosively. Therefore:

$$W_t = R - r$$

where W_t is the die wall thickness, R is outside radius of the die, and r is the final radius of the workpiece. The die wall thickness calculated by the above equations can be further reduced by the use of stiffening ribs (Fig. 8).

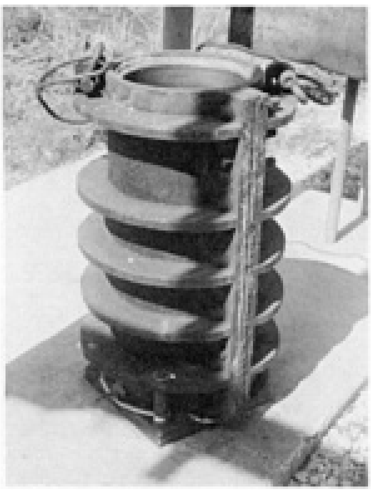

Fig. 8 Explosive forming die with stiffening ribs that was used to size more than 1500 stainless steel tanks. Source: Ref 3.

Materials for Solid Dies. Solid dies made from heat-treated alloy steel maintain contour, surface finish, and dimensional accuracy for a relatively long time. To avoid brittle fracture under overloads, a maximum hardness of 50 HRC is desirable.

Where the need for long life and good surface finish does not justify the cost of alloy steel dies, low-carbon steels such as 1010 or 1020 may be good alternatives. In practice, a light coat of lubricating oil over the steel die surface after each forming operation will provide sufficient protection from rusting. In addition, steel dies should be dried and coated with oil at the end of each day's operation. A castable alloy containing about 95% Zn and small amounts of aluminum and manganese has been widely used when the production quantity does not exceed 100 pieces.

Reinforced concrete has been considered for the construction of dies large enough to exceed the machining capabilities of all-metal dies. The ease of producing large concrete dies is a primary advantage. Disadvantages include the low tensile strength of concrete.

Plaster has been used for one-shot dies. Because the rate of loading is very fast, the shape of the brittle die material is transferred to the metal surface before the die crumbles. Better results are produced if a plaster die is contained in a metal case so that the plaster is loaded in compression to the greatest extent possible. The case should be cylindrical to minimize stress concentration.

Materials for Composite Dies. Glass cloth/epoxy resin laminates backed with either concrete or a sand, gravel, and epoxy mix are used. This system is usually contained in a low-carbon steel container, such as A36 pipe, for cylindrical systems. Where heavy loading is encountered, the life of a plastic laminate is about 25 pieces before it begins to crack and must be replaced. Because replacement cost is low, this is not a serious disadvantage.

Concrete has been used for the die working face when only one shot is needed. Ice dies have been successfully used on domes as large as 3.05 m (120 in.) in diameter.

The use of plastic laminates in zinc alloy dies of complex shapes will provide a considerable savings in die sinking time. The body of the zinc alloy die is cast roughly to shape, and a plastic laminate made from a plaster matrix is then seated in the die by backfilling with a resilient plastic. Dies have also been made from laminate plates and conventionally formed thick-wall shells.

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2. J.S. Rinehart and J. Pearson, *Explosive Working of Metals,* Macmillan, 1963, p 19-20, 263-266
3. A.E. Doherty, Applications of Commercial Explosives to Production of HERF Parts, in *Proceedings of the 9th International Conference on High-Energy Rate Fabrication,* 1986, p 246-250

Explosive Forming

Revised by A.E. Doherty, Explosive Fabricators, Inc.

Transmission Mediums

Most of the early work in explosive forming was done in air, which provided very high peak pressures for very short periods--usually a few microseconds. Consequently, the total impulse available for forming was less than that in a liquid medium, which provides slightly greater confinement of the charge and higher efficiency in terms of total impulse. The size of charge needed for forming a given part in water is approximately 80% smaller than that needed if the art were formed in air.

Liquids. Water is one of the best media for explosive forming because it is readily available in most locations, inexpensive to use, and produces excellent results. The energy absorbed by a medium is a direct function of its density; therefore, considerably more energy might be lost by waves transmitted through a liquid than through air. This loss of energy, however, is more than compensated for by the additional confinement of the explosive charge and the lengthening of the pulse due to the trapped energy. The net result is an increase in total impulse available in a liquid over that transmitted in a gas for the same charge size and standoff distance. When a charge is confined in a liquid medium and the charge is far enough from the surface of the water, several pulses can be obtained as a result of the overexpansion and overcompression of the gas bubble from the explosive charge. The greater confinement of the explosive by the water evens out the pulse distribution and maintains a positive pressure for a period of milliseconds. Figure 9 shows pressure differences between water and air media, at various standoff distances, using a 1.8 kg (4 lb) charge of TNT.

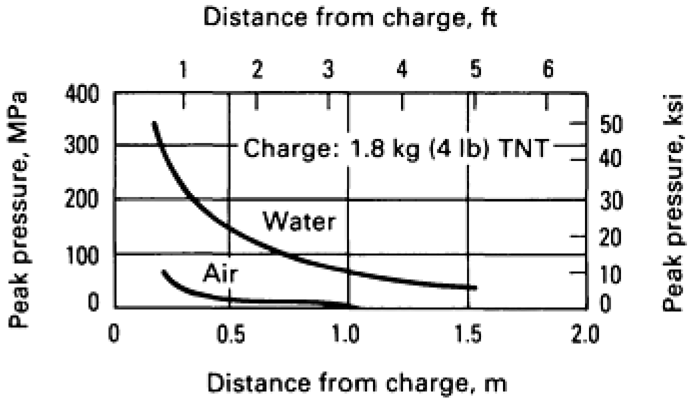

Fig. 9 Peak pressure versus standoff distance for explosive charges fired in air and water.

Other liquids, such as oil, have been successfully used as transmission media. Depending on their densities, they perform in the same general manner as water, but water is usually preferred because of easier handling. Higher-density liquids, such as mercury, are too expensive for general use.

Solid media, in the form of rubber sheets, cast plastics, or even metals, are sometimes used in explosive forming. Plastics and rubber improve the formability of some metals. The use of a plastic transmission medium has proved especially helpful in the close-tolerance sizing of tubing details.

Solid media afford some protection to the surface of the part being formed. Sometimes, in liquid media or in air, small slivers of metal projected from the blasting cap or from wire or other material used to attach the charge may strike the surface of the blank and cause scrap. Difficulty is also sometimes encountered because of slight irregularities in the shape of the explosive charge, which cause the formation of jets. The jet effect is smaller under water and is normally troublesome only at very small standoff distances. Perhaps one of the most beneficial characteristics of a solid medium is the uniform distribution of pressure over the surface of the part being formed.

The explosive forming of heated metals demands the use of some inexpensive medium that will maintain its characteristics at relatively high temperature and will not transmit much heat to the explosive charge. Several materials, including sand and small glass beads, have been used for this purpose. If sand is used, some buffer material should be placed over the blank to prevent the sand from embedding in the work metal or marking the surface.

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Shock-Wave Transmission

Shock waves travel through a medium at the speed of sound. When a minimum of energy loss is desired at an interface between two different media, they should be selected to have a close acoustical impedance match. The acoustical impedance is a function of the density of the medium; therefore, as a first approximation, matching the densities of the media helps to increase the efficiency of the system.

Figure 10 shows the effect on the depth of draw of placing an intermediate medium, such as a sheet of rubber, over a blank. The increase in rubber thickness lowers the maximum depth of draw a given explosive charge will produce, but it may increase the overall depth of draw the piece will tolerate because of the improved distribution of forming stresses on the work metal. The use of a solid intermediate medium allows more material to draw from under the blankholder without wrinkling and without fracturing the cup at the apex.

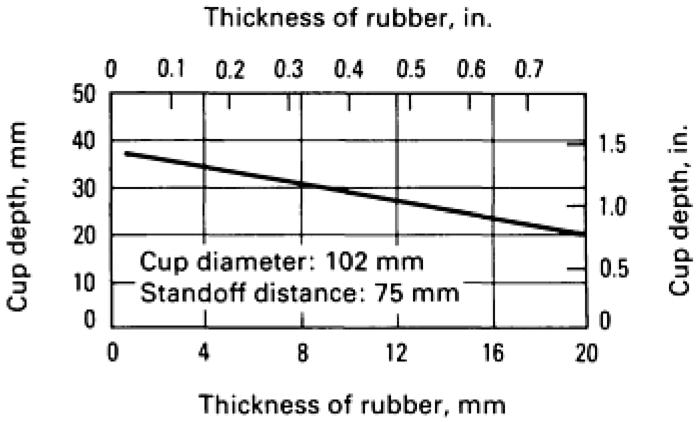

Fig. 10 Effect of thickness of rubber pad placed on blank on depth of draw of a 1.63 mm (0.064 in.) thick PH 15-7 Mo stainless steel cup in explosive forming with 1520 mm (60 in.) of 100 grain Primacord.

The same reasoning is applicable to the impedance between the explosive charge and the medium in which it is detonated. By obtaining a reasonable impedance match between the explosive and the medium, only weak reflections occur at the surface of the medium container. If the container walls are shaped properly, the reflections can be used to reinforce the shock wave that strikes the workpiece. They can also be directed to areas of the workpiece that need greater amounts of energy for forming.

Explosive Forming

Revised by A.E. Doherty, Explosive Fabricators, Inc.

Forming of Sheet

Explosive forming has been most extensively used to produce parts from sheet metal. For concentric shapes, the tooling and charge placement are relatively simple, and only minimal experience is needed to produce acceptable parts. Nonconcentric shapes, however, such as beaded panels, electrode forms, and other asymmetrical shapes, involve techniques using uneven force distributions, shock-wave reflectors, and shaped charges.

Tooling Considerations. For concentric parts, fairly simple techniques such as casting or turning can be used for the production of tools. The more complex dies used for nonconcentric parts necessitate hand finishing or profile milling. Special features can be incorporated into dies to control metal deformation and to minimize buckling.

In simple drawing operations, buckling is prevented by the pressure applied to a blankholder. With concentric parts, an equal blankholder pressure is applied around the circumference; with nonconcentric parts, variations in blankholder pressure are needed to accommodate variations in depth of draw. Pressures can be estimated for concentric shapes, but a trial-and-error system must be used to establish the pressure patterns for nonconcentric shapes. In such systems, the blankholder pressure for the minimum depth of draw should be 2 to 4% more than for areas of maximum draw. Control of metal movement during forming can also be accomplished by using a bead around the die to induce more friction between the blank and die. Another method is to notch the blank perimeter to reduce compression forces. This method is much less flexible, particularly when a trial-and-error approach is used in the initial forming. The forming of thin sheet metals can be assisted by sandwiching the blank between two low-carbon sheets to increase the cross section.

Tolerances as close as ±0.025 mm (±0.001 in.) have been met on small parts by explosive forming. However, working tolerances are generally ±0.25 mm (±0.010 in.). Variations are directly related to the amount of pressure applied in the forming operation. The use of plastic or rubber fillers over parts also has considerable bearing on the variations in workpieces. Because filler materials decrease the total pressure imposed on the part but maintain the pressure for a longer period of time, the increase in total impulse improves conformation to the die and minimizes springback. There are few applications in which explosive forming in steel dies cannot equal or better the dimensional stability of conventional forming.

Placement of Explosive Charges. Explosive-forming operations usually require that the charges be at some standoff distance from the parts to be formed; a contact charge supplies a peak pressure so high that the blank may be ruptured. Positioning of the explosive charges can be performed by a number of techniques, provided the following conditions are met:

- The method of positioning should be substantial enough that immersion in a water tank for firing will not displace the charge
- The rigging for the charge should not break into flying projectiles that might damage the workpiece

To meet these requirements, various types of rigging have been used. For large parts, small-gage wire is used and is generally retrieved with the die. For smaller parts, masking tape is normally preferred for locating the charge because it is easy to handle. Permanent steel rigging can also be used if the charge is separated from the rigging by at least 50 mm (2 in.). Cardboard tubes work well for separating the charge from the rigging if the tube is not immersed in the water longer than 5 min before the charge is fired. For longer immersion times, the tube should be sprayed with a plastic coating.

It is often possible to obtain the same results with different types of charges. For example, tank ends can be formed with cylindrical and spherical charges or with an equal weight of Primacord wrapped on a cardboard tube. Similar results are obtained if the diameter of the Primacord does not exceed twice the diameter of the solid cylindrical charge. Consequently, when the supply of one type of explosive has been depleted, it is often possible to substitute another type that is on hand.

Special shapes of explosive charge are generally needed for the forming of nonconcentric shapes. The development of a special charge is difficult, and the shape is usually made by trial-and-error tests. Energy transmission to the part can also be varied through the use of a rubber blanket covering surface areas that need the least forming. The most prevalent technique involves the use of several charges to work the metal into the die in steps.

Shock-wave reflectors are suitable for producing parts that need a deeper draw in one particular region. However, these reflectors become very complicated if there is more than one such region in a part.

Economics of Explosive Sheet Forming. Simple shapes that are readily formed by conventional methods should not be considered for explosive forming; no economic advantage will be realized. More complex shapes, as well as metals with special properties (such as the work-hardening stainless steels), lend themselves to explosive forming. Size must also be taken into account; explosive forming can produce extremely large items that would be impractical to form by conventional methods.

Cost comparisons for fabricating parts by both explosive and conventional forming methods require sufficient quantities to establish cost information. The number of pieces to be produced affects the relative economy. Although explosive forming is often applicable only to short production runs, as many as 20,000 pieces have been produced competitively by explosive forming.

Explosive Forming

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Forming of Plate

The explosive forming of metal plate has been practical because presses large enough to form heavy plate are generally not available. Economic advantages are realized when a forming operation, prior to the machining of thick parts, can reduce the subsequent machining time and the weight of raw material required. Explosives have been used to pierce holes in heavy shapes. This operation requires the use of a wave guide, with appropriately positioned and sized holes, over the workpiece.

Tooling Considerations. To support the higher loads required for the explosive forming of metal plate, the tooling must be of heavier construction than that used in sheet forming. Plates are usually not formed to close tolerances, particularly because they are often machined in a subsequent operation. Forming is generally done in free-forming dies such as the one shown in Fig. 11.

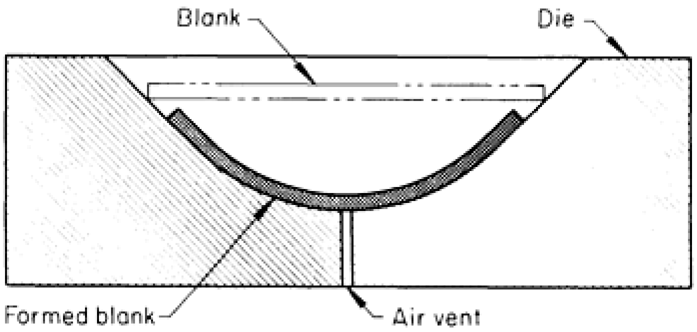

Fig. 11 Tapered-entrance die used for the explosive forming of plate without a blankholder.

Tolerances that can be met in explosive-formed plate materials are the same as those usually applied in the explosive forming of sheet metal, that is ±0.25 mm (±0.010 in.). Considerably greater tolerances than this are usually allowed on parts that are to be subsequently machined. With free-formed plate materials, variations as great as ±6.35 mm (±0.250 in.) have been considered acceptable.

Placement of explosive charges for plate forming is similar to that for sheet forming, except for the scaled-up size of the tooling, workpiece, and explosive charges. Because water is the preferred transmission medium in these operations, the operations must be performed in large water tanks, or if firing is aboveground, a large water bag can be used. In each approach, the explosive energy is transmitted to the workpiece through water.

Because of the relatively simple shapes involved in plate forming, charge shapes and placement are less complex. Centrally located charges and ring charges positioned at the desired standoff distances are usually used.

Forming of Tubes

Explosive forces have been successfully used in tube-forming operations. This process has allowed the formation of many unique tubular shapes by beading and bulging the initial workpieces in selected areas.

Tooling Considerations. To facilitate removal of the completed tubular workpieces, it is necessary to use either split dies or split tapered die inserts, depending on the particular part to be formed. The use of split dies makes evacuation more difficult than with single dies in that rubber seals are necessary between the die halves. The parting line between the die segments will often leave undesirable marks on the formed parts. This marking effect can be lessened by reducing the explosive charge and increasing the number of shots or by increasing the clamping forces on the die halves.

Some control of the amount of bulging can be achieved through the use of end plugs to apply some restraining force to prevent drawing from the ends of the tube. This precaution can be critical in thin-gage tubing, which may wrinkle if the ends of the tube are not restrained.

Shock-wave reflectors can be incorporated into the tooling for explosive forming. They find applications in tooling for nonconcentric shapes or where reentrant angles are desired. With the use of special reflectors, the charge can be placed at one end of the tube, and a reflector can be placed inside the tube to concentrate the shock wave in certain areas. Very large bulges (350% of tube diameter) have been produced with the use of reflectors, intermediate anneals, and step-forming operations. The reflectors can vary considerably in design, ranging from a solid filler with an angled cut (to concentrate the energy on one side of the tube) to exponential shapes for reentrant angles.

Tolerances as small as ±0.025 mm (±0.001 in.) have been held in the forming of small-diameter tubes, but tolerances of the order of ±0.25 mm (±0.010 in.) are more generally accepted. Extremely close tolerances demand the construction of heavy, accurate dies that will withstand repeated heavy loading in the production of the desired parts.

Placement of Explosive Charges. The setup for forming generally requires the use of line charges placed on the centerline of the tube. Alignment of the charge is critical because the tube provides some reflective characteristics to the shock wave, and misalignment can cause higher pressures in the side of the tube closest to the charge. A rigid plastic tube can be used to cover the explosive charge and to keep it in line.

Normally, a die is necessary when a specific contour is wanted. Simple bulges can be formed in heavy-wall tubes without a die, but tolerances cannot be held closely on the diameter or on the contour of the bulge.

Application. The sound-suppressor tubes used on all commercial jet aircraft represent an application that has involved the production of more than 10,000 pieces. The main economic advantage in this application is derived from the close tolerances that can be held, as well as the elimination of brazing and other manual operations normally required for the assembly of conventionally formed tubes.

Explosive Forming

Revised by A.E. Doherty, Explosive Fabricators, Inc.

Forming of Welded Sheet Metal Preforms

Explosive forming has been used in the forming of components from welded preforms. Such preforms are necessary when the initial tube size is larger than that obtainable commercially or when a specialized starting shape is needed. Because subsequent forming operations will be conducted, it is necessary that good-quality ductile welds be made in preform fabrication. Generally, the welds are planished, and the preforms are annealed prior to forming. In addition, when possible, the welds are located in areas where minimal stretching is expected in order to reduce the possibility of weld failure during forming. Figures 12 and 13 show typical aircraft components that were explosively formed from welded sheet metal preforms.

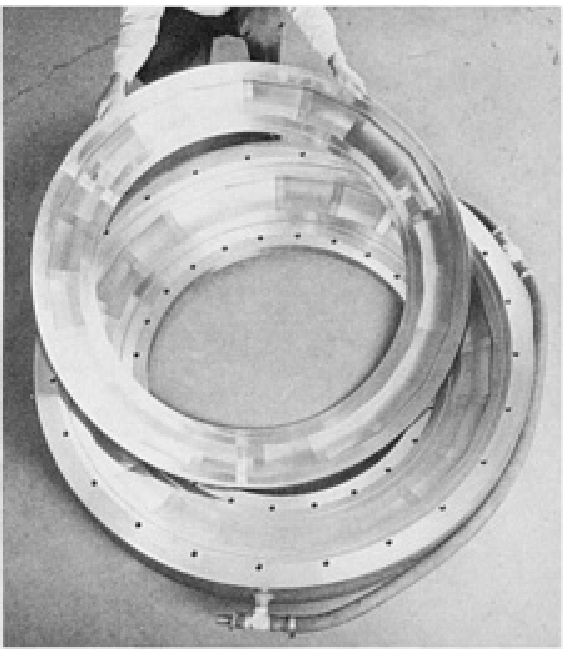

Fig. 12 Aircraft cooling coil that was explosively formed from a welded sheet metal preform. The finished part measures 152 × 457 × 457 mm (6 × 18 × 18 in.) and was fabricated from 1.57 mm (0.062 in.) thick aluminum. Courtesy of Explosive Fabricators, Inc.

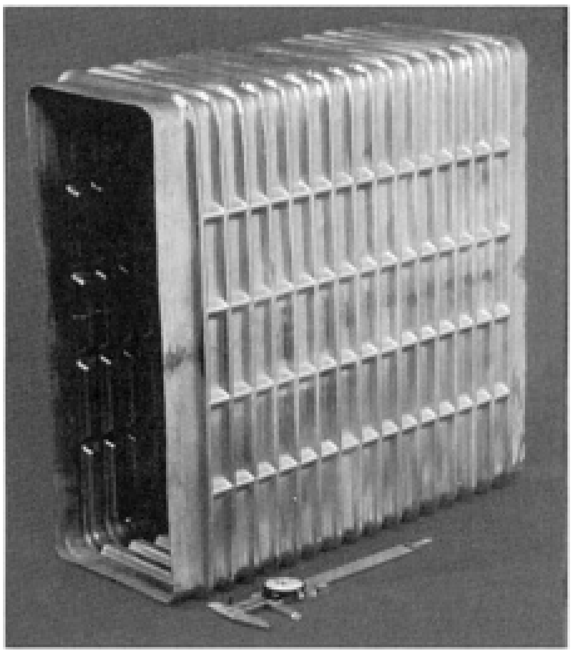

Fig. 13 Jet engine afterburner component, explosively formed from a welded sheet metal preform that has a 914 mm (36 in.) minor diameter and a 1015 mm (40 in.) major diameter. The 0.89 mm (0.035 in.) thick Waspalloy workpiece utilized the 4340 steel die, on which it rests, in its fabrication. Courtesy of Explosive Fabricators, Inc.

Tooling Considerations. The size of part may limit the use of split dies and may limit the process to applications in which natural draft will permit removal of the part from the die. No other special considerations are necessary for tools except for the forming of thin parts that may wrinkle when a vacuum is applied between the preform and the die.

Tolerances for parts that are explosive formed from welded sheet can be held to ±0.25 mm (±0.010 in.), although a more practical tolerance of ±0.81 mm (±0.032 in.) is normally specified. The higher forces that would be necessary to meet closer tolerances would shorten die life and increase die costs for a given production run. Close tolerances also require a higher degree of weld finishing prior to forming.

Placement of Explosive Charges. The type and placement of explosive charges in forming welded sheet assemblies depend on the final shape of the part desired. Line charges positioned at the axis of the assembly are used for long right-cylindrical shapes. With a cone-shaped part, a point charge located near the base of the cone may be preferable. Generally, the same type of explosive system is used in all forming operations regardless of blank construction.

Economics of Forming Welded Assemblies. The economic advantages that may be realized in the explosive forming of welded sheet metal assemblies depend on the complexity of the part design and the number and type of operations needed in conventional fabrication. In addition, the characteristics of the work metal, such as ease of forming, welding, and machining, will influence the comparison. In one case, the explosive forming of a 787 mm (31 in.) long aircraft skin section having 305 and 127 mm (12 and 5 in.) diam ends and made from two aluminum alloys (6061 and 5086) resulted in a savings of 38% per piece over the conventional bulging method, while a savings of only 12% per piece was realized using the conventional shear-spinning method instead of the conventional bulging forming method.

Explosive Forming of Heated Blanks

Refractory metals such as tungsten are explosive formed with better results when the blanks are heated. The arrangement shown in Fig. 14 was used to form a dome from tungsten sheet that was heated to 675 °C (1250 °F). Molten aluminum was used as the transmission medium. The explosive, a 0.012 kg (0.026 lb) charge of composition C-4 plastic demolition explosive (detonation velocity: 8050 m/s, or 26,400 ft/s; supplier: U.S. Government) was protected by an insulated tube to avoid premature explosion. The die was constructed from 4130 steel. A dome 114 mm (4.5 in.) in diameter by 25 mm (1 in.) high was formed within a tolerance of ±0.076 mm (±0.003 in.) in one shot.

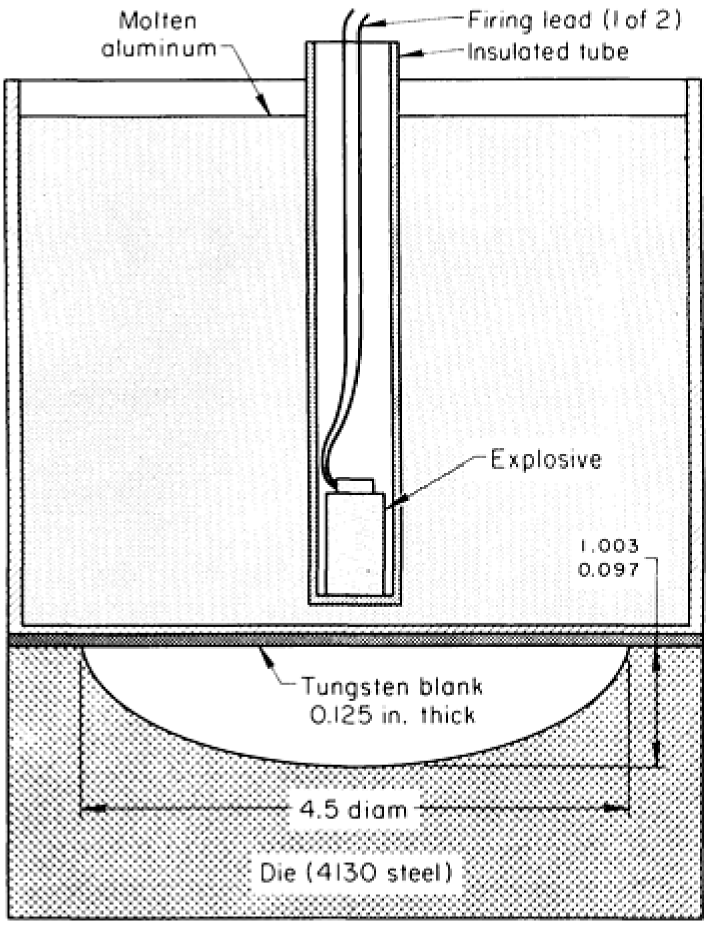

Fig. 14 Use of molten aluminum as a medium in the elevated-temperature explosive forming of tungsten. Dimensions given in inches.

Formability

In normal explosive-forming operations, the major characteristics of the work metal that determine formability are ductility and toughness. It is general practice not to exceed the elongation, as determined by tension testing, in forming a part from the same metal. Toughness criteria cannot be as readily applied, because forming represents biaxial and triaxial stressing, as compared with uniaxial stressing in the tension test. In addition, the design of tooling can influence the apparent formability of a material.

Comparisons of formability of various materials by explosive forming are subject to the particular experimental design under which they are tested. As a result, absolute values of formability are not obtained, but relative behavior for use in other explosive forming operations can be established. A comparison of the formability of some metals, using annealed aluminum alloy 1100 as a basis, is shown in Fig. 15. The apparent formabilities shown can be increased through modified tooling design in other operations. Increasing the forming temperature will also provide obvious forming advantages.

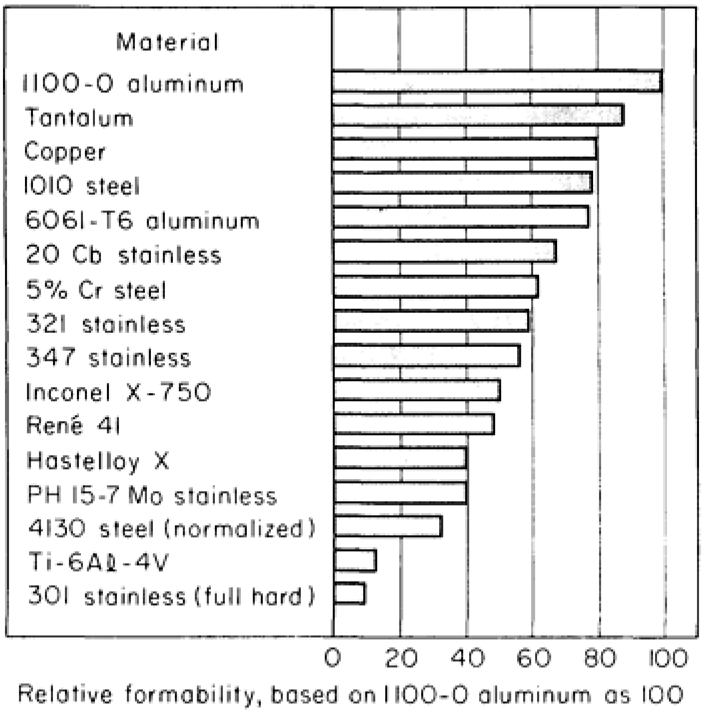

Fig. 15 Formability of various metals by explosive forming relative to that of aluminum alloy 1100-O. The comparison is based on the forming of 0.81 mm (0.032 in.) thick material with explosive at 381 mm (15 in.) standoff distance. All metals were annealed unless otherwise indicated.

Explosive Forming

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Safety

In and around an explosive-forming facility, any fire- or spark-producing equipment must be eliminated. Precautions should include the collection of all matches and lighters from any person entering any building where explosives are present. During operations, only persons absolutely necessary for carrying out the operations should be present.

Misfires during an operation are extremely hazardous and time consuming. Precautions should be taken to ensure that an electrical short circuit does not occur during the immersion of the setup for firing in the water tank. Incorrect placement or improper size of the blasting cap may cause failure of the charge to explode when the cap detonates.

Precautions to be taken if the firing circuit has been energized and the charge does not go off include a check of the continuity of the circuit. If the circuit is good, another attempt should be made to fire the charge. If this fails, the firing circuit should be disconnected from the power source and the power source tested for proper output. If no failure is found in the power source, the lead wires in the firing circuit can be inspected visually from a distance to determine if any shorts have occurred. Under no circumstances should the charge be brought to the surface for examination until 15 min has elapsed from the last time attempts were made to fire the charge. All personnel should leave the area during this waiting period. After the specified time has elapsed, the charge should be brought to the surface and a new cap installed. When defective blasting caps are found, they should be destroyed with any other scrap explosive at the close of operations for each working day.

Explosive Forming

Revised by A.E. Doherty, Explosive Fabricators, Inc.

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2. J.S. Rinehart and J. Pearson, *Explosive Working of Metals,* Macmillan, 1963, p 19-20, 263-266
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Electromagnetic Forming

Revised by Michael M. Plum, Maxwell Laboratories, Inc.

Introduction

ELECTROMAGNETIC FORMING (EMF) is an assembly technique that is widely used to both join and shape metals and other materials with precision and rapidity, and without the heat effects and tool marks associated with other techniques. Also known as magnetic pulse forming, the EMF process uses the direct application of a pressure created in an intense, transient magnetic field. Without mechanical contact, a metal workpiece is formed by the passage of a pulse of electric current through a forming coil.

The parameters that determine the applicability of the EMF process are:

- Forming can be accomplished through a nonmetallic coating or container because the magnetic field passes through electrical nonconductors
- Most of the forming takes place after the pressure impulse has ended, in contrast to most metal-forming processes. The metal is rapidly accelerated, gaining a large amount of kinetic energy by moving only a short distance during the impulse. This kinetic energy subsequently does the actual work of forming
- The metals that are most efficiently formed by EMF are those with relatively high electrical conductivity, such as copper, aluminum, low-carbon steel, brass, and molybdenum. Metals with lower conductivity, such as stainless steel, can be formed by using either very high energy or an intermediate, highly conductive "driver"
- The ratio of the masses of pieces used in assembly operations may be much more significant than their relative mechanical strength or elastic properties. Because EMF does not use static forces, relatively light structures can be used to support the dies
- No torque is applied to the workpiece in swaging and expanding operations, in contrast to spinning and rolling. Because the magnetic field behaves much like a compressed gas, it exerts a uniform pressure that is relatively independent of variations in spacing between the workpiece and the forming coil
- No lubricant is required because the contact between the magnetic field and the workpiece is frictionless
- The peak pressure is limited (by the strength of the forming-coil material) to much lower values than are

commonly encountered in shearing, punching, and upsetting operations. However, the pressure that can be applied by the magnetic pulse can be very high compared to the average pressure in mechanical forming
- The process, being purely electromagnetic, is not limited to repetition rate by the mechanical inertia of moving parts. The timing of the magnetic impulse can be synchronized with microsecond precision, and machines can be made to function at repetition rates of hundreds of operations per minute. The strength of the magnetic impulse can be controlled electrically with high precision

The major application of EMF is the single-step assembly of metal parts to each other or to other components, although it is also used to shape metal parts. Within the transportation industry, for example, one automotive producer assembles aluminum driveshafts without welding to save a significant amount of weight in light trucks and vans to meet requirements for reduced energy consumption. Using the EMF process allows the joining of an impact-extruded aluminum yoke to a seamless tube without creating the heat-affected zone associated with welding. Numerous other uses of the EMF process are described in the section "Applications" in this article.

Electromagnetic Forming

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Process Description

In its simplest form, the EMF process uses a capacitor bank, a forming coil, a field shaper, and an electrically conductive workpiece to create intense magnetic fields that are used to do useful work. This very intense magnetic field, produced by the discharge of a bank of capacitors into a forming coil, lasts only a few microseconds. The resulting eddy currents that are induced in a conductive workpiece that is placed close to the coil then interact with the magnetic field to cause mutual repulsion between the workpiece and the forming coil. The force of this repulsion is sufficient to stress the work metal beyond its yield strength, resulting in a permanent deformation.

Speed of Forming. The conductivity of the workpiece and the eddy currents which interact with the magnetic field of the coil result in a net pressure on the surface of the workpiece. As the workpiece surface moves inward under the influence of this pressure, it absorbs energy from the magnetic field. To apply most of this available energy to forming, and to reduce energy loss due to permeation of the workpiece material (which wastes energy by resistance heating), the forming pulse is kept short. In most forming applications, pulses have a duration of between 10 and 100 μs. The significance of pulse frequency is described in the section "Electrical Principles" in this article.

The basic circuit used for electromagnetic compression forming of a tubular workpiece consists of a forming coil, an energy-storage capacitor, switches, and a power supply of nearly constant current to charge the capacitor, as shown in Fig. 1. Figure 1(a) shows the flux-density pattern of the magnetic field produced by discharging the capacitor through the forming coil in the absence of an electrically conductive workpiece. The evenly spaced flux lines indicate a uniform flux density within the coil. Figure 1(b) shows the change in field pattern that results when the capacitor is discharged through a forming coil in which a tubular workpiece of highly conductive metal has been inserted. The magnetic field does not penetrate the workpiece and is intensified by confinement in the small annular space between the coil and the workpiece (as depicted by more closely spaced flux lines).

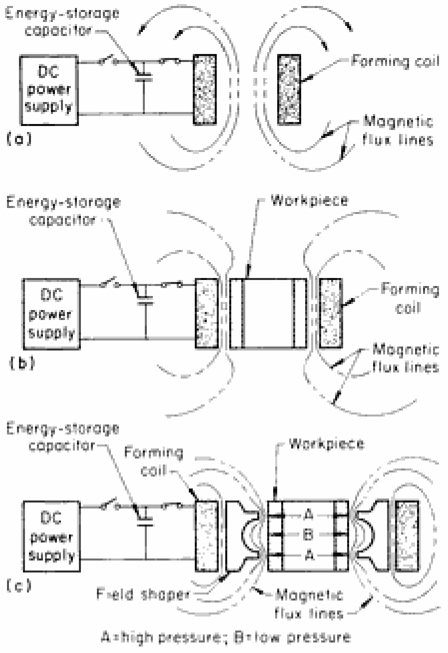

Fig. 1 Basic circuit and magnetic field patterns for electromagnetic compression forming of a tubular workpiece. (a) Field pattern in absence of workpiece. (b) Field pattern with workpiece in forming coil. (c) Field pattern when field shaper is used.

Field shapers, which are massive current-carrying conductors that are inductively coupled to the forming coil, are used to concentrate the magnetic field at the point at which forming is desired. Figure 1(c) illustrates the use of a field shaper to localize the magnetic pressure in certain regions of the workpiece. This technique most efficiently uses stored energy to produce high local forming pressures in desired areas. Field shapers also allow the use of a standard forming coil for a variety of applications. The field shaper, which is simpler to make, can be tailored to the specific part to be formed.

In the following example, a field shaper was used to concentrate the forming force where it was needed, and to limit the force in fragile areas of the workpiece. Thus, the forming pressure along the length of the workpiece was easily controlled.

Example 1: Use of a Field Shaper to Form a Stator Housing.

The housing for the stator assembly of an electric motor was formed in place, as shown in Fig. 2. A steel blank (the workpiece) was compressed onto the stacked laminations and shaped to conform to the supporting mandrel in a single EMF operation. The laminations were bound rigidly in place without the use of rivets or bolts; simultaneously, grooves (for mounting end bell housings) were formed precisely concentric with the inner surface of the laminations.

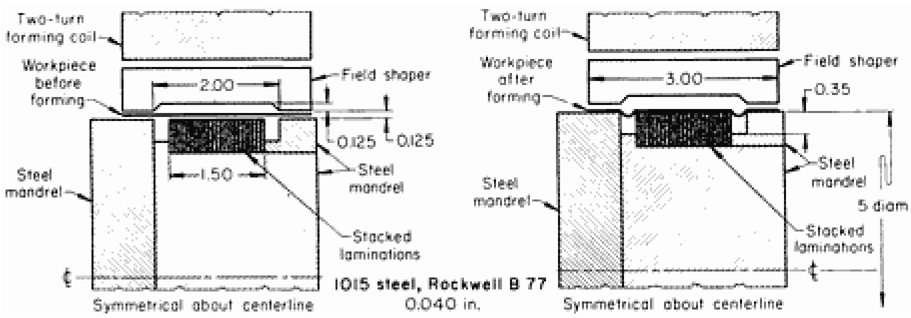

Fig. 2 Setup for EMF of a steel housing around stacked stator laminations, using a field shaper. Dimensions

given in inches.

About 40 kJ (29,500 ft · lbf) of energy was needed, because of the high pressure required for forming the grooves. Because such a high pressure would have deformed the laminations, a field shaper was built into the coil. The pressure along the length of the workpiece in this arrangement varied approximately in inverse proportion to the square of the spacing.

The assembly shown in Fig. 2 was produced with this experimental tooling at a rate of 240 per hour, using manual loading and unloading. The forming was done in a 48 kJ (35,400 ft · lbf) machine.

Forming Methods. Electromagnetic forming can usually be applied to three forming methods: compression, expansion, and contour forming. As shown in Fig. 3(a), a tubular workpiece is compressed by an external coil, usually against a grooved or suitably contoured insert, plug, tube, or fitting inside the workpiece. A tubular workpiece is expanded by an internal coil, as shown in Fig. 3(b), usually against a collar or other component surrounding the workpiece. Flat stock is almost always contour-formed against a die, as indicated in Fig. 3(c).

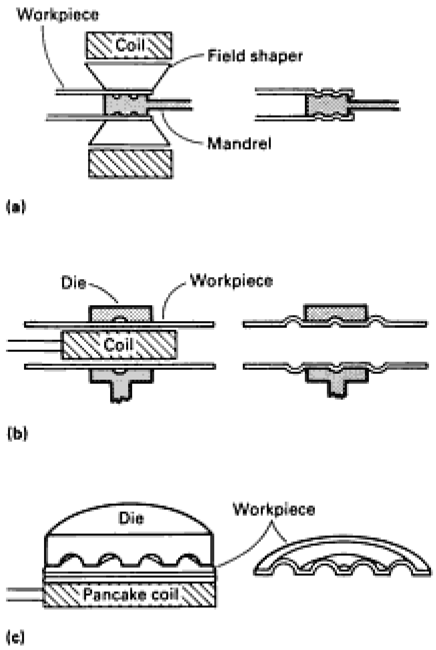

Fig. 3 Three basic methods of EMF. (a) Compression. (b) Expansion. (c) Contour forming.

In special circumstances, such as access problems, it is possible to achieve EMF by attracting metal toward a coil via a special pulse waveform (Ref 1). However, this process is not nearly as efficient as the three forming methods described above.

Workpiece Design. In addition to acting as an electrical conductor, the workpiece must provide a continuous electrical path. The current in a tubular workpiece flows around its circumference. Therefore, if a tubular workpiece were slit through its length, as shown in Fig. 4(a), the resulting interference with the current flow would eliminate the forming forces.

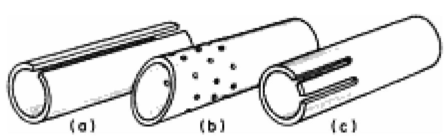

Fig. 4 Design aspects of tubular workpieces that affect applicability of EMF. (a) Full-length slit. (b) Perforations and angular cut. (c) Partial-length slots.

Figure 4(b) shows a tubular workpiece containing perforations as well as a cut at one end that is at an angle with the axis. Such minor irregularities do not seriously interfere with current flow and are acceptable under many conditions. However, deep slots in the end of a tube, such as those shown in Fig. 4(c), interfere with the current flow in such a way as to produce uneven pressure on the workpiece.

Applicability. In compression-forming applications, the pressure requirement is dependent upon the hoop strength of the workpiece. A formula used to determine applicability of the process to a given workpiece:

$$\frac{(YS)(2t)}{OD} = H_y \qquad (H_y)(N) = P_m$$

where *YS* is the yield strength of the material, *t* is the wall thickness of the workpiece, *OD* is the outside diameter, and H_y is the pressure to yield the hoop. Because of factors such as inertia effects, the increase in yield strength that can occur at high strain rates, and the geometry of the part, the magnetic pressure, P_m, required may be a factor, *N,* of two to ten times the pressure required to overcome the static yield strength of the material.

For example, to form a 50 mm (2 in.) OD tube of 6061-T6 aluminum with a wall thickness of 1.3 mm (0.050 in.), the approximate magnetic pressure required is:

$$\frac{(276 \text{ MPa})(2 \cdot 1.3 \text{ mm})}{50 \text{ mm}} = 14 \text{ MPa}$$

or

$$\frac{(40 \text{ ksi})(2 \cdot 0.050 \text{ in.})}{2.0 \text{ in.}} = 2 \text{ ksi}$$

For an *N* of 10, the magnetic pressure required would be 140 MPa (20 ksi). However, the EMF process is capable of applying 340 MPa (50 ksi) in compression when using standard work coils. Therefore, EMF is generally applicable to electrically conductive workpieces that require a magnetic pressure of less than 340 MPa (50 ksi).

Reference cited in this section

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Advantages and Limitations

The EMF process offers several advantages over other forming techniques, but has some limitations.

Repeatability. Because EMF is an electrical process, its energy outputs are nearly infinitely adjustable. The electromagnetic pulse can be controlled to the degree that it affords repeatability of current, and therefore pressure, to within one-half of 1% of the output setting. As a result, the forming is very highly repeatable. In fact, once the equipment has been set up for a particular forming operation, the only forming variable is the material. Operation of the equipment requires only a signal to activate the charging and automatic firing of the capacitor bank. That signal can be given by a microprocessor, as in the case of automated systems, or by an operator pressing palm buttons in a manual operation.

Noncontact. Unlike other mechanical processes in which a tool contacts a workpiece, in EMF the magnetic field that applies the pressure requires no lubrication, leaves no tool marks, and therefore requires no cleanup after forming. Materials that have previously been anodized or plated or have had some other surface preparation can therefore be formed without materially affecting the surface conditions.

One exception that does require lubrication is when the workpiece is driven against a mandrel and then removed. Although the workpiece itself is touched only by the magnetic field, the mechanical interface between the workpiece and the mandrel will often require some type of lubricant to facilitate removal of the workpiece.

Springback. In the process of forming, the material is loaded into its plastic region, resulting in permanent deformation, so that the springback often associated with mechanical processes is virtually eliminated. Because there is no mechanical contact, there is no mechanical stress introduced during forming other than work hardening.

Strength. Joints made by the EMF process are typically stronger than the parent material. This result is not unique to EMF. Compression of a tube will normally result in a wall thickening, which adds to the strength of the joint. Because the forming takes place in a matter of a few microseconds, the high strain rate forming does not affect the material properties in an adverse way.

The speed of joining or forming is limited only by the time required to load and unload the workpiece. Equipment has been designed and is operating in full production at rates of three times per second. No apparent limitations exist to achieving rates several times faster.

Ductility Effect. The EMF process allows increased ductility (that is, formability) for certain aluminum alloys because of the lack of mechanical stress and friction normally encountered with mechanical processes. It has also been demonstrated that parts typically requiring interstage annealing in other mechanical forming processes can be formed in one EMF operation.

Tooling for the process is relatively inexpensive. The machine and the work coils can be viewed as general-purpose tooling. Field shapers are used to couple individual workpieces to the coil. In the case of expansion forming, external split dies are used. If evidence of the parting line on the workpiece is aesthetically unacceptable, tolerances at the split of such dies must be very tight. Such dies are often made of nonconductive material to avoid any potential of inducing currents, as in a steel die, thereby causing electrical arcing between the die halves. In contrast to conventional flat forming, only a female die is used when forming sheet stock by the EMF process.

General Limitations. The speed of joining or forming also represents one of the limitations of the process. Because forming takes place in such a short period, the material does not have an opportunity to stretch; therefore, the process does not lend itself to deep drawing of materials. The process is also limited to those materials that are electrically conductive. Materials with an electrical resistivity of 0.15 $\mu\Omega \cdot$ m or less are ideal candidates for the process. Included in this group are such materials as copper, aluminum, brass, and mild steels. More highly resistant materials can be formed using special EMF equipment that operates at frequencies in the range of 20 to 100 kHz. Such equipment tends to be physically larger and more expensive than systems in general use. One example of such equipment was built for a U.S. Department of Energy contractor to weld end closures of nuclear fuel pins.

Pressure Limit. The maximum pressure that can be applied by standard compression coils is approximately 340 MPa (50 ksi). Thus, the process is restricted to relatively thin-wall tube or sheet products, unless special, strong coils are constructed.

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Production Methods

The EMF technique is suitable for both manual-feed and automated production processes.

Manual-Feed Equipment. With manual feed, the production rate is limited by the speed at which an operator can load and unload parts. To speed up production, insulating fixtures (into which the workpieces are inserted) are typically mounted in the coil or field shaper. For assembly operations, the fixture situates the workpieces in the proper position with respect to one another as well as to the coil or field shaper. Typically, pushing a palm button initiates the charge-and-fire cycle.

A variety of holding or clamping devices are used to secure the workpiece during forming. Production rates of 600 to 1200 assemblies per hour are typical of this type of equipment, shown in Fig. 5.

Fig. 5 Semiautomatic EMF system for assembly of automotive cruise control power units.

Fully Automated Equipment. Special materials-handling equipment is required for those applications whose production rates exceed manual-loading capabilities. High repetition rates can be attained by property matching the characteristics of the power source, the energy-storage unit, the switching components, the forming coil, and the workpiece. A rate of 12,000 operations per hour has been demonstrated in production; faster rates are limited primarily by the capability of the loading equipment.

In one high-speed application, the flame height control orifice of a disposable cigarette lighter is assembled at a rate of three per second. More than a dozen machines installed worldwide produce more than 400 million parts annually. The hollow, caplike aluminum orifice, with a 3.18 mm (0.125 in.) diameter and a 0.89 mm (0.035 in.) wall thickness, is formed on the end of the fuel wick, as shown in Fig. 6. Assemblies are individually fed into the work coil by high-speed handling equipment.

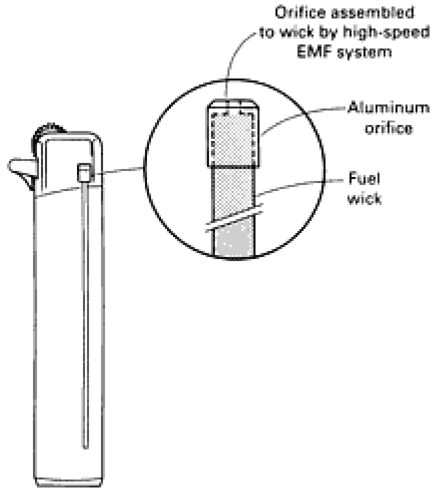

Fig. 6 Disposable cigarette lighter component assembled by EMF.

Electromagnetic Forming

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Applications

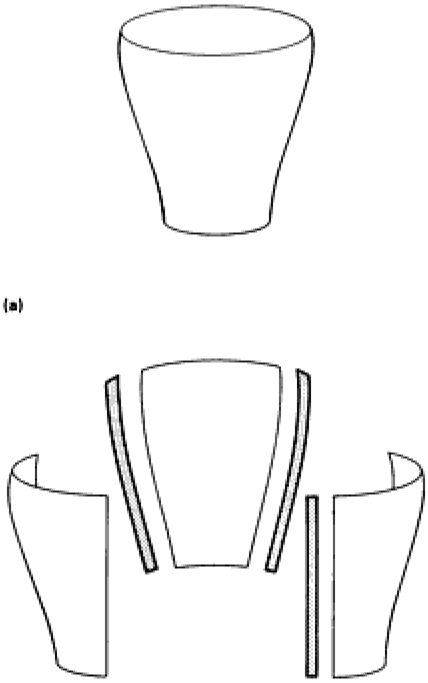
Electromagnetic forming is chiefly used to expand, compress, or form tubular shapes. It is occasionally used to form flat sheet, and it is often used to combine several forming and assembly operations into a single step.

In the automotive industry, EMF is used by all three major U.S. manufacturers and several foreign producers to assemble components such as air conditioner accumulators, high-pressure hoses, shock absorber dust covers, rubber boots on constant velocity (CV) joints, oil cooler heat exchangers, steering wheels, gasoline fill tubes, and accessory motor packages.

Universal joint yokes, drive linkages, cams, gears, and various other linkages or fittings are assembled to drive shafts or torque tubes by EMF. In this type of application, a torque joint, tube, or hollow shaft is compressed onto a fitting inside the tube. Splines, pockets, or knurl configurations can be used in the fittings to provide torque resistance, depending on torque strength requirements, types of materials, and dimensions.

In the manufacture of electrical equipment, EMF is used to join components of high-voltage fuses, insulators, back-shell connectors, and lighting fixtures; heavy-duty electrical connections are made by swaging a terminal sleeve over a conductor cable.

Aircraft applications include engine nacelles (Fig. 7), torque shaft assemblies, control rods and linkages, the forming and assembly of cooling system ducts, and the sizing of tubing. The EMF process is also used in the production of TOW, MLRS, and Viper missiles; helicopter rocket launchers; and in the swaging of preformed rotating bands on ordnance projectiles. In addition, manufacturers of appliances, consumer products, computers, and other products use the technique. In nuclear work, fuel rod sheaths are compressed, fuel pins are welded, and sealing operations are performed in remote, shielded areas.



(a)

(b)

Fig. 7 Aircraft Nacelle Program: EMF (one-piece) versus conventional (multiple-piece) production. (a) EMF production characterized by: one form die, one trim tool, lighter weight, reduced assembly time. Tool cost: 340 h. Production time: 14 h. (b) Conventional production characterized by: six form tools, six trim tools, use of splice plates, additional assembly time. Tool cost: 880 h. Production time: 24 h. Savings for EMF method: 41%.

Axially loaded joints made by EMF are used in some aircraft applications and in actuator rods, where it is important to avoid excess weight. For long life under exposure to high stress and vibration, design of these joints is critical.

These assemblies are made by swaging the end of a tube into circumferential grooves in the second tube or fitting, as shown in Fig. 8. Close tolerances and close fit on the parts to be joined are not necessary. Because a loose slip fit is usually satisfactory, clearance of as much as 9.5 mm (3/8 in.) has been used.

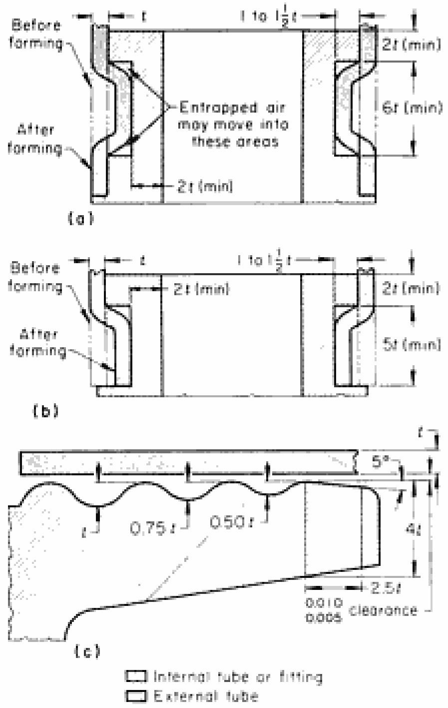


Fig. 8 Three types of axially loaded joints made by EMF. (a) Design for lightly stressed assembly. (b) Alternative design for lightly stressed assembly. (c) Design for highly stressed assembly. Recommended dimensional limits given in inches.

One arrangement that is suitable for joining lightly stressed members is shown in Fig. 8(a). A second arrangement, shown in Fig. 8(b), is equally satisfactory and requires considerably less energy for forming.

The joint contour shown in Fig. 8(c) is recommended for highly stressed, axially loaded assemblies. Three grooves with generous radii are used to avoid stress concentration. The grooves become progressively deeper toward the end of the external tube. Tests indicate a better distribution of stress on all grooves under axial load as a result of the variation in groove depth. Tests also indicate much less tendency for the end fitting to move relative to the tube, up to the yield point of the unformed tube section. This is important to members subjected to repeated reversals of load. Joints are typically made in which the unformed section of tube fails without detectable relative joint movement. When the tube must be highly stressed in service, it is preferable that the fitting (inside part) be of higher yield strength than the tube.

The fitting must be designed with proper cross section or must be adequately supported internally, so that this member does not go through yield; therefore, the difference in springback after forming will contribute to tightness of the joint. It may be desirable to reduce wall thickness of the fitting to ensure deflection and springback, leaving the tube with residual hoop stress.

On large parts with appreciable groove volume, it may be necessary to draw a vacuum on the inside of the tubular component to remove air from the groove area. Otherwise, air can be entrapped and can offer considerable resistance to the high-velocity forming. A simpler way to deal with this problem, when design permits, is to provide extra volume in the form of square corners in the groove, as shown in Fig. 8(a) and 8(b).

Torque joints assembled by EMF are preferred to welded joints in some applications, for at least three reasons: the absence of scale and residual flux; the ease of inspection for quality; and the absence of heat distortion.

The spline joint is best suited for torque joints using tubes with heavy wall thickness. Joints made by driving the tube into pockets machined parallel to the axis of the fitting are used with thin-wall tubes. For low torque requirements, coarse knurling of the fitting before assembling to the tube is satisfactory and economical. The use of specially designed field shapers and coils, as in the example in the following section, enables efficient high-speed production of torque joints.

Automotive Driveshafts. A major high-production application of EMF has been the assembly of aluminum driveshafts (torque tubes) used on minivans, trucks, vans, and recreational vehicles of one U.S. manufacturer. By significantly reducing the mass, a single shaft can replace two-piece units and a center bearing assembly. Electromagnetic forming was chosen because it provided a better torque-carrying capability than did a welded joint of the same diameter. The EMF process creates no heat-affected zone in the aluminum and can be used with alloys not considered to be weldable.

The initial application used a tube of 6063-T832 alloy with a 75 mm (3 in.) inside diameter and a 1.9 mm (0.076 in.) thick wall. The yoke is a multisplined impact extrusion. An additional driver ring is used over the tube and is formed with the tube onto the splines. The 6061-O ring, with a 2.64 mm (0.104 in.) wall, provides additional joint strength and receives the balancing weights (see Fig. 9). By welding weights to the ring, the mechanical integrity of the torque tube is not affected. Tubes are fabricated in various lengths and diameters. All are assembled on semiautomated EMF machines with a maximum capacity of 60 kJ (44,300 ft · lbf).

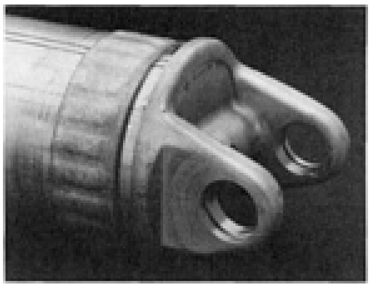


Fig. 9 Aluminum driveshaft assembled by EMF.

Electromagnetic Hammers and Riveters. In addition to contour forming against a die, flat coils are used in two unique aerospace applications: as an electromagnetic hammer and as an electromagnetic riveter. The hammer is used to flatten surfaces on the aluminum skin of the central fuel tank of the space shuttle, while the riveter is used in the assembly of 747 jet aircraft wings.

An electromagnetic hammer exerts momentary high pressure over a small area of a metal workpiece. In contrast to mechanical hammers, it requires no dynamic material contact with the workpiece and consequently produces almost no change in the metal grain structure.

In one version of the hammer, designed for use on an aluminum tank, the currents generate pressures up to 35 MPa (5 ksi), which can deform the aluminum in as short a time as 100 μs. The repulsive force also acts on the coil, causing it to kick back. The coil is therefore mounted in a holder that allows for this motion and makes it possible to reposition the coil simply by sliding it back toward the workpiece. Figure 10 shows a hand-held electromagnetic hammer.

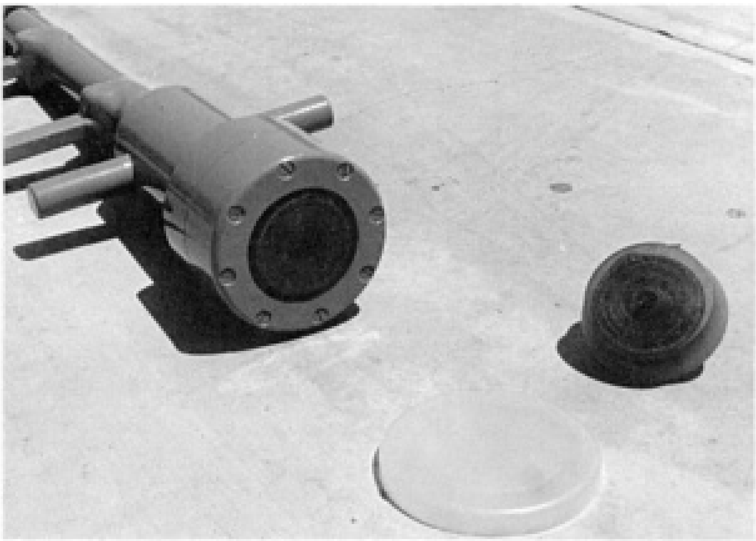


Fig. 10 Hand-held electromagnetic hammer.

In another application, flat coils are used as part of an electromagnetic riveting (EMR) system to drive a mechanical ram to upset rivets. Electromagnetic riveting is a proved method for installing rivets of various materials to precision interference profiles for fluid-tight and fatigue-critical applications. The process makes precision installations without massive, stationary equipment.

The high-velocity single-impact riveting process converts electromagnetic energy into mechanical kinetic energy to drive rivets. The EMR equipment consists of a power supply and two opposed guns with special power transmission cables connecting the guns and the power supply. Compared to large automatic hydraulic riveting machines, the EMR rivet quality is higher; yet the system is substantially lower in initial procurement costs. It features a quieter operation and reduced floor space requirements. Even when compared to conventional hand gun driving, EMR is far superior in quality and cost performance, and noise level is reduced by several orders of magnitude. Standard, available rivets are used in the EMR process, and rivet installations are uniform and repeatable, with an extremely low rejection rate.

The process was initially developed for riveting the 747 aircraft wing panels, which were too large to fit on an automatic hydraulic riveting machine. The process proved so successful that a computer-controlled tool was developed to incorporate EMR into the 767 aircraft wing spar assembly.

The EMR process is considered generally adaptable to any structural design in which fluid-tight, fatigue-critical, or large-diameter fasteners are required. The EMR system has been mounted on a robot for automated assembly of automotive components.

Electromagnetic Welding. The EMF concept has been expanded to encompass impact welding, in addition to forming and mechanical joining. This refinement is referred to as pulsed magnetic (PM) welding and is in use in the manufacture of nuclear fuel pins.

High-frequency high-intensity pulsed magnetic fields are being used to produce fuel pin end closures on cladding of interest to a breeder reactor program. Solid-state welds between the fuel pin cladding and a tapered end plug insert are produced by a pulsed magnetic field that accelerates and collapses the cladding, causing it to exert high-velocity impact on the end plug insert.

The field is produced when capacitively stored energy is switched into a single-turn work coil. The resulting high velocity of the cladding produces an impact bond. The peak magnetic field is in the range of 50 T (500,000 gauss), which translates into pressures in excess of 689 MPa (100 ksi). These pressure loadings accelerate the cladding radially inward. Collapse velocities of 304.8 m/s 1000 ft/s have been measured.

The end plug design is an important parameter in the welding process. The insert is tapered to allow the impact region to self-clean and to relieve itself of the stress waves created by the impact. As the impact point moves along the tapered surface, the high-velocity impact causes plastic deformation at the region of impact. At the high strain rates that occur at impact, the cladding and end plug material behave like high-viscosity fluids. The result is that a small surface layer is ejected. This self-cleaning action results in an atomically clean surface, and welding is accomplished without going to a melt temperature. Bonds have routinely been observed with lengths five to ten times the cladding wall thickness.

Most of the work has been performed on type 316 stainless steel fuel pin cladding that was 20% cold worked with a 5.84 mm (0.230 in.) outside diameter and a 0.38 mm (0.015 in.) wall thickness. Other cladding materials were welded using identical conditions. These included Inconel 706, Nimonic PE 16, and RA 330, which were in the solution-annealed condition.

Combinations of Operations. Some parts that would require a number of separate conventional forming operations can be made in a single operation by EMF. For instance, simple tooling can be devised to combine blanking, piercing, or other cutting operations, and accomplish complex forming in one step. In the example that follows, a simple die was made to pierce two holes in an aluminum alloy tube, expand the tube, and form a 180° flange on one end of the tube, all in one EMF operation.

Example 2: One-Operation Forming and Piercing by EMF.

The tubular part shown at the right in Fig. 11 was produced in one EMF operation by using an expansion coil to form, flange, and pierce a length of tubing against a single-piece die (setup shown at the left and center in Fig. 11). The workpiece was a 100 mm (4 in.) length of aluminum alloy 6061-O tubing with an 83 mm (3¼ in.) outside diameter, 0.89 mm (0.035 in.) thick wall, and a yield strength of 50 MPa (7 ksi).

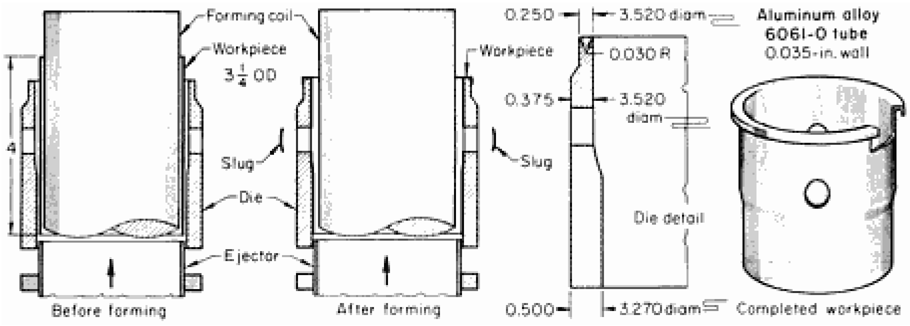


Fig. 11 Forming and piercing a tubular part in one operation. Dimensions given in inches.

The die was turned from 4340 steel tubing with a 12.7 mm (½ in.) thick wall, and two holes were drilled in the die to accomplish the piercing operation. The formed part was removed from the die by means of a simple ejector. The forming equipment had a capacity of 6 kJ (4400 ft · lbf), and cost approximately $18,000. The production rate was 240 pieces per hour, using manual loading and unloading.

Plastics, Composites, Rubber, and Ceramics. The material onto which a workpiece is formed is not a significant factor in the process except that it must be strong enough to withstand the impact of the workpiece. Therefore, assemblies using metal parts formed onto plastics, composites, rubber, and ceramics are common.

Automotive components such as electric fuel pumps, gasoline fill tubes, cruise control power units, and emission control devices use aluminum, usually anodized, over plastics. Wall thicknesses of 0.50 to 1.3 mm (0.020 to 0.050 in.) are assembled over plastics that include Delrin 100, glass-filled polyester, and polycarbonate. Fiberglass, carbon fiber, and other composite materials are joined with aluminum, copper, and brass in applications ranging from high-voltage fuses to drive shafts for trucks.

Banding of rubber boots on shock absorbers, CV joints, and the like was among the earliest applications. Surge arrestors, insulators, and other ceramic devices are assembled by EMF. Its potential application with new types of ceramics has not been fully explored.

Electromagnetic Forming

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Safety

The potential hazards of EMF operations may be categorized as either mechanical and sonic or electrical hazards.

Mechanical and Sonic Hazards. Electromagnetic forming equipment can cause substantial damage if its forces are misdirected, as would be expected of any equipment that exerts high pressure and delivers energy at a high rate. The magnetic pulse produces pressure perpendicular to the surface exposed to the field. Thus, if a cylindrical workpiece is positioned in such a way as to pass completely through a compression coil, only radial forces will be exerted on it during the forming operation. If, however, such a workpiece were inadvertently inserted only partly through the coil, the force produced by the magnetic pulse on the end could be sufficient to eject the workpiece from the coil at high velocity. In the same way, improperly positioned parts could be ejected from an expansion coil. An appropriate coil shield should be provided to protect personnel. Proper fixturing must be used to ensure that the machine will operate safely.

The sound produced in EMF operations results from the rapid movement of the workpiece compressing the air, and ordinarily is at a moderate level. If, however, the area of the workpiece that is moved in the air is large, and the distance moved is great, additional sound conditioning may be required. Production units are designed to comply with Occupational Safety and Health Administration (OSHA) requirements.

The possibility of an electrical arc in a coil or field shaper is a remote potential source of mechanical or sonic hazard. In the unlikely event of an electrical insulation failure, essentially all of the energy in the capacitor bank may be directed into an arc, generating high-intensity sound and perhaps forcibly ejecting bits of insulation or metal. A shield should be placed between the operator and the work coil to protect personnel. Protection against the sonic hazard depends on the distance between the operator and the coil. Care should be taken to prevent metallic chips from falling onto exposed parts of the field shaper gaps, where they could produce an arc-over. Insulators should be regularly checked for wear and cracks.

Electrical Hazards. Voltages used in typical EMF operations range up to 10 kV, and the capacitor banks can deliver extremely high current. Furthermore, voltages up to 50 kV are used for special applications, such as impact welding. All high-voltage components of the system are completely contained in a well-grounded heavy-gage metal cabinet. Doors and panels are electrically interlocked to avoid unauthorized access to high-voltage components. Maintenance personnel should work on energy-storage circuitry only after shorting the storage capacitors according to manufacturer's instructions. Maintenance personnel should also observe all standard precautions when working with the high-voltage components of the machine.

Pulsed voltages of the type that appear at the coil terminals of EMF equipment are much less dangerous than direct voltage. Normally, pulses of no more than 1000 V appear in any exposed portion of the equipment. The typical 10 kHz pulse appears for less than 1 μs. Nonetheless, precautions should be taken to prevent operators from being directly exposed to such voltages. The workpiece should never be held by hand because the possibility always exists that an arc-over between coil and workpiece could occur.

Coils are encased in metal shells that act as eddy current shields. As a result, the magnetic field is reduced to a very low level at any distance beyond the coil. Although the potential dangers may seem obvious, all personnel should be warned against touching or holding a coil, field shaper, or workpiece during the forming operation.

Electromagnetic Forming

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Equipment

The basic pieces of equipment in an EMF system are the capacitor bank, the coil, and the field shaper.

Capacitor banks are the energy-storage devices used for EMF. To charge the capacitor bank, alternating current is converted to high-voltage direct current by a suitable power supply or charging circuit. The charging rate must be fast enough to charge the capacitor bank in an interval not less than the desired repetition rate for the pulse discharge. For maximum energy transfer to the coil during the impulse, internal resistance and inductance in the power supply must be kept to a minimum.

Standard commercial EMF machines have capacitor bank energy sources rated from 8 to 200 kJ (5900 to 147,500 ft · lbf). Units with a capacity greater than 16 kJ (11,800 ft · lbf) are designed on a modular basis with increments of 4 kJ (3000 ft · lbf).

Because production rates with manual loading and unloading are generally about 200 to 600 pieces per hour, machines for this type of operation are designed for pulse repetition rates in this range. The faster pulse repetition rate desired for use with automatic work-handling equipment is readily obtained, however, as it is limited only by the characteristics of the charging circuit and the rate at which the components of the system dissipate heat.

Commonly available machines use capacitor banks rated at 8 to 16 kJ (5900 to 11,800 ft · lbf). Each capacitor is switched into a parallel-plate bus (or low-inductance coaxial cable) system through individual molybdenum anode ignitrons specially designed for pulse service. The capacitors are charged through a constant-current rectifier system.

The energy stored in the bank is precisely measured by a voltage-metering circuit. When the preset energy level is reached, the charging cycle is terminated and the switches (ignitrons) are triggered, either automatically or by an outside pulse, to discharge the capacitors into the forming coil. The storage systems are designed to have very low internal inductance, so that maximum energy is transferred to the forming coil during the impulse.

The maintenance of control circuits and charging circuits using solid-state components is comparable to that required in other industrial equipment of moderate to low complexity. The average life of a capacitor at a cycling rate of 600 operations per hour at maximum energy is about 3 million operations. At reduced energy levels, capacitor life is much longer.

Coils and Field Shapers. The three primary factors in the design of coils and field shapers are electrical characteristics, size, and strength. The theory of their design is extremely complex, and material and construction requirements are highly critical, because forming coils must accept the repetitive discharge of large amounts of electrical energy in pulses lasting only 10 to 100 μs and must generate uniform forming pressures as high as 340 MPa (50 ksi).

Using interchangeable field shapers with standard coils in compression operations promotes convenience, versatility, and economy. For long production runs, coils with fixed field shapers provide greater efficiency and durability. A variety of coils of standardized designs are available. Depending on the type and the application, coils can provide from a few hundred thousand to several million operations before repair or replacement is required.

In terms of size, the diameter of the section in the workpiece to be formed determines the diameter of the working surface of the field shaper, or of the coil when a shaper is not used. A clearance of about 1.3 mm (0.050 in.) per side is ordinarily needed for insulation and work insertion and removal. For maximum efficiency, the clearance is kept as small as possible because the effective force varies inversely with the square of the clearance distance.

In respect to strength, coils are usually of multiturn design, with sufficient mass to withstand repeated forming impacts. The coil not only must be strong enough to sustain these repeated thrusts, but also must be massive and rigid enough to minimize its deflection under load.

The problem of building coils to withstand high load is distinctly different in each of the three basic types of coils. Because there are no stringent volume restrictions on compression coils, they can be strong and massive, regardless of the size of the workpiece.

The coil receives support from a metallic coil body (usually of beryllium copper) and an insulating structure (see Fig. 12 and 13), which are needed for safe and efficient handling and for the efficient use of electrical energy.

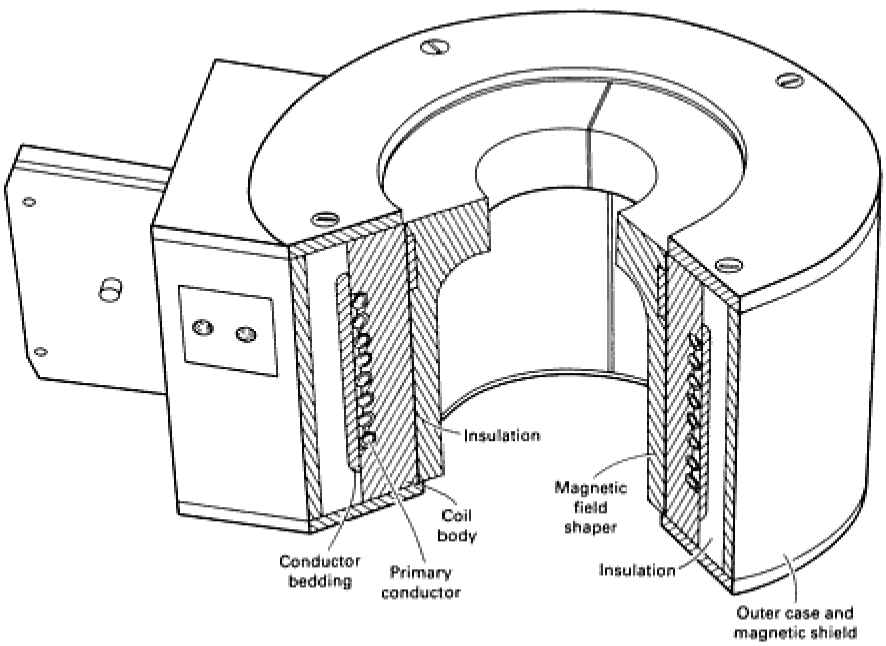

Fig. 12 Sectioned view of magnetic compression coil with removable magnetic field shaper.

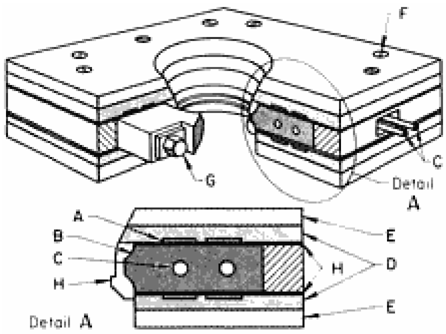

Fig. 13 Heavy duty wafer-type compression forming coil. A, primary conductor; B, beryllium copper field shaper; C, water passage; D, fiberglass insulation; E, steel backup plates; F, press bolt; G, shaper press bolt; H, shaper insulation.

Expansion coils must fit inside the workpiece, and thus the space to provide a strong structure is restricted. In small-diameter coils, shown in Fig. 3(b), the conductors must be relatively small in cross section to allow space for the return flux, and the conductors must be supported by an insulating mandrel. The force that can be exerted by these coils is ultimately limited by the strength of the mandrel. Production coils are typically 50 mm (2 in.) in diameter or greater.

Some flat spiral coils, shown in Fig. 3(c), also depend on the strength of an insulating backing to give the conductors support, so that the peak force they can exert depends mainly on the strength of the support. Other contour-forming coils have been made with massive construction; these have the same load limitations as do compression coils.

General-Purpose Compression Coil. Figure 12 shows a type of compression coil designed for ruggedness and versatility in applications on tubular parts up to 230 mm (9 in.) in diameter. Standard coils have inside diameters of 100, 150, or 250 mm (4, 6, or 10 in.) and have interchangeable field shapers of beryllium copper, each designed for a specific workpiece.

The flux concentration at the inner wall of the field shaper causes it to heat faster than the rest of the assembly. It is cooled by convection or by forced air circulation. In high-speed coils, the field shaper is sometimes water cooled. Heat generated in the coil body and primary conductor is removed by water circulating in the copper tubing of the primary conductor.

Heavy-Duty Compression Coils. Even stronger, more efficient construction is found in a heavy-duty single-turn coil and in the wafer-type coil shown in Fig. 13, for which the forming pressure is limited only by the properties of the shaper material. Wafer coils are used in energy ratings up to 60 kJ (44,300 ft · lbf) and pressure ratings up to 340 MPa (50 ksi).

Expansion Coil. Light-duty expansion coils, for use at pressures up to about 50 MPa (7 ksi), consist of solenoid coils of beryllium copper tubing wound on fiberglass forms. They are commonly up to 250 mm (10 in.) in active length and 100 mm (4 in.) in diameter, and are usually designed for the forming of specific workpieces. Coils that are 0.60 m (2 ft) long or more than 2 m (6 ft) in diameter have been built and applied successfully in the aerospace industry.

In the following example, a deep contour was formed in a hoop-shaped part by impact against a ring die, using an expansion coil.

Example 3: Electromagnetic Forming of a Grooved Hoop-Shaped Part.

As shown in Fig. 14, a grooved hoop-shaped part was formed by expanding a welded blank of 0.75 mm (0.030 in.) thick steel into a ring die, without using a field shaper. The weld bead was ground smooth before forming. Because of the high velocity of the blank when it struck the die, it conformed accurately to the die contour. About 40 kJ (3000 ft · lbf) of energy was used. The 4340 ring die (Fig. 14) was made by rolling, welding on lugs, bolting into a hoop shape, and then turning the inside contour in a lathe. Because the die was not subjected to static pressure, there was no need for the sturdy construction that would have been necessary had other forming methods been used. Using the EMF technique, it was merely necessary that the die be many times more massive than the workpiece.

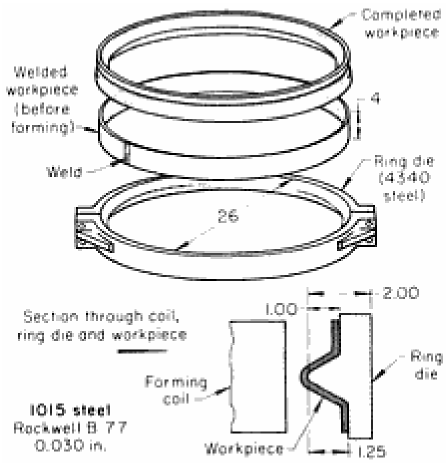

Fig. 14 Grooved hoop-shaped part. Dimensions given in inches.

Flat Spiral Coil. For contour forming of flat blanks, flat spiral coils apply essentially uniform pressure over a circular flat area, except for a small area in the center. It is difficult to achieve durability at peak pressures above 35 MPa (5 ksi) in such coils. The design of a flat spiral coil depends greatly on the requirements of the application. The electromagnetic forming of a contoured part from flat stock, using a flat spiral coil, is described in the example that follows.

Example 4: Electromagnetic Contour Forming of an Orifice From a Flat Blank.

The fluid flow constrictor shown in Fig. 15 was formed by the use of a flat coil in a setup like that shown in Fig. 3(c). The annealed 1010 steel blank, a flat annular disk with a 200 mm (8 in.) diameter, was laid on the coil under the die. The magnetic field generated pressure against the disk, driving it against the die.

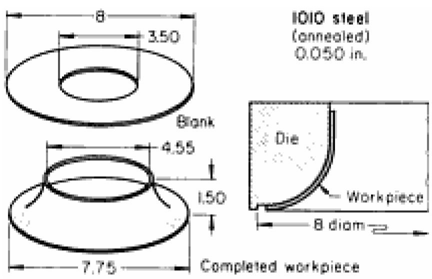

Fig. 15 Contoured part made from a flat blank by EMF. Dimensions given in inches.

The forming equipment had a rating of 12 kJ (8900 ft · lbf). An output of 10 kJ (7400 ft · lbf) was used in forming the orifice. The production rate was 240 pieces per hour. Because of its use as a flow constrictor, the 115 mm (4.55 in.) orifice diameter was accurately held. It was maintained without any trimming after forming. In cross section, the formed part had an exponentially curved contour.

Electromagnetic Forming

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Electrical Principles

Because of the short duration of the magnetic impulse in EMF, the pressure must be high enough to impart sufficient kinetic energy to the workpiece during the pulse to do the desired forming. Any resistance to the motion of the workpiece during the impulse reduces the amount of useful forming energy transferred. Accordingly, for high efficiency, the peak pressure should be several times that necessary to exceed the static yield strength of the workpiece and to overcome any other constraints for the duration of the impulse.

Pressure Waveform. Electrically, a forming coil is an inductor, along with a small amount of series resistance. When an energy storage capacitor is discharged through such a coil, the discharge is a momentary oscillating current. The frequency of this oscillation is often called the ringing frequency by electrical engineers. This oscillating current has a damped sine waveform. The frequency is inversely proportional to the square root of the product of the capacitance of the storage bank and the inductance of the coil. The rate of decrease of the amplitude of the pulse is proportional to the resistance of the coil circuit. The pressure produced by the coil is proportional to the square of this current; thus, the pressure pulse is essentially positive and approximates a damped sine square form, as illustrated in Fig. 16.

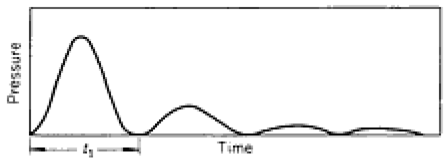

Fig. 16 Typical pressure waveform in EMF.

Virtually all of the forming energy is provided by the first wave; succeeding waves transmit less energy to the workpiece because of their lower energy content and because of the progressively widening gap between coil and workpiece as forming takes place. The portion of the energy of the electrical discharge that is not transferred to the workpiece as kinetic, or deformation, energy appears as resistance heating.

An energy discharge, or pulse, can be characterized by its peak pressure, which is that of the first wave, and by the duration of the first wave (Fig. 16). The time between successive waves changes slightly as the workpiece moves (changing the inductance of the coil) and as heating changes the resistance of the electrical circuit. However, these effects are ordinarily negligible.

Peak pressure is approximately related to other process variables. It is:

- Directly proportional to the energy of the electrical impulse from the capacitor bank
- Inversely proportional to the resistivity of workpiece and coil
- Inversely proportional to the total of the volume of the workpiece and the field shaper penetrated by the electromagnetic field (skin effect) and the volume between the coil and workpiece surfaces

The effect of workpiece size and electrical resistivity on peak pressure is discussed in the section "Typical Energy Relations" below. Wave duration for the first wave in a pressure pulse in the idealized situation is inversely proportional to the ringing frequency of the system:

$$t_1 = \left(\frac{1}{(2f)}\right)$$

Also, to a close approximation:

$$t_1 = k\left(\frac{Cd\ s}{l}\right)^{1/2}$$

where k is a proportionality constant, C is the capacitance of the energy storage bank, d is the diameter of the workpiece (for the surface adjacent to the coil), l is the working length of the workpiece (width of coil), and s is the thickness of the flux pattern between coil and workpiece (including skin depth of workpiece and field shaper). Thus, the wave duration increases with increasing capacitance of energy bank, diameter of workpiece nearest the coil, and spacing between coil and workpiece, but decreases with increasing length of workpiece. The skin depth in workpiece and field shaper increases with the increasing wave duration.

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Typical Energy Relations

The actual energy relations in the forming of aluminum alloy 6061-O, with a resistivity of 0.038 μΩ· m, and low-carbon steel, with a resistivity of 0.12 μΩ· m, are shown in Fig. 17. The data were obtained using a 12 kJ (8900 ft · lbf) machine operating at rated capacity with a standard wafer-type compression coil of the type shown in Fig. 13. In the forming operations described, the initial gap between field shaper and workpiece was 1.5 mm (0.060 in.).

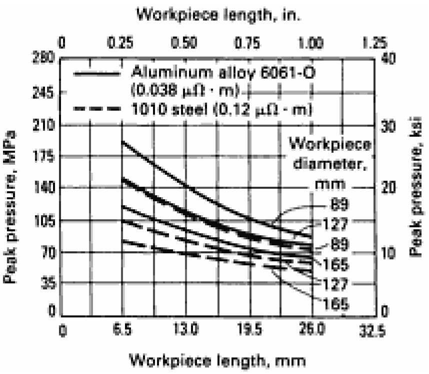

Fig. 17 Effect of workpiece dimensions and electrical resistivity on peak pressure.

The variation of peak forming pressure with workpiece length is shown for tubular workpieces 89 to 165 mm (3½ to 6½ in.) in diameter with a wall thickness greater than 0.75 mm (0.030 in.).

The effects of workpiece dimensions and resistivity on peak forming pressure or pulse height were qualitatively as predicted in the section "Electrical Principles" in this article. The pressure for a workpiece length of 25 mm (1 in.) was about half of that for a length of 6.4 mm (¼ in.); the pressure for a workpiece diameter of 165 mm (6½ in.) was about two-thirds of that for a diameter of 89 mm (3½ in.). Peak pressure was higher for work metal of lower resistivity: 20 to 50% higher for the 6061-O than for the low-carbon steel, with the percentage difference being greater for workpieces of larger diameter and shorter length.

Because the pressure is directly proportional to the energy, Fig. 17 is valid for other energy levels as well. Thus, if the 12 kJ (8900 ft · lbf) equipment produces a pressure of 95 MPa (14 ksi) on a workpiece 125 mm (5 in.) in diameter and 15 mm (0.6 in.) long, it will produce 70 MPa (10.5 ksi) if operated at a 9 kJ (6600 ft · lbf) level, or 50 MPa (7 ksi) at a 6 kJ (4400 ft · lbf) level.

The same relations apply for other combinations of coil and storage bank, as well as for other types of coils. Qualitatively they are similar; quantitatively they depend on the actual dimensions, design of the coil, and capacitance of the storage bank.

Although the length and diameter of the workpiece have a substantial effect on the peak forming pressure, they have only a slight effect on the length of the pressure pulse, which is about 50 to 60 μs. The reason is that coils of the type shown in Fig. 13, to which Fig. 17 refers, are designed to have a limited range of inductance regardless of the workpiece contours.

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Drop Hammer Forming

Introduction

DROP HAMMER FORMING is a process for producing shapes by the progressive deformation of sheet metal in matched dies under the repetitive blows of a gravity-drop hammer or a power-drop hammer. The configurations most commonly formed by the process include shallow, smoothly contoured double-curvature parts; shallow-beaded parts; and parts with irregular and comparatively deep recesses. Small quantities of cup-shaped and box-shaped parts, curved sections, and contoured flanged parts are also formed.

Advantages and Limitations. The main advantages of drop hammer forming are:

- Low cost for limited production
- Relatively low tooling costs
- Dies that can be cast from low-melting alloys and that are relatively simple to make
- Short delivery time of product because of simplicity of toolmaking
- The possibility of combining coining with forming

These advantages must be weighed against the following limitations:

- Probability of forming wrinkles
- Need for skilled, specially trained operators
- Restriction to relatively shallow parts with generous radii
- Restriction to relatively thin sheet (about 0.61 to 1.63 mm, or 0.024 to 0.064 in.; thicker sheet can be formed only if the parts are shallow and have generous radii)

Drop hammer forming is not a precision forming method; tolerances of less than 0.8 to 1.6 mm ($\frac{1}{32}$ to $\frac{1}{16}$ in.) are not practical. Nevertheless, the process is often used for sheet metal parts, such as aircraft components, that undergo frequent design changes or for which there is a short run expectancy.

Hammers for Forming

Gravity-drop hammers and power-drop hammers are comparable to a single-action press. However, they can be used to perform the work of a press equipped with double-action dies through the use of rubber pads, beads in the die surfaces, draw rings, and other auxiliary equipment.

Because they can be controlled more accurately and because their blows can be varied in intensity and speed, power-drop hammers, particularly the air-actuated types, have virtually replaced gravity-drop hammers. A typical air-drop hammer, equipped for drop hammer forming, is shown in Fig. 1.

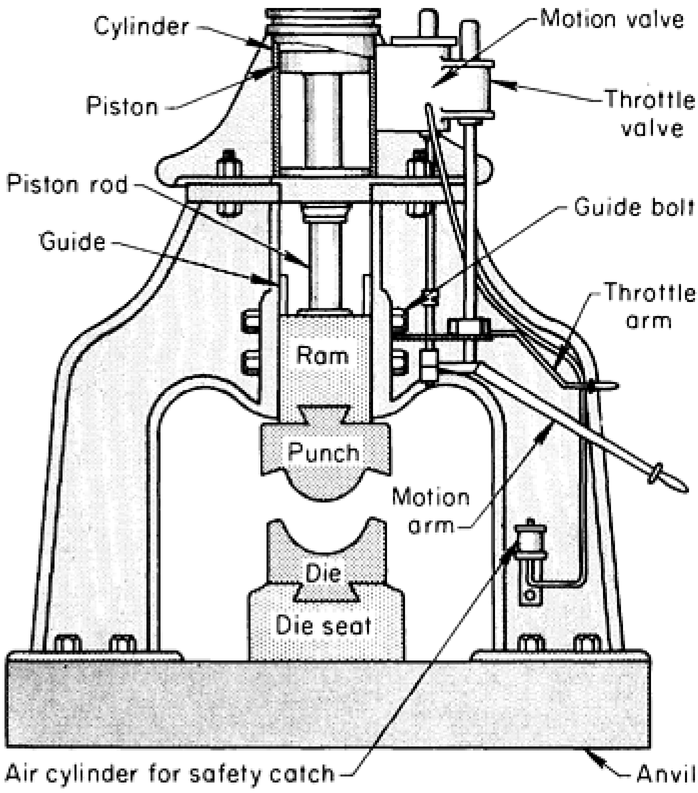

Fig. 1 Schematic of an air-actuated power-drop hammer equipped for drop hammer forming.

Power-drop hammers are rated from 4.5 to 155 kN (10M to 35,000 lbf), representing energies from 15 to 575 U (11,000 to 425,000 ft · lb). Air-drop hammers range in size (ram area) from 762 by 610 mm (30 by 24 in.) to 3.05 by 3.05 m (120 by 120 in.) with impact energies ranging from 8.9 to 134 kJ (6600 to 99,000 ft · lbf). Ram dimensions and other pertinent details concerning these hammers can be found in the article "Hammers and Presses for Forging" in this Volume.

Planishing hammers are used to supplement drop hammer forming. These are fast-operating air-driven or motor-driven machines that are generally used for low-production operations to form dual-curvature surfaces. They are also used to planish welds and to smooth out wrinkles or other imperfections in drawn or drop hammer formed parts.

Tooling

In general, a tool set consists of a die that conforms to the outside shape of the desired part and a punch that conforms to the inside contour (Fig. 1).

Tool Materials. Dies are cast from zinc alloy (Zn-4Al-3.5Cu-0.04Mg), aluminum alloy, beryllium copper, ductile iron, or steel. The wide use of zinc alloy as a die material stems from the ease of casting it close to the final shape desired. Its low melting point (380.5 °C, or 717 °F) is also advantageous. All dies, regardless of die material, are polished.

Punches are usually made of lead or a low-melting alloy, although zinc or a reinforced plastic can also be used. The sharpness of the contours to be formed, the production quantity, and the desired accuracy primarily govern the choice of punch material. Lead has the advantage of not having to be cast accurately to shape, because it deforms to assume the shape of the die during the first forming trial with a blank.

Rubber Padding. In some drop hammer forming, both a working (roughing) punch and a coining (finishing) punch are used. When the working punch becomes excessively worn, it is replaced by the coining punch, and a new coining punch is prepared. Another method of achieving the same results with one punch is to use 3.2 to 25 mm ($\frac{1}{8}$ to 1 in.) thick rubber pads. Rubber that is suitable for this purpose should have a hardness of Durometer A 60 to 90. In some cases, soft semi-cured rubber is used. (More information on Durometer hardness testing is available in the article "Miscellaneous Hardness Tests" in *Mechanical Testing*, Volume 8 of *ASM Handbook*, formerly 9th Edition *Metals Handbook*.) In the positioning of pads for a particular part (Fig. 2), the maximum thickness of rubber is situated where the greatest amount of pressure is to be applied in the initial forming. As forming progresses, the thickness of the rubber is reduced by removing one or more of the pads after each impact.

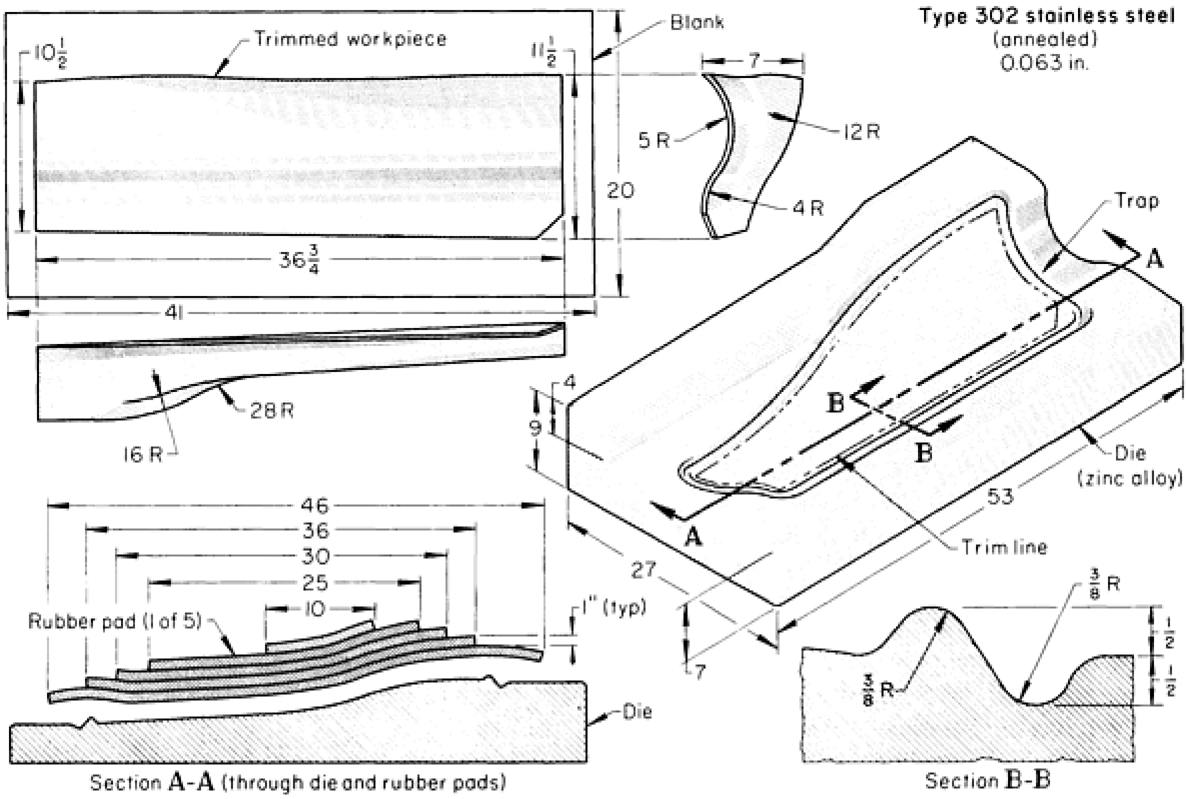

Fig. 2 Smoothly contoured stainless steel part that was hammer formed in a die with a peripheral trap for hold-

down. Dimensions given in inches.

Trapped Rubber Forming. Drop hammer forming using trapped rubber dies is a process derived from the Guerin process, which is today synonymous with the term "rubber-pad" forming (see the article "Rubber-Pad Forming" in this Volume). Both the Guerin and Marform (which is a refinement of the Guerin process) processes use hydraulic pressure instead of a drop hammer as the forming force. The trapped-rubber process using a drop hammer is used extensively by the aerospace and aircraft industries to fabricate sheet metal parts such as instrument panels, tank sections, air frames, stabilizer tips, air ducts, and doors made of aluminum alloys, titanium alloys, and stainless steels.

The drop hammer used in the trapped-rubber process offers several advantages over the hydraulic press used in the Guerin and Marform processes:

- Develops greater force (up to 44 MN or 5000 tonf) per unit of applied energy in shorter time and at less cost
- Minimizes springback as a result of dynamic application of force
- Reduces tendency of material to wrinkle due to better material flow
- Reduces amount of hand finishing required due to dynamic application of pressure

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Lubricants

Lubricants are used in drop hammer forming to facilitate deformation by reducing friction and minimizing galling and sticking, and to preserve or improve surface finish. Selection of a lubricant depends primarily on the type of work metal, the forming temperature, the severity of forming, and the subsequent processing. Recommendations for lubricants used with steels and with aluminum, magnesium, and titanium alloys are given in the sections of this article that deal with the processing of those metals.

Blank Preparation

The blanks for drop hammer forming are generally rectangular and are prepared by shearing. The blank should be large enough to yield a part with a flange 50 to 75 mm (2 to 3 in.) wide in order to facilitate drawing of the metal during forming. When multistage forming is used, the part can be trimmed to provide a flange not less than 13 mm (½ in.) wide for the final forming stage.

Sheared edges are generally satisfactory for drop hammer forming, because the wide flange permits some cracking in the flange area without harming the part. The blank should be deburred to avoid possible damage to the tooling.

Drop Hammer Coining

Tableware, coins, and a variety of decorative items produced in copper alloys, stainless steel, sterling silver, and other metals are commonly coined by the drop hammer process. Processing details and production examples of drop hammer coining are given in the article "Coining" in this Volume.

Multistage Forming

With the drop hammer process, complicated parts can be formed by means of a single die and punch. However, when a large quantity of a particular part is required, it is common practice to adopt a multistage forming technique, employing several sets of dies and punches. The forming operation in any one stage is less severe than if the part were formed in a single operation. Metals that work harden appreciably are usually annealed after each operation, unless a suitable die sequence eliminates the need for annealing.

Unless contact of the work metal with lead is undesirable, the number of stage dies can be kept at a minimum by using lead pads to reduce the depth of the die cavity. Lead can be poured into the die, or lead sheet can be laid in the bottom of the recess, to the desired height; the lead is then formed to contour by a heavy blow of the punch. After the part has been preformed with the padded die, the pad is removed, and the part is then formed to the full depth of the die.

Control of Buckling

Forming a deeply recessed part in thin sheet by any conventional method usually requires a high hold-down force in order to prevent buckling. In drop hammer forming, the hold-down action is restricted to the end of the stroke. Therefore, buckles are free to form during most of the stroke. The hold-down pressure takes effect when the punch contacts the top of the wrinkles formed in the flange and rapidly increases until the die is bottomed. The wrinkles can be removed only at the end of the stroke and only if they are not too deep.

Multistage Processing. To avoid the formation of wrinkles that are too deep to be removed in the late stages of the stroke, the forming process is divided into several forming stages. The wrinkles formed in each stage are slight and can be eliminated at the end of the stroke or, if necessary, by manual hammering. To provide adequate hold-down action at the end of each operation, it is common practice to use stage dies with surfaces that extend slightly beyond the trim line and to use punches equipped with suitable beads or traps.

Hold-Down for Deep Parts. To form deep parts, a series of plywood or metal hold-down rings can be used; one ring is removed after each blow. These rings usually vary in thickness from 6.4 to 25 mm (¼ to 1 in.). The desired metal flow can be approached during the early stages of forming by using a blank that is considerably larger than that required to form the part. The excess metal is trimmed off after several blows. The stiffness of the oversize blank prevents draw-in and therefore induces stretching, which counteracts the tendency toward buckling. The die for a part with a deep recess must be designed with a horizontal surface to accommodate beading.

Processing of Steels

Carbon and low-alloy steels containing less than 0.30% C are the most easily formed by the drop hammer process. Higher carbon content decreases formability and promotes cracking. Although lead additions do not adversely affect the formability of steel, the sulfur additions that are characteristic of resulfurized free-machining steels promote susceptibility to cracking.

All carbon and low-alloy steels require full annealing for satisfactory drop hammer forming.

Stainless steels that are extensively formed by the drop hammer process include AISI types 302, 304, 305, 321, and 347. For severe drop hammer forming, grades containing not less than 10% Ni (and preferably about 12%) should be selected in order to minimize cracking. All stainless steels are drop hammer formed in the fully annealed (solution treated) condition.

Sheet Thickness. The drop hammer forming of steel sheet (particularly stainless steel sheet) less than 0.46 mm (0.018 in.) thick is impractical, because of wrinkling and the difficulties encountered in attempting to planish the wrinkles. The most common range of steel sheet thickness for drop-hammer formed parts is 0.61 to 1.6 mm (0.024 to 0.063 in.). Thicknesses up to 1.98 mm (0.078 in.) have been hammer formed.

Tool Materials. Cast zinc alloy is the most widely used die material for the drop hammer forming of carbon, low alloy, and stainless steels. Alloy cast iron dies are substituted when a large quantity of parts is required. Inserts of an air-hardening tool steel can be used in order to increase the life of dies with sharp fillets and corners.

Punches are made of either zinc alloy or cast iron, and are ground to size. A zinc alloy punch, cast directly into the die, can be used for shallow parts, but it may be undersize (because of shrinkage upon cooling) if the part contains large cavities. Lead punches are also extensively used, although they are easily distorted when used to form steels. Punch life can be considerably increased by facing the punch with an untrimmed finish-formed steel part.

Lubricants. When zinc alloy dies and zinc alloy or lead punches are employed, many steel parts can be formed without a lubricant. Harder tool materials or more severe forming may require the use of a light lubricant, such as SAE 10 or SAE 30 mineral oil.

Precautions for Stainless Steels. Stainless steels, especially the austenitic grades, work harden more than the carbon and low-alloy steels that are suitable for hammer forming. In the cold forming of stainless steel, it is necessary to stretch the metal, rather than allow it to draw into the die. Stretching prevents the formation of wrinkles that are difficult to eliminate. By means of stretching, quarter-hard and even half-hard types 301 and 302 can be drop hammer formed, although only to a very limited extent. Part configuration must be simple and of only moderate depth; otherwise, wrinkles (in a shallow part) or distortion (in a complex shape) will occur. Although it is preferable that the part be made in a single die with a single blow, some commercial quarter-hard parts have required as many as three or four blows for successful forming.

When moderately complex parts are formed in a drop hammer in several stages, it is advisable to consider intermediate annealing in order to offset the effects of work hardening. It is not necessary to pickle after each annealing treatment (provided scaling is not too heavy), except before the finish-forming operation and after the final annealing treatment. If the part is formed in zinc alloy dies, any adhering zinc particles must be removed by pickling or by treatment in a fused salt bath (caustic soda) before annealing treatments (both intermediate and final). This requirement is most important for parts that are to be welded or that will be exposed to elevated-temperature service. Failure to remove the zinc may result in cracking.

Springback. Carbon and alloy steel parts, and especially stainless steel parts, having large radii and smooth contours are more difficult to maintain in desired shape than parts with relatively sharp radii, because of the greater springback under these conditions. Common practice is to compensate for this springback from the desired contour by trial and error. If this method is not successful, the part must be distorted elastically upon assembly, that is, sprung into the final shape.

Parts with reverse contours (saddleback parts) are extremely difficult to form without excessive wrinkling.

Limits in Deep Recessing. When deeply recessed parts are to be formed in a drop hammer, the recesses are limited in both depth and contour. With a single die, a cup-shaped or dome-shaped part can be formed to a limiting depth of 60 to 70% of that obtainable by means of double-action dies. Square and rectangular steel boxes (even shallow ones) require a minimum corner radius of 6.4 mm (¼ in.) or five times the metal thickness, whichever is larger. For deeper boxes, progressively larger corner radii are necessary, and these minimum radii apply to boxes of any width.

Processing of Aluminum Alloys

The drop hammer forming of aluminum alloys is most suitable for limited production runs that do not warrant expensive tooling. The process is often used for parts, such as aircraft components, that undergo frequent design changes. Some forming applications also involve coining and embossing. The article "Forming of Aluminum Alloys" in this Volume contains more information on the forming of aluminum alloy sheet.

Work Metal. Annealed tempers of all aluminum alloys are the most suitable for hammer forming. Intermediate work-hardened tempers of the nonheat-treatable alloys are often used for channel shapes and shallow embossed panels.

Heat-treatable alloys are often partly formed in the annealed condition. The part is then solution heat treated, quenched, restruck to size, and artificially aged. Restriking is also necessary to remove distortion caused by quenching. Drop hammer forming can be done on freshly quenched alloys immediately after quenching, or it can be done later if the alloys are refrigerated to prevent aging.

Sheet Thickness. Under comparable conditions, with the same equipment and with the same thickness of sheet, aluminum wrinkles more easily than steel under a drop hammer. To obtain results comparable to those obtained with steel, aluminum alloy sheet should be at least 40% thicker than the steel, or preferably in the approximate thickness range of 0.86 to 3.18 mm (0.034 to 0.125 in.).

Equipment and Tool Materials. Aluminum alloys are drop hammer formed in gravity-drop, power-drop, and planishing hammers. Dies are cast from aluminum, zinc alloy, iron, or steel. Dies for high production are usually cast in iron or steel. All dies are polished. Most punches are made of lead or a low-melting alloy, although zinc alloy or reinforced plastic can also be used. The softer punch materials have the advantage of deforming readily to assume the shape of the die during forming trials. When planishing hammers are used, the preferred tool material is hardened, polished tool steel.

Forming Characteristics. Annealed aluminum alloys are readily formed under the drop hammer. Simple components can often be produced by a single blow. Deep shapes require extreme care in blank development and die design. Blankholders are not used; therefore, wrinkles are difficult to avoid, especially when thin sheet is being formed.

Processing of Magnesium Alloys

The drop hammer forming of magnesium alloys is performed on preheated sheet in heated dies. This procedure is suited to the production of formed parts having shallow depths and asymmetrical shapes and to parts for which special springback control is required.

Work Metal. Magnesium sheet alloys in the annealed condition are preferred for drop hammer forming. The ideal sheet thickness for forming is 3.2 mm (1/8 in.) and the part should be designed so as to be formable in six stages or fewer. For sheet thinner than 3.2 mm (1/8 in.), ten stages or fewer are recommended.

Equipment and Tool Materials. Both gravity-drop and power-drop hammers are suitable for the forming of magnesium alloys. Zinc alloy is the preferred punch and die material, although lead punches are sometimes used for production runs of not more than 50 pieces. When lead comes in contact with magnesium sheet, there is danger of lead pickup, which can cause corrosion of the sheet. Although lead pickup may occur at room temperature, it is more likely to occur at the elevated temperatures at which magnesium alloys are formed. Therefore, if lead pickup cannot be tolerated or if the production run exceeds 50 pieces, either zinc alloy or cast iron can be substituted for lead.

Lubricants. Vegetable-lecithin oils provide good lubrication at temperatures to 260 °C (500 °F). Suspensions of colloidal graphite may have to be used if temperatures are to exceed 260 °C (500 °F). However, these suspensions are more difficult to remove when parts are cleaned after forming.

Preheating. Magnesium alloy parts are usually formed at temperatures of 230 to 260 °C (450 to 500 °F), depending on the alloy (see the article "Forming of Magnesium Alloys" in this Volume). Heating times are 5 min per stage for sheet up to about 1.29 mm (0.051 in.) thick, and up to 9 min per stage for thicker sheet (up to 3.2 mm, or 0.125 in.).

The oven used to heat the parts between stages should be situated near the drop hammer; the decrease in temperature during transfer from the oven to the hammer will range from 17 to 25 °C (30 to 45 °F) in 5 s. The dies can be heated by placing them in an oven located near the hammer, and they can then be kept at temperature with ring burners or torches during the forming of the part.

Small dies can be anchored to an electrically heated cast iron platen installed on the hammer bed, but this method is impractical for large dies. The punch and die can also be heated by electric elements or by a heat-transfer fluid. The working temperatures should not exceed those recommended.

Rubber pads can be used in the initial forming operation. At 230 °C (450 °F), the reduction obtainable with rubber staging is approximately 10%. Special types of rubber are available for forming at temperatures to 315 °C (600 °F). The rubber pads are removed before the final blow is delivered to set the material.

Springback. One advantage of the elevated temperatures used in the drop hammer forming of magnesium is the marked reduction or total elimination of springback, provided the maximum practical temperatures are always employed. The rate of the deformation is important in drop hammer forming and must be carefully controlled upon severe drawing or when material in the hard (H24) temper is being formed. The rate of deformation can be controlled by the operator, although not to close limits. Parts that require relatively severe forming can be started by allowing the punch to descend slowly into the die and by using subsequent strikes to set the material.

Dimensional Tolerances. Tolerances of ±0.76 mm (±0.03 in.) have been held in the production of magnesium parts. When close tolerances are important, press forming is usually the preferred method for the forming of magnesium parts.

Drop Hammer Forming

Processing of Titanium Alloys

Various titanium sheet alloys have been formed by the drop hammer process, including Ti-13V-11Cr-3Al, Ti-8Al-1Mo-1V, Ti-6Al-4V, and Ti-5Al-2.5Sn. In general, the alloys containing aluminum as the principal alloying element are the most difficult to form. The minimum thickness of titanium sheet for hammer-formed parts is about 0.64 mm (0.025 in.).

Tool Materials. Contact between titanium and low-melting tool materials, such as zinc alloy or lead, should be avoided--particularly when the titanium is formed at elevated temperature or must be heat treated after forming. When these tool materials are used, contact with the workpiece can be avoided by capping the punch and die with sheet steel, stainless steel, or a nickel-base alloy. The choice of capping material depends on the tool life desired. The longest tool life is obtained by capping with nickel alloy sheet such as Alloy 600 (UNS N06600) in thicknesses of 0.64 to 0.81 mm (0.025 to 0.032 in.).

In general, steel and ductile iron dies are used when the tooling must be heated above 205 °C (400 °F). Preheating of both the work metal and the tooling is not uncommon.

Rubber Pads. High-temperature rubber pads are used both in preforming operations before the final strike and as electrical insulators to prevent current loss to the tooling when the work metal blank is heated by the electrical-resistance method.

Lubricants used in the drop hammer forming of titanium should be nonchlorinated. Extreme-pressure oils and both pigmented and nonpigmented drawing compounds are used in most operations.

Preheating Tools and Blanks. Difficult titanium parts are formed at elevated temperature (see the article "Forming of Titanium and Titanium Alloys" in this Volume for recommendations and precautions). Thermal expansion of the blank and the tooling must be considered. If the tooling is not preheated, the amount it expands will depend on the length of time it is in contact with the blank. The allowance for thermal expansion used in the design of tooling for titanium is 0.006 mm/mm (0.006 in./in.) for a forming temperature of 540 °C (1000 °F). The allowance for expansion of circular or elliptical parts should be made radially, not peripherally. When hot sizing is to follow forming, the drop-hammer tooling is usually made to net dimensions without consideration of thermal expansion.

Formability Versus Temperature. Variation of the drop-hammer formability index for two titanium alloys with temperature is given in Fig. 3. It is evident from the curves that significant increases in formability can be achieved at temperatures above 540 °C (1000 °F).

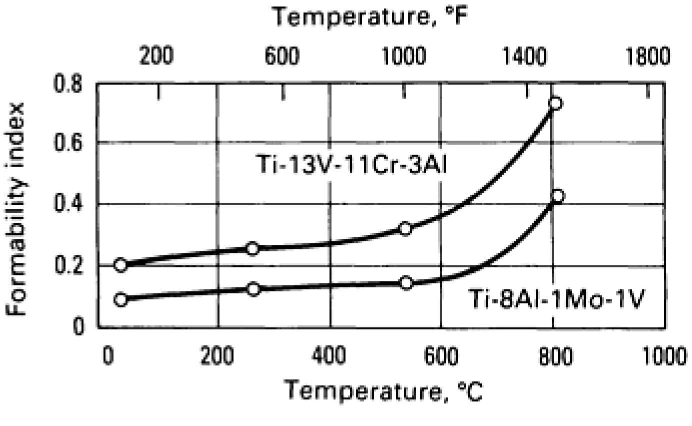

Fig. 3 Effect of forming temperature on the drop hammer formability of two titanium alloys.

Drop Hammer Forming Limits

The severity of permissible deformation in drop hammer forming is limited both by geometrical considerations and by the properties of the work metal. The forming limits can be predicted by considering parts of interest as variations of beaded panels. For parts characterized in this way, the critical geometrical factors are the bead radius *r*, the spacing between beads *s*, and the thickness of the work metal *t* (Fig. 4).

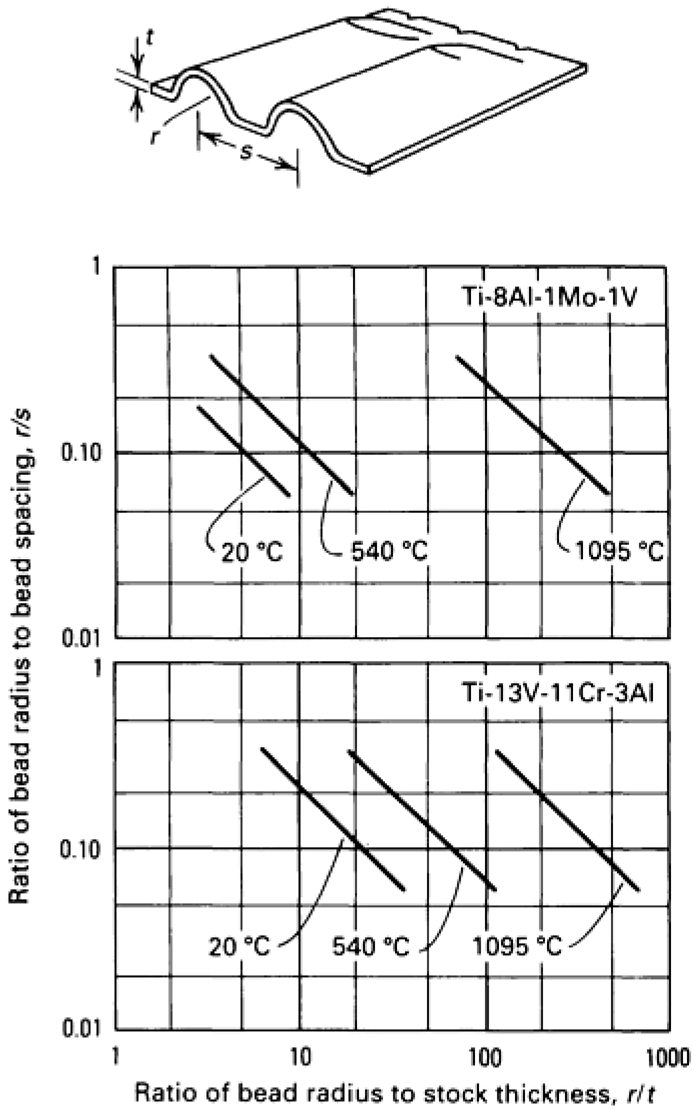

Fig. 4 Formability limits of beaded titanium alloy panels at room temperature and at elevated temperature.

Two of the forming limits depend entirely on dimensional relations and are the same for all materials; the ratio of the bead radius *r* to bead spacing must lie between 0.35 and 0.06. The lower formability limit is controlled by the necessity of producing uniform stretching and avoiding excessive springback. If the *r*/*s* ratio is too small, there will be greater localized stretching at the nose of the punch.

Within the limits set for all materials by the r/s ratio, success or failure in forming beaded panels depends on the ratio of the bead radius to the sheet thickness (r/t) and on the ductility of the work metal. The part will split if the necessary amount of stretching exceeds the ductility available in the material. The splitting limit can be predicted from elongation in a 12.7-mm (0.5-in.) gage length in tension tests at the temperature of interest.

Formability limits for two titanium alloys are plotted in Fig. 4. Both charts show the marked improvements in formability resulting from the better elongation values at elevated temperature.

Introduction

BARS are bent by four basic methods: draw bending, compression bending, roll bending, and stretch bending.

Draw Bending

The workpiece is clamped to a rotating form and drawn by the form against a pressure die (Fig. 1). The pressure die can be either fixed or movable along its longitudinal axis. A fixed pressure die must be able to withstand abrasion caused by the sliding of the work metal over its surface. A movable pressure die, because it moves forward with the workpiece as it is bent, is less subject to such abrasion. It provides better guidance and more uniform restraint of the work material. On power bending machines, draw bending is used more than any other bending method.

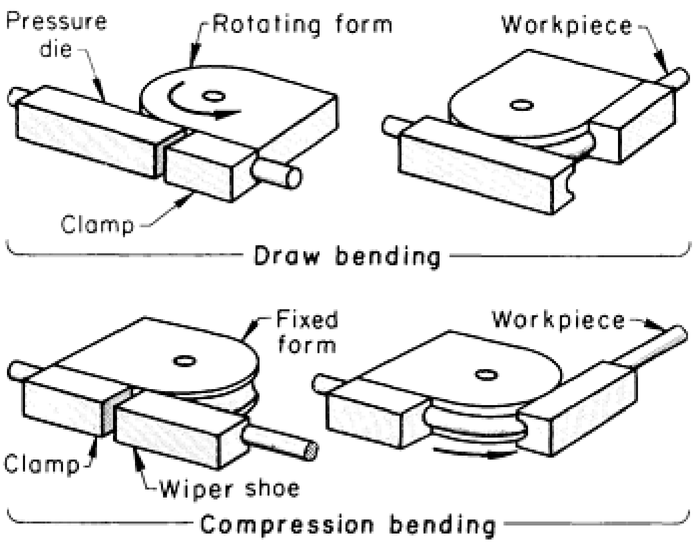

Fig. 1 Essential components and mechanics of draw bending and compression bending of bars and bar sections.

Compression Bending

The workpiece is clamped to a fixed form, and a wiper shoe revolves around the form to bend the workpiece (Fig. 1). Compression bending is most useful in bending rolled and extruded shapes. A bend can be made close to another bend in the workpiece without the need for the compound dies required in draw bending. Although compression bending does not control the flow of metal as well as draw bending, it is widely used in bending presses and in rotary bending machines.

Roll Bending

Three or more parallel rolls are used. In one arrangement using three rolls, the axes of the two bottom rolls are fixed in a horizontal plane. The top roll (bending roll) is lowered toward the plane of the bottom rolls to make the bend (Fig. 2). The three rolls are power driven; the top roll is moved up or down by a hydraulic cylinder. (Other three-roll arrangements are covered in the article "Three-Roll Forming" in this Volume.

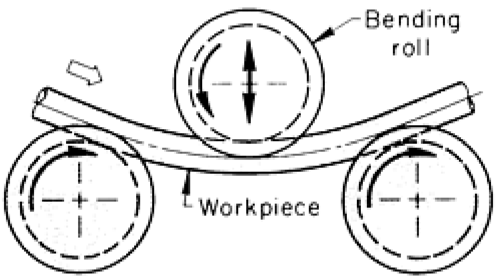

Fig. 2 Operating essentials in one method of three-roll bending.

A roll arrangement for four-roll bending is shown in Fig. 3. The bar enters between the two powered rolls on the left. The lower bending roll is then adjusted in two directions according to the thickness of the bar and the desired angle of bend.

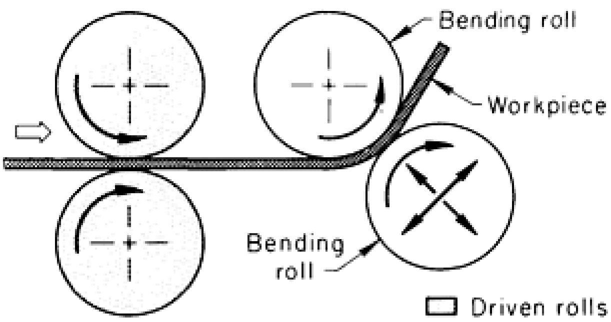

Fig. 3 Operating essentials in four-roll bending.

Rings, arcs of any length, and helical coils are easily fabricated in a roll bender. The bend radius usually must be at least six times the bar diameter or the section thickness in the direction of the bend. To limit distortion in the roll bending of asymmetrical sections, a double section can be made and split in two after bending. Rings are sometimes made by roll bending coils and cutting them into rings for welding.

Roll bending is impractical for making more than one bend in a bar. It is difficult to control springback in a roll bender, and it may take several passes through the rolls to make the needed bend. Therefore, this method of making bends is slower than other methods. Another disadvantage of roll bending is that a short section of each end of the bar is left straight. For three-roll bending, the ends can be preformed in a press before bending, or the straight parts can be trimmed off. The following example describes a production application of three-roll bending.

Example 1: Three-Roll Bending of a Structural Section.

A 7.6 kW (10 hp) three-roll bender was used to bend a steel angle 75 × 75 × 9.5 mm (3 × 3 × $\frac{3}{8}$ in.) into a circular reinforcing flange 1520 mm (60 in.) in diameter. The angle was of hot-rolled ASTM A107 steel. The top roll of the bender was a plain cylinder; each of the two bottom rolls consisted of two cylindrical sections held apart by a spacer to provide a recess for the edge-bent flange. The operations were performed in the following sequence:

- Cut angle to developed length plus 254 mm (10 in.)
- Set rolls to bend correct radius; roll 360°
- Cut off ends of the rolled bar
- Weld the bar into a ring
- Grind the weld flush
- Roll the ring in the three-roll bender to make it a true circle

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Stretch Bending

Large irregular curves are obtained using stretch bending. The workpiece is gripped at the end, stretched, and bent as it is stretched around a form. Usually, less springback occurs when the work is bent while it is stretched. The gripped ends are customarily trimmed off. This method can accomplish in one operation what would otherwise take several operations. The result is a possible savings in time and labor, even though stretch bending is a slow process. The tools, form blocks, or dies for stretch bending are simpler in design and less costly than conventional press tooling. Stretch bending of bars is described in more detail in the article "Stretch Forming" in this Volume.

Bending Machines

The machines used for the bending of bars include the following: devices and fixtures for manual bending, press brakes, conventional mechanical and hydraulic presses, horizontal bending machines, rotary benders, and bending presses. Shapers have also been used to perform specific bending operations.

Manual Bending. Hand-powered machines or fixtures are used in many shops for making bends that do not require much energy to form. This equipment is supplied with ratchets, levers, or gears to give the operator mechanical advantage. Different types of fixtures are used for manual draw bending, stretch bending, or compression bending. Roll bending is seldom done by hand. The tools used in manual bending are the same as those used on some power bending machines. The maximum sizes of low-carbon steel bars that can be manually cold bent are given in Table 1.

Table 1 Maximum sizes of low-carbon steel bars for manual bending

Shape	Size mm (in.)
Rounds	25 mm (1) (diam)
Squares	19 mm ($\frac{3}{4}$) (per side)
Flats bent on flat	9.5 × 102 ($\frac{3}{8}$ × 4)
Flats bent on edge	6.4 × 25 ($\frac{1}{4}$ × 1)
Angles	4.8 × 25 × 25 ($\frac{3}{16}$ × 1 × 1)
Channels	4.8 × 13 × 25 ($\frac{3}{16}$ × $\frac{1}{2}$ × 1)

Press brakes are used for all types of bending, especially in small-lot production (25 to 500 pieces), when standard tooling or low-cost special tooling can be used. Often, the punch is not bottomed in the die; but the stroke is controlled, and the bar is bent "in air" (Fig. 4). With this technique, various bend angles can be made with the same die (see also the article "Press-Brake Forming" in this Volume).

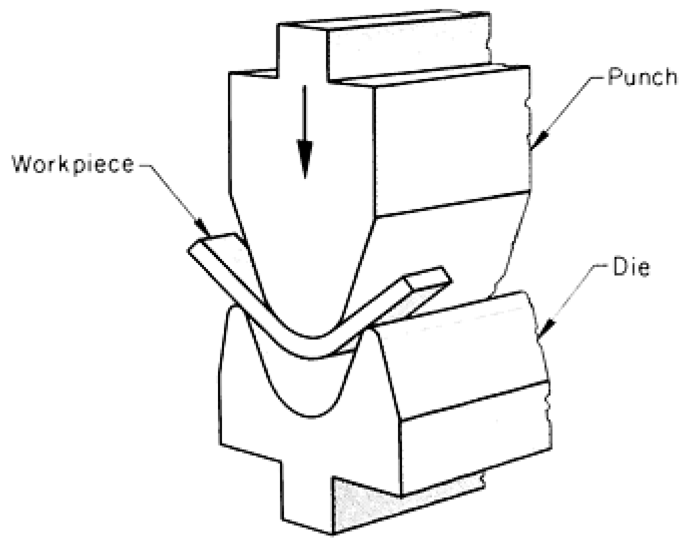

Fig. 4 Air bending of a bar in a press brake.

Mechanical presses are generally used only for mass production, because only large production lots can justify the cost of tooling, which is more than that for most standard bending tools. Figure 5 shows a round bar being bent into a U-bolt in a press. The bar is first cut to length and pointed at both ends (preliminary to a later threading operation). The bar is then loaded into the press and held in a grooved die that bends the bar into a U in one stroke. In the setup shown in Fig. 5, more than one workpiece can be bent at a time. The following example describes an application of a mechanical press in bending bars.

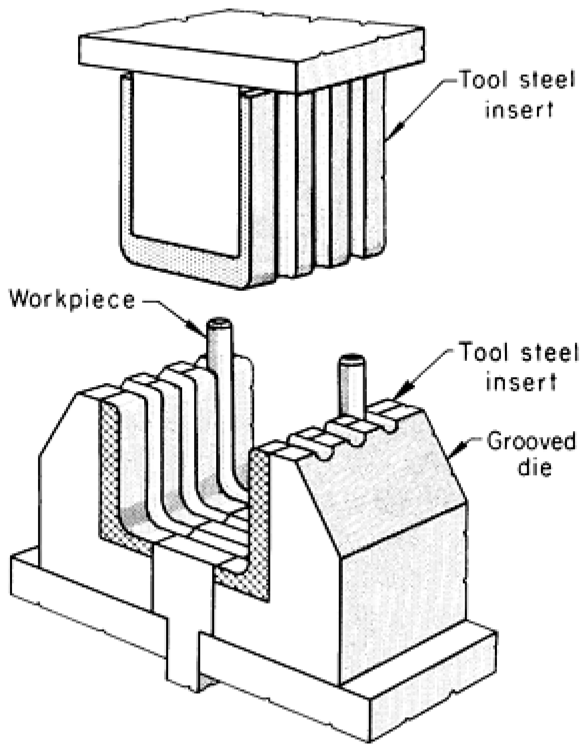

Fig. 5 Use of a grooved die in a mechanical press for bending a round bar into a U-bolt in one stroke.

Example 2: Bending a Welded Assembly in a Mechanical Press.

The wheel spider shown in Fig. 6 had three 254 mm (10 in.) long spokes of 9.52 mm (0.375 in.) diam low-carbon steel. The spider was assembled by welding the three spokes to a 13 mm ($\frac{1}{2}$ in.) thick steel hub. The assembly was loaded into a 670 kN (75 tonf) mechanical press, a double bend (joggle) was made in the spokes, and the short straight surface between the two bends was flattened to 6.4 mm ($\frac{1}{4}$ in.) thick. The wheel rim was then welded to the spokes as shown in Fig. 6(d). Next, the assembly was loaded into another press, in which the legs were sheared flush with the outer edge of the rim and a 3.2 mm ($\frac{1}{8}$ in.) diam hole was pierced in the flattened area of each spoke. The production rate was 25 per minute.

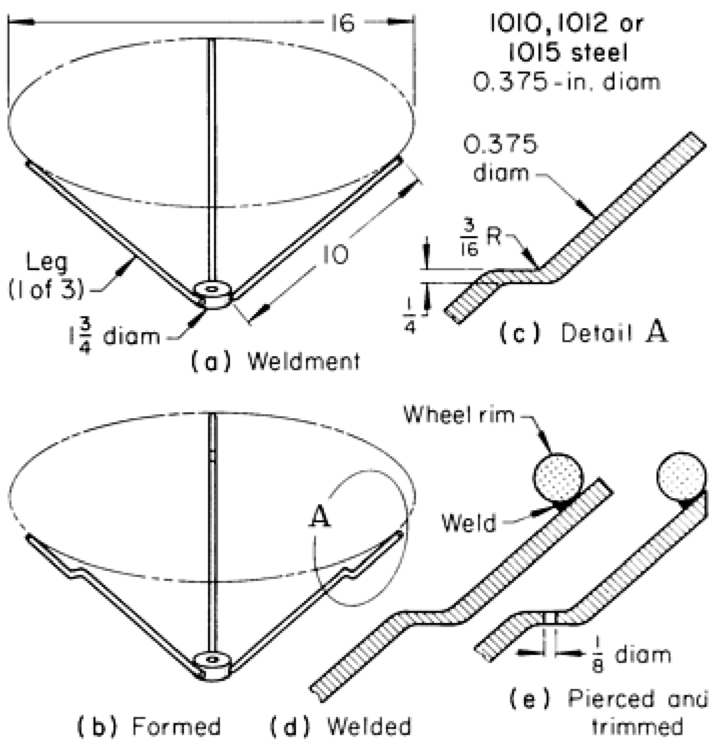

Fig. 6 Welded bar assembly that was formed by bending in a mechanical press. Dimensions given in inches.

Hydraulic presses are often used to bend bars in much the same manner as mechanical presses. Although hydraulic presses are usually slower than mechanical presses, they have the advantage of exerting full force over a long stroke. Therefore, deep bends can often be made on a hydraulic press much smaller than the mechanical press that would be required. In the following example, a hydraulic press needed so little head room that a closed shape could be bent over it.

Example 3: Bending a Double-Bar Structure in a Hydraulic Press.

A double-bar structure was constructed of two 11 mm ($\frac{7}{16}$ in.) diam bars that were connected by welded cross members to form a ladderlike structure. A rectangular shape was formed by making four 90° bends having 16 mm ($\frac{5}{8}$ in.) inside radii. The two bars (sides of the ladderlike structure) were bent simultaneously, using a punch that forced the bars between rollers. By using a small (27 kN, or 3 tonf) vertical hydraulic press, the four bends could be made consecutively, allowing the workpiece to encircle the press ram as bending was completed. The overhead clearance would not have been available with a mechanical press. This technique permitted the fabrication of 360 double bends (90 frames) per hour.

Horizontal bending machines for bending bars consist of a horizontal bed with a powered crosshead that is driven along the bed through connecting rods, crankshaft, clutch, and gear train. Dies are mounted on the bed, and forward motion of the crosshead pushes the bar through the die. The long stroke and generous die space make this machine useful for a variety of cold- and hot-bending operations, although speeds are lower than those for mechanical presses of similar capacity. Horizontal benders are available in capacities from 89 to 2700 kN (10 to over 300 tonf).

Rotary benders, either vertical or horizontal, are used for the draw, compression, or stretch bending of bars. Such machines consist of a rotary table in either a horizontal or vertical position on which the form block or die is mounted (Fig. 1). Suitable hydraulic or mechanical clamping, tensioning, or compressing devices are provided to hold the workpiece while the die rotates to the required position, or while the workpiece is bent about the central forming die. Some machines can make bends by two, or all three, methods.

Bending presses are most widely used for bending tubing (see the article "Bending and Forming of Tubing" in this Volume). However, bending presses are occasionally used for bending bars, as in the following example.

Example 4: Making a Double Crank in a Bending Press.

Double cranks such as the one shown in Fig. 7 were made in a bending press from round bars 7.9 or 9.5 mm ($\frac{5}{16}$ or $\frac{3}{8}$ in.) in diameter. The bars were cut to length and fed into the press to flatten the ends and pierce the holes. The two sharp bends were made one at a time in the same press with a V-die. A bearing bracket was assembled on the crank, followed by a double-staking operation in the same press. The greatest demand on the press was in the end-flattening operation, which required a press capacity of 8900 to 13,300 kN (100 to 150 tonf).

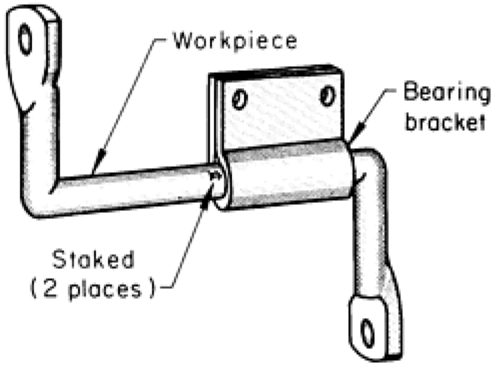

Fig. 7 Double crank produced in a bending press.

Shapers can be tooled for bending operations. One method is to have the fixed die, or anvil, held on the knee of the shaper and the punch mounted on the ram (Fig. 8). The shaper must make one stroke only. The stroke can be adjusted to allow for springback in the workpiece; therefore, the workpiece can be bent to fairly close limits.

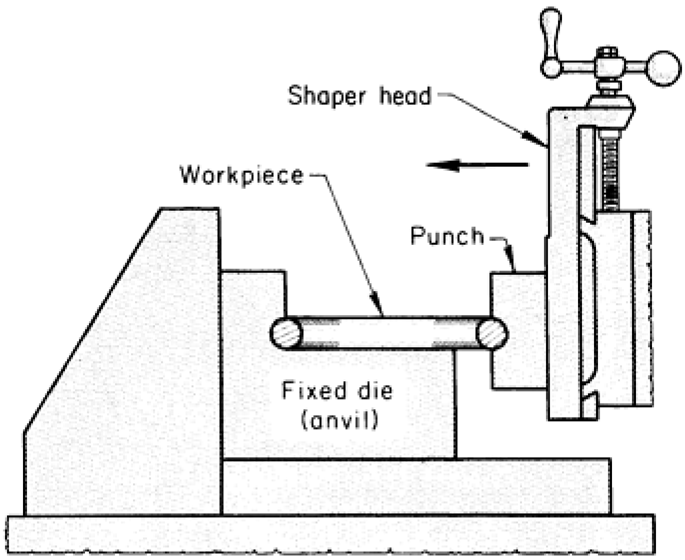

Fig. 8 Use of a shaper for correction of springback in U-bolts, J-bolts, or rings.

The setup shown in Fig. 8 is used to correct for springback in formed parts such as J-bolts, U-bolts, and rings. A V-punch and die mounted in the shaper can make bends of various kinds.

A shaper can also be provided with a rack and pinion to produce rotary motion for bending (Fig. 9). Bend radius can be varied by the use of center pins of different diameters. A typical use of a shaper in the bending of bars is described in the following example.

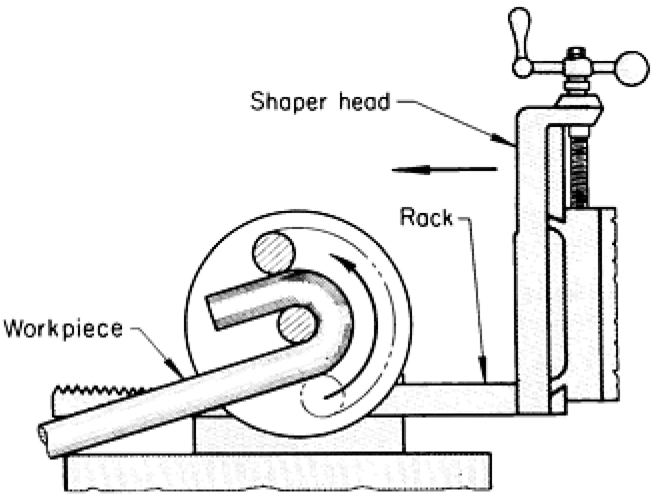

Fig. 9 Bending with rotary motion in a shaper.

Example 5: Shaper Versus Press for the Bending of J-bolts.

Originally, J-bolts were press bent cold in a die from sheared lengths of 13 mm ($\frac{1}{2}$ in.) diam hot-rolled merchant bars of 1025 steel. The press made the J-bolts by bending the bar into a half circle with one long arm. Because the unbalanced support caused variations in bending, some of the parts were unacceptable.

The job was put on a shaper. For this operation, the stock was sheared to double lengths, which the shaper bent into a circular loop with two long arms (the ends). The looped bar was then sheared in half. The cost of tooling for the shaper was much less than that for the press.

A rack-and-pinion-actuated fixture similar to that shown in Fig. 9 was used to bend the bar into a circular loop. The bar stock was placed in the fixture at a slight angle to allow one leg to pass the other during forming. After the bar was cut in two, the J-bolt was finished in a setup such as that shown in Fig. 8.

Tools

Tools for draw and compression bending are shown in Fig. 1. The form used in both processes is shaped to the contour of the bend. It is usually grooved to fit the work. Often, the form is part of a right cylinder whose straight portion (frequently, an insert) provides the surface against which the work is clamped. Hydraulic or mechanical pressure holds the clamp against the workpiece. Annular grooves or roughened surfaces grip the bar or bar section.

To allow for springback, the bend radius is made smaller on the form than is required for the workpiece. The form is also designed for a greater angle of bend than is needed. These two adjustments permit overbending the piece to allow for springback. Such adjustments are made by trial and error. The form is tested and corrections are made before it is heat treated.

The finish on the form (rotating or fixed) should be just good enough to avoid marring the workpiece. For most bar bending, a machined finish is sufficient. For decorative stainless steel and polished aluminum, grinding or polishing of the form surfaces may be needed. However, the clamping area should not be ground or polished unless necessary. The smoother the finish in the clamping area, the greater the danger that the workpiece will slip through the clamp. The pressure die and wiper shoe require a good finish (usually ground) because the work metal must slide along them.

When air bending bars in a press brake, simple V-blocks will suffice for the female dies. The opening of the V-blocks should be eight times stock thickness for standard sections, and ten times stock thickness for heavy sections. The upper die (punch) is shaped to the inside radius of the bend, and the angle of bend is controlled by the length of stroke. The same die set can be used for making various bends, as well as for various stock thicknesses, by adjusting the stroke.

Press brakes can use rubber pads for tooling on bends that need support (see the article "Press-Brake Forming" in this Volume). Completely shaped dies for bottoming can also be used in a press brake, as shown in Fig. 10.

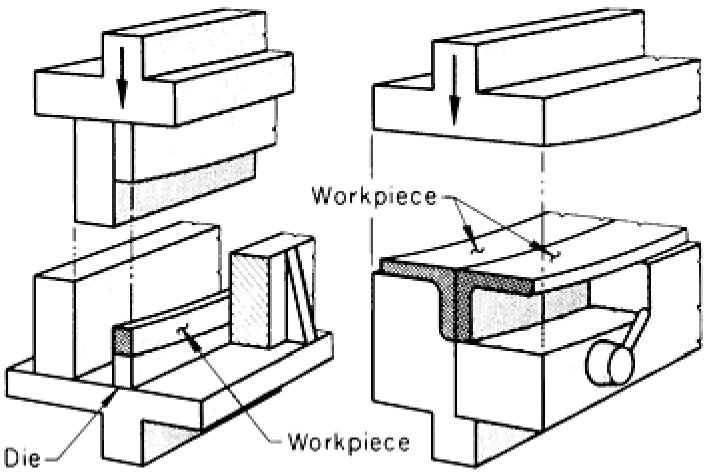

Fig. 10 Die setups in a press brake for edge bending a bar (left) and for bending two structural angles (right).

Most dies used in conventional mechanical or hydraulic presses are completely shaped bottoming dies, which makes them more expensive than tooling for other bending machines. Tooling for bending presses is specially designed to fit the needs of the machine and the work to be done. Dies for bending presses are simple to construct and relatively inexpensive. Tools for stretch bending are covered in the article "Stretch Forming" in this Volume.

Die Materials. Dies are usually made of hardened steel for the production of thousands of pieces per month. Tool steel is used for small one-piece dies. Larger dies are made of low-carbon steel and then carburized and hardened. Clamping inserts are made separately.

For moderate production of a few hundred pieces per month, unhardened carbon steel is often used. If only a few parts are needed, wood or an aluminum alloy may be strong enough for dies.

For bottoming dies in presses, hardened tool steel is always used. For cold bending, A2 tool steel hardened to 58 to 62 HRC is most often selected. A hot-work tool steel, such as H11, hardened to 45 to 50 HRC is usually the choice for hot bending.

Bend Allowance

The stock consumed in a bend (that is, overall length of a bend) can be computed from the radius of curvature at the neutral axis and from the angle of the bend. A formula often used for this computation is:

$$W = 0.01745\ \alpha(r + \delta)$$

where W is the bend allowance, α is the angle of bend (in degrees), r is the radius of bend to inner stock surface, and δ is the distance from the inner surface to the neutral layer (a commonly used approximation when this figure is not known is one-third to one-half stock thickness). The constant 0.01745 is a conversion factor changing degrees to radians.

When bar stock for a workpiece whose ends are within, or very near, the bend area is cut square to the neutral axis, the ends, after forming, will not be square to the neutral axis. The basic reason for this is the difference in circumference of the outer and the inner surfaces of the bend. Additional deviation from squareness can be expected because all of the material toward the outside of the bend from the neutral axis has undergone a tensile load and the material inside this line has been under compressive load. Unless compensation can be made for these variations, the ends of the formed part must be trimmed if they are to be square to the axis. However, when end details, locating surfaces, and other considerations necessitate such action, it is still possible to cut the blanks to size in such a way that trimming after forming is eliminated.

Lubrication

Successful bending depends to a large extent on the type of lubricant used. No one lubricant works equally well on all materials. Selection of a lubricant varies among different shops. Typical lubricants for bending specific metals are listed in Table 2.

Table 2 Typical lubricants for bending various metals

Work metal	Lubricant
Low-carbon steel	**Water-soluble, vegetable-oil base drawing oil**[a]
Stainless steel and other high-alloy iron-base alloys	**Mineral-oil-base drawing oil**[a]
Aluminum alloys and copper alloys	**Mineral oil**
Brass (severe bends)	**Soap solution**[b]
Hot bending of carbon, alloy, and stainless steels	**Molybdenum disulfide**

(a) Available as proprietary material.

(b) Creamy mixture of laundry soap and water

Overlubrication, in either quantity or type of lubricant, must be avoided. Not only is excessive lubrication likely to cause wrinkling, but the cost of removal must be considered. It is never good practice to use a pigmented compound if successful results can be obtained with an unpigmented compound, because pigmented compounds are more difficult to remove.

Wiper dies are lubricated with a very small quantity of high-grade drawing lubricant. It is important not to overlubricate pressure dies and wiper shoes.

Bending and Forming of Tubing

Introduction

THE PRINCIPLES for bending tubing are much the same as those for bending bars (see the article "Bending of Bars and Bar Sections" in this Volume). Two important additional features in the bending of tubes are that internal support is often needed and that support is sometimes needed on the inner side of a tube bend.

The wall thickness of the tubing affects the distribution of tensile and compressive stresses in bending. A thick-wall tube will usually bend more readily to a small radius than a thin-wall tube. Table 1 lists the minimum practical inside radii for the cold draw bending of round steel or copper tubing, with and without various supports against flattening and wrinkling.

Table 1 Minimum practical inside radii for the cold draw bending of annealed steel or copper round tubing to 180°

Radii can be slightly less for a 90° bend, but must be slightly larger for 360°.

Tubing outside diameter	Minimum practical inside radius		
	Grooved bending tools		Cylindrical bending block without mandrel; ratio, <30[a] (poor conditions)
	With mandrel; ratio, <15[a] (best conditions)	**With mandrel or filler; ratio, <50[a]**	

				(normal conditions)			
mm	**in.**	**mm**	**in.**	**mm**	**in.**	**mm**	in.
3.2	$\frac{1}{8}$	1.6	$\frac{1}{16}$	6.4	$\frac{1}{4}$	13	$\frac{1}{2}$
6.4	$\frac{1}{4}$	3.2	$\frac{1}{8}$	7.9	$\frac{5}{16}$	25	**1**
9.5	$\frac{3}{8}$	4.8	$\frac{3}{16}$	9.5	$\frac{3}{8}$	50	**2**
12	$\frac{1}{2}$	6.4	$\frac{1}{4}$	11	$\frac{7}{16}$	75	**3**
16	$\frac{5}{8}$	7.9	$\frac{5}{16}$	14	$\frac{9}{16}$	102	**4**
19	$\frac{3}{4}$	11	$\frac{7}{16}$	17	$\frac{11}{16}$	152	**6**
22	$\frac{7}{8}$	13	$\frac{1}{2}$	19	$\frac{3}{4}$	203	**8**
25	1	14	$\frac{9}{16}$	22	$\frac{7}{8}$	254	**10**
32	$1\frac{1}{4}$	17	$\frac{11}{16}$	25	1	381	**15**
38	$1\frac{1}{2}$	21	$\frac{13}{16}$	29	$1\frac{1}{8}$	508	**20**
44	$1\frac{3}{4}$	24	$\frac{15}{16}$	32	$1\frac{1}{4}$	686	**27**
50	2	27	$1\frac{1}{16}$	35	$1\frac{3}{8}$	889	**35**
64	$2\frac{1}{2}$	35	$1\frac{3}{8}$	41	$1\frac{5}{8}$	. . .	**. . .**
75	3	41	$1\frac{5}{8}$	48	$1\frac{7}{8}$	. . .	**. . .**
89	$3\frac{1}{2}$	48	$1\frac{7}{8}$	54	$2\frac{1}{8}$	. . .	**. . .**

102	4	54	$2\frac{1}{8}$	60	$2\frac{3}{8}$	...	...

(a) Ratio of outside diameter to wall thickness of tubing

Selection of Bending Method

The four most common methods of bending tubing are basically the same as those used in the bending of bars: compression bending, stretch bending, draw bending, and roll bending. The method selected for a particular application depends on the equipment available, the number of parts required, the size and wall thickness of the tubing, the work metal, the bend radius, the number of bends in the workpiece, the accuracy required, and the amount of flattening that can be tolerated.

Hand Versus Power Bending. The bending methods and tooling used in hand bending are the same as those for power bending. Steel tubing as large as 38.1 mm (1.50 in.) in outside diameter with a 1.65 mm (0.065 in.) wall thickness can be bent by hand, but the process is slow and repeatability is questionable. Some hand benders use an adjustable friction device, a kind of sliding brake, to prevent sliding of the tubing. The friction prevents wrinkles and other defects in bending. The following two examples illustrate several of the factors that must be taken into consideration in selecting either hand bending or power bending to fabricate tubing.

Example 1: Hand Bending of a U-Shaped Furniture Part Having Two 90° Bends.

Bending equipment was needed to produce a U-shaped furniture part with two 90° bends from 19 mm ($\frac{3}{4}$ in.) outside diameter 1010 steel welded tubing with a 1.25 mm (0.049 in.) wall thickness. The two bends were made to a 50 mm (2 in.) radius as measured on the tube centerline. A small amount of flattening was tolerated. Production rate was 500 pieces per month.

Because no great accuracy was needed and because the production volume did not warrant more than a minimum investment, a bending fixture for compression bending by hand was selected, along with tooling to allow for both bends to be made in one setup. If production volume had been larger, a power-driven bender or a bending press might have been selected.

Example 2: Power Bending of Machine Tool Hydraulic Lines.

In the production of hydraulic lines for machine tools, from 9.5 mm and 13 mm ($\frac{3}{8}$ and $\frac{1}{2}$ in.) OD steel tubing and from 6.4 mm ($\frac{1}{4}$ in.) OD copper tubing, hand bending required 6400 manhours per year for 44,000 bends. A change to power bending reduced the man-hours needed to 450.

Tools

Tools used for the bending of tubes are similar to those for the bending of bars (see the article "Bending of Bars and Bar Sections" in this Volume). One important difference is that tools for tubes need carefully shaped guide grooves to support the side-walls and to preserve the cross section during the bend.

Form blocks, or bending dies, resemble those described in the article "Bending of Bars and Bar Sections" in this Volume. They either rotate or are fixed, depending on the arrangement of the machine in which they are used. One end of the tube is clamped at the end of the groove in the form block, and the tube is bent by being forced around the block and into the groove. For round tubes, the depth of the groove in the form block should be one-half the outside diameter of the tube to provide sufficient sidewall support.

The block becomes the template for holding the shape of the bend. Form blocks can be made of wood, plastic, or hardboard; if they are to be used for an extensive production run, they can be made of tool steel and hardened.

Clamping blocks hold the end of the tube to the form block and maintain the holding force necessary to make the bending action effective. Although the groove in the clamping block should be well formed, the finish should not be so fine that the tube will slip. Ordinarily, the as-machined finish is adequate, but sometimes ridges or serrations are machined into the clamp to increase the holding force. Rosin can be applied to the tube to prevent it from slipping in the clamp.

If the clamped area is to be part of the finished piece, care must be taken to prevent scratches or mars. If the clamping groove has to be ground or polished to provide a good surface, the portion of the tube to be clamped will have to be longer to distribute the higher clamping force better. When the clamping length is short, the end of the tube is sometimes plugged to prevent it from deforming from high clamping forces. Table 2 lists typical clamping lengths for bending steel tubing.

Table 2 Typical clamping lengths for bending steel tubing

Radius of bend centerline	**Wall thickness of tube, mm (in.)**	Typical length clamped
1 × OD	<0.89 (0.035)	**4-5 × OD**
	0.89-1.65 (0.035-0.065)	**3-4 × OD**
	>1.65 (0.065)	**2-3 × OD**
2 × OD	<0.89 (0.035)	**3-4 × OD**
	0.89-1.65 (0.035-0.065)	**2-3 × OD**
	>1.65 (0.065)	**$1\frac{1}{2}$-$2\frac{1}{2}$ × OD**
3 × OD	<1.65 (0.065)	**2-3 × OD**

Pressure dies are used in the draw bending of tubing to press the workpiece into the groove in the form block and to support the outer half of the tube. The most commonly used pressure die is as long as the developed length of the bend plus some allowance for holding, and it does not slide over the tube but travels with it as it moves toward the bend area (see the article "Bending of Bars and Bar Sections" in this Volume). In one face, it has a groove with a depth that is slightly less than one-half the outside diameter of the tube.

A stationary pressure die or even a roller can be used on noncritical work. Either unit has a tube-forming groove machined in its face. Most stationary pressure dies are made of low-carbon steel, which can be case hardened to resist wear. Tool steel such as O1, A2, or D2, hardened to 55 to 60 HRC, or aluminum bronze is commonly used for sliding dies.

In compression bending equipment, where the tube is clamped to a nonrotating form block, a wiper shoe replaces the pressure die. Its relationship to the workpiece is similar to that of the stationary die described above in that the wiper shoe slides over the workpiece. However, instead of being fixed, the wiper shoe revolves around the stationary form block, progressively pressing the tube into the form block groove. For most applications, the length of the wiper shoe is from three to five times the outside diameter of the tube. The wiper shoe is made of tool steel and hardened to 55 to 60 HRC, or of a bearing bronze.

Wiper dies are stationary straight-groove dies (not to be confused with the wiper shoes described above) that are sometimes needed in draw bending to support the tube on the side opposite the pressure die as the tube is about to be drawn into the contour of the form block. Metal that will form the inside of the bend undergoes severe compression that is transmitted back toward the as yet unbent end of the tube, where it could cause wrinkles if not for the support of the wiper die.

The wiper die has a groove that is machined and ground to conform to the tube being bent and to fit the groove and lips of the form block, ending in a featheredge pointing toward the tangent point of the bend and extending to within 3.2 to 13 mm ($\frac{1}{8}$ to $\frac{1}{2}$ in.) of the tangent point. Although it is difficult to maintain this distance without deflection, it must be done meticulously if the wiper die is to prevent compression wrinkles. Wiper dies are machined from 52100 (or L2 tool steel) for low-carbon steel tubing or from aluminum bronze for stainless steel tubing. Wiper dies are never hardened.

Mandrels, which are described in detail in the following section of this article, are of three general types--rigid, flexible, and articulated--and are made to support the inside of the tube during bending. Rigid mandrels fit the interior of the tube, and are sometimes shaped to conform to the start of the bend. However, because they are rigid, they support the entire circumference of the tube only as far as the point of bending and not beyond the tangent of the bend. Plug mandrels and formed mandrels are included in this category.

Flexible mandrels bend with the tube. They are generally built up of shims or laminae. This type of mandrel is sometimes used with square tubes and box sections where only a few bends are needed. Inserting and removing flexible mandrels is usually difficult. Articulated mandrels include ball mandrels (discussed below) and various other shaped mandrels that are used in much the same way as ball mandrels.

Loose fillers such as sand and various low-melting alloys also serve as mandrels for low-production applications.

Dies used in press-type bending machines are similar to those described in the article "Bending of Bars and Bar Sections" in this Volume. Dies, including wing dies for bending presses, may have grooves for one to six tubes to be bent in one press stroke.

Formed rolls are used in the roll bending of tubes. Grooves corresponding to the outer surfaces of the tubes to be bent are cut or ground into the outer surfaces of the rolls so that they fit the surface of the tube as it is bent. A more complete description of the rolls used in roll bending is available in the article "Three-Roll Forming" in this Volume.

Bending Tubing With a Mandrel

Mandrels are sometimes used in bending to prevent collapse of the tubing or uncontrolled flattening in the bend. A mandrel cannot correct failure in bending after the failure has begun, nor can it remove wrinkles.

Figure 1 shows five types of mandrels used in the bending of tubing. The plug mandrel and the formed mandrel are rigid, but the three other types shown are flexible or jointed to reach farther into the bend.

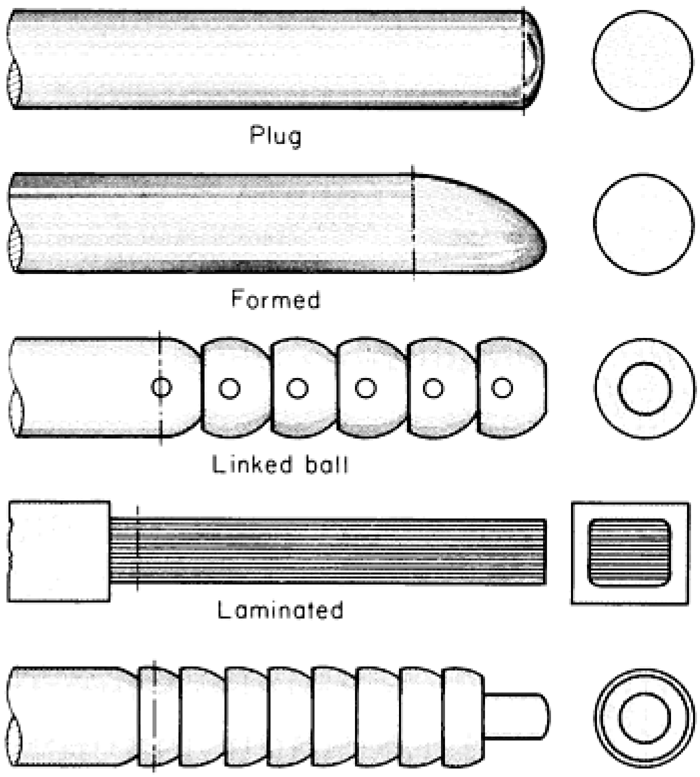

Fig. 1 Five types of mandrels used in the bending of tubing. Broken vertical lines are points at which bends should be tangential to mandrel centerlines.

The largest diameter of the rigid portion of the mandrel should reach a short distance into the bend; the distance that it extends past the tangent straight portion depends on the type of mandrel and the size of the tube and is usually established by trial. If the mandrel extends too far, it can cause a bulge in the bend. Conversely, if the mandrel does not extend far enough, wrinkles may form, or the outer tube surface may flatten in the bend area.

The need for a mandrel depends on the tube and bend ratios. The tube ratio is D/t, where D is the outside diameter and t is the wall thickness. The bend ratio is R/D, where R is the radius of bend measured to the centerline.

Table 3 can be used to determine whether or not a mandrel is needed for bending steel tubing. Figure 2 shows the usual conditions that require the use of a mandrel and what type of a mandrel is needed.

Table 3 Minimum centerline radii for bending steel tubing without a mandrel

Tubing outside diameter		Minimum centerline radius for tubing with wall thickness, mm (in.), of:											
mm	**in.**	**0.89 mm**	**(0.035) in.**	**1.24 mm**	**(0.049) in.**	**1.65 mm**	**(0.065) in.**	**2.11 mm**	**(0.083) in.**	**2.36 mm**	**(0.093) in.**	**3.05 mm**	(0.120) in.
4.8	$\frac{3}{16}$	7.9	$\frac{5}{16}$	6.4	$\frac{1}{4}$	4.8	$\frac{3}{16}$	...	...	...	...	...	...
6.4	$\frac{1}{4}$	13	$\frac{1}{2}$	9.5	$\frac{3}{8}$	7.9	$\frac{5}{16}$	...	...	...	...	...	...

7.9	$\frac{5}{16}$	22	$\frac{7}{8}$	19	$\frac{3}{4}$	16	$\frac{5}{8}$	...	...	...	...	...	**...**
9.5	$\frac{3}{8}$	38	$1\frac{1}{2}$	32	$1\frac{1}{4}$	29	$1\frac{1}{8}$	25	1	...	...	...	**...**
13	$\frac{1}{2}$	57	$2\frac{1}{4}$	50	2	44	$1\frac{3}{4}$	38	$1\frac{1}{2}$	...	...	...	**...**
19	$\frac{3}{4}$	102	4	75	3	64	$2\frac{1}{2}$	50	2	...	...	...	**...**
25	1	203	8	152	6	102	4	75	3	50	2	50	**2**
38	$1\frac{1}{2}$	...	...	...	...	305	12	254	10	203	8	152	**6**
50	2	...	...	...	...	...	...	610	24	508	20	406	**16**
64	$2\frac{1}{2}$	...	...	...	...	...	...	...	...	610	24	508	**20**
75	**3**	**...**	**...**	**...**	**...**	**...**	**...**	**...**	**...**	**...**	**...**	**635**	**25**

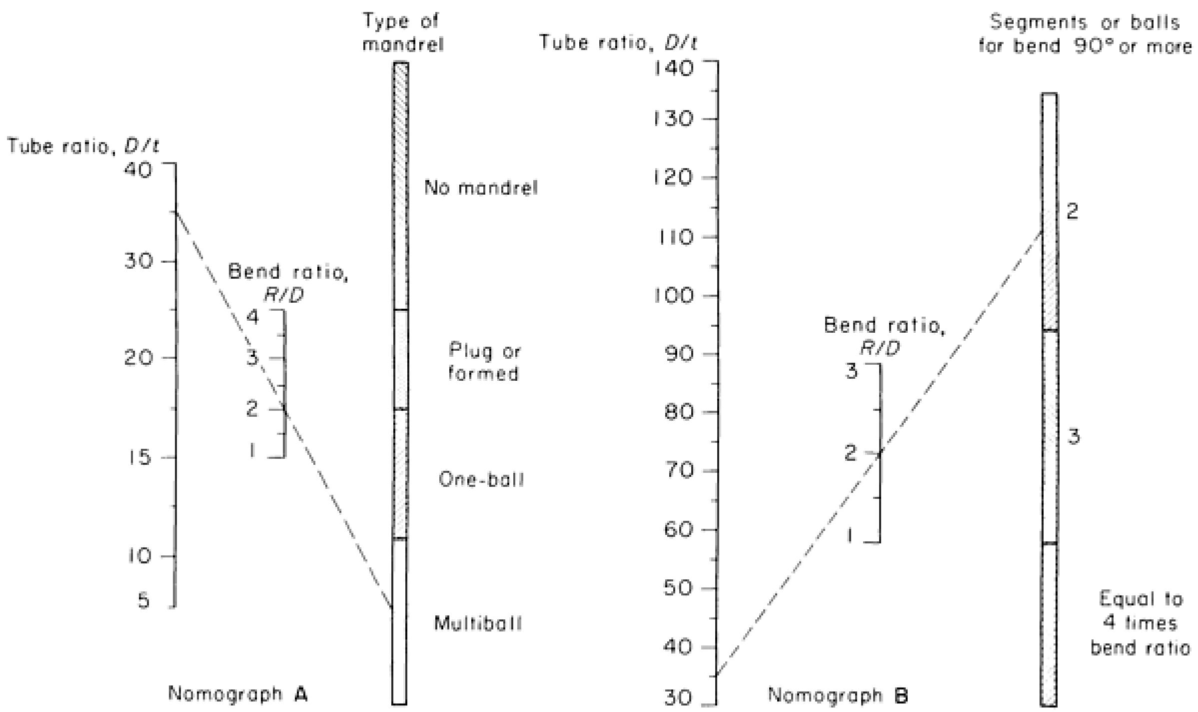

Fig. 2 Nomographs for determining when a mandrel is needed and the correct type to use. See text for explanation.

The nomographs given in Fig. 2 are used in two steps to determine if and when a mandrel is required and which specific type will suffice. In nomograph A, the first step is to find the tube ratio and the bend ratio in the left-hand and center scales and to lay a straightedge across them. The zone on the right-hand scale where the straightedge falls shows whether or not a mandrel is required, and which type. Bends for which D/t is more than 40 always require a multiball mandrel.

If a multiball mandrel is indicated, step two requires the user to refer to nomograph B in Fig. 2. As before, the tube ratio and the bend ratio are located in the left-hand and center scales and a straightedge is laid across them. The number of balls needed in the multiball mandrel will be indicated on the right-hand scale.

Plug and formed mandrels are fixed, and the tube is drawn over the tip of the mandrel in forming. This action work hardens the tube so that it resists flattening during bending. Clearance between the mandrel and the inside of the tube should not be more than 20% of the wall thickness. If the mandrel is too tight, the tube is likely to fail in the bend. Mandrels are necessary in tubes other than round in order to avoid distortion of the cross section. The use of plug and formed mandrels is shown in the following two examples.

Example 3: Use of a Plug Mandrel in Bending Welded Low-Carbon Steel Pipe.

A short length of 19 mm ($\frac{3}{4}$ in.) pipe (26.67 mm, or 1.050 in., OD by 2.87 mm, or 0.113 in., wall) of welded low-carbon steel was bent 90° to a 50 mm (2 in.) radius. Despite the thick wall, the small bend radius made it necessary to use a plug mandrel to support the pipe against flattening. Other tools used were a form block, a clamp, and a pressure die.

The bending machine was a power-driven rotary draw bender rated for a maximum of 25 mm (1 in.) extra-strong seamless low-carbon steel pipe (33.40 mm, or 1.315 in., OD by 4.55 mm, or 0.179 in., wall). The bends were made at the rate of 300 per hour.

Example 4: Bending Oval Tubing With a Formed Mandrel.

Oval tubing measuring 25 × 111 mm (1 × 4$\frac{3}{8}$ in.) in outside dimensions and with a wall thickness of 1.65 mm (0.065 in.) was bent on edge to make a U-shape with two 90° bends at 229 mm (9 in.) radius. The tubing was welded hot-rolled low-carbon steel. Wrinkles, shear marks, or other visible defects were not permitted.

The bends were made in a draw bender rated for 89 mm (3$\frac{1}{2}$ in.) OD by 2.10 mm (0.083 in.) wall thickness steel tubing with 276 MPa (40 ksi) yield strength. This piece, bent with a formed mandrel, form block, clamp, and pressure die, demanded the full rated torque of the machine. The mandrel was lubricated. The bends were made at a rate of 250 per hour.

Ball mandrels with one or more balls are used for many bends. During bending, the metal is stretched tightly over the mandrel, making withdrawal difficult. Withdrawal mechanisms are needed. In thin-wall tubing of softer metals, as the mandrel is withdrawn it sizes the bend somewhat, smoothing the stretched metal and correcting the shape of the cross section.

The bodies and balls of one-ball mandrels used on most tubing are commonly made of carburized low-carbon steel, hardened, ground, and polished. For the bending of stainless steels, they are made of polished aluminum bronze.

One-ball mandrels used in the bending of tubes up to 32 mm (1$\frac{1}{4}$ in.) in outside diameter generally have a body that is undersize 0.13 to 0.18 mm (0.005 to 0.007 in.), with a ball 0.25 to 0.36 mm (0.010 to 0.014 in.) smaller than the inside diameter of the tube. Square or shaped tubes require a mandrel that fits closer. If the bends are in one plane, the body and the ball of the mandrel can be grooved to clear weld flash or seams. More commonly, a mandrel is made undersize to clear the obstruction. When the workpiece must be bent in several planes, it can be reinserted with the seam in the groove, but it is usually better to specify tubing with a controlled weld flash.

Ball mandrels are often made with several balls, as shown in Fig. 1. The balls or segments are always smaller than the body, and they can be jointed by links and pins, ball joints, or steel cable. A linked or jointed mandrel is usually stronger than a comparable mandrel joined by steel cable. The linked mandrel bends in only one plane and is easier to load than one that is less rigid. The ball-jointed mandrel is also in wide use, and it has the advantage of having rotating balls to equalize wear. Ball-jointed ball mandrels are made in many sizes--down to one for tubes as small as 5.64 mm (0.222 in.) in inside diameter. Ball-jointed and steel cable ball mandrels cannot be grooved to clear weld flash and seams, because the mandrel segments rotate.

Many multiball mandrels make bends with a centerline radius that equals the outside diameter of the tubing. Ball mandrels can be used on bends that are not possible with formed mandrels.

Example 5: Multiball Versus Formed Mandrel in Forming a U-Shaped Bend.

A U-shape was produced by making two 90° bends in 32 mm ($1\frac{1}{4}$ in.) OD welded tubing of low-carbon steel in 1.24 and 1.65 mm (0.049 and 0.065 in.) wall thicknesses. The bend radius for both types of tube was 60 mm ($2\frac{3}{8}$ in.). Wrinkles, shear marks, or other visible defects were not permitted.

For the 1.65 mm (0.065 in.) wall thickness tubing, the bend was made with a formed mandrel, which adequately supported tubing of this wall thickness. However, a formed mandrel could not be used for the 1.24 mm (0.049 in.) wall thickness tubing. For the thinner-wall tubing, the D/t ratio was so large that it was necessary to use a well-lubricated three-ball mandrel and wiper die. The ball mandrel was required to support the outer wall in the bend area, and the wiper die was required to prevent wrinkling caused by compression in the inner wall of the bend.

The machine used was a draw bender rated for steel tubing with 89 mm ($3\frac{1}{2}$ in.) OD, 2.10 mm (0.083 in.) wall, and 276 MPa (40 ksi) yield strength.

If the formed mandrel had been used in bending the 1.24 mm (0.049 in.) wall tube, the tube would have deformed excessively from inadequate support. A formed mandrel would have to be advanced farther into the bend area than is normally done. This would cause a hump and greater thinning of the tube wall where the outside of the bend was stretched over the end of the mandrel. When wall thickness is less than approximately 1.24 mm (0.049 in.), this technique should not be used, because there is not enough metal to take the stretch without breaking.

Clearances for mandrels vary from 0 (to produce some bends the mandrel is forced into thin-wall tubing) to 2.41 mm (0.095 in.) or more. The clearance needed depends on stock material, wall thickness, bend radius, and quality of the bend. The better the bend, the more closely the mandrel must fit.

Mandrels are even more necessary in bending nonround tubing than in bending round tubing (see Example 4). Segmented mandrels must be used in almost all bending of square, hexagonal, and octagonal tubing. The number of segments that are needed in the mandrel usually depends on the wall thickness of the tubing.

Chromium plating extends the life of some mandrels, and the plating can be renewed for further use. Platings should not be more than 0.008 to 0.013 mm (0.0003 to 0.0005 in.) thick because thicker platings may flake off. Plated mandrels should be stripped and replated when the plating is worn through at any point. Polished mandrels usually work best, but ground or machined surfaces are satisfactory when slight marking on the inside of the tube is acceptable.

The bending of a fragile thin-wall tube may require the careful use of a multiball mandrel and a wiper die. If the bend is changed to a larger radius or if a stronger or thicker-wall tube is substituted, the mandrel can be changed to a less complicated one, and the wiper die may not be needed. A plug or form mandrel can sometimes be used instead of a multiball mandrel, even when the bend is made to the minimum practical radius.

Dimensional Accuracy. Regardless of other conditions, when accuracy is important, the use of a mandrel is mandatory. The following example describes an application that illustrates the degree of accuracy that can be achieved by using a mandrel.

Example 6: Use of a Plug Mandrel to Hold Close Tolerance on Four Bends.

The tube shown in Fig. 3 was used in a return-line manifold of a high-pressure hydraulic system on a large tractor. The radius of each of the four bends was required to be within ±1°. Overall length was required to be within ±0.38 mm (±0.015 in.).

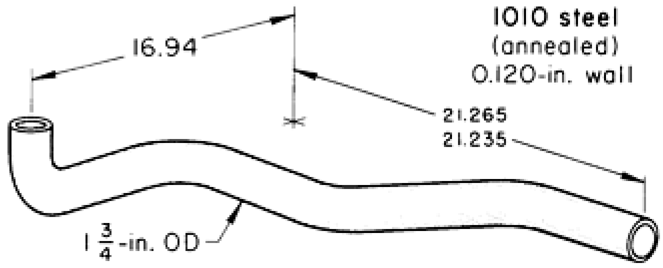

Fig. 3 Manifold tube that was bent to an accuracy of ±1° on each of four bends. Dimensions given in inches.

To achieve this degree of accuracy, a considerable amount of tool adjustment was required during each setup. From two to six tubes were bent before acceptable tubes were produced. To produce a lot of 40 tubes, 2$\frac{1}{2}$ man-hours were required, including setup.

Bending was done in a powered draw bender, using a round-end plug mandrel. The mandrel had a drilled hole to deliver a constant flow of lubricant inside the tube during bending. The lubricant was a mixture of lard oil and low-viscosity mineral oil.

Mandrel Materials. Most mandrels are made of tool steel (W1, O1, A2, and F1 are typical selections) and hardened to 55 to 60 HRC.

Bending Tubing Without a Mandrel

It is less expensive to bend tubing without a mandrel. Trial bending is generally necessary to determine which bends can be made. Tubing with thick walls is more likely to be bendable without a mandrel than thin-wall tubing. Bends with large radii are more likely to be formable without a mandrel than those with small radii. Slight bends are more feasible than acute bends. Wide tolerances on permissible flattening make a bend easier to form without a mandrel. Springback is greater without a mandrel, but it can be compensated for by overbending or lessened by increasing force on the pressure die.

Machines

The machines used in the bending of tubes are essentially the same as those used in the bending of bars (see the article "Bending of Bars and Bar Sections" in this Volume). In general, bending machines fall into three categories: rotary benders (stretch, compression, and draw bending), press benders (stretch and compression bending), and roll benders.

Powered rotary benders are commonly used to bend tubing as large as 203 mm (8 in.) in outside diameter. At least one machine can bend tubing as large as 305 mm (12 in.) OD with a 6.4 mm ($\frac{1}{4}$ in.) wall, and a few special power benders can bend 457 mm (18 in.) pipe.

Boilermakers normally use power benders that can bend 75 mm (3 in.) OD steel tubing with 13 mm ($\frac{1}{2}$ in.) wall to a centerline radius as small as 75 mm (3 in.). The following example describes the use of rotary benders.

Example 7: Bending Steel Tubing for Automobile Seat Frames.

Steel tubing with 25 mm (1 in.) OD and 1.24 mm (0.049 in.) wall was bent 90° to make the frame for automobile seats. The inside of the bend was collapsed into a dimple for clearance, but most of the column strength was maintained by holding the shape of the outside of the bend. This was done in part by collapsing the inner wall of the tube over a convex punch instead of a conventional groove. The centerline radius of the 90° bend was 16 mm ($\frac{5}{8}$ in.). The bends were made two at a time in a fast rotary bender.

Bending presses are hydraulic machines that are made especially for bending both bars and tubes, but most often for tubes. The ram of a bending press can be stopped at any point in the stroke. Wing dies and a cushioning device help to wrap the work around the ram die, as shown in Fig. 4. When the ram moves down, it causes the wing dies to pivot by a camming action and to wrap the workpiece around the ram die. The wing dies wipe the work to control the flow of metal; a compression bend is made on each side of the ram die, without wrinkles or distortion.

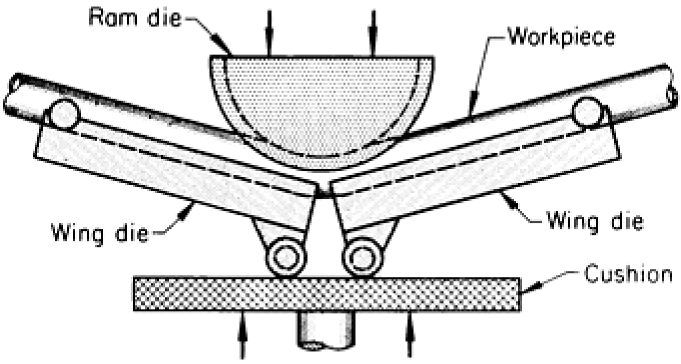

Fig. 4 Essential components and mechanics of a bending press.

A bending press can usually make bends much faster than machines that are not made especially for bending. The open design of the press makes possible the bending of complex shapes in one setup. Single bends can be made sequentially, or the press can make several bends simultaneously. Bends can be made to various angles and in various planes. The tube or bar is usually passed through the press in one direction, and the press makes a sequence of bends automatically. The work is held against stops to locate each bend.

When several bends are made in one or more workpieces at each stroke of the press, all bends are in the same plane. Different angles and bend radii can also be made in the same workpiece, and the angles and spacing of bends can be adjusted. One disadvantage of the bending press is that it causes a slight reduction in the thickness of the workpiece at the bend.

Automatic bending presses are used for production bending. Capacities are 26 to 360 kN (3 to 40 tonf) for bends that generally do not exceed 165°. Bends in the same plane should be separated by a distance equal to twice the outside diameter of the tube. Bends in different planes should be separated by a distance equal to at least three times the outside diameter of the tube. If the bent portion is to be joined with another bend into an accurate circle, the length of the straight legs on each end of the bend should be at least twice the outside diameter of the tube. Bends can be made beyond these limits, but at greater cost. Bends are usually made in dies that are slightly tight on the tubes to prevent flattening and wrinkling. Exhaust pipes for automobiles, with bends in various planes, are made in automatic bending presses, as in the following example.

Example 8: Forming Automobile Exhaust Pipes From 1010 Steel Welded Tubing.

Figure 5 shows an exhaust pipe 2.1 m (82 in.) long with bends to bypass the obstructions on the underside of a vehicle. These pipes were made of hot-rolled or cold-rolled 1010 steel welded tubing with 44 mm ($1\frac{3}{4}$ in.) OD and 1.52 mm (0.060 in.) wall. Hardness was 63 to 78 HRB.

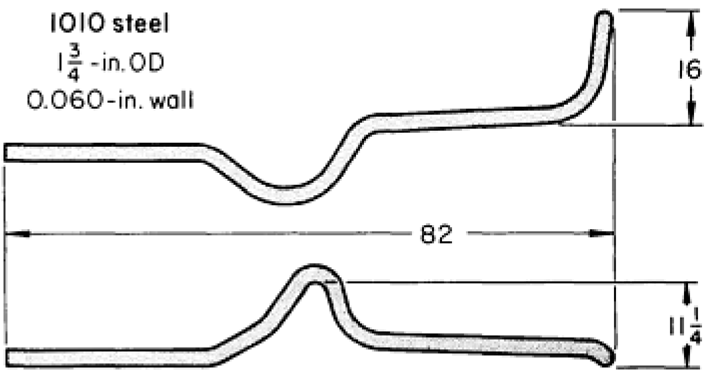

Fig. 5 Automobile exhaust pipe produced in an automatic hydraulic bending press. Dimensions given in inches.

Bending was done in an automatic 180 kN (20 tonf) hydraulic bending press, using a punch with a 127 mm (5 in.) centerline radius in the groove and two wing-die shoes. Locating fixtures and a checking fixture (off the press) were also used.

The press made different angles of bend by using turret-mounted stops to control the stroke. The location of the bend was set by backstops at the right side of the press. Counting the plane of the first bend as 0°, the radial position (roll of the tube) for the plane of each bend in turn was located by stops along a bar on the left of the machine. In loading the tubes for the first bend, it was important to keep all weld seams in the same place so that all tubes would bend alike.

The tube was nested in the wing-die shoes for the first bend, and the work was then moved by hand for each of the other bends. All the bends were standard. Tolerances were:

Angles of bend	**±0° 15'**
Angles of tube ends (all planes)	**±0° 30'**
Maximum depth of depressions and wrinkles	**3.05 mm (0.12 in.)**
Linear dimensions	**±1.52 mm (±0.06 in.)**

All the bends were made cold. The punch was set to clear the work by 13 mm ($\frac{1}{2}$ in.) for loading, and the total machine time to make five bends was 0.1305 min. Production was 130 pieces per hour on a 10,000-piece order.

Roll benders for bending tubes are similar to those used for bending bars, as described in the article "Bending of Bars and Bar Sections" in this Volume, but tolerances are more critical on the rolls and spacing. Roll benders are discussed in the article "Three-Roll Forming" in this Volume. The contour of the rolls must match that of the tube to minimize wrinkling or flattening. Tubes of sizes up to 203 mm (8 in.) OD by 6.10 mm (0.240 in.) wall can be bent into arcs, circles, or helixes. Rings are easily made on three-roll benders. Helix coiling is described in the following example.

Example 9: Coiling a Helix of Round Steel Tubing in a Three-Roll Bender.

Steel tubing of 50 mm (2 in.) OD and 6.10 mm (0.240 in.) wall was coiled in a three-roll bender into a helix 610 mm (24 in.) in mean diameter with 64 mm ($2\frac{1}{2}$ in.) pitch and 20 turns, as shown in Fig. 6. The tubing, 38.4 m (126 ft) long, was made by welding together random lengths 3.0 to 7.3 m (10 to 24 ft) long. The welds were then ground flush. The form rolls were contoured to fit the 50 mm (2 in.) outside diameter of the tubing. The tubing was started into the rolls so that it coiled without stop until the helix was completed. If the coiling had been stopped, the diameter of the helix might not have been constant. After coiling, the helix was removed and trimmed to length.

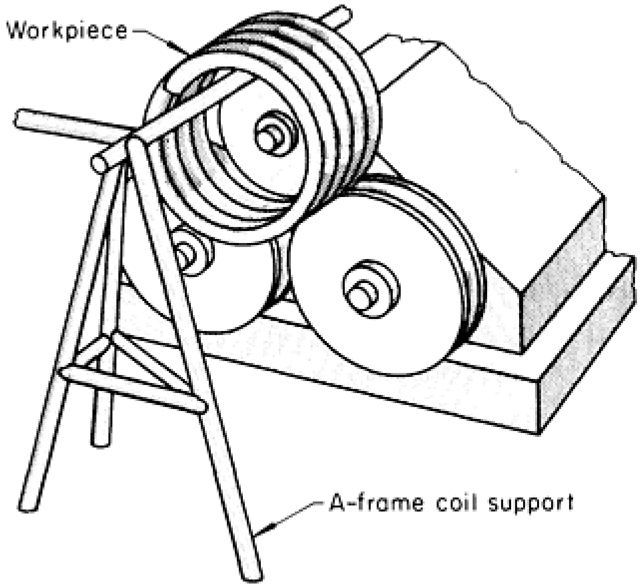

Fig. 6 Coiling a helix from round steel tubing by three-roll bending.

Hot Bending

Most of the bends described so far in this article have been made by cold bending (workpiece at room temperature). There are obvious advantages to cold bending:

- Heating equipment is not needed
- The benefits of previous heat treatment are not destroyed
- Subsequent cleaning or descaling is less likely to be needed
- Workpiece finish is better
- Thermal distortion is avoided

On the other hand, cold bending demands more energy than hot bending for the same bend. There is more springback after a cold bend and more residual stress in the tube. Bends cannot be made to as small a radius cold as they can hot.

Tubes are bent hot to make bends of small radius, adjacent bends with little or no straight tube between them, bends in material with little cold ductility, bends that take too much power to bend cold, and bends in fragile assemblies where the force of cold bending might cause damage. Temperatures and procedures in the hot bending of carbon, low-alloy, medium-alloy, and stainless steel tubes are summarized in Table 4. The disadvantages of bending tubes hot include high cost, slow production, and poor finish on bends.

Table 4 Temperatures and procedures for hot bending of steel tubes

Steel	**Temperature, °C (°F)**	Procedures
Carbon steels		
ASTM A106, A178, A192, A210	980-1095 (1800-2000) or <730-(1350)	**Do not heat beyond 1095 °C (2000 °F) and do not bend between 730 and 870 °C (1350 and 1600 °F)[(a)].**

Low-alloy steels		
ASTM A209, A213 (gr T11 and T22), A335 (gr P2)	980-1095 (1800-2000) or <730-(1350)	**Do not heat beyond 1095 °C (2000 °F) and do not bend between 730 and 870 °C (1350 and 1600 °F)[(a)].**
Alloy steels		
ASTM A213 (gr T5 and T9)	980-1095 (1800-2000)	**Do not heat beyond 1095 °C (2000 °F) and do not bend between 730 and 870 °C (1350 and 1600 °F)[(a)]. Heat treat after bending 730-745 °C (1350-1375 °F).**
Stainless steels		
Types 304, 310, 321	>1150 (2100)	**After bending, heat treat to 1095-1120 °C (2000-2050 °F). Furnace cool to 315 °C (600 °F); air cool[(b)].**
Type 446	**>1150 (2100)**	**Do not bend at less than 870 °C (1600 °F). Heat treat at 790-870 °C (1450-1600 °F); water quench.**

(a) Ductility is sometimes low in this range; therefore, the range should be avoided for hot bending.

(b) This treatment has proved best for maximum strength in service at elevated temperature.

Tubes of carbon steel and most alloy steels can be bent to a much smaller radius by hot bending than by cold bending. A bend radius of 0.7 to 1.5 times the outside diameter of the tube can usually be made by hot bending. The wall thickness of the tube affects this range. The tube wall must not be so thin that it will distort or thin excessively in the outer wall. If the tube ratio (outside diameter divided by wall thickness) is more than 10, the tube probably needs internal support in bending, unless there is some upsetting of the tube that thickens the wall.

Figure 7 shows the tube relations that can usually be successfully bent hot in a die but without a mandrel, and Fig. 8 shows the tube relations that can usually be successfully bent hot with a mandrel or filler. Figure 7 applies to tubes 38 to 75 mm (1.5 to 3 in.) in diameter; Fig. 8 is for tubes of all diameters.

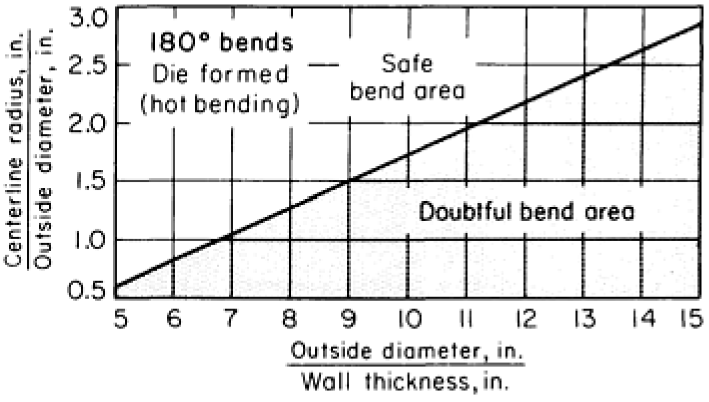

Fig. 7 Chart for determining conditions for the successful hot bending of tubes 38 to 75 mm (1.5 to 3 in.) in

diameter without the use of mandrel.

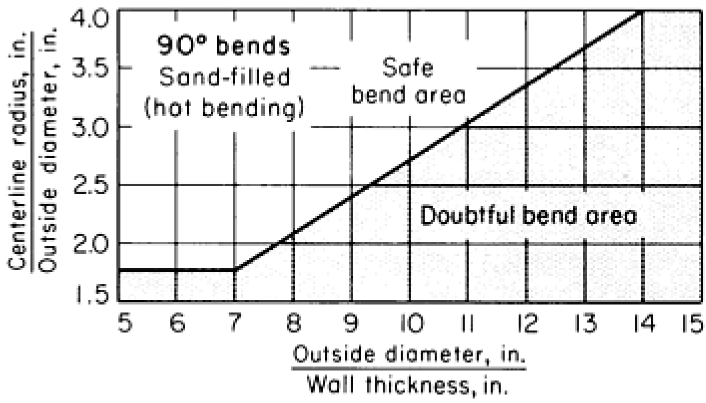

Fig. 8 Chart for determining conditions for the successful hot bending of tubes of all diameters with the use of a mandrel or filler.

If a mandrel is not practical for internal support of the tube, the tube can be packed with dry sand. First, a plate is welded to one end of the tube to block the end. The tube is filled with sand and is tamped or vibrated to make sure the sand is packed tight. Another plate is welded to the open end, the tube is heated, and the bend is made. Finally, the plates are cut off and the sand is emptied out. Various specialized techniques for bending tubes hot are described in Examples 10, 11, and 12.

Example 10: Five-Step Bending of a Tube.

Return bends for the coils of a boiler were made by bending 50 mm (2 in.) outside diameter carbon steel tube 180° on a 38 mm ($1\frac{1}{2}$ in.) centerline radius. The tube had a 6.60 mm (0.260 in.) wall. The sequence of operations was:

- Bend 180° on 114 mm ($4\frac{1}{2}$ in.) centerline radius in a conventional rotary bender
- Heat the bend area to 980 to 1095 °C (1800 to 2000 °F) in a furnace
- Reshape the bend to 50 mm (2 in.) radius in a bending press
- Close legs to 75 mm (3 in.) between centers in a vertical press
- Restrike in the vertical press to make bend radius 38 mm ($1\frac{1}{2}$ in.)

The last three operations listed above were performed in rapid sequence so that all bends were made before the tube cooled below 870 °C (1600 °F). Production rate was 30 pieces per hour.

Thinning of the outer wall can sometimes be controlled better in hot bending by heating only the part of the workpiece that will be the inner wall of the bend. This reduces its compressive strength so that the bend causes very little stretch of the outer wall. This method makes good bends with centerline radii of 1.3 to 1.5 times the outside diameter of the tube with the usual tooling in a rotary bender.

Boiler tubes are bent by heating one side for one-third of the way around the tube. The heated portion becomes the inside of the bend, and because it yields more easily in compression, thinning of the outer wall is limited. The tubes are bent to a U-shape in a bending machine. The U can then be reheated on the inside as before, and the bend can be squeezed in a press in one or more steps to make a narrower U, as in the following example.

Example 11: Localized Heating for a Compression Bend.

Return bends were made for use in the economizer of a boiler. The tube was made of carbon steel, 50 mm (2 in.) OD by 7.21 mm (0.284 in.) wall. The operations were:

- Heat the tube to 705 to 730 °C (1300 to 1350 °F) in a special burner that heated the bottom 120° of the tube for a 457 mm (18 in.) length where the bend was to be made (Fig. 9)
- Bend the tube in a rotary bender 180° to 190 mm ($7\frac{1}{2}$ in.) between centers with the heated portion on the inside of the bend
- Squeeze the tube between dies in a hydraulic press to 152 mm (6 in.) between centers
- Squeeze, as in the third step, to 121 mm ($4\frac{3}{4}$ in.) between centers
- Squeeze, as in the third step, to 89 mm ($3\frac{1}{2}$ in.) between centers
- Reheat the entire surface of the bend for 203 mm (8 in.) on each side
- Size in a die
- Squeeze, as in the third step, to 64 mm ($2\frac{1}{2}$ in.) between centers
- Normalize

The production rate, with three men working, was 20 bends per hour.

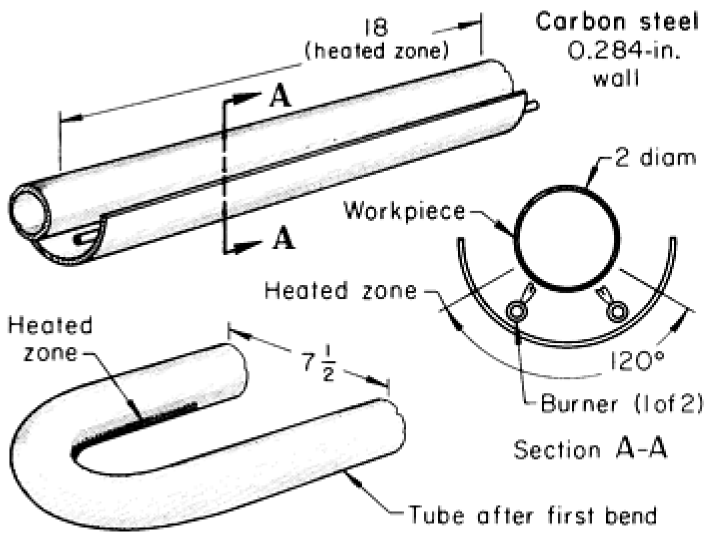

Fig. 9 Compression bend produced in tubing, and special burner used for localized heating of the workpiece before bending. Dimensions given in inches.

Dry Sand as Filler. Large tubes are commonly bent hot while they are filled with dry sand. The bending is done on a bending table made of cast iron plates. The plates have a continuous pattern of cored holes. A bending form for the desired radius is bolted to the bending table. Steel pins or stops can be placed in the holes in the table to keep the work in line. A clamshell furnace in sections is the usual source of heat. Winches, jacks, and hoists supply the bending force. A typical application is described in the following example.

Example 12: Hot Bending a Large Tube on a Cast Iron Bending Table.

A 180° bend with a 381 mm (15 in.) radius was made near the center of a 4.6 m (15 ft) length of steel tube, 184 mm ($7\frac{1}{4}$ in.) OD by 25 mm (1 in.) wall. A plate was welded to one end of the tube to close it. The tube was dropped, closed-end down, into a pit and was filled with sand. The tube was vibrated and tamped to make sure the sand was well compacted, and the open end was closed with another welded-on plate.

The bending table was set up for the bend. The proper form block was bolted to the table, and lines tangential to the bend were laid out for proper location of stop pins (Fig. 10).

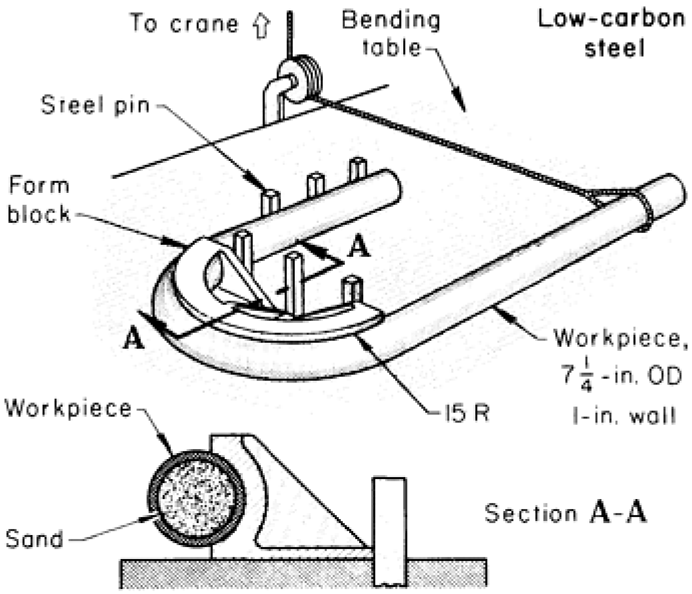

Fig. 10 Final position in the hot bending of a large sand-filled tube.

The tube was heated to 980 to 1095 °C (1800 to 2000 °F) in a clamshell furnace and set on the bending table. One end was clamped down to the table. The other end was pulled by a cable, which, guided by a series of strategically located pulleys fastened to the table, bent the tube incrementally around the form block to the final position shown in Fig. 10.

When the bend passed inspection, the ends of the tube were cut off and the sand was poured out. The entire process, including preparation and handling, took 45 min.

Hot Versus Cold Bending. The selection of hot or cold bending can depend on available equipment, the cost of new equipment, labor costs, the urgency of the job order, and the size of the production lot.

Tube Stock

Tubes are classed as seamless, welded, lock-seam, butt-seam, and jacketed.

Steel tubing is available both seamed and seamless. Seamed tubing with internal flash demands special consideration when a mandrel is to be used in bending (see the section "Bending Tubing With a Mandrel" in this article).

Lock-seam tubing can be bent if the seams are tight. A test is to twist a 914 mm (3 ft) long section of tubing in the hands; any grating or slipping indicates a seam too loose to make good bends.

Butt-seam tubing is similar to welded tubing but with no weld at the joint; it is seldom used. To make good bends, it must be accurate in dimensions, have no scale, and be bent with the seam in the plane of the bend. A mandrel must always be used. It is more economical to use welded tubing.

Stainless steel jacketed tubing is made by roll forming a sheet of stainless steel onto a butt-seam tube of low-carbon steel. The stainless steel jacket is rolled into a lock seam in the open seam of the inner tubing. For best bending, the stainless steel jacket should be at least 0.51 mm (0.020 in.) thick, and the two layers should be rolled tightly together. Tools for bending such jacketed tubing cost more than tools for bending plain tubing; an aluminum bronze wiper die, a hardened steel mandrel, and unusually high pressure on all tools are required, and all of these add to the cost of the tooling. In addition, the seam must be in the plane of the bend, either inside or outside.

Galvanized steel tubing can be bent to a radius as small as four times the outside diameter. For tube to be bent to smaller radii, galvanizing should be done after bending, because the galvanized coating is likely to flake if galvanized stock is used.

Aluminum-coated tubing (hot dipped) can be bent by essentially the same techniques used for uncoated tubing of the same diameter and wall thickness, using slightly higher clamping pressures to avoid slipping. Additional information is available in the article "Press Forming of Coated Steel" in this Volume.

Seamless tubing should be free of scale or rust. Wall thickness, concentricity, and hardness vary in seamless steel tubing. These variations are likely to cause variable springback, wrinkles, and excessive flattening. Common pipe in all sizes and thicknesses is easily bent if it is clean and free of rust or scale, inside and out.

Stainless steel tubing can be bent to a greater angle, at a given radius, than low-carbon steel. Austenitic types in the 300 series are most commonly bent because they are strong and ductile. Tubing in a stabilized condition at a temper no higher than quarter hard will make good bends with low scrap rates. Both welded and seamless tubing are available. Thin-wall tubes should have the exact diameter and wall thickness specified. Annealing is usually recommended after bending operations.

Copper alloy tubing is usually extruded. It is easily bent in the annealed condition, and it has little springback. Copper and some brasses may not need to be annealed. Copper-nickel alloys, however, are more difficult to bend and have greater springback.

When copper alloys are annealed, as most of them are, oxides should be removed by pickling before the tube is bent to protect the tooling. Oxides increase friction and wear in bending.

Aluminum alloy tubing, like copper alloy tubing, is usually extruded, or extruded and drawn. Soft aluminum may tear or collapse upon bending. The oxide coating that forms on exposed surfaces of aluminum alloys is abrasive to tooling. Lubrication prolongs tool life.

Anodized aluminum and decorated aluminum can usually be bent without damaging the finish. Aluminum pipe is bent by hand to a radius usually not less than four times the outside diameter. The bending of aluminum tubing is discussed in the article "Forming of Aluminum Alloys" in this Volume.

Bending Thin-Wall Tubes

The techniques used to bend thin-wall tubes are the same as those used to bend standard tube and pipe, but they are more carefully applied. A tube can be classified as thin wall if the ratio of outside diameter to wall thickness (*D*/*t*) is greater than 30 to 1. The wall thickness, if not related to the tube diameter, is a meaningless measure. For example, a tube wall 0.51 mm (0.020 in.) thick would be a standard wall thickness for a tube 3.2 mm ($\frac{1}{8}$ in.) in outside diameter, but for a 152 mm (6 in.) tube it would be a very thin wall. The centerline radii given in Table 5 for bending tubing of various *D*/*t* ratios with a ball mandrel and a wiper die are conservative and are often exceeded.

Table 5 Average practical centerline radii for bending thin-wall steel tubing with a ball mandrel and wiper die

Tubing outside	Average centerline radii for tubing with wall thickness, mm (in.), of:

mm	in.	**0.89 mm**	**(0.035) in.**	**1.24 mm**	**(0.049) in.**	**1.65 mm**	**(0.065) in.**	**2.11 mm**	**(0.083) in.**	**2.36 mm**	**(0.093) in.**	**3.05 mm**	(0.120) in.
13	$\frac{1}{2}$	13	$\frac{1}{2}$ (a)	13	$\frac{1}{2}$ (a)	...	...	...	...	...	...	...	...
16	$\frac{5}{8}$	16	$\frac{5}{8}$ (a)	16	$\frac{5}{8}$ (a)	...	...	...	...	...	...	...	...
19	$\frac{3}{4}$	19	$\frac{3}{4}$	19	$\frac{3}{4}$ (a)	19	$\frac{3}{4}$ (a)	...	...	...	...		
22	$\frac{7}{8}$	32	$1\frac{1}{4}$	29	$1\frac{1}{8}$	25	1 (a)	...	...	...	...	...	...
25	1	44	$1\frac{3}{4}$	38	$1\frac{1}{2}$	32	$1\frac{1}{4}$	29	$1\frac{1}{8}$ (a)	...	...	...	...
29	$1\frac{1}{8}$	64	$2\frac{1}{2}$	50	2	44	$1\frac{3}{4}$	38	$1\frac{1}{2}$	...	...	...	...
32	$1\frac{1}{4}$	98	$3\frac{7}{8}$	89	$3\frac{1}{2}$	75	3	64	$2\frac{1}{2}$	50	2	...	...
38	$1\frac{1}{2}$	127	5	108	$4\frac{1}{4}$	95	$3\frac{3}{4}$	83	$3\frac{1}{4}$	70	$2\frac{3}{4}$	57	$2\frac{1}{4}$
50	2	229	9	203	8	178	7	152	6	127	5	89	$3\frac{1}{2}$
64	$2\frac{1}{2}$	305	12	267	$10\frac{1}{2}$	235	$9\frac{1}{4}$	203	8	165	$6\frac{1}{2}$	127	**5**
75	**3**	**381**	**15**	**330**	**13**	**279**	**11**	**254**	**10**	**229**	**9**	**203**	**8**

(a) No wiper die required

Machines used to bend thin-wall tubing have a greater capacity than necessary so that they will be stable and rigid. Their bending action must be smooth and steady. Run-out on machine spindles should not exceed 0.013 mm (0.0005 in.). Mandrel rods must be heavy enough so that they do not stretch or buckle when the slip-fitting mandrel is inserted into the tubing.

Auxiliary equipment includes:

- A means for pressurizing the tubes with air or hydraulic oil (hydrostatic mandrel) to keep them from necking after they are drawn past the last mandrel ball
- Hydraulic feed on pressure dies to hold tubes in compression

- Mandrel oscillators that move the mandrel back and forth to keep the tubes from necking down

The amplitude of mandrel oscillation can be adjusted from 3.2 to 25 mm ($\frac{1}{8}$ to 1 in.); frequency, from 1 to 500 cpm.

Tools for bending thin-wall tubes must be more accurately made than those for standard tubes. The form block or bending die should have a run-out at the bottom of the groove of not more than 0.025 mm (0.001 in.). The depth of the groove should equal 55% of the outside diameter of the tube. The width of the groove should equal the outside diameter of the tube plus 10% of the wall thickness. The width of the clamping groove on the bending die should equal the outside diameter of the tube minus 10% of the wall thickness. The length of the clamping groove should be five to six times the outside diameter of the tube unless special clamping provisions such as flaring or clamping cleats are included. The clamp and the bending die can be keyed or doweled for perfect alignment. Clamping plugs are sometimes used; these should either be slip fitted in the tube or expandable.

The pressure die should have a groove wider than the tube outside diameter by an amount equal to 15% of the wall thickness. The width should not vary from end to end by more than 0.013 mm (0.0005 in.). Variation in the groove will cause a pinching or relieving effect. If all tools are properly adjusted, only light pressure is needed on the pressure die, which can be adjusted against a solid bar with the same diameter as the outside diameter of the tube.

The wiper die has a groove whose width is equal to the outside diameter of the tube plus 10% of the wall thickness. The groove should be highly polished and have a thin coat of light oil. Too much or too heavy oil will cause wrinkles. The groove must be a full half-circle in cross section to support the entire inner half of the tube. The groove must also fit closely to the form die for at least 15° back of the point of bend so that it cannot be forced away by the pressure buildup of the compressed inner wall of the bend. Failure to maintain the position of the wiper die can cause wrinkles.

Multiball mandrels are generally used with thin-wall tubes. They have a clearance no greater than 10% of the wall thickness of the tube. The mandrel must be positioned very carefully so that the full diameter of the body is just at the start of the bend (first ball of cable or ball-socket mandrels at the bend tangent). A template should be used to set the mandrel. If auxiliary oil or air pressure in the tube is not used, there must be enough balls to reach completely around the bend.

Interlock tooling is sometimes used for bending thin-wall tubes. The clamp is keyed to the form block, the wiper die is locked to the pressure die, and the pressure die is locked into alignment with the form block. Interlock tooling was specifically developed for automatic bending; but it has some advantages for general bending. The tools will not crush or mark the work, and setup time and scrap can be reduced.

Material should be especially uniform in thin-wall tubing that is to be bent and should all be from the same source--preferably the same heat. Because tooling dimensions are held closely, close-tolerance tubing is recommended despite its added cost.

Production Example. Thin-wall tubing is frequently bent to elbows that have a centerline radius equal to the diameter, and it is not uncommon for the diameter to be as much as 90 times greater than the wall thickness--for example, a 152 mm (6 in.) diam tube with a 1.65 mm (0.065 in.) thick wall. Many such elbows are used in vacuum-line service, an application in which no wrinkles are permitted. They are commonly made from 1020 steel tubing in the as-received condition. Bends are made with ball mandrels, wiper dies, and an oil-base lubricant. Some manufacturers of elbows use chromium-plated tools to minimize tool wear.

It is often difficult to prevent thin-wall tubing from slipping during bending. Methods used to provide adequate clamping are described in the following example.

Example 13: Procedures to Prevent Slipping of Tubes During Bending.

The tube shown in Fig. 11 was used in a high-pressure hydraulic circuit of an earthmover. Five bends, ranging from approximately 20 to 86°, were made in a powered compression bender. All bends were made on a 152 mm (6 in.) centerline radius.

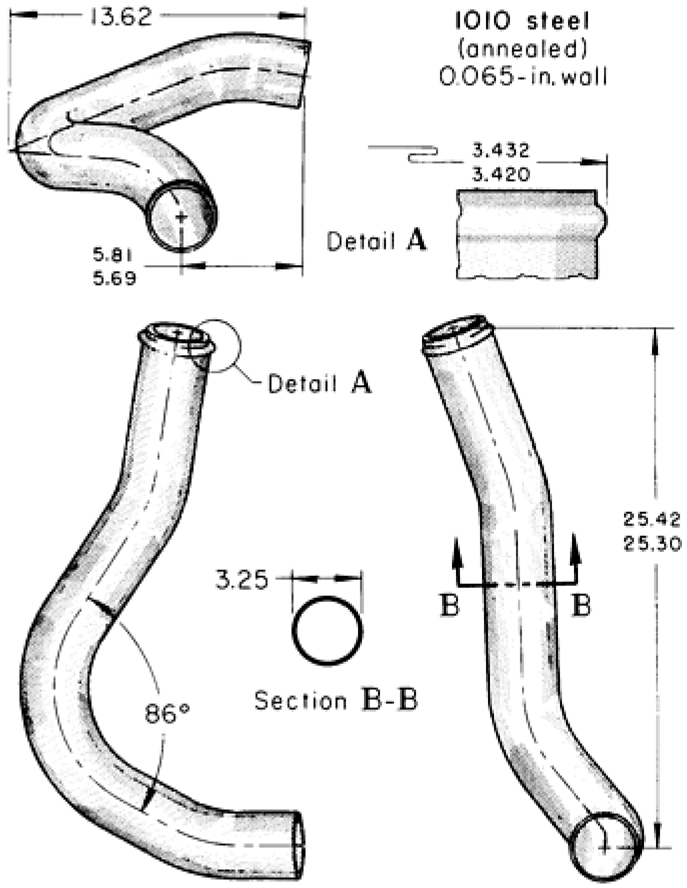

Fig. 11 Component of a hydraulic system that required five bends. Dimensions given in inches.

The 86° bend posed a problem because only 127 mm (5 in.) of tube was available for clamping; adjacent bends were in other planes. Surfaces of the clamps were rough, but the tube slipped during bending, causing unacceptable bends. The first approach was to line the clamps with emery cloth, but this did not add enough friction to prevent slipping. A second approach was to increase the force on the clamp. Until a hydraulic cylinder could be installed to provide this force, a factory lift truck was used. Acceptable bends were produced by increasing the force on the pressure die.

The tubing had a phosphate coating. A three-ball mandrel with an oil hole provided a constant supply of lubricant (a mixture of lard oil and mineral oil) to the inside of the tube. Tools were of W1 or W2 tool steel, hardened.

Lubrication for Tube Bending

Where a mandrel is used, both the mandrel and the interior of the tube are heavily coated with a thick lubricant. Pigmented lubricants are useful for adding body between the mandrel and the tube. Thick lubricants are sometimes heated to 120 °C (250 °F) and sprayed onto the inner surface of the tube. An oil hole in a mandrel can be used to lubricate the inside of a tube during bending (Example 13).

The wiper die, on the other hand, needs only a very light lubricant, applied sparingly, if at all. Nothing must interfere with the close fit between wiper die and tube, which prevents compression wrinkles.

Not all metals react to lubricants in the same way. In general, mineral oils are always acceptable, as are organic fats. Certain sulfur and chlorine additives can stain or corrode stainless steel or copper and should be used with caution. For aluminum, special additives have been developed for use with light or medium mineral oil; the same formulations work well on copper and brass.

Tube Spinning

Revised by Jack D. Stewart, Sr., Stewart Enterprises, Inc.

Introduction

TUBE SPINNING is a rotary-point method of extruding metal much like cone spinning, except that the sine law (see the article "Spinning" in this Volume) does not apply. Because the half angle of a cylinder is zero, tube spinning follows a purely volumetric rule, depending on the practical limits of deformation that the metal can stand without intermediate annealing. Tube spinning is also limited by the smallest percentage reduction in thickness that will ensure complete flow of the metal. This minimum reduction is usually 15 to 25%, depending on the metal and on the thickness of the original tube.

Applicability. Spinning is one method of reducing the wall thickness of tubular shapes and increasing their strength, particularly for aircraft and aerospace applications. Producing specific shapes from tubing is a major function of tube spinning. For example, one or more flanges can be spun at selected areas on a tube, often at a savings in labor and material costs when compared with other processes such as machining. Tube spinning has also been used because ring forgings having the desired relationship between wall thickness and length were not available.

All ductile work metals are suitable for tube spinning; the practical ranges of compositions and strengths are approximately the same as for the power spinning of cones. Metals as hard as 35 HRC have been successfully spun. Most tube spinning is accomplished without heating the workpiece.

The amount of wall reduction that can be accomplished without intermediate annealing is given for a number of metals in Table 2 in the article "Spinning" in this Volume. The amount of permissible reduction is often greater for the spinning of tubes than for the spinning of cones or hemispheres, particularly in the case of backward spinning.

The range of tube sizes that can be spun depends primarily on the available equipment. Tubelike preforms that have an inside diameter in a range from 4.75 mm to 3 m (0.187 to 120 in.) have been successfully spun. Wall thicknesses of the starting tubes are often as great as 25.4 mm (1.0 in.) for steel and 31.8 mm (1.25 in.) for aluminum and under ideal circumstances have exceeded 41.3 mm ($1\frac{5}{8}$ in.) for steel and 63.5 mm ($2\frac{1}{2}$ in.) for aluminum.

The minimum size of tube that can be spun is more likely to be a limiting factor than the maximum size, because of machine characteristics. For example, a large machine is not well suited to spinning small tubing, because it has insufficient spindle speed. The relationship between spindle speed and tube size should be such that a minimum of 120 m/min (400 sfm) can be obtained. Very small (<9.5 mm, or 0.38 in., diam) tubes are usually spun on machines that hold the tube stationary while the tool rings rotate around it.

Preform Requirements. Preform is the name commonly applied to a tube or a tubular shape before it is spun. A preform may be a straight, symmetrical tube, or it may have been changed in shape by the addition of an internal flange for clamping. Tubular shapes used for spinning include forged or centrifugally cast tubes (both of which are completely machined before spinning), welded tubing, seamless tubing, and extruded tubing.

For spinning, the inside diameter should vary no more than ±0.051 mm (±0.002 in.) on tubes up to 76 mm (3 in.) in inside diameter or no more than ±0.152 mm (±0.006 in.) on tubes with inside diameters of 75 to 510 mm (3 to 20 in.). Tolerances of +0.5, -0 mm (+0.020, -0 in.) are typical for tubes 635 mm to 1.27 m (25 to 50 in.) in diameter; tolerances of +0.75, -0 mm (+0.030, -0 in.) are used for larger sizes. The roundness and wall thickness of the preform will also affect tolerance. A 41.3-mm ($1\frac{5}{8}$-in.) wall thickness, 3-m (120-in.) preform will usually have at least 1.7 mm (0.067 in.), and frequently 4.75 mm (0.187 in.), of positive tolerance to permit loading.

The wall thickness of the preform should be within ±0.075 mm (±0.003 in.) unless the preform is machined all over in which case wall thickness should be within ±0.025 mm (±0.001 in.). Ovality should be within 0.05 mm (0.002 in.) for small-diameter preforms within 0.30 mm (0.012 in.) for large diameters.

Tube Spinning

Revised by Jack D. Stewart, Sr., Stewart Enterprises, Inc.

Methods of Tube Spinning

Two distinctly different techniques are used for tube spinning; namely, backward and forward. They are so termed because of the directional relationships between metal flow and tool travel. In both methods, the workpiece is fixed in one position at one end, and the remaining length is free to slide along the mandrel.

Backward Spinning. In backward spinning, the workpiece is held against a fixture on the headstock, the roller advances toward the fixed end of the workpiece, and the work metal flows in the opposite direction (Fig. 1a). Two advantages of backward spinning over forward spinning are:

- The preform is simpler for backward spinning because it slides over the mandrel and does not require an internal flange for clamping
- The roller traverses only 50% of the length of the finished tube in making a 50% reduction of the tube wall, and only 25% of the final length for a 75% reduction

The latter advantage not only increases production but also allows workpieces to be spun that are beyond the normal capacity of the machine. For example, a machine having only a 1.27-m (50-in.) length of stroke can produce a workpiece 2.54 m (100 in.) long using a 50% reduction. Backward tube spinning is also unique in that the majority force distribution is compressive. Consequently, metals with relatively low ductility can sometimes be backward spun to significant reductions.

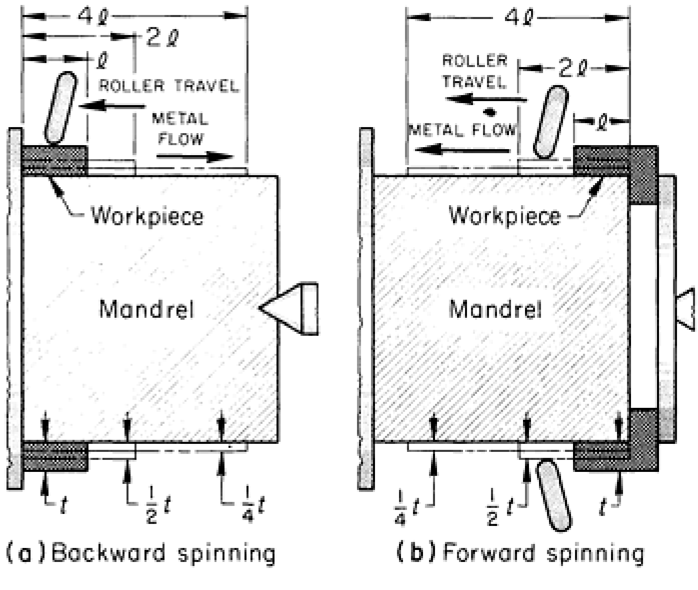

Fig. 1 Metal flow and roller travel in backward and forward tube spinning.

The major disadvantage of backward tube spinning is that the first portion of the spun tube must travel the greatest distance and is therefore the most susceptible to distortion (Fig. 2). This disadvantage is seldom critical when spinning tubes of constant wall thickness. However, when the preform has weld sculptures of substantially greater thickness than the tube wall (as in solid fuel rocket cases), distortion can be a problem. For example, in backward spinning of the tube shown in Fig. 2, one side became an inch longer than the other, even though the preform was essentially perfect and the mandrel was accurate within 0.038 mm (0.0015 in.) total indicator reading.

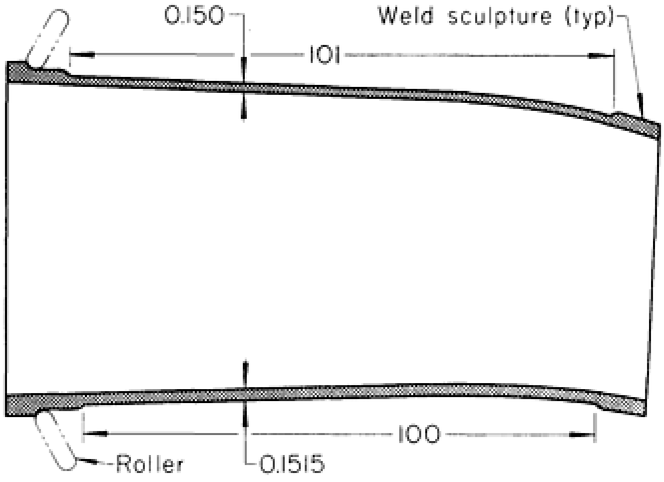

Fig. 2 Example of distortion in backward tube spinning. See text for explanation. Dimensions given in inches.

Forward Spinning. In forward spinning, the roller moves away from the fixed end of the workpiece, and the work metal flows in the same direction as the roller, usually toward the headstock (Fig. 1b). The main advantage in forward spinning as compared to backward spinning is that forward spinning will overcome the problem of distortion (Fig. 2). In forward spinning, closer control of length is possible because as metal is formed under the rollers it is not required to move again, and any variation caused by the variable wall thickness of the preform is continually pushed ahead of the rollers, eventually becoming trim metal beyond the finished length.

The disadvantages of forward spinning are that:

- Some arrangement such as the one illustrated in Fig. 1(b) must be made for clamping the preform to the mandrel at the tailstock end
- Production is slower in forward spinning because the roller must traverse the finished length of the workpiece

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Tube Spinning

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Machines for Tube Spinning

Machines used for tube spinning are usually the same as those used for the power spinning of cones or other shapes. The few special features required for tube spinning are normally specified and can be supplied on all power spinning machines. A power spinning machine will have the same size capacity for tube spinning as for cone spinning. A 1.9 × 2.5-m (75 × 100-in.) machine has the capacity to spin a tube 1.9 m (75 in.) in diameter 2.5 m (100 in.) long. Machines available have the capacity to spin tubes ranging in size from 457 mm (18 in.) in diameter to 3.8 m (150 in.) long. The length dimension applies to the travel of the roller, or tool carriage, so that by using backward tube spinning it is possible to produce a workpiece much longer than is indicated by the size of the machine. The maximum size of the workpiece that can be produced using the backward technique is limited by two considerations:

- The amount of reduction that can be taken on the final spinning operation (a 50% reduction would result in a tube twice as long). The machine force capacity and work metal characteristics will determine the possible reduction
- The clearance provided for workpiece removal. A machine with a capacity of 1.27 m (50 in.) usually

has enough tailstock motion so that a workpiece 1.27 m (50 in.) long can be removed from a mandrel 1.27 m (50 in.) long. Many machines have been built with greater-than-standard tailstock clearance so that backward tube spinning can be used to the greatest advantage

The capacity in terms of force for spinning cones is seldom specified for tube spinning. A machine with the force capability for spinning a 25-mm (1-in.) thick plate into a cone through a 50% reduction in one operation does not have the capability of reducing a tube with a 25-mm (1-in.) thick wall 50% in one pass. Therefore, most tube spinning is done in smaller reductions per pass.

Cone spinning is a shearing-type operation, while tube spinning is similar to extrusion, which requires higher compressive forces. This has been determined by sectioning blanks, scribing the surfaces in block patterns, silver brazing them together, spinning them (both shear and tube), and resectioning to expose the scribed surfaces. In the shear-spun parts, the material is displaced along a shear plane, the square blocks resembling parallelograms; in the tube-spun parts, the squares are elongated in the direction of the axis of the tube, but are compressed in the radial direction.

Most tube spinning is done on machines with two opposed rollers. This practice minimizes the deflection caused by spinning with one roller when the length-to-diameter ratio of the mandrel and workpiece is large. Even on machines employing opposed rollers, when the length-to-diameter ratio is excessively large, deflection of the mandrel is often a problem because the mandrel and workpiece are pushed off center. To counteract this problem, machines have been built with more than two rollers. When three or more rollers are used, they have the same centering effect as a steady rest.

Most modern tube-spinning machines are numerically controlled by computers. For spinning straight tubes, mechanical stops can be used to limit the travel of the cross-slide unit and thus control the diameter of the workpiece. Numerically controlled machines, however, are rarely run against such stops; computer control offers greater flexibility and the advantage of compensating for deflection and taper. At least one very large machine has been retrofitted with laser detectors to compensate for column deflection automatically. Accuracy with this system is in the range of ±0.038-mm (0.0015-in.) wall thickness over more than 2.54 m (100 in.) in length at a nominal 2.54-mm (0.100-in.) wall thickness.

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Tools for Tube Spinning

The tools required for tube spinning are a mandrel, rollers (two are usually required), a puller ring (for removing the workpiece from the mandrel), a drive ring (which can also be used as a puller ring), and a control system (such as computer numerical control or a tracer system).

Mandrels. Many mandrels for tube spinning are made solid. However, as size increases and weight becomes excessive, the usual practice is to hollow them out; this is done by coring if the mandrels are made from castings or by boring if they are made from forgings or bars. Mandrels are sometimes fabricated from several machined components.

Mandrel wear is a major problem because of the severe service to which mandrels are subjected. Wear increases as the strength of the work metal increases or as the wall thickness of the workpiece decreases. The only means of minimizing mandrel wear or deterioration is to make the mandrels from extremely wear-resistant metals.

Alloy cast iron (usually hardened to about 58 HRC) is often used as a mandrel material for limited-production spinning. In many cases, alloy cast iron (sometimes used as-cast) has given acceptable results even for medium-production spinning, provided the work metal is easy to spin and the wall of the as-spun workpiece is not too thin. Conversely, when the application is more severe, alloy cast iron mandrels have been known to fail by spalling and pitting after spinning only a few pieces.

Mandrels made of steels such as 4150 and 52100 hardened to about 60 HRC have proved successful for many spinning applications, particularly when severity, as determined by work metal and wall thickness, is considerably less than maximum. In some applications, it has been desirable to sacrifice some wear resistance to gain toughness in the mandrel. Under these conditions, a hot-work tool steel such as H12 hardened to 52 to 55 HRC has been used. Tool steels such as D2 or D4 hardened to approximately 60 to 62 HRC have proved best for mandrels when service is rigorous, particularly for high-production spinning.

Regardless of what mandrel material is used, best practice calls for a light polishing of the mandrel after every 10 to 20 workpieces. This is done to remove any metal pickup and therefore prevent scratches on the inside diameter of the workpiece.

Rollers used for tube spinning are subjected to rigorous service. Typical tube-spinning rollers (tool rings) are shown in Fig. 3. A surface finish of 0.25 μm (10 μin.) or better is preferred. Most of the rollers used for tube spinning are made from D2 or D4 tool steel hardened to HRC 60 or slightly higher; M4 tool steel at 62 HRC has also given satisfactory service. Rollers of M42 tool steel heat treated to 64 to 65 HRC and quadruple tempered have demonstrated excellent wear resistance. Rollers made in accordance with this practice have been known to last for 4000 to 5000 h when spinning hot rolled tubes of 1020 to 1025 steel.

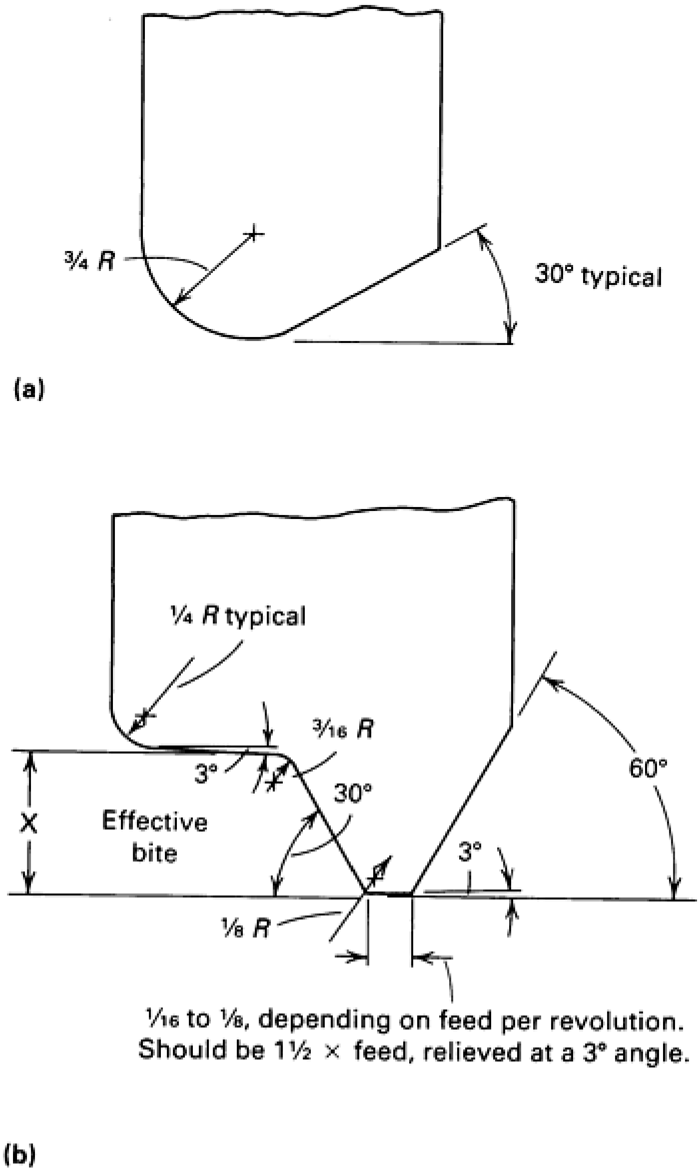

Fig. 3 Typical tool rings (rollers) used in tube spinning. (a) Pure radius tool ring. Common radii range from 9.5 to 25 mm ($\frac{3}{8}$ to 1 in.); sets are usually prepared in 6.4 mm ($\frac{1}{4}$ in.) increments. (b) Typical tube-spinning tool ring designed to operate properly at bite X only. The ring must be changed for each different reduction desired. Dimensions given in inches.

Staggered rollers have been successfully used for the forward spinning of workpieces such as missile cases. The two rollers shown in Fig. 4 are staggered radially so that each takes a portion of the total bite. When this practice is employed, the lead roller takes approximately 30% of the total bite, and the second roller takes the remainder. The only disadvantage is that more power is required because more metal is moved per unit of time.

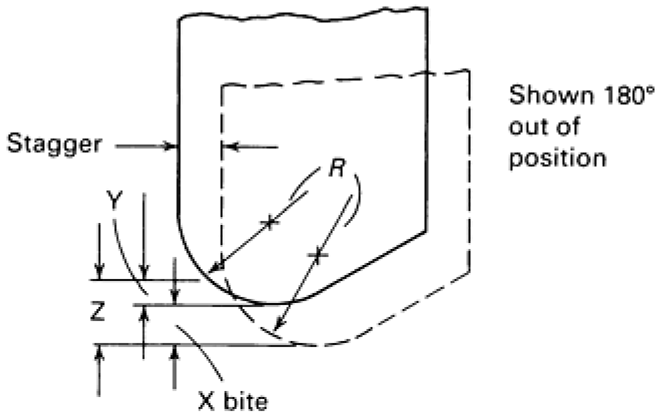

Fig. 4 Typical use of two staggered pure radius tool rings (rollers) for tube spinning. The lead tool ring is normally set for a bite Y that is approximately 30% of the total bite Z.

Auxiliary tools for tube spinning, such as stock pullers and drive rings, are usually made from a low-carbon steel such as 1020.

Tube Spinning

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Tube Wall Thickness Limitations

Limitations on the wall thickness of the preform that is practical for tube spinning are influenced by the formability characteristics of the work metal and available power. Depending on the characteristics of the work metal, the minimum reduction for the first pass is approximately 15%, but for many metals, it is greater than 15%. Because the reduction must always be a percentage of wall thickness, power consumption increases with wall thickness.

Metals that can usually be spun satisfactorily using a wall reduction of 15% on the first pass include low- and medium-carbon steels, alloy steels (including high-strength steels such as D-6ac), and all of the low-carbon stainless steels. With the largest standard machine currently available, the maximum starting wall thickness for the above metals is about 28.6 mm (1.125 in.). Under certain circumstances, however, a larger starting wall thickness may be acceptable. For example, a 3 m (10 ft) diam Inconel ring with a starting wall thickness of 41.3 mm ($1\frac{5}{8}$ in.) has been produced by spinning. The main reason such a starting wall thickness could be used is the large diameter of the preform; there is a relationship between preform starting wall thickness and diameter. Larger diameters permit thicker walls up to the machine power limits.

Successful spinning of the softer metals, such as the aluminum alloys, requires a higher percentage of reduction per pass (30% minimum) to prevent the formation of a large burr at the leading edges of the rollers. However, less force is required to spin aluminum. Therefore, for spinning aluminum alloys 2014 and 2024, using the largest available machine, the wall thickness of the preform can still be as great as 28.6 mm (1.125 in.), even though the percentage reduction is greater than for higher-strength metals.

To spin extremely soft metals such as 3003 aluminum, percentage reduction per pass must be increased. For spinning this grade of aluminum or other metals at a similar hardness, about 12.7 mm (0.500 in.) is the maximum wall thickness of the preform. Spinning of thicker-wall preforms of soft metals has been accomplished, but subsequent machining operations were required to produce acceptable surfaces.

Minimum Wall Thickness. The minimum thickness of preform wall that can be successfully spun is not clearly established, although small preforms with walls as thin as 1.0 mm (0.040 in.) have been spun. Spinning of extremely thin-wall preforms is infrequent.

Dimensional accuracy of the wall after spinning is not appreciably affected by preform thickness, provided total reduction between process anneals is no greater than 80%.

Tube Spinning

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Effect of Machine Variables in Tube Spinning

The two basic machine variables that affect workpiece accuracy are feed per revolution and machine deflection.

Feed per revolution is of concern during the development phase of a tube-spinning operation because it directly affects finished wall thickness, surface finish, and workpiece diameter. Increased feed per revolution will produce a workpiece having greater wall thickness, smaller inside diameter (inside diameter lighter to the mandrel), and rougher surface finish.

Because feed per revolution and roller radius or bite are interrelated, the effect of changes in feed per revolution can be modified by changes in the dimensions of the roller. An increase in roller radius produces thicker walls, larger inside diameters, and finer surface finishes on spun parts. Therefore, except for the effect on wall thickness, an increase in roller-to-workpiece contact area through the use of increased roller radius has the same effect as a decrease in feed per revolution. Similarly, increasing feed from 0.76 to 1.27 mm/rev (0.030 to 0.050 ipr) and increasing roller radius from 6.35 to 12.7 mm (0.250 to 0.500 in.) may cause an increase in wall thickness, but will have little or no effect on inside diameter and surface finish. To maintain uniform wall thickness, the distance between the roller and the mandrel must be decreased to compensate for the increased roller deflection that results from increases in feed per revolution or from increased area of roller contact (larger radius).

Machine deflection varies among different machines and must be determined by experimentation for each different setup. To some extent, deflection can be established for a particular size of machine. For example, it has been determined that a 1.07 × 1.27 m (42 × 50 in.) movable-slide machine will have approximately 0.64 mm (0.025 in.) of deflection when reducing D-6ac steel from a wall thickness of 2.92 to 1.78 mm (0.115 to 0.070 in.). However, roller shape, roller setting, and feed per revolution can cause a variation in deflection ranging from 0.25 to 1.27 mm (0.010 to 0.050 in.). Whether or not rollers are staggered (Fig. 4) has a marked effect on the magnitude of machine deflection.

Further variation in deflection among makes and models of machines can result from slight variations in the mechanical condition of slides, roller-synchronizing systems, hydraulic circuits, or gear trains. These variables primarily affect the uniformity of feed per revolution; if uniformity is not maintained during spinning the result will be a workpiece with varying wall thickness.

Deflection must be compensated for. One technique for controlling workpiece diameter involves adjusting the feed per revolution.

Machine variables cause the most difficulty in unit or low production. Once settings have been established for the spinning of a given workpiece on a specific machine, it remains only to adjust for any changes in roller shape, roller wear, or feed rate. If these adjustments are made, machine variables will not appreciably affect workpiece accuracy during production runs.

Finish of Tube-Spun Parts

Roller radius, composition and condition of work metal, feed per revolution, and lubricants affect the surface finish of tube-spun workpieces.

Roller Radius. Standard rollers for tube spinning can be modified for a specific application to produce workpiece finishes as good as 1 to 2 μm (40 to 80 μin.). Optimum results are usually obtained by modifications in lead angle, relief angle, and width of flat, depending on the work metal and the reduction per pass. When surface finish is of secondary importance, the standard type of roller is preferred because the contact area is low, thus minimizing horse-power requirements. The main disadvantage of standard rollers is that any given shape is restricted to a narrow range of bite; when there is a marked difference in reduction per pass and finish is highly important, it may be necessary to change rollers between passes.

In most tube spinning, optimal surface finish is obtained by using large-radius rollers staggered and offset as shown in Fig. 4. A surface finish of 0.5 to 1 μm (20 to 40 μin.) is common when using this type of tooling. Compared to standard rollers, the large-radius rollers are also better adapted to producing consistent surface finishes where there is substantial variation in percentage of reduction per pass. However, power demand is greater for large-radius rollers than for standard rollers.

Work Metal Variables. Composition and condition of the work metal affect the surface finish obtained in tube spinning. Some work metals are extremely susceptible to burring and tearing. For example, 6061-O aluminum alloy can be reduced only a small amount in one pass because of its susceptibility to burring. However, when this alloy is solution treated and aged, good surface finish can be obtained using reductions of 25 to 30% per pass. For optimal surface finish on some work metals (notably cast preforms of stainless or maraging steels), a common technique is to make one relatively heavy spinning pass (for example, 30%) and then take a light machining cut to remove burrs and tears prior to additional spinning passes. Thick walls in conjunction with very small preform diameters, regardless of other conditions, are likely to result in unacceptable surface finish.

Feed per revolution has a marked effect on surface finish in tube spinning. For an otherwise established set of conditions, surface finish becomes rougher as feed rate is increased. In most tube spinning, dimensional accuracy is more important than finish and must be given primary consideration in establishing rates of feed.

Lubricants. The effect of lubrication on surface finish is less important than might be expected, although efficient lubrication is recommended for obtaining an optimal finish. During tube spinning, the most reliable indication of lubricant efficiency is obtained by observing the lubricant pattern on the mandrel after workpiece removal. A thin, even film is ideal. Dry spots indicate poor application, or breakdown during forming. Such conditions will result in scratches in the workpiece inside diameter and excessive deterioration of the mandrel. Lubricants and coolants for all power spinning are discussed in the article "Spinning" in this Volume.

Tube Spinning

Revised by Jack D. Stewart, Sr., Stewart Enterprises, Inc.

Speeds and Feeds for Tube Spinning

The technique for controlling speed is less complex for tube spinning than for cone spinning because in tube spinning there is no substantial change in diameter during a spinning cycle. In tube spinning, best results are obtained by using speeds considerably higher than would be used for most metal-cutting operations. Speeds used in practice vary widely and depend greatly on the capabilities of the machine; 120 m/min (400 sfm) is about the minimum speed for best results in any tube-spinning operation, and speeds much higher than 120 m/min (400 sfm) are usually preferred. The minimum speed of 120 m/min (400 sfm) often limits the minimum size of the tube that can be spun because of limitations of spindle speed. Speeds of 180 to 360 m/min (600 to 1200 sfm) are most common, mainly because this range is more compatible with spindle speeds for the size of the work being spun.

Maximum speed is not critical for tube spinning until adequate coolant cannot reach the roller contact points and the workpiece overheats. Similarly, there is no close correlation between work metal composition and spinning speed.

Feeds for tube spinning may be expressed either as millimeters (inches) per revolution or as millimeters (inches) per minute. Feeds used in practice cover a range as great as 38 to 380 mm/min (1.5 to 15 ipm). In terms of millimeters per revolution, feeds vary from approximately 0.076 to 0.20 mm/rev (0.003 to 0.080 in./rev). However, most tube spinning is done at the lower rates of feed.

A higher feed rate usually results in a coarser finish on the workpiece. Feeds are often adjusted as required to obtain a specified dimension.

Tube Spinning

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Effects of Tube Spinning on Work Metal Properties

Tube spinning cold works the metal and has a marked effect on its properties; the magnitude of change depends on the percentage reduction and the susceptibility of the metal to work hardening. Table 1 shows how percentage reduction affects the tensile strength, yield strength, and elongation of 1015 steel tube.

Table 1 Effect of increasing reduction in wall thickness on mechanical properties of 1015 steel tube

Preform was 184 mm ($7\frac{1}{4}$ in.) in outside diameter by 12.7 mm ($\frac{1}{2}$ in.) in wall thickness.

Wall thickness		Total reduction, %	Tensile strength		Yield strength		Elongation, %, in 50 mm (2 in.)
mm	in.		MPa	ksi	MPa	ksi	
12.73	0.501	0	386	56	229	33.2	**34.5**
10.46	0.412	17.5	541	78.5	476	69.4	**13.5**
8.33	0.328	35	572	83	525	76.2	**12.5**
4.90	0.193	61	598	86.8	542	78.6	**11**

The change in strength and ductility for several metals after various percentages of reduction of wall thickness is given in Table 2; the influence of composition is evident. For example, 4130 steel during a wall reduction of 80% increased 72% in tensile strength and decreased 71% in ductility (measured by elongation). Type 304 stainless steel subjected to the same wall reduction increased 202% in tensile strength and decreased 88% in elongation.

Table 2 Mechanical properties of various alloys before and after power spinning

Alloy	Reduction in wall thickness, %	Tensile strength, MPa (ksi)		Yield strength, MPa (ksi)		Elongation in 50 mm (2 in.), %	
		Before	**After**	**Before**	**After**	**Before**	After
1010 to 1020 steel, hot rolled	50	386 (56)	600 (87)	228 (33)	541 (79)	34.5	**11**
1020 steel, cold rolled	65	421 (61)	758 (110)	310 (45)	621 (90)	30	**9**
1045 steel	70	518 (75)	793 (115)	351 (51)	703 (102)	30	**8**
Alloy steel (Fe-0.44C-1.49Si-1.98Cr-0.48Mo)	91	758 (110)	1910 (277)	534 (77.5)	1600 (232)	19.3	**9**
4130 steel	80	560 (81)	965 (140)	360 (52)	721 (104.5)	28	**8**
Type 304 stainless steel	80	593 (86)	1792 (260)	233 (34)	1172 (170)	65.6	**8**

Tube Spinning

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Tube Spinnability

The spinnability of a metal is the maximum reduction it can withstand before failure during spinning. A test setup for determining tube spinnability by forward spinning is shown in Fig. 5. The roller path was set at an angle, ϕ, of 2 to 4° such that the wall thickness of the tube was gradually reduced from t_0 to t_f, where the tube failed. Typical sections from the tests (Fig. 6) show that 2024-T4 aluminum fractured under the roller in a brittle manner, while 6061-T6 aluminum, annealed copper, and low-carbon steel all failed in tension behind the roller. A similar transition in the type of failure has also been observed in shear spinning. The complete agreement between maximum reduction in shear and tube spinning is noteworthy. Therefore, as in shear spinning, maximum reduction in tube spinning can be estimated from the reduction of area in a tension test.

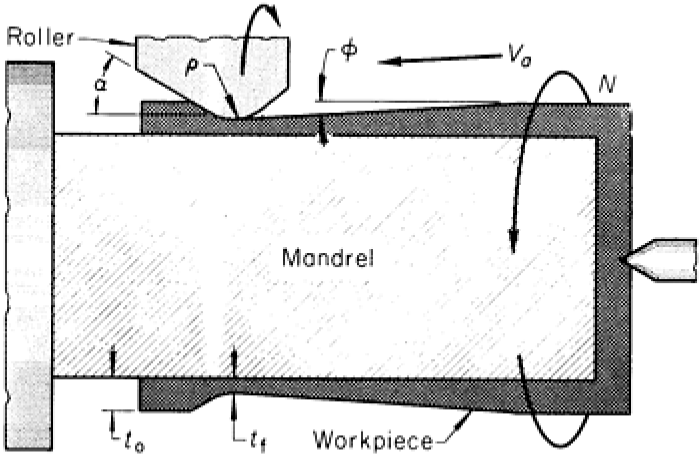

Fig. 5 Setup for testing tube spinnability.

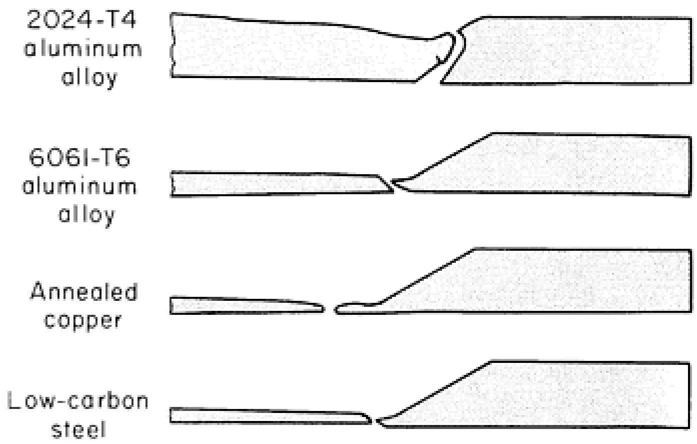

Fig. 6 Locations of fractures in tubes of four different metals tested for spinnability.

In studies of the effects of feed, roller corner radius, and roller angle on maximum tube-spinning reduction, only feed had an adverse influence; the other variables had no appreciable influence. Up to a tensile reduction of area of approximately 45%, maximum reduction in tube spinning depends on the ductility of the metal, and beyond this range, there is a maximum spinning reduction of about 80% regardless of the ductility of the work metal.

Straightening of Bars, Shapes, and Long Parts

Introduction

BARS, bar sections, structural shapes, and long parts are straightened by bending, twisting, or stretching. Deviation from straightness in round bars can be expressed either as camber (deviation from a straight line) or as total indicator reading (TIR) per unit of length. Total indicator reading, which is twice the camber, is measured by rotating a round bar on its axis on rollers or centers and recording the needle travel on a dial gage placed in contact with the bar surface, generally midway between the supports. The indicator reading divided by the distance between the supports gives the straightness in total indicator reading per unit of length. Alternatively, the deviation is expressed in terms of the distance between the supports. The effect that changing the distance between supports has on the reading is illustrated in Fig. 1; the difference in readings illustrates the importance of including support distance and location of indicators in a straightness specification.

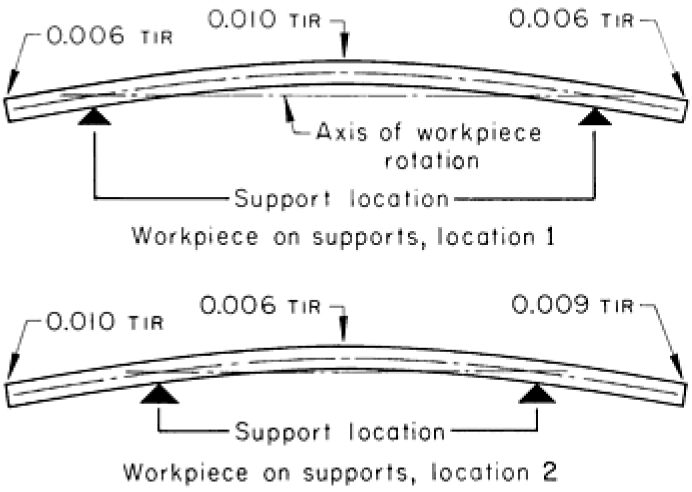

Fig. 1 Effect of distance between supports on straightness readings for round bars.

Sections other than round are usually checked for camber by placing a straightedge against the bar and measuring with suitable gages the distance between the straightedge and the bar at the midpoint of its length. In flat bars and structural members, camber is sometimes referred to as the deviation from straightness parallel to the width, and bow as the deviation parallel to the thickness (Fig. 2).

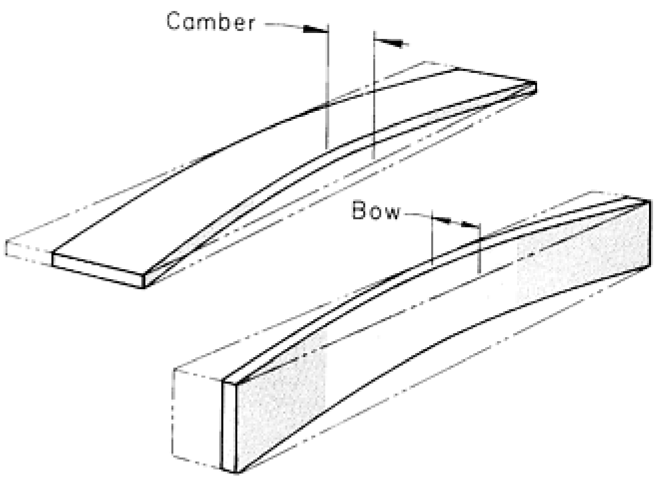

Fig. 2 Camber and bow in flat bars.

Runout is minimal at the nodes of curvature. If a bar has compound curvature, it can be misleading to check for camber or total indicator reading only at the midpoint of the bar length. Serious errors may result unless dial-gage readings are taken at short intervals over the entire length of the bar, or unless gangs of indicators are used at short intervals along the bar.

Straightness Tolerances. Federal Specification 48 ("Tolerances for Steel and Iron Wrought Products") establishes the straightness tolerances on some steels as:

- Hot-rolled carbon steel bars: 6.4 mm deviation per 1.5 m ($\frac{1}{4}$ in. deviation per 5 ft) or 4.2 mm per m (0.050 in. per ft)
- Hot-rolled alloy steel and high-strength low-alloy steel bars: 3.2 mm deviation per 1.5 m ($\frac{1}{8}$ in. deviation per 5 ft) or 2.1 mm per m (0.025 in. per ft)

- Hot-rolled stainless steel and heat-resisting steel bars for machining: 3.2 mm deviation per 1.5 m ($\frac{1}{8}$ in. deviation per 5 ft) but not to exceed 2.1 mm per m (0.025 in. per ft)
- Cold-finished carbon steel bars; turned, ground, and polished bars, or drawn, ground, and polished bars: machine straightened within limits (1.6 mm deviation per 1.5 m or $\frac{1}{16}$in. deviation per 5 ft) reasonable for satisfactory machining in an automatic bar machine
- Cold-finished stainless steel and heat-resistant steel bars for machining: 1.6 mm deviation per 1.5 m ($\frac{1}{16}$ in. deviation per 5 ft) but not to exceed 1.0 mm per m (0.0125 in. per ft)
- Carbon steel, stainless steel, and heat-resistant steel structural shapes (except wide flange sections): 2.1 mm deviation per m (0.025 in. deviation per ft)
- Wide flange sections used as beams: 1.0 mm deviation per m (0.0125 in. deviation per ft)
- Wide flange sections used as columns: up to 14 m (45 ft) long, 1.0 mm deviation per m (0.0125 in. deviation per ft) but not over 9.5 mm ($\frac{3}{8}$ in.); over 14 m (45 ft) long, 9.5 mm ($\frac{3}{8}$ in.) plus 1.0 mm per m (0.0125 in. per ft) beyond the 14 m (45 ft) length

Material Displacement Straightening

Manual Straightening. The original method of hand straightening is still extensively used when accuracy and precision are required or when the shape of the bar or part makes machine straightening impractical. The tools used in manual straightening include hammers and mallets, anvils, surface tables, vises, levers, grooved blocks, grooved rolls, twisting devices, various fixtures, and heating torches. The use of a grooved block (Fig. 3a) illustrates the basic principle of manual straightening by bending.

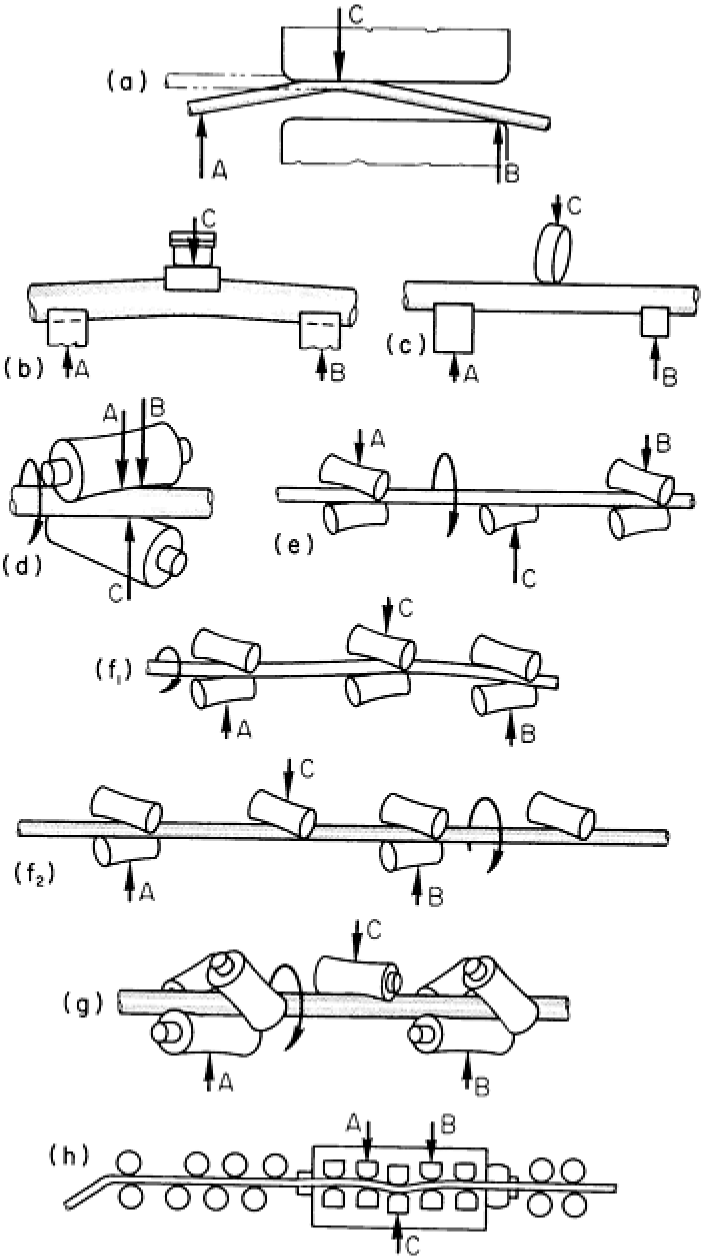

Fig. 3 Principle of straightening by bending. (a) Manual straightening with a grooved block. (b) Straightening in a press. (c) Simplest form of rotary straightening. (d) Two-roll straightening. (e) Five-roll straightening. (f_1 and f_2) Two arrangements of rolls for six-roll straightening. (g) Seven-roll straightening. (h) Wire straightening. In all methods shown, the bar is supported at points A and B, and force at C on the convex side causes straightening. See text and subsequent illustrations for details of the straightening methods shown here.

Shafts for centrifugal irrigation pumps are an example of parts that usually must be manually straightened because of the accuracy required and the necessity of doing the work at the installation site. Most of these shafts are 3.0 to 6.1 m (10 to 20 ft) long, have diameters of 19 to 50 mm ($\frac{3}{4}$ to 2 in.), and are of cold-drawn 1045 steel. The steel supplier generally straightens the cold-drawn stock within 0.13 or 0.25 mm (0.005 or 0.010 in.) TIR in 3.0 m (10 ft), but the shafts are often bent slightly in transport and in handling. It is common to hand straighten the shafts at installation--within 0.13 mm (0.005 in.) TIR in 6.1 m (20 ft). The shafts are rotated on supports and are deflected with a lever.

Special cold-drawn sections as long as 3.7 m (12 ft) are commonly straightened manually. Many special sections are similar enough to standard flats that they can be straightened in standard two-direction roll straighteners, but quantities are often too small to warrant the cost of special rolls. Other special sections may be too complex in shape for machine straightening.

Special sections almost always have twist after cold drawing. The twist must be removed before the section can be straightened. One end of the section is held in a vise or in a special fixture, while the other end is twisted with a wrench or special handle. When the twist is corrected, the bar can be straightened to remove camber and bow.

Manual Peening. Straightening of a workpiece by peening (material displacement) is accomplished manually by placing the workpiece on heavy, flat plates and rapidly hammering on the concave side of the distorted portion of the workpiece surface. Elimination of the distortion allows the workpiece to lie flat. This method is applicable when straightening round parts with a high degree of hardness, such as drill bit blanks. Unfortunately, the technique is time consuming and does require highly skilled operators.

High-Production Peening. In peen straightening, also known as pulse straightening, the workpiece is pulsed on the low side opposite the high point of the bend (Fig. 4). The pulsating tooling compresses the workpiece material and burnishes its surface. To counteract existing stresses in the workpiece, the material expands to straighten the part. The metal structure is stabilized, the part retains its straight configuration in storage, and the material is unaffected by the shock and vibration encountered during subsequent machining operations. This method is gaining wide acceptance in high-volume production operations in which cast and forged steel camshafts, hardened steel transmission shafts, crankshafts, and disk plates require straightening.

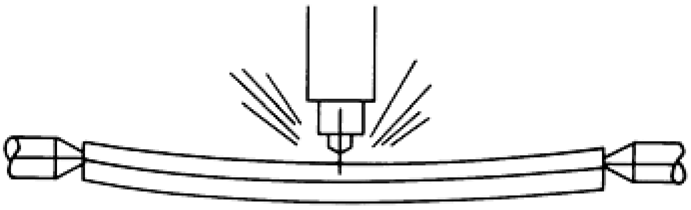

Fig. 4 Schematic of principles involved in straightening by automatic peening.

Stretch Straightening. Many bars and shapes can be easily straightened by stretching. However, this technique is usually confined to the straightening of shapes that are uniform in cross section and length. The advantages of stretch straightening include low costs for tooling and for maintenance, simplicity of operation, and (usually) completion of straightening in one operation. The disadvantages include waste by trimming 152 to 467 mm (6 to 18 in.) from the ends of bars damaged by gripping, the need (usually) for two men to do the straightening, the need for cutoff equipment, and production of only 30 to 40 bars per hour.

A stretch straightener has two heads with grips that clamp on the ends of the bar. One head can be adjusted to suit the workpiece length. The other head (tailstock) is powered for stretching and for rotation to correct twist in the workpiece.

Stretching machines are made in sizes to exert stretching forces of 135 to 4450 kN (15 to 500 tonf) to workpieces that may be from 6 to 30 m (20 to 100 ft) long. Stretching must stress the work beyond its yield strength. For complete straightening, the bar should be stretched 2%. To accomplish this and to overcome the greater strength caused by work hardening, the stretching machine needs a capacity of 10 to 15% beyond the yield strength of the bar.

Some straightening can be done by stretching only to the yield strength of the work, but this would not be sufficient to remove completely some sharp bends and twists. A stretcher with a capacity of 1340 kN (150 tonf) can straighten, to some extent, a low-carbon steel bar with a cross section of 9700 mm^2 (15 $in.^2$), 6450 mm^2 (10 $in.^2$), of austenitic stainless steel, or 4550 mm^2 (7 $in.^2$) of ferritic stainless and can do complete straightening on 8400 mm^2 (13 $in.^2$) of low-carbon steel, 5150 mm^2 (8 $in.^2$) of austenitic stainless, or 3900 mm^2 (6 $in.^2$) of ferritic stainless steel. Figure 5 shows the relationship between stretcher force and the cross-sectional area of the bar. The deviation that remains after stretch straightening can sometimes be corrected by manual straightening.

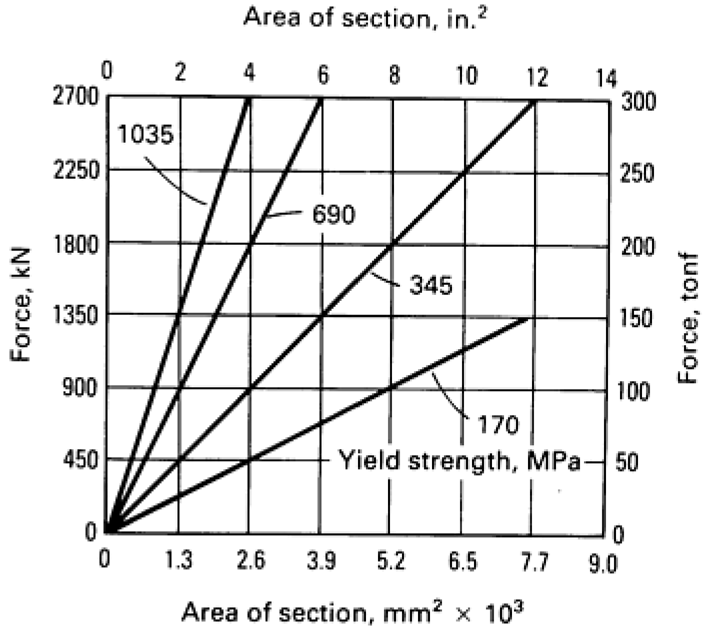

Fig. 5 Relationship between stretcher force and area of bar cross section in the stretch straightening of steel bars of various yield strengths.

Most hot-rolled bars can be straightened by stretching. Straightening by stretching also works well on rolled and extruded bars of aluminum and austenitic and ferritic stainless steels, but not on martensitic stainless steels unless they are first annealed. Low-carbon steels are easy to stretch straighten, but annealing before stretching becomes more necessary as the carbon content increases. Because it is slow and limited in effectiveness, stretch straightening is not widely used.

Straightening by Heating

Alloy steel bars and shapes with a hardness exceeding 50 HRC, as well as fabricated stainless steel parts, frequently warp because of the stress set up during fabrication, machining, or heat treatment. These items can usually be straightened by the application of heat and, in most cases, force. The heat can be localized in the area to be straightened, or the entire piece can be heated--either to the tempering temperature or to about 30 °C (50 °F) below it. (Heating to a temperature above that required for tempering will reduce the hardness, as will prolonged heating at the tempering temperature itself.) Low-carbon steel bars can also be straightened by heating.

Localized Heating. Torches are used to apply heat to the convex side of warped parts. A small area is heated to a dull red. The localized heating causes the workpiece to expand, but some straightening occurs during cooling. Skillful heating, cooling, and gaging of the workpiece can result in reasonable straightness.

Torch heating causes soft spots in hardened steel workpieces. Localized heating with a torch can also cause localized residual tensile stress that can be undesirable even in an unhardened workpiece if it is subjected to cyclic loading.

In press straightening with the use of localized heat, the workpiece is supported at each end with suitable blocks. A stop block is placed directly under the ram to limit the amount of deflection. With the high points of its curvature up, the workpiece is pressed down until it rests lightly on the stop block; heat is then applied. For a heat-treated workpiece, the amount of heat is usually governed by the original tempering temperature, and the distance the workpiece can be deflected and released without fracture depends on the type and hardness of the steel, the heat treatment, and the shape of the workpiece. Another method of controlling deflection without breakage is to place the workpiece on shims while it rests on a flat surface, apply pressure to the surface of the workpiece, then heat and release. If the workpiece is still not straight, it will be necessary to use more shims and reheat or to allow the workpiece to cool longer before releasing the pressure.

Where a flame cannot be effectively directed or may damage the metal, a small weld bead can sometimes be used as the source of heat. Weld beads are applied to the convex area, allowed to cool, and machined off if necessary.

Heating Below Tempering Temperature. Heating and press straightening are generally not applicable to steel at high hardness levels. The force required to cause permanent set is close to the rupture strength of the steel, and even with extreme care, failure is probable. At medium and lower hardness levels, heating to a temperature about 30 °C (50 °F) below the tempering temperature will permit press straightening to be done successfully. Straightening becomes more difficult as the part cools, and only slight corrective straightening should be attempted at the lower temperature levels. Considerable skill is required to perform such operations and to hold tolerances within 0.08 to 0.25 mm (0.003 to 0.010 in.) over a length of 0.45 to 1.22 m (18 to 48 in.).

After the workpiece has been straightened, it is tempered to the required hardness. Tempering relieves stress set up during straightening and during the hardening cycle. This stress will often deform the workpiece; consequently, workpieces straightened by heating and pressing should be clamped in restraining fixtures during tempering. Fixturing can correct a slight distortion and prevent distortion during tempering.

Temper straightening is used to correct the distortion caused by heat treatment. The workpiece is first tempered to a hardness somewhat higher than required, then clamped in a straightening fixture and tempered to the required hardness. The greater the hardness difference between the first and the corrective tempering operations, the more accurate the dimensions will be. Temper straightening is most successful at hardness levels of 55 HRC and lower.

Deep-hardening alloy and tool steels that are being martempered to minimize distortion should be held straight during the cooling period after austenitizing and until the completion of martempering. If straightness is not maintained throughout martempering, the workpiece will warp as martensite continues to form. Straightening should be done below 480 °C (900 °F). Cold bars or chills contacting the high side will more rapidly extract the heat from the workpiece and aid in straightening.

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Straightening in Presses

Round bars up to 50 mm (2 in.) in diameter and from 0.6 to 3.0 m (2 to 10 ft) in length are often straightened in an arbor press. Larger workpieces are similarly straightened in power presses, which may have power rolls and hoists to move the work.

The principle of press straightening is illustrated in Fig. 3(b). The bar to be straightened is supported at points A and B with the convex side of the bow or kink toward point C. Sufficient force is applied at C to cause the bar to become bowed in the opposite direction. The force must be great enough to exceed the elastic limit of the material, but it must set up just enough strain in the bar to allow it to return to the straight position (but no farther) when the pressure is released. The greater the bow in the bar and the higher its elastic limit, the greater the force required to produce the correct amount of strain. To straighten a bar by press straightening, the metal must be capable of cold deformation, and it must strain harden.

In press straightening, the operator usually locates kinks or bows in round bars by holding a piece of chalk close to the surface of the bar and then rotating the bar so that any high spots will be marked by the chalk. The high spot is then brought under the ram of the straightening press, and sufficient force is applied to remove the kink or bow. In shapes other than rounds, the out-of-straight condition must be detected visually or with the aid of a straightedge. This type of straightening requires considerable skill on the part of the operator. A straightening press is sometimes referred to as a gag press and the straightening operation as gagging.

Hydraulic or mechanical presses are used to straighten bars, shapes, and shaftlike parts before, between, and after heat-treating operations. Some bars that are roll straightened do not meet straightness requirements and must receive a final press straightening. Presses are also used to straighten large diameter bars in preparation for turning or grinding. Finish-ground or turned products with highly finished surfaces are press straightened to avoid the spiral marks produced by rotary straightening. Press straightening does not change the size of the bar, but rotary straightening--because of the rolling action or the alteration in residual stress caused by bending or both--may cause a change in bar size.

Some high-strength steels and stainless steels are too hard to be straightened in any way except in a press, and some metals are too hard to be straightened without heat, unless the bar is first annealed. The cold straightening of bars of these metals may cause the bars to break.

Press straightening is easier when the bar has a hardness of less than 40 HRC. The bar can be retempered to relieve the stress introduced during straightening.

In a straightening press, a round bar is usually set on spring-loaded rollers near the ends of the bar. Thus, the bar can be rotated on its axis while a dial gage shows any deviations from straightness. As the press ram moves down, it presses the bar into V-blocks that support the straightening pressure. This action is repeated, sometimes with the bar shifted or the roller supports moved, until the bar is straight enough to meet specifications. The V-blocks and roller supports can be moved to change the leverage and to adjust the application of the force. A straightening press is better suited to the correction of short bends and kinks than to the correction of long bends. A straightening press can hold the distortion of heat-treated bars and shafts to 0.25 mm (0.010 in.) or less, as shown in Examples 1 and 2.

Example 1: Straightening of a Heat-Treated Shaft in a Press.

A D2 tool steel shaft with a hardness of 63 to 65 HRC and a straightness specification of 0.25 m (0.010 in.) TIR is shown in Fig. 6. The shaft was hung in a vertical furnace and preheated to 205 °C (400 °F), 540 °C (1000 °F), and 815 °C (1500 °F) before heating at 540 °C (1850 °F). The part was air cooled to 260 °C (500 °F). Because inspection at that temperature showed distortion of 0.38 to 0.64 mm (0.015 to 0.025 in.) TIR, the bar was straightened in a manual hydraulic press to 0.18 mm (0.007 in.) TIR before it cooled to 205 °C (400 °F). At 65 °C (150 °F), the shaft was again straightened to within 0.18 mm (0.007 in.) TIR.

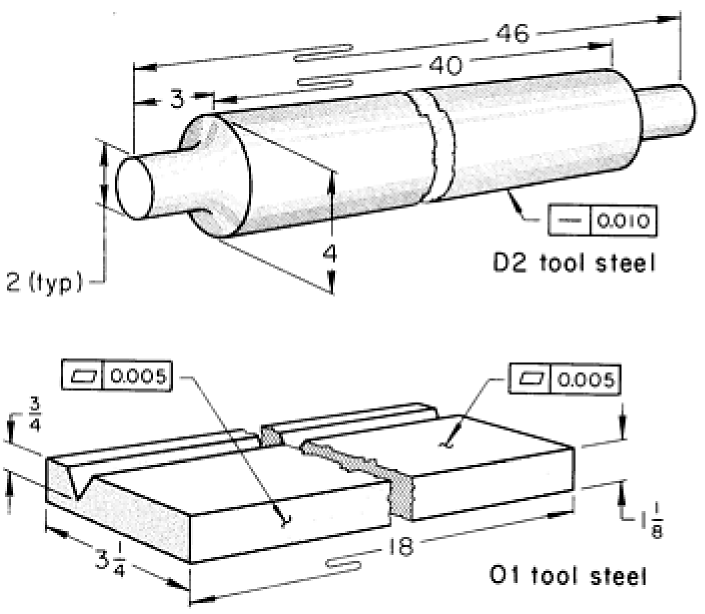


Fig. 6 Shaft and grooved bar that were press straightened after heat treatment. Shaft hardness: 63 to 65 HRC. Bar hardness: 61 to 63 HRC. Dimensions given in inches.

The shaft was clamped in a V-block for tempering at 150 °C (300 °F) for 6 h, then given a subzero treatment and straightened to within 0.20 mm (0.008 in.) TIR. The shaft was again clamped in the V-block, using shims for straightening, while being retempered at 150 °C (300 °F) for 6 h. Final straightening in the press was to 0.18 mm (0.007 in.) TIR, which was better than was required.

Example 2: Straightening of a Heat-Treated Bar in a Press.

A flat bar of O1 tool steel having a 19 mm ($\frac{3}{4}$ in.) deep V-groove along one edge is shown in Fig. 6. The specified hardness was 61 to 63 HRC. The top and sides had to be flat within 0.13 mm (0.005 in.).

The bar was preheated to 595 °C (1100 °F), heated at 805 °C (1480 °F), and marquenched at 190 °C (375 °F). The bar was then clamped in a fixture and cooled to 38 °C (100 °F). Inspection showed 0.15 mm (0.006 in.) maximum variation for the top and side. The bar was reclamped in the fixture and tempered at 150 °C (300 °F) to a hardness of 63 to 64 HRC. The bar was reclamped with shims to straighten it and was retempered at 165 °C (325 °F) to a hardness of 61 to 62 HRC. The bar was then straightened in a press within 0.13 mm (0.005 in.) at the top and side.

Clamping of Workpiece. If the workpieces in Examples 1 and 2 had been heat treated without being clamped, they would have been free to distort and would have needed more press straightening. The straightening of bars and shafts during transformation and during tempering is more efficient and costs less, and it is sometimes the only way in which straightness specifications can be met. Structural parts for aircraft are commonly straightened by a combination of methods, as shown in Examples 3, 4, and 5. Example 6 describes a procedure for heat treating and straightening a long, thin rectangular bar in a press. Round shafts are often straightened before they are ground, as in Example 7 and 8.

Straightening presses are used as accessories to other equipment, such as blooming mills that roll blooms or billets 127 to 178 mm (5 to 7 in.) thick. A 1.8 MN (200 tonf) press with a bed 0.6 × 1.2 m (2 × 4 ft) can straighten such blooms or billets within 6.4 mm per 1.8 m ($\frac{1}{4}$ in. per 6 ft) in lengths as great as 4.9 m (16 ft) without the use of spacers or shims. Many blooms or billets do not need straightening--for example, if they are to be cut into pieces for forging stock.

Example 6 describes a procedure for heat treating and straightening a long, thin rectangular bar in a press. Round shafts are often straightened before they are ground, as in Example 7 and 8.

Example 3:

A structural aircraft part called a cap was made from modified 4330 steel bar 4.7 mm ($\frac{3}{16}$ in.) thick × 1.7 m (66 in.) long (Fig. 7). The part was channel shaped with one flange removed for a portion of its length to produce an angle section. Holes 3.2 mm ($\frac{1}{8}$ in.) in diameter were made in both sides of the angle section and in the channel section. Heat treatment consisted of suspending the workpiece by the angle end in a salt bath at 845 °C (1550 °F), quenching in salt at 245 °C (470 °F), and air cooling. After being heat treated, the parts were cleaned and checked for hardness (aim was 46 to 49 HRC). With hardness less than 50 HRC, a subzero treatment was given prior to tempering; with hardness of 50 HRC or harder, the workpiece was clamped in a fixture, tempered at 315 °C (600 °F) for 5 h, and finally air cooled. The workpiece warped about 25 mm in 1.7 m (1 in. in 66 in.) after quenching, and a warp of about 6.4 mm ($\frac{1}{4}$ in.) remained after fixture tempering. A camber of 6.4 mm ($\frac{1}{4}$ in.) was easily removed manually, but a workpiece with a camber of more than 6.4 mm ($\frac{1}{4}$ in.) was retempered in the fixture before straightening. Shape and straightness were checked in a fixture using a 0.51 mm (0.020 in.) feeler gage. After being tempered at 315 °C (600 °F), the workpiece had a hardness of 46 to 49 HRC.

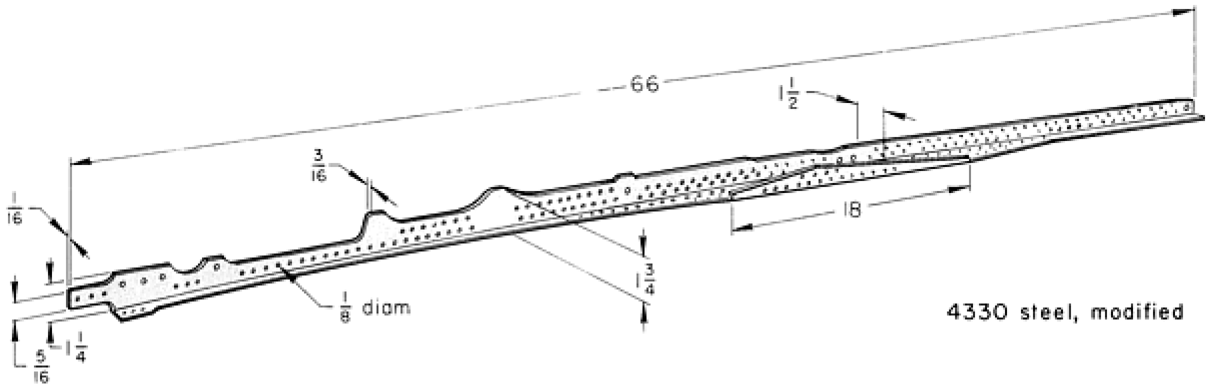


Fig. 7 Aircraft part that was straightened by a combination of methods. Hardness: 46 to 49 HRC. Dimensions given in inches.

Rotation of about 60° was required to correct for 0.76 mm (0.030 in.) twist. An 89 kN (10 tonf) hydraulic press was used to correct short bends. The workpiece was supported on slotted blocks, placed about 360 mm (14 in.) apart, which gave support to the flanges while pressure was applied. A force of about 13 kN (3000 lbf) deflected the channel section 9.5 to 13 mm ($\frac{3}{8}$ to $\frac{1}{2}$ in.) for a camber correction of 3.3 mm/m (0.020 in. per 6 in.). A force of 4.4 to 6.7 kN (1000 to 1500 lbf) on the angle section produced about the same deflection and correction.

Stretching was used to maintain spacing and alignment for the 3.2 mm ($\frac{1}{8}$ in.) diam holes. Because of the thin sections, the part shrank 0.76 to 1.0 mm (0.030 to 0.040 in.) after quenching in salt at 245 °C (470 °F). To correct this shrinkage, the part was preheated at 290 °C (550 °F) for 30 min, then clamped in the tempering fixture and heated at 315 °C (600 °F) for 5 h. As the fixture expanded from the heat, the part was stretched. After slowly cooling in the fixture, the part had a permanent stretch of 0.51 to 0.76 mm (0.020 to 0.030 in.), which corrected hole alignment and spacing. About 8 min was required for clamping the part in the tempering fixture, and 22 min for hand straightening.

Example 4:

A welded double-channel structural member made of 4340 steel 1.8 m (72 in.) long and weighing 5.2 kg (11.5 lb) (Fig. 8) was austenitized at 830 °C (1525 °F) for 40 min, martempered at 245 °C (470 °F), stress relieved at 205 °C (400 °F), and cleaned. The unusual feature of this operation was the simultaneous straightening, bending, and tempering.

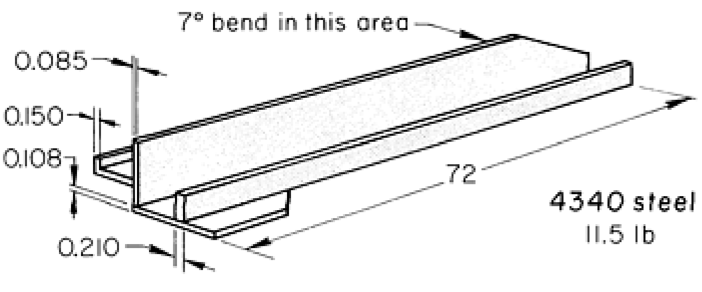


Fig. 8 Welded structural member that was straightened during and after heat treatment. Dimensions given in inches.

The workpiece was machined on a straight plane and then fixture bent 7° on one end during tempering. The fixture was constructed with various gibs and filler blocks milled to fit the contours of the workpiece, including allowance for the 7° bend, which extended for approximately 305 mm (12 in.) (Fig. 8). The workpiece was then placed on the fixture, and all holding clamps were placed in position. The fixture-and-workpiece assembly was then heated to 315 °C (600 °F). At this time, all clamps were tightened, thus making the 7° bend at one end. The assembly was then heated (tempered) to 540 °C (1000 °F for 4 h.

After tempering the channel was straightened in a hydraulic press to achieve final alignment within 0.76 mm (0.030 in.). A special gage was used to inspect alignment and the bend. Time for straightening and gaging was 30 min.

Example 5:

A structural bar with a tapered-width channel 4.8 mm ($\frac{3}{16}$ in.) thick and 1.7 m (66 in.) long was made of 17-4 PH stainless steel (Fig. 9). The part was solution treated to 39 to 42 HRC, finish machined, then aged for 1 h at 480 °C (900 °F). It was straightened to within 0.38 mm (0.015 in.) camber in a hydraulic press upon removal from the aging treatment. The part could be straightened only until it cooled to 370 °C (700 °F), which took 10 min. Because 20 min was required to straighten the part, reheating to 480 °C (900 °F) was necessary.

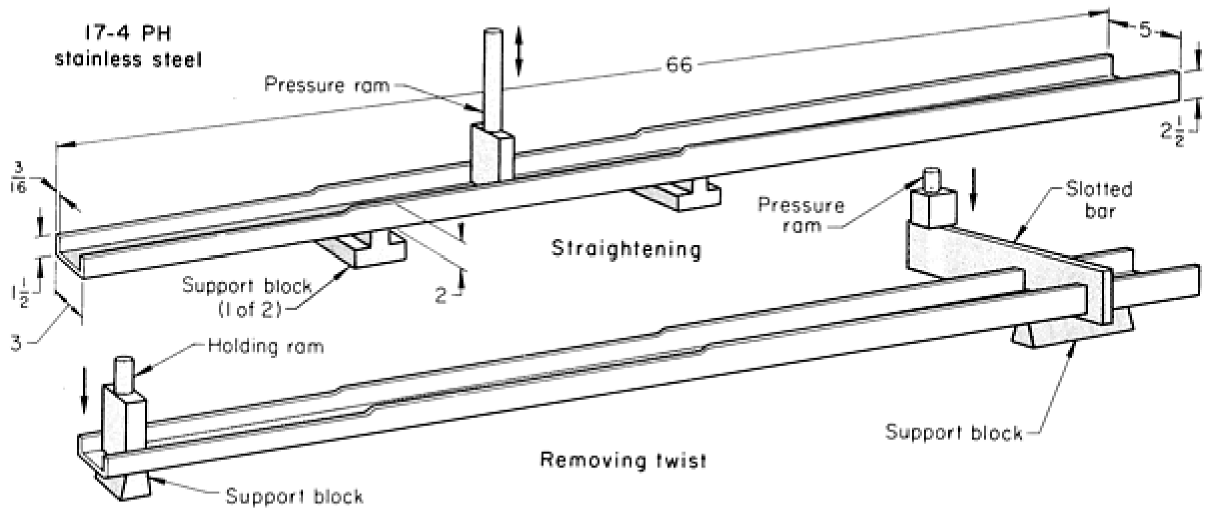


Fig. 9 Setups for straightening and removing twist from a stepped channel. Dimensions given in inches.

The channel was supported on two blocks 406 mm (16 in.) apart while force was applied by a pressure ram through a block fitted to the inside contour (upper view, Fig. 9). A force of 13.3 kN (3000 lbf) deflected the part about 25 mm (1 in.) for a correction of 0.51 mm (0.020 in.). Force was applied at 50 mm (2 in.) increments along the bar.

Two 44 kN (5 tonf) hydraulic presses mounted on a large steel table were used to remove the twist. One ram held one end of the part against a block on the table while the second ram untwisted the part (lower view, Fig. 9). A slotted bar served as a lever to twist the channel 30° for a permanent correction of 0.83 mm per 1 m (0.020 in. per 24 in.) or 1.3 mm (0.050 in.) for the entire length.

Example 6: Straightening a Long, Thin Rectangular Bar in a Hydraulic Press.

A rectangular bar of 17-4 PH stainless steel was 75 mm (3 in.) wide, 2.1 m (84 in.) long, and 6.4 mm ($\frac{1}{4}$ in.) thick (except for 75 mm, or 3 in., at each end, where it was 25 mm, or 1 in., thick). The bar was solution treated and finish machined, which caused a bowing of 6.4 mm ($\frac{1}{4}$ in.). The bar was clamped in a fixture and aged at 480 °C (900 °F), which reduced the bowing to 3.2 mm ($\frac{1}{8}$ in.). After aging, the bar was removed from the fixture, reheated to 425 °C (800 °F), and straightened in an 89 kN (10 tonf) hydraulic press, using 13.3 kN (3000 lbf) of force between support blocks 406 mm (16 in.) apart. This caused deflection of 19 mm ($\frac{3}{4}$ in.) for a correction of 0.76 mm (0.030 in.). It took 20 min and two to three ram strokes per 406 mm (16 in.) setup, as well as five setups per bar, to straighten the bar within 1.5 mm (0.060 in.).

Example 7:

A shaft of medium-carbon steel, 102 mm (4 in.) in diameter × 6.1 m (20 ft) long, was heat treated to 269 to 321 HB, straightened for turning, turned in a lathe, and then straightened for centerless grinding. The shaft lay on rollers beneath the ram of the press, which permitted it to be rotated and to be moved along its axis. Spring-loaded blocks supported the rollers so that straightening pressure would first deflect the springs, letting the shaft down on movable V-block anvils for straightening. The springs pushed the rollers up, lifting the shaft off the anvils when the ram moved up. The shaft was rotated under a dial indicator to find the high spots. The spots were marked with chalk so that they could be moved beneath the ram. The shaft was straightened within tolerance by pressing, moving the shaft, and pressing again. In an 8-h day, 5 to 15 shafts were straightened.

Example 8:

After being heated to 1010 °C (1850 °F) and air cooled, a shaft of D2 tool steel, 50 mm (2 in.) in diameter × 1.7 m (66 in.) long, was straightened within 0.51 mm (0.020 in.). The straightening began when the shaft had cooled to 480 °C (900 °F) from 1010 °C (1850 °F). An 89 kN (10 tonf) hydraulic press applied 8.9 kN (2000 lbf) of force to the shaft, which was supported on anvil blocks 457 mm (18 in.) apart. The shaft was continuously gaged with dial indicators as it was rotated on its axis in order to check the location of the high and low points.

Between 480 and 260 °C (900 and 500 °F), the shaft deflected easily under a load of 910 kg (2000 lb), causing a deflection of 3.2 mm per 508 mm ($\frac{1}{8}$ in. per 20 in.). When the shaft cooled further, straightening was more difficult and required increased force and a longer holding time.

Gaging and straightening continued until the shaft had cooled to 65 °C (150 °F); the shaft was then tempered at 480 °C (900 °F) for 2 h, resulting in a hardness of 59 to 60 HRC. A difficult shaft was sometimes clamped to a 76 × 76 × 1830 mm (3 × 3 × 72 in.) bar with shims and retempered. If the straightness error was 2.0 mm (0.080 in.), a 1.5 mm (0.060 in.) shim was used on each end for a deflection of 3.5 mm (2.0 + 1.5 mm), or 0.140 in. (0.060 + 0.080 in.), and the shaft was retempered at 495 °C (925 °F). The higher tempering temperature made the shaft slightly softer, but generally straightened it within the specified 0.51 mm (0.020 in.).

Parallel-Roll Straightening

Roll straightening is a cold-finishing mill process by which bars and structural shapes are provided with straightness adequate for most applications. For bars and shapes on which close tolerances must be maintained, roll straightening can be followed by press straightening.

In one type of roll straightening, square, flat, hexagonal, and other flat-sided bars are continuously passed between sets of parallel-axis rolls (Fig. 10 and 11). Uniform bends are introduced such that the bar is straight when it leaves the rolls. By varying the distance between roll centers and the amount of offset, the degree of bend can be adjusted according to the section size and yield strength of the metal being straightened.

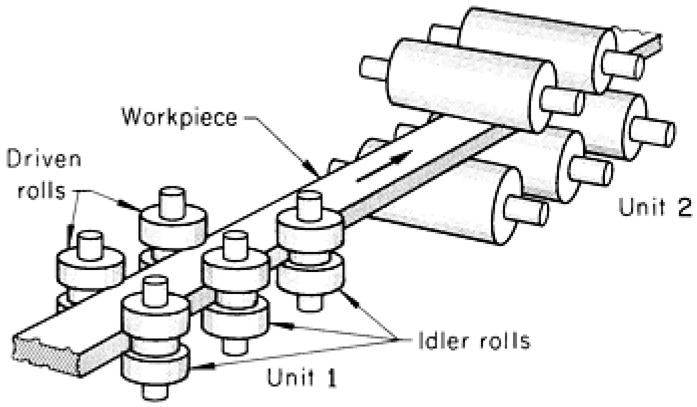


Fig. 10 Arrangement of vertical-shaft and horizontal-shaft rolls in a roll straightener for straightening a rectangular-section bar.

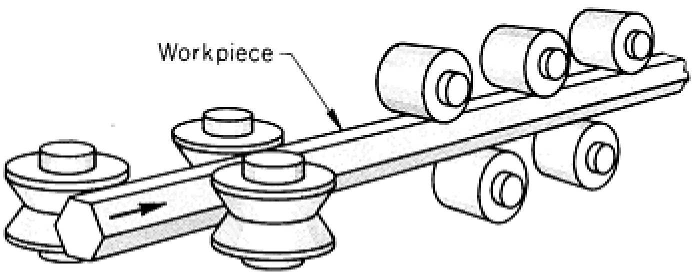


Fig. 11 Straightening of a hexagonal bar in a two-plane roll straightener.

Round bars can be straightened on parallel-axis roll straighteners, but there is no way to prevent a round workpiece from turning on its axis as it passes through the machine. In rotary straighteners, round bars rotate and advance through the rolls so that the bars are bent uniformly in all planes; the rolls are adjustable so that the bars emerge straightened. Rotary straighteners for round bars usually consist of rolls that can be set at variable angles to each other (see the section "Rotary Straighteners" in this article.

Parallel-Roll Straighteners. Square, flat, hexagonal, and other flat-sided bars can be straightened in both directions in one pass through a straightener having two sets of parallel rolls in planes 90° to each other (Fig. 10), or in two passes through a straightener having a set of parallel rolls in only one plane, by turning the bar 90° on its axis between passes.

In machines with a single straightening plane, the rolls are mounted on horizontal shafts as shown in unit 2 of Fig. 10. If the horizontal rolls are grooved (like the vertical rolls of unit 1, Fig. 10), the straightening in one plane also produces some straightening in a plane 90° to the first plane.

For high accuracy of straightness and high production rate, a second unit is added in a plane perpendicular to the first unit (Fig. 10). Unit 1 has vertical shafts for straightening curvature in the horizontal plane; unit 2 has horizontal shafts for straightening curvature in the vertical plane. The two driven rolls rotate, but are otherwise stationary; the three idler rolls are adjustable away from and toward the workpiece.

The first driven roll contacted by the bar is set with enough space between the first two idler rolls to curve the bar uniformly with the concavity toward the first driven roll. As the bar passes over the second idler roll and is held in position by the second driven roll, the concave side of the bar is reversed. The amount of reversal can be controlled by the position of the second idler roll, and with that roll properly positioned, the bar will emerge straight from the third idler roll. With a greater number of rolls, the most severe curvatures are reduced at the entry end of the machine. This results in less work for the remaining rolls and provides for better straightening of small curvatures. The number of rolls in a set of straightening rolls ranges from 4 to as many as 13; the most common number is eight or nine.

The amount of adjustment in roll spacing is determined by:

- The resistance of the work metal to deflection beyond its elastic limit. The greater the resistance, the

- farther apart the rolls must be spaced to provide sufficient shaft and bearing capacities
- The distance must be short enough to produce a permanent set in the smallest bar and to straighten it
- If sections with high width-to-thickness ratios are being straightened, the distance must be great enough so that pressure from the rolls does not upset the edges of the bar

Machines are built with both fixed and adjustable roll-center distances. In adjustable center distance machines, a separate housing carries the roll assembly to permit positioning along the straightener bed. These housings either reduce the space for the shafts or limit the minimum distance between the rolls.

Most sections can be straightened adequately in two planes. A flat bar is shown in Fig. 10 as it passes between grooved straightening rolls on vertical shafts and then through plain-face rolls on horizontal shafts. The bar passes first through the vertical-shaft unit because it is natural for the bar to enter with the flat side lying against the feed table. It is also natural to straighten in the grooved rolls first because the grooves that guide the bar also produce some straightening in the second plane and ensure proper entry of the bar into the second unit. If the machine were reversed, it would be desirable to groove the rolls on the horizontal shafts to make certain that the bar would not "walk off" the rolls.

Grooving the rolls for thin flat bars helps to reduce upsetting of the edge and twisting of the bar. A hexagonal bar is shown in Fig. 11 as it passes between grooved straightening rolls on vertical shafts and through flat-face rolls on horizontal shafts. This roll arrangement follows the same principles as those for flat bars.

Sections that are symmetrical in both planes are easier to roll straighten than nonsymmetrical sections. Sections that are symmetrical in one plane but nonsymmetrical in the plane at 90° are usually best straightened in the symmetrical plane. Axial adjustment of some of the rolls to produce additional straightening in the nonsymmetrical plane is necessary in this method, as in the straightening of the angle section shown in Fig. 12.

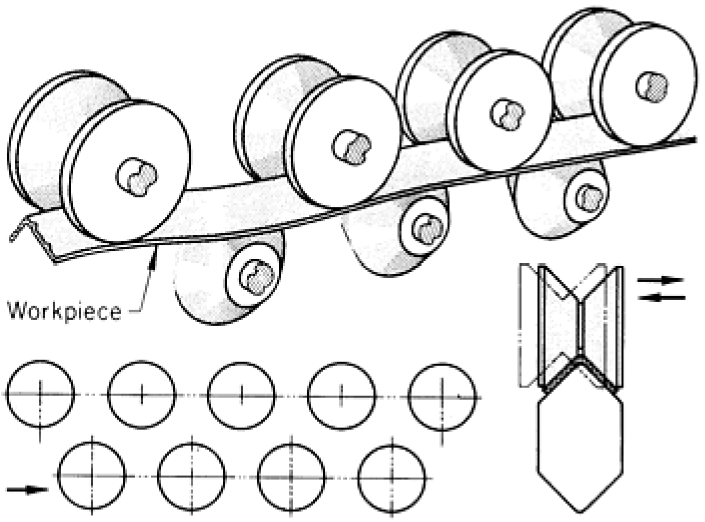


Fig. 12 Roll straightening of a structural angle. The top rolls can be adjusted horizontally and vertically.

Angles are best straightened in horizontal-shaft rolls with the apex of the angle up and roll adjustment made in the vertical plane. The angle lies naturally on the feed table and is straightened in both directions. To take out bends 90° to the plane in which the angle is straightened, one or two top rolls are adjusted axially to deflect the angle in this direction.

A structural channel can be passed through horizontal-shaft rolls with the flanges of the channel up or down. The upper rolls are staggered vertically and horizontally to remove camber in both planes.

Square or nearly square bars can be straightened on the diagonal in a single-plane machine using V-shaped upper and lower rolls (Fig. 13a) similar to the upper rolls used for straightening the angle shown in Fig. 12. In this method of straightening, only the shaded portion of the cross section shown in Fig. 13(b) is stressed beyond the elastic limit; therefore, the results may not be satisfactory. A square is better straightened in a two-plane machine, in which the areas stressed beyond the elastic limit are greater and more nearly symmetrical, as shown in Fig. 13(c). Square and hexagonal bars as large as 102 mm (4 in.) and flat bars as wide as 305 mm (12 in.) are straightened in roll straighteners. Larger bars are usually straightened in presses.

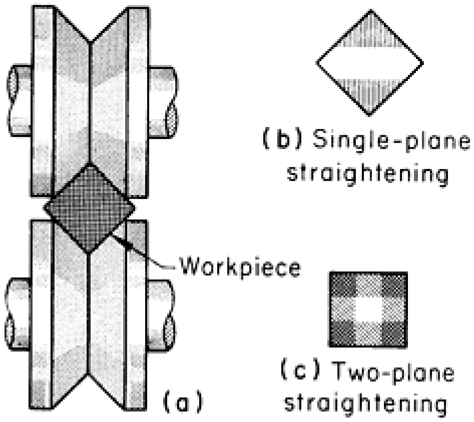


Fig. 13 Roll straightening of square bars. (a) Setup for single-plane roll straightening. (b) and (c) Stress patterns that result from single-plane and two-plane straightening.

Bars of low-carbon steel seldom change size in straightening. Steel having 0.30% C or more may enlarge slightly in section because straightening redistributes the stress that remains from previous operations. For example, a bar of 1045 steel 30 mm ($1\frac{3}{16}$ in.) square may enlarge 0.05 mm (0.002 in.) in one pass through a roll straightener, and 0.10 mm (0.004 in.) in two passes. The bars shorten as they enlarge in section in accordance with the Poisson's ratio of the material.

Square, hexagonal, and flat bars to 19 mm ($\frac{3}{4}$ in.) in cross section are sometimes cold drawn from coils of hot-rolled stock. The drawn bars are straightened in roll straighteners and sheared to length. To correct the curvature resulting from coiling, rotary or two-plane straighteners with sets of six to eight rolls in each plane are used.

Rotary Straighteners

Round bars or shaftlike parts of all types of metal are straightened in rotary straightening machines of two basic types: crossed-axis-roll machines and rotary-arbor machines. The basic principle of rotary straightening is that the workpiece is fed forward and deflected beyond its elastic limit by crossed-axis rolls that also impart the rotary motion. The surface of the bar is alternately subjected to tensile and compressive stresses as it rotates in the straightener. Rotary straighteners are available with two to nine rolls.

A two-roll rotary straightener consists of two rolls that are directly opposed and positively driven. One of the rolls is concave, and the other has a relatively straight face (Fig. 14). The angularity adjustment of the rolls at opposite inclinations rotates and feeds the bar through the machine. Straightening is accomplished by flexing the workpiece into the throat of the concave roll by the modified straight-face roll (Fig. 3d). The bar is positioned vertically by means of a bottom guide or top and bottom guides (not shown in Fig. 14) so that the axis of the bar coincides with the centerline of its path between the rolls.

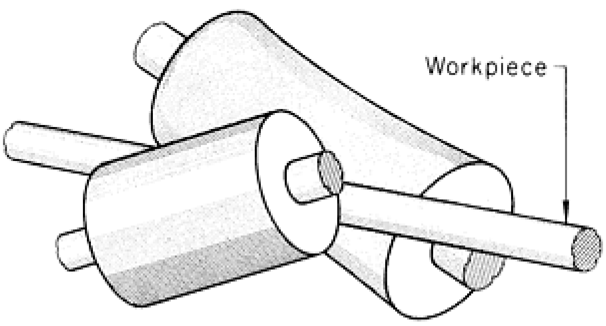


Fig. 14 Arrangement of rolls in a two-roll rotary straightener (top and bottom guides not shown).

The amount of bend given a bar as it passes through the machine depends on two adjustments made by the operator:

- The angle of the rolls to the axis of the bar
- The roll pressure, which is selected by adjusting one of the rolls toward or away from the other

Roll-angle and pressure adjustments depend on the size of the bar being straightened and its mechanical properties. In general, the larger the bar, the greater the roll angle, provided the mechanical properties are about the same. A heat-treated bar (tensile strength: 862 to 1030 MPa, or 125 to 150 ksi) will require a smaller roll angle and more pressure than a bar of the same size and grade that has been annealed, or annealed and cold drawn.

In two-roll rotary straightening, the workpiece is subjected to a continuous straightening action from the point of entrance to the work rolls to the end of the workpiece as it leaves the rolls. Therefore, there is no variation in size within the bar, as is sometimes encountered with multiroll straighteners. Two-roll straighteners can be used for short workpieces, such as rocker-arm shafts and chain-link pins, because all of the flexing is contained within the cavity of one roll. Two-roll straighteners are also used for sizing or for correcting out-of-roundness in hot-rolled bars. Extremely soft metal may be reduced in diameter if too much pressure or too large a roll angle is used. Two-roll straighteners can be used to remove end kinks and to round out squashed ends, both of which sometimes occur when bars are cold sheared to length prior to straightening.

Two-roll rotary straighteners inherently have a lower through speed than multiroll straighteners. The roll inclination must be kept lower (about 20°) in two-roll straighteners; therefore, the rotational speed of the bar is much higher in relationship to the forward speed.

The span over which bending takes place is considerably shorter in two-roll machines than in multiroll rotary straighteners, because in two-roll machines all bending takes place within the length of the rolls and not from roll to roll. With such a short span, much more force must be applied to the bar by the bending equipment than with multiroll machines. Bars from 1.6 to 255 mm ($\frac{1}{16}$ to 10 in.) in diameter can be straightened in two-roll rotary machines.

In addition to finish straightening, the two-roll rotary machine can be used to rough straighten hot-rolled round bars, which may be very crooked and may have sharp hooks and round and scaly surfaces; to straighten and size cold-drawn round bars, which may be bowed but have no sharp bends; and to polish or burnish to improve surface finish after grinding. Additional rolls should be kept for straightening only, sizing only, and polishing only.

Multiroll Rotary Straighteners. Another type of machine used in the straightening of bars is the multiroll rotary straightener. Figure 15 shows a five-roll rotary straightener, which consists of two driven rolls and three idler rolls. The two end idlers oppose the driven rolls, and between them is the middle or pressure roll. All rolls are concave, and the roll inclination is adjustable in order to obtain the maximum length of contact between the roll surface and the workpiece (see also Fig. 3e). Bottom cast iron guide shoes are located at the entry and exit ends between the driven rolls and their respective opposing idlers to position the bar properly.

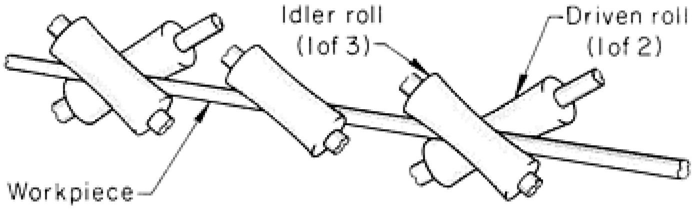


Fig. 15 Arrangement of rolls in a five-roll rotary straightener.

A six-roll rotary straightener has a roll arrangement similar to that of a five-roll machine; the sixth roll is placed either opposite the middle roll or outboard of the exit-end powered rolls (see these roll arrangements in Fig. 3f). Seven-roll arrangements consist of two three-roll clusters with a middle idler roll (Fig. 3g). Small cluster-roll straighteners have been extensively used for specialty work on small workpieces, such as valve push rods (~7.9 mm, or $\frac{5}{16}$ in., in diameter) and rocker-arm shafts; however, these straighteners are used most often for straightening large tubing (60 to 610 mm, or $2\frac{3}{8}$ to 24 in., in diameter).

In operation, the rolls are angularly adjusted to accommodate various bar sizes. With the average angle selected as 30°, the adjustment may vary from about 28 to 30°, depending on the size of the bar being straightened.

In a five-roll straightener, the middle idler roll is adjusted to put enough bend in the bar to exceed the elastic limit of the metal. As the bar is fed through the straightener and rotated by the entrance and exit rolls, the adjustment of the pressure roll causes the bar to bend beyond its elastic limit in all directions perpendicular to its longitudinal axis. This action produces a straight bar with symmetrical stresses.

Optimum settings of roll angle vary somewhat with bar size. Typical settings are given in Table 1.

Table 1 Typical settings of roll angle for five-roll rotary straighteners for use on bars of various diameters

Diameter of bar		Setting of roll angle, degree
mm	**in.**	
19	$\frac{3}{4}$	**26** $\frac{5}{8}$
25	1	**26** $\frac{7}{8}$
32	$1\frac{1}{4}$	**27**
38	$1\frac{1}{2}$	**27** $\frac{1}{4}$
44	$1\frac{3}{4}$	**27** $\frac{1}{2}$
50	2	**27** $\frac{3}{4}$
57	$2\frac{1}{4}$	**28**
64	$2\frac{1}{2}$	**28** $\frac{1}{4}$
70	$2\frac{3}{4}$	**28** $\frac{1}{2}$
75	3	**28** $\frac{3}{4}$
83	$3\frac{1}{4}$	**29**

89	3 1/2	**29 1/4**
95	3 3/4	**29 1/2**
102	4	**29 5/8**
108	4 1/4	**29 7/8**
114	**4 1/2**	**30**

Recommended settings for the starting setup; these will vary slightly in the actual setup used.

Cold-drawn bars that are straightened in a multiroll rotary straightener usually increase in diameter during the straightening operation. Low-carbon steel bars with up to about 0.15% C show a negligible increase in diameter. However, as the carbon content increases, the amount of change increases. It is not uncommon for 50-mm (2-in.) diam cold-drawn bars of 1050 steel to increase as much as 0.1 mm (0.004 in.) in diameter. These bars will decrease in length by about 13 mm per 3.7 m (1/2 in. per 12 ft) as a result of the increase in diameter. This shortening must be considered when bars are cut to exact lengths before straightening.

When cold-drawn bars are to be straightened in a multiroll rotary straightener, selection of the cold-drawing die size is important if the bars are to be held within standard size tolerances. Most grades, particularly those having high carbon content, should be drawn to the low side of the diameter tolerance to compensate for the increase during straightening. The extreme ends of the bars, which do not get the full effect of the bend by the pressure roll, do not increase in diameter. After straightening, the bar ends will remain the same size as when cold drawn.

Straightening in a multiroll rotary straightener does not work harden the bar stock to any appreciable extent. This is desirable when the bars are to be cold headed or cold extruded.

The basic five-roll straightener has been modified so that four of the rolls are driven and only the middle pressure roll is an idler. The heavier feeding pressure obtainable with driven entry and exit pressure rolls is advantageous in that a polishing effect can be obtained on products such as cold-drawn steel. In addition, the driven rolls provide more traction so that a heavier deflection can be exerted by the middle straightening roll.

In a further modification of the five-roll straightener, all five rolls are driven. This eliminates the need for guides between the rolls, but roll speed synchronization becomes important.

Hot-rolled round steel bars are generally straightened commercially in two-roll or multiroll rotary straighteners. Bars as large as 255 mm (10 in.) in diameter and having yield strengths up to 690 MPa (100 ksi) have been straightened in these machines. Some machines are modified by adding hydraulic loading to the straightening mechanisms and by adding a shear pin to provide for any shock loading that might be encountered because of the extreme out-of-roundness of large hot-finished bars.

Rotary-arbor straighteners are used to straighten coiled rod or wire up to 32 mm (1 1/4 in.) in diameter. The straightening is done by an arbor rotating around the wire as it passes through the machine, as shown in Fig. 16 (see also Fig. 3h). The arbor encloses five pairs of cast iron straightening dies. The dies are equally spaced in a fixed spacing that relates to the size capacity of the machine. The greater the capacity, the greater the fixed spacing of the dies.

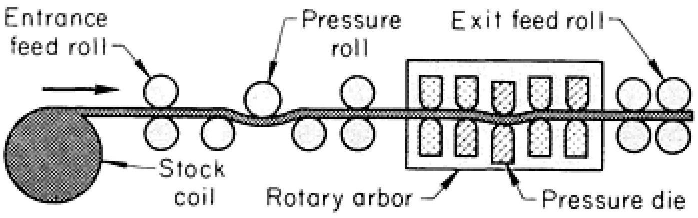


Fig. 16 Arrangement of rolls and dies in a rotary-arbor straightener used for the straightening of coiled rod or wire.

The dies, bell-mouthed for easier entrance of the wire, are locked in place by adjusting screws. The pairs of dies at the two ends of the straightener arbor are set so that the wire is always at the center of the arbor in these dies. The middle die is called the pressure die because it is set to bend the wire slightly, as shown in Fig. 16. The dies on either side bend the wire slightly in the opposite direction.

The machine is set by trial. If the wire comes out bowed, usually the pressure die has not been set to bend the wire enough. If the wire comes out wavy, the pressure die may be bending the wire too far. Helical marks may be caused by lack of lubrication, imperfect dies, or an embedded sliver of metal.

Growth. During straightening in a rotary-arbor machine, most grades of cold-drawn carbon steel and of alloy steel with more than 0.15% C, will increase in diameter (as much as 0.15 mm, or 0.006 in.) unless the wire has been stress relieved before straightening.

Speed of straightening in rotary-arbor straighteners is usually 23 to 61 m (75 to 200 ft) per min, depending on the size capacity of the machine and on the type of wire.

Cut lengths of bars are straightened in mechanisms such as the one shown in Fig. 17. A helical motion is imparted to the bars by pairs of rotating, offset friction disks, which burnish the bar as they feed it through three straightening bushings that turn freely in bearings. The middle bushing is adjusted to deflect the bar, just enough for good straightening action. Such a machine made in various size capacities can straighten bars 2 to 32 mm ($\frac{5}{64}$ to $1\frac{1}{4}$ in.) in diameter at speeds of 28 to 50 m (92 to 164 ft) per min.

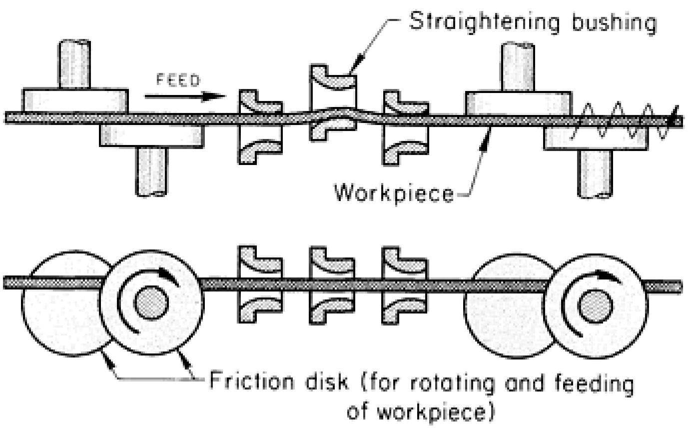


Fig. 17 Plan and side views of a mechanism for straightening cut lengths of bars.

Straightening Stainless Steel for Cold Heading. Coiled stainless steel wire (series 300 and 400) 1.6 to 15.5 mm (0.062 to 0.610 in.) in diameter requires moderate straightness while being fed into cold-heading machines. Wire 1.0 to 3.2 mm (0.040 to 0.125 in.) in diameter can be hand straightened sufficiently for entering the feed rolls. The feed rolls then pull the wire with enough tension to remove the coil radius as the wire leaves the coil reel. For parts having a length-to-diameter ratio of 4 to 1 to 8 to 1, no further straightening is necessary. The feed rolls provide sufficient straightness to permit the blank to be cut to length and transferred to the die station. After cold heading, the part has the straightness obtained in the heading operation.

Parts up to 152 mm (6 in.) long cold headed in open-die headers require a straightness of 0.1 mm in 102 mm (0.004 in. in 4 in.). A single-plane five- or six-roll straightener placed 90° to the feed roll is usually used. Single-plane and two-plane straighteners mounted on portable pedestals are available as machine accessories.

Straightening of Bars, Shapes, and Long Parts

Automatic Press Roll Straightening

One of the fastest straightening processes available is the automatic press roll-straightening method. It is capable of straightening small concentric parts at up to 1200 pieces per hour; larger parts with greater ovality, such as cold extrusion axles and transmission shafts, can be made at 225 pieces per hour.

Roll straightening features support and straightening roll assemblies that resemble those used in a roller V-block, but it includes a headstock unit equipped with a drive mechanism to rotate the workpiece. This process uses a press frame with a hydraulically powered ram (Fig. 18). Two or more lower support roll assemblies are mounted on the bed of the device, while one or more upper support rolls are mounted on the ram. Additional roll assemblies are needed to straighten a series of bows (known as snaking). The headstock consists of a chuck or driver used to rotate the workpiece. The equipment is particularly suited to the straightening of cylindrical solid parts as well as tubular parts having walls that are thick enough to withstand the pressure of the rolls without being deformed (see the article "Straightening of Tubing" in this Volume).

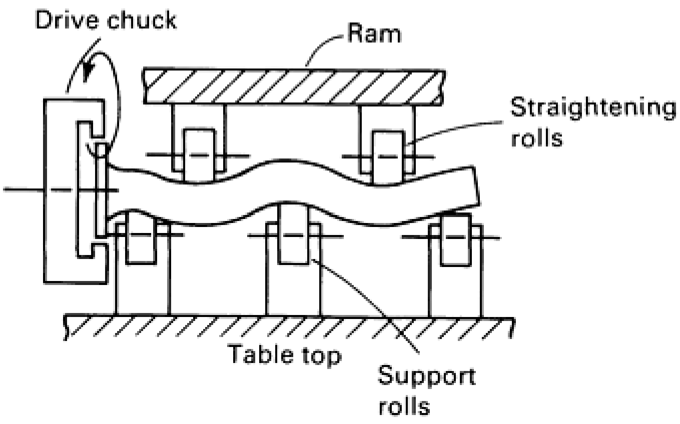


Fig. 18 Illustration of equipment used in automatic press roll straightening.

The process is initiated by placing the workpiece on the lower rolls and having the driver move forward to engage the part and to rotate the workpiece. The overhead rolls situated on the ram are then applied to deflect the part in a bowlike arc while the workpiece rotates. This procedure subjects the material to a plastic strain. When the material exceeds the yield point, the ram action is released to complete the cycle.

The advantages of this method include a stretch-relieving action that accompanies the straightening as well as the ability to handle a wide range of types and degrees of out-of-straightness conditions. Concentric parts with no ovality can be held to a close tolerance and require minimal operator skill.

The disadvantages of this technique are that it cannot be used on nonround parts, thin-wall tubes, or parts having variable diameters. Only hardened shafts with a hardness of 38 HRC or less should be straightened using this method, because the depth of hardness limits its effectiveness.

Moving-Insert Straightening

Designed for use on linear, flat, or irregularly shaped parts, moving-insert straightening is accomplished by reciprocal strokes transmitted to tooling inserts by a rotary-cam action. When positioned between two rows of movable inserts situated on a tool base, a part is subjected to a series of reciprocal strokes that overbend the workpiece by a preset amount (Fig. 19). The amplitude of the movement is progressively reduced during the cycle until it approaches a straight line, at which point the workpiece is also straight. The degree of bending movement and the number of bending cycles are adjustable, and varying insert spacing is available to accommodate a wide range of soft or heat-treated components. The primary advantages of this straightening technique are its ability to straighten flat or irregularly shaped parts, with or without projections, bends, and so on; the ability to produce straightness or slight curves when needed; a tolerance as close as 0.03 mm (0.001 in.) throughout the length of the part, depending on its configuration; and minimal skill requirements imposed on the operator.

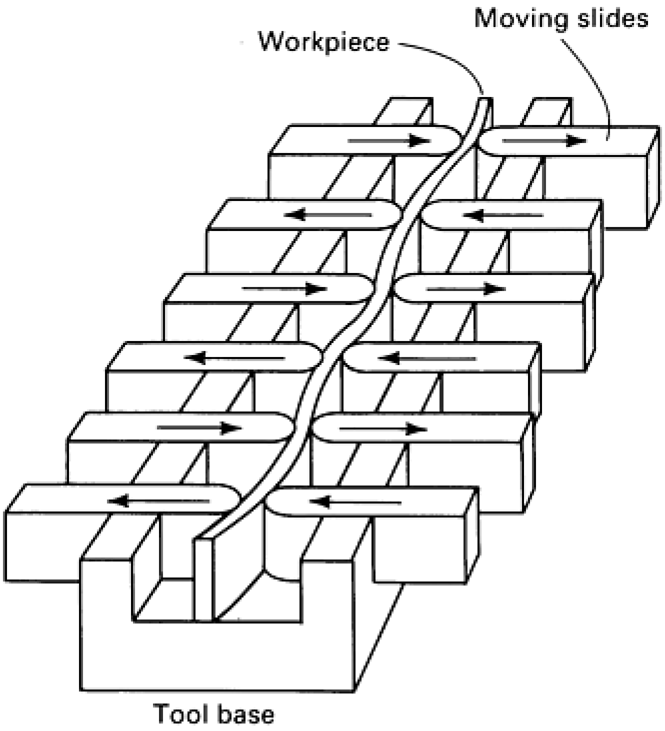


Fig. 19 Moving-insert straightening for linear, flat, or irregularly shaped objects.

Parallel-Rail Straightening

Parts that feature multiple diameters and heads, such as bolts and spindles, require a straightening device that adjusts to the contours of the workpiece. The parallel-rail method performs this function by employing a series of parallel rails. One group of rails is located on a slide, and the other group is located on a ram or head above and between the bottom rails. As shown in Fig. 20, the cylindrical or thick-wall tubular part is positioned between the rails, and the ram is lowered by adjustable hydraulic pressure to overbend the workpiece. Simultaneously, the lower slide moves forward and rotates the workpiece. At the end of the stroke cycle, the pressure decreases to zero, and the part is now straightened. The configuration of the workpiece determines the number and position of the adjustable rails needed to complete the straightening operation.

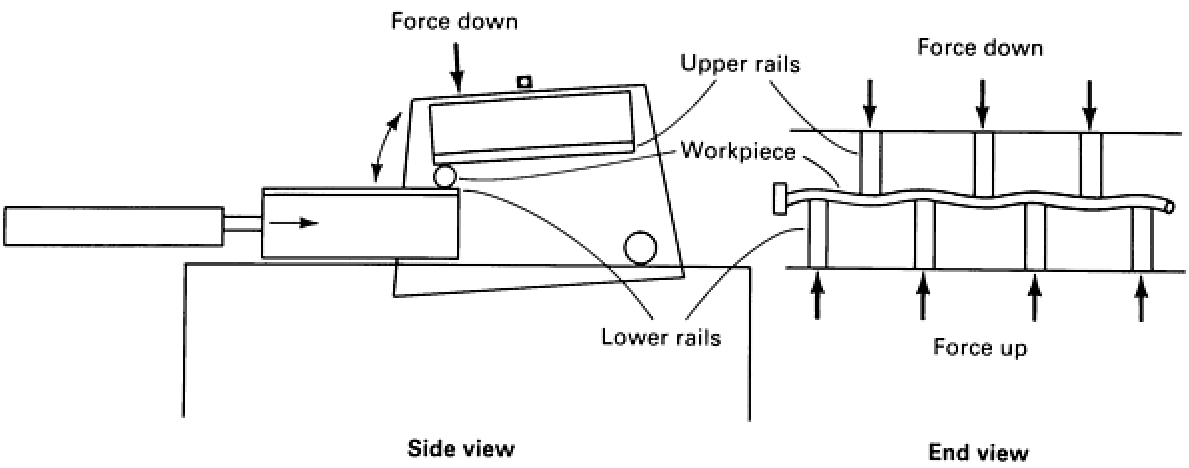


Fig. 20 Parallel-rail straightening for cylindrical or thick-wall tubular parts, either symmetrical or with variations

in diameter.

With this technique, production rates of 500 to 600 pieces per hour can be obtained with manual loading and unloading methods. Production rates of 1000 pieces or more per hour can be obtained with automatic loading and unloading equipment.

Among the advantages of parallel-rail straightening are:

- Minimal operator skill required
- Easy adjustment to make rails conform to workpiece configuration
- Complex parts featuring headed areas and multiple diameters can be straightened
- High production rates combined with low tooling costs

The primary disadvantage of parallel-rail straightening is that workpiece dimensions are limited by the machine width of 610 mm (24 in.) as well as maximum diameters of 20 mm (0.8 in.).

Straightening of Bars, Shapes, and Long Parts

Epicyclic Straightening

Epicyclic straightening is specifically suited to straightening linear parts, tubing, and solid cylindrical parts featuring a variety of cross-sectional shapes. After the workpiece is securely supported by locating fixtures at either end, a straightening arm secures the part in the approximate center of its length (additional straightening arms may be necessary on some parts due to length and configuration considerations). The straightening arm is programmed to move in a circular or elliptical path about the neutral axis of the part. The cross section of the part is taken through its elastic limit as the amplitude of the arm motion is increased. At the elastic limit, the motion decreases, and the arm moves to a stationary position at the neutral axis of the workpiece to produce a straight and stable part.

The motion of the straightening arm is a circular path for round parts, while parts with varying cross sections, such as an I-beam section, require an elliptical path (Fig. 21). The feed rate should be maximized to reach the yield point as quickly as possible. The degree of straightness required determines the feed rate necessary to return the part to its neutral axis.

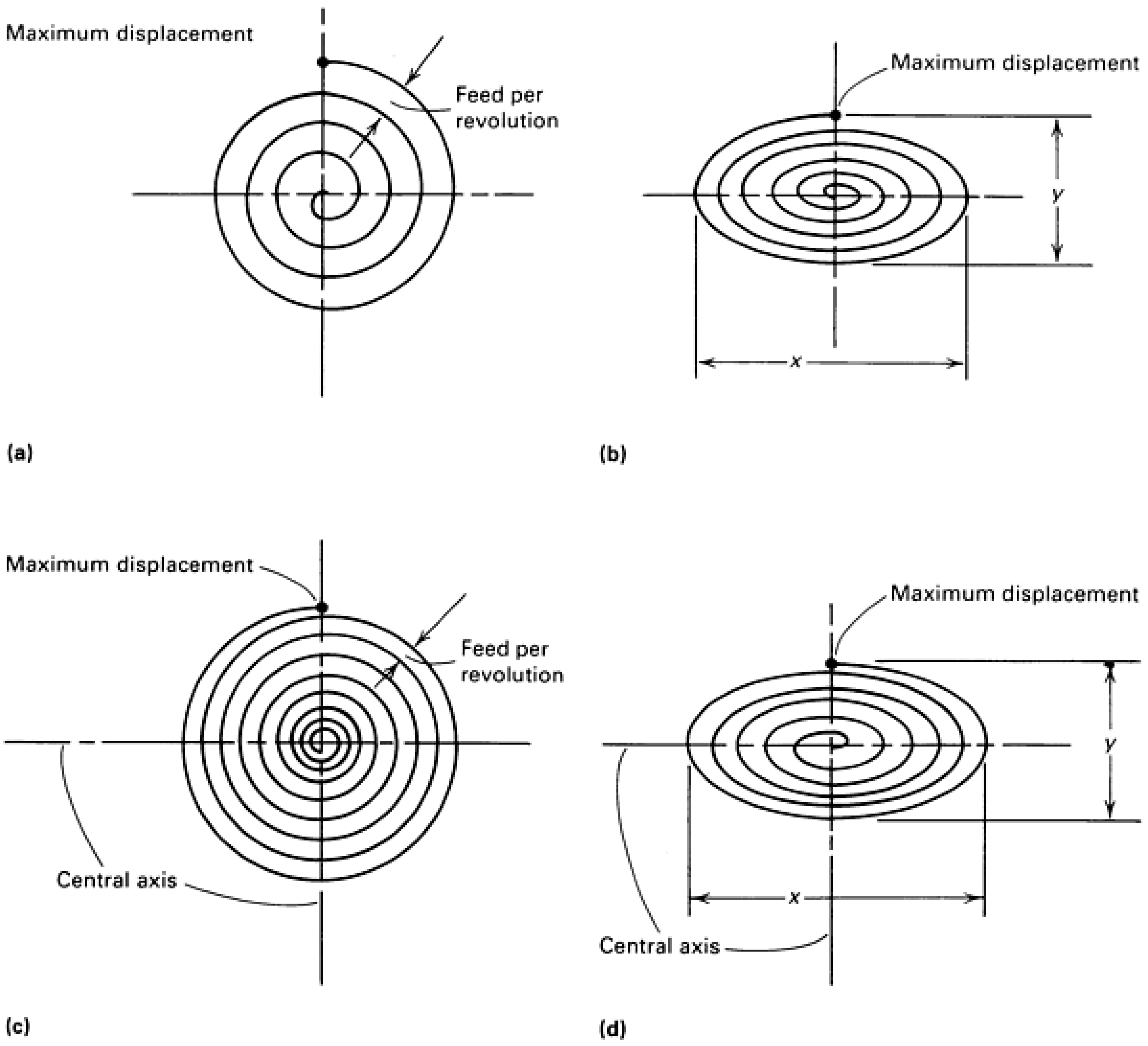


Fig. 21 Circular and elliptical paths of the motion arm on an epicyclic straightener. (a) Displacement circular. (b) Displacement elliptical. Ratio *x*:*y* = 2:1. (c) Feed to central axis circular. (d) Feed to central axis elliptical. Ratio *x*:*y* = 2:1.

Epicyclic straightening can be used on such parts as I-beam axles, trailer axles, asymmetrical forgings, and thin-wall propeller shaft or drive shaft tubes. Production rates range from 80 parts per hour (trailer axles) to 300 parts per hour (propeller shaft tubes).

Straightening of Tubing

Revised by Philippe Delori, SMS Sutton, Inc.

Introduction

TUBING of any cross-sectional shape can be straightened by using equipment and techniques that are basically the same as those discussed in the article "Straightening of Bars, Shapes, and Long Parts" in this Volume. In general, a round tube that has warped in annealing or other heat treatment is given a rough-straightening pass in a press or a roll straightener, followed by one or more passes in a rotary straightener and, if required, a finish pass in a press straightener.

Rough straightening eliminates excessive bow that would cause whipping in the inlet trough. Because the tube does not rotate during rough straightening, almost any amount of bow can be accepted; therefore, both roll and press straightening can be used. If the tube is only slightly bowed, initial rough straightening can be omitted.

A rotary straightening pass is necessary for finish straightening, but need not be used when the tube requires another cold-drawing operation. Two or more rotary passes may be required when the tube has developed excessive out-of-roundness, bow, or multiple kinks in previous processing.

If the tube is considerably out-of-round, a normal straightening pass, with only a middle roll offset, will not be effective. A preliminary pass with no offset in the middle rolls and all rolls set to ovalize may be required before the normal straightening pass.

Most tubing, especially the smaller sizes (32 mm, or $1\frac{1}{4}$ in., in outside diameter and smaller), requires only one or two rotary straightening passes. For larger-diameter tubing (38 mm, or $1\frac{1}{2}$ in., in outside diameter and larger), or when straightness better than standard is required, additional work can be done in a press straightener to remove short end hooks or to secure precision straightness. Press straightening converts long, gentle bows into a series of short bows ending at each point where the punch hits.

Straightening of tubing having shapes other than round is done in roll or press straighteners with much the same considerations governing the choice as those for similar solid shapes. Asymmetrical sections, such as airfoil or teardrop designs, require press straightening. Stretch straighteners are helpful in detwisting and straightening asymmetrical shapes.

Effect of Tubing Material

Procedures and tooling are influenced by the tubing material. The principal factors are the composition and condition of the work metal, wall thickness, type of tubing (seamless or welded), and type and extent of distortion. Greater force and more rugged equipment are required for straightening tubes with thick walls and high elastic limit.

Wall Thickness. The amount of distortion that occurs in the hollow section where it bears against the straightening tools is relatively small in thick-wall tubes, which approach solids in their behavior. Distortion becomes significant as tube walls become thinner, necessitating precautions against permanent distortion. Damage to thin-wall tubing appears as spot dinges or ovality from punch straightening, or as spiral dinges (called rings) or ovality from rotary straightening.

Type of Tubing. Seamless tubes often have a nonuniform wall thickness, which makes the straightening operations more difficult. In a welded tube, the weld and adjacent material may have mechanical properties that differ considerably from those of the rest of the tube. Residual stress from welding may also affect straightening.

Type and Extent of Distortion. Crookedness in tubing can result from distortion during heat treatment or cooling, from distortion during cold drawing as the result of eccentric wall, from hooked ends on hot-finished tubes (usually caused by misalignment or roll wear in the hot-sinking mill), or from accidental bending during processing. End hooks are pronounced bends that appear close (usually within 455 mm, or 18 in.) to the leading or trailing ends of some tubular products.

Straightening of Tubing

Revised by Philippe Delori, SMS Sutton, Inc.

Control of Straightening Pressure

Pressure exerted on the workpiece by the straightening rolls must be carefully controlled to prevent permanent damage, especially to thin-wall tubing. This can be done by increasing the distance between the points at which the tool contacts the workpiece, thus reducing the total force, or by increasing the contact area between tool and tubing, thus reducing the unit pressure on the workpiece.

In the rotary straightening of thin-wall tubing, the length of contact between the roll and the tube and the distance between sets of rolls should both be maximum, and care should be exercised in the adjustment of opposed rolls to avoid excessive ovalizing and pressure on a short section of tubing. In a two-roll machine, the length of curve is limited to the length of the roll, and tubing with very thin walls may be subject to ringing at the roll shoulders where the pressure is greatest. This limits the two-roll machine to a maximum diameter-to-wall thickness ratio of about 15 to 1 for full straightening.

Press Straightening

The straightening press can rough straighten tubing prior to roll straightening or rotary straightening, or it can completely straighten tubing by removing end hooks that cannot be removed easily by any other method. End hooks and large cambers sometimes must be removed to permit the tubing to enter the rotary straightener and to prevent dangerous whipping of the work. A press can also be used to straighten welded tubing before the bead is reduced. It is best to reduce the bead before straightening the welded tubing in a rotary straightener.

Tooling. In the press straightening of thin-wall tubing, there should be a wide spacing between saddles. If there are short kinks in the tube, the spacing is dictated by their length, and operator skill must be relied on to obtain the best possible straightening.

Rolls with a semicircular groove for holding the tubing are sometimes used for end supports. The spacing of these rolls can easily be adjusted to suit the length of the bow. The pressure shoe attached to the press ram is flat, with a semicircular groove to distribute force over a greater area. The diameter of the groove should approximate the diameter of the tube to avoid flattening of the tube under pressure.

Applications. Straightening presses are used on tubes to remove kinks, camber, and other distortions caused by mishandling, cutting off, and heat treatment. Straightening presses can also be used to reduce the out-of-roundness of tubing.

Straightening of Tubing

Revised by Philippe Delori, SMS Sutton, Inc.

Parallel-Roll Straightening

Single-plane and two-plane roll straighteners for tubular products are basically the same as those for solid bars. Machines for tubular products may have somewhat longer center distances than bar straighteners and at the same time may not require shafts and bearings that are as large. The roll-center distance is generally a function of the outside dimensions of the tube, and it increases approximately as the outside dimensions of the tube increase. Less force is required for straightening tubes than solid bars of equal outside dimensions.

Round tubing is best straightened on machines equipped with semicircular grooved rolls. The grooves must conform closely to the tube size, and each size must have its own set of rolls, or set of grooves in a multigrooved roll. Round tubing will twist slightly in the rolls, thus avoiding the effect of straightening and resulting only in rough straightening.

Tubing that is badly warped can be rough straightened as easily in a roll straightener as in a press. The roll straightener operates faster than the press but takes longer to set up. When straightening 50 mm (2 in.) diam, 6 m (20 ft) long tubes, for example, the roll straightener can process 250 to 400 pieces per hour as compared to 100 to 120 pieces per hour in a press. However, changeover time for a roll straightener is 16 min; a press setup can be changed in as little as 6 min.

The cost of roll straightening increases greatly for the larger tube sizes. Therefore, a large tonnage of tubes must be processed to justify the cost of machines for straightening tubes that are larger in outside diameter than approximately 75 mm (3 in.).

Square and rectangular tubing is commonly straightened in either single-plane or two-plane machines (see the article "Straightening of Bars, Shapes, and Long Parts" in this Volume). If the rolls of a single-plane machine are grooved to fit the tube, an appreciable straightening effect in a plane spaced at 90° is provided. When additional straightening is required, square tubes are turned 90° for a second pass through the rolls. A two-plane roll straightener is better suited to rectangular tubing than a single-plane roller because the rolls need not be reset for the second pass.

Straightening of Tubing

Revised by Philippe Delori, SMS Sutton, Inc.

Two-Roll Rotary Straightening

The principle employed in the rotary straightening of round tubes is basically the same as that for solid round bars. The driven rolls, set at a predetermined angle, rotate the tube while conveying it in a lineal direction. The crest of the bow is stressed to, or beyond, the elastic limit once during each revolution, and the maximum stress point is repeated spirally along the length of the tube. The distance between each stress point depends on the lineal travel for each revolution of the tube. Approximate values for lineal travel can be determined by multiplying the tube circumference by the tangent of the angle of the rolls. Special contoured rolls can be used to eliminate stress points, yielding tubes with uniform stresses.

Two-roll rotary straighteners (see the article "Straightening of Bars, Shapes, and Long Parts" in this Volume) are used primarily on tubes having a diameter-to-wall thickness ratio of no more than 15 to 1. The machine is equipped with two skewed rolls, between which two guide shoes are mounted. One roll has a concave contour; the other is straight or convex. The rolls can be arranged in a horizontal or a vertical plane. Machines with rolls arranged in the vertical plane are of relatively new design.

The tube is held between the guide shoes while the straight or convex roll bends the tube between the ends of the concave roll. The maximum deflection depends on the depth and the skew angle of the concave roll.

Two concave rolls have also been used to straighten tubes. The concave rolls are set to make full-length contact along the surface of the tube, and the crest of the bow is ovalized between the rolls several times before the tube emerges from the machine.

Straightening of End Hooks. The two-roll machine can remove most of the sharp bend at the end of tubing if the rolls are ground to suit the deflection requirements of the tube material and are set at an angle to suit the size of tube and length of end hook. The resulting curve in the rolls is suitable for a specific range of tube sizes with a specific elastic limit. Wider variations in tube size and grade of material can be processed by changing the angle setting of one or both rolls to produce the required deflection.

The design of the two-roll machine permits the rolls to be set at a small angle with the centerline of the stock. The smaller the angle, the less through feed per revolution of the tube, and because of the small helix angle created by feed per revolution, a large portion of the tube is subject to maximum bending stress.

Short bends and end hooks can be straightened in a two-roll machine, because all bending takes place within the length of the rolls and not from roll to roll, as is the case with multiple-roll machines. The short span greatly increases the load necessary for straightening, which partly explains why a two-roll machine is unsuccessful in straightening thin-wall tubing.

Roll Angle. Contoured rolls are designed for a specific range of tube sizes and materials. This is approximately the same range of conditions that can be handled by the equivalent multiple-roll straighteners. For most applications, the rolls are set at an angle of 15 to 25°. The contour of the rolls can be varied to suit specific applications. For example, a roll of shallow concavity is used for materials having low elastic limits, while a roll of deeper concavity can be used to straighten materials having higher elastic limits.

Limitations. The two-roll machine is not ordinarily used to finish straighten tubing if the ratio of outside diameter to wall thickness is greater than 15 to 1. The crushing strength of a thin-wall tube in the short span of a two-roll straightener is such that the tube will crush, or ring, before it bends if the rolls are set to remove the maximum bend. However, if the amount of bend to be removed is reduced by a preliminary rough-straightening operation, the machine can be used to finish straighten tubing having a diameter-to-wall thickness ratio considerably greater than 15 to 1, depending on the amount of straightening done in the preliminary operation.

Polishing of the tube surface can be either beneficial or detrimental. The hourglass shape of the rolls presents different diameters to the surface of the tube, resulting in some slipping. This burnishing action improves surface finish, although excessive slipping can produce a burnished spiral on the work surface.

Scratches may result when foreign material becomes embedded in the guide shoes. The use of nylon shoes and a soluble oil as a lubricant will reduce scratches.

Sizing After Derodding. Producers of cold, drawn tubing use internal mandrels to control the inside diameter, and two-roll crossed-axis machines are used to extract the mandrel (derod) after the drawing process. The identical rolls apply heavy pressure on the workpiece, thus expanding the tubing to allow removal of the mandrel.

The expanded tubing does not always return to its drawn diameter. The external dimensions of tubing can be corrected by drawing the tubing through dies that are undersize by an amount equal to the amount by which the tubing expands during derodding.

Straightening of Tubing

Revised by Philippe Delori, SMS Sutton, Inc.

Multiple-Roll Rotary Straightening

Rotary straighteners with five, six, seven, or even more rolls are also used to straighten tubing. The five-roll machine consists of two two-roll clusters and a middle deflecting roll. This machine has two large rolls on one side that are opposed or nearly opposed by three small rolls on the other. Two of the three small rolls and the two large rolls function as entry and exit feed rolls. The third small roll located between the other two small rolls functions as a deflecting roll. The rolls can be arranged in the horizontal or in the vertical plane.

In some machines, only the two large rolls are driven; in others, all rolls except the deflecting roll are driven, and sometimes all five rolls are driven. When more than two rolls are driven, the speed matching of roll surfaces becomes important. Matching can be obtained by maintaining the correct relationship between roll diameters or, more easily and accurately, by a differential drive between the two roll banks or, in some machines, by driving the rolls with individual motors having relay, continuous-feedback, or similar controls.

The six-roll machine has two middle deflecting rolls opposed to one another, similar to the entry and exit rolls in the five-roll straightener, but it differs from the five-roll straightener in that all rolls are of equal diameter. Normally, four or all six of the rolls are powered. Another type of six-roll straightener has a roll arrangement similar to that of a five-roll straightener with an additional outboard roll.

A seven-roll rotary straightener has two three-roll clusters--one at the entry end and one at the exit end of the straightener--and a middle deflecting roll (Fig. 1). Normally, the two bottom rolls are driven and the five others are idlers. The middle roll (deflecting roll) moves vertically, and the four end idler rolls move in a circular path about pivot points in the base and apply pressure to the tube for feeding and straightening. This seven-roll arrangement has the greatest effect at the middle roll because the tube is held perfectly in the pass.

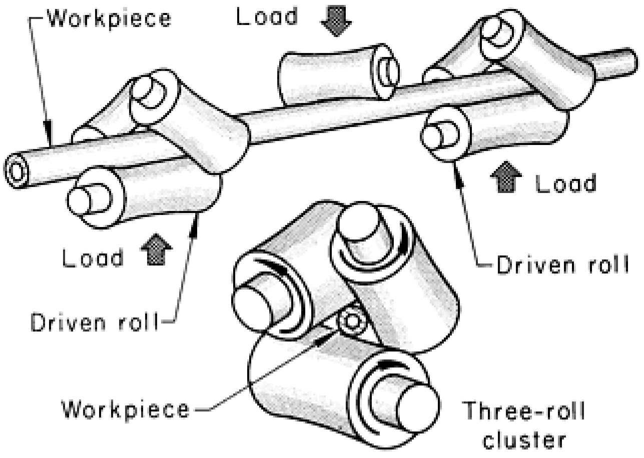


Fig. 1 Arrangement and principles of operation of three-roll clusters in a seven-roll rotary straightener

Various other multiple-roll straighteners have been built for specific applications. The general principles that apply to the use of multiple-roll machines are the same as those already described for two-roll straighteners.

Middle-roll offset straightening bends a straight tube by an amount that stresses the outer fibers of the tube to the elastic limit. When the straightening load is released, the tube springs back to its initially straight position. The outer fibers of an initially bowed tube are stressed beyond the elastic limit, and the tube springs back to a straight position.

Straightening of tubing in this manner can be termed single-pass straightening. It is possible, however, to arrange the rolls in the multiple-roll machine to double straighten each tube as it is conveyed through the machine. The middle roll stand is offset to deflect a bowed tube and to stress its outer fibers beyond the yield point, and in a similar manner, the tube is deflected over the third roll stand by an auxiliary roll or by the discharge table. The tube is stressed at both the middle and third roll stands--or, in effect, double straightened--with each pass through the machine.

Bending the tube, however, creates an area of strain or cold work directly under the bending load. The rotation of the tube as it goes through the machine generates a spiral of strained area around the periphery of the tube. The centerline of the third roll stand should be spaced such that no one point on the tube duplicates itself at the same location on both the middle and third rolls, thus eliminating the possibility of reworking the strained area.

Tube Deflection. The amount δ by which the middle roll or rolls must be offset from the entry and delivery rolls varies inversely with the outside diameter of the tube to be processed, as demonstrated below:

$$\delta = \frac{Sl^2}{6ED}$$

where S is the yield strength of the metal, l is the distance between the outboard rolls, E is the modulus of elasticity, and D is the outside diameter of the tube.

As the diameter of the tube decreases, the deflection requirements increase over a given span. Therefore, the factor that determines the smallest-diameter tube that a machine can process is the deflection requirement of the tube over the span of the rolls. Approximately 19 mm ($\frac{3}{4}$ in.) is the maximum deflection that can be applied to most tubes without adversely affecting travel of the tube through the rolls of a rotary straightening machine.

Applications. Multiple-roll machines are advantageous in processing thin-wall tubes, and thick-wall tubes having a high ratio of diameter-to-wall thickness. This is because of the lower unit loading on the workpiece that is applied across the longer bending spans between adjacent rolls. The multiple-roll straightener has an additional advantage in higher throughput speeds than other rotary straighteners, because of the higher angularity settings of the rolls. Therefore, multiple-roll straighteners are widely used in tube mill production lines.

Multiple-roll machines are ordinarily used for applications in which the primary purpose is the sizing or burnishing of the workpiece. They do not straighten as accurately as two-roll machines, nor do they remove end hooks effectively.

Long bows in medium- and heavy-wall tubes are removed by closing each roll pass and bending the tube with the middle roll. Closed-pass rolling provides full-length support within each roll stand. Straightening in this manner requires less deflection and spreads the bending load over the full length of each roll.

Long bows can sometimes be removed from extra-heavy-wall tubes by opening each roll stand and bending the tube between the two bottom rolls with the top middle roll. The object is to exert a minimum load on the tube.

Thin-wall tubes should be processed with low unit loads applied to the surface of the tube and with minimum deflection. High loads and excessive deflection will ring the interior of the tube. The bending load can be spread across the surface of the tube by angling the middle roll toward the centerline of the pass. Ovalizing and spreading the roll contact lower the required amount of deflection to produce a straight tube. Deflecting a tube over a long span requires a large offset that can make it difficult to convey the tube through the machine. However, proper roll design and roll spacing make it possible to process a wide range of outside diameters in the multiple-roll rotary straightening machine.

End hook can be removed from thin- and medium-wall tubes by ovalizing and bending the tube in the middle roll pass. Removing end hook from thick-wall tubing on multiple-roll straighteners is difficult because the center-to-center distance between the No. 1 and 2 roll stands is usually greater than the length of the end hook.

The roll angle of a straightening machine depends on the outside diameter of the tube to be straightened. In general, a machine used for small-diameter tubing in the range of 6.4 to 19 m ($\frac{1}{4}$ to $\frac{3}{4}$ in.) may have a roll angle of 40 to 45°. In some special highspeed straighteners, when the tubing is almost straight to begin with and is easy to straighten, a 40° angle can be used for tubing as large as 50 mm (2 in.). For machines in the range of 50 to 152 mm (2 to 6 in.), a common angle is 30°. For very large tubing 455 to 610 mm (18 to 24 in.) in diameter, the angle can be as low as 17$\frac{1}{2}$°.

To fit a large tube within the size range of the machine to the roll contour, the angle between the centerline of the tube and the roll must be greater than that required for a small-diameter tube. The angle can be adjusted from 2 to 3° on either side of the nominal angle for a given machine--the lowest angle for the smallest size and the largest angle for the largest size.

Entry and Delivery Tables. Well-designed entry and delivery tables are important in the rotary straightening of tubular products. The rotary straightening machine is designed to support, guide, and straighten a tube within the length between the first and last roll stands. It is not designed to feed, support, or confine a long tube over its full length.

Entry-table guides confine the portion of the tube that is beyond the limits controlled by the rotary straightening machine in order to minimize rotary whipping of the unstraightened portion. The table guides must be designed to suit the size range, grade of material, and travel rate of tube that is to be processed. A combination of entry and delivery tables and guides keeps the amount of straightener deflection, or offset, to a minimum, increases the range of sizes that can be straightened in any one machine, and decreases the possibility of damaging the tube.

The entry table should also feed the tube into the rotary straightener with skewed rolls, so that the tube will be rotating and moving lineally when it enters the straightener. When in-line straighteners are used at the mill, the straightener and tables must be designed to keep up with the speed of the mill.

Straightening of Tubing

Revised by Philippe Delori, SMS Sutton, Inc.

Ovalizing in Rotary Straighteners

Some straightening effect can be produced in certain tubular products not only by bending but also by squeezing them elastically between the opposed straightening rolls as the tubes are processed through a rotary straightening machine. The rolls have a concave curvature through which a line of contact is produced between the roll and the workpiece for almost the full length of the roll.

Machines having sets of two opposed rolls are used to combine bending and ovalizing in the straightening of round tubes. Squeezing results in high residual stresses that can be removed by subsequent bending operations.

Collapse Strength. The cold working of a tubular product by bending and ovalizing in a rotary straightening machine can reduce the collapse strength or external-pressure resistance of the tube. Therefore, cold working should be kept to a minimum. The least amount of bending and/or ovalizing that will still yield a straight tube should be used. It is believed that a round tube obtained by squeezing will result in good collapse resistance.

Introduction

WIRE FORMS are used to give a high strength-to-weight ratio, an open construction (as in fan guards or baskets), resilience to absorb shock, and the economy of automated production of formed parts. When production quantities are small or the size of the finished article is large, the wire may be straightened and cut to length as a preliminary operation before the individual pieces are fed into hand benders, kick presses, power presses equipped with appropriate dies, or coiling devices. For large quantities, the wire is straightened as it comes from the coil and is fed directly and continuously into power presses, automatic forming or spring-coiling machines, multiple-slide machines, or special machines actuated by cams, air, or hydraulic cylinders. Wire drawing is discussed in the article "Wire, Rod, and Tube Drawing" in this Volume.

Operations other than bending that are performed on wire include:

- *Threading* with single-head or multiple-head chasers, or with flat-die or rotary-die roll threaders. Roll dies can also be used for knurling, pointing, and chamfering
- *Heading* in open-die rod headers, to make a variety of heads such as flat, round, slotted, indented hexagon, tee, and ball
- *Swaging or extruding* of long points or reduced-diameter sections on rotary-die swagers or long-stroke headers
- *Welding* with resistance, arc, or gas

Speed of Forming. Increasing the speed of forming can result in out-of-tolerance parts, increased springback, and wear on the tools and machine caused by increased force and torque. With machines in which some of the tools are air-actuated and some are not, enough time must be allowed for the air-actuated tools to cycle to prevent the machine from going out of phase. For example, a machine with a mechanical drive and air-actuated tools was constantly out of phase when making 70 formed and welded assemblies per hour. By reducing the speed by about 5%, the air valves and cylinders had time to complete their cycles, and were in phase with the mechanical devices.

Tools used for forming wire should be made of tool steel hardened to 56 to 61 HRC. Water-hardening tool steels such as AISI W1 are usually adequate. For more severe forming and longer tool life, D2 tool steel is recommended. Surfaces contacting the wire should be polished to prevent marking. They can usually be hardened after tryout in the soft state.

Springback is variable and difficult to control in the forming of wire, as it is in most pressworking operations. Springback varies with the type and temper of wire and may be different for each lot of a specific type and temper. The most practical way to determine springback is to make trial bends either before the tools have been hardened or on temporary tools. The necessary final correction for springback is usually made at the final tool setup, after the tools have been hardened.

Effect of Material Condition

Most wire forming is done at room temperature. Wire made of low-carbon steel is usually formed in the as-drawn condition. Medium-carbon steel wire (1035 to 1060) is usually annealed before severe forming and heat treated after forming.

Surface Finish. A rough surface on the wire may cause short tool life. Plated wire is as easily formed as is bare wire, except that if the plating loosens or peels, it may damage the tools. Platings of gold, tin, solder, or other soft metals may show marks readily; however, soft plating may act as a lubricant during the forming of wire. Whether the wire can be plated before forming may depend on the severity of forming and the subsequent fabricating operations. Welding, for instance, may require that plating be done after forming.

Properties. The strength of wire is important in forming, especially when making steel springs. The required tensile strength is developed in spring wire either by cold drawing through a series of dies with up to 85% reduction in cross section, or by heat treating steel containing 0.60 to 0.70% C, quenching in oil, and tempering the wire. The elastic limit in torsion of spring wire is more important to its use in a spring than is its tensile strength. Information on the mechanical properties of steel spring wire is available in the article "Steel Springs" in *Properties and Selection: Irons, Steels, and High-Performance Alloys,* Volume 1 of the *ASM Handbook.*

Forming of Wire

Rolling of Wire in a Turk's-Head Machine

A Turk's-head machine generally has four rolls that will accommodate wire of one cross section (generally, round) and cold roll it to another shape. Uses of the machine are:

- To make accurate, square, and narrow rectangular wire directly from round wire
- To finish special shapes from round or preformed rough shapes
- To put edge contours on flat metal ribbon

Operation. The machine has a cluster of four rolls with the four axes in the same plane and at right angles to each other, as shown in Fig. 1. In operation, a coil of wire is supported in a pay-off reel; the wire is pulled through the rolls by a capstan and then recoiled. A drawbench can be used for pulling short lengths (up to 30 m, or 100 ft) through the rolls.

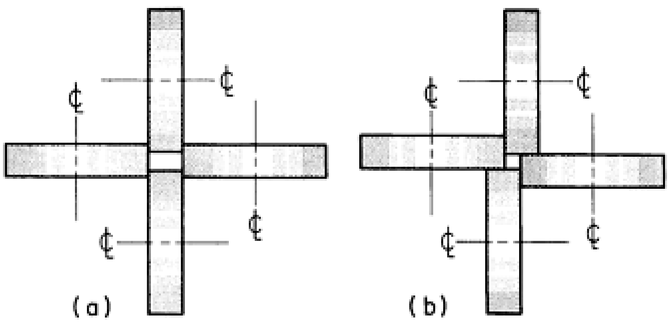


Fig. 1 Turk's-head rolls. (a) Positioned in line to form a rectangular cross section. (b) Offset to form a square section

Depending on the shape to be formed, the narrow rolls can be centered (opposed), as in Fig. 1(a), or they can be offset, as in Fig. 1(b). Although the rolls shown are plain cylinders, these can be replaced with rolls ground to any shape that will form the desired cross section. Some sections require several passes through the machine: It may take two passes to roll an accurate sharp-cornered square wire, and three or more passes may be needed to make a complex section fill properly.

Simple or complex shapes may be drawn through a Turk's-head machine as fast as 180 m/min (600 ft/min), depending on the force and speed available in the drawbench and the amount of heating in the operation. In general, the sections that can be formed depend on the ductility of the wire and are limited to shapes that can be ground into the rolls and shapes that are suited to the roll design, symmetrical, and no wider than twice the round wire thickness (unless preformed wire is used).

Some Turk's-head machines have three rolls to make triangular shapes and other shapes suited to a three-roll design.

Accuracy of forming in a Turk's-head machine depends on:

- Accuracy and uniformity of the initial round wire in size, shape, smoothness, hardness, and ductility
- Dissipation of heat caused by cold working
- Smooth operation of drive in accelerating, running, and decelerating
- The amount of reduction in area or change in section in one pass

Any variation in size of the round wire pulled through the rolls may cause changes in size and shape of the product. If the round wire is oversize in a portion of its length, it may cause a sharper corner in the shape and thus a longer cross-corner dimension, or it may form a fin. If the round wire is undersize, the shape will not be well filled, and the cross-corner dimension will be decreased.

Variations in the hardness and ductility of round wire can also cause variations in the cross-corner dimension of a shape. Hard spots increase the cross-corner dimension; soft spots decrease it. A rough or unlubricated surface increases cross-corner size; a smooth, oiled surface makes it smaller. Heating of the rolls caused by cold working may enlarge the rolls, making the product smaller. A coolant is frequently used to remove heat from the rolls.

Nonuniform acceleration, running and deceleration of the capstan, and changes in tension on the wire may also cause variations in the formed shape. The greater the reduction in section size of the wire as it is drawn through the rolls, the greater the chances of variations in the formed shape.

Tolerances in wire formed in a Turk's-head machine in ordinary production are ±0.05 mm (±0.002 in.), but ±0.013 mm (±0.0005 in.) is a reasonable tolerance if all important factors are controlled.

Spring Coiling

Small quantities of springs may be coiled in a lathe. The arbor around which the spring is wound is held in the chuck, and two wooden friction blocks are mounted on the cross slide. Numerous hand-operated devices are also used. Production coiling is done in single-purpose automatic spring coilers.

In a standard spring-coiling machine, a pair of feed rolls pushes a calculated length of straightened wire through restricting guides against a coiling point and around a fixed arbor into a coil. At the end of the coiling cycle, the feed rolls stop, and a cutoff mechanism actuates a knife, which severs the completed spring against the arbor. A flying knife separates the completed spring from the wire strand.

Standard spring-coiling machines range in size from those that can coil only fine wire to those that can form 19 mm ($\frac{3}{4}$ in.) diam cold-drawn or 15.9 mm ($\frac{5}{8}$ in.) diam pretempered wire. Each coiler can process a range of wire diameters, depending on the number and size of half-round grooves in the feed rolls. A set of feed rolls usually has grooves of three or four different sizes. For example, a machine might coil wire 2.32 to 5.26 mm (0.0915 to 0.207 in.) in diameter and make a spring with an index (ratio of mean spring diameter to wire diameter) ranging from 3 to 18. The length of wire fed is controlled by the feed rolls.

A coiler equipped with a variety of attachments and cams can produce almost any type of spring, including tight-wound extension springs, common compression springs with either open ends or ends closed for grinding, barrel-type springs of various contours, tapered springs, single-coil springs, variable-pitch compression springs, and torsion springs.

A tight-wound extension spring is wound in a standard coiler, but the end loops are formed in one of three ways:

- One or more of the end loops are opened up or pulled out in a secondary operation to form the required end hooks
- End loops are automatically formed by deflecting the wire into shape as a part of the coiling operation
- Automatic handling equipment and attachments are incorporated as additions to a regular coiler to make the loops

The second method is less complicated to set up and operate, but both it and the third method are limited to wire less than 1.27 mm (0.050 in.) in diameter and, because of setup time, to production runs of not less than 10,000 pieces.

A machine equipped with a torsion-spring attachment forms straight extended arms; these arms can be formed and looped as desired in a second operation.

Compression springs are wound in standard coilers equipped with a pitch tool located under the first formed coil. This tool, controlled by cams, regulates the spacing between the coils, which may be either uniform or variable. The ends of compression springs may be plain, plain and ground, square, or square and ground.

The use of round wire predominates in making compression springs, although square, rectangular, or special-section wire is necessary in some applications. Square or rectangular wire is used to obtain the maximum load capacity for a given space. Wire with square corners before coiling will upset at the inside of the coil and become trapezoidal in section after coiling. This limits the deflection per coil, especially with small ratios of mean diameter to wire thickness.

Accuracy. The cams, gears, and other parts of a coiling machine become worn as the machine is used, resulting in a less accurate product. Some product inaccuracies can be reduced by control of the speed of the machine.

Dimensional variations for different materials are caused by variations in springback and by distortion during heat treatment. Variations in pitch and diameter depend on the speed of coiling. Dimensional variation depends also on the ratio of wire size to diameter and the ratio of pitch to spring diameter. By increasing the limits slightly, coiling speed and production rate may be increased.

Many springs are acceptable with inaccuracies or with a wide tolerance in dimensions and performance, but some springs (valve springs, for instance) must be more accurate. Variations in mechanical properties of wire will result in nonuniform springs. Standard spring wire has a permissible tensile-strength variation of 172 to 241 MPa (25 to 35 ksi); wire for valve springs has a limit of variation of 138 MPa (20 ksi). The range for any one coil seldom exceeds 35 MPa (5 ksi).

Coiling must be fast enough to produce an even flow of wire from the pay-off reel. If the wire flow is jerky or nonuniform, the dimensions of the spring may vary excessively.

Forming of Wire

Manual and Power Bending

Manual and power bending are done in rotary benders, pneumatic or hydraulic formers, and special fixtures to locate the bend and hold the part. The blanks are straightened and cut to length before forming.

A rotary bender can be either manual or power operated. The precut blank is placed between a center pin or form block and a stop pin. The arm supporting the wiper block is rotated clockwise, thus forming the wire around the center pin. An adjustable stop controls rotation of the arm so that uniformity is maintained from piece to piece. The eyebolt shown in Fig. 2 was formed in a rotary bender from 12.7 mm ($\frac{1}{2}$ in.) diam cold-drawn stock at a production rate of 300 pieces per hour.

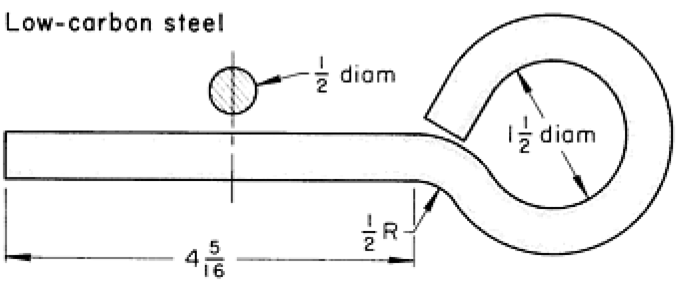


Fig. 2 Eyebolt formed in a rotary bender. Dimensions given in inches

The center pin can be changed to suit the bend radius. Form blocks can be used for larger radii. The stop pin is movable to accommodate different bend radii and stock thicknesses. Round, square, or rectangular wire can be bent in this equipment.

A more complex wire form, such as the scroll shown in Fig. 3, can be formed around blocks mounted in a rotary bender. The compact shape at each end could be formed by holding the wire taut while it is wrapped around a rotating form block, or in a machine with a spring-loaded or air-loaded wiper block. Another form block could be used for the long curves. The form blocks in these two operations must be designed to compensate for springback. The sharp bends at the outer ends can be made in a rotary bender around a center pin.

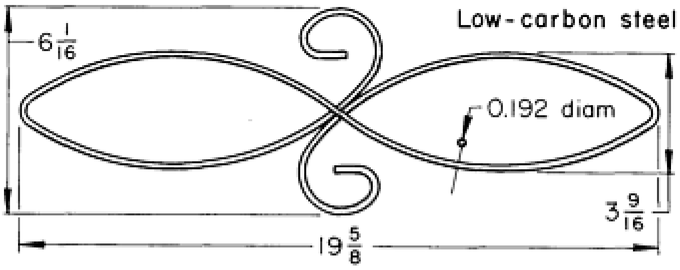


Fig. 3 Complex wire scroll formed in a rotary bender. Dimensions given in inches

A new development in wire-bending machines is a hydraulic, computer-controlled (CNC), three-axis wire bender. This machine is completely automatic, taking wire directly from the coil, straightening it, making any number of bends through any angle and curvature and in any plane, and cutting the finished product. The three-axis CNC wire bender can handle mild steel, spring steel, or aluminum wire from 2 to 7 mm (0.078 to 0.275 in.) in diameter. Bending is by a single head that rotates to give the third dimension. When the wire diameter is unaltered, changeover from one bend to another can take as little as 5 s because bending programs are stored in a built-in memory. When the wire diameter varies, change-overs can be accomplished in about 15 min.

Forming in Multiple-Slide Machines

Multiple-slide machines are automatic mass-production machines that make completed wire (or sheet metal) products from coiled stock. They can straighten, feed, cut, stamp, and form the wire, all in one continuous operation. Attachments are used for additional operations on wire up to 12.7 mm ($\frac{1}{2}$ in.) in diameter. Feed lengths of wire used to make one part may be as much as 915 mm (36 in.).

Most multiple-slide machines are horizontal, but some are vertical and some are inclinable. Production lots of 10,000 pieces usually justify the use of a multiple-slide machine, but smaller lot sizes may also be economical.

A multiple-slide machine usually includes a wire straightener, feed mechanism, and stock clamp, and has a bed with four forming slides, a center post, and a stripper. The bed has room for a press head (an attachment that does presswork) and may have provision for other attachments. More information on forming in multiple-slide machines is available in the article "Forming of Steel Strip in Multiple-Slide Machines" in this Volume.

Production Problems and Solutions

Problem: A small garment eye was produced at the rate of 300 parts per minute, but desired flatness was not obtained. The eye was to be formed in one plane.

Solution: The tooling was changed to form the eye on a plate, confining the wire. Forming sections were supported in the plate and were retracted below the surface of the plate before ejection.

Problem: Broaching of a flat on a small formed part for a business machine caused fine chips to collect in the forming tools, interfering with the work.

Solution: Forming was done on the upper level, and broaching was accomplished on the lower level of the machine. A small jet of air blew the chips down and away from the forming tools.

Problem: A small electronics part was formed around a fragile center post that broke after a few thousand pieces were formed.

Solution: The tooling was changed so that the part was made in two stages. A heavy center post was used for the first form, where the greatest pressure was exerted. A center post having the shape of the smaller section controlled final closing of the part.

Problem: Ordinary forming tools could not form brass wire of 1.02 mm (0.040 in.) diam into a 9.53 mm (0.375 in.) OD ring.

Solution: The formed ring was sized by being pushed through a die below the forming level, by means of a ring-setting attachment.

Problem: Steel wire 5.71 mm (0.225 in.) in diameter was formed into seat wire in a large multiple-slide machine, using an 860 mm (34 in.) feed length. The front toot was 510 mm (20 in.) wide. The bearings of the front shaft became hot, and the cam roller of the front forming slide had to be replaced often.

Solution: The addition of auxiliary front slides at each side of the standard front slide provided more direct application of the forming pressure, with three motions from the front position, reducing the load on each tool. Forming loads were exerted at different points in the machine cycle, reducing bearing pressure and allowing faster machine speed.

Problem: A specially arranged multiple-slide machine for forming and welding handles from low-carbon steel wire 2.4 mm ($\frac{3}{32}$ in.) in diameter made imperfect welds. There were also variations in forming. The wire varied in diameter and tensile strength, which affected the feed length and forming. Operators had not been trained to adjust and maintain the machine.

Solution: The wire was specified to closer tolerances and inspected before use. Operators were trained, and a program of preventive maintenance was initiated. Production efficiency increased to 75% from as low as 25%.

Lubricants

Requirements of lubricants for wire-forming operations are more severe than for most other metalworking operations. The exceptionally high working pressures that may be reached require special lubricants to prevent galling, seizure, or fracture of the wire, as well as excessive tool wear. Improper lubricating oils or compounds interfere with close-tolerance work and cause variations in the finished parts. The lubricant varies with the type of wire. Aluminum, copper alloys, basic steel wire, and steel spring wire each require a different lubricant. Lubricants for wire forming can generally be classed in three groups: inorganic fillers, soluble oils, and boundary lubricants.

Inorganic fillers include solids such as white lead, talc, graphite, and molybdenum disulfide in a vehicle such as a neutral oil or paraffin oil.

Soluble oils include mineral oils to which agents such as sodium sulfonates have been added to make the oil emulsifiable in water. Soluble oils are good for cooling and corrosion prevention.

Boundary lubricants are thin, adsorbed films and are usually subjected to high unit pressures. Thin-film lubricants are of two basic types:

- *Polar Lubricants.* Lubricants, or constituents of lubricants, capable of either physical or chemical adsorption on a solid surface to form a thin film that resists mechanical removal and provides lubrication under high unit pressures
- *Extreme-Pressure Lubricants.* Lubricants capable of reacting chemically with solid surfaces under rubbing conditions, to prevent welding and provide lubricant reaction products on the surface. Extreme-pressure lubricants permit high unit loading with a minimum of surface wear and damage

Chemically active constituents of typical boundary lubricants are sulfur, chlorine, and phosphorus compounds.

Applications. Certain types of oil, wax, and tallow are used to lubricate aluminum and scaly steel wire. Mixtures of lard oil or of paraffin oil in kerosine, or of oil and a soap solution, have been used as lubricants for wire forming.

Often the lubricant used in drawing wire is expected to stay on the drawn wire in a quantity that is adequate for subsequent forming operations. Many severe operations, such as upsetting and spring coiling, may be done without additional lubrication, but additional lubricant may be used in some press and rolling operations.

The lubricant remaining after wiredrawing should be enough to lubricate a wire formed over a form tool or a mandrel. The lubricant should be a hard, dry coating, such as a mixture of lime and metal soaps. This will protect the wire from damage in forming and will extend the life of the tools without sticking to them.

Zinc phosphate is often used to coat wire before it is redrawn into smaller sizes. It is also used in coiling thick, high-tensile spring wire into a closed helix. In other difficult forming, such as a large upset, a zinc phosphate coating is a good lubricant for all tools.

Wire for forming into products to be electroplated is usually drawn with a lubricant that can be easily removed and that does not contain small particles that could become embedded in the surface of the wire. After drawing, the wire may be sprayed or dipped in thin oil. The oil protects the wire from corrosion and serves as a lubricant in the forming operations.

Steel wire with a metal coating, such as zinc, tin, copper, brass, or lead, is often used in forming. In some operations, the metal coating provides all the lubrication necessary.

When lubricant must be added to the wire, it can be applied at the uncoiler or at the tools. Soluble oil or wax in water is most practical and is easy to remove in cleaning. Some formed wire must be completely clean.

Spring wire is supplied with a coating that acts as a lubricant. The coating may be a mixture of soap and lime or of borax or phosphate, or it may be a plating (or displacement coating) of cadmium, zinc, tin, or copper. When the coil is to be electrically normalized, a borax coating is specified; other coatings insulate the wire from good electrical contact.

The most unusual lubricant may be the one that comes on the oil-tempered grade of valve spring wire. During heat treatment, oxidation of the surface is permitted under carefully controlled conditions. The scale thus formed acts as a lubricant during coiling. Its characteristics must be carefully controlled with respect to thickness, adherence, and flakiness, for not only must it supply the required lubrication during coiling, but it should detach from the surface at the same time.

Shearing of Plate and Flat Sheet

Revised by Robert A. Westerkamp, Cincinnati Inc.

Introduction

SHEARING of sheet and plate is broadly classified according to the type of knife (cutter) used--straight or rotary. Straight-knife shearing is used for squaring and cutting flat stock to the required shape and size. It is usually used for square and rectangular shapes, although triangles and other straight-sided shapes are also sheared with straight knives. Rotary shearing (not to be confused with slitting, which is discussed in the article "Slitting and Shearing of Coiled Sheet and Strip" in this Volume) is used for producing circular or other contoured shapes from sheet or plate.

Shearing of Plate and Flat Sheet

Revised by Robert A. Westerkamp, Cincinnati Inc.

Straight-Knife Shearing

In straight-knife shearing, the work metal is placed between a stationary lower knife and a movable upper knife. As the upper knife is forced down, the work metal is penetrated to a specific portion of its thickness. The unpenetrated portion then fractures, and the work metal separates (Fig. 1). The amount of penetration depends largely on the ductility and thickness of the work metal. The knife will penetrate 30 to 60% of the work metal thickness for low-carbon steel, depending on thickness (see the section "Capacity" in this article). The penetration will be greater for a more ductile metal such as copper. Conversely, the penetration will be less for metals that are harder than low-carbon steel.

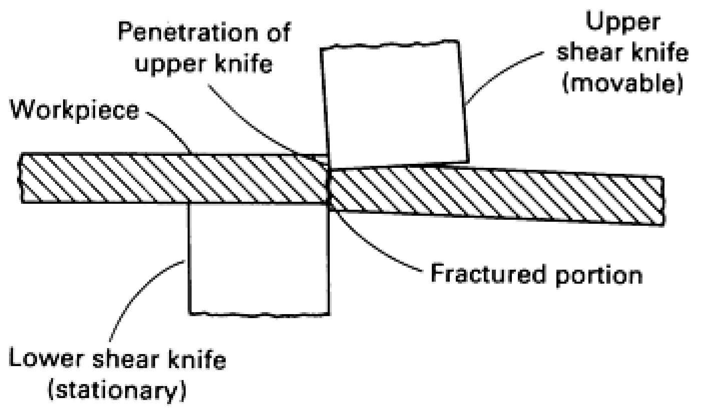


Fig. 1 Schematic showing the mechanism of straight-knife shearing.

A sheared edge is characterized by the smoothness of the penetrated portion and the relative roughness of the fractured portion. Sheared edges cannot compete with machined edges, but when knives are kept sharp and in proper adjustment, it is possible to obtain sheared edges that are acceptable for a wide range of applications. The quality of sheared edges generally improves as work metal thickness decreases.

Applicability

Straight-knife shearing is the most economical method of cutting straight-sided blanks from stock no more than 50 mm (2 in.) thick. The process is also widely used for cutting sheet into blanks that will subsequently be formed or drawn. Because shear gaging can be set within ±0.13 mm (±0.005 in.), the shearing process is generally limited to ±0.4 mm ($\pm\frac{1}{64}$ in.) tolerances in 16 gage material. The tolerance range increases with thickness.

Straight-knife shearing is seldom used for shearing metal harder than about 30 HRC. When extremely soft, ductile metal (especially thin sheet) is sheared, the edges of the metal roll and large burrs result. As the hardness of the work metal increases, knife life decreases for shearing a given thickness of metal.

In general, it is practical to shear flat stock up to 38 mm (1 $\frac{1}{2}$ in.) thick in a squaring shear. Squaring shears up to 9 in (30 ft) long are available (even longer shears have been built), and some types are equipped with a gap that permits shearing of work metal longer than the shear knife.

Machines for Straight-Knife Shearing

Punch presses and press brakes are sometimes used for shearing a few pieces or are used temporarily when more efficient equipment is not available. Production shearing, however, is usually done in machines that are designed for this operation.

Squaring shears are usually used for trimming and cutting sheet or plate to specific size (Fig. 2). Squaring shears (also called resquaring or guillotine shears) are available in a wide range of sizes and designs. Some types permit slitting by moving the work metal a predetermined amount in a direction parallel with the cutting edge of the knife after each stroke of the shear.

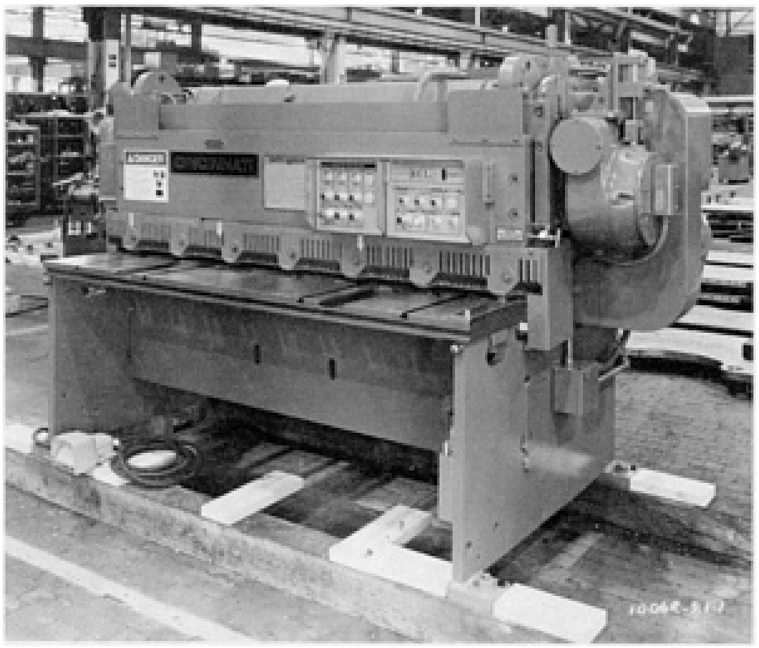


Fig. 2 Typical squaring shear. Courtesy of Cincinnati Inc.

The sheet or plate is held rigid by hold-down devices while the upper knife moves down past the lower knife. Most sheet or plate is sheared by setting the upper knife at an angle. The position of one knife can be adjusted to maintain optimal clearance between the knives. Squaring shears can be actuated mechanically, hydraulically, or pneumatically.

Mechanical Shears. The power train of a mechanical shear consists of a motor, the flywheel, a worm shaft that is gear driven by a flywheel, a clutch that connects the worm gear drive to the driveshaft, and a ram actuated by the driveshaft through eccentrics and connecting links. Under most operating conditions, a mechanical shear can deliver more strokes per minute (spm) than a hydraulic shear. Some mechanical shears cycle as fast as 100 spm.

Another advantage of the mechanical shear is that, because of the energy stored in the flywheel, a smaller motor can be used for intermittent shearing. For example, a mechanical shear with a no-cutting or free-running speed of 65 spin can make approximately six full shearing strokes (maximum thickness and length of cut) per minute with a standard motor. However, when the same shear is cutting at full capacity in a rapid shear mode, a much larger motor is required. For such rapid cutting, there is not enough time between cuts for the smaller motor to restore the speed of the flywheel.

An additional advantage of the mechanical shear is that its moving knife travels faster than the moving knife of a hydraulic shear. In some cases, greater knife speed can decrease work metal twist, bow, and camber.

Hydraulic shears are actuated by a motor-driven pump that forces oil into a cylinder against a piston; the movement of the piston energizes the ram holding the upper knife. A hydraulic shear can make longer strokes than a mechanical shear. In general, long shears and shears with low-carbon steel capacities above 12.7 mm ($\frac{1}{2}$ in.) are almost all hydraulic.

Hydraulic shears are designed with a fixed load capacity. This prevents the operator from shearing material that exceeds capacity and therefore saves costly damage to the machine structure. This is a basic advantage of hydraulic shears.

The total load that is experienced during the cut is related to the rake angle, sharpness of the knives, mechanical properties of the material, type of material, knife clearance, and the depth of the back piece. It is possible to stall the machine on a rated-capacity cut if the clearance is incorrect, the knife is dull, or the back piece is excessively deep. In this way, the hydraulic shear is protected from damage caused by overloading. A mechanical shear would not be constrained by an overload prevention system and would continue to cut under nearly all conditions.

Most mechanical shears are provided with enough horsepower to build up the flywheel speed after each cut, but not enough to allow the operator to run full-capacity cuts in a high-speed manner. Mechanical shears are rated in strokes per minute, not cuts per minute. Most shearing applications do not require high-speed cutting.

Hold-down pressure must be greater than the forces generated in cutting the work material. These forces depend on the knife clearance, rake angle, and depth of material back piece.

Greater knife clearance must be used to prevent the hydraulic shears from stalling when shearing large-area work, especially at or near maximum thickness capacity. For example, in cutting a 3 × 3 m (10 × 10 ft) plate into two equal parts, greater shearing force is required than for trimming a narrow strip from a 3 m (10 ft) long plate.

Pneumatic shears are used almost exclusively for shearing thin metal (seldom thicker than 1.52 mm, or 0.060 in.) in relatively short pieces (seldom longer than 1.5 m, or 5 ft).

Alligator shears have a shearing action similar to that of a pair of scissors. The lower knife is stationary, and the upper knife, held securely in an arm, moves in an arc around a fulcrum pin. This type of machine is most widely used for shearing bars and bar sections and for preparing scrap.

Alligator shears are available in various sizes, including those that can shear plate up to 32 mm ($1\frac{1}{4}$ in.) thick by 762 mm (30 in.) long and plate up to 50 mm (2 in.) thick in shorter lengths. These machines vary in weight from about 1130 to 19,500 kg (2500 to 43,000 lb). The lighter machines can be made portable; the heavier machines, however, must be firmly anchored in concrete, especially if they will be used in conjunction with roller conveyor tables in the shearing of plate.

Capacity. Most shearing machines are rated according to the section size of low-carbon steel they can cut. The tensile strength of low-carbon steel sheet and plate is generally no higher than 520 MPa (75 ksi); the yield strength, no greater than 350 MPa (51 ksi). Shears frequently are rated in terms of their ability to cut low-carbon steel with a tensile strength of 414 MPa (60 ksi) and yield strength of 276 MPa (40 ksi). An allowance for normal over-tolerance material thickness is included in the capacity rating of the machine. The use of a machine for shearing other metals is primarily based on the relationship of the tensile strength and ductility of low-carbon steel to that of the metal to be sheared. Metals with a tensile strength greater than that of low-carbon steel almost always reduce the capacity of the machine. For example, the machine capacity for shearing high-strength low-alloy steels is reduced to about two-thirds to three-quarters of the rated capacity for low-carbon steel. Conversely, for shearing aluminum alloys, machine capacity can range from $1\frac{1}{4}$ to $1\frac{1}{2}$ times the rated capacity for low-carbon steel.

Table 1 compares the shearing capacities of various metals with those of low-carbon steel. The metal thicknesses given in Table 1 are based on the thickness of low-carbon steel that can be sheared with the same shearing capacity. For example, a specific force is required to shear 6.4 mm ($\frac{1}{4}$ in.) thick low-carbon steel. Table 1 shows that the same force can shear only a 4.8 mm ($\frac{3}{16}$ in.) thickness of type 302 stainless steel, but can shear a 9.5 mm ($\frac{3}{8}$ in.) thickness of aluminum.

Table 1 Shearing capacities for various metals compared to those for low-carbon steel

Thickness of low-carbon steel[(a)]		Thickness that can be sheared with same force as for low-carbon steel					
		AISI type 302 stainless steel[(b)]		Full-hard steel strip		Aluminum alloys	
mm	in.	mm	in.	mm	in.	mm	in.

1.52	0.060	0.91	0.036	1.22	0.048	1.90	0.075
1.90	0.075	1.22	0.048	1.52	0.060	3.05	0.120
3.05	0.120	1.52	0.060	1.90	0.075	3.40	0.134
3.40	0.134	1.90	0.075	2.67	0.105	4.8	$\frac{3}{16}$
4.8	$\frac{3}{16}$	3.40	0.134	3.9	$\frac{5}{32}$	5.6	$\frac{7}{32}$
6.4	$\frac{1}{4}$	4.8	$\frac{3}{16}$	4.8	$\frac{3}{16}$	6.4	$\frac{1}{4}$
7.9	$\frac{5}{16}$	5.6	$\frac{7}{32}$	5.6	$\frac{7}{32}$	9.5	$\frac{3}{8}$
9.5	$\frac{3}{8}$	6.4	$\frac{1}{4}$	6.4	$\frac{1}{4}$	11.1	$\frac{7}{16}$
11.1	$\frac{7}{16}$	7.9	$\frac{5}{16}$	7.9	$\frac{5}{16}$	12.7	$\frac{1}{2}$
12.7	$\frac{1}{2}$	9.5	$\frac{3}{8}$	9.5	$\frac{3}{8}$	15.9	$\frac{5}{8}$
15.9	$\frac{5}{8}$	11.1	$\frac{7}{16}$	11.1	$\frac{7}{16}$	19.0	$\frac{3}{4}$
19.0	$\frac{3}{4}$	12.7	$\frac{1}{2}$	12.7	$\frac{1}{2}$	25.4	1
22.2	$\frac{7}{8}$	15.9	$\frac{5}{8}$	15.9	$\frac{5}{8}$	31.8	$1\frac{1}{4}$
25.4	1	19.0	$\frac{3}{4}$	19.0	$\frac{3}{4}$	38.1	$1\frac{1}{2}$
31.8	$1\frac{1}{4}$	25.4	1	25.4	1	50.8	2

(a) Also applicable to soft to half-hard strip steel, alclad steel, and copper and copper alloys.

(b) Also applies to most other austenitic stainless steels, normalized alloy steels such as 4130 or 8630, annealed high-carbon steels, and annealed tool steels

Ductility, measured by the elongation of the work metal, can also affect machine capacity. For example, annealed copper, because of its high elongation, requires as much shearing effort as low-carbon steel, even though copper has considerably lower tensile strength. Similarly, carbon steel with very low carbon (<0.1% C) and higher-than-normal elongation will reduce the capacity of a machine.

Power Requirements. The energy consumed during shearing is a function of the average stress, the cross-sectional area to be sheared, and the depth of maximum knife penetration at the instant of final fracture of the work metal. For any metal, the amount of energy consumed is proportional to the area under the shearing stress-strain curve for that metal.

Figure 3 shows typical shearing stress-strain curves for hot-rolled and cold-rolled steels. The distance through which the force acts (knife penetration) is near 35% of the work metal thickness for hot-rolled steel and 18.5% for cold-rolled steel. For example, in the curve for hot-rolled steel, the average stress under the curve is 73.5% of the maximum shearing stress S_{max}, and the distance through which the force acts is 35% of the work metal thickness. Therefore, the energy E used in shearing hot-rolled steel is:

$$\begin{aligned} E &= 0.735\, S_{max} \cdot Wt \cdot 0.35\, t \\ &= 0.257\, S_{max} \cdot Wt^2 \end{aligned} \quad \textbf{(Eq 1)}$$

where W is work metal width and t is work metal thickness. Applying Eq 1 to the curve for cold-rolled steel in Fig. 3 yields an energy consumption of $0.136S_{max} \cdot Wt^2$.

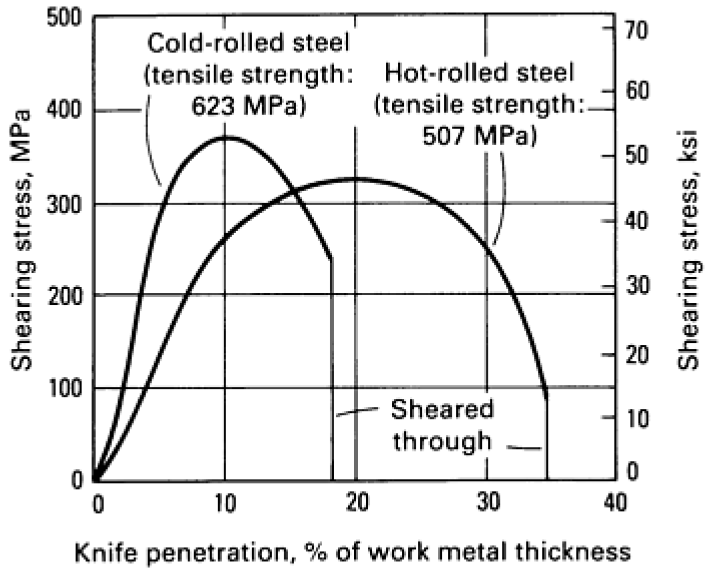


Fig. 3 Typical shearing stress-strain curves for hot-rolled and cold-rolled steels.

The maximum instantaneous horsepower HP_{max} required for cutting work metal in a shear is determined by:

$$HP_{max} = \frac{Wt \cdot S_{max} \cdot V}{33\,000} \quad \textbf{(Eq 2)}$$

where V is the speed of the shear knife and the other variables are as previously defined.

The average power requirement HP_{ave} for a shear making n cuts per minute in hot-rolled steel is:

$$HP_{ave} = \frac{Wt^2 \cdot n \cdot S_{max}}{1\,540\,000} \quad \textbf{(Eq 3)}$$

Equations 2 and 3 determine the net power required for actual shearing of the workpiece. The amount of power needed to operate the hold-down system and to overcome friction must be added to net power.

Friction depends on the design of the shearing machine and the knife, type of bearings, alignment, lubrication, temperature of operation, and size of the machine in relation to the area of the section to be sheared. When shearing metal of nearly the maximum size for which a shear is designed, the loss of horsepower by friction for well-designed machines seldom exceeds 25% of the gross horsepower.

Accessory Equipment for Straight-Knife Shearing

Certain pieces of accessory equipment have been incorporated into most shear designs and are required for efficient and accurate straight-knife shearing.

Hold-downs (Fig. 2) are mechanical or hydraulic devices that hold the work metal firmly in position to prevent movement during shearing. The most efficient hold-down system is a series of independent units that securely clamps stock of varying thickness automatically and without adjustment.

The force on each hold-down foot must be substantial and can range from several hundred pounds on a machine for shearing sheet to several tons for shearing plate. Hold-downs must be timed automatically with the stroke of the ram so that they clamp the work metal securely before the knife makes contact and release their hold instantly after shearing is completed.

Back gages are adjustable stops that permit reproducibility of dimensions of sheared workpieces in a production run. Most gages are controlled electrically. Push-button control provides a selection of fast traverse speeds and slow locating movements for accurate final positioning. The addition of a microcomputer (Fig. 4) permits gage positions to be quickly entered. An LED display enables the operator to confirm immediately the gage position entered, the current gage position, or the final gage location after positioning. Accurate gage screws, compensating nuts, precision slides and guides, and decimal indicators permit repeatable gage settings to an accuracy of 0.025 to 0.05 mm (0.001 to 0.002 in.).

Fig. 4 Microprocessor-controlled shearing machine, with inset (lower right) showing LED display. Courtesy of Cincinnati Inc.

For thin sheet, magnetic overhead rollers eliminate sag and support the sheet for accurate gaging to a depth of 1.2 m (4 ft) into the shear. For rapid and accurate cutting, back gages are equipped with electronic sensors that automatically trip the shear only when the sheet is accurately positioned.

Pneumatic sheet supports are used to support ferrous and nonferrous thin sheet. Sheet support arms are designed to elevate into a horizontal position, flush with the shear table, permitting material to be supported in the correct position against the backgage stop. Blank inaccuracies due to unsupported and poorly positioned sheets are virtually eliminated.

Back gages are also equipped with retractable stops for shearing mill plate. With the stops out of the way, mill plate of almost any length can be fed into the shear and cut to the desired length. When stops are not used, the workpiece can be notched or scribed to indicate the cutoff position.

Front Gages. When gaging from the front of the machine, the operator locates the work metal by means of stops secured in the table or in the front support arms. Power operation of the front support arms allows the blank dimensions to be entered digitally using a microcomputer-based control. Front gaging is often done by means of a squaring arm.

Squaring arms (Fig. 5) are extensions attached to the entrance side of a shearing machine that are used to locate long sections of work metal in the proper position for shearing. Each arm is provided with a linear scale and with stops for accurate, consistent positioning of the work metal. Squaring arms are reversible to allow use of the shear at either end and to distribute the wear on the shear knives. Power operation can be added to the squaring arm for increasing blank accuracy and reducing setup time. This type of squaring arm prevents the arm from moving from side to side.

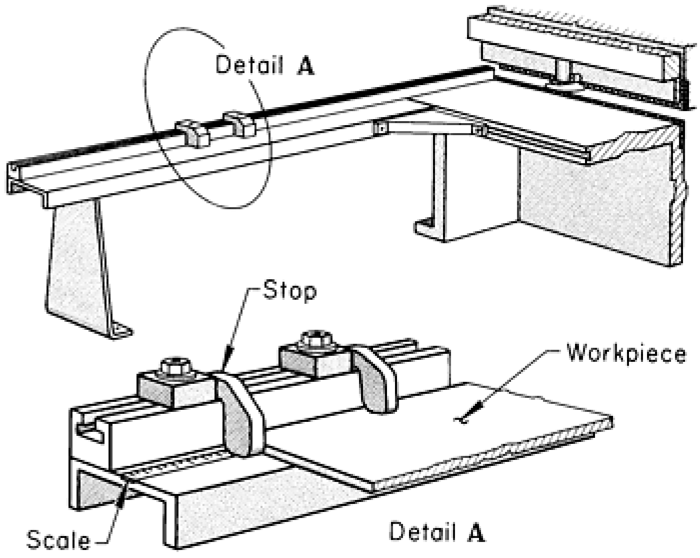


Fig. 5 Squaring arm attachment for positioning long pieces in a shearing machine.

Straight Shear Knives

Most shear knives are made in one piece from tool steel; some are made of carbon or alloy steel. The composition, thickness, and quantity of metal being sheared are the most important factors in the selection of knife material. In Table 2, AISI D2 tool steel is often recommended for cold shearing metals up to 6.4 mm ($\frac{1}{4}$ in.) thick. Knives made of modified A8 or H13 and S5 tool steels are recommended for low-volume production or for occasional shearing of metals up to 6.4 mm ($\frac{1}{4}$ in.) thick (except the more highly abrasive metals such as silicon steel). Knives made of A2 tool steel have been satisfactory for the high-production cold shearing of soft nonferrous metals, but D2 knives are usually more economical because of better wear resistance. Knives made of D2 tool steel are usually not recommended for cold shearing materials more than 6.4 mm ($\frac{1}{4}$ in.) thick, because they are likely to break under impact loads. However, depending mainly on knife design and length of cut, knives made of D2 tool steel have been successfully used for cold shearing aluminum alloys up to 32 mm (1 $\frac{1}{4}$ in.) thick.

Table 2 Recommended materials for straight shear knives cold shearing of flat metals

Metal to be sheared	**Thickness, ·6.4 mm ($\frac{1}{4}$ in.)**		**Thickness, 6.4-12.7 mm ($\frac{1}{4}$ - $\frac{1}{2}$ in.)**		Thickness, >12.7 mm ($\frac{1}{2}$ in.)
	Low production	**High production**	**Low production**	**High production**	
Carbon and low-alloy steels (up to 0.35% C)	Modified A8; H13; L6	D2	Modified A8; H13; L6	A2	**S5**[(a)]
Carbon and low-alloy steels (0.35% C)	Modified A8; H13; L6	D2	Modified A8; H13; L6	S5	**S5**[(a)]
Stainless steels and heat-resistant alloys	Modified A8; H13; L6	D2	S5	A2	**S5**[(a)]
Silicon electrical steels	D2	D2; carbide	S5	S5	[(b)]
Copper and alloys; aluminum and alloys	Modified A8; H13; L6	A2; D2	Modified A8; H13; L6	A2	**S5**[(a)]
Titanium and titanium alloys	**D2**	**D2**	. . .	. . .	. . .

(a) S5 is preferred for stock thicker than 19 mm ($\frac{3}{4}$ in.).

(b) Seldom sheared thicker than 12.7 mm ($\frac{1}{2}$ in.)

Modified A8 or H13 tool steels are suitable for knives for some cold-shearing applications in which the work metal is more than 6.4 MM ($\frac{1}{4}$ in.) thick, as indicated in Table 2. However, the shock-resistant grades S2 and S5 are usually recommended for shearing heavy sections of all metals.

The length and the design of the knife sometimes influence the selection of knife material. Although water-hardening tool steels such as W1 and W2 are suitable for many cold-shearing applications, heat treatment causes greater distortion in these steels than in the oil-hardening or air-hardening grades. A bar 4.16 m (164 in.) long is needed to make a shear knife 4.16 m (164 in.) long from D2 tool steel. The same knife made from W2 requires a bar 4.17 m (164 $\frac{1}{4}$ in.) long. Both steels elongate when heat treated, but W2 will bow more readily than D2. Because straightening is difficult, the W2 knife must have more grinding stock. The additional grinding decreases the depth of the hardened shell and shortens the useful life of the knife.

Hardness. The rate at which a knife wears in cold shearing depends primarily on its carbon content, alloy content, and hardness. Insufficient hardness in a knife that is used for cold shearing will shorten its service life. In one application, a knife made of S5 tool steel with a hardness of 44 HRC wore three times as fast as one with a hardness of 54 HRC used under the same conditions. Despite the desirability of having shear knives as hard as possible to minimize wear, it is often necessary to sacrifice some hardness to prevent knife breakage as the hardness or thickness of the metal being sheared increases.

Recommendations for the hardness of knives for cold shearing cannot always be made without knowledge of the details of the operation. For example, a D2 knife performed satisfactorily at a hardness of 61 HRC in one application, but knives at this hardness broke under similar operating conditions in a different plant. Knives made of D2 steel will usually operate successfully at 58 to 60 HRC for shearing low-carbon steel up to 6.4 mm ($\frac{1}{4}$ in.) thick, and knives made of D2 have often been successfully used at 60 to 62 HRC. However, in shearing high-strength low-alloy steel, the hardness of a D2 knife must be kept below 58 HRC to prevent breakage.

The shock-resistant S-grade tool steels are used in the hardness range of 50 to 58 HRC. The higher end of this range is applicable to the shearing of steels 6.4 to 12.7 mm ($\frac{1}{4}$ to $\frac{1}{2}$ in.) thick and to nonferrous metals. As shock loading increases with the shearing of harder or thicker metals, knife hardness is decreased toward the low side of the above hardness range.

Rake is the slope of the angle formed by the cutting edges of the upper and lower knives (Fig. 2). It is usually expressed as the ratio of the amount of rise to a given linear measurement. For example, a rake of 21 mm/m ($\frac{1}{4}$ in./ft) means that the upper knife rises 21 mm for each meter ($\frac{1}{4}$ in. for each foot) of linear distance along the knives. Rakes below 21 mm/m ($\frac{1}{4}$ in./ft) are rarely used; a rake of 42 mm/m ($\frac{1}{2}$ in./ft) or higher is typical of many plate shears.

Rake is used to permit progressive shearing of the work metal along the length of the knife. This reduces the amount of force required and allows the use of a smaller machine than would be necessary if the cutting edges of the knives were parallel.

It is not possible, however, to calculate the required force for different work metal thicknesses based solely on change of rake, because knife penetration varies for different thicknesses. Even for low-carbon steel, the amount of knife penetration before fracture occurs can be as great as 60% of the work metal thickness for 3.43 mm (0.135 in.) thick stock and as little as 30% for 19 mm ($\frac{1}{4}$ in.) thick stock.

The primary disadvantage of using a high rake angle is that it increases the distortion of the work. Large rake angles can also cause slippage and therefore require high hold-down forces.

Clearance. Excessive knife clearance causes the work metal to be wiped down between the knives during cutting and results in heavy burring or flanging of the work metal. The burrs and deformed metal are objectionable because of their interference with subsequent processing. A more serious consequence of excessive clearance is that it can cause the workpiece to be pulled between the knives, and this in turn causes overloading of the machine and may result in failure of machine components or shear knives.

When soft metals are sheared, insufficient clearance causes double (secondary) shearing, which appears as a burnished area at the top and bottom of a sheared edge with a rough area between the burnished edges. Because of the difference in action, greater tolerances are usually required when shearing plate than when shearing sheet--all other conditions being equal. Some mechanical shears are constructed to operate with a fixed clearance, and no adjustments are made for variations in work metal composition or thickness. The knife clearance is set for the thinnest material to be sheared, and double shear is avoided if the range of thicknesses sheared is not too large.

Because of deflection, clearance at the center of the knife is usually set less than that at the ends. Knife clearance (except on machines using a fixed clearance) is generally increased as work metal thickness increases. For example, in one plant, a clearance of approximately 0.076 mm (0.003 in.) is used for squaring shears with a capacity of up to 6.4 mm ($\frac{1}{4}$ in.) thick low-carbon steel. To minimize knife deflection, clearance at the center of the knife is reduced to 0.051 mm (0.002 in.). Similarly, the clearance for shearing 6.4 to 25 mm ($\frac{1}{4}$ to 1 in.) thick low-carbon steel is 0.36 mm (0.014 in.) at each end of the knives and 0.30 mm (0.012 in.) at the center of 3 to 3.7 m (10 to 12 ft) long knives used in a mechanical shear.

Ram Speed in Straight-Knife Shearing

The speed of the ram (and, in turn, that of the knife) has a slight effect on results in the shearing of flat stock. Low linear speed produces a rough sheared surface. As speed is increased, a cleaner sheared surface is obtained. In general, speeds to 21 to 24 m/min (70 to 80 ft/min) can be used without difficulty when shearing annealed metals. Regardless of the speed used, adequate hold-down force is mandatory.

Accuracy in Straight-Knife Shearing

The dimensional accuracy obtained in shearing is influenced by the capacity and condition of the machine, condition of the knives, knife clearance, work metal thickness, and work metal condition. Expressed as total tolerance, sheets no thicker than approximately 3.43 mm (0.135 in.) can be cut to size within 0.25 mm (0.010 in.), and strips can be sheared to width at the same total tolerance. These tolerances apply to stock up to 3.7 m (12 ft) long that is essentially free from stress and is flat within commercial limits. Sheets that are not flat or that have residual stress, or both, cannot be sheared with the same accuracy.

Greater tolerances are required in shearing plate. A total tolerance of 0.5 to 1.0 mm (0.020 to 0.040 in.) can be maintained when plate is sheared in squaring shears. Dimensions can be held to tolerance of about ±1.6 mm (±$\frac{1}{16}$ in.) when shearing in alligator shears.

Shearing of Plate and Flat Sheet

Revised by Robert A. Westerkamp, Cincinnati Inc.

Rotary Shearing

Rotary shearing, or circle shearing (not to be confused with slitting), is a process for cutting sheet and plate in a straight line or in contours by means of two revolving, tapered circular cutters. Table 3 lists recommended cutter materials.

Table 3 Recommended knife materials for the rotary shearing of flat metals

Metal to be sheared	Thickness to be sheared		
	4.8 mm ($\frac{3}{16}$ in.) or less	**4.8-6.4 mm ($\frac{3}{16}$ - $\frac{1}{4}$ in.)**	6.4 mm ($\frac{1}{4}$ in.)
Carbon, alloy, and stainless steels	D2[(a)]	A2[(b)]	**S4; S5**
Silicon electrical steels	M2[(c)]; D2[(d)]	D2	. . .
Copper and aluminum alloys	A2; D2	A2; D2	**A2[(e)]**

Titanium and titanium alloys	**D2(f); A2(g)**	. . .	. . .

(a) L6 is also recommended for shearing carbon and alloy sheet containing >0.35% C.

(b) D2 is also recommended for low-carbon and low-alloy sheet.

(c) For sheet >0.8 mm ($\frac{1}{32}$ in.) thick.

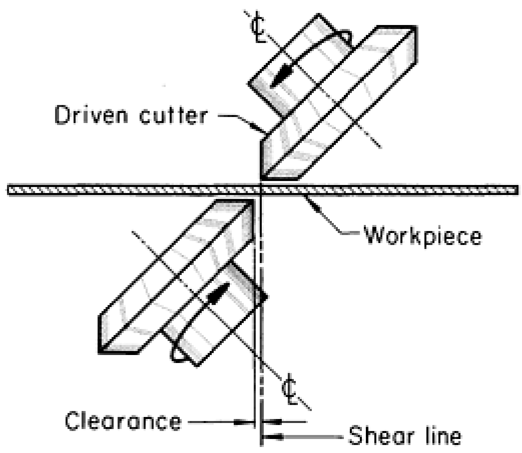
(d) For sheet >0.8 mm ($\frac{1}{32}$ in.) thick.

(e) S5 is recommended for sheet >12.7 mm ($\frac{1}{2}$ in.) thick.

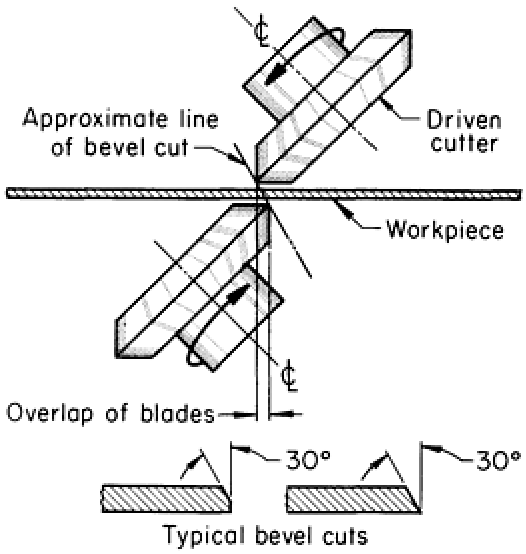
(f) For sheet <3.2 mm ($\frac{1}{8}$ in.) thick.

(g) For sheet >3.2 mm ($\frac{1}{8}$ in.) thick

For conventional cutting to produce a perpendicular edge, the cutters approach each other and line up vertically at one point (Fig. 6a). The point of cutting is also a pivot point for the workpiece; because of the round shape of the knives, they offer no obstruction to movement of the workpiece to the right or left. This feature permits the cutting of circles and irregular shapes that have small radii, as well as cutting in straight lines.



(a)



(b)

Fig. 6 Two types of rotary shearing. (a) Conventional arrangement of cutters for producing a perpendicular edge. (b) Overlap of cutters for producing a beveled edge.

Overlapping of the cutters to the position shown in Fig. 6(b) permits the shearing of smooth beveled edges in straight lines or circular shapes. With the cutters positioned as shown in Fig. 6(b), a bevel can be cut across the entire thickness of the workpiece, resulting in a sharp edge on the bottom of the workpiece, or (by varying the overlap of the cutters) only a corner of the workpiece can be sheared off, leaving a vertical edge (or land) for approximately half the workpiece thickness.

The shearing of workpieces into circular blanks requires the use of a holding fixture that permits rotation of the workpiece to generate the desired circle. For straight-line cutting in a rotary shear, a straight-edge fixture is used, mounted in the throat of the machine behind the cutter heads.

Applicability. Any metal composition or hardness that can be sheared with straight knives can be sheared with rotary cutters. In general, rotary shearing in commercially available machines is limited to work metal 25 mm (1 in.) thick or less. There is no minimum thickness. For example, wire cloth made from 0.025 mm (0.001 in.) diam wire can be successfully sheared by the rotary method.

Circles up to 3 m (10 ft) in diameter or larger can be produced by using special clamping equipment. Minimum diameters depend on the thickness of the work metal and the size of the rotary cutters. With material up to 3.2 mm ($\frac{1}{8}$ in.) thick, the minimum circle that can normally be cut is 152 mm (6 in.) in diameter. For 6.4 mm ($\frac{1}{4}$ in.) thick stock, the minimum diameter is 230 mm (9 in.), and for 25 mm (1 in.) thick stock, the minimum diameter is 610 m (24 in.).

Rotary shearing is limited to cutting one workpiece at a time. As in straight-knife shearing, multiple layers cannot be sheared, because each layer prevents the necessary breakthrough of the preceding workpiece.

Rotary shearing, plasma cutting, laser cutting, gas cutting, and electric-arc cutting are competitive for some operations. Each can produce straight or beveled edges of comparable accuracy. The selection of one of the four processes depends largely on the thickness of the work metal. In general, rotary shearing and laser cutting are used for sheet and plate less than 12.7 mm ($\frac{1}{2}$ in.) thick, and gas cutting is used for thicknesses of 12.7 mm ($\frac{1}{2}$ in.) or more (see the articles "Laser Cutting" and "Thermal Cutting" in this Volume).

Gas cutting is less suitable for cutting a single thickness of sheet or thin plate because the heat causes excessive distortion, but it is often feasible to minimize this problem by stack cutting (cutting several thicknesses at a time). Gas cutting is more versatile than rotary shearing; it can produce smaller circles than rotary shearing and can produce rings in one operation. Gas cutting, however, produces a relatively large heat-affected zone on the workpiece.

Laser cutting produces an extremely narrow kerf that provides unmatched precision for cutting small holes, narrow slots, and closely spaced patterns. Complex openings, contours, and patterns, which are impossible to cut with conventional tools, are routinely cut using a laser and require little if any additional processing. A smaller heat-affected zone than traditional thermal cutting processes minimizes distortion and improves part quality.

Circle Generation. For cutting circles, the workpiece is placed in a special fixture consisting of a C-shaped, deep-throated frame having a rotating pin or clamp point at its outer extremity. The maximum circle that can be sheared is governed by the depth of the throat of the clamp and by the amount of clearance necessary to permit the rotating workpiece to clear the deep part of the C-frame on the machine. Therefore, when using square blanks, removal of the corners allows a larger circle to be cut.

There are two methods of holding the center point of the work metal during circular shearing. In one method, the work metal is clamped by a screw-type handwheel or by an air cylinder, each of which incorporates two pivoting pressure disks--one above and one below the workpiece. The disks permit the workpiece to rotate in a horizontal plane. The other method is by center pinning. In this method, a hole is drilled or punched in the work metal for locating and rotating it on a pin in the center clamping attachment. The hole is at the predetermined center of the circle to be produced.

Of the two methods, center pinning provides greater rigidity because the work metal cannot slip off center during shearing. The circle generated when the work metal is held by the clamping method may not be perfectly true if the clamping fixture has not been properly located or if it has shifted because of pressure on the cutters. The disadvantage of center pinning is that a hole must be made in the work and must be closed by plug welding if it is not wanted in the finished product.

Adjustment of Rotary Cutters. The upper cutter head and drive of a rotary shear is raised and lowered by power. A clutch mechanism limits upward and downward travel. Power movement of the upper cutter is essential (especially when cutting plate stock) because the shearing edges of the cutters must be moved toward each other in proper alignment to create the initial shearing action. In setting up, the work metal is often rotated in the clamp attachment, with the cutter exerting light pressure to determine whether a true circle is being generated. Additional pressure is then applied by the vertical screwdown of the upper cutter to cause shearing.

Only the upper cutter is rotated by the power-drive system. The pinching and rotating action of the upper cutter causes the work metal to rotate between the cutters, and the work metal causes the bottom cutter to rotate.

The position of the upper cutter in relation to the lower cutter is important. Figure 6(a) shows the setting for shearing a straight edge. Clearance between the cutters is just as important as it is with straight knives.

Overlap of the cutters, as shown in Fig. 6(b), produces a bevel cut. The degree of bevel up to a maximum of 30° can be adjusted by changing the amount of overlap of the cutters.

Accuracy of the sheared circle depends on the rigidity of the center clamping device, sharpness of the cutters, maintenance of optimal clearance between the cutters, thickness of the work metal, and cutting speed. For work metal up to about 3.2 mm ($\frac{1}{8}$ in.) thick, dimensional accuracy within ±0.8 mm ($\pm\frac{1}{32}$ in.) can be obtained when generating a 762 mm (30 in.) diam circle. With proper setup of equipment, the sheared edge will show only a slight indication of the initial penetration.

Speeds of 2.4 to 6.7 m/min (8 to 22 ft/min) are most commonly used for the rotary shearing of metal up to 6.4 mm ($\frac{1}{4}$ in.) thick. Speeds of 1.5 to 3 m/min (5 to 10 ft/min) are used for rotary shearing metal that is 6.4 to 25 mm ($\frac{1}{4}$ to 1 in.) thick.

Flanging and Joggling. With cutters replaced by forming tools, the rotary shear can be used to form flanges and joggles on flat stock. The maximum joggle that can be produced is usually limited to the thickness of the work metal. Because the work metal is made to flow into a different shape during flanging or joggling, the amount of energy required reduces the capacity of the machine to 75% of the rated capacity for shearing. Figure 7 shows a typical setup for forming a joggle.

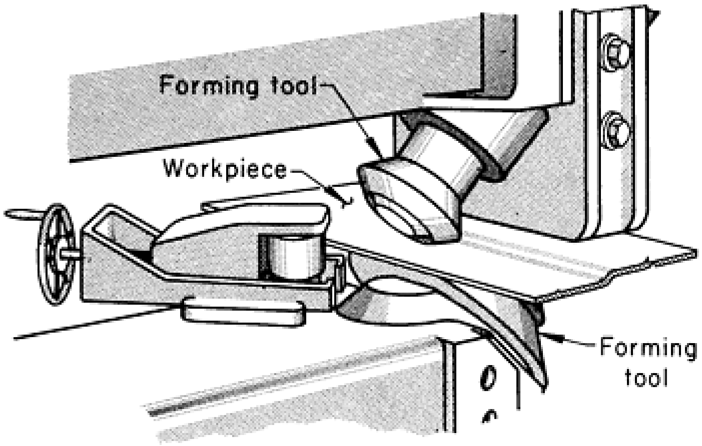


Fig. 7 Setup and tooling for forming a joggle in plate stock using a rotary shear.

Shearing of Plate and Flat Sheet

Revised by Robert A. Westerkamp, Cincinnati Inc.

Safety

Shearing machines must be equipped with devices for protecting personnel from the hazards of shear knives, flywheels, gears, and other moving parts. The guards and safety devices used must be rigid enough to withstand damage from operating personnel moving heavy material into position.

The squaring shears used for sheet metal should have guards on all moving parts, including flywheels, hold-downs, and knives. The treadle, whether mechanical or electrical, should have a lock for supervisory control. Knife and hold-down guard openings should be large enough to provide visibility but small enough to keep the operator's fingers out of the danger area. Proper opening dimensions are outlined in ANSI standard B11.4-1983.

The shears used for shearing plate are more difficult to safeguard because of the greater clearances needed under the hold-downs and upper knife to permit entry of the plate (especially when it is bowed or buckled). Guards on shears for plate should be of the type that raises only when the plate is inserted and then rests on the surface of the plate. When there is no workpiece in the machine, the guards rest within 6.4 mm ($\frac{1}{4}$ in.) of the surface of the table.

Shears should comply with the construction requirements of the Occupational Safety & Health Act and National Safety Standards such as ANSI B11.4-1983. Additional safety information can be obtained from the loss prevention group of major insurance carriers for Workman's Compensation and the National Safety Council. Safety regulations also cover the maximum noise level permitted from a shearing operation to prevent permanent impairment of hearing.

Slitting and Shearing of Coiled Sheet and Strip

Revised by Eric Theis, Herr-Voss Corporation

Introduction

COILED SHEET OR STRIP is cut to size for further processing by slitting longitudinally, dividing it into narrower coils, and shearing transversely for cutting into flat pieces of specified length.

Slitting and Shearing of Coiled Sheet and Strip

Revised by Eric Theis, Herr-Voss Corporation

Slitting

Slitting is accomplished by passing the strip between slightly overlapping circular blades mounted on rotating arbors. A slitting line for cutting wide coiled stock into narrower widths consists essentially of an uncoiler for holding the coil, a slitter, and a re-coiler for re-coiling the slit strips (Fig. 1). Other equipment can be added to the line for automatic feed-up and handling, steering, shape correction, slit-coil tensioning, and packaging.

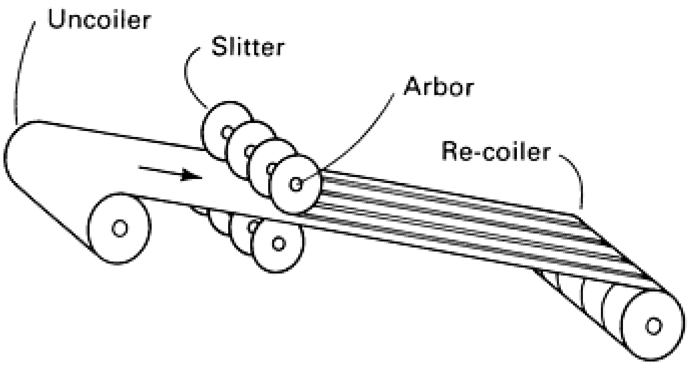


Fig. 1 Principal components for slitting wide coiled stock into narrower widths

Slitting lines are broadly classified as pull-through or driven types. The choice between pull-through and driven lines depends largely on strip shear strength and thickness, number of slits, and slitting speed. In general, when the metal to be slit is less than 0.25 mm (0.010 in.) thick, a drive or helper-drive slitter is preferred because thin-gage metal is likely to tear.

In pull-through slitting lines (Fig. 2), drive motors on the slitter and uncoiler are used only to feed the coil stock through the slitter up to the re-coiler gripper. After the strips are attached to the re-coiler, the motors for the uncoiler and slitter are disengaged, and the driven re-coiler pulls the strip from the uncoiler through the slitter. Some lines use drag devices on the uncoiler to increase strip tension during slitting.

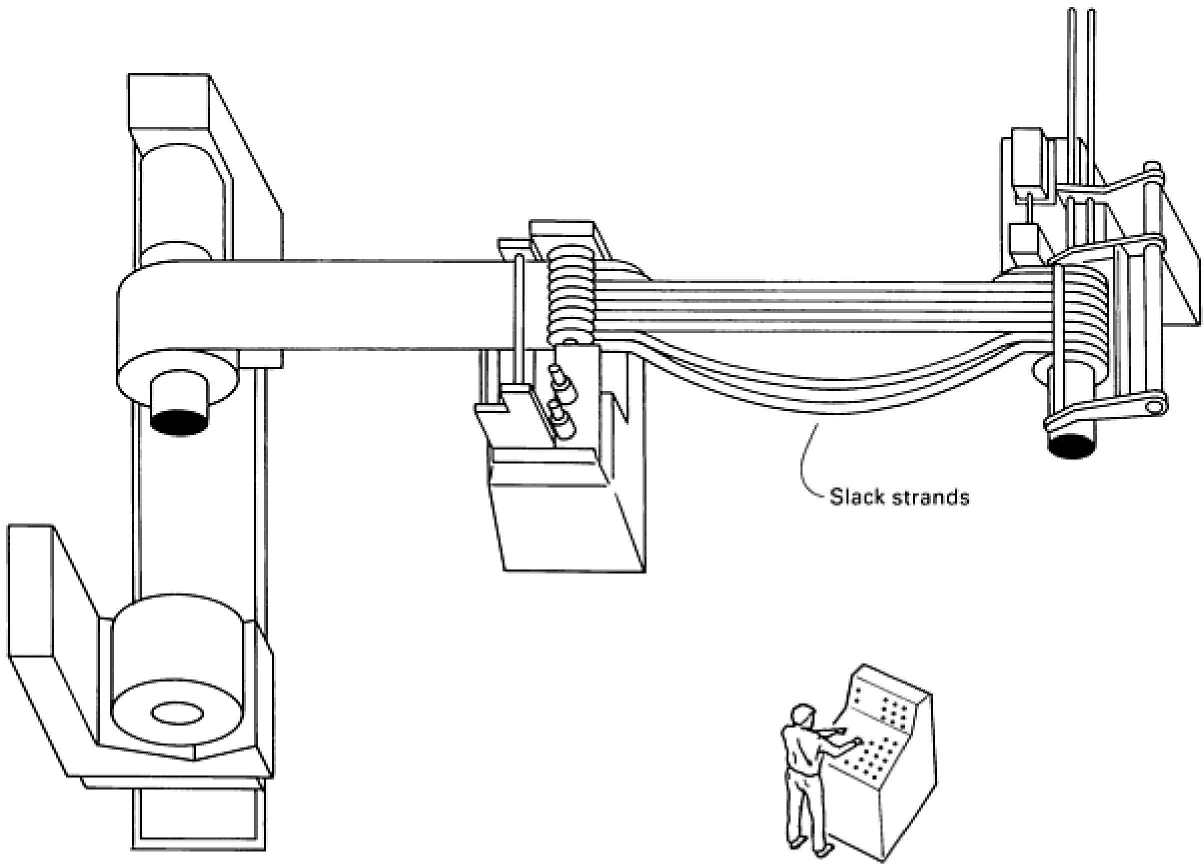


Fig. 2 Pull-through slitting line in which motor-driven re-coiler pulls strip through slitter. Motors for the uncoiler and the slitter are used only to attach uncoiled sheet to the re-coiler through the slitter device and are then disengaged.

A pull-through slitter with a helper drive is also available. In this case, the torque applied to the slitter arbors, from the slitter drive motor, reduces the tension on the pulled strip to avoid snagging at the entry slitter knives. The helper torque is insufficient to drive the slitters alone, thus eliminating the speed-match problems of a pure, driven slitter.

In the driven-type line (Fig. 3), the slitter and the re-coiler are driven by separate motors. These motors are synchronized to maintain approximately constant speed of the metal as it travels through the slitting line. However, a slack loop must be maintained--especially on lighter materials--between the slitter and the re-coiler to allow for minor differences in strip speed.

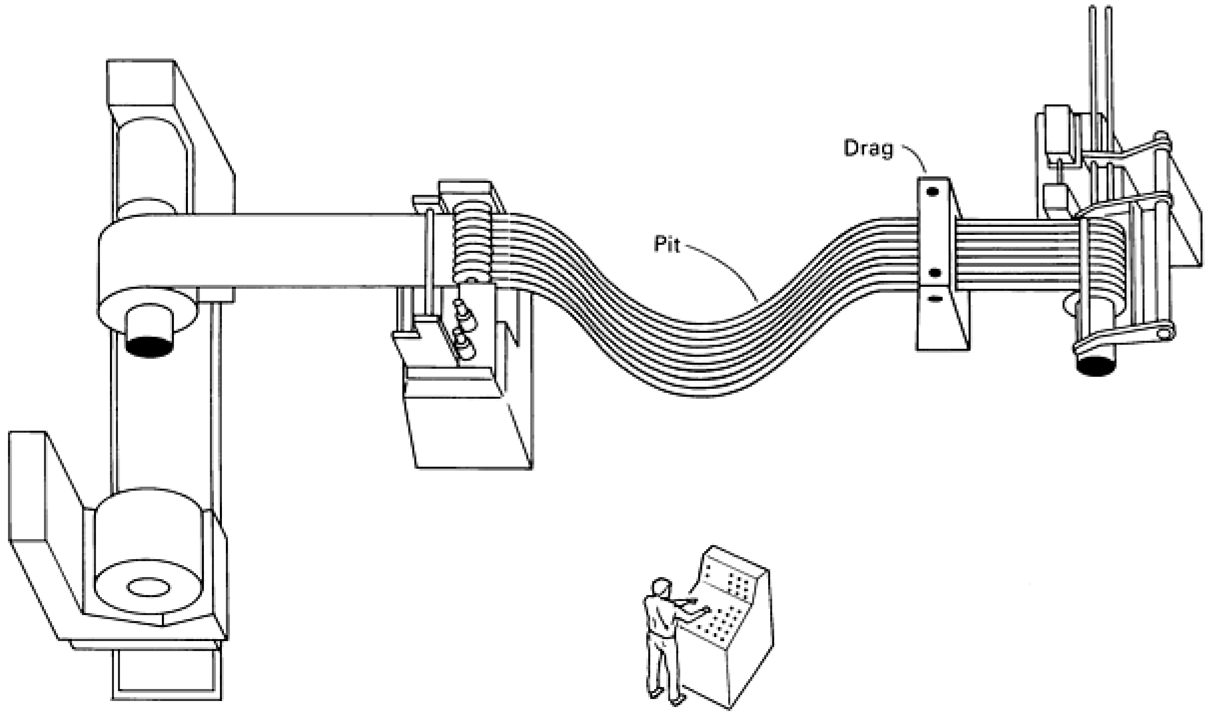


Fig. 3 Driven-type slitting line with uncoiler, slitter, and re-coiler all driven by separate motors synchronized to maintain constant speed of sheet materials. Minor differences in speed still require the use of slack loops in the pit.

Burrs are normally found to some extent on all slit edges. The severity of the slitting burr depends primarily on the sharpness of the slitting knives and the horizontal and vertical clearance between them. These factors are influenced by machine rigidity, machine and knife maintenance, knife and stripper setup, number of cuts and location on the arbor, and thickness and hardness of the material being slit. If burr-free edges are required, subsequent equipment can be installed in the line or downstream to roll down or to remove the burr.

Knives must be maintained in a sharpened and clean condition, free of nicks and metal pickup on the slitting edge. Knife setup is critical. Computer programs and modern tooling are available for establishing the precise location of slitting knives on the machine arbors for various materials and material thicknesses.

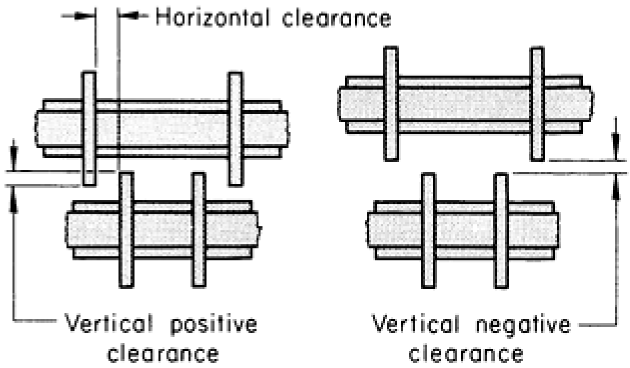
Knife clearances may be very different under slitting loads than during the unloaded condition at time of setup. Vertical deflection of the slitter arbors is a function of arbor diameter and loading, which are both related to the number of cuts, the type of material, and compression of the elastic strippers. Horizontal deflection, in the direction of slit-coil travel, can be caused by insufficient arbor diameter relative to pulling tension in the case of a pull-through machine. Side-to-side knife clearances can be affected by the type and condition of the arbor bearing housing. Simple slitter designs accomplish vertical arbor adjustment with vertical screws adjusting the up/down position of either end of one of the arbors. The clearances required in this type of arbor control arrangement provide the least slitter knife control for precision slitting. Slitter housing designs incorporating bearings mounted in eccentric sleeves, which can be rotated for vertical adjustment, generally provide the best control for precision cutting. Designs incorporating a single, fixed arbor with the other arbor vertically adjustable through eccentrics provide close control of knife arbor position, but change the passline to an off-horizontal orientation at various points in the adjustment process. The most accurate system is one employing eccentric vertical adjustments of both upper and lower arbors (one clockwise and the other counterclockwise) so that the passline always remains in the horizontal orientation.

Minimum burr height can be achieved only through careful control of all of the variables, especially machine design, maintenance, tooling, and setup. Table 1 shows the vertical (positive and negative) and horizontal clearances used in slitting operations at one plant.

Table 1 Vertical and horizontal clearances of slitter blades

Work metal thickness		Clearance	
mm	**in.**	**mm**	in.
Vertical positive			
0.25	0.010	0.08	**0.003**
0.51	0.020	0.18	**0.007**
0.76	0.030	0.25	**0.010**
1.07	0.042	0.36	**0.014**
1.24	0.049	0.43	**0.017**
1.50	0.059	0.56	**0.022**
1.73	0.068	0.51	**0.020**
2.11	0.083	0.46	**0.018**
2.41	0.095	0.38	**0.015**
2.59	0.102	0.33	**0.013**
3.02	0.119	0.18	**0.007**
3.40	0.134	0.13	**0.005**
3.81	0.150	0.00	**0.000**
Vertical negative			
4.27	0.168	0.05	**0.002**
4.52	0.178	0.10	**0.004**
4.75	0.187	0.15	**0.006**

5.08	0.200	0.20	**0.008**
Horizontal			
0.20 or less	0.008 or less	0.000	**0.000**
0.23-0.25	0.009 to 0.010	0.013	**0.0005**
0.28-0.48	0.011 to 0.019	0.025	**0.001**
0.51 or more	0.020 or more	**7 to 8% of the thickness of the work metal**	



Camber (Fig. 4) is present to some extent in almost all coiled metal. Cambered strip has one side longer than the other, therefore, it tends to sweep to the left or the right. The slitting line can also induce camber in the slit strands. Slitters generally have small metal disks, called separators, to control recoiling of slit strands on the re-coil mandrel. Where a number of strands are being slit from a master coil, the insertion of separators at the re-coiler necessitates the fan out of the slit strands after they leave the slitter (Fig. 5). If the slitting equipment is properly aligned, the center strands will be straight, with the outer strands cambered to the outside (left toward left and right toward right) on a pull-through or helper-drive line.

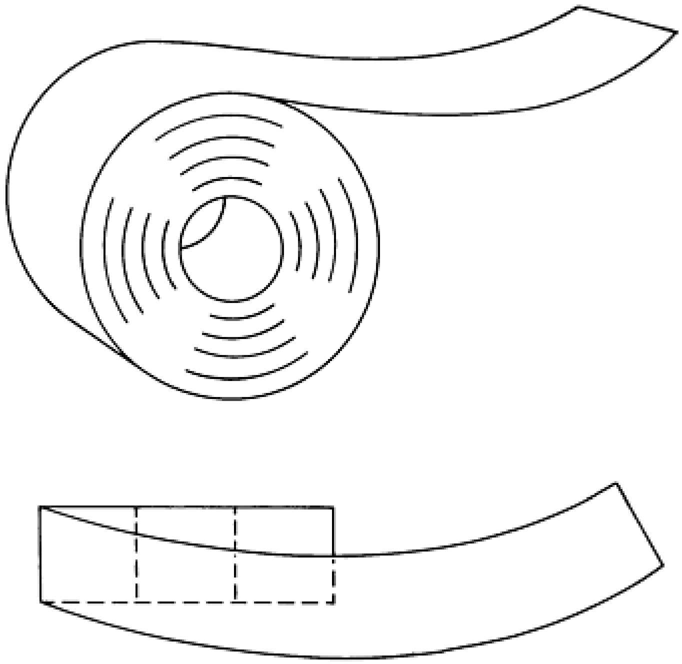


Fig. 4 Illustration of camber in a coil of metal showing one side actually longer than its opposite parallel side.

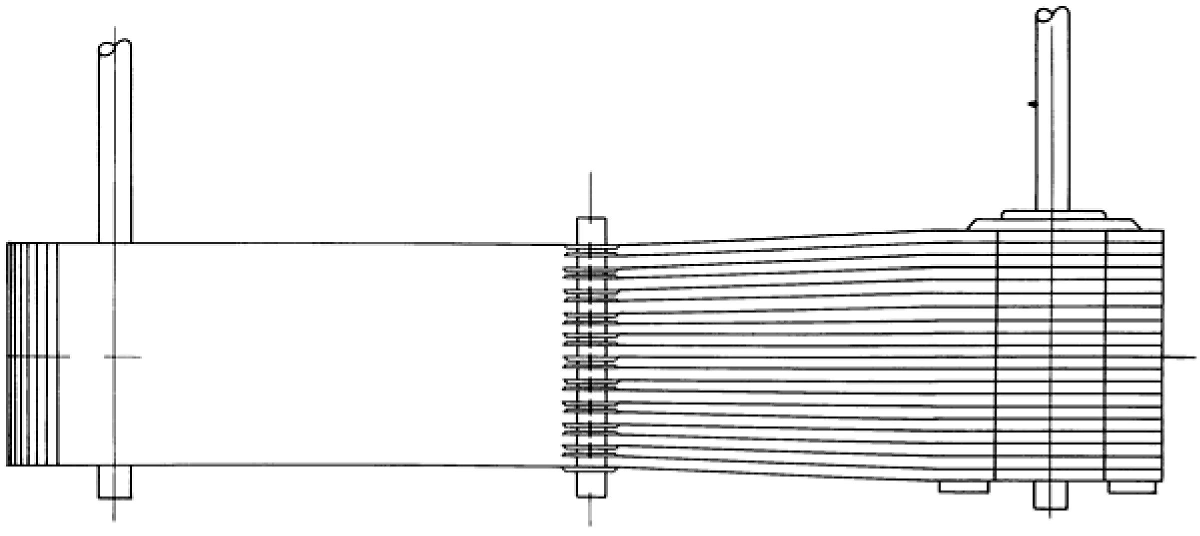


Fig. 5 Slitting-induced camber caused by insertion of separators at re-coiler, necessitating fan out of slit strands

Most slitters follow the camber in the original master coil during the slitting process. Slitter-induced camber, on the other hand, can be controlled by proper line design, maintenance, and tooling setup. The technology is available for controlling or eliminating most of the camber subsequent to slitting and prior to re-coiling.

The camber of the outer slit strands, combined with wavy edges in the master coil, can produce slit coils, which are sometimes referred to as snakes. These are particularly objectionable to stamping shops. Shape correction devices in modern slitting lines can often control or eliminate these problems.

Re-Coiling of Slit Stock. Variation in thickness across the width of coil stock often causes problems upon re-coiling. The center of the master coil is generally thicker than the edges. Thicker center strands will build up on the re-coiler at a faster rate than the thinner strands at the sides, resulting in tighter center coils. The apparent strand length differential on the outside strands can be substantial on lighter-gage materials. If they are too loose, the outer slit coils will sag after removal from the re-coiler. The wraps of loose coils can slip and telescope during subsequent handling. Telescoped and unstable coils are difficult to handle during uncoiling.

Paper stuffing (Fig. 6) is one method of preventing loose coils caused by different rewind speeds. The operator inserts pieces of paper or cardboard into the wrap as the outer slit-coil strands are re-coiled. This procedure is dangerous to personnel, but it does increase the effective diameter of the coil and therefore the tension on the strand. However, the material used may prove objectionable to the end user, because it could cause jamming of feeding devices in subsequent operations.

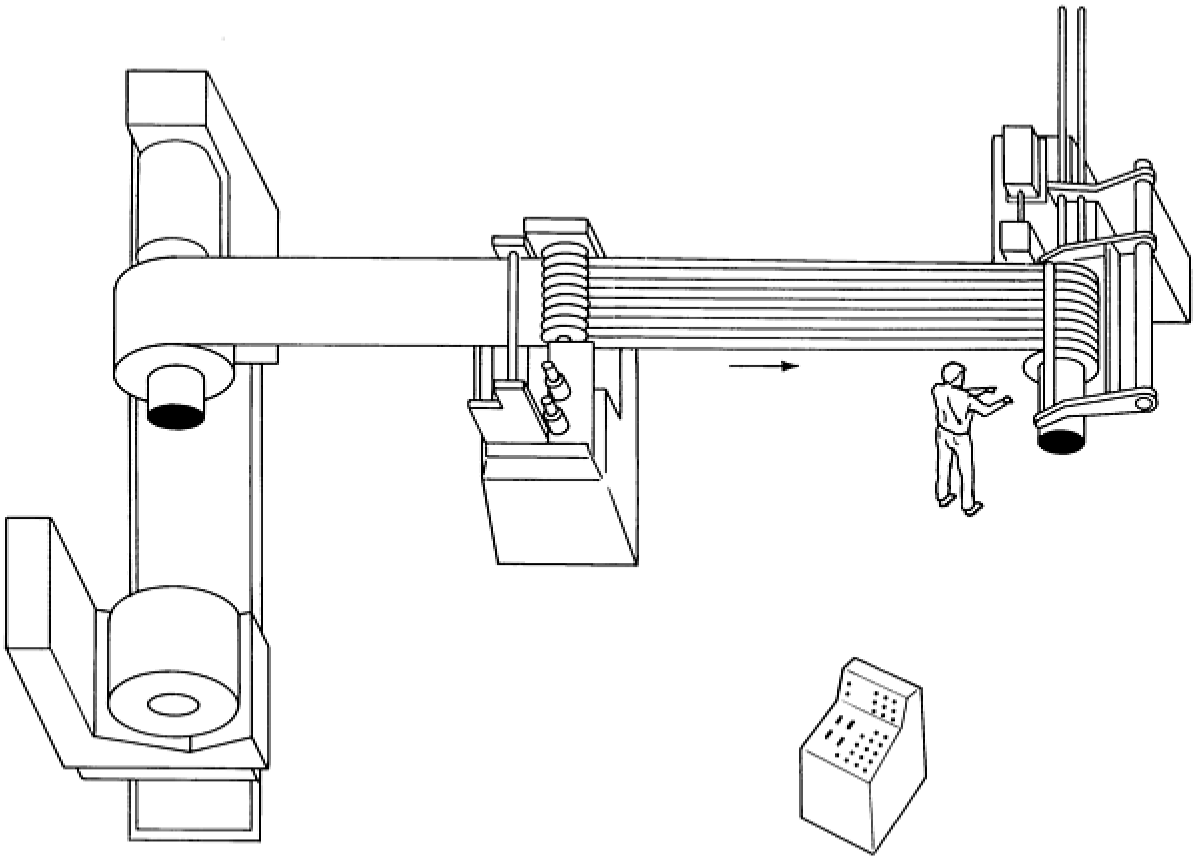


Fig. 6 Operator inserting pieces of paper or cardboard into wrap as outer slit-coil strands are re-coiled in order to increase strand tension. This procedure is known as paper stuffing.

There are two possibilities for producing the tight coils normally found in modern slitting lines. One technique involves a drag-producing friction device, placed just before the re-coiler, with a deep pit to accumulate the excess strand length differential from the outer slit multiples. The other method, a patented process (Fig. 7), involves slight elongation of the tighter strands so that all slit strands rewind at the same rate and tightness, eliminating the apparent strand length differential and the need for friction drags or accumulator pits.

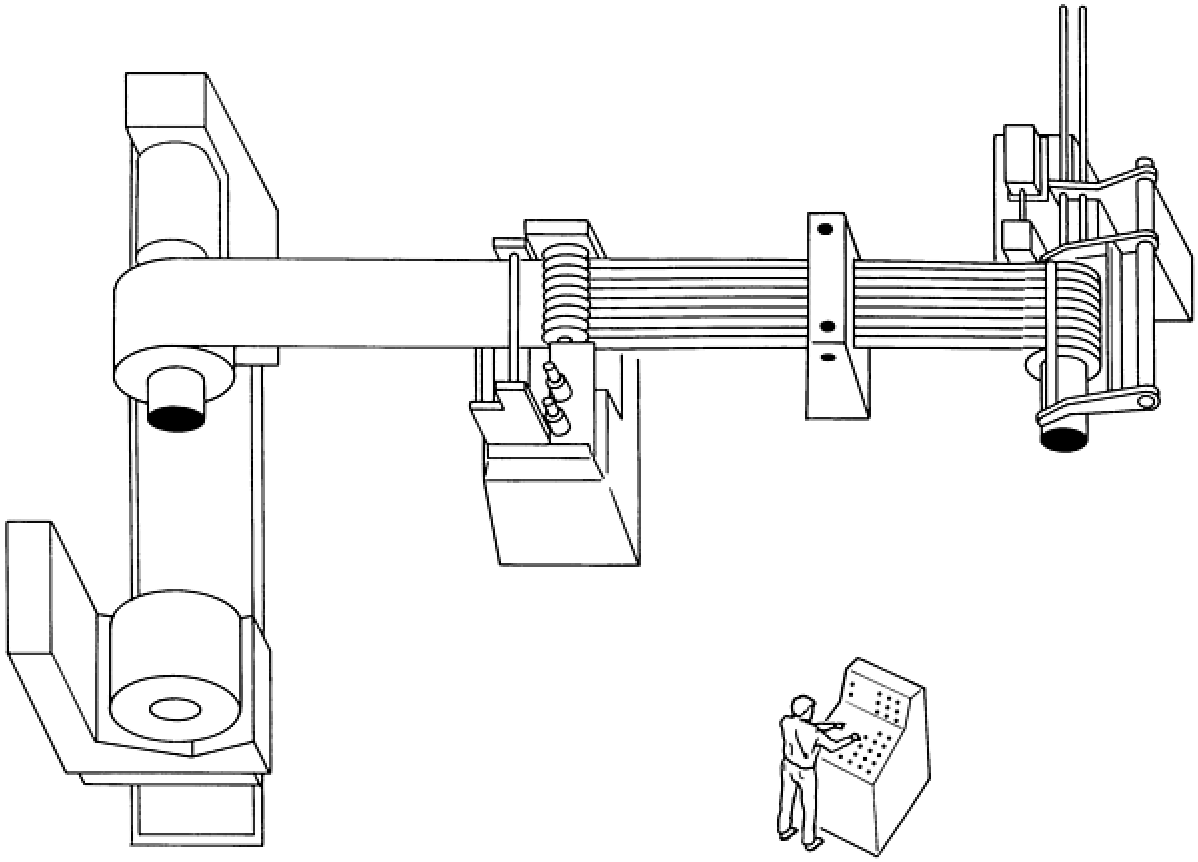


Fig. 7 Mechanism introduced as an additional component between slitter and re-coiler to elongate the tighter strands slightly so that all slit strands rewind at the same rate and tightness

The capacity of slitting lines is usually stated in terms of the number of cuts of a certain thickness of a specified metal. Thicker material can often be slit if fewer cuts are being made.

Effect of Slitting Speed on Productivity. There are many more linear feet of material in a thin-gage coil than in a coil of thicker material. Therefore, for practicality, lightgage lines generally run at higher speeds. On older lines, increasing the slitting speed may have a small effect on overall productivity because the slitting phase of the operation may occupy only a small fraction of the total time. On lines with manual feed-up and tool change, the greatest proportion of the total operating time by far is consumed in handling operations. Table 2 shows a breakdown of the time for slitting three lengths of coils at average speeds of 99 to 198 m/min (325 and 650 ft/min) on one of these simple lines. As the data show, doubling the slitting speed results in only a 3 to 12% reduction in overall cycle time for any length of coil.

Table 2 Effect of speed and coil length on slitting time versus total cycle time

For slitting coiled sheet 914 mm (36 in.) wide and 1.6 mm (0.062 in.) thick, using seven cuts

Operation	Time, min, at average speed of:	
	99 m/min (325 ft/min)	198 m/min (650 ft/min)
Coil length: 210 m (690 ft)[(a)]		
Slitting	2	**1**

Coil handling[b]	28	**28**
Total cycle	**30**	29
Coil length: 610 m (2000 ft)[c]		
Slitting	6	**3**
Coil handling[b]	28	**28**
Total cycle	**34**	31
Coil length: 910 m (3000 ft)[d]		
Slitting	9	**4.5**
Coil handling[b]	28	**28**
Total cycle	37	32.5

(a) 813 mm (32 in.) OD coil; weight: 2380 kg (5240 lb).

(b) Removing bands from coil and loading it on uncoiler, threading, attaching six strands and placing separators on re-coiler drum, placing one band on each coil, and stripping it off the re-coiler.

(c) 1220 mm (48 in.) OD coil; weight: 6800 kg (15,000 lb).

(d) 1520 mm (60 in.) OD coil; weight: 10,200 kg (22,500 lb)

Modern slitting lines can provide fully automatic slit-coil feed-up in 3 to 6 min. Lines employing automatic cartridge-type slitter-head exchange can exchange tooling stands in 1 min or less. These factors represent a substantial reduction in downtime between coils. In such a case, increases in slitting speed may have a more profound effect on overall productivity (measured in tons per hour).

Slitting and Shearing of Coiled Sheet and Strip

Revised by Eric Theis, Herr-Voss Corporation

Cut-to-Length Lines

Cut-to-length lines (also called blanking lines or shearing lines) are used to produce cut-to-length sheets from coil stock. These machines uncoil the strip, flatten or level it (see section "Flatteners and Levelers for Strip" in this article), cut it to length, and then stack the sheets. There are two basic types of shear employed: stationary and flying die or rocker type.

Flying-die or rocker shear lines continuously feed the coil, with the cutoff blades arranged to travel in the same direction and speed as the strip during the cutoff process. Because of the difficulties involved in synchronizing this speed match with accurate length positioning, these lines are generally used where length tolerances are less critical.

Stationary Shears. Most cut-to-length lines employ stationary shears. The strip is stopped at the shear during the cut. These lines are generally more accurate than other types. There are three types of feeding arrangements for fixed shear lines: start/stop, hump table, and looping pit.

Stop/start cut-to-length lines (Fig. 8) are usually arranged so that the coil is fed into the shear to a prescribed length and then stopped during the cut. Upon completion of the cut, the coil feed accelerates until it stops for the next cut. Close tolerances can be obtained, and line configuration is simple; however, the speed in meters per minute or sheets per minute is slow. Therefore, these lines are generally confined to heavy-gage requirements. Stopping the coiled sheet in the flattener or leveler prior to shearing may also leave a mark on lighter-gage materials.

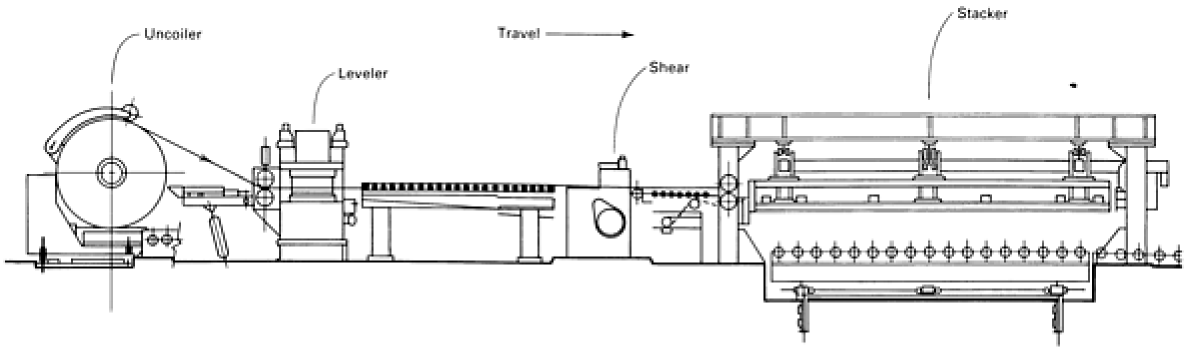


Fig. 8 Stop/start cut-to-length line in which coil travel is stopped to allow material to be cut to a prescribed length in the shear and then restarted until material is again at the prescribed length, when the stop/start cycle is repeated

With the hump or loop-feed configuration, the uncoiler and flattening equipment operates continuously. The strip is stopped at the shear, and this causes excess material to rise above the passline in a hump or fall below the passline into a looping pit just before the shear.

Hump Table. Stationary shear lines with hump tables (Fig. 9) consist of an uncoiler, a flattener, and/or a leveler to correct for strip shape and to feed the strip over a hump table, a stationary shear, a gage table with retractable stop, and a stacker that stacks the cut sheets as they are delivered from the gage table.

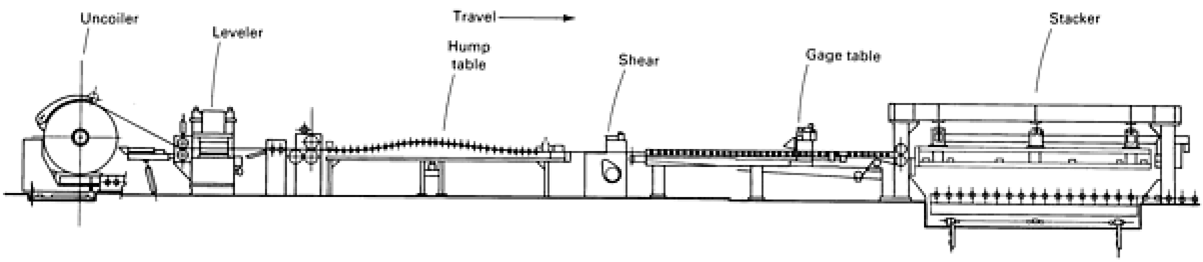


Fig. 9 Stationary shear lines with hump table operate with strip continuously moving from uncoiler even during shearing sequence, causing the coil sheet to form a loop above the passline and over the hump table. A limit switch actuates shear when the uncoiled strip touches a retractable stop.

The retractable stop with gage table, which follows the shear sequence, is used to control the length of the cut sheets. When the uncoiled strip touches the gage stop, it trips a limit switch that actuates the shear. Because the strip continues to flow from the uncoiler, it causes a loop to form above the hump table in front of the shear. When shearing is completed, the gage stop retracts, and the cut sheet is delivered to the stacker. As the cut sheet is removed, it trips a limit switch that resets the gage stop. Then, the shear opens, permitting the strip to slide out of its loop through the shear and onto the gage table against the gage stop again, and the cycle is ready to be repeated.

Looping Pit. As shown in Fig. 10, many stationary shear lines have precision measuring feeder rolls just before the shear, instead of hump and gage tables. In these lines, there is a looping pit below the passline, after the straightener/leveler and before the feed rolls and shear. The flattener and/or leveler runs continuously, with coil stock accumulating in the pit during the cut cycle. Side guides control the feed angle of the strip for maximum cut squareness as it exits the looping pit and enters the shear. On some lines, the shear knives can be pivoted to cut trapezoidal blanks.

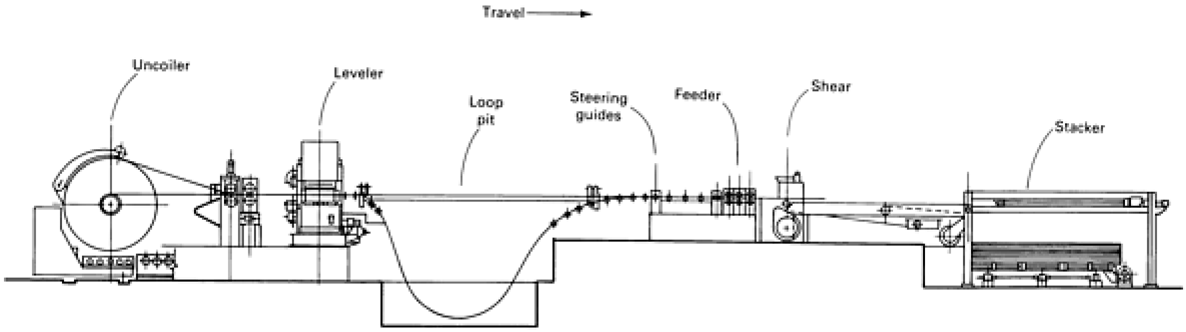


Fig. 10 Stationary shear lines with looping pit located below passline and between straightener/leveler and feed rolls and shear

A stationary shear line generally provides the best squareness and length tolerance and most productivity for lighter-gage materials. Because of the nature of the loop, this feeding method is not practical for strip over about 6.4 mm ($\frac{1}{4}$ in.) thick. A hump line may be somewhat less expensive when the costs of installation are included, but tolerance, squareness, and productivity may be sacrificed.

Rotary drum shears are sometimes used in high-production sheet mills at speeds to 300 m/min (1000 ft/min). The tinplate flying shear, a special type of rotary drum shear, is made specifically for cutting coils of tinplate into lengths for use in the manufacture of cans.

Blanking lines are a special derivation of shearing lines. Most shearing lines, of any of the above configurations, are designed for cutting sheets ranging in length from 0.6 m (2 ft) to 8 to 9 m (25 to 30 ft). Because these sheets are to be fabricated later into an end product, length tolerance requirements may not be critical. Blanking lines have the special capability of rapidly cutting and stacking relatively short and dimensionally accurate blanks.

Edge-trim slitters are incorporated into some line configurations. However, these follow the camber or sweep found in almost all master coils obtained from the mill. These slitters cannot be counted on to eliminate or even control camber or to provide improved sequences.

Cut-to-length line capacity may be limited by the shear. Most shears include a raked blade. As a result, the shear, and therefore the cut-to-length line, does not have a short/thick capacity. If, for example, the line capacity were 6.4 × 1830 mm ($\frac{1}{4}$ × 72 in.), the maximum thickness for 305 mm (12 in.) wide material would still be only 6.4 mm ($\frac{1}{4}$ in.).

Dimensional accuracy of the cut sheet length depends on the line configuration, condition of the equipment, speed and length of sheet, and the condition of the master coil. In previous generations of equipment, an accuracy of ±1.6 mm (± $\frac{1}{16}$ in.) was acceptable. Most modern lines are accurate to ±0.8 mm (± $\frac{1}{32}$ in.), except on very long sheets. Sophisticated equipment is also available that can produce sheets or blanks with tolerances of ±0.4 mm (± $\frac{1}{64}$ in.).

Revised by Eric Theis, Herr-Voss Corporation

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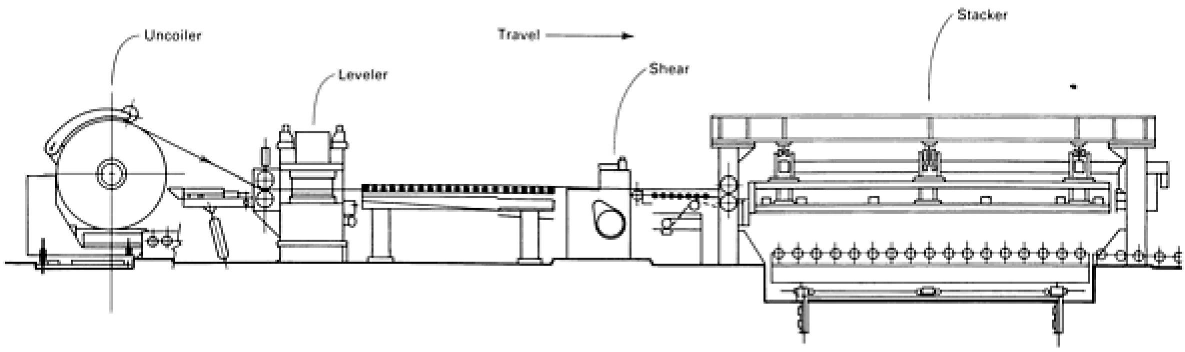

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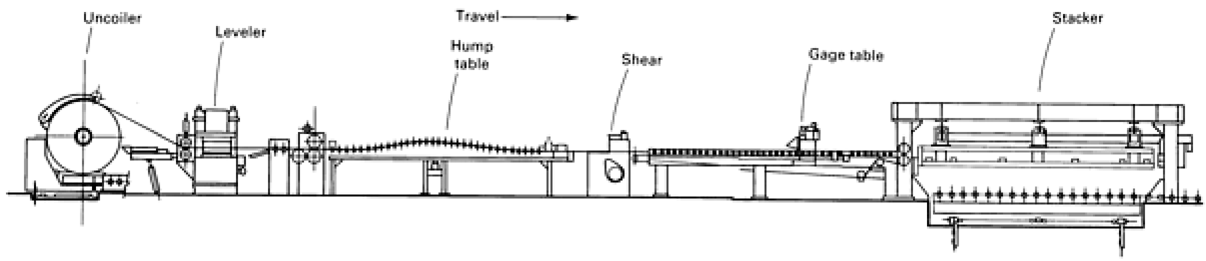

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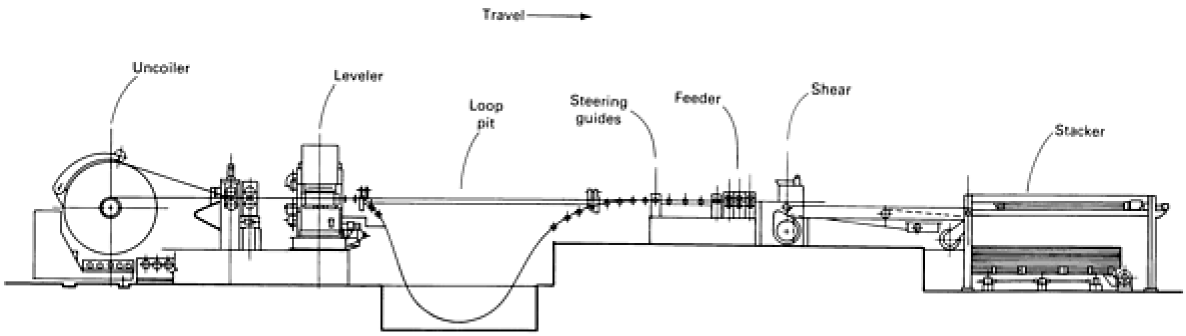

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A stationary shear line generally provides the best squareness and length tolerance and most productivity for lighter-gage materials. Because of the nature of the loop, this feeding method is not practical for strip over about 6.4 mm ($\frac{1}{4}$ in.) thick. A hump line may be somewhat less expensive when the costs of installation are included, but tolerance, squareness, and productivity may be sacrificed.

Rotary drum shears are sometimes used in high-production sheet mills at speeds to 300 m/min (1000 ft/min). The tinplate flying shear, a special type of rotary drum shear, is made specifically for cutting coils of tinplate into lengths for use in the manufacture of cans.

Blanking lines are a special derivation of shearing lines. Most shearing lines, of any of the above configurations, are designed for cutting sheets ranging in length from 0.6 m (2 ft) to 8 to 9 m (25 to 30 ft). Because these sheets are to be fabricated later into an end product, length tolerance requirements may not be critical. Blanking lines have the special capability of rapidly cutting and stacking relatively short and dimensionally accurate blanks.

Edge-trim slitters are incorporated into some line configurations. However, these follow the camber or sweep found in almost all master coils obtained from the mill. These slitters cannot be counted on to eliminate or even control camber or to provide improved sequences.

Cut-to-length line capacity may be limited by the shear. Most shears include a raked blade. As a result, the shear, and therefore the cut-to-length line, does not have a short/thick capacity. If, for example, the line capacity were 6.4 × 1830 mm ($\frac{1}{4}$ × 72 in.), the maximum thickness for 305 mm (12 in.) wide material would still be only 6.4 mm ($\frac{1}{4}$ in.).

Dimensional accuracy of the cut sheet length depends on the line configuration, condition of the equipment, speed and length of sheet, and the condition of the master coil. In previous generations of equipment, an accuracy of ±1.6 mm (± $\frac{1}{16}$ in.) was acceptable. Most modern lines are accurate to ±0.8 mm (± $\frac{1}{32}$ in.), except on very long sheets. Sophisticated equipment is also available that can produce sheets or blanks with tolerances of ±0.4 mm (± $\frac{1}{64}$ in.).

Slitting and Shearing of Coiled Sheet and Strip

Revised by Eric Theis, Herr-Voss Corporation

Flatteners and Levelers for Strip

Coiled sheet or strip often contains three distinct shape defects (Fig. 11):

- When uncoiled, the strip retains some of the residual longitudinal curvature from its original coiled condition or a related across-the-strip curvature
- A longitudinal length differential within the strip causing wavy edges or center buckle
- Variations in thickness of the strip

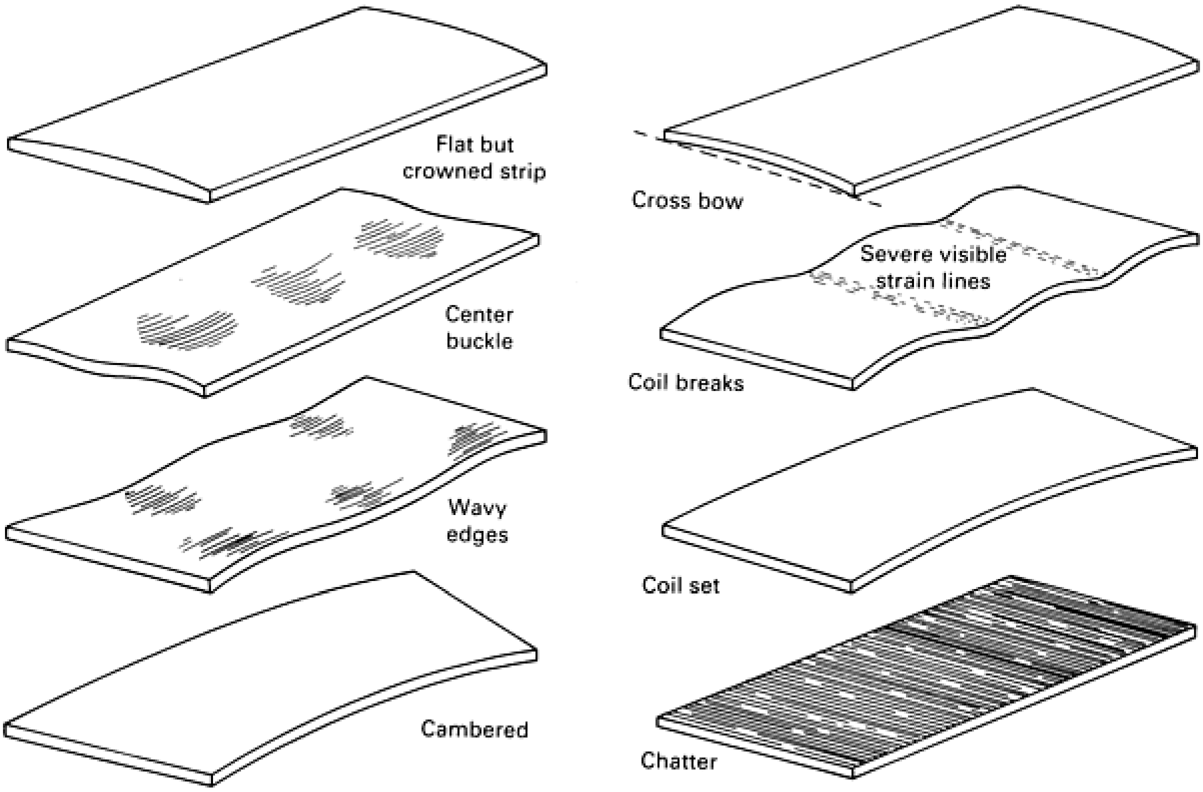

Fig. 11 Defects inherent in the manufacture of coiled sheet or strip

Properly designed and operated flatteners, such as the one illustrated in Fig. 12, can control or eliminate residual longitudinal curvature conditions, which are termed coil set and cross bow. Roller revelers (Fig. 13), properly designed and operated, can control or eliminate both residual longitudinal curvature and longitudinal length differential. Neither flatteners nor roller levelers, however, can significantly affect thickness variation.

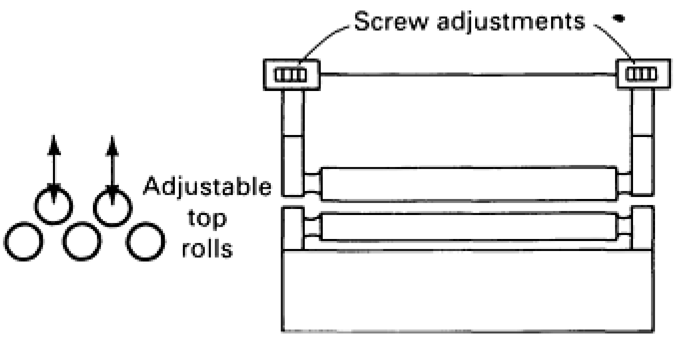

Fig. 12 Five-roll flattener used to eliminate such defects as coil set and cross bow in coiled sheet or strip

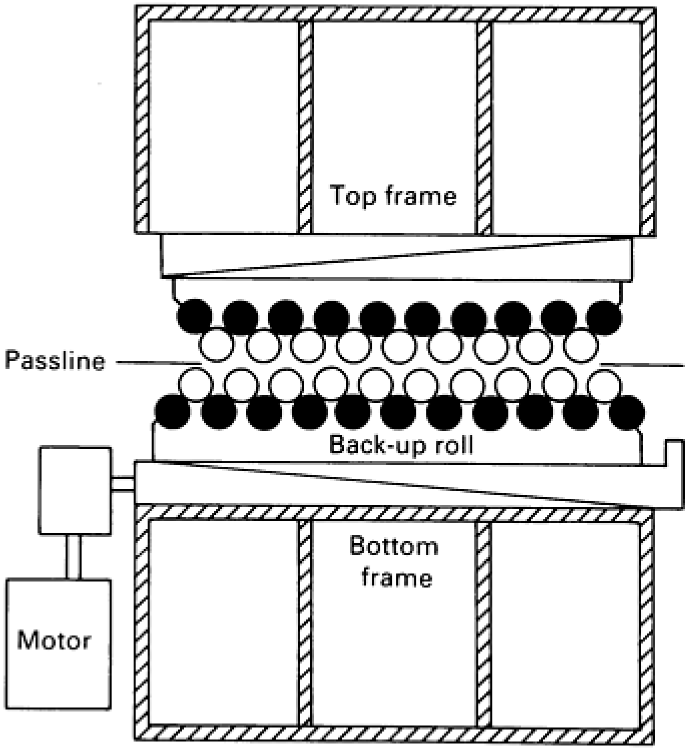

Fig. 13 Corrective leveler with adjustable backup rollers used to eliminate coil set, crossbow, edge-wave, and center or quarter buckles conditions in coiled sheet or strip

In each case, strip is passed between sets of upper and lower offset rolls, alternately bending the strip up and down. Levelers have the ability to control the deflection of these rolls so that one portion of the strip can be subjected to more deformation than another (Fig. 14).

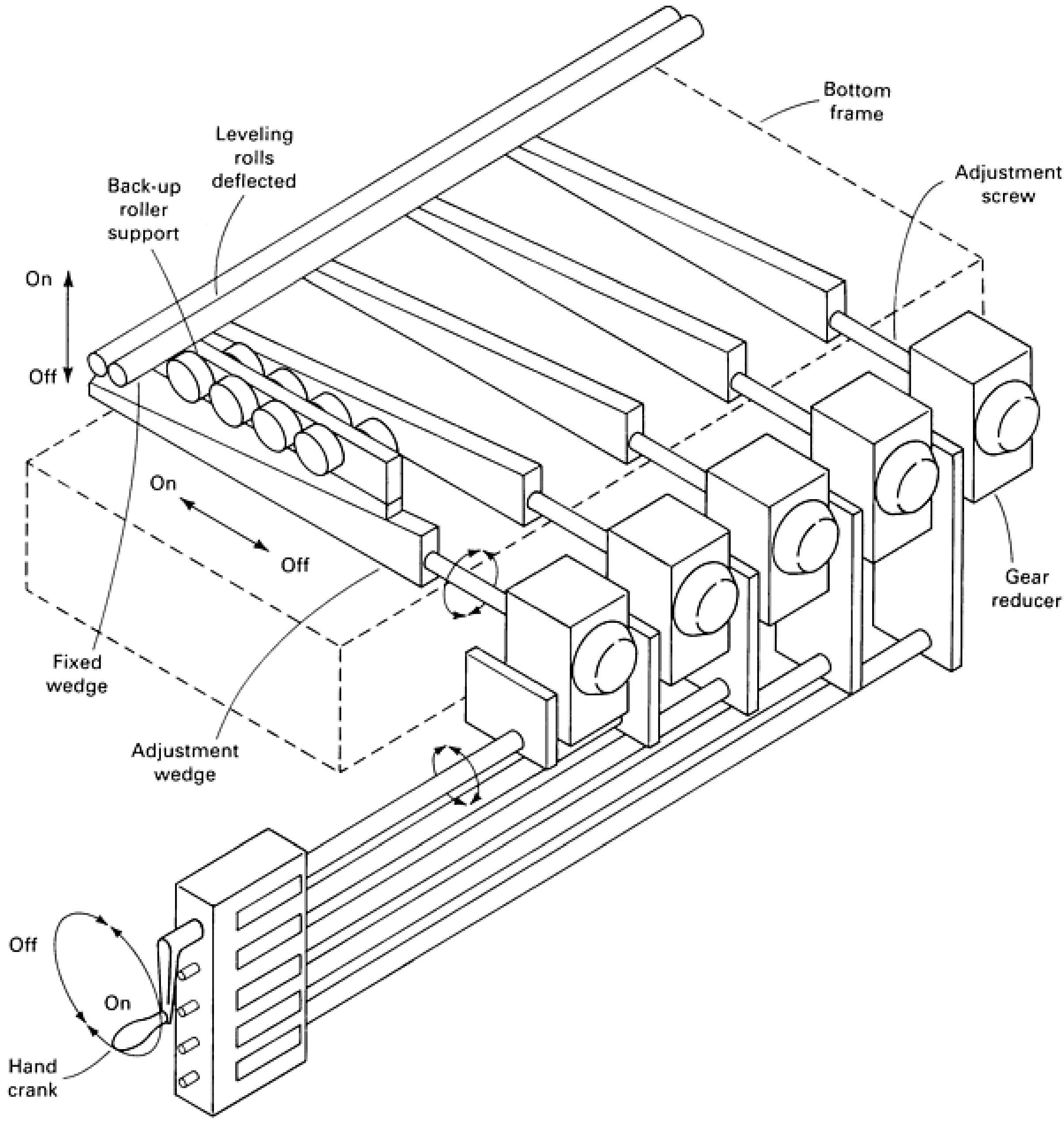

Fig. 14 Wedge-type backup roller adjustment for the most accurate control of leveling roll deflection

The process of leveling strip calls for a high-precision piece of equipment, with absolutely minimal machine deflection during operation. The flattener and leveler in any coil-processing line is the first step in producing quality strip or blank shapes. Gaging systems, feeding systems, and so forth cannot maintain optimal accuracy if the strip itself is not consistently flat initially.

Shearing of Bars and Bar Sections

Introduction

BARS AND BAR SECTIONS are sheared between the lower and upper blades of a machine in which only the upper blade is movable. As the upper blade is forced down, the work metal is distorted and caused to fracture. There are also shears, such as impact cutoff machines, which utilize a horizontal knife movement to shear the bar sections. Figure 1 shows the appearance of a sheared round bar. The burnished area, or depth of shear action by the blade, is usually one-fifth to one-fourth the diameter of the bar. In visual examination of a sheared edge, the burnished portion appears smooth, while the fractured portion is comparatively rough.

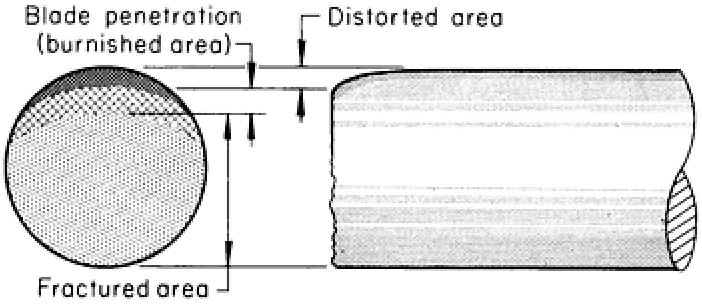

Fig. 1 Effects of shearing a round bar with a straight blade.

Applicability. In general, any metal that can be machined can be sheared, but power requirements increase as the strength of the work metal increases. Further, blade design is more critical and blade life decreases as the strength of the work metal increases. Equipment is available for shearing round, hexagonal, or octagonal bars up to 152 mm (6 in.) in diameter or thickness, rectangular bar and billets up to 75 × 305 mm (3 × 12 in.) in cross section, and angles up to 203 × 203 × 38 mm (8 × 8 × $1\frac{1}{2}$ in.).

Straight blades can be used to shear bars and bar sections, although a considerable amount of distortion occurs, as shown in Fig. 1. In addition, the concentration of shock on the blades is high when shearing with straight blades (particularly when shearing round bars). Preferred practice is to use blades that conform to the shape of the work metal, as discussed in the section "Blade Design and Production Practice" in this article. Angles are usually sheared in a special machine, or in a special setup with conforming blades.

Because of the conditions illustrated in Fig. 1, the edges sheared with straight blades are not as high quality as edges that are sawed or otherwise machined. However, when blades are sharp and accurately adjusted, sheared edges that are acceptable for a wide range of applications can be obtained. The quality of sheared edges usually increases as thickness of the work metal decreases.

Accuracy of Cut. Workpieces properly supported on both sides of the shear blades by a roller conveyor table and placed squarely against a gage stop securely bolted to the exit side of the machine can ordinarily be cut to lengths accurate to +3.2, -0 mm (+$\frac{1}{8}$, -0 in.) on shears that can cut bars up to 102 mm (4 in.) in diameter. When larger shears are used, the breakaway of the metal can cause a variation of ±4.8 mm (±$\frac{3}{16}$ in.). Fairly consistent accuracy in the shearing of slugs can be obtained by careful adjustment of the gage setting, especially if the slugs are produced on a weight-per-piece basis. Supporting the free end of the material on a spring-supported table will minimize bending during the shearing operation, thus providing better control over the length of cut.

Selection of Cutoff Method. The method of cutting off bars can be determined by the edge condition required for subsequent operations. Sawing usually produces a uniform cut edge with little or no damage to the microstructure in the immediate area. Gas cutting produces an edge that resembles a sawed edge in smoothness and squareness. However, the cut edge of some steels becomes hardened during gas cutting, thus making subsequent machining difficult. A sheared edge is usually easy to machine, but can make the fit-up of parts of a weldment more difficult and can increase warpage because of wider gaps.

Power Requirements. The net horse-power required for shearing can be estimated from the following formula:

$$\text{hp} = \frac{AVS}{33\ 000} \quad \textbf{(Eq 1)}$$

where A is the cross-sectional area of the workpiece (in square inches), V is the speed of the shear blade (in feet per minute), and S is the shear strength of the work metal (in pounds per square inch). The 33,000 is foot-pounds per minute per horsepower. For metric use, the power in English units (hp) should be multiplied by 0.746 to obtain kilowatts. It may be necessary to increase the calculated value as much as 25% to compensate for machine inefficiency.

Although Eq 1 is used for estimating, it is of limited value because it does not consider the ductility of the metal. The formula is based on shearing low-carbon steel. Copper, for example, is more ductile than steel; therefore, the distance of blade penetration before fracture in copper will be greater than that in steel. Conversely, when shearing metals that are less ductile than low-carbon steel, the distance of blade penetration before breakaway will be less. Power requirements are also affected by the ductility of the work metal.

Cutting Speed. The speed at which material is sheared without adverse effect can range from almost zero to 21 or 24 m (70 or 80 ft) per min. However, as speed increases above 6.1 or 7.6 m (20 to 25 ft) per min, problems are encountered in holding the workpiece securely at the blade without the far end whipping, especially with material 6.4 mm ($\frac{1}{4}$ in.) thick or more. When bars harder than 30 HRC are cut at speeds of 12 to 15 m (40 to 50 ft) per min or higher, chipping of the blade is common (see Example 1).

Shearing of Bars and Bar Sections

Machines

The production shearing of bars and bar sections is usually done in machines with a throat opening designed for large, bulky workpieces. These machines include alligator and guillotine shears, and a multipurpose machine with interchangeable punches and dies for shearing, punching, and coping. Squaring shears, normally used for sheet and plate, can also be used for cutting bar stock to length. Punch presses and press brakes can be provided with appropriate tooling for shearing operations.

In alligator shears (also known as pivot or nutcracker shears), the lower blade is stationary, and the upper blade, held securely in an arm, moves in an arc around a fulcrum pin (Fig. 2). The shearing action is similar to that of a pair of scissors. A crankshaft transmits power to the shearing arm, and the leverage applied produces the force for shearing. Maximum shearing force is obtained closest to the fulcrum, and the mechanical advantage decreases as the distance between the point of shearing and the fulcrum increases. Therefore, with the maximum opening (largest rake angle), capacity is maximum for any shear. As the blade begins downward travel and rotation about its fulcrum, the cross-sectional area engaged by the blade is increased; therefore, more energy is expended, and the upper blade is slowed down until the breakaway point is reached. At this point, the mechanical forces have overcome the resistance of the metal, and the remainder of the cross-sectional area breaks off.

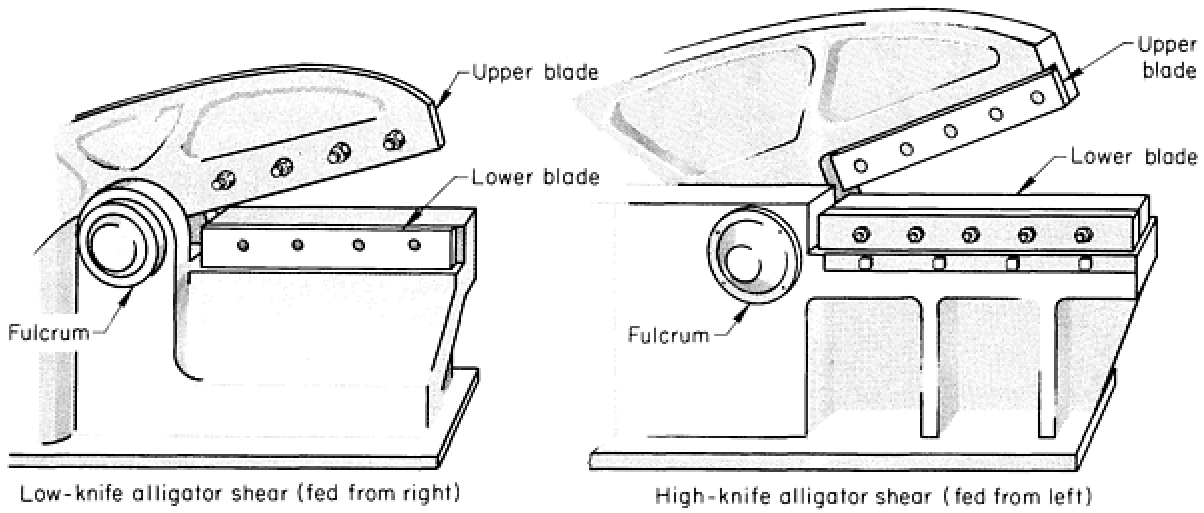

Fig. 2 Low-knife and high-knife alligator shears. See text for discussion.

The capacity of an alligator shear is designated as the maximum cross-sectional area of flat plate or round bar that can be sheared, based on work metal having a tensile strength of 275 MPa (40 ksi). An alligator shear can be used not only for bars and bar sections but also for plate, sheet, and strip within the limitations of the blade length of the machine. Alligator shears are extensively used for preparing scrap because the single pivot point allows the blades to open wide enough to accept bulky objects. For example, on a machine with 305 mm (12 in.) long blades, the opening between upper and lower shear blades is approximately 133 mm ($5\frac{1}{4}$ in.); on a machine with 914 mm (36 in.) long blades, the opening can be as great as 279 mm (11 in.).

The two types of shearing arms used on alligator shears are low-knife and high-knife (Fig. 2). In the low-knife type, the cutting edge of the lower blade is in line with the center of its fulcrum pin; in high-knife shear, the cutting edge of the lower blade is on a plane above the centerline of the fulcrum pin. In general, the low-knife shear is preferred for cutting bars and bar sections. The high-knife shear is preferred for cutting flat stock and for use in scrap yards. A high-knife shear, by making successive cuts, can shear flat stock that is wider than the length of the shear blades. An alligator shear is further classified as either right-hand or left-hand (each is shown in Fig. 2), depending on the side from which it is fed.

The weight of alligator shears ranges from about 1100 to 19,500 kg (2500 to 43,000 lb). The lighter shears can be made portable on wheels, on a sled-type skid, or on blocks. The heavier machines must be anchored in concrete.

The speed of alligator shears ranges from approximately 50 strokes per minute for the smallest power-driven machine to about 18 strokes per minute for the largest heavy-duty shear. Except for small hand-operated types, alligator shears are usually mechanically driven; a flywheel provides uniform, sustained power during cutting.

The number of strokes per minute can be reduced by changing either the motor speed or the diameter of the flywheel pulley. An increase in the number of strokes may affect the stored energy of the flywheel and reduce the shearing capacity. On continuously cutting shears, greater speed will reduce workpiece-positioning time for the operator and therefore may reduce output rather than increase it. On single-stroke machines, cutting time is minimal compared to the time required for accurate positioning of the stock.

Guillotine shears are designed for cutting bars and bar sections to desired lengths from mill stock. They are extensively used throughout the fabricating industry. Two general types are available: open end (Fig. 3) and closed end. The shear illustrated in Fig. 3 is termed an open-end shear because it has a C-frame construction with one end open and unsupported. Open-end shears are either single end, for one operator, or double end, for two operators. On double-end machines, both ends can be right-hand or left-hand, or one end can be right-hand and the other end left-hand, depending on the type of shearing to be done. A closed-end guillotine shear, on the other hand, is basically the same as the one shown in Fig. 3 except that it has frame supports on both sides.

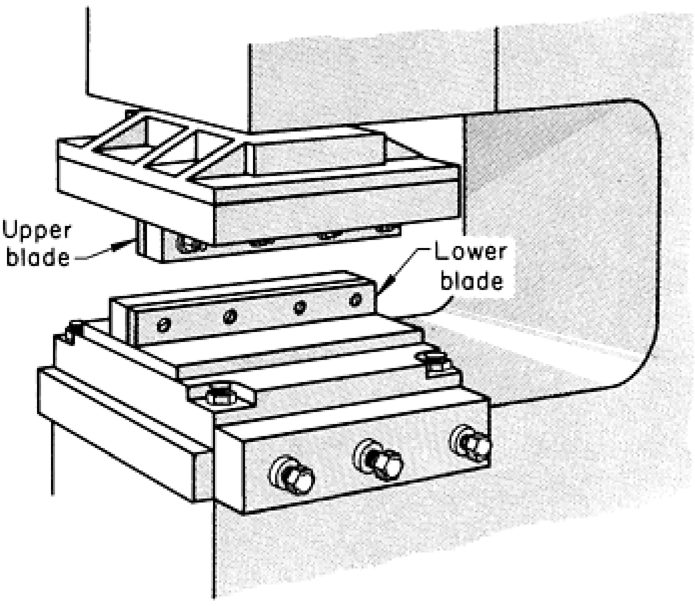

Fig. 3 Open-end guillotine shear.

An open-end shear has the advantage of giving the operator a clear view of the blades. However, because one end is open, a heavier frame and more floor space are required than for a closed-end shear of equal capacity.

Guillotine shears for bars and angles are available in capacities to 2700 kN (300 tonf). Either intermittent or continuous operation is possible. Guillotine shears can be equipped with simple straight blades (Fig. 3) or with two or more short blades having specific shapes.

Guillotine shears are actuated mechanically, hydraulically, or pneumatically. Hydraulic and pneumatic machines are lighter in weight for a specific shearing power, are more economical, and operate with less vibration than mechanical shears. Therefore, hydraulic or pneumatic machines up to 89 kN (10 tonf) capacity are completely portable, and units of 1800 to 2700 kN (200 to 300 tonf) capacity are semiportable (need not be solidly mounted in concrete).

Combination machines are multipurpose machines used primarily in metal-fabricating shops where there is a constant need for shearing small quantities (often two or three pieces) of a variety of shapes and sizes from bars or bar sections. Some combination machines can also be used for punching, slotting, and notching.

Many combination machines incorporate several devices within the frame for performing different operations; therefore, a new setup is not required for each. A holder for an interchangeable punch and die is located in an area with a deep throat. This facilitates the punching of holes, slots, or notches in plates and bars, and in webs or legs of structural members.

A slide moving at 45° from vertical carries a blade for shearing angles. The support bed is on a swivel so that the ends of the angle section can be varied as desired from 45 to 90°. In two strokes of the machine, angles can be sheared to produce miter joints for subsequent welding (see the section "Shearing of Angles" in this article).

Combination machines can be used for cutting square and rectangular notches in the leg of an angle. These machines can be set up to cut a 90° V-shaped notch in angles that subsequently will be bent into frames. Other shapes, such as beams and channels, can be notched in a similar manner if the machine can accommodate the vertical height between the upper and lower legs of the workpiece. Provision is also made for shearing bars with guillotine-type blades or special blades (see the section "Conforming Blades" in this article).

Combination machines are available in capacities ranging from 110 to 890 kN (12 to 100 tonf).The 110 kN (12 tonf) machine can punch a 14 mm ($\frac{9}{16}$ in.) diam hole through a 6.4 mm ($\frac{1}{4}$ in.) thick section and can shear 75 × 75 × 6.4 mm (3 × 3 × $\frac{1}{4}$ in.) angles, 22 mm ($\frac{7}{8}$ in.) diam rounds, 19 mm ($\frac{3}{4}$ in.) squares, and 102 × 6.4 mm (4 × $\frac{1}{4}$ in.) flats. The 890 kN (100 tonf) machine can shear 152 × 152 × 16 mm (6 × 6 × $\frac{5}{8}$ in.) rounds, 50 mm (2 in.) squares, and 203 × 19 mm (8 × $\frac{3}{4}$ in.) flats.

Punching and shearing machines are deep-throat C-frame machines for the punching, shearing, notching, or coping of plates, bars, and structural sections. Shoes, into which punches, dies, and shear blades can readily be inserted, are mounted on the bed and ram.

The shoes either rest on the bed or are overhung; the overhanging type is designed so that structural shapes can be punched in both web and flange. The plain type of shoe is primarily used for plate work; however, plates can be worked with the overhanging shoes. Both types of die blocks are fitted with die sockets that hold dies of different inside diameters. The punch holder is adjustable to suit the location of the dies. Table 1 lists the capacities for punching and shearing with this type of machine.

Table 1 Maximum workpiece dimensions that can be accommodated in vertical open-cap punching and shearing machines of various tonnage ratings

Machine rating		**Punching**				Shearing			
		Hole diameter		**Plate thickness**		**Plate thickness**		Size of bar section or plate	
kN	**tonf**	**mm**	**in.**	**mm**	**in.**	**mm**	**in.**	**mm**	in.
445	50	21	$\frac{13}{16}$	19	$\frac{3}{4}$	13	$\frac{1}{2}$	127 × 13	**5 ×** $\frac{1}{2}$
870	$97\frac{1}{2}$	32	$1\frac{1}{4}$	25	1	22	$\frac{7}{8}$	152 × 25	**6 × 1**
1390	156	50	2	25	1	29	$1\frac{1}{8}$	203 × 25	**8 × 1**
2780	$312\frac{1}{2}$	64	$2\frac{1}{2}$	38	$1\frac{1}{2}$	38	$1\frac{1}{2}$	254 × 38	**10 × 1**$\frac{1}{2}$
4450	500	102	4	38	$1\frac{1}{2}$	50	2	254 × 64	**10 × 2**$\frac{1}{2}$

6230	**700**	**152**	**6**	**38**	**$1\frac{1}{2}$**	**64**	**$2\frac{1}{2}$**	**305 × 75**	**12 × 3**

Shearing							
Diameter of round		**Size of square**		Size of angle			
				Cutting on square		Cutting on angle	
mm	**in.**	**mm**	**in.**	**mm**	**in.**	**mm**	in.
38	$1\frac{1}{2}$	32 × 32	$1\frac{1}{4} \times 1\frac{1}{4}$	75 × 75 × 9.5	$3 \times 3 \times \frac{3}{8}$	102 × 102 × 9.5	**$4 \times 4 \times \frac{3}{8}$**
50	2	38 × 38	$1\frac{1}{2} \times 1\frac{1}{2}$	102 × 102 × 13	$4 \times 4 \times \frac{1}{2}$	152 × 152 × 9.5	**$6 \times 6 \times \frac{3}{8}$**
64	$2\frac{1}{2}$	50 × 50	2 × 2	152 × 152 × 13	$6 \times 6 \times \frac{1}{2}$	. . .	**. . .**
89	$3\frac{1}{2}$	75 × 75	3 × 3	203 × 203 × 19	$8 \times 8 \times \frac{3}{4}$	. . .	**. . .**
127	5	89 × 89	$3\frac{1}{2} \times 3\frac{1}{2}$	203 × 203 × 32	$8 \times 8 \times 1\frac{1}{4}$	. . .	**. . .**
152	**6**	**108 × 108**	**$4\frac{1}{4} \times 4\frac{1}{4}$**	**203 × 203 × 38**	**$3 \times 8 \times 1\frac{1}{2}$**	**. . .**	**. . .**

Fixturing is an important consideration in the shearing of bars. For safety and the proper functioning of open-end shears (Fig. 3) and for shearing units such as those shown in the center and at the right in Fig. 7, hold-down fixtures are essential. Guide pins are also helpful, especially when shearing with conforming blades as shown in Fig. 7.

Shear Blades

Shock-resistant tool steels such as S2 or S5 are most commonly used as blade materials for cold shearing, although L6 has also been successfully used for some applications. Some plants use shear blades made from carbon or alloy steel with hardfaced shearing edges.

The hardness of tool steel blades usually ranges from 45 to 55 HRC (sometimes as high as 58 HRC). The upper end of this range can be used when work metal thickness does not exceed 13 mm ($\frac{1}{2}$ in.). As work metal thickness increases, the hardness of the blade should be decreased to the lower end of this range, but not below 45 HRC unless experience with previous applications warrants it. The practice in most plants is to start with blades near the lower end of the range, increasing their hardness only after experience proves it safe. Excessive blade wear is usually preferable to blade breakage.

Blade Material for Hot Shearing. Blades for the hot shearing of bar stock are usually made of H11, H12, or H13 hot-work tool steel (see the article "Wrought Tool Steels" in *Properties and Selection: Irons, Steels, and High-Performance Alloys*, Volume 1 of the *ASM Handbook*). Tool steel for blades can also be made of compositions manufactured by powder metallurgy processes. There are no data to prove the superiority of one of these steels over the others. Grades H21 and H25 are sometimes used, but they are more costly and are recommended only when H11 has been tried and found to be inadequate.

The hardness of blades for hot shearing varies considerably with the thickness and temperature of the metal to be sheared and with the type and condition of the shearing equipment. However, hardness is usually maintained at 38 to 48 HRC.

For high-alloy metals to be sheared at high temperatures, higher-alloy blades may be needed. High-temperature engine-valve alloys have been sheared with T1 high-speed steel blades.

Hardfaced blades are satisfactory for hot shearing and are used exclusively in some plants. The material for the blade body is usually 1030 or 1045 steel. Additional information on hardfacing technology is available in the articles "Hardfacing, Weld Cladding, and Dissimilar Metal Joining" in *Welding, Brazing, and Soldering*, Volume 6, and "Metal and Alloy Powders for Welding, Hardfacing, Brazing, and Soldering" in *Powder Metal Technologies and Applications*, Volume 7 of the *ASM Handbook.*

Blade Profile. The cross section of an alligator shear blade for cutting bars and shapes is normally rectangular. Light-duty blades are about 32 mm ($1\frac{1}{4}$ in.) wide by 102 mm (4 in.) deep by 305 mm (12 in.) long. Blades for machines of about maximum size are commonly about 50 × 127 × 914 mm (2 × 5 × 36 in.). For mounting, blades are provided with countersunk holes as shown in Fig. 4 that allow bolt heads to be sunk sufficiently to prevent interference between blades.

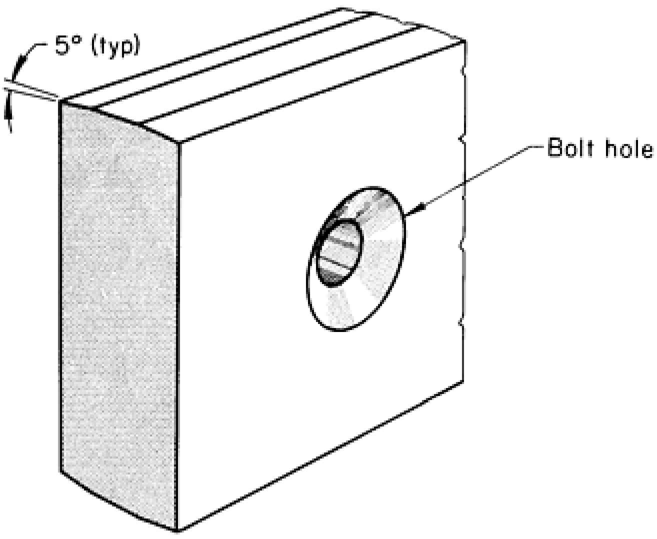

Fig. 4 Straight shear blade ground with negative rake on all four cutting edges.

Blade clearance for shearing bars and bar sections ranges from 0.13 to 0.38 mm (0.005 to 0.015 in.). The smaller clearance is used for shearing clean work metal; the larger clearance is preferred for shearing scaly products to prevent scale or other foreign material from lodging between blades and scoring the surfaces.

Blades for alligator shears are available with grooves across the width to prevent forward movement of the work metal when the upper blade descends, so that more of the cutting length of the blade can be used. Most blades have four cutting edges that are identically ground (Fig. 4); therefore, by inverting the blade and reversing its direction, all four cutting edges can be used before the blade is returned for sharpening. Resharpening any of the four edges requires grinding of one or both faces of an edge. Consequently, a blade that shows severe damage, such as breakout of a section, must be ground to a new, clean and sharp edge. To avoid such major regrinding, blades should be kept free from large nicks and mushrooming.

Most blades are ground to a slight negative rake, as shown in Fig. 4. The intent is to cause the work metal to begin to flow from a slight bending action before actual shearing takes place. Blades provided with a negative rake of 5 to 10° are often less susceptible to chipping at the cutting edge than those ground with a 90° edge (zero rake).

Shear Blade Life. Service data on shear blade life are scarce because maintenance programs in most high-production mills call for removal of blades and redressing during scheduled shutdowns, regardless of the condition of the blades at the time. Blade life in number of cuts before regrinding has been variously reported at 5000 to more than 2 million. Even when an attempt is made to make blade material and cutting conditions as nearly identical as possible, variations in blade life of 100% or more have been reported.

Blade life depends to a great extent on the composition and hardness of the work metal (see the article "Selection of Materials for Shearing and Slitting Tools" in *Properties and Selection: Stainless Steels, Tool Materials, and Special-*

Purpose Metals, Volume 3 of the 9th Edition of *Metals Handbook*). The angle of the cutting edge often affects blade life, and in some cases, shearing speed has a marked effect on blade life. For example, harder work metal usually requires a lower shearing speed in order to avoid blade chipping and premature dulling. When work metal hardness is 30 HRC or higher, speeds greater than 15 m (50 ft) per min are not recommended, and much slower speeds may be required for acceptable blade life. The following example demonstrates the effects of blade angle and speed on blade life.

Example 1: Reduction in Speed for Prolonged Blade Life.

When 1085 flat spring steel 3.2 and 4.8 mm ($\frac{1}{8}$ and $\frac{3}{16}$ in.) thick and 50 mm (2 in.) wide, ranging in hardness from 30 to 34 HRC, was sheared to lengths of 1.2 and 1.5 m (4 and 5 ft) at a blade speed of 21 m (70 ft) per min in a punch press, blades had to be replaced every 2 to 3 weeks (200 to 500 cuts) because of chipping. In addition, regrinding was required during this period. When the procedure was changed to shearing at 3 m (10 ft) per min in a C-frame shear, blade life was increased to an average of 10,000 cuts before regrinding was required, and chipping was eliminated. Blades for both the punch press and the shear were made from S1 tool steel and ranged in hardness from 54 to 56 HRC.

Cost. Because of the relatively small amount of machining needed to make a shear blade, compared with the machining needed to make an intricate impression in a die block or a forming die, the cost of the material is an important part of the total cost of a blade. A blade made from S2 tool steel costs 1.8 times as much as one made from W2 tool steel, and 0.7 times as much as one made from D2 tool steel.

Shearing of Bars and Bar Sections

Blade Design and Production Practice

The straight-edge blades described in the preceding section and illustrated in Fig. 4 can shear almost any bar or shape that is within the capacity of the machine. However, unacceptable distortion may result in some shapes of workpieces when they are sheared with blades that are not designed for cutting specific shapes.

Conforming Blades. One method of minimizing distortion in sheared bars employs two hardened blades mounted face-to-face, with identical holes through each blade. The holes should conform to the shape of the work metal and should be large enough to allow easy passage through the blades (Fig. 5a). One blade is movable vertically and one is stationary. Relatively little movement of the machine is required when blades of this type are used. In addition, because the blades completely encircle the work metal, hold-downs are not needed. However, these blades are usually limited for use on specially built or combination machines.

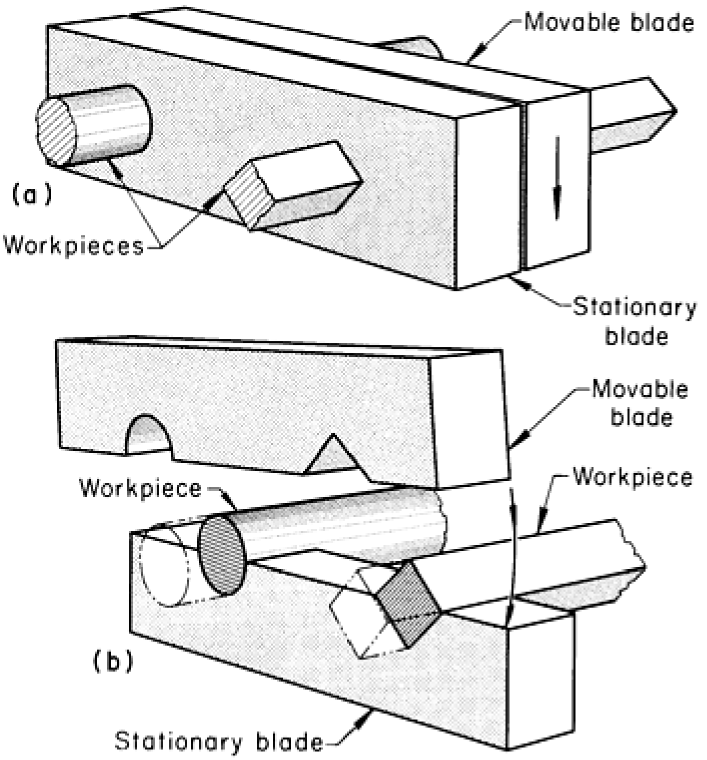

Fig. 5 Two types of blades for the shearing of bars. See text for discussion.

The shearing of round and square bars is more frequently done with the open-type blades illustrated in Fig. 5(b). Each blade is contoured to accommodate one-half the cross section of the work metal. The upper blade moves in a vertical direction, while the lower blade remains stationary. When using this technique, some type of hold-down is needed. Because of the stiffness of the work metal, the hold-down for bars should permit slight movement of the work metal in the axial direction to avoid double shearing. The hold-down can be a simple set screw (to permit adjustment) in a bracket or can be a more elaborate unit, such as a handwheel assembly utilizing an Acme thread.

For shearing square bars with any type of blade, the work metal should be placed so that the movement of the blade is across the diagonal of the square. With this technique the shearing force is applied to four sides instead of two, resulting in a smoother sheared surface. Shearing across the diagonal provides support on two sides of the square shape, which minimizes distortion, and permits more than one size of bar stock to be sheared in a given hole.

Best practice for shearing round bars is to use blades with holes for each size of stock to be cut. Blade holes appreciably larger than the stock size cause excessive distortion of the workpiece.

Shearing of angles is done either in a combination machine or by double cutting. In a combination machine--the more common method--two blades such as those shown in Fig. 6(a) are used. One blade, usually the one that is stationary, is L-shaped and is positioned as shown. The movable blade is square or rectangular and is mounted with its two cutting edges parallel to those of the stationary blade. Figure 6(a) also shows that the space between the blades in the loading position is the same shape as the workpiece.

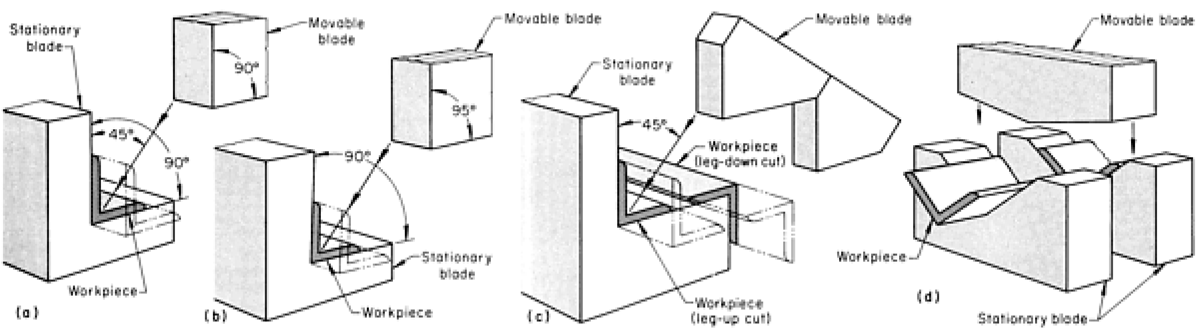

Fig. 6 Four types of blades for the shearing of angle sections. (a) to (c) Shearing in a combination machine. (d) Double-cutting method.

The movable blade travels at 45° toward the stationary blade, and both blades contact the work metal uniformly. Shearing by this technique is essentially a blanking cut, and distortion of the workpiece is minimal. One disadvantage of the method is that all cutting occurs at once, resulting in a high shear load. This condition is not important when small angle sections are cut; however, for work metal larger than 102 × 102 × 13 mm (4 × 4 × $\frac{1}{2}$ in.), the movable blade should be provided with rake to prevent excessive loading.

To provide a rake angle between the movable and stationary blades, the included angle between the cutting edges of the upper or movable blade is increased to 95°, as shown in Fig. 6(b). Shearing begins at the extremity of each leg and progresses toward the root of the angle. The increase in the included angle of the movable blade results in some distortion of the drop-cut piece; the amount of distortion is about equal to the difference in angle between the movable and stationary blades (5° is normal). The part remaining on the table or stationary blade is not distorted.

Most combination shearing machines use a more versatile blade arrangement than those shown in Fig. 6(a) and 6(b). The setup shown in Fig. 6(c) is used to shear angle sections in both the leg-up and the leg-down positions. A swiveling table locates and holds the workpiece during shearing. With the swiveling table and two positions for the workpiece, the flanges can be easily mitered to any specific angle. For example, when shearing angle sections for a frame having the leg on the inside, the table would be set and locked at 45°. One end is mitered by placing the section in a leg-down position on the table and shearing off enough to make a clean cut. The other end is mitered by placing the section leg-up on the table and shearing to the proper length. The opposite positions are applicable when angle sections for a frame having the leg on the outside are being cut.

Shearing at a 45° angle reduces the capacity of the machine because a greater length of metal is cut at one time when a 90° cut is made. For example, a machine with a capacity of 203 × 203 × 32 mm (8 × 8 × 1$\frac{1}{4}$ in.) when making a 90° cut has a capacity of only 203 × 203 × 25 mm (8 × 8 × 1 in.) when cutting at 45°.

Double cutting of angle sections, also called slugging, is used less frequently than shearing in a combination-type machine. This technique uses two stationary blades, spaced 13 mm ($\frac{1}{2}$ in.) apart, and one movable V-shaped blade arranged as shown in Fig. 6(d). The movable blade has a shallow V-shape that does not conform to the shape of the workpiece. Shearing starts at the extremity of each leg and progresses to the root of the angle, producing a 13 mm ($\frac{1}{2}$ in.) wide slug that is pushed out the bottom between the stationary blades.

In the double-cutting method of shearing, distortion occurs only in the slug, because the work metal is supported by the two stationary blades. There are two disadvantages of using the double-cutting method. First, increased power is required for making two cuts at the same time, and second, some metal is lost in the slug. The two stationary blades must be firmly supported to prevent their spreading during the cutting operation.

A similar tool can be used for shearing a channel section. The stationary blades should closely fit the contour of the channel section. Double cutting is adaptable to a guillotine shear, a combination machine, or a press.

Multiple Setups. Fabricating shops often must shear small quantities to a variety of shapes. To handle such work, many shops use a machine with a multiple setup such as the one shown in Fig. 7. Without changing blades, the following operations can be performed:

- Double shearing of angle sections (Fig. 7, left)
- Straight-blade shearing (Fig. 7, center)
- Shearing of round and square bars and single shearing of L-sections (Fig. 7, right)

All movable blades are attached to a single ram.

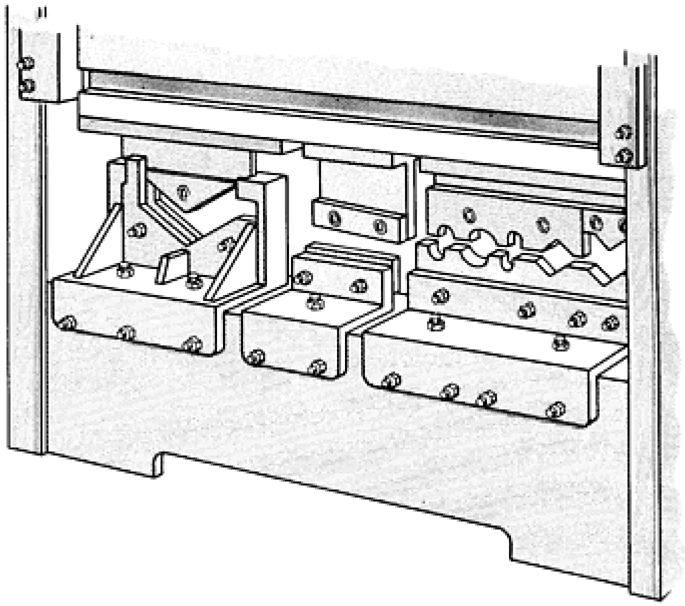

Fig. 7 Multiple setup for various types of shearing--singly or in combination.

Impact Cutoff Machines

Impact cutoff machines produce high-quality cut slugs or blanks of precise length from round, square, or specially shaped bars or coils. Two precision cutoff dies are engaged in opposite directions within the impact block, with short simultaneous strokes fracturing the metal. The result (Fig. 8) is a clean cut at the interface of the two dies, producing slugs or blanks with length tolerances of well within ±0.13 mm (±0.005 in.) and virtually no deformities at reduction speeds of 300 cuts per minute.

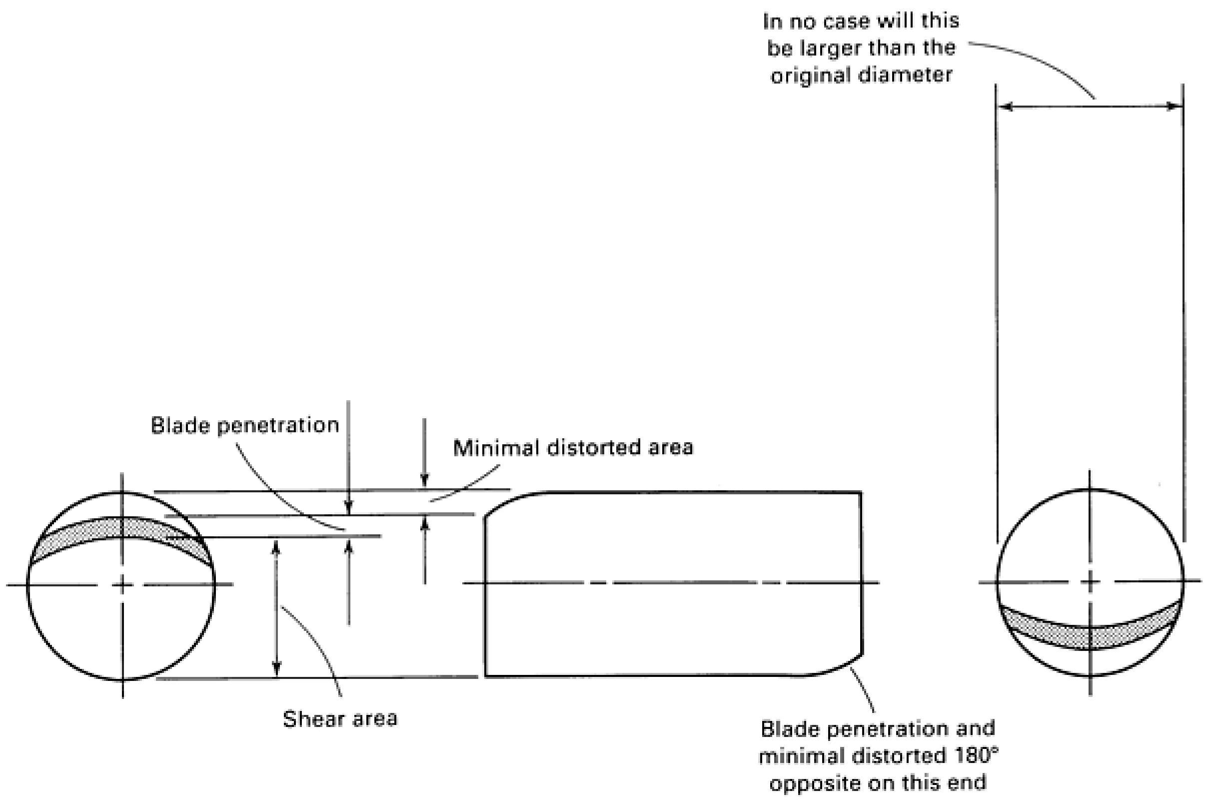

Fig. 8 Effects of shearing a round bar with double-die impact cutoff-type shear.

The quality of cut obtained surpasses that produced by any other known shear-type cutoff device. Squareness is held to close tolerances, and the cut pieces are virtually free of burrs, distortion, and edge rollover.

On materials such as carbon steel of 414 MPa (60 ksi) tensile strength, impact cutoff machines are capable of cutting 64 mm ($2\frac{1}{2}$ in.) diam stock up to 914 mm (36 in.) long. Doubling the tensile strength of the material to 818 MPa (120 ksi) reduces the maximum diameter capacity to 44 mm ($1\frac{3}{4}$ in.).

Principle and Machine Construction. Impact cutoff machines utilize a unique double-impact cutting principle (Fig. 9). Stock is fed into a pair of precision cutoff dies that have a cavity shaped to the same configuration as the stock. These opposed dies are actuated with short, simultaneous strokes by two flywheel-cam assemblies. This double impact fractures the metal, cleanly cutting the confined stock at the interface of the two dies.

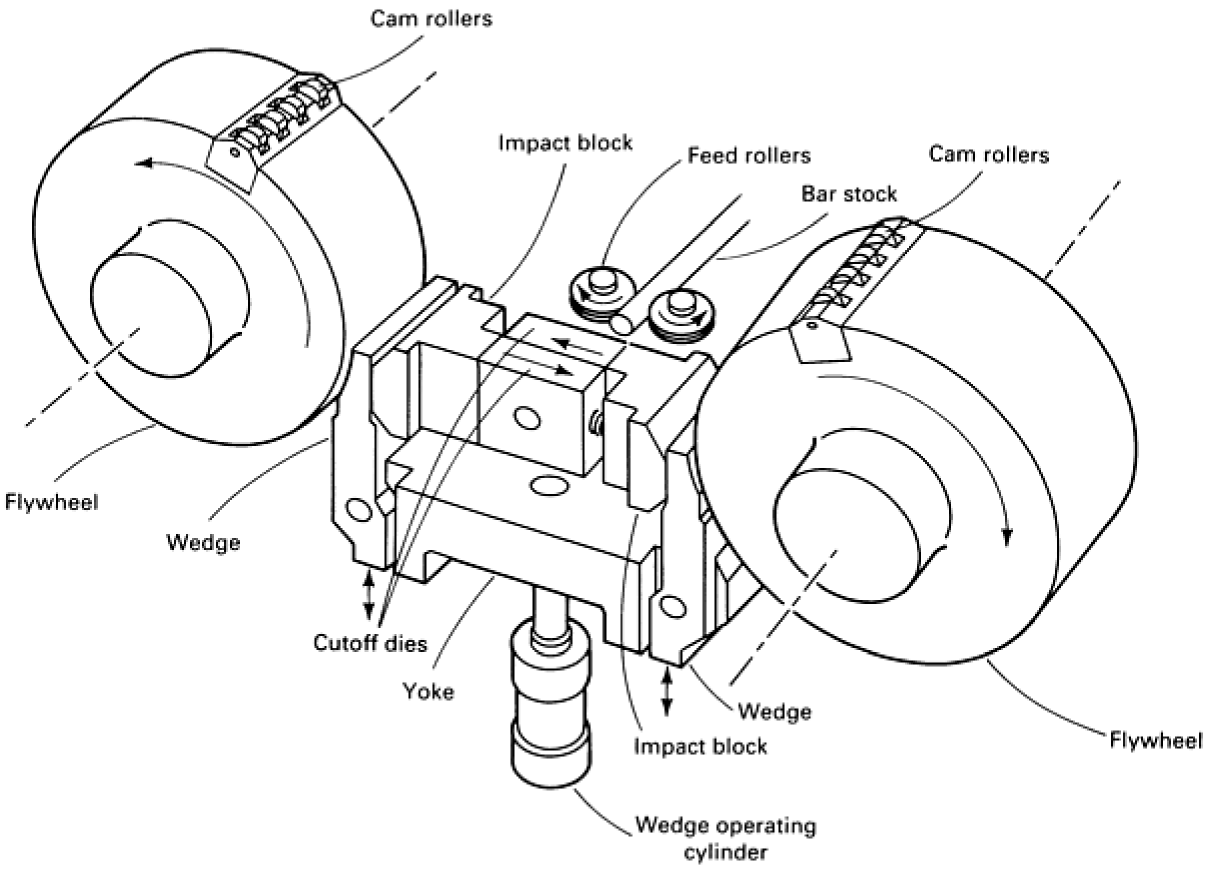

Fig. 9 Impact cutoff machine shearing bar stock with twin cutoff dies that are actuated by cam rollers on identical flywheel-cam assemblies.

The cutoff dies are located within an impact block. The flywheels rotate at a constant speed, and a cut takes place only when a pair of air-operated wedges are brought into position to close the gap. Wedge elevation is triggered by a positive stop that controls the length of cut. The positive stop features a micrometer adjustment for precise control of the blank length.

Either a short or a long target--stock-stop or length gage--is used, depending on the length of cut. In actual operation, the target moves out of the way when the leading edge of the workpiece engages it. This motion activates the circuit that controls the wedges to make the cut. Stock is fed into the dies and up to the positive stop by a hydraulically driven roll feed.

Die life is excellent in impact cutoff machines and ranges from tens of thousands to hundreds of thousands of cuts before resharpening is required. A number of variables affect this, with the most important being the amount of clearance between the workpiece and the die.

The dies are usually of the inserted type. The longest service life has been obtained with inserts made of M2 high-speed steel hardened to 60 to 62 HRC.

When resharpening of the cutoff die is required because of wear, the insert is first removed and then replaced in the holder with a 0.51 mm (0.020 in.) shim behind it. The protruding portion of the insert is then sharpened by a simple surface grinding operation. Removing the die blocks for sharpening or replacement is fast and simple. The die-retaining plate unbolts, allowing the cutting dies to be easily lifted out.

Revised by Ed Craig, AGA Gas, Inc.

Introduction

THERMAL CUTTING processes differ from mechanical cutting (machining) in that the cutting action is initiated either by chemical reaction (oxidation) or melting (heat from arc). All cutting processes result in the severing or removal of metals. Additional information on cutting processes used in metal-forming operations can be found in the articles "Laser Cutting" and "Abrasive Waterjet Cutting" in this Volume.

Oxygen cutting is accomplished through a chemical reaction in which preheated metal is cut, or removed, by rapid oxidation in a stream of pure oxygen. Typical oxygen cutting processes are oxyfuel gas, oxygen lance, chemical flux, and metal powder cutting. Oxyfuel gas cutting and its modifications, chemical flux cutting and metal powder cutting, which are used to cut oxidation-resistant materials, are discussed in this article.

Arc cutting melts metal by heat generated from an electric arc. Because extremely high temperatures are developed, arc cutting can be used to cut almost any metal. Modifications of the process include the use of compressed gases to cause rapid oxidation (or to prevent oxidation) of the workpiece, thus incorporating aspects of the gas cutting process. Arc cutting methods include air carbon arc, gas metal arc, gas tungsten arc, shielded metal arc, plasma arc, and oxygen arc cutting. The methods of industrial importance that are covered in this article include plasma arc cutting, air carbon arc cutting, electric arc cutting using consumable tubular electrodes (Exo-Process), and oxygen arc cutting.

Thermal Cutting

Revised by Ed Craig, AGA Gas, Inc.

Oxyfuel Gas Cutting

Oxyfuel gas cutting includes a group of cutting processes that use controlled chemical reactions to remove preheated metal by rapid oxidation in a stream of pure oxygen. A fuel gas/oxygen flame heats the workpiece to ignition temperature, and a stream of pure oxygen feeds the cutting (oxidizing) action. The oxyfuel process, which is also referred to as burning or flame cutting, can cut carbon and low-alloy plate of virtually any thickness. Castings more than 750 mm (30 in.) thick commonly are cut by the oxyfuel process. With oxidation-resistant materials, such as stainless steels, either a chemical flux or metal powder is added to the oxygen stream to promote the exothermic reaction. Equipment for such cutting is somewhat awkward, however, and speeds and cut quality are lower than those obtained with plasma arc cutting.

The simplest oxyfuel gas cutting equipment consists of two cylinders (one for oxygen and one for the fuel gas), gas flow regulators and gages, gas supply hoses, and a cutting torch with a set of exchangeable cutting tips. Such manually operated equipment is portable and inexpensive. Cutting machines, employing one or several cutting torches guided by solid template pantographs, optical line tracers, numerical controls, or computers, improve production rates and provide superior cut quality. Machine cutting is important for profile cutting, that is, the cutting of regular and irregular shapes from flat stock.

Principles of Operation

Oxyfuel gas cutting begins by heating a small area on the surface of the metal to the ignition temperature of 760 to 870 °C (1400 to 1600 °F) with an oxyfuel gas flame. Upon reaching this temperature, the surface of the metal appears bright red. A cutting oxygen stream is then directed at the preheated spot, causing rapid oxidation of the heated metal and generating large amounts of heat. This heat supports the continued oxidation of the metal as the cut progresses. Combusted gas and the pressurized oxygen jet flush the molten oxide away, exposing fresh surfaces for cutting. The metal in the path of the oxygen jet burns. The cut progresses, making a narrow slot, or kerf, through the metal.

To start a cut at the edge of a plate, the edge of the preheat flame is placed just over the plate edge to heat the material. When the plate heats to red, the cutting oxygen is turned on, and the torch moves over the plate to start the cut.

During cutting, oxygen and fuel gas flow through separate lines to the cutting torch at pressures controlled by pressure regulators, adjusted by the operator. The cutting torch contains ducts, a mixing chamber, and valves to supply an oxyfuel gas mixture of the proper ratio for preheat and a pure oxygen stream for cutting to the torch tip. By adjusting the control valves on the torch handle or at the cutting machine controller, the operator sets the precise oxyfuel gas mixture desired. Depressing the cutting oxygen lever on the torch during manual operation initiates the cutting oxygen flow. For machine cutting, oxygen is normally controlled by the operator at a remote station or by numerical control. Cutting tips have a single cutting oxygen orifice centered within a ring of smaller oxyfuel gas exit ports. The operator changes the cutting capacity of the torch by changing the cutting tip size and by resetting pressure regulators and control valves. Because different fuel gases have different combustion and flow characteristics, the construction of cutting tips, and sometimes of mixing chambers, varies according to the type of gas.

Oxyfuel gas flames initiate the oxidation action and sustain the reaction by continuously heating the metal at the line of the cut. The flame also removes scale and dirt that may impede or distort the cut.

The rate of heat transfer in the workpiece influences the heat balance for cutting. As the thickness of the metal to be cut increases, more heat is needed to keep the metal at its ignition temperature. Increasing the preheat gas flow and reducing the cutting speed maintain the necessary heat balance.

Oxygen flow must also be increased as the thickness of the metal to be cut increases. To maintain a steady-state reaction at a satisfactory cutting speed, the velocity and volume, as well as the shape of the oxygen jet, must be closely controlled. Because the cutting-oxygen jet is surrounded by preheating flames, it is affected by these gases and the surrounding atmosphere. The jet must have sufficient volume and velocity to penetrate the depth of the cut and still maintain its shape, force, and effective oxygen content. There is also a relationship between the purity of the cutting oxygen and the time required for oxidation. This invariably has an influence on the ultimate cutting speed.

Quality of Cut. The limits within which the cutting reaction can effectively operate are determined by many factors besides those mentioned. Oxyfuel gas cutting involves control of more than twenty variables. Suppliers of cutting equipment provide tables that give approximate gas pressures for various sizes and styles of cutting torches and tips, along with recommended cutting speeds; these are the variables that the operator can control. Other variables include type and condition (scale, oil, dirt, flatness) of material, thickness of cut, type of fuel gas, and quality and angle of cut. (When not otherwise defined, a cut is usually taken to mean a through or "drop" cut, made in horizontal plates with the cutting tip in the vertical position.)

Higher cutting speeds with good cut quality are obtained during the oxyfuel process using a special tip and torch configuration that provides a curtain of oxygen around the cutting oxygen. The protective curtain maintains a higher level of cutting oxygen purity, which speeds up the oxidation process. Cutting speeds can be increased by approximately 25% for thicknesses up to 25 mm (1 in.).

When dimensional accuracy and squareness of the cut edge are important, the operator must adjust the process to minimize the kerf (the width of metal removed by cutting) and to increase the smoothness of the cut edge. Careful balancing of all cutting variables helps attain a narrow kerf and smooth edge. The thicker the work material, the greater the oxygen volume required and, therefore, the wider the cutting nozzle and kerf.

Process Capabilities

Oxyfuel gas cutting processes are primarily used for severing carbon and low-alloy steels. Other iron-base alloys and some nonferrous metals can be oxyfuel gas cut, although process modification may be required and cut quality may not be as high as is obtained in cutting the more widely used grades of steel. High-alloy steels, stainless steels, cast iron, and nickel alloys do not readily oxidize and therefore do not provide enough heat for a continuous reaction. As the carbon and alloy contents of the steel to be cut increase, preheating or postheating, or both, often are necessary to overcome the effect of the heat cycle, particularly the quench effect of cooling.

Some of the high-alloy steels, such as stainless steel, and cast iron can be cut successfully by injecting metal powder (usually iron) or a chemical additive into the oxygen jet. The metal powder supplies combustion heat and breaks up oxide films. Chemical additives combine with oxides to form lower temperature melting products that flush away.

Applications. Large-scale applications of oxyfuel cutting are found in shipbuilding, structural fabrication, manufacture of earth-moving equipment, machinery construction, and the fabrication of pressure vessels and storage tanks. Many machine structures originally made from forgings and castings can be made at less cost by redesigning them for oxyfuel gas cutting and welding, with the advantages of quick delivery of plate material from steel suppliers, low cost of oxyfuel gas cutting equipment, and flexibility of design.

Structural shapes, pipe, rod, and similar materials can be cut to length for construction or cut up in scrap and salvage operations. In steel mills and foundries, projections such as caps, gates, and risers can be severed from billets and castings. Mechanical fasteners can be quickly cut for disassembly using oxyfuel gas cutting. Holes can be made in steel components by piercing and cutting. Machine oxyfuel gas cutting is used to cut steel plate to size, to cut various shapes from plate, and to prepare plate edges (bevel cutting) for welding.

Gears, sprockets, handwheels, clevises and frames, and tools such as wrenches can be cut out by oxyfuel gas torches. Often, these oxyfuel cut products can be used without further finishing. However, when cutting medium- or high-carbon steel or other metal that hardens by rapid cooling, the hardening effect must be considered, especially if the workpiece is to be subsequently machined.

Thickness Limits. Steel less than 3 mm ($\frac{1}{8}$ in.) thick to over 1.5 in (60 in.) thick can be cut by oxyfuel gas cutting, though some sacrifice in quality occurs near both ends of this range. With very thin material, operators may have some difficulty in keeping heat input low to avoid melting the kerf edges and to minimize distortion. Steel under 6 mm ($\frac{1}{4}$ in.) thick often is stacked for cutting of several parts in a single torch pass. Procedures for light cutting (<9.5 mm, or $\frac{3}{8}$ in. thick), medium cutting (9.5 to 250 mm, or $\frac{3}{8}$ to 10 in. thick), heavy cutting (>250 mm, or 10 in. thick), and stack cutting are discussed in "Oxyfuel Gas Cutting" in *Welding, Brazing, and Soldering*, Volume 6 of the *ASM Handbook*.

Advantages and Disadvantages. A number of advantages and disadvantages are apparent when oxyfuel gas cutting is compared to other cutting operations such as arc cutting, milling, shearing, or sawing. The advantages of the oxyfuel process are:

- Metal can be cut faster. Setup is generally simpler and faster than is the case for machining and about equal to that of mechanical severing (sawing and shearing)
- Oxyfuel gas cutting patterns are not confined to straight lines as in sawing and shearing, or to fixed patterns as in die-cutting processes. Cutting direction can be changed rapidly on a small radius during operation
- Manual oxyfuel gas cutting equipment costs are low compared to those for machine tools. Such equipment is portable and self-contained, requiring no outside power, and well suited for field use
- When properties and dimensional accuracy of gas cut plate are acceptable, oxyfuel gas cutting can replace costly machining operations. It offers reduced labor, overhead, material, and tooling costs, and faster delivery
- With advanced machinery, oxyfuel gas cutting lends itself to high-volume parts production
- Large plates can be cut in place quickly by moving the gas torch rather than the plate
- Two or more pieces can be cut simultaneously using stack cutting methods and multiple-torch cutting machines

The disadvantages of the oxyfuel process include:

- Dimensional tolerances are poorer than they are for machining and shearing
- Because oxyfuel gas cutting relies on oxidation of iron, it is limited to cutting steels and cast iron
- Heat generated by oxyfuel gas cutting can degrade the metallurgical properties of the work material adjacent to the cut edges. Hardenable steels may require preheat and/or postheat to control microstructure and mechanical properties
- Preheat flames and the expelled red hot slag pose a fire hazard to plant and personnel

Factors Affecting Oxyfuel Gas Cutting

Oxygen consumption varies widely in practice, depending on whether maximum economy, speed, or accuracy is sought. Literature supplied by torch and related equipment suppliers provides general guidelines for the amount of oxygen consumed for varying metal thicknesses.

As the cutting oxygen flows down through the cut, the quantity available for reaction decreases. If the flow of oxygen is relatively large and sharply coherent, the rate of cutting through the depth of the cut is not affected; that is, the cutting face will remain vertical if the oxygen is in excess and the cutting speed is not too great.

However, if the oxygen flow is insufficient, or the cutting speed is too high, the lower portions of the cut will react more slowly. As a result, the cutting face will become curved, as shown in Fig. 1. The horizontal distance between the points of entry and exit is called drag. Drag often is expressed as a ratio or as a percentage of the metal thickness.

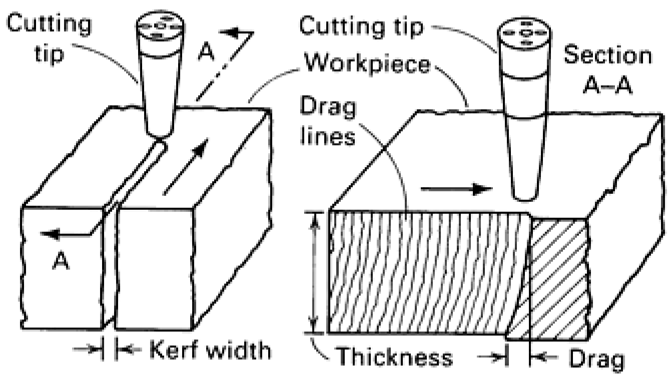

Fig. 1 Cross section of work metal during oxyfuel gas cutting showing drag on cutting face.

Drag can be stabilized; at the proper drag ratio, the heat from the molten metal flowing down the curve is efficiently used. Drag is a rough measure of cutting quality and of economy in oxygen consumption. In metal thicknesses up to 50 or 75 mm (2 or 3 in.), a 10 to 15% drag is associated with good quality of cut and good economy. Higher quality demands less drag; more drag indicates poorer quality and low oxygen consumption. Too much drag may lead to incomplete cutting.

In very thin sections, drag has little meaning; the main problem is control of high heat input compared to low heat sink. In very thick sections, the opposite is true; the problem is to avoid excessive drag. All the input variables controlled by the operator (size and type of cutting tip, preheat flames, oxygen flow, and cutting speed) can be used to control drag.

Oxygen purity, as well as the alloy content of the steel being cut, affects the chemical reaction in oxyfuel gas cutting. Oxygen purity also affects combustion heat. The oxygen supplied from cylinders for oxyfuel gas cutting is usually at least 99.5% pure. A 0.5% departure from this purity (99% O_2) decreases the cutting efficiency. At 90% purity, cutting is very difficult, and at lower purities it is often impossible. The impurities consist of inert gases and water vapor. The effective purity of oxygen can also be reduced by gaseous combustion products from the preheat flames and from the metal being cut.

Alloying of iron affects oxyfuel gas cutting, usually by reducing the rate of oxidation. The total alloy content in low-alloy steel usually does not exceed 5%, and the effect on cutting speed is slight. Alloying elements affect oxyfuel gas cutting of steel in two ways. They may make the steel more difficult to cut, or they harden the cut edge, or both. In highly alloyed steel, the oxidizing characteristics of alloying elements and the constituents formed in alloying may make sustained oxidation difficult or even impossible. The effects of alloying elements on cutting are evaluated in Table 1. In any steel, preheat accelerates the chemical reaction; higher alloy steels, therefore, may need preheating beyond that provided by the preheat flames of the gas torch to promote cutting.

Table 1 Effects of alloying elements on resistance of steel to oxyfuel cutting

Element	Effect on oxyfuel cutting
Aluminum	**Extensively used as a deoxidizer in steelmaking; has no appreciable effect on oxygen cutting unless present in amounts above 8 to 10%; above this percentage, plasma arc cutting or metal powder cutting should be used**

Carbon	**Steels containing up to 0.25% C can readily be flame cut; higher-carbon steels should be preheated to prevent hardening and cracking; graphitic carbon makes flame cutting of cast iron difficult; cast iron containing up to 4% C can be flame cut when a powder, flux, or filler rod is used as a supplemental oxidizing agent**
Chromium	**Steels containing up to 5% Cr can be flame cut without difficulty; steels with chromium content of 10% or more require metal powder, chemical flux, or plasma arc cutting**
Cobalt	**When present in the amounts normally used in steelmaking, cobalt has no noticeable effect on flame cutting**
Copper	**Up to 3% Cu has no effect on flame cutting**
Manganese	**Has no effect on flame cutting of carbon steels; steel containing 14% Mn and 1.5% C are difficult to cut and must be preheated**
Molybdenum	**Steels with up to 5% Mo can be cut easily; this is true of AISI 41XX steels; high molybdenum-tungsten steels require metal powder or plasma arc cutting**
Nickel	**Steels with up to 3% Ni and less than 0.25% C may be readily cut by OFC; up to 7% Ni requires flux additions to the oxygen stream; stainless steels, from 18-8 to 35-15 types, require chemical flux, metal powder, or plasma arc cutting**
Phosphorus	**The amount usually found in steel has no effect on flame cutting**
Silicon	**No effect in steels with up to 4% Si; in higher-silicon steels with high carbon and manganese contents, preheating and postannealing are usually needed to avoid hardening and cracking**
Sulfur	**Amounts usually found in steel have no effect; higher sulfur content slows cutting speed and emits sulfur dioxide fumes**
Tungsten	**Steels containing up to 14% W are readily flame cut, but cutting is more difficult with a higher percentage; high red-hardness tungsten steels are difficult to flame cut and require preheating**
Vanadium	**The amounts normally found in steel do not interfere with flame cutting**

Preheating may consist of merely warming a cold workpiece with a torch or it may require furnace heating of the work beyond 540 °C (1000 °F). For some alloy steels, preheat temperatures are 200 to 315 °C (400 to 600 °F). Carbon steel billets and other sections occasionally are cut at 870 °C (1600 °F) and higher.

In oxyfuel gas cutting, preheating is accomplished by means of the oxyfuel gas flame, which surrounds the cutting oxygen stream. At cut initiation, the preheat flame, the result of oxygen and fuel gas combustion, brings a small amount of material to ignition temperature so that combustion can proceed. After cutting begins, the preheat flame merely adds heat to compensate for heat lost by convection and radiation or through gas exhausted during cutting. The flame also helps to remove or burn off scale and dirt on the plate surface; the hot, combusted gases protect the stream of cutting oxygen from the atmosphere.

Preheating may also be applied over a broader area of the work. It may include soaking the entire workpiece in a furnace to bring it up to 100 to 200 °C (200 to 400 °F), or a simple overall warm-up with a torch to bring cold plate to room temperature. A preheat significantly improves cutting speed, allowing faster torch travel for greater productivity and reduced consumption of fuel gas. Broader preheat smooths the temperature gradient between the base metal and the cut edge, possibly reducing thermal stress and minimizing hardening effects in some steels.

Combustion of Gases

Each cutting job entails a different type or volume of work to be completed. Consequently, the best gas for all cutting in a fabricating plant is found through experimentation. Evaluating a gas for a single job requires a test run that monitors fuel gas and oxygen flow rate, labor costs, overhead, and the amount of work performed. If plant production varies from week to week, gas performance should be measured over a long enough period to achieve an accurate cost analysis. Any of the fuel gases may perform well over a range of flow rates. When comparing gases, performance should be rated at the lowest flow rate that gives acceptable results for each gas. The most important preheat fuel gases are acetylene, natural gas, propane, propylene, and Mapp. Their properties are given in Table 2. These gases are hydrocarbons, which give off carbon dioxide and water vapor as the products of complete combustion. Flames of hydrocarbon gases are complex, displaying successive cones as a result of stepped chemical reactions. With acetylene, the products of complete combustion cannot exist at the temperature of the inner cone. Combustion is completed in the cooler, outer sheath of the flame. Chemical equations for combustion reactions of hydrocarbon gases often are simplified by treating the reactions as though the products were formed in only one step.

Table 2 Properties of common fuel gases

	Acetylene	**Propane**	**Propylene**	**Methylacetylene-propadiene (Mapp)**	Natural gas
Chemical formula	C_2H_2	C_3H_8	C_3H_6	C_3H_4 (Methylacetylene, propadiene)	**CH_4 (Methane)**
Neutral flame temperature					
°F	5,600	4,580	5,200	5,200	**4,600**
°C	3,100	2,520	2,870	2,870	**2,540**
Primary flame heat emission					
Btu/ft^3	507	255	433	517	**11**
MJ/m^3	19	10	16	20	**0.4**
Secondary flame heat emission					
Btu/ft^3	963	2,243	1,938	1,889	**989**
MJ/m^3	36	94	72	70	**37**
Total heat value (after vaporization)					
Btu/ft^3	1,470	2,498	2,371	2,406	**1,000**

MJ/m^3	55	104	88	90	**37**
Total heat value (after vaporization)					
Btu/lb	21,500	21,800	21,100	21,000	**23,900**
kJ/kg	50,000	51,000	49,000	49,000	**56,000**
Total oxygen required (neutral flame)					
vol O_2/vol fuel	2.5	5.0	4.5	4.0	**2.0**
Oxygen supplied through torch (neutral flame)					
vol O_2/vol fuel	1.1	3.5	2.6	2.5	**1.5**
ft^3oxygen/lb fuel (60 °F)	16.0	30.3	23.0	22.1	**35.4**
m^3oxygen/kg (15.6 °C)	1.0	1.9	1.4	1.4	**2.2**
Maximum allowable regulator pressure					
psi	15	Cylinder	Cylinder	Cylinder	**Line**
kPa	103				
Explosive limits in air, %	2.5-80	2.3-9.5	2.0-10	3.4-10.8	**5.3-14**
Volume-to-weight ratio					
ft^3/lb (60 °F)	14.6	8.66	8.9	8.85	**23.6**
m^3/kg (15.6 °C)	0.91	0.54	0.55	0.55	**1.4**
Specific gravity of gas (60 °F, 15.6 °C)					
Air = 1	**0.906**	**1.52**	**1.48**	**1.48**	**0.62**

Source: American Welding Society

Acetylene (C_2H_2) combustion produces a hot, short flame with a bright inner cone at each cutting-tip port; the hottest point is at the tip of this inner cone. Combustion starts in the inner cone and is brought to completion in a cooler, blue, outer flame. The sharp distinction between the two flames helps to adjust the ratio of oxygen to acetylene.

Depending on this ratio, the flame may be carburizing (reducing), neutral, or oxidizing. A neutral flame results when just enough oxygen is supplied for primary combustion, yielding carbon monoxide (CO) and hydrogen (H_2). These products then combine with oxygen in ambient air to form the blue, outer flame, yielding carbon dioxide (CO_2) and water (H_2O). The neutral ratio of oxygen to acetylene is about 1 to 1, and the flame temperature at the tip of the inner cone is about 3040 °C (5500 °F). This flame is used for manual cutting.

When the oxygen-to-acetylene ratio is reduced to about 0.9 to 1, a bright streamer begins to appear, and the flame becomes carburizing, or reducing. A carburizing flame is sometimes used for rough cutting of cast iron.

When the oxygen-to-acetylene ratio is increased to more than 1 to 1, the inner cones are shorter, "necked in" at the sides, and more sharply defined; this flame is oxidizing. Flame temperature increases until, at a ratio of about 1.7 to 1, the temperature is maximum, or somewhat over 3095 °C (5600 °F) at the tip of the cones. An oxidizing flame can be used for preheating at the start of the cut, and for cutting very thick sections.

According to the equation:

$$2C_2H_2 + 5O_2 \rightarrow 4CO_2 + 2H_2O$$

an oxygen-to-acetylene ratio of 2.5 to 1 is required for a complete reaction. For complete combustion, however, as much as 1.5 parts of oxygen is taken from ambient air. In oxyacetylene cutting, part of this oxygen may be supplied from the cutting oxygen, but total oxygen consumption is relatively low, an advantage of acetylene over all other fuel gases. Operation of oxyacetylene equipment in confined spaces, such as the inside of a closed tank or vessel, requires forced ventilation to supply the additional air needed for breathing and for flame combustion.

Acetylene must be used at pressures below 105 kPa (15 psi), which is a stable operating range. Safety codes specify equipment and handling practices for acetylene. When supplied in special cylinders, acetylene is dissolved in acetone, which is contained in a porous mass that fills the cylinder. This technique eliminates the sensitivity of acetylene at pressures over 105 kPa (15 psi). Such cylinders can be filled to pressures exceeding 105 kPa (15 psi), but not greater than 1725 kPa (250 psi). Acetylene may also be supplied from generators. With either means of supply, safety regulations must be observed to avoid sudden decomposition and explosion.

Despite some disadvantages, acetylene has been used for cutting for a longer time than any other gas. Its performance is well understood, equipment for it is perfected and widely marketed, and it is readily available. It has become the standard against which other gases are compared.

Natural gas is a mixture of gases, but consists principally of methane, and therefore is usually given the chemical symbol for methane (CH_4). One source defines the most widely used mixture as 85% methane (CH_4), 4% ethane (C_2H_6), and 11% (N_2, H_2, O_2, H_2O). Some wells produce natural gas with large proportions of ethane and propane.

The chemical equation for complete combustion:

$$CH_4 + 2O_2 \rightarrow CO_2 + 2H_2O$$

indicates an oxygen-to-methane ratio of 2 to 1; this ratio is used for the preheat flame. Maximum flame temperature at the tip of the inner cones is about 2760 °C (5000 °F). Both higher and lower temperatures have been reported; also, the optimum oxygen-to-gas ratio is about 2 to 1. The flame is more diffuse than with acetylene; heat intensity is lower; and adjustment for carburizing, neutral, and oxidizing flame is less clearly defined. Initial cutting speeds are slower, and oxygen consumption is greater. Also, more time is required for preheating with natural gas than with acetylene. An excess of oxygen shortens preheat time, but increases consumption of oxygen. Furthermore, natural gas cannot be used for welding of steel, so extra installations are needed if this operation is to be performed.

Despite these disadvantages, the use of natural gas for cutting has increased. It is the lowest-cost commercial fuel gas and, with careful torch adjustment, produces excellent cuts in light-to-heavy-gage material.

Neither acetylene nor natural gas accumulates in low pockets. When burned alone in air, the flame of natural gas does not produce soot.

Propane (C_3H_8) is a petroleum-base fuel usually supplied as a liquid in storage tanks from which it is drawn off as a gas. The gas is dispensed from bulk storage tanks through pipelines. It has a narrow range of flammability and is relatively stable, but is heavier than air. Complete combustion requires an oxygen-to-propane ratio of 5 to 1. However, about 30% of the oxygen needed is taken from the ambient air. When the ratio of oxygen to propane is 4.5 to 1, the flame temperature is about 2760 °C (5000 °F) at the tip of the inner cones. At 4.25 to 1, the flame temperature is about 2650 °C (4800 °F). Flame properties are similar to those of natural gas, with respect to diffuseness, heat intensity, flame adjustment, and cutting speed. When burned alone in air, the flame is soot-free.

Propylene is a liquefied gas similar to propane. It has a higher flame temperature than propane. The flame temperature of propylene is about equal to Mapp gas, although its heat content is slightly less. On a volume basis, propylene is usually less expensive than acetylene; it does, however, consume more oxygen during combustion. The combustion equation for propylene is:

$$2C_3H_6 + 9O_2 \rightarrow 6CO_2 + 6H_2O$$

The combustion ratio for propylene is 4.5 to 1. Line oxygen for a neutral flame is about 3.5 to 1. Distributors sell propylene under various trade names, either pure or as improved mixtures with propane and other hydrocarbon additives.

Mapp gas (stabilized methylacetylene-propadiene) is a proprietary gas mixture; it is shipped and stored as a liquid, either in bulk storage tanks or in portable cylinders.

Both methylacetylene and propadiene have the chemical symbol C_3H_4 and by themselves are unstable, giving off their heat of formation during decomposition. As with acetylene, this heat is in addition to the heat of combustion. However, the methylacetylene-propadiene mixture in Mapp gas is stabilized by the addition of other hydrocarbons. The composition of Mapp gas is not disclosed, so the chemical equation for complete combustion in oxygen is not given. However, when the flame is neutral, the ratio of oxygen to fuel gas is about 2.3 to 1; the normal operating ratio for cutting varies from 2.5 to 1 to 4 to 1, depending on speed and thickness. Maximum flame temperature at the tips of the inner cones, reported as 2925 °C (5300 °F), occurs at oxygen-to-fuel ratios from 3.5 to 1 to 4 to 1. Flames can be adjusted for carburizing, neutral, or oxidizing conditions.

Mapp gas is heavier than air, but it has a strong odor to reveal its presence in case it leaks or has collected in low pockets. At low temperatures, Mapp gas withdrawal rates from the cylinder are reduced. At about 0 °C (32 °F), methylacetylene has a vapor pressure of only 14 kPa (2 psi).

Effect of Oxyfuel Cutting on Base Metal

During the cutting of steel, the temperature of a narrow zone adjacent to the cut face is raised considerably above the transformation range. As the cut progresses, the steel cools through this range. The cooling rate depends on the heat conductivity and mass of the surrounding material, on loss of heat by radiation and convection, and on speed of cutting. When steel is at room temperature, the rate of cooling at the cut is sufficient to produce a quenching effect on the cut edges, particularly in heavier cuts in large masses of cold metal. Depending on the amount of carbon and alloying elements present and on the rate of cooling, pearlitic steel transforms into structures ranging from spheroidized carbides in ferrite to harder constituents. The heat-affected zone (HAZ) may be 0.8 to 6.4 mm ($\frac{1}{32}$ to $\frac{1}{4}$ in.) deep for steels 9.5 to 150 mm ($\frac{3}{8}$ to 6 in.) thick. Approximate depths of the HAZ in oxyfuel gas cut carbon steels are given in Table 3. Some increase in hardness usually occurs at the outer margin of the HAZ of nearly all steels.

Table 3 Approximate depths of HAZ in gas-cut carbon steels

Plate thickness		HAZ depth	
mm	**in.**	**mm**	in.

Low-carbon steels			
<13	$<\frac{1}{2}$	<0.8	$<\frac{1}{32}$
13	$\frac{1}{2}$	0.8	$\frac{1}{32}$
150	6	1.4	$\frac{1}{18}$
High-carbon steels			
<13	$<\frac{1}{2}$	<0.8	$<\frac{1}{32}$
13	$\frac{1}{2}$	0.8-1.6	$\frac{1}{32}-\frac{1}{16}$
150	**6**	**1.4-6.0**	$\frac{1}{18}-\frac{1}{4}$

Note: The depth of the fully hardened zone is considerably less than the depth of the HAZ. For most applications of gas cutting, the affected metal does not have to be removed.

Low-Carbon Steel. For steels containing 0.25% C or less, cut at room temperature, the hardening effect is usually negligible, although at the upper carbon limit it may be significant if subsequent machining is required. Short of preheating or annealing the workpiece, hardening may be lessened by ensuring that the cutting flame is neutral to slightly oxidizing, the flame is burning cleanly, and the inner cones of the flame are at the correct height. By increasing the machining allowance slightly, the first cut usually can be made deep enough to penetrate below the hardened zone in most steels. Mechanical properties of low-carbon steels generally are not adversely affected by oxyfuel gas cutting.

Medium-Carbon Steels. Steels having carbon contents of 0.25 to 0.45% are affected only slightly by hardening caused by oxyfuel gas cutting. Up to 0.30% C, steels with very low alloy content show some hardening of the cut edges, but generally not enough to cause cracking. Over 0.35% C, preheating to 260 to 315 °C (500 to 600 °F) is needed to avoid cracking. All medium-carbon steels should be preheated if the gas cut edges are to be machined.

High-Carbon and Alloy Steels. Gas cutting of higher-carbon (over 0.45% C) and hardenable alloy steels at room temperature may produce, on the cut surface, a thin layer of hard, brittle material that is susceptible to cracking from the stress of cooling. The cooling stress that causes cracking is similar to the stress that causes distortion.

Microcracks, or even incipient cracks, can be dangerous, because in service under tension they can develop into large fractures. The problems of hardening and the formation of residual stress can be alleviated by preheating and annealing.

Preheating serves three purposes. It:

- Reduces the temperature gradient near the cut during cutting. This lowers differential expansion, which may cause distortion or upsetting of the metal. Metal upset during the heating cycle can produce excessive stress in cooling
- Increases the cutting speed and improves the surface of the cut, especially in heavier sections and in the difficult-to-cut steels
- Reduces the cooling rate in the annealing range for the heat-affected portion of the cut during the cooling cycle.

By slower cooling, more ductile microstructures are obtained, and the formation of the hard martensitic structures is suppressed

If the higher-carbon and alloy steels are adequately preheated (and, in certain instances, annealed afterward), no cracks will occur. Ordinarily, a preheat temperature of 260 to 315 °C (500 to 600 °F) is sufficient for high-carbon steels; alloy steels may require preheating as high as 540 °C (1000 °F). Preheat temperature should be maintained during cutting. Thick preheated sections should be cut as soon as possible after the piece has been withdrawn from the furnace.

Local preheating involves heating that area of the workpiece that encloses what will become the HAZ of the cut. If the area to be heated is small and the section is not too thick, the preheating flame of a cutting torch may be used, but usually a special heating torch is required.

Local preheating is used when it is impossible or impractical to preheat the entire workpiece. It is important to heat the workpiece uniformly through the section to be cut, without causing too steep a temperature gradient. A multiflame heating torch is sometimes mounted ahead of the cutting torch in machine-guided cutting. Local preheating also can be accomplished using a preheat adaptor.

Annealing serves two main purposes in controlling the effects of gas cutting in carbon and low-alloy steels. It restores the original structure of the steel, whether it be predominantly pearlitic or predominantly ferritic with spheroidized carbide, and it also provides stress relief. Many steels do not require annealing if they have been properly preheated. (See *Heat Treating,* Volume 4 of the *ASM Handbook*, for annealing practices for specific steels.)

Local annealing, also called flame annealing, is a localized postheat treatment that can be used to prevent hardening or to soften an already hardened cut surface. Either the preheating flame of the cutting torch or a special heating torch may be used for local annealing, depending on the mass of the workpiece and the area to be covered. The heat-affected portion of the workpiece should be heated uniformly, and the temperature gradient at the boundary of the heated mass should be gradual enough to avoid distortion of the workpiece.

Local annealing is not a substitute for preheating; it cannot correct damage done during cutting, such as upsetting of the metal or cracking at the cut edges. Local annealing is limited to steel plate up to 40 mm ($1\frac{1}{2}$ in.) thick. From 40 to 75 mm ($1\frac{1}{2}$ to 3 in.) thick, heat should be applied to both sides of the plate. This method is not suitable for thicknesses over 75 mm (3 in.). If local annealing cannot be done simultaneously with cutting, the cut edges should be tempered after cutting with a suitable heating torch.

Stainless steels do not support oxyfuel combustion and therefore require metal powder cutting, chemical flux cutting, or plasma arc cutting processes. Except for stabilized types, stainless steels degrade under the heat of metal powder or chemical flux processes. Carbide precipitation occurs in the HAZ about 3 mm ($\frac{1}{8}$ in.) from the edge, where the metal has been heated to 425 to 870 °C (800 to 1600 °F) long enough for dissolved carbon to migrate to the grain boundaries and combine with the chromium to form chromium carbide. The chromium-poor (sensitized) regions near grain boundaries are subject to corrosion in service. This type of corrosion can be prevented by a stabilizing anneal, which puts the carbon back into solution. However, the required quench through the sensitizing temperature range may distort the material.

Water quenching of the cut edge directly behind the cutting torch may avert sensitization. Because it takes about 2 min at sensitizing temperature for carbide precipitation to occur, water quenching must be done immediately. Distortion is more likely with this method than with the stabilizing anneal. Still another procedure is to remove the sensitizing zone entirely by chipping or machining.

Distortion, which is the result of heating by the gas flame, can cause considerable damage during cutting of thin plate (<8 mm, or $\frac{5}{16}$ in., thick), cutting of long narrow widths, close-tolerance profile cutting, and cutting of plates that contain high residual stresses. The heat may release some of the locked-in stress, or may add new stress. In either case, deformation (warpage) may occur, thereby causing inaccurate finished cuts. Plates in the annealed condition have little or no residual stress.

Deformation. In cuts made from large plates, the cutting thermal cycle changes the shape of narrow sections and leaves residual stress in the large section (see Fig. 2). The temperature gradient near the cut is steep, ranging from melting point at the cut to room temperature a short distance from it. The plate does not return to its original shape unless the entire plate is uniformly heated and cooled.

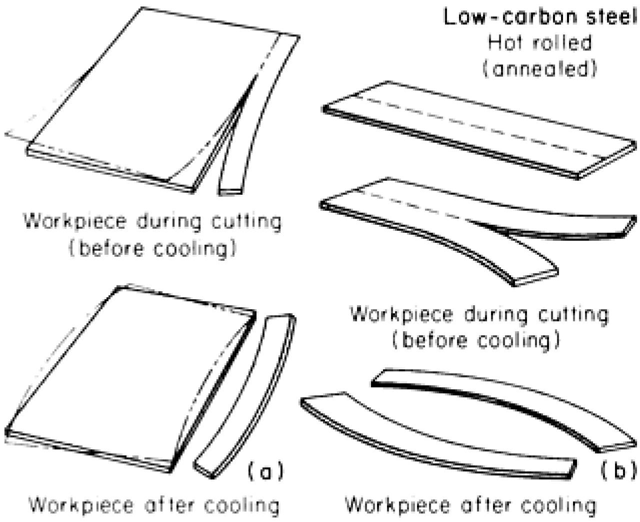

Fig. 2 Effects of oxyfuel gas cutting thermal cycle on shape of sections. (a) Plate with large restraint on one side of kerf, little restraint on the other side. Phantom lines indicate direction of residual stress that would cause deformation except for restraint. (b) Plate with little restraint on either side.

As the metal heats, it expands, and its yield strength decreases; the weakened heated material is compressed by the surrounding cooler, stronger metal. The hotter metal continues to expand elastically in all directions until its compressive yield strength is reached, at which point it yields plastically (upsets) in directions not under restraint. The portion of this upset metal at about 870 °C (1600 °F) is virtually stress-free; the remainder is under compressive stress that is equal to its yield strength. Metal that expands but does not upset is under compressive strength below yield. The net stress on the heated side of the neutral axis causes bowing of a narrow plate during cutting, as shown in Fig. 2.

As the heated metal begins to cool, it contracts, and its strength increases. First, the contraction reduces the compressive stress in the still-expanded metal. When the compressive stress reaches zero and the plate regains its original shape, previously upset metal also has regained strength. This metal is now in tension as it cools, and its tensile yield strength increases. Tension increases until the metal reaches room temperature. Residual tensile stress in the cooling side of the neutral axis causes the bowing of narrow plates after cooling (Fig. 2). Controlled upsetting is the basis of flame straightening.

Control of Distortion. Preheating the workpiece can reduce distortion by reducing differential expansion, thereby decreasing stress gradients. Careful planning of the cutting sequence also may help. For example, when trimming opposite sides of a plate, both sides should be cut in the same direction at the same time. When cutting rings, the inside diameter should be cut first; the remaining plate restrains the material for the outside-diameter cut. In general, the larger portion of material should be used to retain a shape for as long as possible; the cutting sequence should be balanced to maintain even-heat input and resultant residual stresses about the neutral axis of the plate or part.

Equipment

Commercial gases are usually stored in high-pressure cylinders. Natural gas--primarily methane--is supplied by pipeline from gas wells. The user taps into local gas lines. Acetylene, dissolved in acetone, is available in clay-filled cylinders. High-volume users often have acetylene generators on site. For heavy consumption or when many welding and cutting stations use fuel gas, banks of gas cylinders are maintained at a central location in the plant, and the gas is manifolded and piped to the point of use.

Manual gas cutting equipment consists of gas regulators, gas hoses, cutting torches, cutting tips, storage tanks, reverse flow check valves, and flashback arrestors. Auxiliary equipment may include a hand truck, tip cleaners, torch ignitors, and protective goggles. Machine cutting equipment varies from simple rail-mounted "bug" carriages to large bridge-mounted torches that are driven by computer-directed drives.

Gas regulators reduce gas pressure and moderate gas flow rate between the source of gas and its entry into the cutting torch to deliver gas to the cutting apparatus at the required operating pressure. Gas enters the regulating device at a wide range of pressures. Gas flows through the regulator and is delivered to the hose-torch-tip system at the operating pressure, which is preset by manual adjustment at the regulator and at the torch. When pressure at the regulator drops below the preset pressure, regulator valves open to restore pressure to the required level. During cutting, the regulator maintains pressure within a narrow range of the pressure setting.

Regulators should be selected for use with specific types of gas and for specific pressure ranges. Portable oxyacetylene equipment requires an oxygen regulator on the oxygen cylinder and an acetylene regulator on the acetylene cylinder, which are not interchangeable.

High-low regulators conserve preheat oxygen when natural gas or propane is the preheat fuel used in oxyfuel gas cutting. These gases require a longer time to start a cut than do acetylene or Mapp gas. High-low regulators reduce preheat flow to a predetermined level when the flow of cutting oxygen is initiated. When the regulator switches from high to low, preheat cutback may range from 75 to 25% as plate thicknesses increase from 9.5 to 200 mm ($\frac{3}{8}$ to 8 in.). High-low regulators are used for manual and automatic cutting with natural gas propane and liquefied petroleum gas (LPG).

Hose. Flexible hose, usually 3 to 13 mm ($\frac{1}{8}$ to $\frac{1}{2}$ in.) in diameter, rated at 1380 kPa (200 psig) maximum, carries gas from the regulator to the cutting torch. Oxygen hoses are green; the fittings have right-hand threads. Fuel gas hoses are red; the fittings have left-hand threads and a groove cut around the fitting. For heavy cutting, two oxygen hoses may be necessary, one for preheat and one for cutting oxygen. Multiple-torch cutting machines often have three-hose torches.

Cutting torches, such as the one shown in Fig. 3, control the mixture and flow of preheat oxygen and fuel gas and the flow of cutting oxygen. The cutting torch discharges these gases through a cutting tip at the proper velocity and flow rate. Pressure of the gases at the torch inlets, as well as size and design of the cutting tip, limits these functions, which are operator controlled.

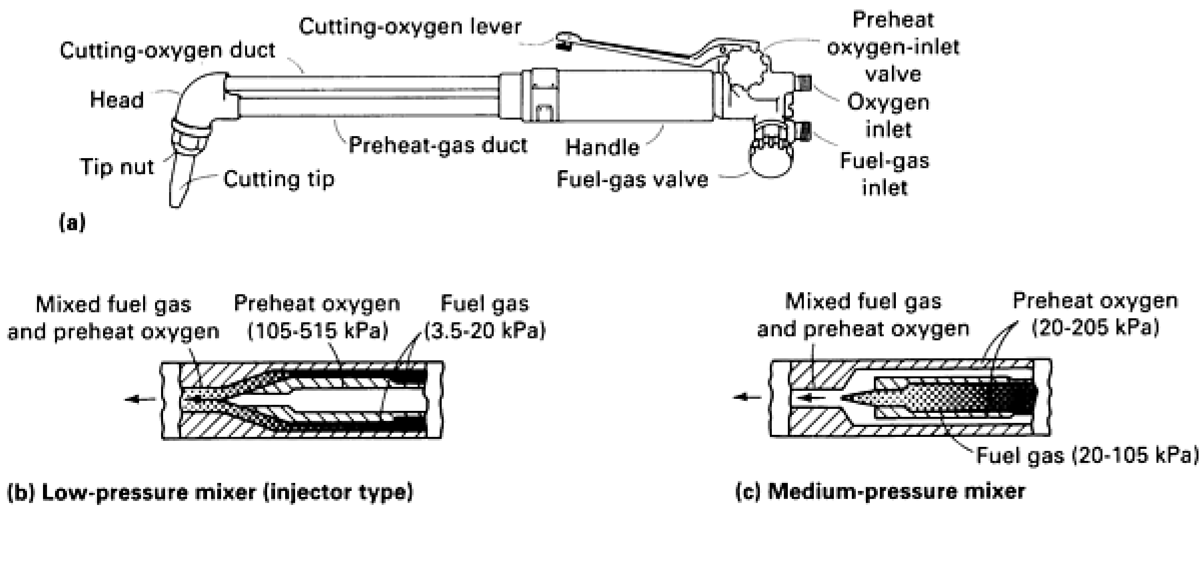

Fig. 3 (a) Typical manual cutting torch in which preheat gases are mixed before entering torch head. (b) and (c) Sections through preheat gas duct showing two types of mixers commonly used with the torch shown. After the workpiece is sufficiently preheated, the operator depresses the lever to start the flow of cutting oxygen. Valves control the flow of oxygen and fuel gas to achieve the required flow and mixture at the cutting tip.

Oxygen inlet control valves and fuel gas inlet control valves permit operator adjustment of gas flow. Fuel gas flows through a duct and mixes with the preheat oxygen; the mixed gases then flow to the preheating flame orifices in the cutting tip. The oxygen flow is divided: A portion of the flow mixes with the fuel gas, and the remainder flows through the cutting-oxygen orifice in the cutting tip. A lever-actuated valve on the manual torch starts the flow of cutting oxygen; machine cutting starts the oxygen from a panel control.

Fuel gases supplied at low pressure (usually below 21 kPa, or 3 psi), such as natural gas tapped from a city line, require an injector-mixer (Fig. 3b) to increase fuel gas flow above normal operating pressures. Optimum torch performance relies on proper matching of the mixer to the available fuel gas pressure.

Cutting tips are precision-machined nozzles, produced in a range of sizes and types. Figure 4(a) shows a single-piece acetylene cutting tip. A two-piece tip used for natural gas (methane) or LPG is shown in Fig. 4(b). A tip nut holds the tip in the torch. For a given type of cutting tip, the diameters of the central hole, the cutting-oxygen orifice, and the preheat ports increase with the thickness of the metal to be cut. Cutting tip selection should match the fuel gas; hole diameters must be balanced to ensure an adequate preheat-to-cutting-oxygen ratio. Preheat gas flows through ports that surround the cutting-oxygen orifice. Smoothness of bore and accuracy of size and shape of the oxygen orifice are important to efficiency. Worn, dirty bores reduce cut quality by causing turbulence in the cutting-oxygen stream.

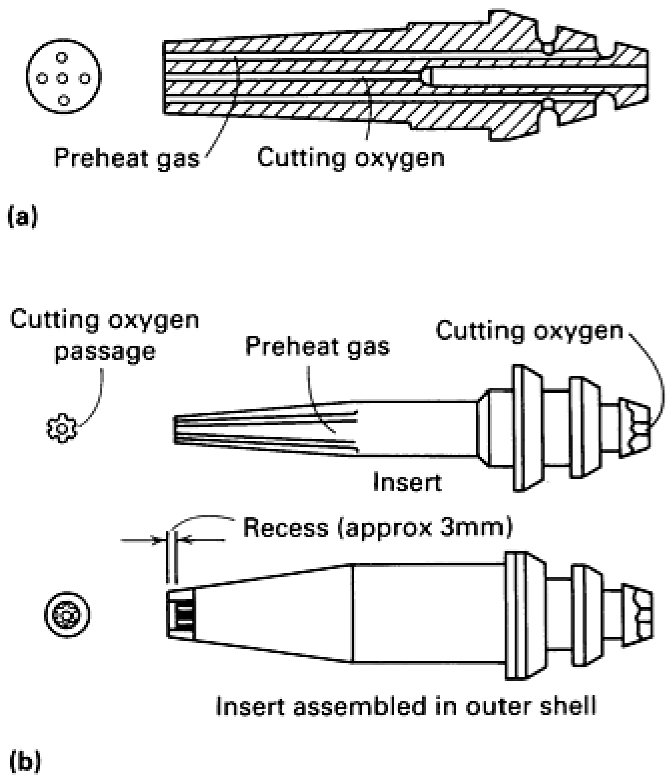

Fig. 4 Types of cutting tips. (a) Single-piece acetylene cutting tip. (b) Two-piece tip for natural gas or LPG. Fuel gas and preheat oxygen mix in tip. Recessed bore promotes laminar flow of gas and anchors the flame when natural gas or propane is used.

The size of the cutting-tip orifice determines the rate of flow and velocity of the preheat gases and cutting oxygen. Flow to the cutting tip can be varied by adjustment at the torch inlet valve or at the regulator, or both.

Increasing cutting-oxygen flow solely by increasing the oxygen pressure results in turbulence and reduces cutting efficiency. Turbulence in the cutting oxygen causes wide kerfs, slows cutting, increases oxygen consumption, and lowers quality of cut. Consequently, larger cutting tips are required for making heavier cuts.

Standard tips, as shown in Fig. 5(a), have a straight-bore oxygen port. Oxygen pressures range from 200 to 400 kPa (30 to 60 psi) and are used for manual cutting. High-speed tips, or divergent cutting tips (Fig. 5b), use a converging, diverging orifice to achieve high gas velocities. The oxygen orifice flares outward. High-speed tips operate at cutting-oxygen pressures of about 700 kPa (100 psi) and provide cutting jets of supersonic velocity. These tips are precision made and are more costly than straight-drilled tips, but they produce superior results: improved edge quality and cutting speeds 20%

higher than standard tips. Best suited to machine cutting, high-speed tips produce superior cuts in plate up to about 150 mm (6 in.) thick. Above this thickness, advantages of their use decrease, and they are not recommended for cutting metal more than 250 mm (10 in.) thick.

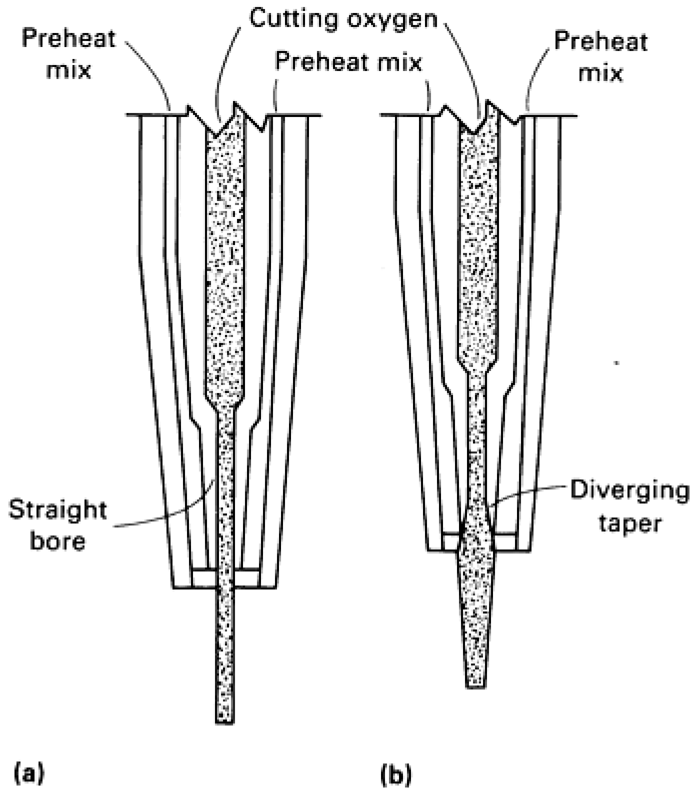

Fig. 5 Oxyfuel cutting tips. (a) Standard cutting tip with straight-bore oxygen orifice. (b) High-speed cutting tip with divergent-bore oxygen orifice.

Equipment Selection Factors. Natural gas and liquefied petroleum gases operate most efficently with high-low gas regulators; injector-type cutting torches; and two-piece, divergent, recessed cutting tips. Acetylene cutting is most efficient with divergent single-piece tips. If acetylene is supplied by low-pressure generators, an injector-type torch is ideally suited to most cutting applications.

Two-piece divergent cutting tips are best suited for use with Mapp gas; the tip recess should be less than one used for natural gas or propane. Injector-type torches and high-low regulators are not required with Mapp gas.

Guidance Equipment. In freehand cutting, the operator can usually follow a layout accurately at low speeds, but the cut edges may be ragged. For accurate manual cutting at speeds over 250 mm/min (10 in./min), the torch tip should be guided with a metal straightedge or template. Circles and arcs are cut smoothly with the aid of a radius bar, a light rod clamped and adjusted to the torch at one end, while the other end is held at the circle center.

Machine guidance equipment includes magnetic tracing of a metal template, manual spindle tracing, optical tracing of a line drawing, guidance by numerically controlled tape or by programmable controllers, and computer-programmed guidance equipment (Fig. 6).

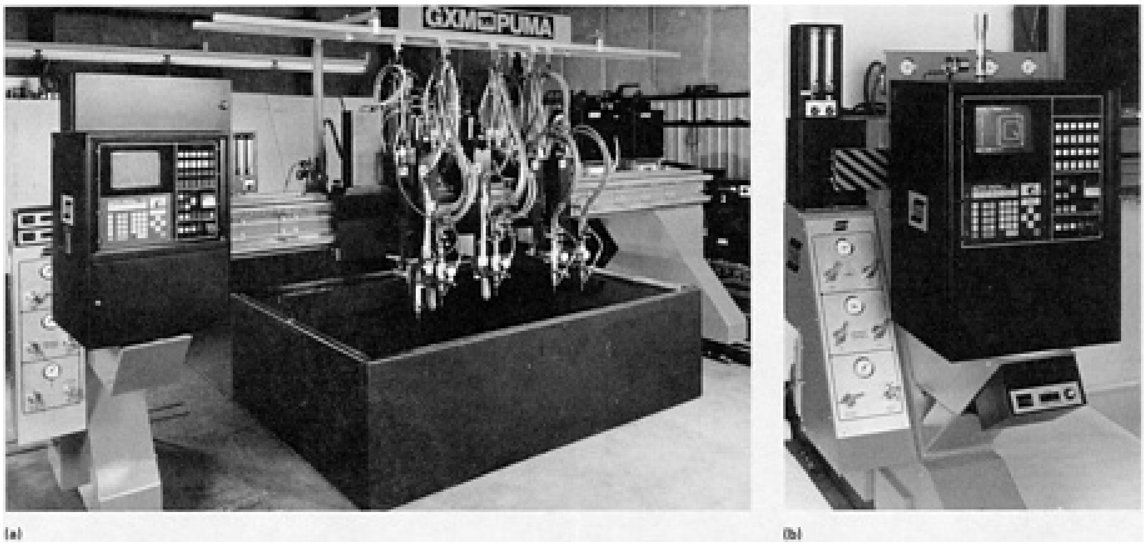

Fig. 6 Gantry shape cutting system. (a) CNC-controlled cutting tool incorporating oxyfuel torches, plasma arc torches, 90° indexing triple-torch oxyfuel stations for straight-line beveling, and zinc powder or punch markers. (b) Close-up of CNC control console. Courtesy of ESAB North America, Inc.

Portable cutting machines are used primarily for straight-line and circular cutting. Components include a torch mounted on a motor-driven carriage that travels on a track or other torch guidance device. The operator adjusts travel speed and monitors the operation.

Machine cutting torches are of heavy construction of in-line design. The torch casing has a rack, which fits into a gear on the torch holder, for raising and lowering the torch over the work. Ducts and valves are encased in a single tube. The cutting tip is mounted axially with the tube. A valve knob or a lever-operated poppet valve replaces the spring-loaded cutting-oxygen lever of the manual torch.

On some portable machines, gases are supplied to connections on the carriage, rather than directly to the torch to avoid hose drag on the torch. Short hoses are used from machine connections to the torch. Some carriages can accommodate two or more torches operating simultaneously, for such operations as squaring and beveling.

The operator follows the carriage to make adjustments. When plates are wavy or distorted, the operator may need to adjust torch height to avoid losing the cut. When carefully operated, track-guided torches can produce cuts at speeds and quality approaching those obtainable with stationary cutting machines.

Stationary cutting machines, as shown in Fig. 6 and 7, are used for straight-line and circular cuts, but their primary use is for cutting complex parts, that is, for cutting shapes. Plate to be cut is moved to the machine.

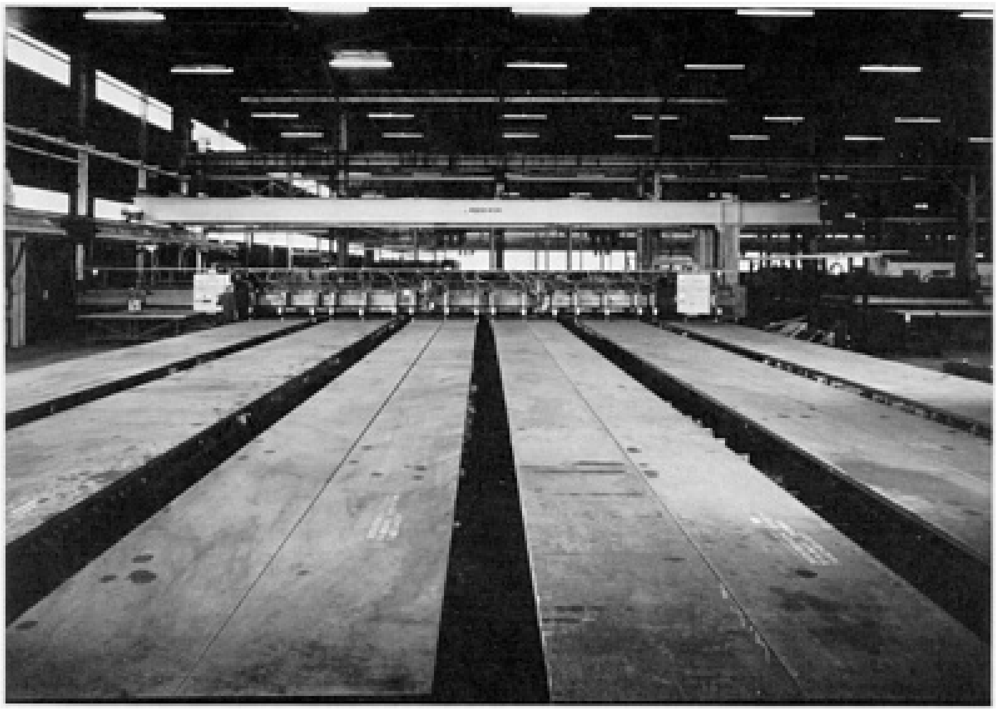

Fig. 7 Stationary oxyfuel gas cutting machine.

On shape-cutting machines, cutting torches move left and right on a bridge mounted over the cutting table. The bridge moves back and forth on supports that ride on floor-mounted tracks. The combined movement of the torches on the bridge and the bridge on the track allows the torch to cut any shape in the *x*-*y* plane. Bridges are either of cantilever or gantry design. Suppliers classify cutting-machine capacity by the maximum width of plate that can be cut.

Machine Directions. Methods for directing the motion of shape cutting machines have become increasingly sophisticated and include manual, magnetic, and electronic means of control. The simplest machines have one or two torches and use manual or magnetic tracing.

For manual tracing, the operator either steers an idler wheel or spindle around a template or guides a wheel or focused light beam around an outline on paper. Cutting speed is controlled by setting the speed of the tracing head (pantograph director) or by setting the speed of the torch carriage (coordinate drive). Cutting speed in manual tracing is about 350 mm/min (14 in./min), depending on operator skill.

Magnetic tracing is done with a knurled magnetized spindle that rotates against the edge of a steel template. The spindle is linked to a pantograph. Direct-reading tachometers, showing cutting speed in inches per minute, assist in adjusting cutting speed. These control methods are relatively slow.

Faster, electronic tracers use a photo-electric cell that scans the reflection of a beam of light directed on the outline of a template. Templates are line drawings on paper, white-on-black paper cutouts, or photonegatives of a part outline. To hold tolerances closer than $\frac{1}{16}$ in. continuously, templates of plastic film, glass cloth, or some other durable, dimensionally stable material should be used.

In scanning the edge of a white-on-black template, the circuit through the photoelectric cell balances when the cell senses an equal amount of black and white. A change in this balance sends an impulse to a motor that moves the tracing head back to balance. In line tracing, the photoelectric cell scans the line from side to side. As long as the light reflects equally from both sides of the line, the steering signals balance. When the photocell scans more light on one side of the line than on the other, the scanner rotates to balance.

Some machines adjust to permit parts to be cut about $\frac{1}{32}$ in. larger or smaller than the template. This feature, called kerf compensation, is useful for cutting to close tolerances, especially when the template has insufficient kerf allowance.

Coordinate-drive machines translate motion 1 to 1 or in other ratios. Such ratio cutting permits the use of templates in any proportion, from full-scale to one-tenth of part size.

Tape Control. Cutting machine movement may be controlled by electronic signals from punched tape (numerical control). These machines do not require templates, and the tape may be easily stored and used many times.

Some cutting machines receive directions from a microprocessor, programmed directly or from punched tape. The most sophisticated machines take directions from a computer (computerized numerical control, or CNC) and use computer graphics (Fig. 6).

Nesting of Shapes

Savings in material, labor, and gas consumption can be gained by nesting parts in the stock layout for single-torch or multiple-torch operation. Savings can be realized whenever one cut can be made instead of two. Sometimes a shape can be modified for better nesting. The advent of computer graphics allows cutting-machine programmers to create layouts of part patterns on cathode ray tube screens (Fig. 8), manipulating cutting patterns for greatest plate use. Several firms offer programs that closely optimize parts nesting.

Fig. 8 Parts programming system for nesting of shapes. Layouts of part patterns can be performed on-screen using such a system, resulting in optimum material use. Courtesy of ESAB North America, Inc.

Revised by Ed Craig, AGA Gas, Inc.

Metal Powder Cutting

Finely divided iron-rich powder suspended in a jet of moving air or dispensed by a vibratory device is directed into the gas flame in metal powder cutting. The iron powder passes through and is heated by the preheat flame so that it burns in the oxygen stream. Heat generated by the burning iron particles improves cutting action. Cuts can be made in stainless steel and cast iron at speeds only slightly lower than those used for equal thicknesses of carbon steel. By adding a small amount of aluminum powder, cuts can be made through copper and brass. For information on the types of metal powder used for cutting operations, see the article "Metal and Alloy Powders for Welding, Hardfacing, Brazing, and Soldering" in *Powder Metal Technologies and Applications*, Volume 7 of the *ASM Handbook.*

Equipment. In metal powder cutting, a gas torch with an external powder attachment (Fig. 9), or a torch with built-in powder passages, is used. A vibratory or pneumatic powder dispenser (Fig. 10), air supply, and powder hose are required, in addition to fuel and oxygen lines. The equipment may be used manually for removing metal, such as risers from castings, or mechanized for straight-line or shape cutting by machine. Powder cutting torches mounted on gas cutting machines are capable of cutting stainless steel.

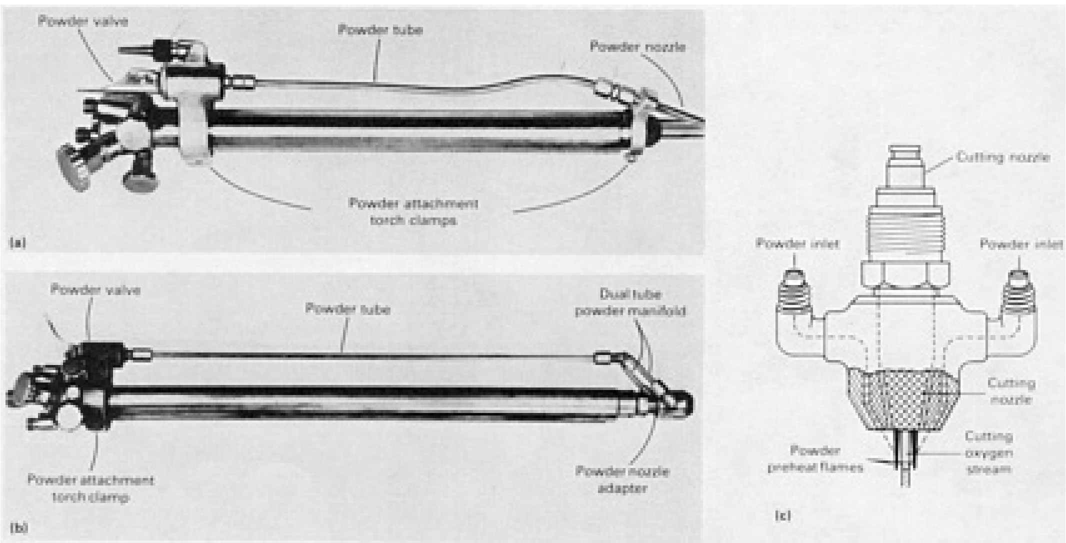

Fig. 9 Powder cutting attachments. (a) Single-tube attachment. (b) Multijet attachment. (c) Enlargement of powder nozzle adapter.

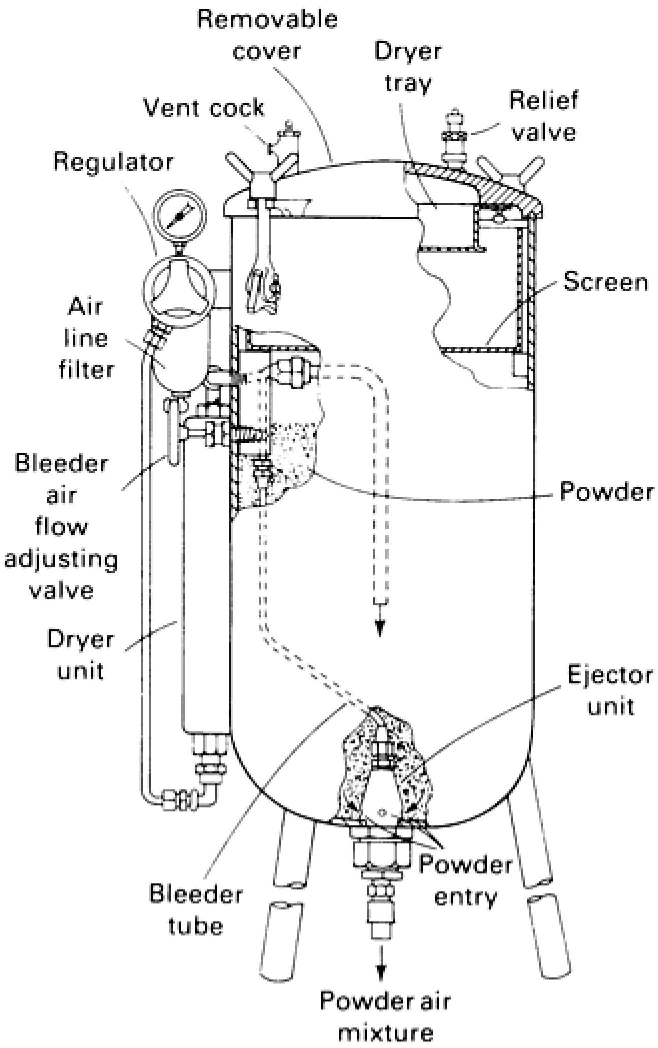

Fig. 10 Pneumatic powder dispenser for use in metal powder cutting.

Thickness. In plate 25 to 100 mm (1 to 4 in.) thick, powder cuts can be produced by machine to an accuracy of 0.8 to 1.6 mm ($\frac{1}{32}$ to $\frac{1}{16}$ in.). Heavier sections are seldom cut except for the trimming of castings; in this application, hand cutting requires greater allowances to avoid damage. Typical metal powder cutting applications include removal of risers; cutting of bars, plates, and slabs to size; and scrapping.

Quality of Cut. The kerf has a layer of scale which, on stainless steel, flakes off as the workpiece cools. The surface exposed after scale removal has the texture of sandpaper. Light grinding is normally sufficient to smooth high spots and remove iron particles and oxide. Unstabilized austenitic stainless steel may become sensitized by the heat of cutting. Powder cut cast iron develops a hardened case at the surface, which may require annealing or removal by grinding.

Thermal Cutting

Revised by Ed Craig, AGA Gas, Inc.

Chemical Flux Cutting

Chemical flux cutting processes are well suited to materials that form refractory oxides. Finely pulverized flux is injected into the cutting oxygen before it enters the cutting torch. The torch has separate ducts for preheat oxygen, fuel gas, and cutting oxygen. When the flux strikes the refractory oxides that are formed when the cutting oxygen is turned on, it reacts with them to form a slag of lower melting temperature compounds. This slag is driven out, enabling oxidation of the metal to proceed.

Chemical fluxing methods are used to cut stainless steel. The operator should have an approved respirator for protection from toxic fumes generated by the process.

Thermal Cutting

Revised by Ed Craig, AGA Gas, Inc.

Plasma Arc Cutting

Plasma arc cutting employs an extremely high-temperature, high-velocity, constricted arc between an electrode contained within the torch and the piece to be cut. The arc is concentrated by a nozzle onto a small area of the workpiece. The metal is continuously melted by the intense heat of the arc and then removed by the jetlike gas stream issuing from the torch nozzle. Because plasma arc cutting does not depend on a chemical reaction between the gas and the work metal, because the process relies on heat generated from an arc between the torch electrode and the workpiece, and because it generates very high temperatures (28,000 °C, or 50,000 °F, compared to 3000 °C, or 5500 °F, for oxyfuel), the transferred arc cutting mode can be used on almost any material that conducts electricity, including those that are resistant to oxyfuel gas cutting. Using the nontransferred arc method, nonmetallic objects such as rubber, plastic, styrofoam, and wood can be cut with a good quality surface to within 0.50 to 0.75 mm (0.020 to 0.030 in.) tolerances.

The past decade has seen a great increase in use of plasma arc cutting, because of its high cutting speed (Fig. 11). The process increases the productivity of cutting machines over oxyfuel gas cutting without increasing space or machinery requirements.

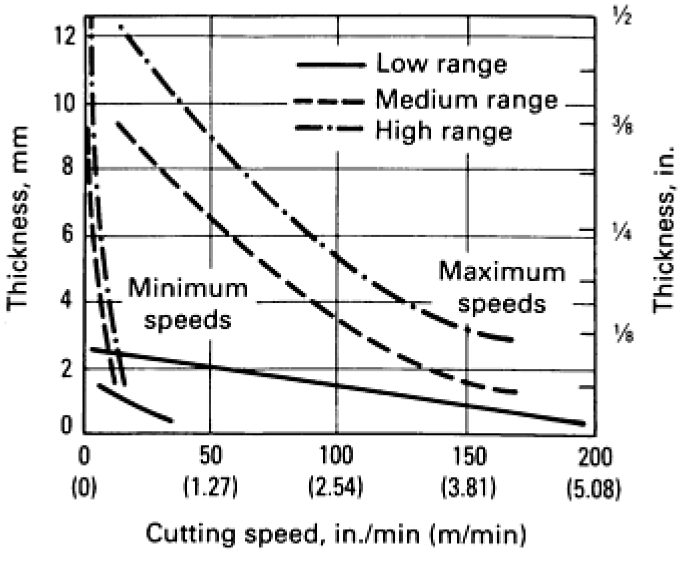

Fig. 11 Typical cutting speeds for plasma arc cutting of carbon steel or stainless using 6.8 m^3/h (240 ft^3/h) of air at 345 kPa (50 psi) from a single source. This information represents realistic expectations using recommended practices and well maintained systems. Other factors such as parts wear, air quality, line voltage fluctuations, and operator experience may also affect system performance.

Operating Principles and Parameters (Ref 1)

The basic plasma arc cutting torch is similar in design to that of a plasma arc welding torch. For welding, a plasma gas jet of low velocity is used to melt base and filler metals together in the joint (see the article "Plasma Arc Welding" in *Welding, Brazing, and Soldering,* Volume 6 of the *ASM Handbook*). For the cutting of metals, increased gas flows create a high-velocity plasma gas jet that is used to melt the metal and blow it away to form a kerf. The basic design and terminology for a plasma arc cutting torch are shown in Fig. 12.

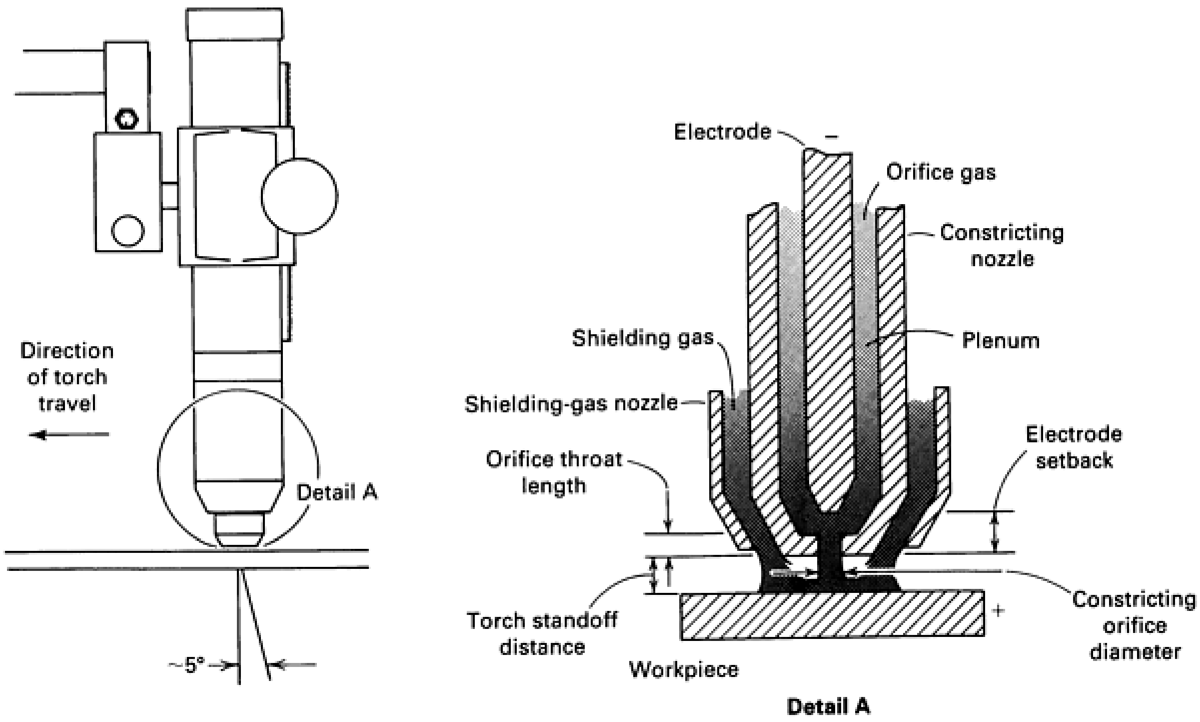

Fig. 12 Components of a plasma arc cutting torch.

All plasma arc torches constrict the arc by passing it through an orifice as it travels away from the electrode and toward the workpiece. As the orifice gas passes through the arc, it is heated rapidly to high temperature, expands, and accelerates as it passes through the constricting orifice. The intensity and velocity of the arc plasma gas are determined by such variables as the type of orifice gas and its entrance pressure, constricting orifice shape and diameter, and the plasma energy density on the work.

The basic plasma arc cutting circuitry is shown in Fig. 13. The process operates on direct current, straight polarity (dcsp), electrode negative, with a constricted transferred arc. In the transferred arc mode, an arc is struck between the electrode in the torch and the workpiece. The arc is initiated by a pilot arc between the electrode and the constricting nozzle. The nozzle is connected to ground (positive) through a current-limiting resistor and a pilot arc relay contact. The pilot arc is initiated by a high-frequency generator connected to the electrode and nozzle. The welding power supply then maintains this low current arc inside the torch. Ionized orifice gas from the pilot arc is blown through the constricting nozzle orifice. This forms a low-resistance path to ignite the main arc between the electrode and the workpiece. When the main arc ignites, the pilot arc relay may be opened automatically to avoid unnecessary heating of the constricting nozzle.

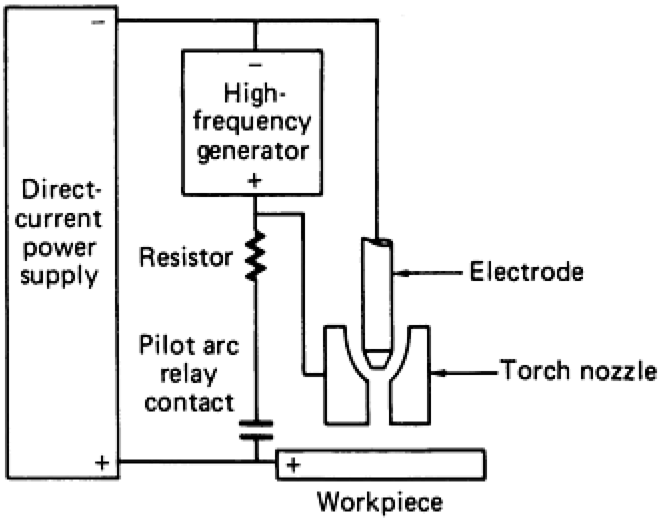

Fig. 13 Plasma arc cutting circuit. The process operates on direct current electrode negative (straight polarity). The arc is initiated by a pilot arc between the electrode and torch nozzle. Pilot arc is initiated by the high-frequency generator, which is connected to the electrode nozzle.

Plasma arc cutting was originally developed for severing nonferrous metals using inert gases. Modifications of the process and equipment to allow the use of oxygen or compressed air in the orifice gas permitted the cutting of carbon and alloy steel with improved cutting speeds and a cut quality similar to that obtained with oxyfuel cutting.

Because the plasma constricting nozzle is exposed to the high plasma flame temperatures (estimated at 10,000 to 14,000 °C, or 18,000 to 25,000 °F), the nozzle is sometimes made of water-cooled copper. In addition, the torch should be designed to produce a boundary layer of gas between the plasma and the nozzle.

Several process variations are used to improve the plasma arc cutting quality for particular applications. They are generally applicable to materials in the 3 to 38 mm ($\frac{1}{8}$ to $1\frac{1}{2}$ in.) thickness range, depending on the current rating of the plasma machine. Auxiliary shielding in the form of gas or water is used to improve cutting quality.

Dual-flow plasma cutting provides a secondary gas blanket around the arc plasma, as shown in Fig. 14. The usual orifice gas is nitrogen or compressed air. The shielding gas is selected for the material to be cut. It may be compressed air for mild steel, CO_2 for stainless steels, and an argon-hydrogen mixture for aluminum.

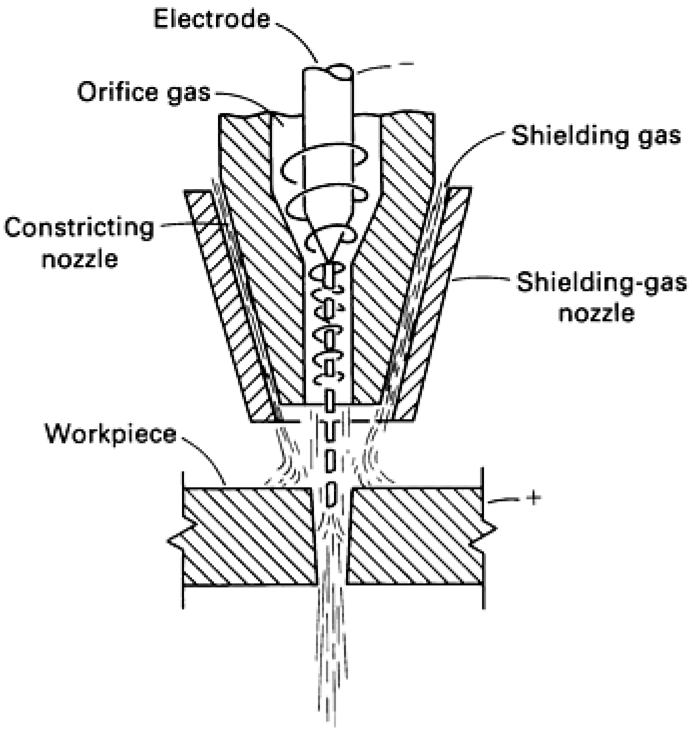

Fig. 14 Dual-flow plasma arc cutting.

Water Shield Plasma Cutting. This technique is similar to dual-flow plasma cutting. Water is used in place of the auxiliary shielding gas.

Water Injection Plasma Cutting. This modification of the plasma arc cutting process uses a symmetrical impinging water jet near the constricting nozzle orifice to further constrict the plasma flame. The arrangement is shown in Fig. 15. The waterjet also shields the plasma from turbulent mixing with the surrounding atmosphere.

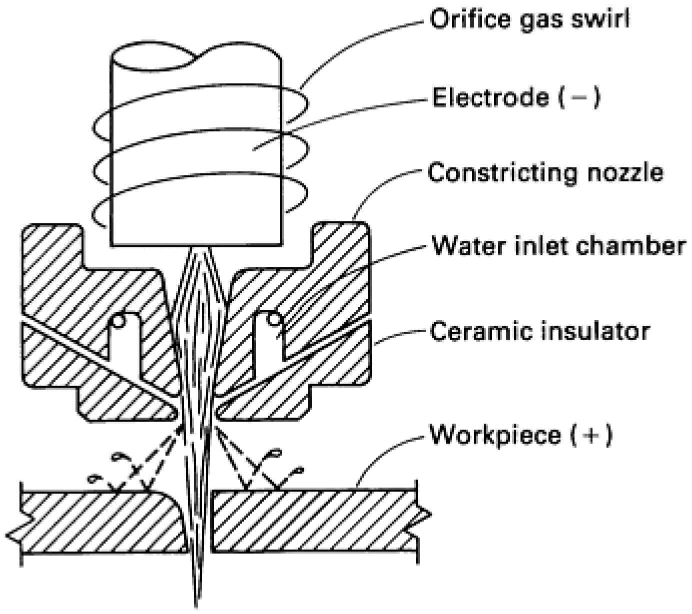

Fig. 15 Water injection plasma arc cutting arrangement.

Selection of Gas. Any gas or gas mixture that does not degrade the properties of the tungsten electrode or the workpiece can serve as a plasma gas. The gas mixture varies according to the plasma equipment design criteria. The most commonly used gas is compressed air; all common metals, such as carbon and alloy steels, stainless steels, and aluminum, can be cut with compressed air. As the metal thickness increases (over 25 mm, or 1 in., with steels and stainless steels), benefits are derived from the use of nitrogen plasma with CO_2 shielding. Aluminum cut quality is improved using argon-hydrogen plasma and nitrogen as the secondary gas blanket. When high-duty cycles are used, a change from compressed air to nitrogen/CO_2 prolongs the consumable life.

Machine Ratings. In selecting a plasma cutting unit, the thickness of plate to be cut and the required cutting speed should be considered. Table 4 shows thickness capacity and midrange cutting speeds of four plasma units. These ratings are an average of speeds quoted by two manufacturers of plasma arc cutting equipment.

Table 4 Cutting speed of plasma arc cutting machines for stainless steel

Machine rating, A	Cutting speed, m/min (in./min)							
	Plate thickness, mm (in.)							
	1.5 ($\frac{1}{16}$)	**3 ($\frac{1}{8}$)**	**6 ($\frac{1}{4}$)**	**9 ($\frac{3}{8}$)**	**13 ($\frac{1}{2}$)**	**25 (1.0)**	**50 (2.0)**	75 (3.0)
30	0.75-1.5 (30-60)	0.5-0.75 (20-30)	0.13-0.25 (5-10)	. . .	. . .	. . .	. . .	**. . .**
50	1.5-3.0 (60-120)	1.3-2.5 (50-100)	0.6-1.3 (25-50)	0.13-0.25 (5-10)	0.025-0.13 (1-5)	. . .	. . .	**. . .**
100	. . .	1.5-2.8 (60-110)	0.75-1.5 (30-60)	0.5-1.0 (20-40)	0.4-0.5 (15-20)	0.13-0.25 (5-10)	. . .	**. . .**
400	**. . .**	**3-4 (120-150)**	**4-4.3 (150-170)**	**3-3.5 (120-140)**	**2.5-3 (100-120)**	**1.0-1.5 (40-60)**	**0.25-0.5 (10-20)**	**0.08-0.2 (3-8)**

The thickness capacity of a cutting unit should first be examined to determine the cutting speed it can achieve for a given application. Next the speeds quoted for the next-larger unit should be studied to see whether the greater speed justifies its higher cost. For example, a 30 A unit cuts 6 mm ($\frac{1}{4}$ in.) stainless steel plate at 125 to 250 mm/min (5 to 10 in./min); the 50 A unit cuts 6 mm ($\frac{1}{4}$ in.) plate at 635 to 1270 mm/min (25 to 50 in./min), a significant increase. If the average required cutting thickness exceeds 75% of the maximum thickness capacity of a unit, the next larger size should be considered.

Pierce capacity is usually half the cutting thickness capacity, an important consideration in selection of plasma equipment. To cut plate thicker than 75 mm (3 in.), connecting 400 A or 500 A units in parallel extends thickness capacity.

Technique. With a machine-operated plasma arc torch, standoff distance from the work metal is about 5 to 20 mm ($\frac{1}{4}$ to $\frac{5}{8}$ in.). In manual operation, the current and rate of gas flow are set, and the arc is struck by pressing a button on the torch, which is guided manually over the work. At the end of the cut, the arc is automatically extinguished, and the control opens the contactor and closes the gas valves. The operator can extinguish the arc at any time by moving the torch away from the work metal.

Quality of Cut. Most plasma cutting torches impose a swirl on the orifice gas flow pattern by injecting gas through tangential holes or slots (Fig. 14 and 15). As a result of the swirl of the plasma gas, walls of plasma arc cuts have a V-shaped included angle of 2 to 4° on one of the cut edges. When a straight edge is required on the cut part, the operator must operate the torch carefully so that the bevel is on the scrap side of the cut. When the operator is facing the direction of torch travel, if the gas swirls clockwise, the bevel will be on the left side of the cut. In many cases, a small bevel is acceptable; it may even be used as a weld preparation. The relationship of torch travel direction to the part with clockwise swirl of the orifice as is illustrated in Fig. 16.

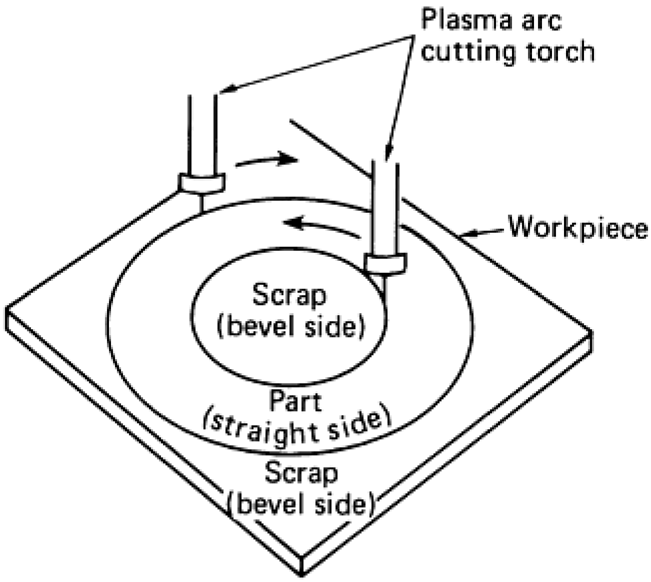

Fig. 16 Relationship of torch travel direction to the part with clockwise swirl of the orifice gas. With the clockwise swirling plasma gas, the bevel side of the cut is on the left when the operator is looking in the direction of the torch travel. To achieve straight cuts on the inner diameter and the outer diameter of the ring, torch directions must reverse to keep the right side of the cut on the part edge.

Quality of cut includes surface smoothness, kerf width, degree of parallelism of the cut faces, dross adhesion on the bottom of the cut, and sharpness of top and bottom faces. Table 5 provides data on the causes of imperfections in plasma arc cutting of low-carbon steel, stainless steel, and aluminum.

Table 5 Causes of imperfections in plasma arc cuts

Type of imperfection	Cause of imperfection		
	Low-carbon steel	**Stainless steel**	Aluminum
Top edge rounding	Excessive speed, excessive standoff	Excessive speed, excessive standoff	**Seldom occurs**
Top edge dross	Excessive standoff, dross easily removed	Excessive standoff, excessive hydrogen	**Excessive standoff, dross easily removed**
Top side roughness	Seldom occurs	Excessive hydrogen or standoff, insufficient speed	**Insufficient hydrogen**
Side bevel--positive	Excessive speed, excessive standoff	Excessive speed, excessive standoff	**Excessive speed, insufficient hydrogen**
Side bevel--negative	Seldom occurs	Seldom occurs	**Excessive hydrogen**

Top side undercut	Excessive hydrogen	Excessive hydrogen	**Insufficient speed, insufficient hydrogen**
Bottom side undercut	Seldom occurs	Slight effect at near-optimum conditions	**Seldom occurs**
Concave surface	Seldom occurs	Excessive hydrogen	**Excessive hydrogen, insufficient speed**
Convex surface	Excessive speed	Insufficient hydrogen, excessive speed	**Seldom occurs**
Bottom edge rounding	Excessive speed	Seldom occurs	**Seldom occurs**
Bottom dross	Excessive hydrogen or speed, insufficient standoff	Insufficient speed, excessive hydrogen	**Excessive speed**
Bottom side roughness	**Insufficient standoff**	**Seldom occurs**	**Insufficient hydrogen**

Width of kerf is $1\frac{1}{2}$ to 2 times the kerf of conventional oxyfuel gas cutting. The range is usually 5 to 10 mm ($\frac{3}{16}$ to $\frac{3}{8}$ in.), although some users achieve 0.8 mm ($\frac{1}{32}$ in.). For thick work metal, width of kerf may exceed 9 mm ($\frac{3}{8}$ in.).

Heat-Affected Zone. The high speeds possible with plasma arc cutting result in relatively low heat input to the workpiece. Heat-affected zones are therefore narrow. The HAZ on stainless steel plate 25 mm (1 in.) thick cut at 1270 mm/min (50 in./min) is 0.08 to 0.13 mm (0.003 to 0.005 in.). Sensitization is usually avoided.

Bevel cutting for weld preparation is an important application of plasma arc cutting. The intense heat of the process makes it suitable for all types of beveling at a higher efficiency than oxyfuel gas cutting.

Applications

Plasma arc cutting can be used to cut any metal. Most applications are for carbon steel, aluminum, and stainless steel. It can be used for stack cutting, plate beveling, shape cutting, and piercing.

In stack cutting, the plates should be clamped together as closely as possible. However, plasma arc cutting can usually tolerate wider gaps between carbon steel plates than can oxyfuel gas cutting. When high plasma arc cutting speeds are used, there is less distortion of the top plate. Several plates of 1.5 to 6 mm ($\frac{1}{16}$ to $\frac{1}{4}$ in.) thickness can be economically stack cut.

For shape cutting, plasma arc cutting torches are used on shape cutting machines similar to those used for oxyfuel gas cutting (Fig. 6). Generally, plasma arc shape cutting machines can operate at higher travel speeds than is possible with oxyfuel gas cutting machines. Because of the fumes and heat produced by the cutting action, water tables are sometimes used with plasma arc shape cutting machines. The water just touches the bottom of the plate, where it traps the fumes, slag, and dross as they emerge from the bottom of the kerf. It also helps reduce noise.

Plasma arc cutting of carbon steel plate can be done faster than with oxyfuel gas cutting processes in thicknesses below 75 mm (3 in.) if the appropriate equipment is used. For thicknesses under 25 mm (1 in.), plasma arc cutting speed can be up to five to eight times greater than that for oxyfuel gas cutting (Fig. 17). For thicknesses over 38 mm ($1\frac{1}{2}$ in.), the choice of plasma arc or oxyfuel gas cutting depends on other factors such as equipment costs, load factor, and applications for cutting thinner plates and nonferrous metals. Characteristics of plasma arc cutting and oxyfuel gas cutting are compared in Table 6.

Table 6 Comparison of OFC and PAC processes

	Oxyfuel	Plasma arc
Flame temperature	3040 °C (5500 °F)	**28,000 °C (50,000 °F)**
Action	Oxidation, melting, expulsion	**Melting, expulsion**
Preheat	Yes	**No**
Kerf	Narrow	**Wide**
Cut	Both sides square	**One side square**
Speed	Moderate	**High**
Heat-affected zone	Moderate	**Narrow**
Cutting ability:		
Carbon steel	Yes	**Yes**
Stainless steel	Requires special process	**Yes**
Aluminum	No	**Yes**
Copper	No	**Yes**
Special alloys	Some	**Yes**
Nonmetallics	**No**	**Yes**

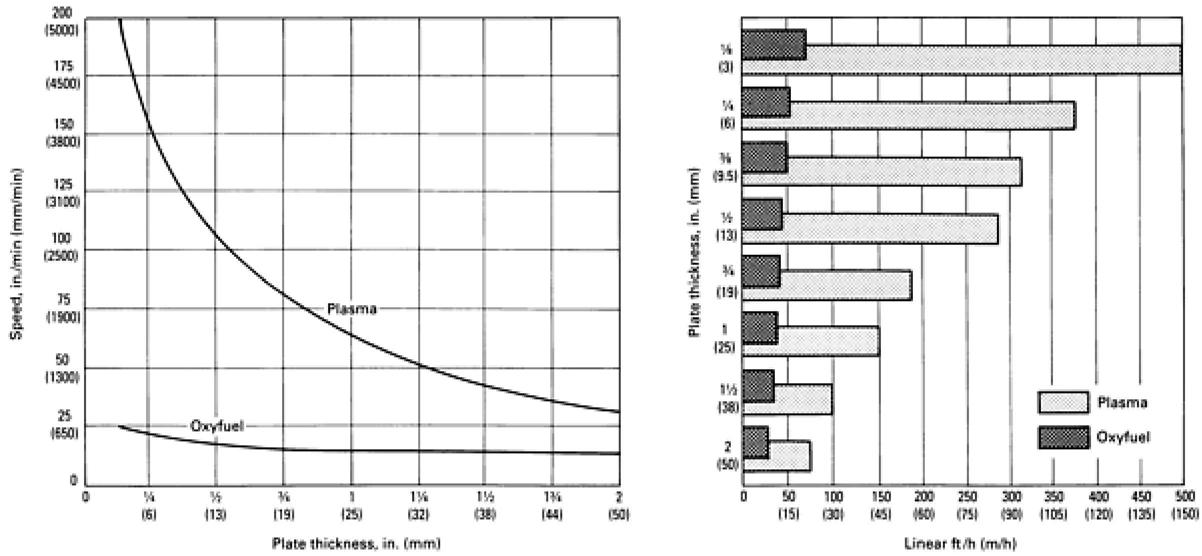

Fig. 17 Comparison of oxyfuel gas cutting and plasma arc cutting of plain carbon steel.

Reference cited in this section

1. W.H. Kearns, Ed., *Welding Handbook,* Vol 2, *Welding Processes--Arc and Gas Welding and Cutting, Brazing, and Soldering,* 7th ed., American Welding Society, 1978, p 499-507

Thermal Cutting

Revised by Ed Craig, AGA Gas, Inc.

Air Carbon Arc Cutting and Gouging

Air carbon arc cutting and gouging severs or removes metal by melting it with the heat of an arc struck between a carbon-graphite electrode and the base metal. A stream of compressed air blows the molten metal from the kerf or groove. Its most common uses are for weld joint preparation; removal of defective welds; removal of welds and attachments when dismantling tanks and steel structures; and removal of gates, risers, and defects from castings. The process cuts almost any metal, because it does not depend on oxidation to keep the cut going. A holder clamps the carbon-graphite electrode in position parallel to an air stream, which issues from orifices in the electrode holder to strike the molten metal immediately behind the arc. The electrode holder contains an air flow control valve, an air hose, and a cable. The cable connects to the welding machine; the air hose connects to a source of compressed air. Cutting action in the air carbon arc process is illustrated in Fig. 18.

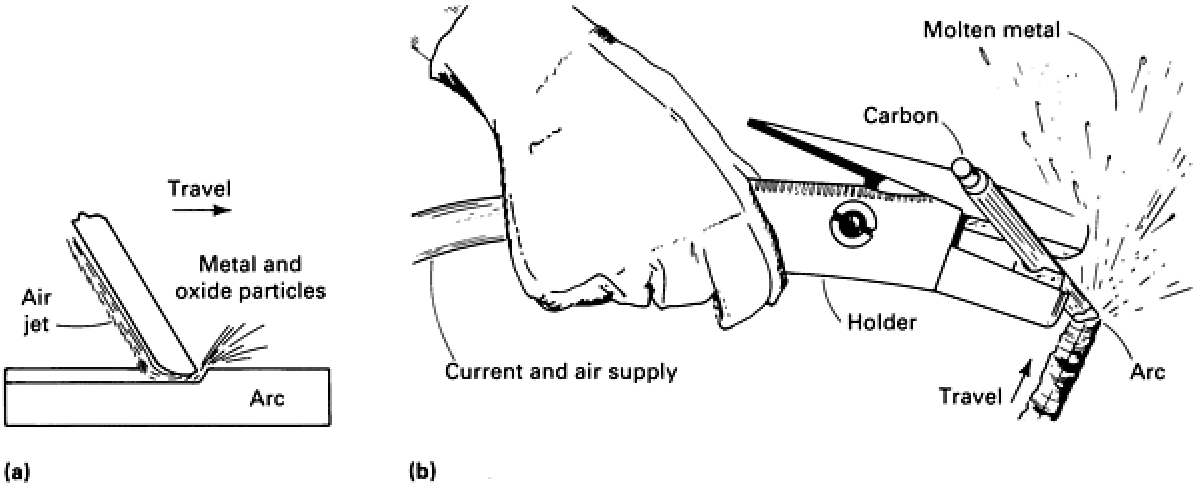

Fig. 18 (a) Air carbon arc cutting action. (b) Manual air carbon arc cutting.

The low heat input of air carbon arc gouging makes this process ideal for joint preparation and for weld removal on high-strength steels. Base-metal temperatures rise very little, about 80 °C (150 °F) in most applications.

Rough cutting is done manually. Accurate work calls for electrode holders mounted on motor-driven carriages.

Pipe Fabrication. Fabricators of structural steel, pressure vessels, tanks, and pipe use hand torches, semiautomatic torches, and fully automatic torches. A typical pipe fabrication plant uses two automatic air carbon arc torches. One, mounted on a large traveling manipulator, works with several sets of turning rolls and in tandem with submerged arc welding units. Longitudinal and circumferential seams are square butted, welded on the inside, backgouged to sound weld metal, then welded on the outside. The second torch, mounted on a pedestal, backgouges circumferential seams at another station.

Power Supply. Constant-voltage direct current with a flat to slightly rising voltage characteristic is best for most air carbon arc cutting applications. Direct current is preferred; copper alloys, however, cut better with alternating current. Table 7 provides data on power sources for air carbon arc cutting and gouging.

Table 7 Power sources for AAC and gouging

Equipment	**Polarity**	Use
Variable-voltage motor-generator, resistor, and resistor grid	Direct current	**All electrode sizes**
Constant-voltage motor generator, rectifier	Direct current	**Electrodes > $\frac{1}{4}$ in. in diameter**
Transformer	Alternating current	**Alternating current electrodes only**
Rectifier	**Alternating current, direct current**	**Direct current from three-phase transformer only; single-phase source not recommended. Use alternating current with alternating current electrodes only.**

Air Supply. Compressed air from a shop line or a compressor at 550 to 700 kPa (80 to 100 psi) should be used; pressure as low as 275 kPa (40 psi) is suitable for light work. Deep grooves in thick metal require pressures up to 860 kPa (125 psi). Air hoses should have a minimum inside diameter of 6 mm ($\frac{1}{4}$ in.) with no constrictions. Air pressure is not critical in air carbon arc cutting; the process requires a sufficient volume of air to ensure a clean, slag-free surface. The amount of air required depends on the type of work (0.08 to 0.9 m^3/min, or 3 to 33 ft^3/min, for manual operations and 0.7 to 1.4 m^3/min, or 25 to 50 ft^3/min, for mechanized operations).

Air carbon arc cutting electrodes are made from mixtures of carbon and graphite. The three basic types of air carbon arc cutting electrodes are:

- Direct current copper-coated electrodes, which are used most frequently because of long life, stable arc characteristics, and groove uniformity. These electrodes are produced in diameters from 4 to 20 mm ($\frac{5}{32}$ to $\frac{3}{4}$ in.)
- Direct current uncoated electrodes, which have limited use. These electrodes, although generally restricted to diameters of less than 9 mm ($\frac{3}{8}$ in.), are available with diameters from 3 to 25 mm ($\frac{1}{8}$ to 1 in.)
- Alternating current copper-coated electrodes, which have additions of rare-earth metals to provide arc stabilization with alternating current. These electrodes are produced in 5, 6, 9, and 13 mm ($\frac{3}{16}$, $\frac{1}{4}$, $\frac{3}{8}$, and $\frac{1}{2}$ in.) diameters

Cross sections vary; round electrode rods are most common. Electrodes also come in flat, half-round, and special shapes to produce specially designed groove shapes.

Technique. The angle of the electrode, speed of cut, and amount of current determine depth and contour of the cut or groove. The electrode is held at an angle, and an arc is struck between the end of the electrode and the work metal. The electrode is then pushed forward. Data on groove depth, electrode size, current, and travel speed for air carbon arc gouging is available from various equipment manufacturers.

For through-cutting, the electrode is placed at a steeper angle, almost vertically inclined. Plate thicknesses greater than 13 mm ($\frac{1}{2}$ in.) may require multiple passes.

Grooves as deep as 25 mm (1 in.) can be made in a single pass. A steep angle, approaching that used for through-cutting, and rapid advance produce a deep, narrow groove; a flatter angle and slower advance produce a wide, shallow groove. Electrode diameter directly influences groove width. Operators should use a wash or weave action to remove excess metal such as risers and pad stubs, or in surfacing. Smoothness of the gouged or cut surface depends on the stability of electrode positioning, as well as on the steadiness of the electrode as it advances during the cutting operation. Mechanized gouging, with the electrode and holder traveling in a carriage on a track, produces smoother surfaces five times faster than does manual work.

Absorption of Carbon. Reverse polarity air carbon arc cutting removes metal faster than does straight polarity. However, the current carries carbon from the electrode to the base metal, increasing its carbon content. To minimize hardenability, the air stream must be adjusted to ensure removal of all molten metal.

Thermal Cutting

Revised by Ed Craig, AGA Gas, Inc.

Exo-Process

A relatively new electric arc cutting process, called the Exo-Process, has been developed. Similar to flux-cored processes, it uses a consumable tubular electrode and a specially designed gun that feeds high-speed compressed air to the arc (Fig. 19). The air flow functions to push molten metal from the gouge cavity, to constrict the arc for more precise control, and to cool the electrode. The system can be adapted to conventional gas metal arc welding equipment (it requires a direct current constant-voltage power source--150 A minimum--and a conventional wire feeder).

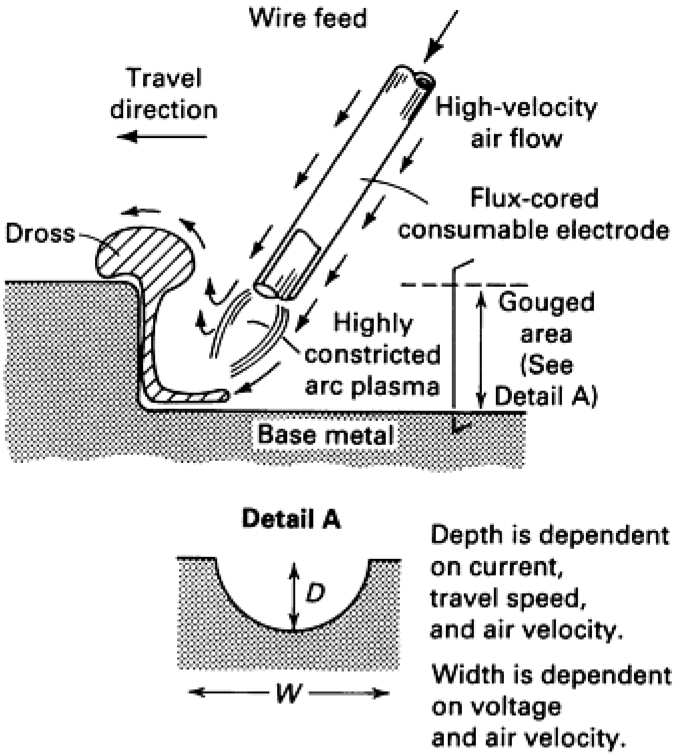

Fig. 19 The Exo-Process for gouging.

The velocity of the air flow at the arc is the key factor for straight cutting. A 1.5 mm ($\frac{1}{16}$ in.) wire size can cut up to 6 mm ($\frac{1}{4}$ in.) thick carbon steel. Speed and edge cut quality on most commercial metals and alloys is good, particularly for sheet metal thicknesses. Gouge quality on carbon steels is also good. The process would be well suited for automated equipment, in that high travel speeds may be attained. An obvious benefit of the process is that it can be mounted on a gas metal arc dual-wire feed system to provide the operator with a multifunctional welding and cutting unit.

Thermal Cutting

Revised by Ed Craig, AGA Gas, Inc.

Oxygen Arc Cutting

Oxygen arc cutting uses a flux-covered tubular steel electrode. The covering insulates the electrode from arcing between it and the sides of the cut. The arc raises the work material to combustion temperature; the oxygen stream burns the material away. Oxidation, or combustion, liberates additional heat to support continuing combustion of sidewall material as the cut progresses. The electric arc supplies the preheat necessary to obtain and maintain ignition at the point where the oxygen jet strikes the surface of the work. The process finds greatest use in underwater cutting.

When cutting oxidation-resistant metals, melting action occurs. The covering on the electrode acts as a flux; it functions in a manner similar to that of powdered flux or powdered metal injected into the gas flame in the flux-injection method of oxyfuel gas cutting of stainless steel.

Equipment. Oxygen arc cutting uses direct or alternating current, although direct current electrode negative (DCEN) is preferred. The electrode and the electrode holder convey the electric current and oxygen to the arc. Electrode holders must be fully insulated; underwater cutting requires a flashback arrester, and the electrode must have a watertight plastic coating. Components of an oxygen arc electrode are shown in Fig. 20.

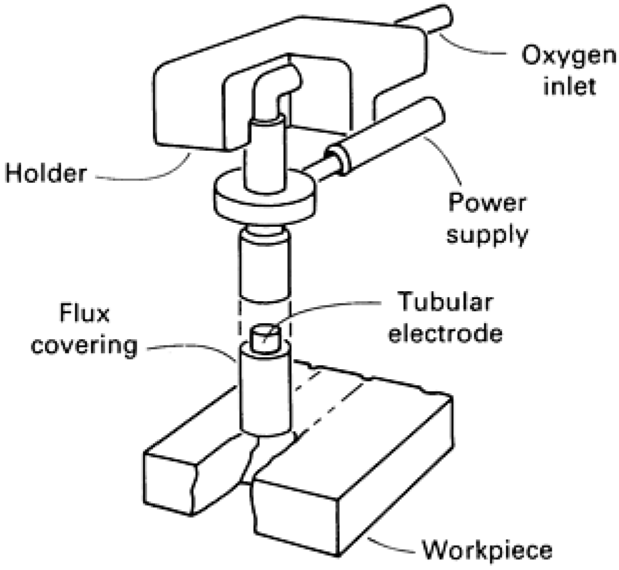

Fig. 20 Components of an oxygen arc electrode.

Thermal Cutting

Revised by Ed Craig, AGA Gas, Inc.

Reference

1. W.H. Kearns, Ed., *Welding Handbook,* Vol 2, *Welding Processes--Arc and Gas Welding and Cutting, Brazing, and Soldering,* 7th ed., American Welding Society, 1978, p 499-507

Laser Cutting

Gregg P. Simpson, Peerless Laser Processors Division, Peerless Saw Company; Thomas J. Culkin, Lumonics Materials Processing Corporation

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Introduction

INDUSTRIAL LASERS are being used in numerous material processing applications. They can weld microswitches and auto transmission gears, scribe and machine ceramic substrates, and drill jet engine turbine blades and baby bottle nipples. They are also used in heat treating, cladding, ablating, and marking. However, cutting represents their largest single application.

The versatility of the laser in cutting operations is responsible for its widespread use. The same laser can be used to cut men's suits, newspapers, circuit boards, motorcycle fenders, circular saw blades, stainless steel auto exhaust tubing and 13 mm (0.5 in.) thick alloy steel for aircraft disc brakes.

Its flexibility makes the laser an ideal tool for prototype or production work. Because laser cutting is a noncontact process, no tool wear occurs. Laser systems can cut intricate parts with accuracies of ±0.025 mm (±0.001 in.) and with surface finishes better than 1.3 μm (50 μin.) for some steels, and better than 0.50 μm (20 μin.) for some nonmetals.

Laser Cutting

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Definition of a Laser

First, the word laser is an acronym for light amplification by stimulated emission of radiation. Second, there are essentially three components that are necessary for lasing action. There must be an active media that can be excited, a method of exciting the media, such as an electrical discharge between an anode and cathode, and a resonator.

The molecules of the active media must be excited in order to stimulate the emission of radiant energy or light. As the molecules are charged to a higher energy state, they excite their electrons into a higher energy level. Because unstable electrons seek their lowest energy state, they release this added energy as light particles, known as photons.

The resonator consists of two parallel mirrors that reflect light particles between them, thereby amplifying the stimulated emission of light. Of the two mirrors in the resonator, the rear mirror is 100% reflective, while the front, or output, mirror is typically only 50% reflective and therefore 50% transmissive. The percentage of light that is transmitted through the front mirror is commonly known as the laser beam. It is this parallel, monochromatic, intense beam of light particles that is used for material processing. The percentage of light remaining inside the resonator is necessary to maintain the continuous stimulated emission of photons.

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Laser Types

The carbon dioxide (CO_2) laser and the neodymium-yttrium aluminum garnet (Nd-YAG) laser are by far the most commonly used material-processing lasers. The CO_2 laser relies on a gas mixture of CO_2, helium (He), and nitrogen (N) as its active media. It usually uses an electrical discharge between an anode and cathode as the method of media excitation, and the standard two-mirror resonator. The CO_2 laser produces a wavelength of 10.6 μin (420 μin.), which is invisible to the human eye. Visible light falls between 0.4 and 0.7 μm (15 and 28 μin.). Although practical material-processing lasers have powers ranging from 150 W to 8 kW, CO_2 lasers have been built with powers above 25 kW. Most lasers used for cutting have powers that range from 150 W to 3 kW. Figure 1 shows a typical CO_2 gas laser design.

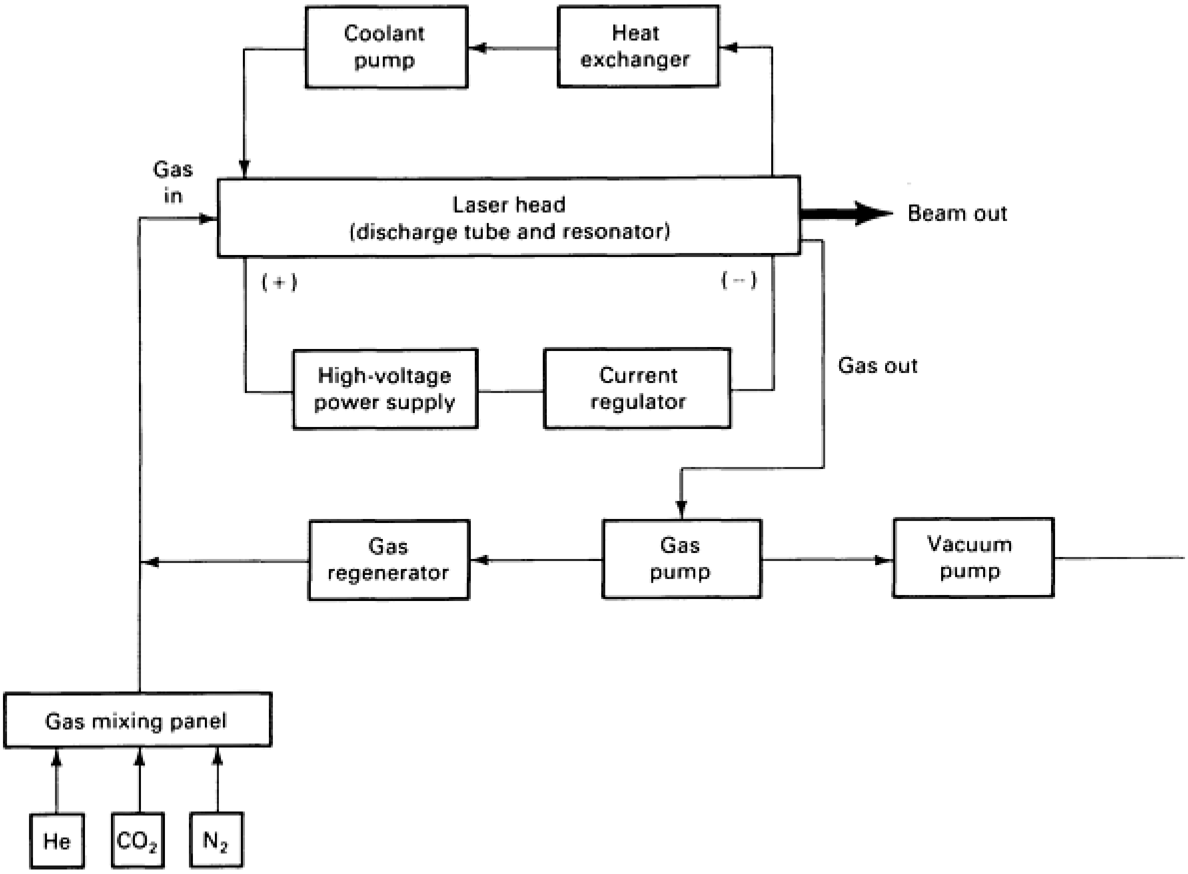

Fig. 1 General CO_2 laser design.

CO_2 Lasers. There are three basic types of CO_2 gas lasers: slow axial flow, transverse flow, and fast axial flow.

The slow axial flow laser is an older, proven design that has excellent mode, stability and pulsing capabilities. They are available with powers up to approximately 800 W, allowing them to cut steels up to 6.5 mm (0.25 in.) thick. Figure 2 shows the typical design theory of a slow axial flow laser. The lasing action occurs by injecting laser gas at a pressure of approximately 2.7 kPa (20 torr) into an evacuated glass tube. The tube has a rear mirror and an output coupler at either end. A high-voltage glow discharge is then passed down the tube between an anode and cathode, causing lasing to occur. This design, which can only create 70 W/m (20 W/ft) of tube length, requires a long resonator cavity to produce high power levels.

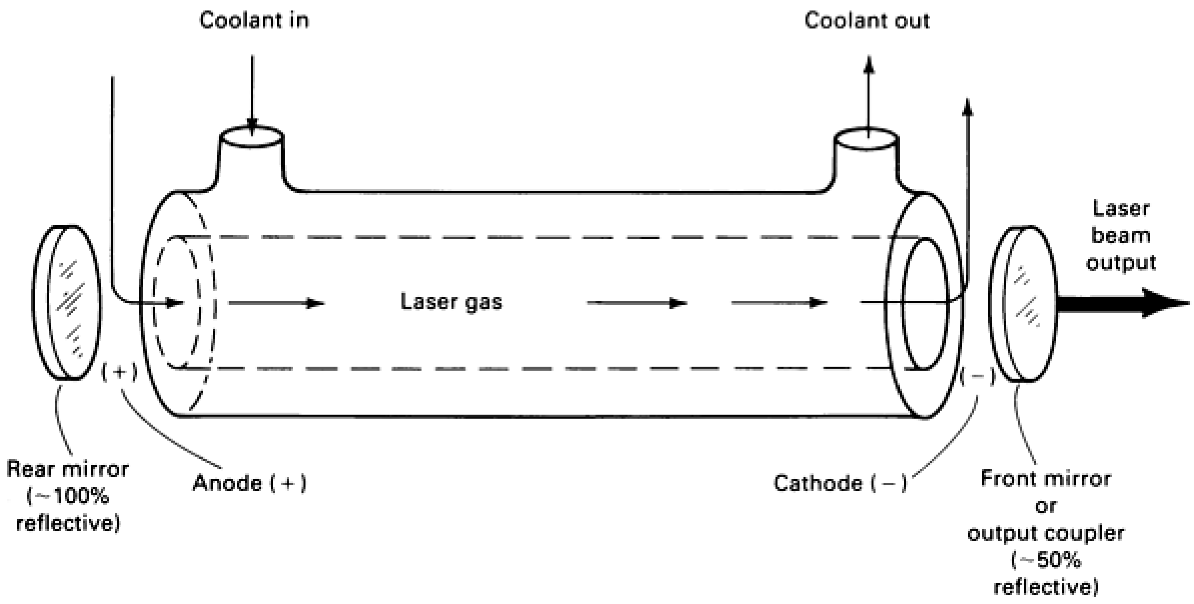

Fig. 2 Slow axial flow CO_2 laser.

The transverse-flow laser was developed in response to the size versus power limitations of the slow axial flow laser. Transverse-flow laser design allows power to be generated to approximately 25 kW. The transverse laser accomplishes this by using a tangential blower to move a high volume of laser gas transversely across the anode-cathode electrical path, as shown in Fig. 3. The basis of this design is that power is directly related to the volume of laser gas that is excited at any one time. The drawbacks are that these lasers cannot be electronically pulsed for cutting very intricate geometries, and the beam quality, or mode, is not ideal for high quality cutting applications.

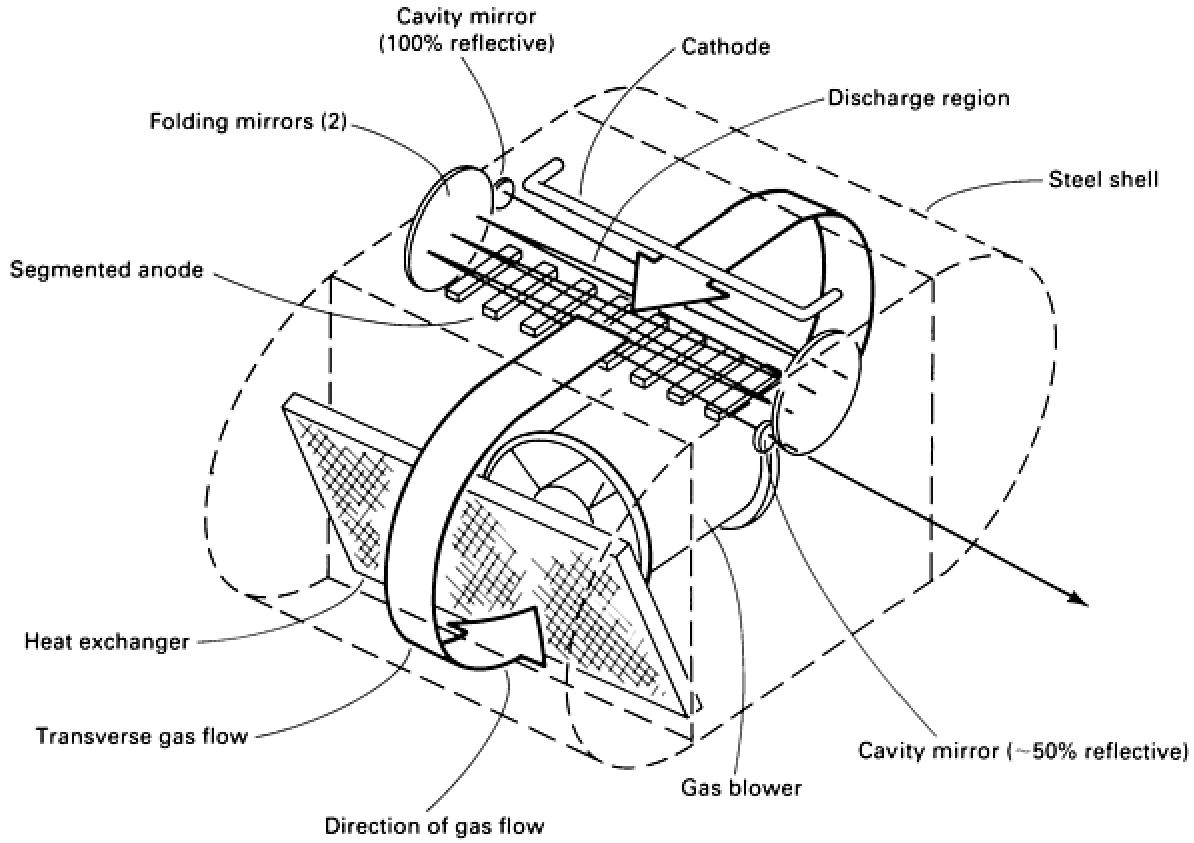

Fig. 3 Transverse or cross-flow CO_2 laser.

The fast axial flow laser is the newest CO_2 type. It combines the high power of the transverse flow laser with the beam quality and some of the pulsing capabilities of the slow axial flow laser. Its design is similar to the slow axial flow laser, except that a tangential blower forces a large amount of laser gas axially down the resonator, as shown in Fig. 4. This increases the available power to 700 W/m (210 W/ft) of active laser resonator. This laser type is currently being built with power up to 3 kW, and is becoming increasingly popular as a cutting laser because of its increased power, excellent beam quality, and pulsing capabilities.

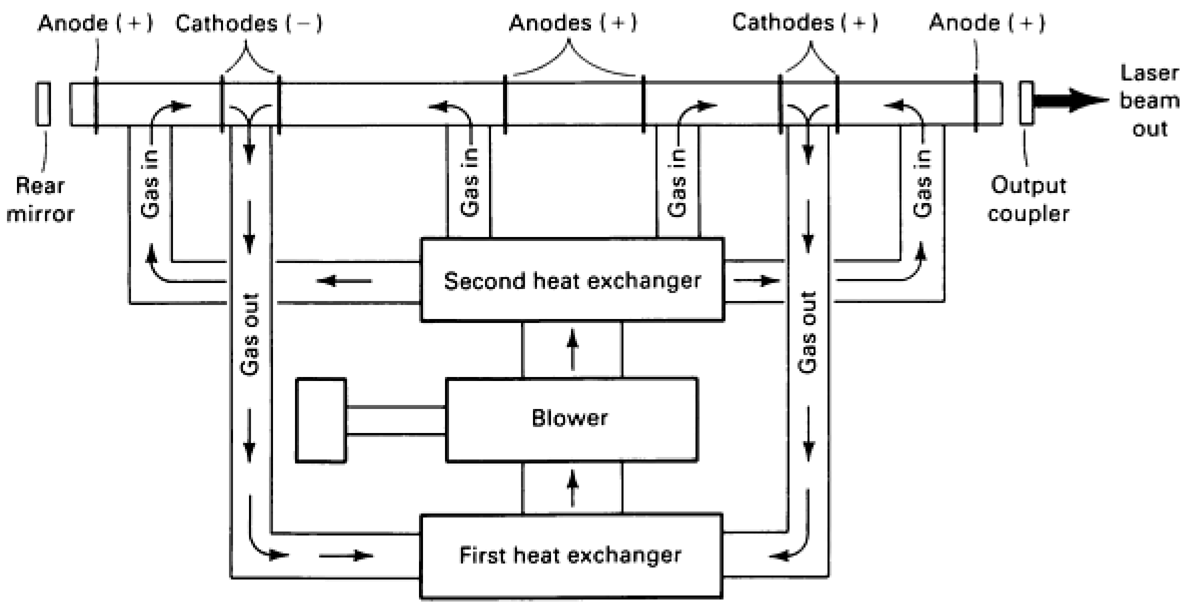

Fig. 4 Typical fast axial flow CO_2 laser.

Both the slow and fast axial flow lasers can operate in either the continuous wave (CW) or electronically pulsed modes. In CW operation, the laser operates at a continuous power level. This type of operation provides the highest cutting travel speeds. Because of the high speeds required to cut thinner material above 3175 mm/min (125 in./min), a loss of accuracy can occur on intricate parts because of motion system limitations. Overheating will also occur on any thickness if the part is very complex. The solution to these problems is to pulse the laser electronically, thereby allowing intermittent high peak powers (approximately two to eight times the peak CW power) and overall lower average powers, as shown in Fig. 5. This results in controlled heat input and higher accuracies, but at reduced feed rates, compared to CW operation. Typical pulse rates for CO_2 laser cutting range from 100 Hz to 1000 Hz.

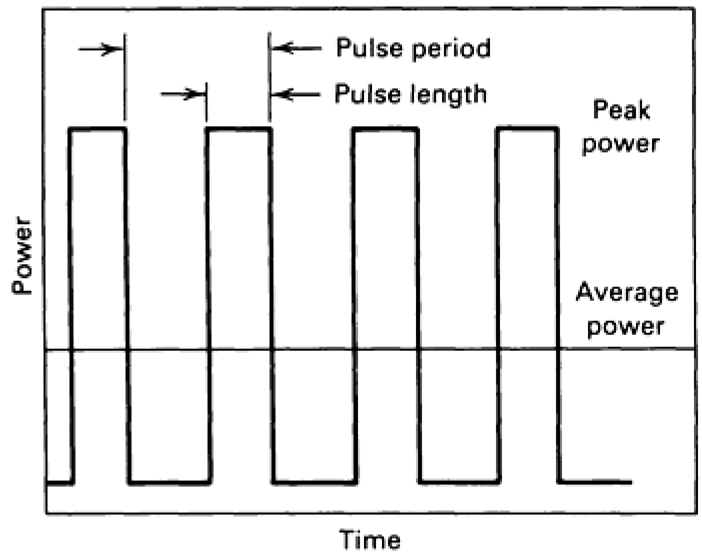

Fig. 5 Pulsed waveform.

The Nd-YAG is the second type of industrial laser. It is a solid-state laser in which the active medium is neodymium, which is dissolved in a matrix of yttrium aluminum garnet. The resulting crystal is formed into a rod, which is excited by external flash lamps using xenon or krypton. As the lamps flash, the light is absorbed by the rod, exciting the medium to emit photons. This laser uses the same type of two-mirror resonator described earlier. The Nd-YAG laser is a pulse-only laser with a cutting speed limited to about 762 mm/min (30 in./min). This laser also emits invisible infrared light, but with a wavelength of 1.06 μm (41.7 μin.), compared to 10.6 μm (417 μin.) for a CO_2 laser. Because of this shorter wavelength, the Nd-YAG beam is more readily absorbed by metals, and is therefore used to cut gold, silver, copper, platinum, and other metals that would be very reflective to a CO_2 laser. The Nd-YAG laser also does a superb job of drilling and trepanning small holes in metals and is typically used on aircraft jet engine parts made from high-temperature superalloys and titanium. Figure 6 shows a typical Nd-YAG laser system design.

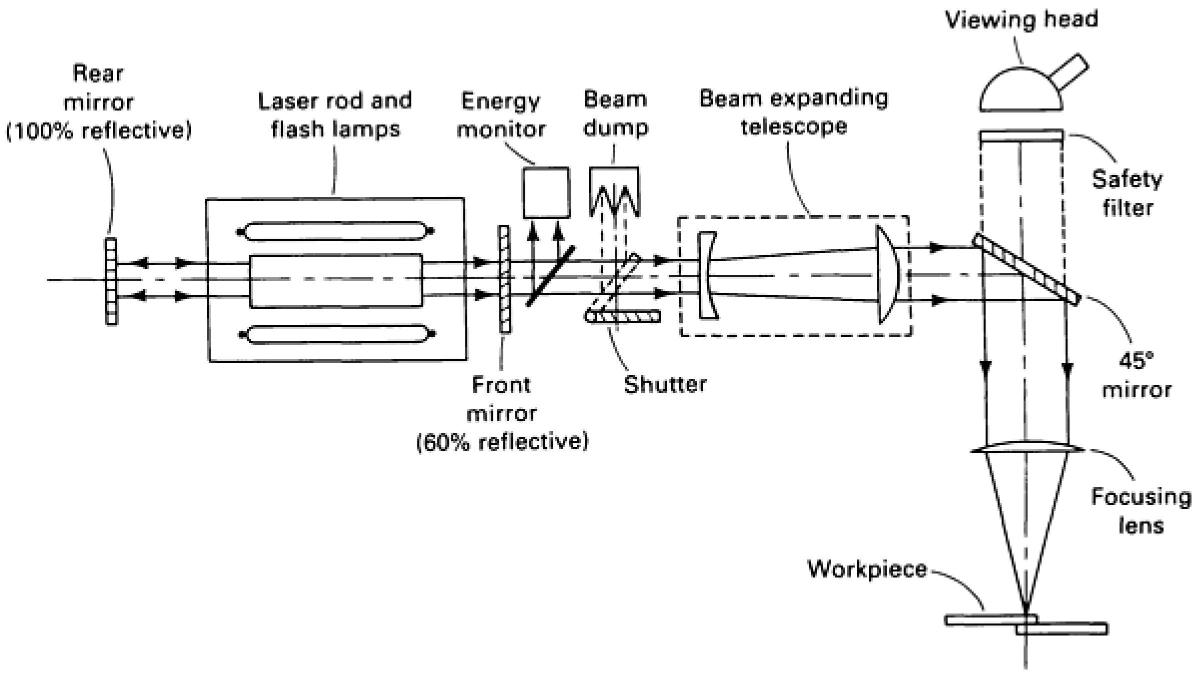

Fig. 6 Typical Nd-YAG laser design.

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Competing Cutting Methods

The advantages and disadvantages of several conventional metal-shaping processes that compete with the laser are shown in Table 1. While the laser does not displace any of these processes in terms of their special capabilities, laser cutting does fill a very important void. For example, the laser is the ideal method for producing short-run or prototype blanked parts of large, complex or small, intricate shapes (see Fig. 7). The choice to laser cut these parts is based on cost. The expense of temporary tooling or edge finishing far exceeds the cost of laser cutting.

Table 1 Advantages and disadvantages of laser cutting versus traditional metal cutting methods

Cutting method	**Advantages**	Disadvantages
Laser	Good edge quality, good accuracy, small kerf, narrow HAZ, no distortion, little noise; cuts nonmetals, cuts small and complex shapes	**High equipment cost, limited to under 13 mm (0.5 in.) thick, slower feed rates over 6.4 mm (0.25 in.); cuts single layer**
Plasma arc	Lower equipment costs, faster feed rates over 6.4 mm (0.25 in.); cuts over 13 mm (0.5 in.) thick	**Lower accuracy, decreased edge quality, larger kerf, wider HAZ, noisy, higher operating costs; only cuts metal**
Laser	Good edge quality, good accuracy, lower scrap rate, no distortion, small kerf, no tooling or tool wear, increased part nesting; cuts complex shapes, cuts up to 13 mm (0.5 in.) thick, cuts tempered materials and nonmetals	**Higher equipment costs, lower process rate, higher costs on larger part quantities**
Nibbling (turret punch press)	Good process rate, lower equipment costs, economical on medium to high production runs	**Lower edge quality, high tool wear, high tooling costs, low accuracy, distortion, scrap; only cuts 10 mm (0.38 in.) thick**
Laser	Good edge quality, no tooling or dies, short setup times, rapid and low-cost design changes, noncontact cutting; cuts complex shapes and three-dimensional shapes, cuts tempered materials	**Higher equipment costs, low rate on high volumes**
Punch press	High volume rates; lower costs at high volumes; cuts over 13 mm (0.5 in.) thick	**Greater tool fabrication time, higher tool costs and maintenance, more setup time, poorer tool design, part stresses, lower edge quality; only cuts annealed steel**
Laser	High feed rate, economical on small and medium quantities; cuts nonmetals and nonconductive metals	**Lower edge quality, higher equipment costs, thickness limitations**
Wire electric discharge machining	Good edge quality, good accuracy, lower equipment costs, cuts over 13 mm (0.5 in.) thick, cuts very fine and complex shapes	**Very slow on any thickness, fixturing**

Laser	Narrow kerf, faster feed rates, good accuracy, good edge quality	**Small HAZ, fumes; cuts limited materials**
Abrasive water jet	No HAZ, cuts up to 152 mm (6 in.) material thickness, no distortion, cuts all materials	**Abrasive disposal, noisy, high-pressure plumbing, slow feed rates**
Laser	Flexibility, faster feed rate, short setup time	**Small HAZ, accuracy, high equipment costs**
Numerically controlled milling	**No HAZ, good accuracy, good edge quality, low equipment costs**	**Limited feed rate, high tool costs and maintenance**

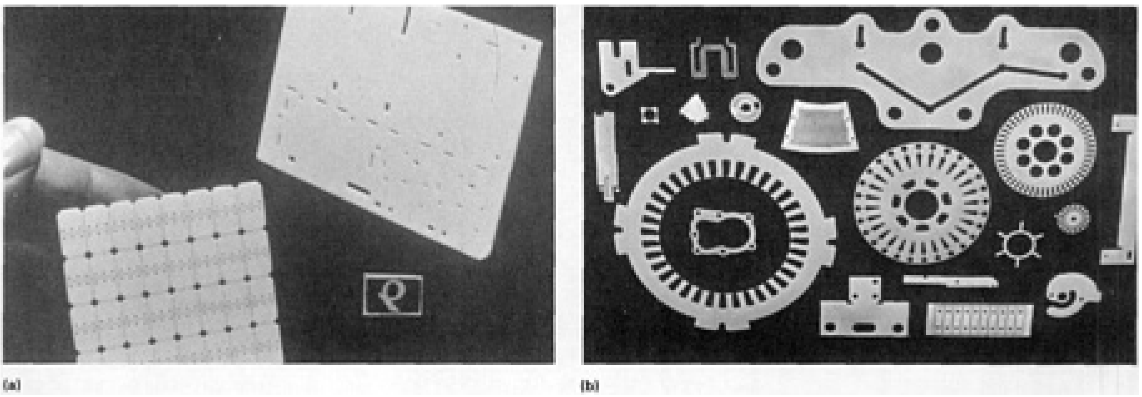

Fig. 7 Typical laser-cut parts. (a) Ceramic. (b) Metal.

A second, broader example is the cutting of industrial-quality circular saw blades. Several years ago a major manufacturer of such blades purchased a two-axis laser system to blank out the entire saw blade. These saw blades are being produced in sizes ranging from 50 mm (2 in.) to 915 mm (36 in.) in diameter, with an average order quantity of 22 pieces. Because of the small order quantity and the highly customized tooth profiles, many setups and operations were required to punch the geometries. However, by using the laser, which cut the saw geometry in just one operation, the manufacturer was able to reduce the cost on many items up to 30% over conventional punching, milling, turning, and blanking methods.

Laser cutting does have limitations for many applications. In the case of the heat-affected zone and fusion layer present in some aerospace hardware, laser cutting can only be used to produce a semifinished part that requires further processing using a different method.

Laser cutting advantages and disadvantages must be carefully considered for each particular application before deciding whether use of the laser is advantageous. Because applications are too far-ranging to generalize, in-depth information should be obtained from a laser manufacturer, systems house, laser job shop, or consultant.

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General Cutting Principles

The mechanism for cutting steel with a laser is basically the same as cutting steel with an oxygen-fuel process in which the fuel gas acts to heat the material so the oxygen can oxidize and react exothermically with the steel to produce the cutting action. The oxygen also helps to sweep the molten material out of the kerf. In laser cutting, the fuel gas is replaced with a laser beam focused to about 0.1 mm (0.004 in.), resulting in a power density of 1 MW/cm^2 (6.5 MW/in.^{2}). It is this characteristic that allows a 3.94 mm (0.155 in.) thick alloy steel chain saw bar to be cut out at 2.54 m/min (100 in./min) with a 0.15 mm (0.006 in.) kerf width and without part distortion.

Laser cutting can also be accomplished on nonferrous and nonmetallic materials using assist gases. In the case of nonferrous metals (aluminum, copper, brass, and bronze), which have high thermal conductivities, but do not react with assist gases and are reflective to the laser beam, cutting occurs when the laser beam heats the material well above its melting point, and an assist gas, such as air, argon, or helium, is used to sweep the molten material out of the cut. The inert gases are used only when the cut edge of the material must be free of any impurities that would reduce its serviceability in a very harsh environment.

In the case of nonmetallic materials with low thermal conductivities and high beam absorption, such as wood, cloth, and paper, vaporization of the material upon cutting is nearly 100%. Because material vapors have a tendency to rise, an assist gas, such as compressed air, is used at a low pressure to protect the focusing lens from the damaging vapors. Organic materials such as plastics and woods are also cut with lower gas pressures. Acrylic plastic will yield a fine polished edge if cut with an inert gas or compressed air at 70 kPa (10 psi) pressure. Table 2 summarizes the general purposes of assist gases.

Table 2 Assist gas usage

Gas	**Major function**	Usage
Oxygen	Promotes chemical reaction	**Cutting ferrous metals**
Argon, helium, and nitrogen	Inhibit chemical reaction	**Cutting thin metal for oxide-free edge; cutting chemically reactive metals and materials**
Air and inert gases	**Remove excess by-products**	**Lens protection, absorptive plume removal; cooling to clear hot gases away from thermally sensitive materials; cutting nonmetals**

Process Variables. There are seven basic parameters in the laser-cutting process: beam quality (mode), power (CW or pulse), travel speed, assist gas, nozzles, focusing lens, and focal-point position. Slight changes in any one of these parameters can yield significant changes in cut quality. To fine-tune excellent cut quality, it is recommended that only one parameter at a time be varied, while the others remain constant.

Beam quality, or mode, is a very important parameter. A laser should be used with a TEM_{00} or Gaussian mode, which is ideal for cutting (Fig. 8a). A Gaussian mode has most of its energy in its center. Figures 8(b) and 8(c) illustrate the power density versus the radial distance from the beam centerline for both TEM_{00} and TEM_{01*} modes. The TEM_{00} mode can be focused to a smaller spot size than can TEM_{01*}, and has greater energy density of power per unit area, thereby increasing cutting efficiency. This TEM_{00} mode decreases the heat input to the part, which allows faster cutting speeds and a smaller heat-affected zone (HAZ). Because the beam can be focused to a smaller spot size, kerf width is also reduced.

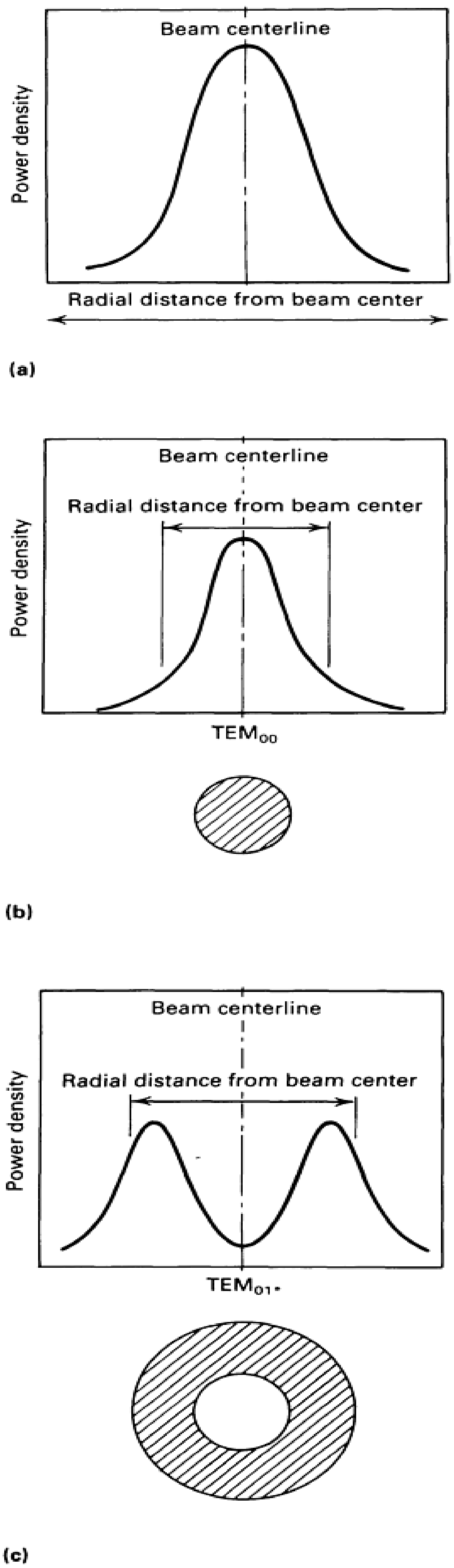

Fig. 8 (a) TEM_{00} or Gaussian mode energy distribution. (b) TEM_{00} power density (sharp tool). (c) TEM_{01*} power

density (blunt tool).

Power plays a significant role in respect to both feed rates and thicknesses. For example, Fig. 9 shows that increasing power increases speed and thickness for a given material, while holding all other parameters constant. Figure 9 also shows that the maximum material thickness to be cut also increases with increasing power. When pulse cutting is used, the average powers decrease, while the peak powers of each pulse increase from two to eight times the maximum CW power. This reduction of average power reduces the feed rate by about 60 to 80%.

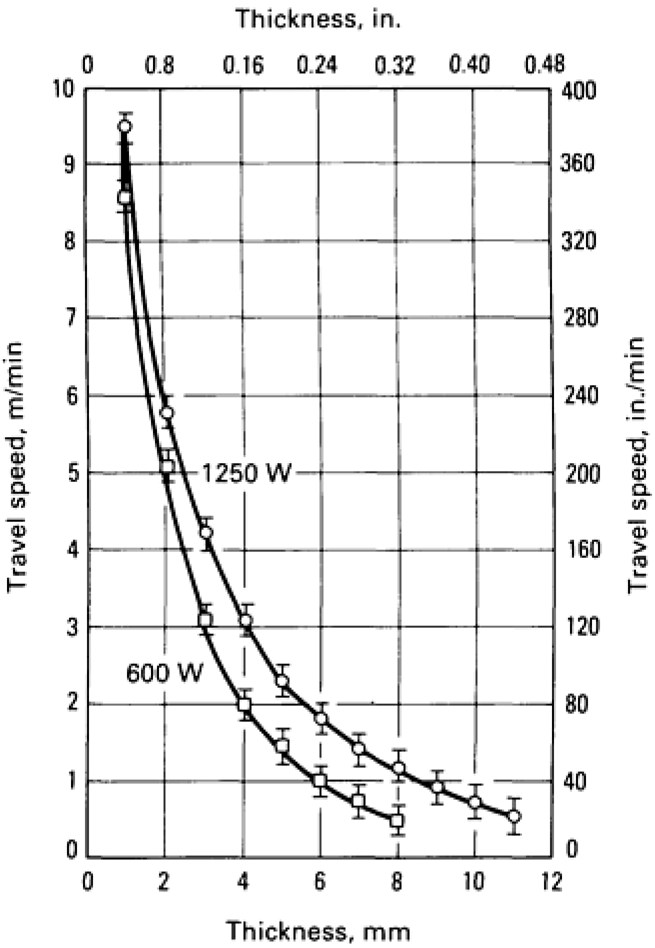

Fig. 9 Travel speed versus thickness for 600 W and 1250 W CO_2 lasers. Focused power at workpiece using 65 mm ($2\frac{1}{2}$ in.) focal length lens. Oxygen assist gas at 350 kPa (50 psi). Carbon and alloy steels used.

An optimum travel speed exists that yields the best cut quality, while holding all other parameters constant. Because feed rate is very material dependent, experimentation is needed in order to obtain the best results. However, for most metals, the best cut is obtained at the maximum achievable speed, which also helps to minimize the HAZ. If the feed rate is too low, burning will occur and a large HAZ with slag formation will be present. In nonmetals such as wood or cloth, excessive charring will be present on the cut face. If the feed rate is too fast, the beam will climb out of the cut and only partial cutting will occur.

Use of oxygen as the assist gas for cutting steel and stainless steel increases cutting speeds by 20 to 40% compared to use of air. Air is used mostly to cut nonmetals because it helps reduce oxidation and burning. When cutting stainless steel with an oxygen assist gas, slag or a fusion layer occurs, which is not tolerable in some applications. Although argon or helium assist gases eliminate oxidation, they reduce travel speeds by up to 50%.

The gas nozzle design and standoff distance, which is the distance between the workpiece and the nozzle, can significantly affect cut quality. A property designed nozzle will produce a laminar, high flow rate assist gas through the cut. The laminar flow can be affected by the standoff distance. If the standoff distance is too great, the smoothly flowing gas tends to break up. This disrupts the flow through the kerf and decreases the edge quality. Typical nozzle diameters are 1 to 2 mm (0.040 to 0.080 in.) with 0.5 to 3 mm (0.020 to 0.12 in.) standoff distances.

A focusing lens is used to focus the beam on the workpiece. This increases the power density of the beam. The lens is used because the output beam of a laser is typically 11 to 21 mm ($\frac{7}{16}$ to $\frac{13}{16}$ in.) in diameter and does not possess enough energy per unit area to melt and vaporize materials. The lens can focus the beam to a spot size of 0.1 mm (0.004 in.) in diameter. The same principle is in operation as when using a magnifying glass to focus sunlight on a piece of paper, causing it to burn.

Lenses are classified by focal length, or the distance from the lens to the point at which the spot size is smallest (Fig. 10). Typical lenses come with focal lengths that range from 38 to 254 mm (1.5 to 10 in.). Shorter focal length lenses, have higher energy densities because they have a smaller spot size, as shown in Fig. 11. These lenses, however, have a limited depth of field, or usable beam. Depth of field is the area of the focused beam that has enough energy density to process materials. This spot size limitation can be offset by using a larger input beam on a longer focal length lens, as shown in Fig. 12. Short lenses are typically used on reflective materials, such as aluminum, or on thin materials, for faster feed rates and an improved surface finish. Longer focal length lenses are usually used on materials 6.4 mm (0.25 in.) or more thick because of their greater depth of field, which produces a squarer cut and sharper geometry definition at the bottom of the cut. Because of increased spot size, these lenses decrease feed rate, and overall surface finish quality, while increasing the HAZ. Most lenses in use are between 64 and 127 mm (2.5 and 5.0 in.) in focal lengths.

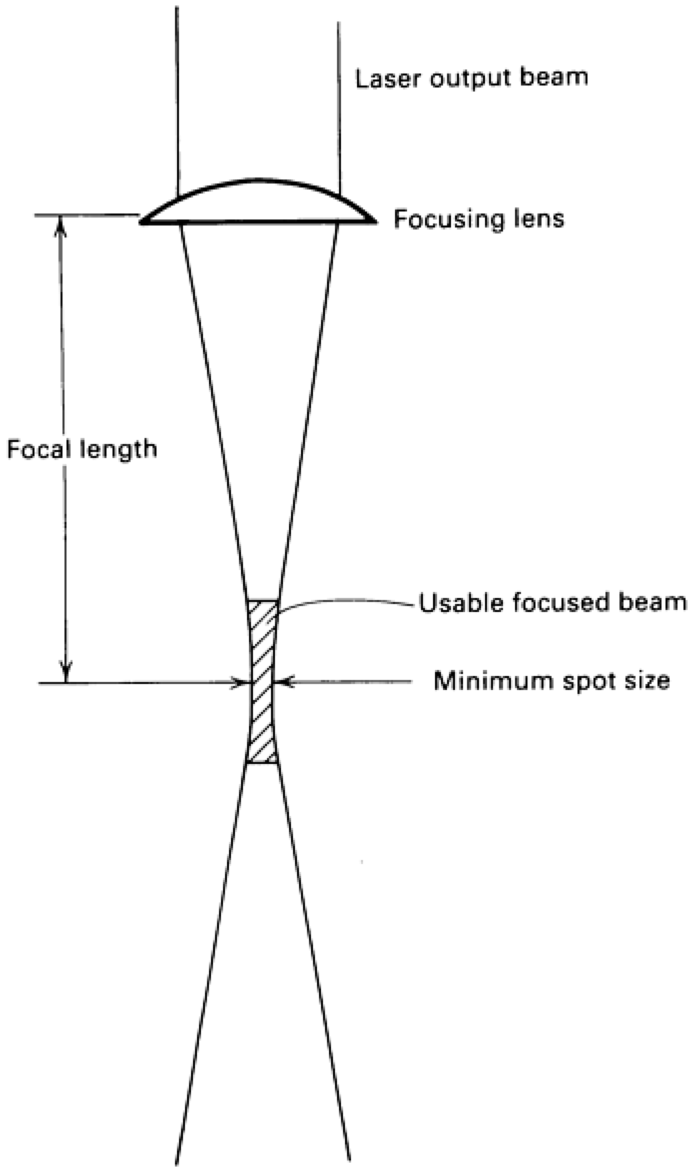

Fig. 10 Geometry of focused beam.

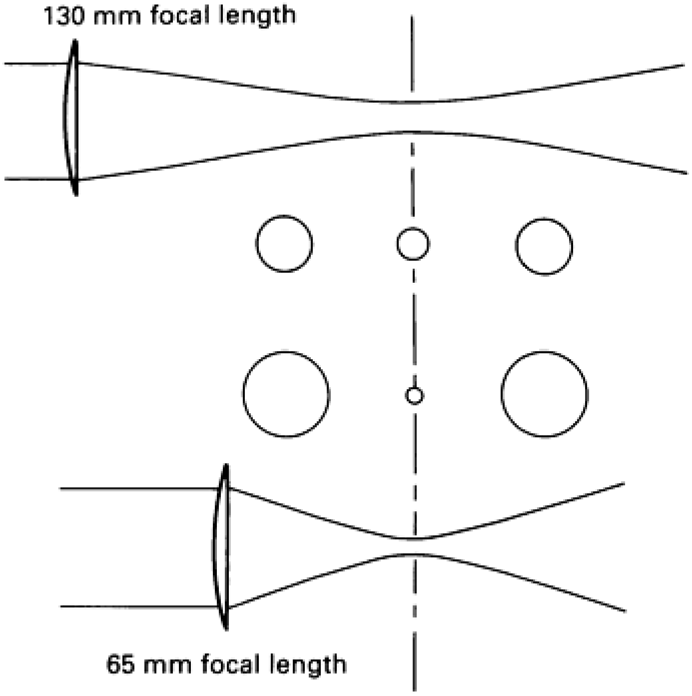

Fig. 11 Beam diameters at equal distances from focus position for lenses of different focal length.

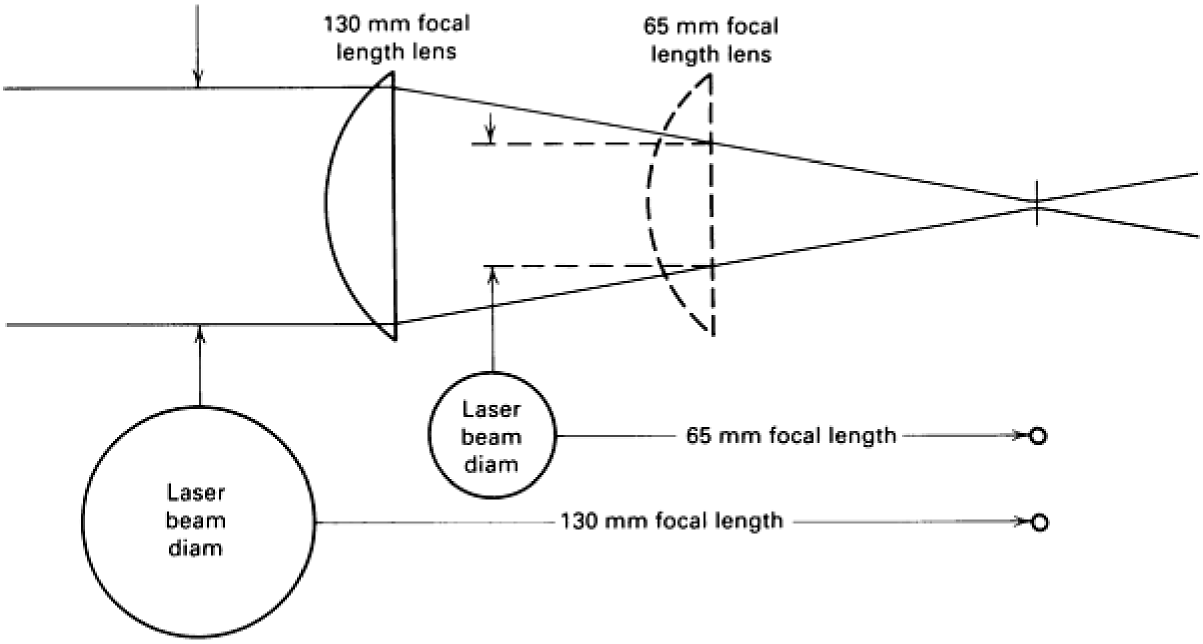

Fig. 12 Beam expansion for some spot size.

The focal point position greatly affects the surface finish and dimensional accuracy of the part. Accuracy is affected because the beam is never parallel (Fig. 11); hence, as the focal point is moved to different positions, the kerf width can increase or decrease. This effect must be considered when cutting high-accuracy parts.

Surface finish is very dependent on the focal point. For the majority of metals, the focal point is set slightly below the work surface. For other materials, such as stainless steels, the focus is set well below the surface of the material to yield the best cut. However, the focal position itself is very dependent on the type of material and other process parameters and therefore must be manipulated to find the optimum setting.

Material Conditions. Several material conditions can affect the quality of the laser cut. First, surface cleanliness can have a detrimental effect on edge quality. All steel alloys should be either hot rolled, pickled, and oiled; or cold rolled. Mill scale, which can interfere with the beam, greatly reduces edge quality and dimensional accuracy. Rusted steel also decreases cut quality. No other special cleaning methods are required.

Second, flatness of the material to be cut affects the focal point of the beam. The focal point must be controlled to ±0.25 mm (0.010 in.) to achieve the best possible cut. Increasing surface roughness can deflect the assist gas, resulting in a nonlaminar assist gas flow, thereby decreasing edge quality. Most sheet steel is smooth enough to be laser cut.

Third, coatings on the material surface usually have no adverse effect on cutting. Thin layers of plastic on metal surfaces are cut without problem, although proper fume collection procedures must be used. Electrogalvanized steel can be laser cut provided that the galvanized layer is thin. Again, it is recommended that fume collection be used. Steels with paint on one surface should be cut without oxygen because a paint-oxygen reaction produces a very poor cut.

Finally, the ambient temperature of materials must be taken into consideration. Because carbon steels react exothermically with oxygen at low temperatures of approximately 40 °C (104 °F), high ambient temperatures result in a cut with wide, rough, low dimensional accuracy. This effect also must be taken into account when cutting very intricate geometries in carbon steels. However, pulse cutting at reduced feed rates reduces the heat input and allows satisfactory cutting of very small and intricate shapes.

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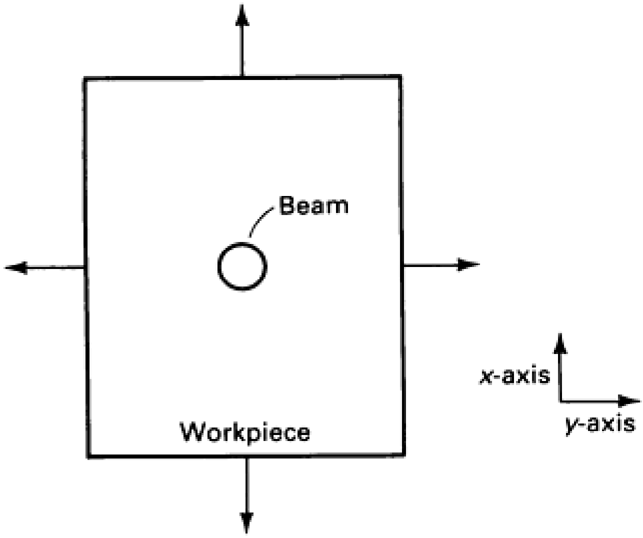
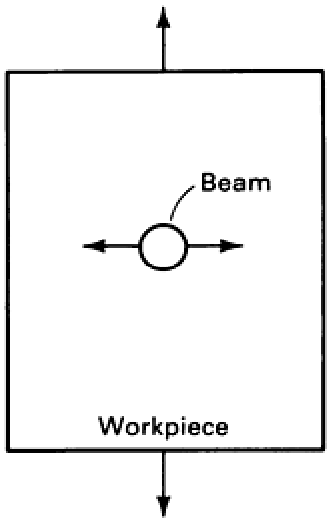
System Equipment

The basic laser-cutting system consists of a laser, a motion system, a controller that is computer numerically controlled (CNC), and a beam delivery system. Optional equipment includes water chillers, dust/fume collectors, compressed air equipment, transformers and related electrical equipment, and a computer-aided design/computer-aided manufacturing (CAD/CAM) system. Typical costs for an installed system can range from $100,000 for a small, two-axis cutting system to almost $1 million for a large, five-axis cutting system.

Many styles and sizes of motion systems are available. The most popular is the *x-y* axis table system. The *x-y* motion is coordinated by a CNC controller and is used to blank contours out of flat sheet stock. These systems range in size from 305 × 305 mm (12 × 12 in.) to 1.6 × 3.0 m (63 × 120 in.) for a large-bed sheet-cutting system. These two-axis motion systems are available in three styles, each of which manipulates the workpiece and/or beam differently in the course of contouring a part, as shown in Fig. 13.



(a)



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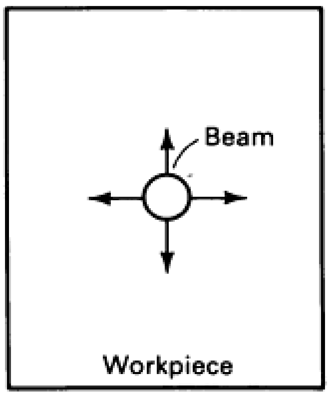

(c)

Fig. 13 Three types of two-axis motion systems. (a) Moving *x-y* table, stationary beam. (b) Moving *x*-axis table, moving *y*-axis beam. (c) Moving *x-y* beam, stationary table.

The first system, shown in Fig. 13(a), has a moving workpiece and a stationary beam, which is the simplest and least costly system. Motion systems of this type usually provide the highest accuracies for laser cutting. For cutting areas larger than 1.2 × 1.2 m (48 × 48 in.), the mass of a 2-axis table system becomes large and therefore reduces travel speeds and accuracies. For large-sheet cutting requirements, it is advantageous to move the workpiece in one axis, while moving the beam and cutting head assembly in the other axis, as shown in Fig. 13(b). This allows for significantly less mass movement on the motion system, while incorporating some floor space savings. For systems that are 1.2 × 2.4 m (48 × 96 in.) or larger, it is more common to move the beam and optics on a gantry-style motion system, as shown in Fig. 13(c). These systems do, however, require more attention to beam alignment. A moving optics system can also be used on smaller systems in which the parts are too heavy to move accurately, or on assembly lines.

Most laser-cutting systems usually employ some type of floating third, or *z*, axis to allow the focused beam to be introduced to the workpiece, because a workpiece is not usually perfectly flat. These floating cutting heads sense height changes through contact and non-contact methods. Figure 14 shows a contact method using roller balls.

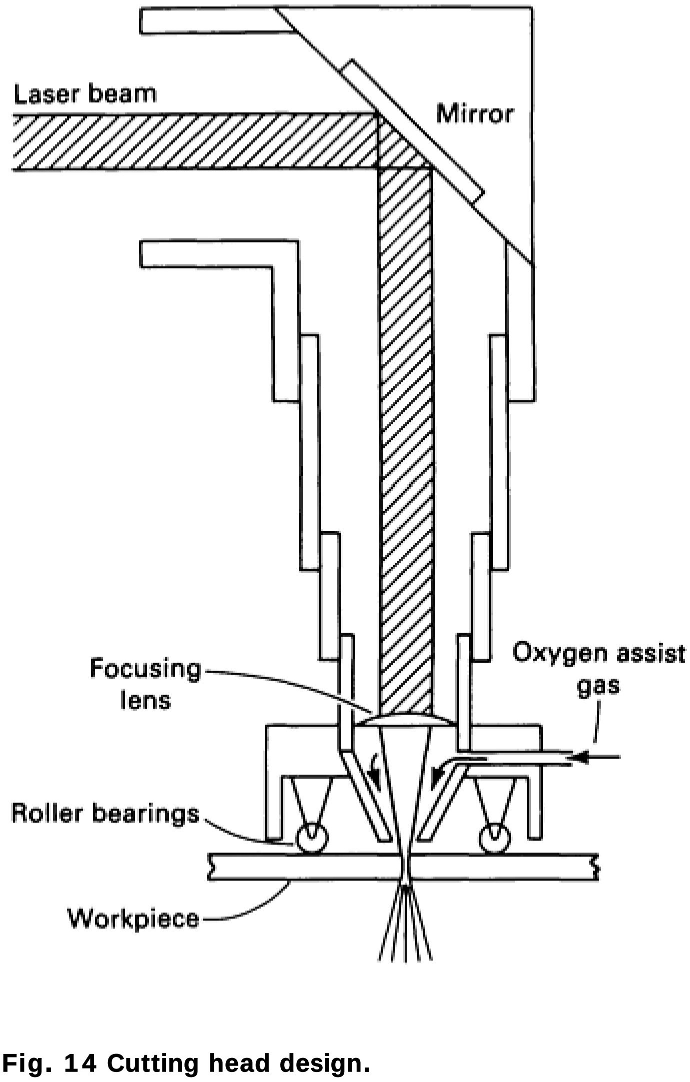

Fig. 14 Cutting head design.

Other types of motion systems have three, five, seven, and nine axes of programmable motion. The three-axis systems have either a programmable *z* or a rotary axis. The *z*-axis allows cutting on parts with different heights, whereas a rotary axis allows contour cutting of tubing. The five-axis gantry-style motion systems incorporate *x*, *y*, and *z* axes, with a wrist on the *z*-axis. A wrist is a set of orthogonal rotating mirrors providing increased beam manipulation. This system allows contour cutting on formed parts and is necessary because the beam must be normal to the surface being cut. Typical applications are the trimming of stamped parts such as automotive and motorcycle fenders. In specialized cases, a nine-axis robot can manipulate either the beam or the workpiece. Some applications include cutting holes for sunroofs in auto assembly lines or other types of ventilation.

A CNC controller is used to coordinate the motion and the laser operation. The controller reads standard NC programs that describe the contour to be cut. The NC programs are prepared with a CAD/CAM system. Then either the NC file is punched on paper tape and read into the controller by a tape reader, or a number of other methods including floppy disks, erasable programmable read-only memories, and a direct numerical control between the controller and the off-line computer.

A dust/fume collecting system should be installed: laser cutting of metallics produces dust, which should be removed, while non-metallics can produce smoke and fumes, which can be irritating and/or hazardous and likewise should be removed from the work area. Optional equipment includes a water chiller to cool the laser and external optics, transformers for proper electrical supply, and compressed dry air for the beam delivery and assist gas.

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Laser-Cutting Applications

Virtually any metal can be laser cut. The conditions for cutting some metals, however, are difficult to obtain. Following is a general description of how well each metal group can be cut.

All carbon and alloy steels can be laser cut to over 13 mm (0.50 in.) thick with an oxygen assist gas. As Fig. 9 shows, the feed rates are dependent on available power at the workpiece. A general rule of thumb is that for a given thickness, the feed rate increases about 50% when power is doubled. The resulting kerfs can be as small as 0.1 mm (0.004 in.) thick with a 65 mm ($2\frac{1}{2}$ in.) focal length lens. The resulting HAZ is small (about 0.1 to 0.3 mm, or 0.004 to 0.012 in.), depending on thickness and speed. The edges are clean, smooth, and square.

Higher-carbon steel exhibits an improved edge quality, although the HAZ is slightly larger and harder. Impurities of phosphorus and sulfur can cause some edge burning: Lower-quality steels exhibit this edge burning, whereas alloy steels generally do not. In fact, the alloy steels, such as chrome-nickel-moly (for example, 4340), and chrome-moly (for example, 4130), are perfect candidates for extremely high quality edges that are very smooth and clean. These steels do, however, require about 50% more time to pulse a starting hole than is the case with a similar thickness of plain carbon steel. Another consideration is that the HAZ is slightly larger and harder (about 45 to 60 HRC) depending on the alloy and carbon content. This means that further processing can be difficult on the laser-cut edges unless they are annealed.

Laser-cut tool steels have almost the same results. The exceptions are the alloys that have very dense alloying elements, such as tungsten. These alloys retain a large amount of heat when molten, which helps to limit thickness and produce very rough cuts having heavy slag deposits. The air- and oil-hardened alloys exhibit very good edge quality and can be cut up to 10.2 mm (0.400 in.) thick at about 0.8 m/min (32 in./min) at 1300 W. Alloys such as D-2 and M-2 can only be cut up to about 4 mm (0.160 in.) thick with the same power.

Stainless steel alloys are also readily cut using a laser. The feed rates are reduced, however, because these alloys do not react as effectively with oxygen as carbon steel alloys do. An inert assist gas may be used to obtain a weld-ready edge, free of all oxides, at the expense of about one-half the oxygen-assisted speed. Stainless steels maintain their corrosion resistance because the HAZ is small.

In general, the ferritic (400 series) stainless steels produce smoother cuts with less slag than austenitic (300 series) stainless steels do. These stainless alloys do not contain nickel and can be cut to about 6.5 mm (0.260 in.) thick.

The added presence of nickel does affect the energy coupling and heat transfer in the alloy. This means that these alloys are not effectively laser cut above 5 mm (0.197 in.) thick. The viscosity of the molten nickel is very high and has a tendency to migrate and adhere to the bottom of the cut. This increases the heat in the metal and produces a large HAZ, as well as a rough cut starting approximately one-third to one-half the way through the material. This effect can be reduced by using high-pressure assist gas jets or rapid-cooling methods (such as water) in the cut.

Nonferrous Alloys. Aluminum alloys can readily be laser cut, but only to about 4 mm (0.160 in.) thick. The thickness is limited because aluminum has high reflectivity at infrared wavelengths and high thermal conductivity. To overcome these effects, the laser must have a TEM_{00} mode, which allows for tighter focusing and higher power outputs, that is, 500 W or more. To further improve the cutting, short focal length lenses and high assist gas pressures help reduce and/or eliminate slag that forms when molten material is blown to the back side of the cut and solidifies; the slag is very easily removed. Feed rates are generally 25% slower than when laser cutting stainless steels.

Copper, brass, and bronze are even more reflective and heat conductive than is aluminum. Because brass and bronze are alloys of copper, they can be laser cut, but with limited thickness and speed. The cuts can be rough, and a slag is present on the bottom of the cut. Copper has been cut under ideal conditions in thicknesses up to 6.35 mm (0.25 in.) with 1200 W. The speeds are extremely slow, so practical upper cutting is limited to about 2.6 mm (0.100 in.).

Other high-tech alloys can also be laser cut. Titanium cuts very well up to about 7 mm (0.275 in.) thick. An inert assist gas is usually employed to prevent an oxide layer from forming. Oxygen can be used, but because titanium reacts very strongly with it, high feed rates are needed to prevent burning the material.

Nonmetal Cutting. Most nonmetals rely on the same technique for laser cutting. The cutting action is dependent on the ability of the laser beam to be absorbed by and to vaporize the material being cut. Because the cut does not rely on an oxidation reaction, as in steel cutting, the laser vaporizes the material with the aid of an assist gas (either inert gas or compressed air) to help purge the cut of the vaporized material. The CO_2 laser is typically preferred for nonmetal cutting because of its good absorption by many materials and its high cutting speeds.

Acrylic is easily cut with the CO_2 laser. Thicknesses up to 50 mm (2 in.) have been cut for aircraft windshields, but most work is done at thicknesses of about 6.4 mm (0.25 in.), while a 150 W unit provides speeds of 1.25 m/min (50 in./min). The cut edge is usually frosted at these speeds. A fine polished edge is attainable in acrylic at about half speed with very low assist gas pressure.

Quartz and nontempered glass are commonly cut for a variety of applications. The edge quality of glass is rough compared to laser cuts in most other materials, at speeds up to 2.5 m/min (100 in./min) for a 3 mm (0.10 in.) thickness using a 600-W CO_2 laser, and about 5 m/min (200 in./min) for a 1200 W unit.

Quartz, on the other hand, cuts with a fine, clean edge with speeds of 5 m/min (200 in./min) for 1 mm (0.040 in.) thick material and 1 m/min (40 in./min) for 3 mm at the 600 W level.

Ceramic of 96% Al_2O_3 (alumina) is used for electronic substrates and is very brittle; the noncontact cutting characteristic of the laser therefore makes it the preferred method. The laser is used in the pulse mode to minimize heat input to the part. A high-quality edge is produced at 0.5 m/min (20 in./min) for alumina substrates up to 1 mm (0.04 in.) and 0.25 m/min (10 in./min) in the 1 to 2 mm (0.04 to 0.08 in.) range with a 500 W CO_2 laser in the pulsed mode.

Aramid fibers have tensile strengths that exceed those of steel. The fiber is woven into sheets for use as bullet-proof vests and is bonded in layers with epoxy for structural aircraft parts. Its high strength makes it very difficult to cut with conventional cutting methods. The CO_2 laser cuts with high travel speeds and can bond the cut edges of aramid and most other woven fabrics to prevent fraying. Aramid, bonded in layers with epoxy, can be cut in thicknesses up to 10 mm (0.40 in.). Because of the nature of the epoxy, the cut edge of the aramid-epoxy composite is darkened upon laser cutting. This is more pronounced in thicker samples with heavy epoxy layers. At the 600 W level, typical speeds are 12.75 m/min (500 in./min) for 1.25 mm (0.05 in.) thick material and about 7.75 m/min (300 in./min) for 3 mm (0.10 in.) aramid-epoxy composites.

Intricate shapes can be cut from wood with thicknesses well over 25 mm (1.0 in.). A solid wood product cuts very nicely and leaves a smooth but darkened edge. A popular application is cutting patterns in 15 to 19 mm (0.60 to 0.75 in.) beech plywood for steel rule die boards. Speed and focus changes create different width slots for different width blades (up to about 2 mm, or 0.08 in.). Therefore, speeds range from 0.5 to 1.8 m/min (20 to 70 in./min) for a 600 W CO_2 laser.

The laser is becoming an increasingly popular machine tool because of its flexibility in material processing. However, laser technology is still a relatively new manufacturing process and much education is still necessary in the industrial sector before its advantages can be fully realized.

Abrasive Waterjet Cutting

J. Gerin Sylvia, Department of Industrial and Manufacturing Engineering, University of Rhode Island

Introduction

ABRASIVE WATERJET CUTTING operates by the impingement of a high-velocity abrasive-laden fluid jet against the workpiece, yet it produces no heat (and therefore no heat-affected zone) to degrade metals or other materials. The finished edge obtained by the process often eliminates the need for postmachining to improve surface finish.

A coherent fluid jet is formed by forcing high-pressure abrasive-laden water through a tiny sapphire orifice. The accelerated jet exiting the nozzle travels at more than twice the speed of sound and cuts as it passes through the workpiece. Cuts can be initiated at any point on the workpiece and can be made in any direction of contour--linear or tangential. The narrow kerf produced by the stream results in neither delamination nor thermal or nonthermal stresses along the cutting path.

In addition to applications in the machining of superalloys; armor plate; titanium; and high-nickel, -chromium, and -molybdenum alloys, abrasive waterjet machining can also be used to cut concrete, rock, glass, ceramics, composites, and plastics. The ability of the abrasive waterjet to cut most metals without any thermal or mechanical distortion places this innovative process on the leading edge of material cutting technology.

Development of Abrasive Waterjet Technology. In 1968, Dr. Norman Franz filed his first patents on the use of high-pressure water streams to cut materials. The first commercial application of this process, in 1971, involved the cutting of 9.5 mm ($\frac{3}{8}$ in.) thick pressed board for manufacturing furniture forms. Since then, numerous waterjet units have been installed by various manufacturers worldwide. Waterjet cutting technology, which involves pumping a 0.08 to 0.46 mm (0.003 to 0.018 in.) diam water stream at 207 to 414 MPa (30 to 60 ksi), was initially developed to cut or slit nonwoven materials, fiberglass building products, corrugated box materials, and plastics.

It was later found that hard or extremely dense materials such as metals and aerospace composites could be cut when particles of dry abrasives such as garnet and silica were added to the waterjet. This modification produced the abrasive waterjet and is responsible for the ability to cut advanced materials much more efficiently than with standard mechanical or thermal cutting methods. With abrasives added to the waterjet, the liquid stream itself is merely the medium that propels the abrasive instead of being the primary cutting force.

Abrasive waterjet cutting is used to cut metals and composite materials, such as boron/aluminum honeycomb, aluminum/boron carbide, and graphite composites, into intricate shapes and curves with virtually no heat input into the workpiece. It has been in use in industrial applications since 1983.

Metals and advanced composites developed for use in the aerospace industry are among the most difficult-to-machine materials. Whether hard as steel or flexible as rubber, these materials must be able to withstand the stresses of supersonic flight. Ironically, the same properties that make space-age materials invaluable for aerospace applications also make them all but impossible to machine. Reciprocating or ultrasonic knives can be used to cut uncured epoxy-base composites, but not the finished components. The cutting rates provided by lasers and plasma arc systems are adequate, but their extreme heat changes the chemical composition of the composite materials and leaves a heat-affected zone in metal-matrix materials.

Abrasive waterjet technology eliminates the problems of delamination and frayed areas, which add to the cost of machining. This elimination of secondary machining has spurred interest in this technology and has accelerated its development.

Advantages. The advantages of abrasive waterjet machining are summarized as follows:

- Ability to cut through most sections of dense or hard materials, such as metals and glass, leaving a clean, finished edge--3.2 to 6.3 μm (125 to 250 μin.) roughness with 60, 80, or 100 grit abrasive at 0.22 to 1.1 kg/min (0.5 to 2.5 lb/min)--without the need for secondary machining
- Ability to produce contours, shape-cutting, bevels of any angle, and three-dimensional profiling, because the process is omnidirectional
- Easy integration into computer-controlled systems, optical tracers, and full-scale six-axis robots. The cutting head weighs as little as 4.5 kg (10 lb) for easy mounting on robotic arms; precision robotics can accommodate cutting heads weighing 23 to 32 kg (50 to 70 lb)
- Wide availability and low cost of garnet and silica, the most common abrasive materials used
- Low water consumption (0.473 L/min, or 0.125 gal./min), which translates to 28 L/h (7.5 gal./h) despite the high pressures used

Limitations. This device cannot replace tools that mill, turn, or drill blind holes or perform other operations that involve cutting or drilling to a partial depth. Glass and composite materials should be pierced at low pressures (70 to 83 MPa, or 10 to 12 ksi) to minimize chipping and delamination. Tempered glass is an example of one material that should not be machined with an abrasive waterjet.

Abrasive Waterjet Cutting

J. Gerin Sylvia, Department of Industrial and Manufacturing Engineering, University of Rhode Island

Operating Principles and Abrasive System Components

A hand-held abrasive waterjet unit can be used, but accuracy and quality are compromised because of human instability. The abrasive nozzle must be held firmly and accurately, and the standoff of the nozzle, as well as the rate of cut, must be closely controlled. Modern abrasive waterjet installations require a six-axis servo-type robot, such as the IRb-60 (Fig. 1), that is programmed for point-to-point linear movements in which arcs or circles are approximated by numerous short straight-line segments.

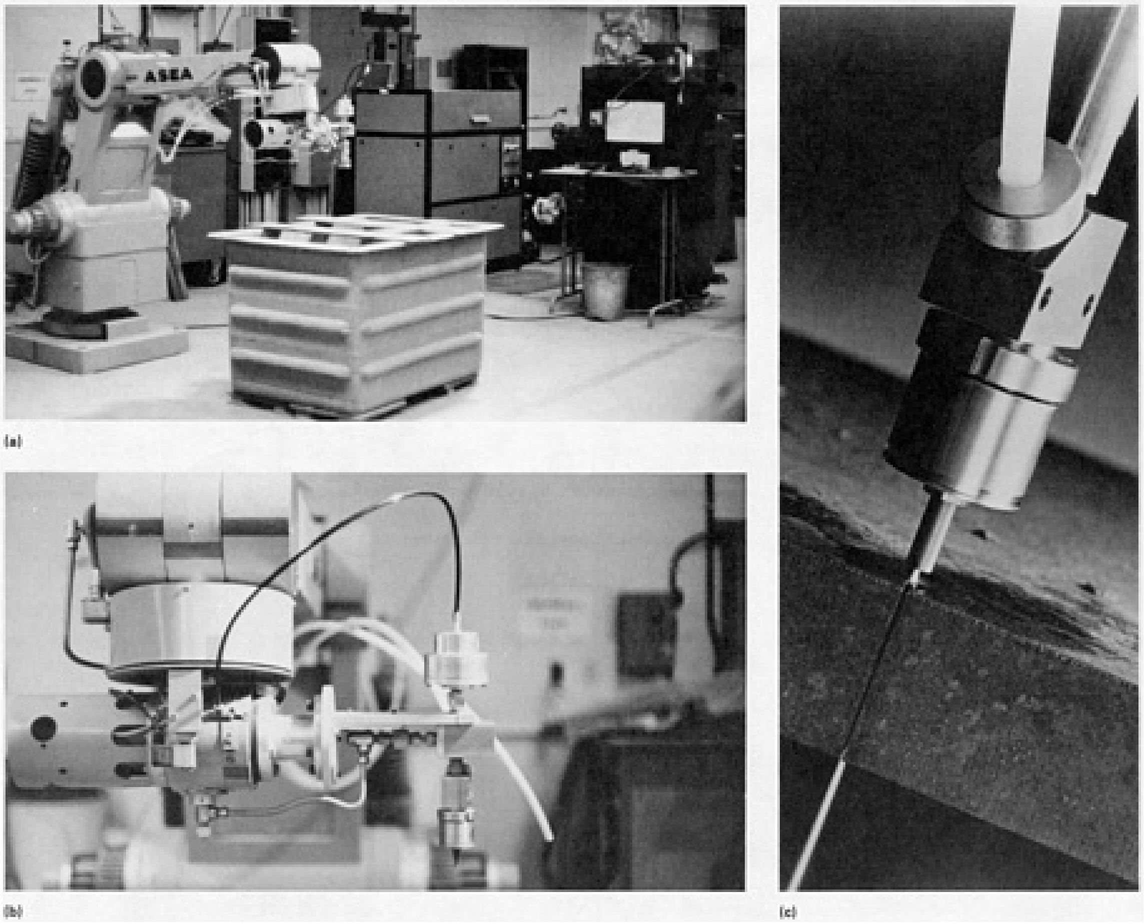

Fig. 1 State-of-the-art abrasive waterjet installation. (a) IRb-60 robot with containment tank. High-pressure pump is shown to the right of the robot. Robot controller is to right of tank. (b) Abrasive waterjet nozzle assembly mounted on IRb-60 robot arm to interface with robot. Black air line on top is connected to electric solenoid located on robot arm. High-pressure water line attaches at center-left of nozzle and flexes as robot arm moves and rotates. (c) Close-up of abrasive waterjet nozzle assembly showing unit cutting through 75 mm (3 in.) thick tool steel plate. Courtesy of Flow Systems, Inc.

Cutting Principle

The abrasive waterjet cuts material by the action of abrasive solids (entrained by the waterjet) on the workpiece. Depending on the properties of the material, cutting occurs by erosion, shearing, failure under rapidly changing localized stress fields, or micromachining effects. A small abrasive jet nozzle is used (Fig. 2). Water is pressurized to 414 MPa (60 ksi) and expelled through a sapphire nozzle to form a coherent high-velocity (914 m/s, or 3000 ft/s) jet. A stream of abrasive particles is introduced into the nozzle to form a concentrated abrasive jet slurry (0.5 to 2.5 kg/L, or 4 to 20 lb/gal.). The momentum of the waterjet as it travels toward the nozzle is transferred to the solid particles, and thus their velocities are rapidly increased.

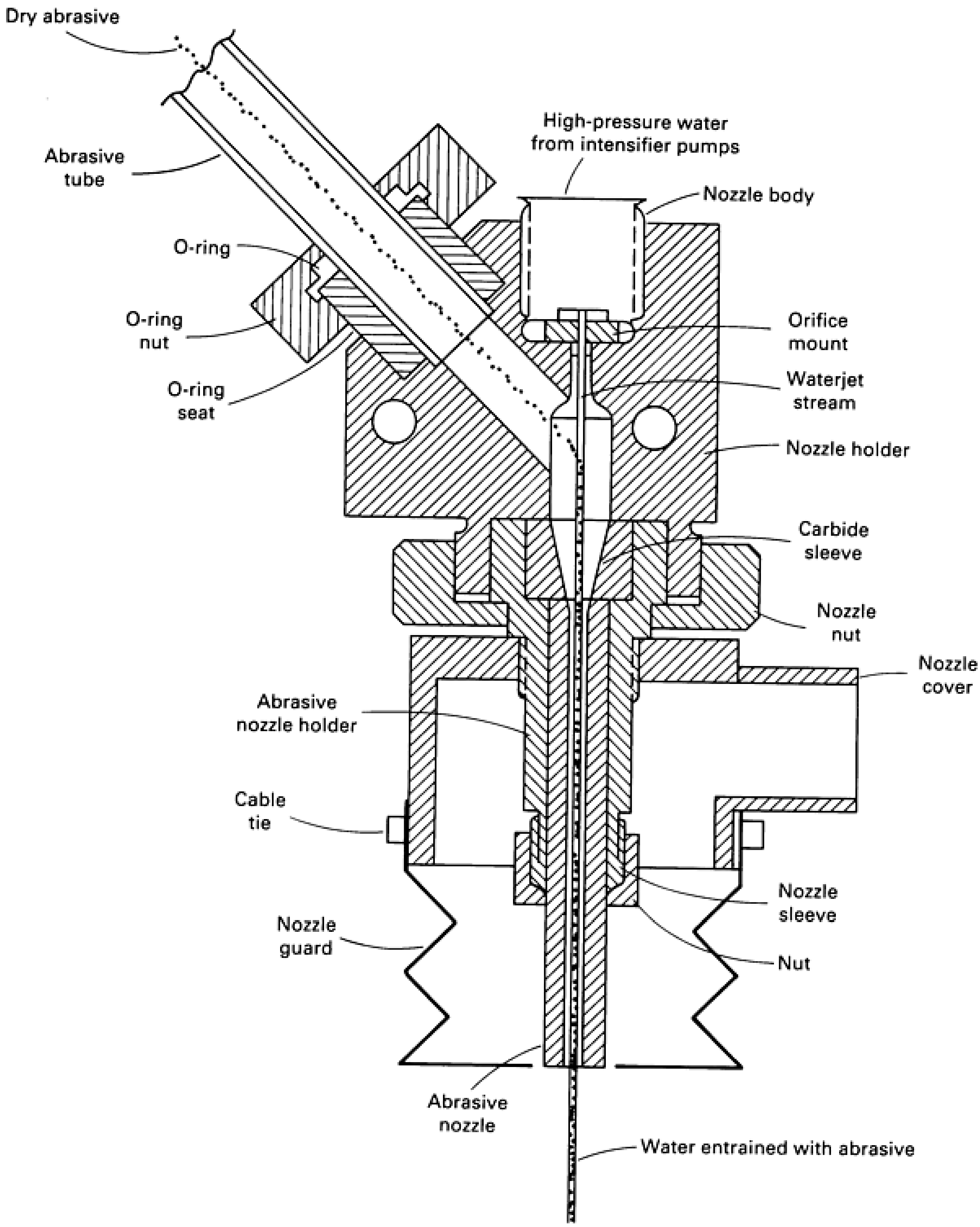

Fig. 2 Cross-sectional view of abrasive nozzle assembly showing path of water and abrasives.

This momentum transfer between the waterjet and the abrasive is a complex phenomenon. There is a limited dynamic stability of the high-pressure waterjet, and it breaks into droplets that accelerate the solid particles. In addition, the solid particles impose drag forces on the waterjet.

The result of this momentum transfer between the water and the abrasive particles is a focused high-velocity stream of abrasive. The cutting rate is controlled by changing the feed rate, the standoff distance, the waterjet pressure, or the abrasives.

Abrasive System Components

The primary components of an abrasive waterjet cutting system are the dual intensifier pump, the nozzle assembly, and the abrasive catcher assembly. These components are connected by a network of hoses and swivels and are controlled by a system of control valves and sensors (Fig. 3).

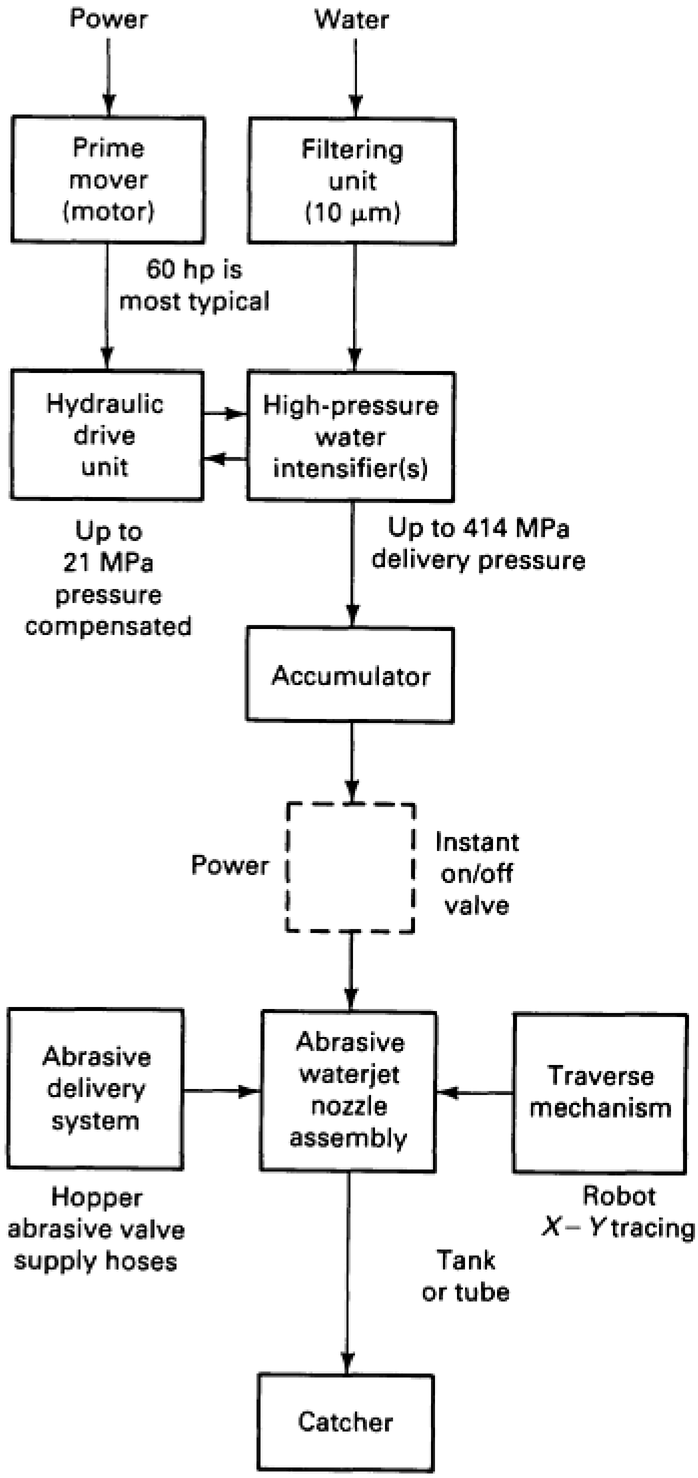

Fig. 3 Block diagram of abrasive waterjet system components.

Dual Intensifier Pump. A single or dual intensifier pump driven by a 45 kW (60 hp) motor creates a water pressure of 207 to 414 MPa (30 to 60 ksi) and a flow rate to 13 L/min (3.5 gal./min). The motor drives a hydraulic radial piston pressure compensated pump. With a hydraulic oil reservoir of approximately 140 L (37 gal.), the pump pressurizes the hydraulic oil to 19.0 MPa (3 ksi). This drives two dual water intensifiers with an intensification ratio of 20 to 1; that is, the water pressure is twenty times the oil pressure.

The pressure intensifier principle is best illustrated by the force equilibrium of the double-acting piston (Fig. 4). Hydraulic oil pressure acting on the piston results in a force on the plunger pressurizing the water in the small chamber. Force equilibrium is achieved when the water pressure equals the hydraulic oil pressure multiplied by the effective area of the piston divided by the area of the plunger (assuming no friction losses). The ratio of the effective piston area to the plunger area is termed the pressure intensification ratio. Because the intensification ratio is constant by virtue of the fixed piston-to-plunger-diameter ratio, water pressure can be regulated by controlling the hydraulic oil pressure.

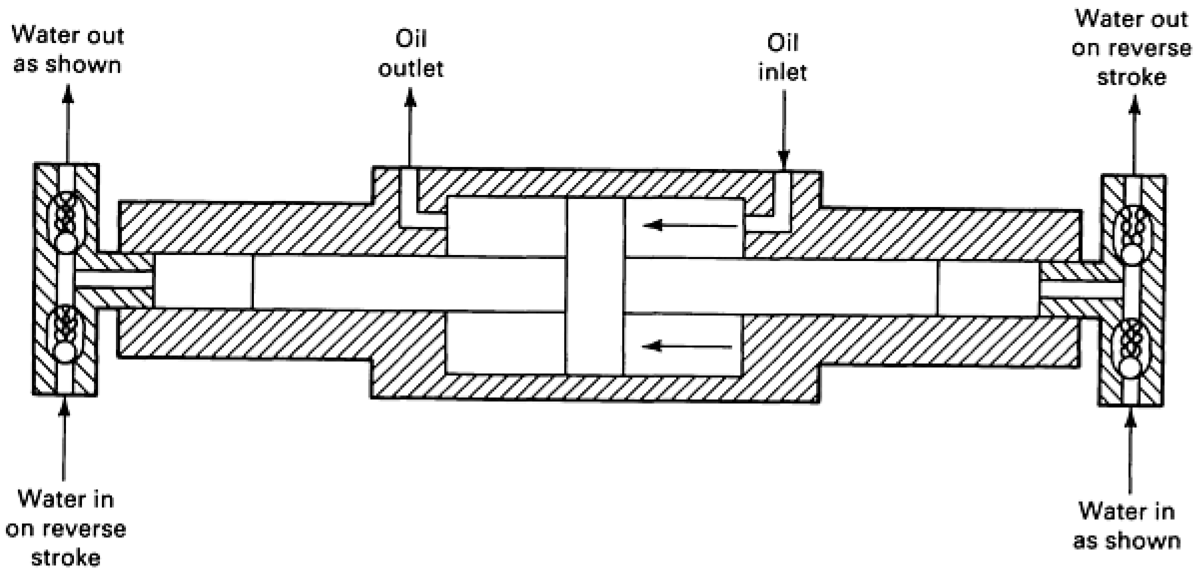

Fig. 4 Cross-sectional view of the pressurization of water to 414 MPa (60 ksi) using the fluid pressure intensifier principle.

To dampen pressure oscillations in the high-pressure output, the high-pressure water is routed to a shock attenuator. Sensors are installed throughout the intensifier pump to monitor strategic flow rates, temperatures, fluid levels, and operating conditions. Before the water enters the intensifier pumping system, it is filtered in three stages from 10 to 1 to 0.05 μm (400 to 40 to 2 μin.) to remove small particles of matter and minerals that could damage seals and valves of the pump. It is necessary to prevent cavitation of the water as it enters the intensifier pump systems. The inlet water pressure is increased to a minimum of 410 kPa (60 psi).

The microprocessor-based controller, through operator-actuated keypad commands, determines the oil pressure delivered to the intensifier by the electric motor driven hydraulic pump. Incoming filtered water is pressurized through the principle of water intensification and routed to the waterjet cutting equipment. Figure 5 illustrates the principal parts of the intensifier pump.

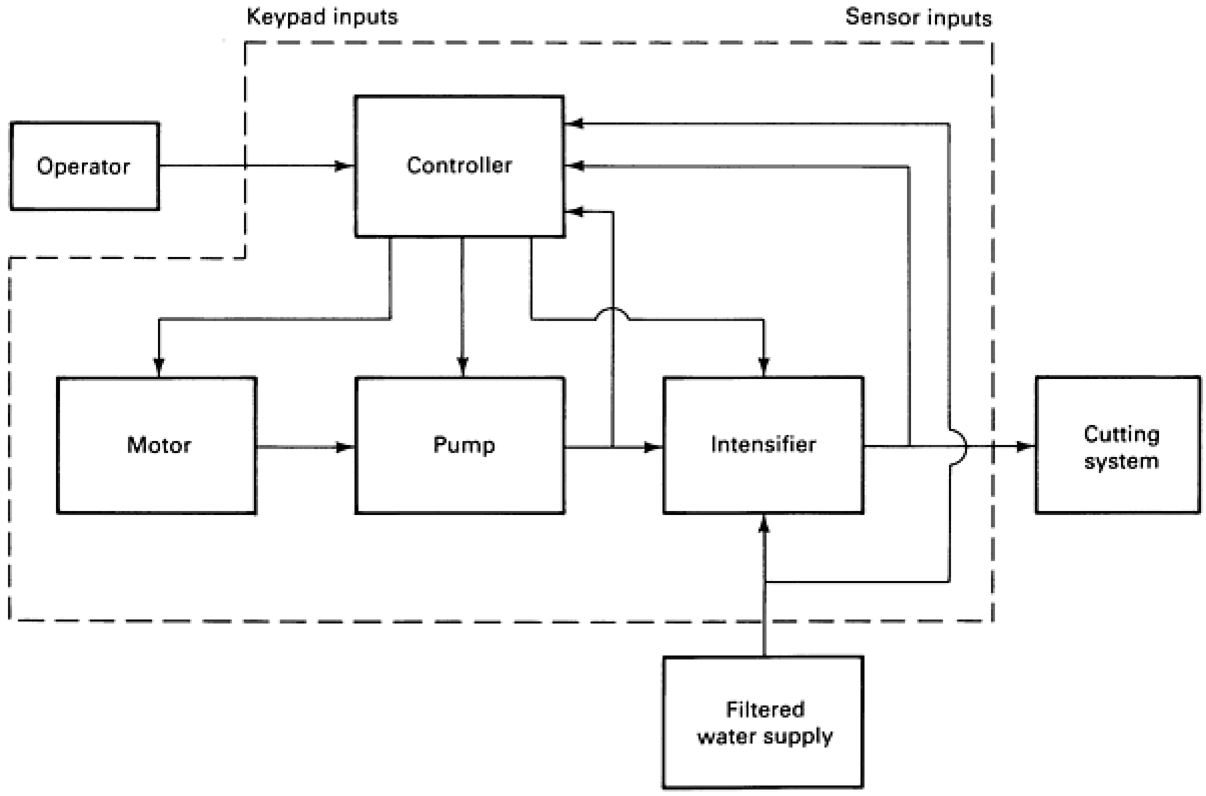

Fig. 5 Block diagram of intensifier pump assembly and its relationship to the microprocessor-based controller and cutting system.

Particle Stream Erosion Nozzle. The high-pressure water is directed through stainless steel lines and swivel joints to the particle stream erosion nozzle assembly. Here the water flows through a 0.13 to 0.51 mm (0.005 to 0.020 in.) diam sapphire jewel orifice in the mixing chamber. The dry abrasive particles are fed into the mixing chamber to become entrained in the water stream (due to the venturi effect created) and then directed through a 0.51 to 2.23 mm (0.020 to 0.090 in.) diam tungsten carbide nozzle. At this point, cutting of material takes place directly under the nozzle as both water and abrasives exit the nozzle at velocities of approximately 640 m/s (2100 ft/s) in a coherent, focused stream ranging in diameter from 1.0 to 1.5 mm (0.040 to 0.060 in.).

Operating life of the synthetic sapphire orifice, which has a 5-min replacement time, is 250 to 500 h. The operating life of the tungsten carbide abrasive nozzle is limited to 0.50 to 6 h because of the erosive effects of the accelerated water/abrasive stream.

The abrasive waterjet catcher system collects the spent fluid after it passes through the material being cut. The design of the catcher system is based on whether the cutting system uses a stationary nozzle or a moving nozzle. For a stationary nozzle, the workpiece is fed to the cutting operation, and a tank is used to collect the spent fluid (Fig. 6). A moving nozzle can be used with the same type of setup if the cutting area is contained within the tank area. The tank should be lined with ceramic pieces to suppress the cutting or piercing of the tank lining by the abrasive waterjet. Multiple pieces of concrete block, brick, thick slate, and white iron have been used to alleviate this problem. The pieces work well with a moving nozzle, but must be moved or replaced at varied intervals. Abrasives settle to the bottom, and the tank requires periodic cleaning. The accumulated water is drawn off through a valve placed low in the tank wall.

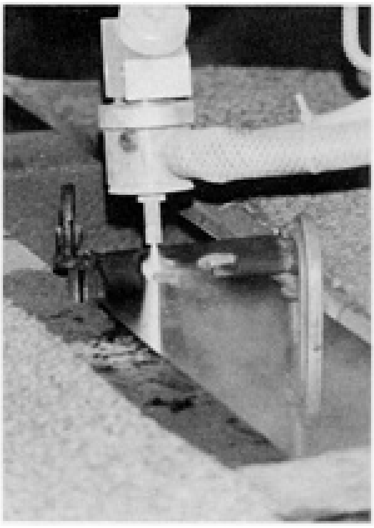

Fig. 6 Stationary waterjet nozzle cutting through a movable workpiece (René 100 alloy gating contacts). Courtesy of Department of Industrial and Manufacturing Engineering, University of Rhode Island.

A system incorporating a funnel-shaped catcher containing metallic shot to disperse the energy of the liquid has been designed for use with a movable nozzle. This device has a relatively long life expectancy as a catcher.

Abrasive Waterjet Cutting

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Abrasives

Surface finish is an important part of the performance, wear, and appearance of a product. Parts may perform better or may have a higher fatigue strength or a better appearance with a higher degree of surface finish.

Two distinct surface textures are produced when cutting with an abrasive waterjet. The top half of the thickness may have a surface roughness of 3.3 to 10 μm (130 to 400 μin.). The bottom half of the cut may have striations formed by the exit of the garnet from the workpiece (Fig. 7).

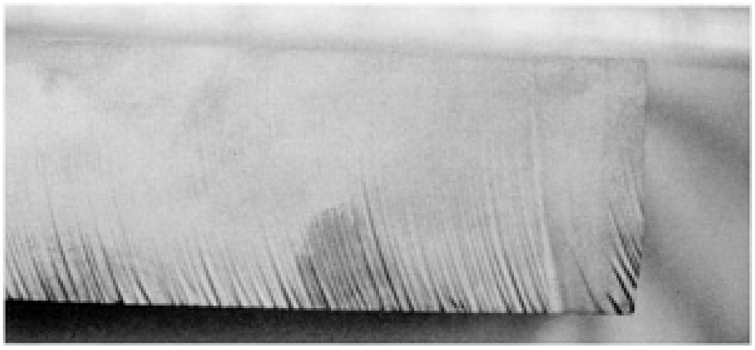

Fig. 7 Striations formed by the exit of garnet abrasives at the bottom of a workpiece. Material is 22 mm ($\frac{7}{8}$ in.) thick 4340 steel cut at 38 mm/min ($1\frac{1}{2}$ in./min). Top surface has a 3.6 μm (140 μin.) surface finish; bottom surface has a 4.7 μm (185 μin.) surface finish. Courtesy of Department of Industrial and Manufacturing

Engineering, University of Rhode Island.

Garnet Versus Silica. Tables 1 and 2 list surface finish ranges for garnet and silica abrasives. The following conclusions can be drawn from these data:

- Traverse cutting speed is the most important variable affecting surface finish
- Flow rate is the second most significant variable affecting surface finish
- Pressure is an important variable affecting surface finish, but is dependent on the specific garnet size
- Garnet will cut faster than a comparably sized silica abrasive
- A 60 grit garnet will cut 23.5% faster than a 60 to 80 grit silica abrasive
- Silica sands produce a higher-quality surface finish than garnet
- The highest-quality surface finish occurs with the slowest comparable cutting speed and at the highest flow rate for both garnet and silica

Table 1 Surface finish range for various grits of garnet abrasives

Flow rate kg/min (lb/min)	**Nozzle pressure MPa (ksi)**	**Cutting speed, %**			Surface finish, grit									
					36		**60**		**80**		**100**		150	
		100	**80**	**50**	**µm**	**µin.**	**µm**	**µin.**	**µm**	**µin.**	**µm**	**µin.**	**µm**	µin.
0.45 (1.0)	207 (30)	X			8.9-10.2	350-400	8.1-8.9	320-350	7.1-8.6	280-340	4.1-4.6	160-180	4.3-4.6	**170-180**
			X		8.6-9.7	340-380	8.6-9.4	340-370	6.6-7.4	260-290	4.3-4.6	170-180	4.2-4.6	**165-180**
				X	7.6-8.4	300-330	6.9-7.4	270-290	5.6-6.4	220-250	4.3-4.6	170-180	2.9-3.2	**115-125**
	241 (35)	X			8.1-9.4	320-370	8.1-8.9	320-350	6.9-7.6	270-300	4.8-5.6	190-220	4.1-4.3	**160-170**
			X		5.8-6.6	230-260	7.1-8.1	280-320	6.9-7.6	270-300	4.6-5.1	180-200	3.4-3.8	**135-150**
				X	5.8-6.6	230-260	7.4-8.1	290-320	5.1-5.8	200-230	4.1-4.4	160-175	3.3-3.6	**130-140**
	276 (40)	X			7.1-8.4	280-330	7.1-7.9	280-310	7.1-8.4	280-330	4.3-4.6	170-180	3.7-4.1	**145-160**
			X		7.1-7.4	280-290	8.1-9.1	320-360	6.4-7.4	250-290	4.7-5.1	185-200	3.3-4.1	**130-145**
				X	6.6-	260-	6.9-	270-	5.8-	230-	4.1-	160-	2.7-	**105-**

					7.1	280	7.6	300	6.4	250	4.6	170	3.0	**120**
0.68 (1.5)	207 (30)	X			7.1-7.9	280-310	8.1-8.9	320-350	5.3-6.1	210-240	4.4-5.3	175-210	4.1-4.4	**160-175**
			X		6.9-7.9	270-310	7.6-8.4	300-330	5.1-5.8	200-230	4.8-5.5	190-215	3.9-4.4	**155-175**
				X	5.6-6.4	220-250	6.4-7.1	250-280	4.6-5.1	180-200	4.4-4.7	175-185	2.9-3.2	**115-125**
	241 (35)	X			7.1-7.6	280-300	7.1-8.1	280-320	6.6-7.1	260-280	4.3-4.6	170-180	3.0-3.4	**120-135**
			X		7.4-7.9	290-310	7.4-8.4	290-330	5.3-6.1	210-240	4.4-4.7	175-220	3.3-3.6	**130-140**
				X	6.1-6.6	240-260	6.4-7.1	250-280	4.8-5.6	190-220	4.6-4.8	180-190	3.0-3.4	**120-135**
	276 (40)	X			7.1-8.6	280-340	6.6-7.9	260-310	6.1-6.6	240-260	4.3-4.6	170-180	3.4-3.7	**135-145**
			X		7.1-7.9	280-310	8.1-8.9	320-350	5.3-6.1	210-240	4.4-4.7	175-185	3.3-3.7	**130-145**
				X	6.1-7.1	240-280	6.4-7.1	250-280	5.1-5.8	200-230	3.6-3.9	140-155	2.9-3.2	**115-125**
0.91 (2.0)	207 (30)	X			7.6-8.6	300-340	6.4-7.1	250-280	5.1-5.8	200-230	4.3-4.6	170-180	4.4-5.3	**175-210**
			X		6.6-7.1	260-280	6.4-7.4	250-290	4.8-5.6	190-220	4.8-6.1	190-240	3.8-4.3	**150-170**
				X	6.1-6.6	240-260	6.1-6.9	240-270	4.8-5.3	190-210	3.7-4.1	145-160	2.4-2.8	**95-110**
	241 (35)	X			7.4-8.1	290-320	7.4-8.4	290-330	5.8-6.9	230-270	4.1-4.4	160-175	4.1-4.3	**160-170**
			X		6.4-7.4	250-290	7.1-8.1	280-320	5.1-5.8	200-230	3.7-4.3	145-170	4.4-4.7	**175-185**
				X	5.6-6.1	220-240	6.6-7.1	260-280	4.8-5.6	190-220	3.2-3.6	125-140	3.3-3.8	**130-150**

	276 (40)	X			6.9-7.4	270-290	7.4-8.1	290-320	4.8-5.6	190-220	3.8-4.4	150-175	3.4-4.1	**135-160**
			X		5.8-6.9	230-270	7.1-8.1	280-320	4.6-5.3	180-210	3.7-4.2	145-165	4.2-4.7	**165-185**
				X	**5.8-6.6**	**230-260**	**6.4-6.9**	**250-270**	**4.8-5.6**	**190-220**	**3.2-3.6**	**125-140**	**2.9-3.2**	**115-125**

36 grit surface finish values read off 1000-unit scale; 60, 80, 100, and 150 grit values read off 300-unit scale.

Table 2 Surface finish range for various grits of silica abrasives

Flow rate kg/min (lb/min)	**Nozzle pressure MPa (ksi)**	**Cutting speed, %**			Surface finish, grit					
					35-60		**60-80**		80-120	
		100	**80**	**50**	**µm**	**µin.**	**µm**	**µin.**	**µm**	µin.
0.45 (1.0)	207 (30)	X			4.1-4.8	160-190	5.2-5.7	205-225	4.8-5.7	**190-225**
			X		7.0-7.2	275-285	5.6-6.0	220-235	4.3-4.7	**170-185**
				X	5.7-6.1	225-240	5.0-5.7	195-225	3.9-4.3	**155-170**
	241 (35)	X			6.5-7.2	255-285	5.5-5.8	215-230	4.1-4.4	**160-175**
			X		6.2-7.0	245-275	4.6-5.5	180-215	4.1-4.4	**160-175**
				X	5.5-6.1	215-240	4.6-5.0	180-195	3.3-3.8	**130-150**
	276 (40)	X			6.5-7.1	255-280	5.3-5.7	210-225	4.3-4.6	**170-180**
			X		5.8-6.4	230-250	4.8-5.7	190-210	3.9-4.4	**155-175**
				X	5.7-6.5	225-255	4.6-4.8	180-190	3.6-3.9	**140-155**
0.68 (1.5)	207 (30)	X			5.8-6.2	230-245	5.8-6.1	230-240	4.4-4.8	**175-190**
			X		6.0-6.5	235-255	4.6-5.3	180-210	4.2-4.6	**165-180**
				X	6.0-6.7	235-265	4.3-4.7	170-185	3.0-3.4	**120-135**

	241 (35)	X			5.7-6.1	225-240	5.3-5.8	210-230	3.6-3.9	**140-155**
			X		5.8-6.2	230-245	4.6-5.1	180-200	3.4-3.8	**135-150**
				X	5.6-6.1	220-240	4.4-4.7	175-185	3.2-3.6	**125-140**
	276 (40)	X			6.0-6.9	235-270	4.8-5.3	190-210	3.7-4.2	**145-165**
			X		6.0-6.5	235-255	4.7-5.2	185-205	3.6-3.9	**140-155**
				X	5.8-6.5	230-255	4.3-4.6	170-180	3.3-3.8	**130-150**
0.91 (2.0)	207 (30)	X			6.0-6.5	235-255	4.4-4.7	175-185	5.1-5.5	**200-215**
			X		6.1-6.7	240-265	5.0-5.3	195-210	4.2-4.6	**165-180**
				X	5.6-6.0	220-235	4.1-4.4	160-175	3.3-3.6	**130-140**
	241 (35)	X			5.8-6.6	230-260	4.3-4.6	170-180	4.4-4.8	**175-190**
			X		6.1-6.6	240-260	4.2-4.6	165-180	3.6-3.9	**140-155**
				X	5.2-5.8	205-230	3.9-4.2	155-165	3.2-3.3	**125-130**
	276 (40)	X			6.0-6.6	235-260	4.2-4.4	165-175	3.6-4.1	**140-160**
			X		5.7-6.2	225-245	4.2-4.4	165-175	3.6-3.9	**140-155**
				X	**5.7-6.2**	**225-245**	**3.3-3.7**	**130-145**	**3.0-3.2**	**120-125**

All surface finish values read off 300-unit scale.

Thus, garnet is an effective abrasive. Silica sand may be adequate for cleaning operations involving the cutting of thin metals or composites. Abrasives such as aluminum oxide or silicon carbide should be used for tough materials such as ceramics. However, these harder abrasives generally decrease the life of the nozzle.

Angular grain shapes will shear the workpiece much more efficiently than round grain abrasives. The abrasive particles should be of uniform size.

Reclaiming of abrasives is not feasible, because much time and effort would be required in drying and then regrading the particle sizes. The amount of abrasive that would be reusable does not warrant the expense. New graded garnet and silicon sands are readily available and inexpensive.

Cleanup and disposal of the abrasive fluid can be a problem. Because the spent abrasive slurry has the consistency of mud, it is advisable to wait until the slurry is dry before attempting cleanup and disposal. Garnet is environmentally safe to dispose of. However, the particles mixed in with it from the material being cut may be classified as hazardous waste, and their disposal may be subject to both state and federal EPA regulations.

Abrasive Waterjet Cutting

J. Gerin Sylvia, Department of Industrial and Manufacturing Engineering, University of Rhode Island

Calculation of Abrasive Waterjet Speeds

The following parameters are assumed:

- Sand flow rate of 0.68 kg/min (1.5 lb/min)
- Water flow rate of 4.5 L/min (1.2 gal./min)
- Jewel size of 0.46 mm (0.018 in.) diameter

Using the continuity of momentum equation and assuming that the water is not compressed at all, the theoretical (ideal) velocity of the water exiting the jewel orifice is 461 m/s (1513 ft/s), and the theoretical velocity of the garnet and water at the nozzle is 402 m/s (1319 ft/s).

If, instead of 4.5 L/min (1.2 gal./min), it is assumed that the water flow rate is 3.84 L/min (1 gal./min)* and that the water is compressed by 8%, the actual flow rate is 3.5 L/min (0.92 gal./min). Therefore, the actual velocity of the water exiting the jewel orifice is 354 m/s (1160 ft/s), and the actual velocity of the garnet and water at the nozzle is 297 m/s (973 ft/s). The speed of sound under normal atmospheric conditions is about 335 m/s (1100 ft/s).

The following examples illustrate the effect of different water pressures and garnet flow rates on a single thickness of two different metals as well as the effects of these variables on different thicknesses of one metal.

Example 1: Analysis of Varying Water Pressures and Garnet Flow Rates on 9.5 mm ($\frac{3}{8}$ in.) Thick 6061T6 Aluminum and 6.4 mm ($\frac{1}{4}$ in.) Thick Type 304 Stainless Steel.

All piercings were made with a 3.2 mm ($\frac{1}{8}$ in.) nozzle standoff having a 1.6 mm (0.062 in.) nozzle diameter. The garnet flow rates were 0.23, 0.45, 0.68, 0.79, and 0.91 kg/min (0.5, 1.0, 1.5, 1.75, and 2.0 lb/min). The water pressures were 55, 69, 103, 138, 172, and 207 MPa (8, 10, 15, 20, 25, and 30 ksi). The water pressure was held constant at 207 MPa (30 ksi) for the cutting, and the garnet flow rates were varied at 0.45, 0.68, and 0.91 kg/min (1.0, 1.5, and 2.0 lb/min).

Figures 8 and 9 show that much more time is required for piercing stainless steel than for aluminum at low water pressure. This reflects the hardness of the stainless steel. Although the two metals are not the same thickness, similarities between Fig. 8 and 9 are evident.

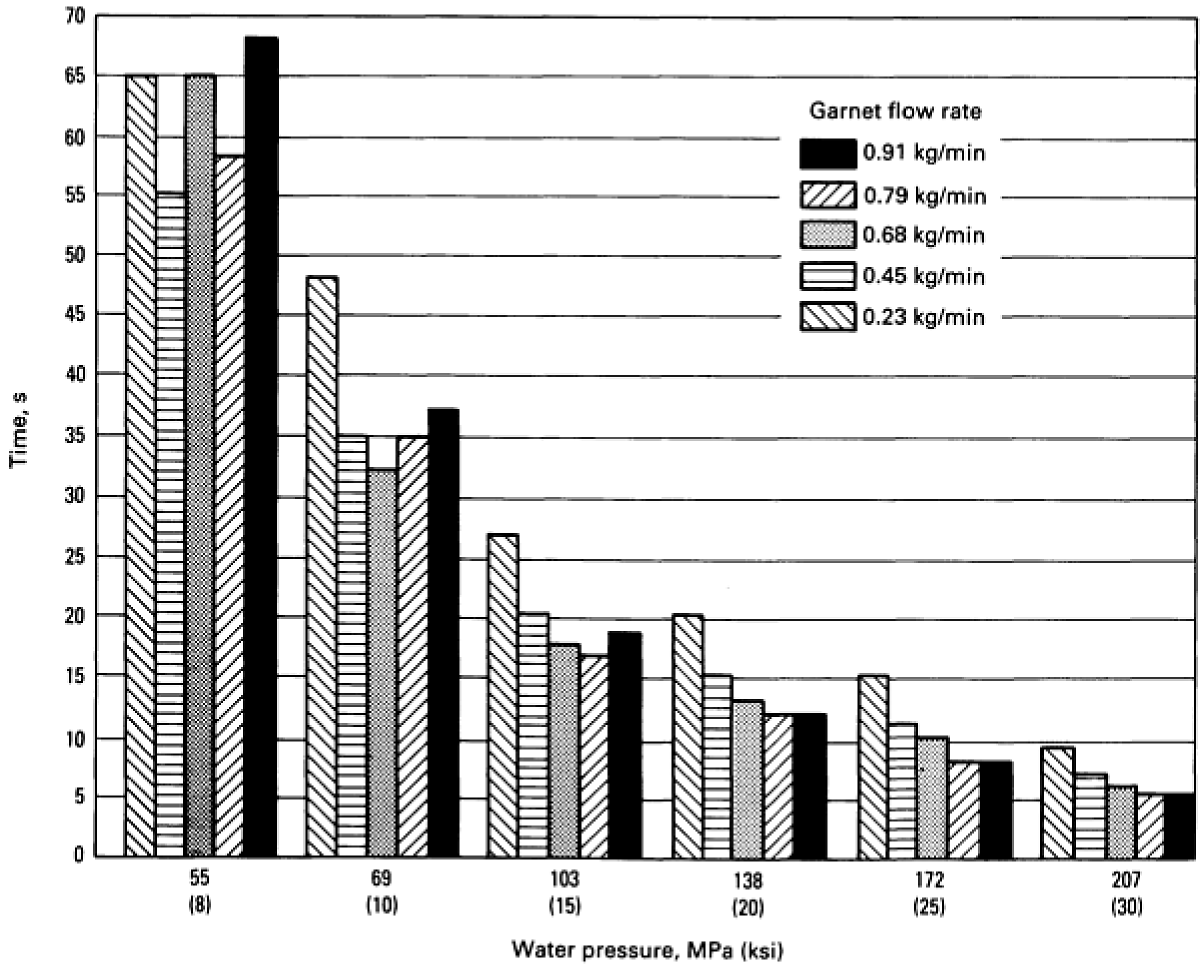

Fig. 8 Water pressures and garnet abrasive flow rates versus piercing times for 9.5 mm ($\frac{3}{8}$ in.) thick 6061 T6 aluminum. Source: Department of Industrial and Manufacturing Engineering, University of Rhode Island.

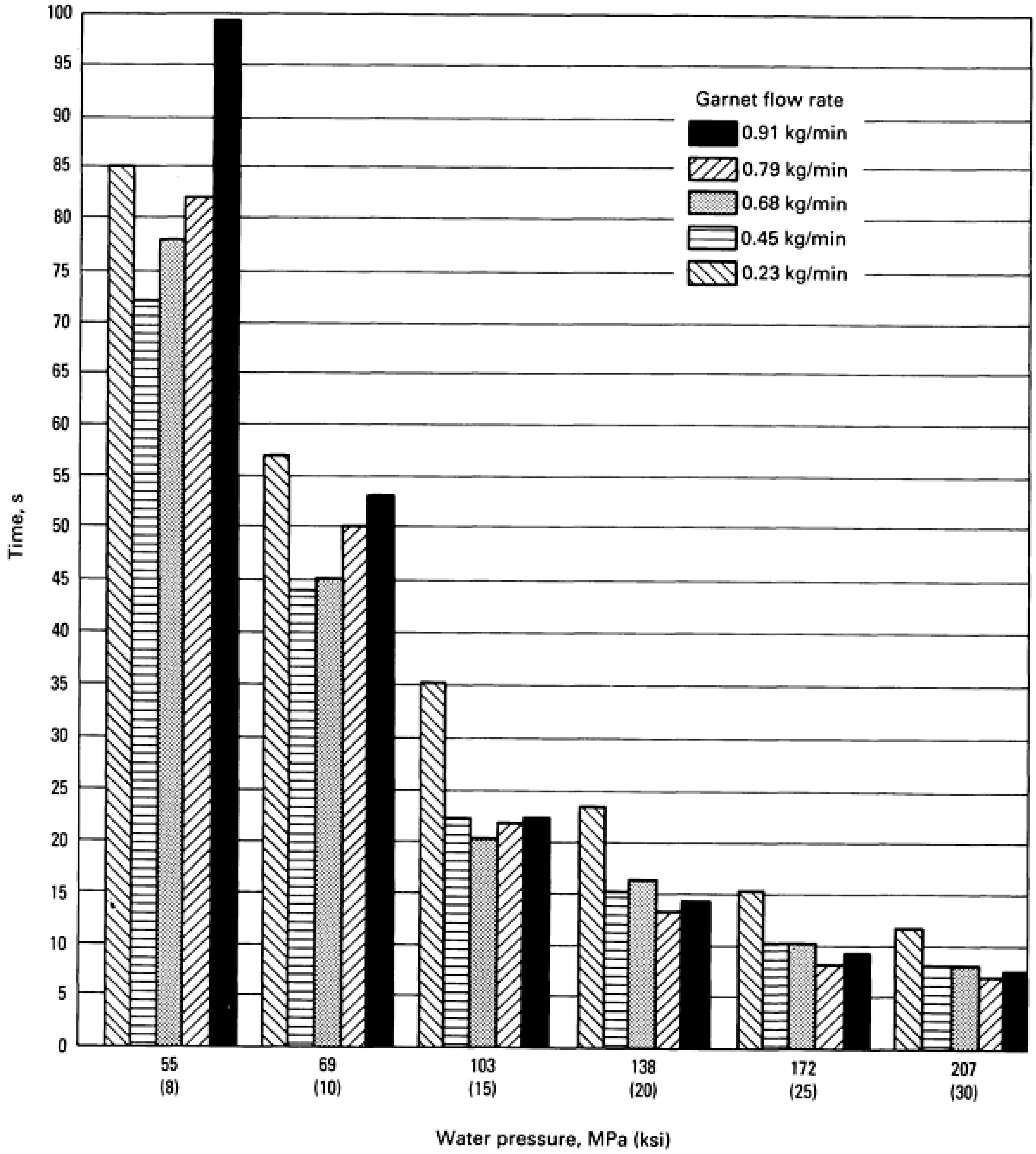

Fig. 9 Water pressure and garnet abrasive flow rates versus piercing times for 6.4 mm thick type 304 stainless steel. Source: Department of Industrial and Manufacturing Engineering, University of Rhode Island.

The garnet flow rate affects the piercing time. High garnet flow rate versus low garnet flow rate, with pressure being constant, requires a longer time for piercing.

Figure 10 shows that piercing time acts as an exponential function relating to thickness. The same trend can be seen with garnet flow rates of 0.45, 0.68, and 0.79 kg/min (1.0, 1.5, and 1.75 lb/min).

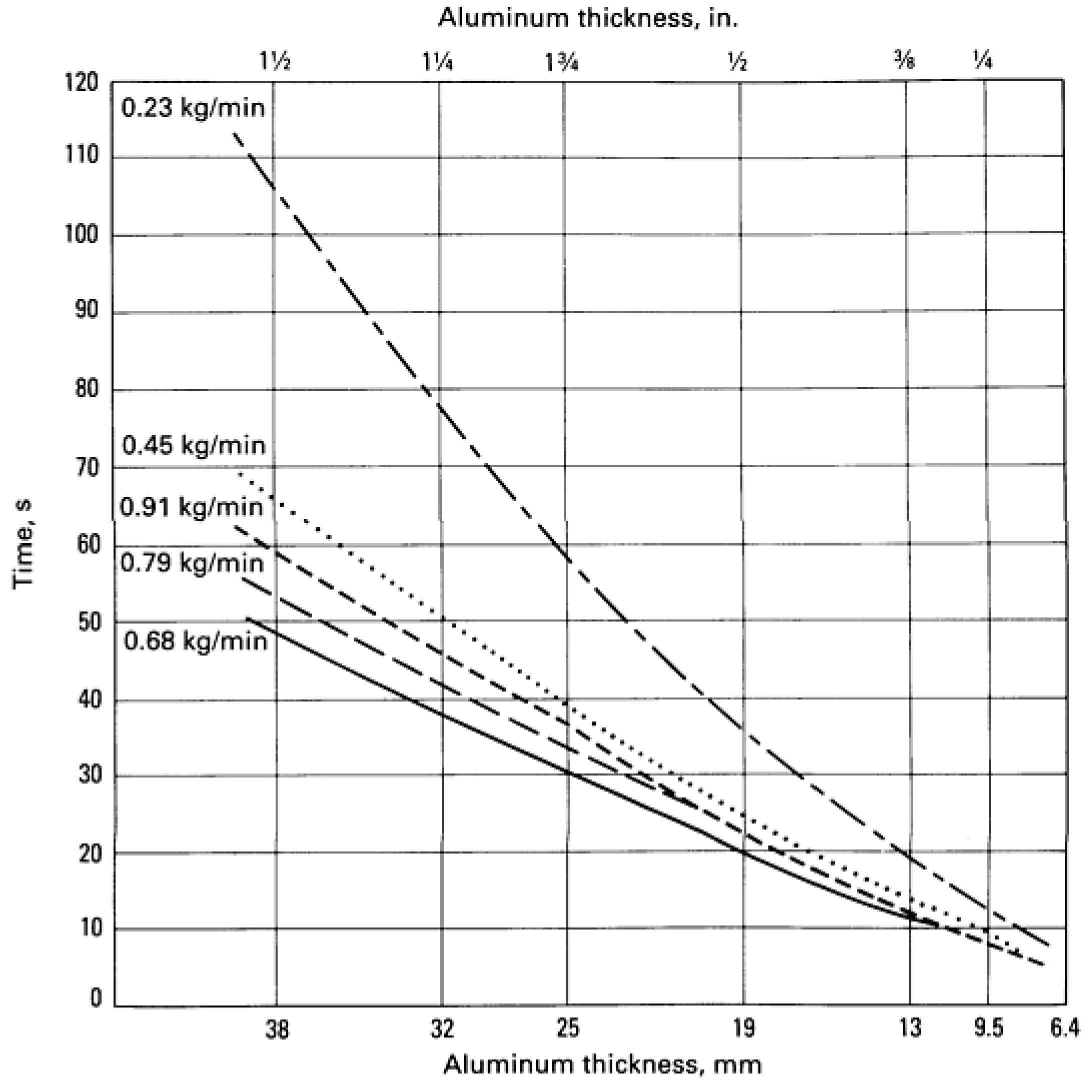

Fig. 10 Garnet abrasive flow rates and section thickness versus piercing times for 6061-T6 aluminum at a water pressure of 207 MPa (30 ksi). Source: Department of Industrial and Manufacturing Engineering, University of Rhode Island.

Figure 11 suggests that the fastest cutting rate was 134 mm/min (5.29 in./min) at garnet flow rates of 0.45 and 0.68 kg/min (1.0 and 1.5 lb/min). With a garnet flow rate of 0.68 kg/min (1.5 lb/min), the stainless steel was not cut completely through at 127 mm/min (5.0 in./min). This appears to reflect an unknown error.

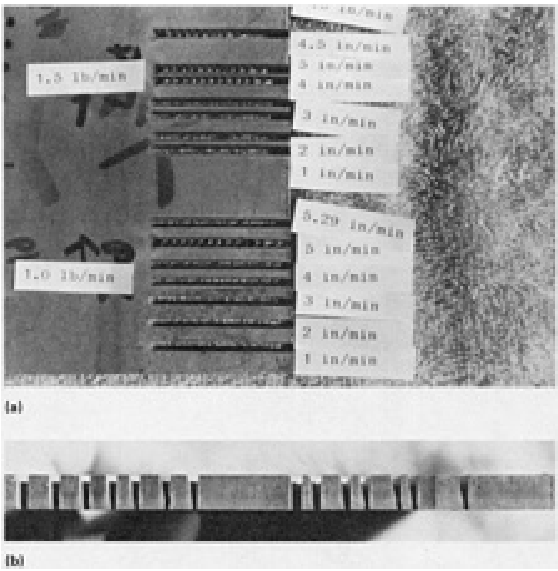

Fig. 11 Two views of 6.4 mm ($\frac{1}{4}$ in.) thick stainless steel cut at garnet flow rates of 0.45 and 0.68 kg/min (1.0 and 1.5 lb/min). (a) Top view. Top piece was cut at 250 mm/min (10 in./min). (b) Side view. Right section is 0.68 kg/min (1.5 lb/min). Left section is 0.45 kg/min (1.0 lb/min). Courtesy of Department of Industrial and Manufacturing Engineering, University of Rhode Island.

Pieces that were not cut completely through had small ridges in the half kerf of the cut. The number of ridges in the cut corresponded to an equal number of cycles of the intensifier pump.

At cutting speeds greater than 102 mm/min (4 in./min), a noticeable taper was evident in the kerf width. At cutting speeds less than 102 mm/min (4 in./min), no significant taper was evident. Varying the garnet flow rates had no significant effect on the width of the kerf.

The aluminum (Fig. 12) was cut at 305 mm/min (12 in./min) with garnet flow rates of 0.45 and 0.91 kg/min (1.0 and 2.0 lb/min). When using 0.45 kg/min (1.0 lb/min) and a cutting speed of 255 mm/min (10 in./min), there was difficulty in cutting completely through the piece.

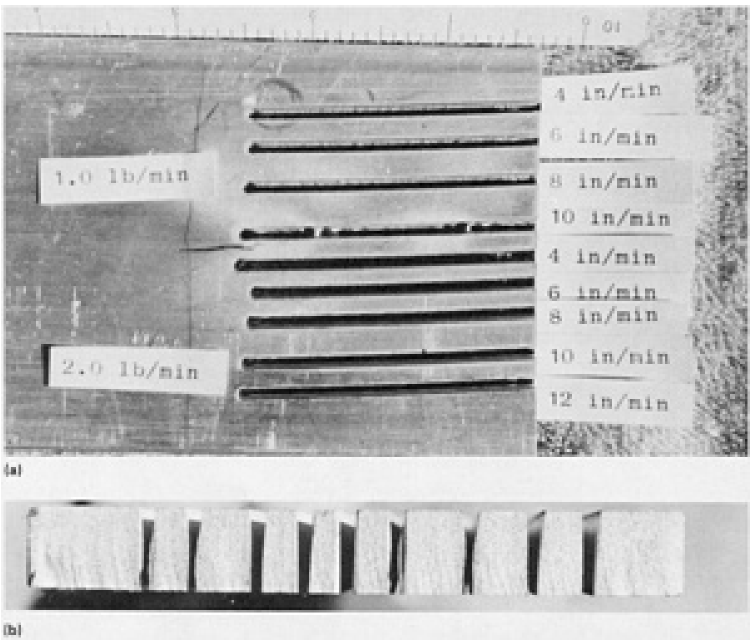

Fig. 12 Two views of 9.5 mm ($\frac{3}{8}$ in.) thick 6061 T6 aluminum cut at garnet flow rates of 0.45 and 0.91 kg/min (1.0 and 2.0 lb/min). (a) Top view. The top four cuts are at 0.45 kg/min (1.0 lb/min). The bottom five cuts are at 0.91 kg/min (2.0 lb/min). (b) Side view. Right four cuts are at 0.45 kg/min (1.0 lb/min); left five cuts are at 0.91 kg/min (2.0 lb/min). Courtesy of Department of Industrial and Manufacturing Engineering, University of Rhode Island.

Similar characteristics were found in the study of stainless steel and aluminum. There is a rounding effect, and the kerf is larger for cuts at the same speed but increasing garnet flow. As aluminum thickness increased to 38 mm (1$\frac{1}{2}$ in.), higher water pressure was needed with an increase in the garnet flow rate.

Example 2: Analysis of Varying Water Pressures and Garnet Flow Rates on Several Thicknesses of 6061 T6 Aluminum Bar Stock.

A piece of aluminum 6061 T6 bar stock 64 mm (2$\frac{1}{2}$ in.) wide by 38 mm (1$\frac{1}{2}$ in.) thick and 610 mm (2 ft) long was machined down to provide step thicknesses of 6.4, 9.5, 13, 19, 25, 32, and 38 mm ($\frac{1}{4}$, $\frac{3}{8}$, $\frac{1}{2}$, $\frac{3}{4}$, 1, 1$\frac{1}{4}$, and 1$\frac{1}{2}$ in.), as shown in Fig. 13(a).

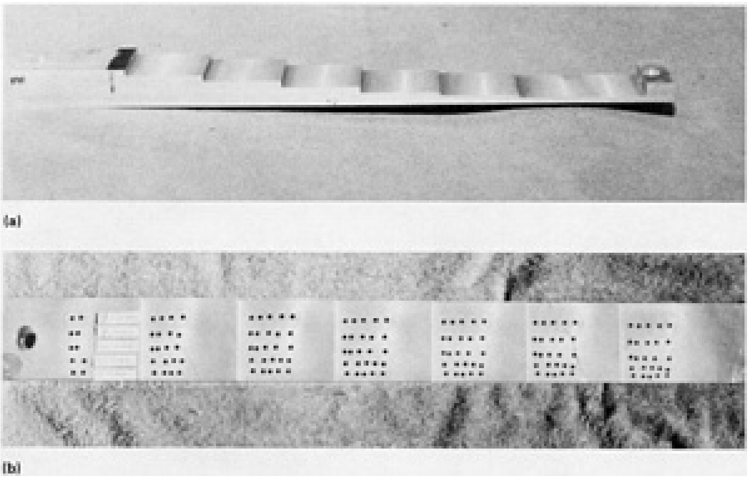

Fig. 13 Two views of various thicknesses of 6061 T6 aluminum used in piercing studies with varying water pressure and garnet flow rates used on each thickness. (a) Three-dimensional view of workpiece showing varying thickness of 640 mm (2 ft) long slab before piercing. From left to right, slab thicknesses are 38, 32, 25, 19, 13, 9.5, and 6.4 mm (1$\frac{1}{2}$, 1$\frac{1}{4}$, 1, $\frac{3}{4}$, $\frac{1}{2}$, $\frac{3}{8}$, and $\frac{1}{4}$ in.). (b) Top view of piece after piercing was performed. Left side is 38 mm (1$\frac{1}{2}$ in.) section. Courtesy of Department of Industrial and Manufacturing Engineering, University of Rhode Island.

Each section was pierced at several garnet flow rates with varying water pressures. The water pressures were 83, 103, 138, 172, and 207 MPa (12, 15, 20, 25, and 30 ksi). The garnet flow rates were 0.23, 0.45, 0.68, 0.79, and 0.91 kg/min (0.5, 1.0, 1.5, 1.75, and 2.0 lb/min).

The times were recorded using a digital timer connected to the robot controller. A new 1.6 mm (0.062 in.) nozzle was used with a 3.2 mm ($\frac{1}{8}$ in.) standoff.

An evaluation of the piercings illustrated that the entrance and exit hole sizes reflected the water pressure, sand flow, and section thickness (Fig. 13b). With the 0.23 kg/min (0.5 lb/min) garnet flow rate, there was a slight overall decrease in hole size as the section thicknesses were reduced.

Note cited in this section

* This flow rate is calculated by counting the number of times the intensifier pump cycled in 1 min. With this particular configuration, it cycled 50 times. The result obtained is (50 cycles/min) (0.076 L/cycle) = 3.8 L/min, or in English units: (50 cycles/min) (0.02 gal./cycle) = 1.0 gal./min.

Abrasive Waterjet Cutting

J. Gerin Sylvia, Department of Industrial and Manufacturing Engineering, University of Rhode Island

Factors Affecting Cut Quality

Bending of the kerf can be readily seen in the Plexiglass piece shown in Fig. 14. This bending motion occurs because the abrasive stream loses energy as it cuts and travels further down into the target piece and becomes progressively less efficient. Meanwhile, the nozzle is still moving and cutting the material at the top, and this forces the kerf to bend away from the direction of the motion of the nozzle. At high traverse speeds--especially in thick materials--this bending of the kerf will be most noticeable at the very end of the material.

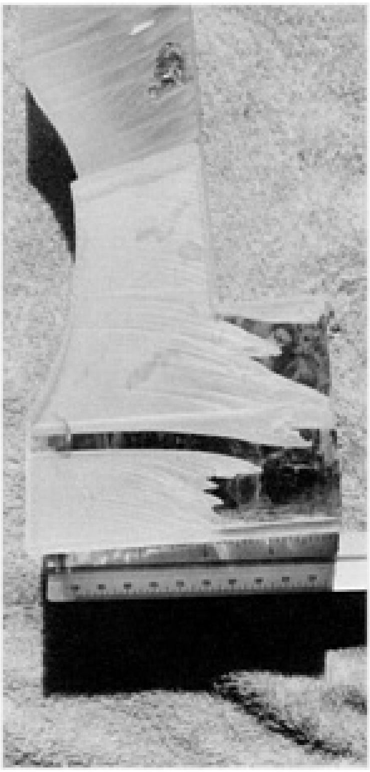

Fig. 14 Bending of kerf in a Plexiglass workpiece subjected to excessively high traverse cutting speeds. The portion at the bottom was pierced before cutting, and this caused a section of the hole to be eroded away as it was subsequently cut by abrasive waterjet nozzle. Courtesy of Department of Industrial and Manufacturing Engineering, University of Rhode Island.

This is the case with the 50 mm (2 in.) thick semihardened steel section shown in Fig. 15. Because the bend is a smooth one, the cutting action at the bottom depends on the continual cutting at the top. When the end of the material is reached, there is a sudden interruption in this bend, and the jet stream is no longer deflected onto the uncut bottom section. This leaves an uncut crescent-shaped piece at the very bottom of the end of the cut that must be cut again or broken apart by hand. This problem can be alleviated if the computer program of the robot includes provisions for either slowing the traverse rate at the very end of the cut so that the stream is barely bent and/or changing the approach angle of the nozzle at the very end of the cut.

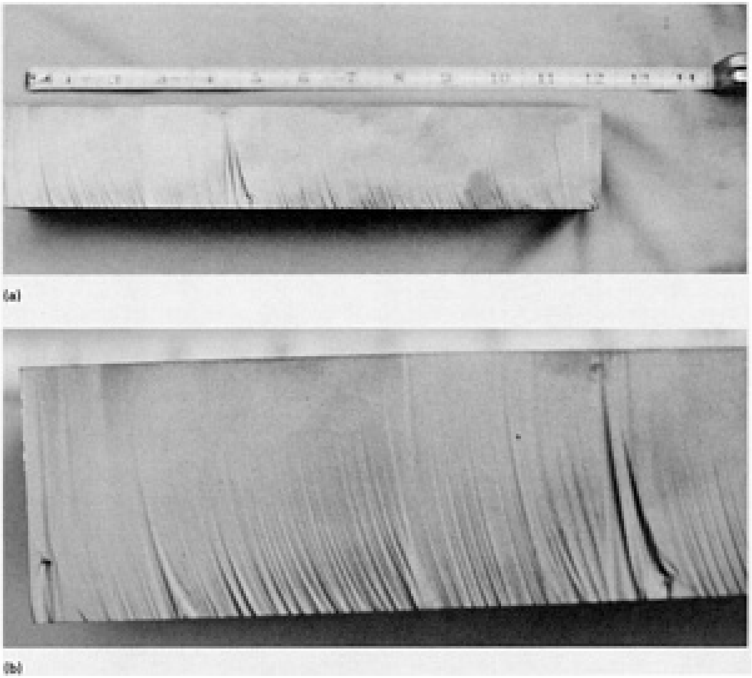

Fig. 15 Crescent-shaped striations in a 50 mm (2 in.) thick piece of semihardened steel cut at 22.3 mm/min (0.88 in./min). Direction of cut is from top of picture to bottom. (a) Waterjet failed to cut completely through the steel in two places (far left as well as at the 108 mm, or $4\frac{1}{4}$ in., mark). (b) Close-up of workpiece shows striations caused by bending of kerf, a result of the high traverse rate of the abrasive waterjet. Courtesy of Department of Industrial and Manufacturing Engineering, University of Rhode Island.

Incomplete Initial Cuts. The effects of an incomplete first cut can be seen in the Plexiglass shown in Fig. 14 and in the cut tantalum-silicon piece shown in Fig. 16. A very poor surface finish can be expected at the bottom of the cut if the abrasive waterjet stream does not cut completely through the material on the first attempt. At the bottom of the cut, where the cutting power of the garnet is expended, there are rough ridges, and the kerf width is greatly expanded because of the bouncing around of the excess garnet. This wider kerf and expanded area are especially noticeable on the cut surfaces of the tantalum-silicon piece (Fig. 16).

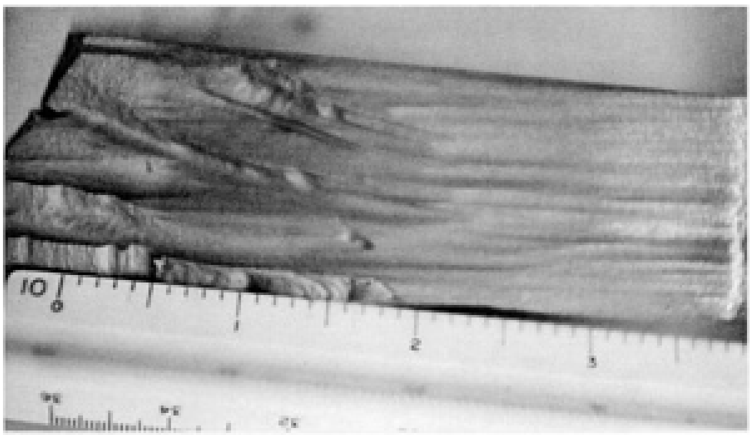

Fig. 16 Poor surface finish on a tantalum-silicon workpiece resulting from an incomplete initial cut by the abrasive waterjet stream. Left side is bottom of cut. Courtesy of Department of Industrial and Manufacturing

Engineering, University of Rhode Island.

Special Precautions for Thicknesses Over 6.4 mm ($\frac{1}{4}$ in.). Tantalum-silicon and Plexiglass samples up to 102 mm (4 in.) thick have been successfully cut with an abrasive waterjet. In cutting any material over approximately 6.4 mm ($\frac{1}{4}$ in.) thick, special precautions should be taken if a corner cut is to be made in the piece or if the material is to be pierced and then line cut.

When the Plexiglass piece shown in Fig. 14 was pierced, the hole itself was very smooth. However, when a traverse motion was initiated after piercing, the bending of the stream caused a section to the left of the hole to be cut away. This was not the desired effect. The same problem can occur when attempting a turn in the middle of the piece. The stream cannot simply bend around the turn. The nozzle must be stopped for a sufficient amount of time to allow the bend to straighten out, and then the nozzle can continue its motion.

Work Hardening. Abrasive waterjet machining alters the hardness of the cut surface of a number of metals. This slight work hardening is indicated in Table 3.

Table 3 Effect of abrasive waterjet cutting on surface hardening of metals

Metal	Hardness[a]	
	Base	Abrasive waterjet cut
Titanium	34 HRC	**34.3 HRC**
Aluminum 6061 T6	54.8 HRB	**58.7 HRB**
Magnesium	54.1 HRB	**58.4 HRB**
Carbon steel A-572	82.8 HRB	**84.5 HRB**
Tool steel	**91.5 HRB**	**93.3 HRB**

Source: M. Hashish, "Application of Abrasive Waterjets to Metal Cutting," Flow Industries, Inc., 1986

(a) Average of five measurements.

Abrasive Grit Size. Figure 17 shows how abrasive flow rates and grit number affect the depth of cut when machining cast iron. Figure 17 also indicates that neither coarse (36 and 16 grit) nor fine particles (100 and 150 grit) of garnet are the most effective abrasives. Medium abrasive of 60 and 80 grit has been found to be the most effective cutting media for a wide variety of metals.

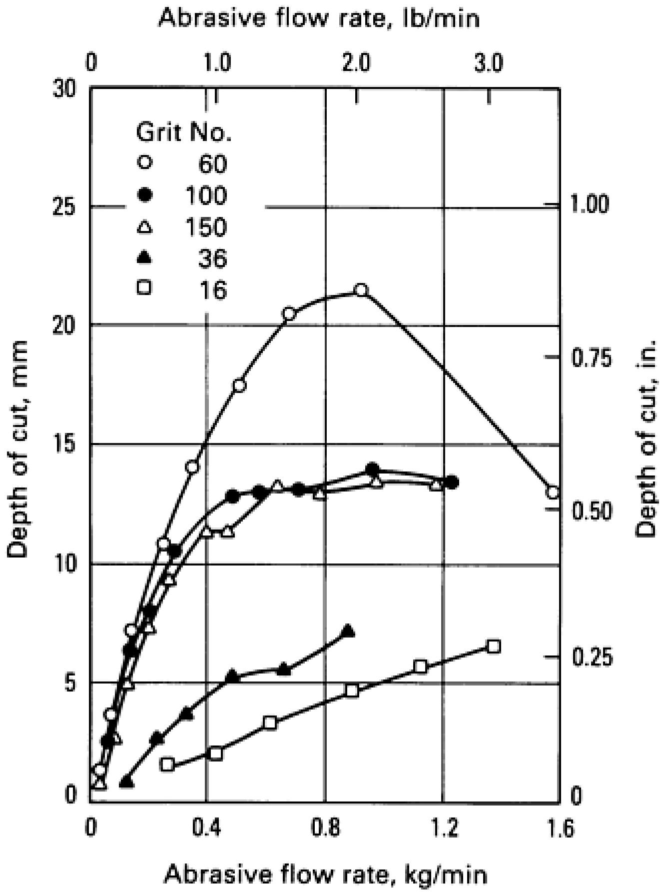

Fig. 17 Effect of abrasive flow rate and grit number on depth of cut (garnet abrasive; 220 MPa, or 32 ksi-water pressure; 0.46 mm, or 0.018 in., waterjet diameter; 152 mm/min, or 6 in./min, traverse speed; cast iron). Source: Department of Industrial and Manufacturing Engineering, University of Rhode Island.

Pressure is the most important parameter to be optimized in considering the depth of cut required in metals. Soft metals are insensitive to particle size but are very much affected by pressure. Figure 18 indicates the effect of increasing water pressure on 11 metals and a ceramic (Al_2O_3).

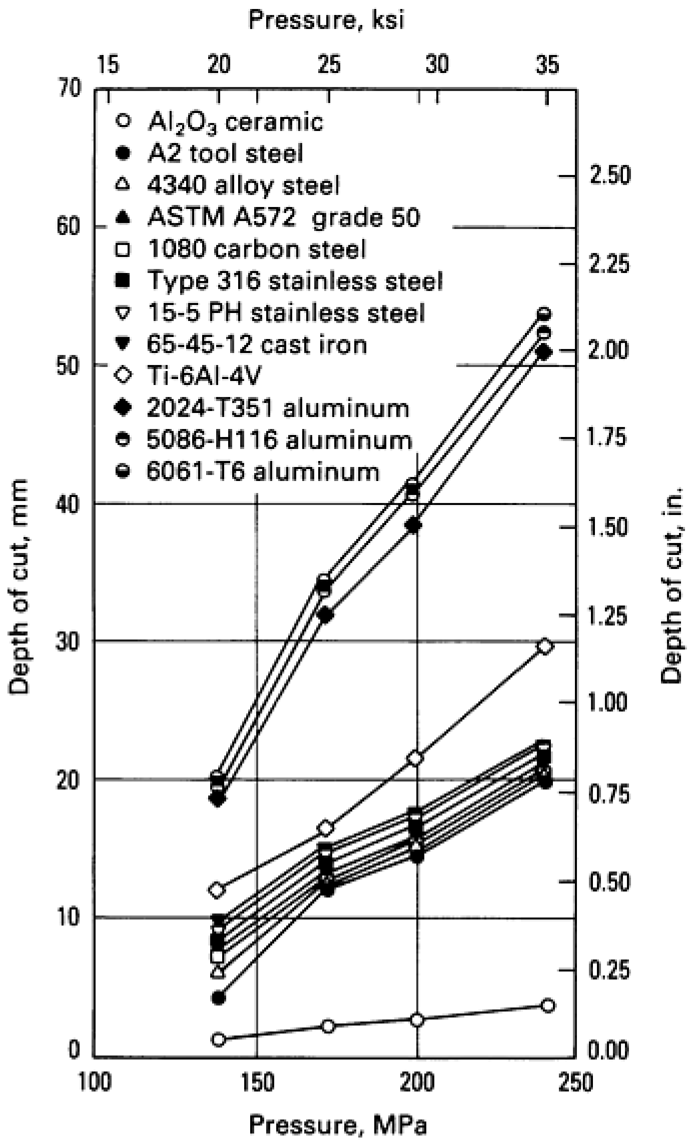

Fig. 18 Depth of cut results for different materials (60 grit garnet abrasive; 0.91 kg/min, or 2 lb/min, abrasive flow rate; 0.51 mm, or 0.020 in., waterjet diameter; 152 mm/min, or 6 in./min, traverse speed). Source: Department of Industrial and Manufacturing Engineering, University of Rhode Island.

Abrasive Waterjet Cutting

J. Gerin Sylvia, Department of Industrial and Manufacturing Engineering, University of Rhode Island

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Applications for Abrasive Waterjet Cutting

Machining of Metals and Metal-Matrix Composites. The following list summarizes the various applications for abrasive waterjet cutting:

- Foundries (removal of burned-in sand, cutting gates, and risers from cast parts)
- Naval and commercial shipyards (high-strength steel, lead, and so on)
- Railroad cars (manufacture and repair)
- Metal fabrication shops
- Aircraft manufacturers (titanium, Inconel, stacked metals)

- Heavy equipment manufacturers (tractors, hoists, cranes, industrial winches, derricks)
- Industrial vehicles (trucks, tankers, construction vehicles)
- Structural fabrications (bridges, sky-scrapers) and heavy aluminum works
- Specialty metal fabrication (titanium, nickel alloys, chromium alloys)
- Military vehicles (tanks, armored personnel carriers, landing craft)
- Oil and gas (oil well casings, pipeline repair, platform repair)
- Mining (metal structures)

The cutting speed of the abrasive waterjet has made it a valuable tool for high-volume applications. Table 4 indicates the effect of material thickness on cutting speed for eight widely used metals and an aluminum oxide ceramic.

Table 4 Effect of material thickness on abrasive waterjet cutting speed for various metals and a ceramic material

Abrasive used is garnet.

Parameters					**Material thickness mm (in.)**	Maximum cutting rate, mm/min (in./min)								
Diameters, mm (in.)		**Abrasive**		**Nozzle pressure MPa (ksi)**		**Metals**								Ceramic
Orifice	**Nozzle**	**Feed rate kg/min (lb/min)**	**Mesh size**			**Aluminum and aluminum alloys**	**Brass**	**Carbon steel**	**Copper**	**Alloy 718**	**Stainless steel**	**Titanium**	**Tool steel 38 HRC**	99.6% Aluminum oxide
0.23 (0.009)	0.79 (0.031)	0.23 (0.5)	100	310 (45)	0.8 (0.031)	4570 (180)	1270 (50)	1520 (60)	1270 (50)	1520 (60)	1140 (45)	2030 (80)	890 (35)	**127 (5)**
					1.6 (0.063)	2030 (80)	762 (30)	1270 (50)	1020 (40)	1140 (45)	762 (30)	1520 (60)	762 (30)	**61 (2.4)**
					3.2 (0.125)	1270 (50)	457 (18)	762 (30)	559 (22)	559 (22)	610 (24)	1140 (45)	635 (25)	**38 (1.5)**
0.33 (0.013)	1.19 (0.047)	0.68 (1.5)	80	240 (35)	6.4 (0.250)	762 (30)	254 (10)	508 (20)	305 (12)	305 (12)	483 (16)	762 (30)	432 (17)	**23 (0.9)**
					12.7 (0.500)	457 (18)	102 (4)	305 (12)	152 (6)	152 (6)	254 (10)	457 (18)	330 (13)	**15 (0.6)**
0.46 (0.018)	1.19 (0.047)	0.91 (2.0)	80	240 (35)	19.0 (0.750)	305 (12)	25 (1)	203 (8)	75 (3)	75 (3)	152 (6)	305 (12)	254 (10)	**8 (0.3)**
					25.4 (1.00)	203 (8.0)	13 (0.5)	152 (6)	38 (1.5)	38 (1.5)	102 (4)	152 (6)	191 (7.5)	**. . .**
0.56 (0.022)	1.57 (0.062)	1.46 (3.2)	60	240 (35)	50.8 (2.00)	152 (6.0)	8 (0.3)	75 (3)	15 (0.6)	5 (0.2)	57 (2.25)	75 (3)	127 (5)	**. . .**

(0.022)	(0.062)				76.2 (3.00)	127 (5.0)	5 (0.2)	50 (2)	8 (0.3)	3 (0.1)	38 (1.5)	50 (2)	50 (2)	. . .
					102 (4.00)	**102 (4.0)**	**3 (0.1)**	**25 (1)**	**3 (0.1)**	. . .	**25 (1)**	**25 (1)**	**25 (1)**	. . .

Source: Flow Systems, Inc.

Cost savings of up to 50% have been obtained by one aerospace manufacturer using the abrasive waterjet to cut titanium and other metals for the B1-B bomber. The parts machined have included such labor-intensive components as wingsweep fairings, upper and lower panels, weapons bay doors, and miscellaneous bonded panels. These are large and complex parts--mostly 6.4 to 9.5 mm ($\frac{1}{4}$ to $\frac{3}{8}$ in.) thick and averaging 6400 to 7600 linear mm (250 to 300 in.) of periphery--that are ideally suited to a robot system capable of precision five-axis cutting over an extensive area. Data are unavailable for the 6.4 to 9.5 mm ($\frac{1}{4}$ to $\frac{3}{8}$ in.) thick material, but the firm is currently cutting 1.6 mm (0.063 in.) titanium using 0.68 kg/min (1.5 lb/min) of 60 grit red garnet abrasive at 305 mm/min (12 in./min).

Another aerospace manufacturer uses abrasive waterjet cutting to modify C-5 transport plane wing structures to upgrade C-5As to C-5Bs. Garnet is used as the abrasive to cut through aluminum and titanium up to 64 mm (2.5 in.) thick.

Where metallurgical contamination is a concern, as in the cutting of bimetallics, abrasive waterjet machining overcomes the problems of distortion, delamination, and contamination.

Abrasive waterjet cutting is also cost effective for composites. Metal-matrix composites that are cut mechanically at 25 mm/min (1 in./min) can be cut by an abrasive waterjet at 381 to 762 mm/min (15 to 30 in./min).

Figures 19, 20, 21, and 22 illustrate the wide variety of metals (including difficult-to-machine materials such as titanium and Alloy 100) and intricate shapes (specifically the bracket shown in Fig. 21 and the turbine rotor in Fig. 22) that can be easily cut with the abrasive waterjet. The stainless steel guide bracket shown in Fig. 21 illustrates profiling around corners that would be difficult to achieve with standard machining methods. This part would require 16 or more hours to complete using conventional methods; with the abrasive waterjet, such a part can be machined within a few hours.

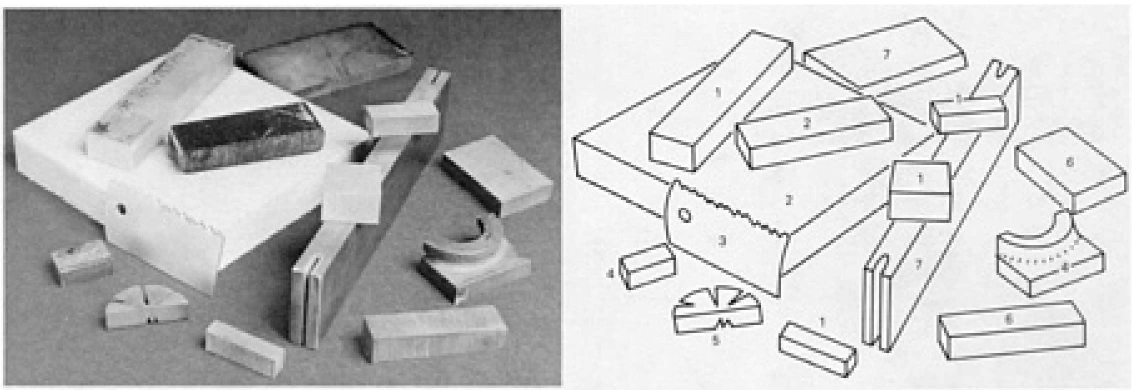

Fig. 19 Various metals plus fiberglass cut with an abrasive waterjet. 1, aluminum; 2, fiberglass; 3, hardened tool steel; 4, ductile iron; 5, titanium; 6, carbon steel; 7, stainless steel. Courtesy of Ingersoll-Rand Waterjet Cutting Systems.

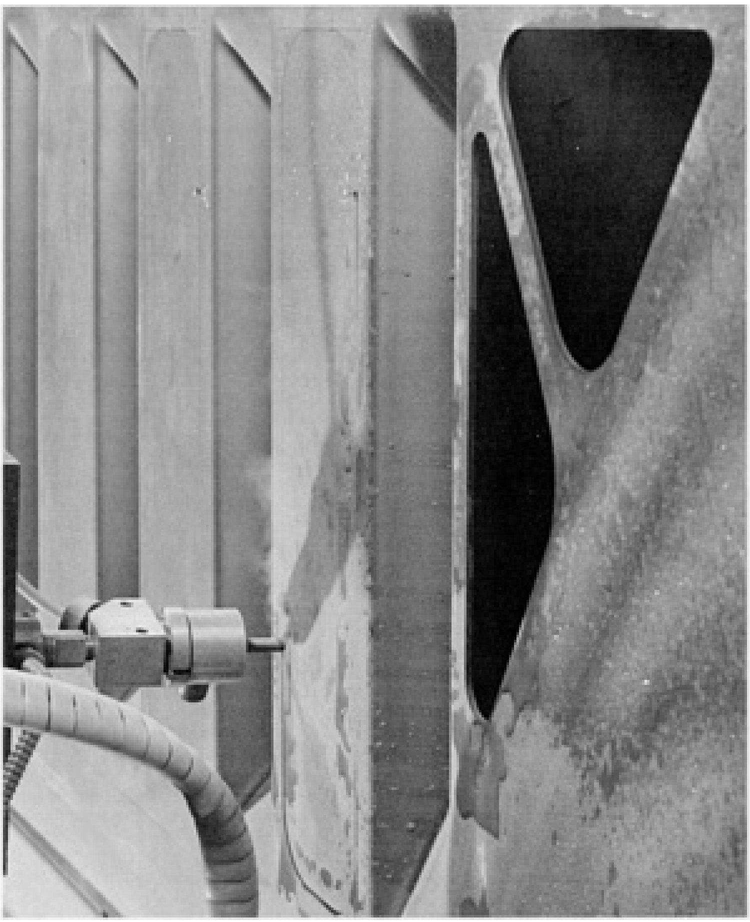

Fig. 20 Profiling of titanium for aerospace applications using an abrasive waterjet. Courtesy of Flow Systems, Inc.

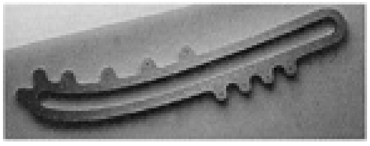

Fig. 21 Stainless steel aircraft guide bracket showing the profiling capabilities of the abrasive waterjet. Courtesy of Flow Systems, Inc.

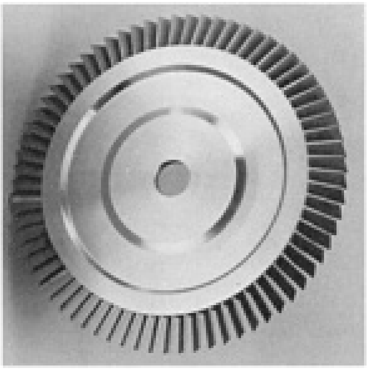

Fig. 22 Turbine rotor machined from a solid Alloy 100 blank using an abrasive waterjet that accelerated a 60

mesh garnet abrasive at 207 MPa (30 ksi). Courtesy of Flow Systems, Inc.

Figure 23 summarizes the capabilities of the laser beam, oxyfuel, plasma arc, and the abrasive waterjet in cutting a variety of metals. Current technology limits the abrasive waterjet to cutting metals having a maximum thickness of 152 mm (6 in.). Detailed information on the other techniques is available in the articles "Thermal Cutting" and "Laser Cutting" in this Volume.

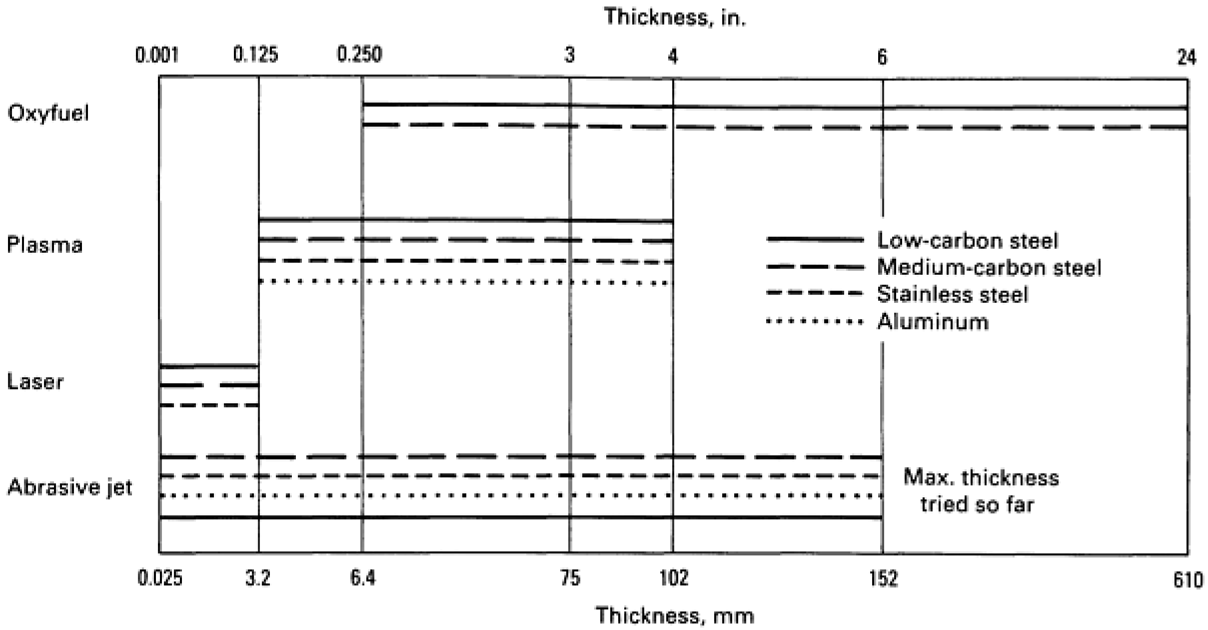

Fig. 23 Cutting thickness ranges for various cutting processes.

Machining of Nonmetallics. Figures 24 and 25 show some of the nonmetallic materials that are readily cut by the abrasive waterjet. Table 5 lists the cutting speeds obtainable for typical nonmetallic materials.

Table 5 Effect of material thickness on abrasive waterjet cutting speed for nonmetallic materials

Abrasive used is garnet.

Parameters					**Material thickness mm (in.)**	Maximum cutting rate, mm/min (in./min)									
Diameters, mm (in.)		**Abrasive**		**Nozzle pressure MPa (ksi)**		**Plastics and composite**							Glass		
Orifice	**Nozzle**	**Feed rate kg/min (lb/min)**	**Mesh size**			**Acetal**	**Acrylic**	**Carbon/ carbon composite**	**Epoxy/glass composite**	**Graphite/ epoxy composite**	**Aramid fiber composite**	**Polypropylene**	**Laminate**	**Plate**	Stained
0.23 (0.009)	0.79 (0.031)	0.23 (0.5)	100	310 (45)	0.8 (0.031)	3180 (125)	3050 (120)	2540 (100)	6350 (250)	4450 (175)	2540 (100)	2540 (100)	. . .	7620 (300)	**7620 (300)**
					1.6 (0.063)	2290 (90)	2030 (80)	1910 (75)	5720 (225)	3810 (150)	1520 (60)	1910 (75)	. . .	6350 (250)	**6350 (250)**
					3.2 (0.125)	1780 (70)	1400 (55)	1400 (55)	4570 (180)	3180 (125)	1020 (40)	1220 (48)	660 (26)	5080 (200)	**5080 (200)**
					6.4 (0.250)	1270 (50)	915 (36)	1020 (40)	2540 (100)	2540 (100)	510 (20)	915 (36)	559 (22)	3810 (150)	**3810 (150)**
					12.7 (0.500)	890 (35)	457 (18)	508 (20)	1020 (40)	1270 (50)	279 (11)	610 (24)	457 (18)	2540 (100)	**2540 (100)**
0.33 (0.013)	1.19 (0.047)	0.68 (1.5)	80	240 (35)	19.0 (0.750)	610 (24)	305 (12)	254 (10)	711 (28)	635 (25)	152 (6)	381 (15)	305 (12)	1270 (50)	**1270 (50)**
					25.4 (1.00)	381 (15)	254 (10)	127 (5)	559 (22)	508 (20)	75 (3)	203 (8)	203 (8)	635 (25)	**. . .**

0.46 (0.018)	1.19 (0.047)	0.91 (2.0)	80	240 (35)	50.8 (2.00)	152 (6)	102 (4)	20 (0.8)	305 (12)	254 (10)	25 (1.0)	102 (4)	127 (5)	381 (15)	. . .
					76.2 (3.00)	64 (2.5)	25 (1.0)	8 (0.3)	127 (5)	127 (5)	13 (0.5)	50 (2)	25 (1)	127 (5)	. . .
					102 (4.00)	**25 (1)**	**8 (0.3)**	**3 (0.1)**	**25 (1)**	**50 (2)**	**3 (0.1)**	**38 (1.5)**	**20 (0.8)**	**50 (2)**	**. . .**

Source: Flow Systems, Inc.

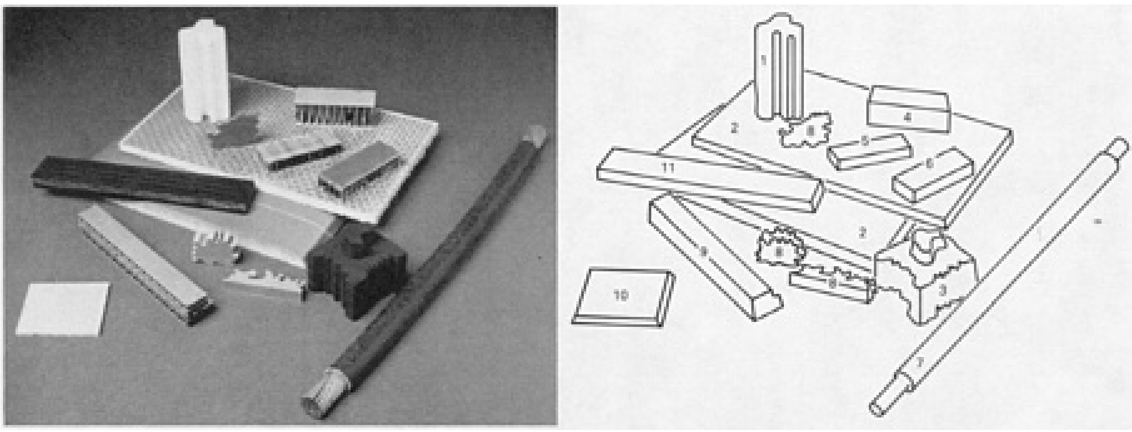

Fig. 24 Various plastics and composite materials that were cut and profiled using an abrasive waterjet. 1, styrofoam; 2, Kevlar; 3, foam rubber; 4, Nomex; 5, graphite Nomex; 6, Kevlar Nomex; 7, electrical wire; 8, laminated paper; 9, cardboard; 10, presintered ceramic; 11, ABS. Courtesy of Ingersoll-Rand Waterjet Cutting Systems.

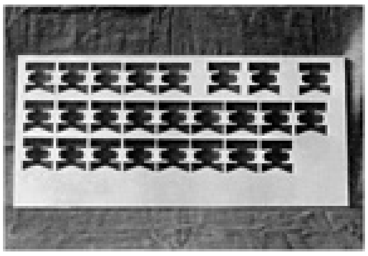

Fig. 25 Polyurethane rubber cut with an abrasive waterjet. Courtesy of Sugino USA, Inc.

Listed below are recent installations of representative abrasive waterjet technology used to cut nonmetallics for automotive applications:

- Trimming of thermoformed wood-fiber substrate interior door panels at speeds of 20 m/min (800 in./min)
- Elimination of an airborne asbestos dust problem encountered in the manufacture of 13 mm ($\frac{1}{2}$ in.) thick brake shoe linings; kerf loss was reduced to 0.25 mm (0.010 in.) while cutting efficiency was increased 30 to 50%
- Finish cutting to size of 3.9 kg ($8\frac{1}{2}$ lb) rear door panels after initial rough cutting with more conventional methods; the panels were made of two layers of sheet molding compound coated with continuous layers of polyester resin paste and then rolled to remove trapped air

Domestic and international electronics firms are using waterjet technology to cut the plastic laminates used in printed circuit boards. The hairlike size of the waterjet kerf and the omnidirectional, sharp cutting capabilities of the waterjet are ideally suited to the precision cutting and trimming of the boards even when they are loaded with their electronic components. The absence of any lateral forces or mechanical pressure, usually associated with mechanical cutters, eliminates the board flexure that could break solder joints.

J. Gerin Sylvia, Department of Industrial and Manufacturing Engineering, University of Rhode Island

Safety

Safety problems caused by such conditions as fire hazards and dust and noise pollution are minimized through the use of abrasive waterjet cutting, as follows:

- Safety is increased in an already hazardous atmosphere, particularly in comparison to flame and/or plasma cutting torches. Because there is no heat buildup with abrasive waterjet cutting, fire hazards are eliminated. There is no radiation emission or danger from flying slag particles
- Airborne dust is virtually eliminated, making operation less hazardous to personnel working in close proximity to the machine. Containment or other methods of airborne dust control are unnecessary
- Noise levels range from 85 to 95 dBA, which is consistent with OSHA regulations

Abrasive Waterjet Cutting

J. Gerin Sylvia, Department of Industrial and Manufacturing Engineering, University of Rhode Island

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Introduction

STAINLESS STEELS are blanked, pierced, formed, and drawn in basically the same press tools and machines as those used for other metals. However, because stainless steels have higher strength and are more prone to galling than low-carbon steels and because they have a surface finish that often must be preserved, the techniques used in the fabrication of sheet metal parts from stainless steels are more exacting than those used for low-carbon steels. In general, stainless steels have the following characteristics, as compared with those of carbon steels:

- Greater strength
- Greater susceptibility to work hardening
- Higher propensity to weld or gall to tooling
- Lower heat conductivity

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Selection of Alloy

The properties and selection of stainless steels are discussed in the Section "Specialty Steels and Heat-Resistant Alloys" in *Properties and Selection: Irons, Steels, and High-Performance Alloys*, Volume 1 of the *ASM Handbook.*

General ratings of the relative suitability of the commonly used austenitic, martensitic, and ferritic types of stainless steels to various methods of forming are given in Table 1. These ratings are based on formability and on the power required for forming.

Table 1 Relative suitability of stainless steels for various methods of forming

Suitability ratings are based on comparison of the steels within any one class; therefore, it should not be inferred that a ferritic steel with an A rating is more formable than an austenitic steel with a C rating for a particular method. A, excellent; B, good; C, fair; D, not generally recommended

Steel	0.2% yield strength, 6.89 MPa (1 ksi)	Suitability for:							
		Blanking	Piercing	Press-brake forming	Deep drawing	Spinning	Roll forming	Coining	Embossing
Austenitic steels									
201	55	B	C	B	A-B	C-D	B	B-C	B-C
202	55	B	B	A	A	B-C	A	B	B

301	40	B	C	B	A-B	C-D	B	B-C	B-C
302	37	B	B	A	A	B-C	A	B	B
302B	40	B	B	B	B-C	C	. . .	C	B-C
303, 303(Se)	35	B	B	D[(a)]	D	D	D	C-D	C
304	35	B	B	A	A	B	A	B	B
304L	30	B	B	A	A	B	A	B	B
305	37	B	B	A	B	A	A	A-B	A-B
308	35	B	. . .	B[(a)]	D	D	. . .	D	D
309, 309S	40	B	B	A[(a)]	B	C	B	B	B
310, 310S	40	B	B	A[(a)]	B	B	A	B	B
314	50	B	B	A[(a)]	B-C	C	B	B	B-C
316	35	B	B	A[(a)]	B	B	A	B	B
316L	30	B	B	A[(a)]	B	B	A	B	B
317	40	B	B	A[(a)]	B	B-C	B	B	B
321, 347, 348	35	B	B	A	B	B-C	B	B	B
Martensitic steels									
403, 410	40	A	A-B	A	A	A	A	A	A
414	95	A	B	A[(a)]	B	C	C	B	C
416, 416(Se)	40	B	A-B	C[(a)]	D	D	D	D	C
420	50	B	B-C	C[(a)]	C-D	D	C-D	C-D	C
431	95	C-D	C-D	C[(a)]	C-D	D	C-D	C-D	C-D

440A	60	B-C	. . .	C[(a)]	C-D	D	C-D	D	C
440B	62	. . .	. . .	. . .	. . .	D	. . .	D	D
440C	65	. . .	. . .	. . .	. . .	D	. . .	D	D
Ferritic steels									
405	40	A	A-B	A[(a)]	A	A	A	A	A
409	38	A	A-B	A(b)	A	A	A	A	A
430	45	A	A-B	A[(a)]	A-B	A	A	A	A
430F, 430F(Se)	55	B	A-B	B-C[(a)]	D	D	D	C-D	C
442	. . .	A	A-B	A[(a)]	B	B-C	A	B	B
446	50	A	B	A[(a)]	B-C	C	B	B	B

(a) Severe sharp bends should be avoided.

As Table 1 shows, the austenitic and ferritic steels are, almost without exception, well suited to all of the forming methods listed. Of the martensitic steels, however, only types 403, 410, and 414 are generally recommended for cold-forming applications. Because the higher carbon content of the remaining martensitic types severely limits their cold formability, these steels are sometimes formed warm. Warm forming can also be used to advantage with other stainless steels in difficult applications.

Formability. The characteristics of stainless steel that affect its formability include yield strength, tensile strength, ductility, (and the effect of work hardening on these properties), and the r value. The composition of stainless steel is also an important factor in formability. Figure 1 compares the effect of cold work on the tensile strength and yield strength of type 301 (an austenitic alloy), types 409 and 430 (both ferritic alloys), and 1008 low-carbon steel sheet.

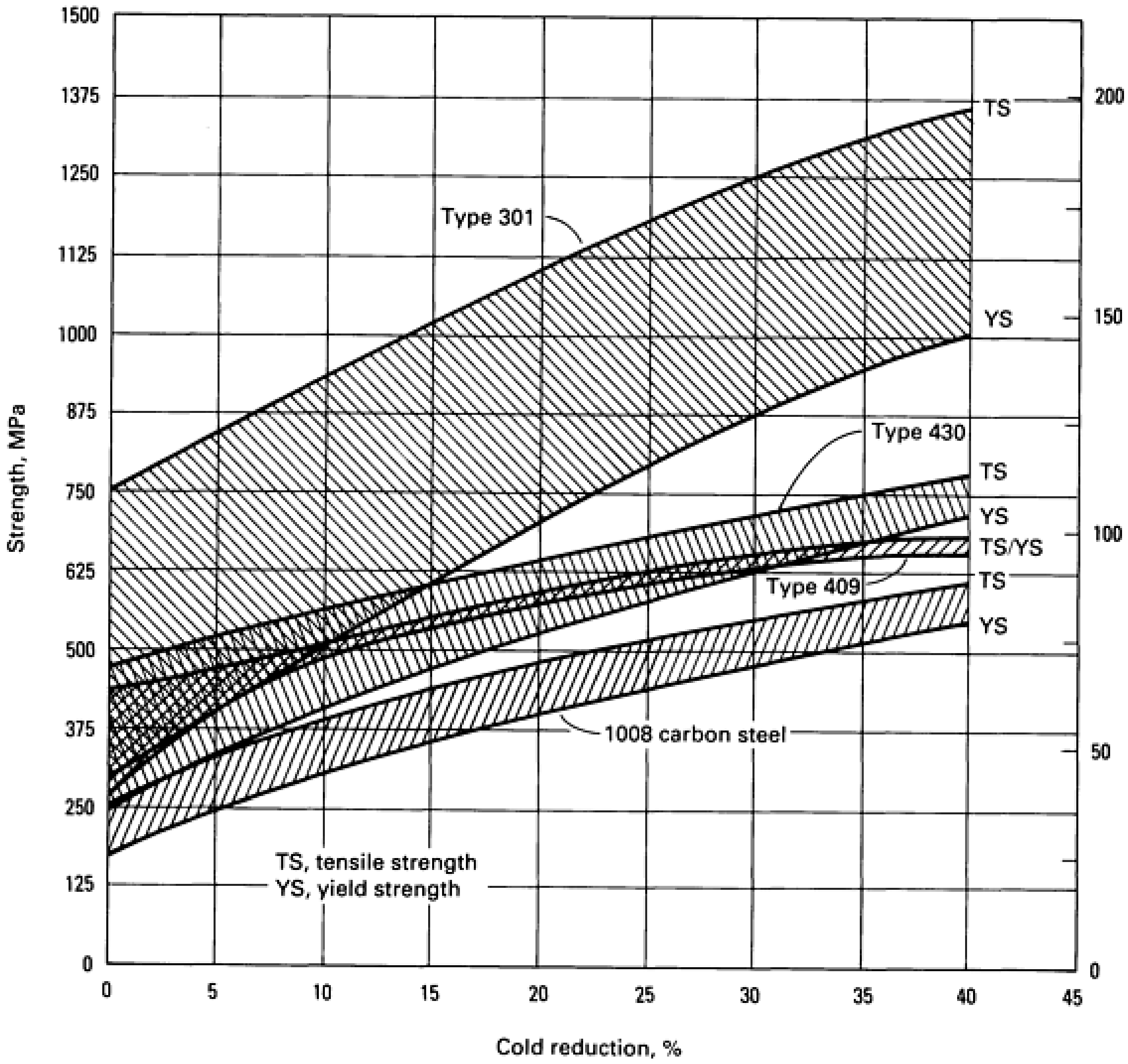


Fig. 1 Comparison of work-hardening qualities of type 301 austenitic stainless steel, types 409 and 430 ferritic stainless steels, and 1008 low-carbon steel.

Formability of Austenitic Types. Type 301 stainless steel has the lowest nickel and chromium contents of the standard austenitic types; it also has the highest tensile strength in the annealed condition. The extremely high rate of work hardening of type 301 results in appreciable increases in tensile strength and yield strength with each increase in the amount of cold working, as measured by cold reduction (Fig. 1). This response to work hardening is particularly important for structural parts, including angles and channel sections--which, after fabrication, are expected to have additional strength and stiffness. On the other hand, for deep-drawing applications, a lower rate of work hardening is usually preferable and can be obtained in the austenitic alloys that have higher nickel contents; notably, types 304, 304L, and 305.

In general, the austenitic alloys are more difficult to form as the nickel content or both the nickel and the chromium contents are lowered, as in type 301. Such alloys show increased work hardening rates and are less suitable for deep drawing or multiple forming operations. The presence of the stabilizing elements niobium, titanium, and tantalum, as well as higher carbon contents, also exerts an adverse effect on the forming characteristics of the austenitic stainless steels. Therefore, the forming properties of types 321 and 347 stainless steel are less favorable than those of types 302, 304, and 305.

Formability of Ferritic Types. The range between yield strength and tensile strength of types 409 and 430 narrows markedly as cold work increases, as shown in Fig. 1. This response is typical of the ferritic alloys and limits their formability (ductility) (in comparison with the austenitic alloys). Nevertheless, types 409 and 430, although lacking the formability of type 302, are widely used in applications that require forming by blanking, bending, drawing, or spinning.

One of the most important applications for type 430 stainless steel is in automotive trim or molding. Type 409 stainless steel has found wide acceptance as the material of choice in automotive exhaust systems.

Comparison With Carbon Steel. The curves for 1008 low-carbon steel are included in Fig. 1 as a reference for the evaluation of stainless steels. The decrease in formability of 1008 steel with cold work appears to fall between that of types 409/430 and that of the more formable type 301. Figure 1 also shows that cold work does not increase the strength of 1008 as rapidly as it does that of type 301 and the ferritic alloys.

Stress-Strain Relations. Figure 2 shows load-elongation curves for six types of stainless steel: four austenitic (202, 301, 302, and 304), one martensitic (410), and one ferritic (430). The figure also shows that the type of failure in cup drawing of the austenitic types was different from that of types 410 and 430, as shown in Fig. 2. The austenitic types broke in a fairly clean line near the punch nose radius, almost as if the bottom of the drawn cup were blanked out; types 410 and 430 broke in the sidewall in sharp jagged lines, showing extreme brittleness as a result of the severe cold work.

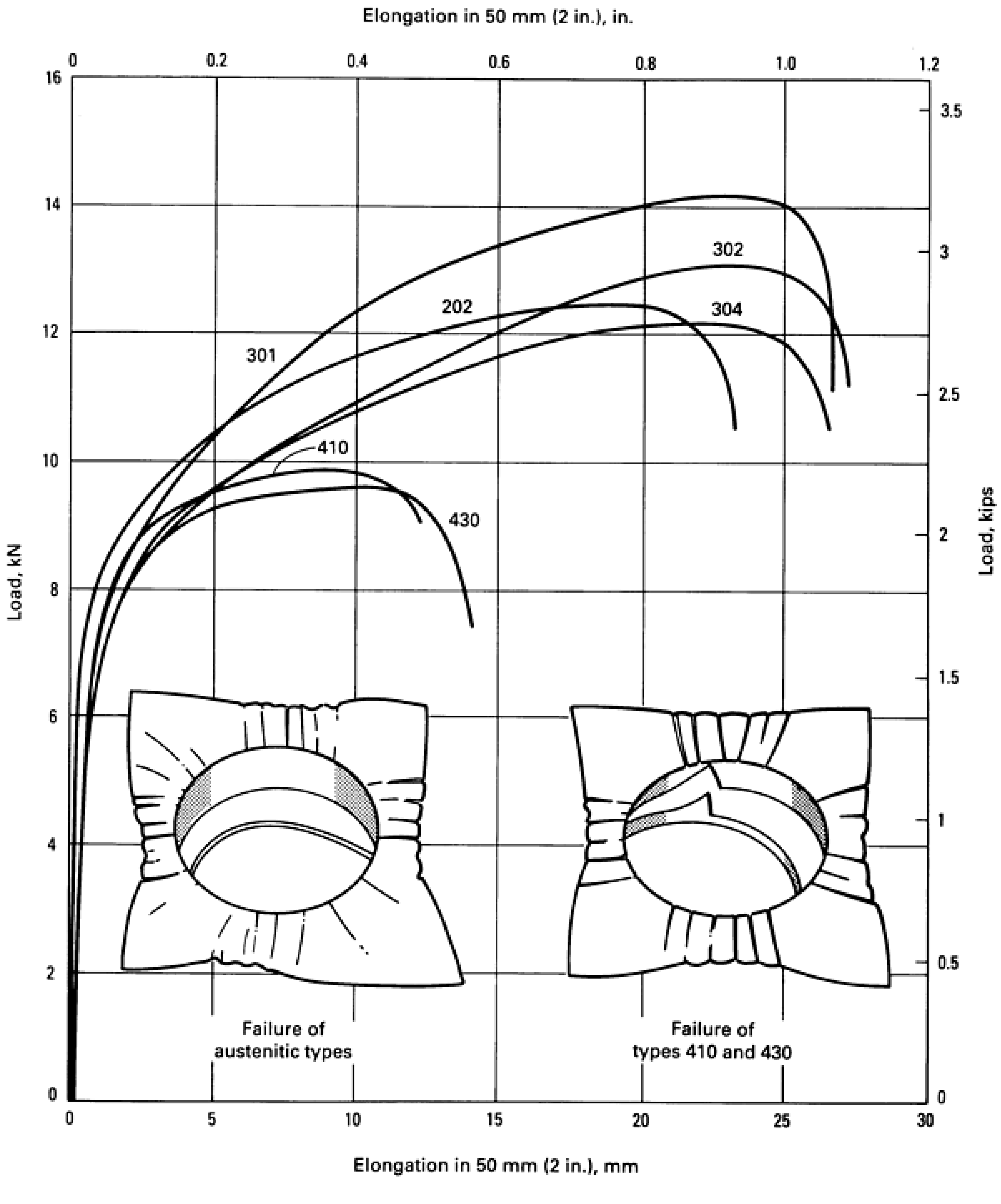


Fig. 2 Comparison of ductility of six stainless steels and of the types of failure resulting from deep drawing.

As suggested by the data in Fig. 2, the power required to form type 301 exceeds that required by the other austenitic alloys. In addition, type 301 will develop maximum elongation before failing. Types 410 and 430 require considerably less power to form, but fail at comparatively low elongation levels.

Power requirements for the forming of stainless steel, because of the high yield strength, are greater than those for low-carbon steel; generally, twice as much power is used in forming stainless steel. Because the austenitic steels work harden rapidly in cold-forming operations, the need for added power after the start of initial deformation is greater than that for the ferritic steels. The ferritic steels behave much like plain carbon steels once deformation begins, although higher power is also needed to start plastic deformation.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Lubrication

Lubrication requirements are more critical in forming stainless steels than in forming carbon and alloy steels, because it usually is necessary to preserve the high-quality surface on stainless steels and because stainless steels have higher strength, greater hardness, lower thermal conductivity, and higher coefficient of friction. In forming stainless steels, galling and spalling occur more readily, and higher temperatures are reached in a larger volume of the workpiece. Local or general overheating can change the properties of the work metal and lubricant.

Table 2 lists the lubricants ordinarily used in forming stainless steel by various processes. Except for the special-purpose lubricants graphite and molybdenum disulfide, the lubricants are listed in the approximate order of increasing ability to reduce galling and friction. The ratings in Table 2 also consider other suitability factors, such as cleanliness and ease of removal. The desirable characteristics of a lubricant include the ability to reduce friction and wear, the dissipation of heat, durability, nonreactiveness with the base steel, and ease of removal. The higher temperatures generated when forming stainless alloys, particularly the austenitic varieties, frequently lead to breakdown of the polar lubricant molecules.

Table 2 Suitability of various lubricants for use in the forming of stainless steel

Ratings consider effectiveness, cleanliness, ease of removal, and other suitability factors. A, excellent; B, good; C, acceptable; NR, not recommended

Lubricant	**Blanking and piercing**	**Press-brake forming**	**Press forming**	**Multiple-slide forming**	**Deep drawing**	**Spinning**	**Drop-hammer forming**	**Contour roll forming**	Embossing
Fatty oils and blends[(a)]	C	B	C	A	C	A	C	B	**B**
Soap-fat pastes[(b)]	NR	NR	C	A	B	B	C	B	**C**
Wax-base pastes[(b)]	B	B	B	A	B	B	C	B	**A**
Heavy-duty emulsions[(c)]	B	NR	B	A	B	B	NR	A	**B**

Dry film (wax, or soap plus borax)	B	B	B	NR	B	A	B	NR	**A**
Pigmented pastes[b] [d]	B	NR	A	B	A	C	NR	NR	**NR**
Sulfurized or sulfochlorinated oils[e]	A	A	B+	A	C	NR	A	B	**A**
Chlorinated oils or waxes[b]									
High-viscosity types[g]	A[h]	NR	A	NR	A	NR	A[i]	A	**NR**
Low-viscosity types[j]	B+	A	A	A	B	NR	A[i]	A	**A**
Graphite or molybdenum disulfide[k]	**NR**	**[l]**	**[l]**	**NR**	**[l]**	**NR**	**[l]**	**NR**	**NR**

(a) Vegetable or animal types: mineral oil is used for blending.

(b) May be diluted with water.

(c) Water emulsions of soluble oils; contain a high concentration of extreme-pressure sulfur or chlorine compounds.

(d) Chalk (whiting) is commonest pigment: others sometimes used.

(e) Extreme-pressure types; may contain some mineral or fatty oil.

(f) Extreme-pressure chlorinated mineral oils or waxes; may contain emulsifiers for ease of removal in water-base cleaners.

(g) Viscosity of 4000 to 20,000 SUS (Saybolt Universal seconds, see ASTM D 2161 for more detailed information).

(h) For heavy plate.

(i) For cold forming only.

(j) Viscosity (200 to 1000 SUS) is influenced by base oil or wax, degree of chlorination, and additions of mineral oil.

(k) Solid lubricant applied from dispersions in oil, solvent, or water.

(l) For hot-forming applications only

Among stainless steel lubricants for severe drawing applications, the extreme-pressure (EP) additive types are the most desirable. Additives of chlorine or sulfur tend to react chemically with the steel surface at higher temperatures and form a readily shearable compound. Chlorine is the more popular EP additive, because sulfur tends to react with some steel tooling.

Mineral oils, soap solutions, and water emulsions of general-purpose soluble oils are omitted because they are ineffective in most forming of stainless. The recommended lubricants are discussed further in the sections that deal with the individual forming processes in the remainder of this article.

As a precaution, all lubricants should be removed, and the parts thoroughly dried, after completion of the sheet metalworking operation. Most lubricants must be removed before the formed parts are heat treated; this applies particularly to those containing insoluble solids, sulfur, or chlorine. In addition, certain metastable austenitic stainless alloys can react with their lubricant and cause delayed cracking in heavily strained areas. Rapid removal of the lubricant is therefore desirable.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Blanking and Piercing

The shear strength of stainless steel is about twice that of low-carbon steel. Therefore, the available force for the blanking or piercing of stainless steel should be 50 to 100% higher than that for equivalent work on carbon steel.

Tools and power can be saved if the stock can be blanked at about 175 °C (350 °F). The finish will be better as well. Power requirements can also be reduced by using angular shear on the punch or the die. (Additional information is available in the Section "Blanking and Piercing of Steel Sheet, Strip, and Plate" in this Volume.)

Die Materials. Cutting edges must be of a hard, strong material. Recommended die materials, in order of suitability for increasing quantities, include O1, A2, D2, and D4 tool steels and carbide. Additional information is available in the article "Selection of Material for Blanking and Piercing Dies" in this Volume. The use of carbide for high-volume production in applications that do not require the impact resistance of tool steels is illustrated in the following example.

Example 1: Use of a Carbide Die to Form a Miniature Piece.

The cathode shown in Fig. 3 was produced in a three-stage progressive die made of carbide by piercing, blanking, and forming. The piece was trough-shaped, 6.4 mm ($\frac{1}{4}$ in.) long, of type 304 stainless steel, 0.08 mm (0.003 in.) thick. One end was rounded, and the other was V-shaped. The difference in contour of the two ends kept the pieces from stacking. Before forming, the blank was 9.5 mm ($\frac{3}{8}$ in.) wide. The piece was pierced with 68 holes, each 0.31 mm (0.012 in.) square. In this operation, the material was displaced by a pointed punch, rather than removed by a flat-nose punch. The pieces were cut from 152 mm (6 in.) wide strip, producing 16 pieces at a stroke. The press was a 130 kN (15 tonf) mechanical press that ran at 240 strokes per minute.

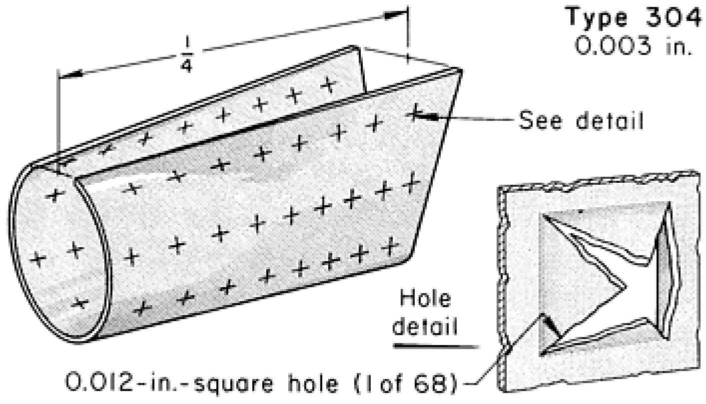


Fig. 3 Cathode produced in a progressive die with carbide tools. Dimensions given in inches.

Clearance between punch and die should be about the same as that for the blanking and piercing of cold-rolled low-carbon steel. Some manufacturers use less than 0.03 mm (0.001 in.) per side; others specify 5 to 10% of stock thickness per side for sheet and 10 to 15% of stock thickness for plates and bars. Studies have shown, however, that larger clearances--12.5 to 13.5%, and even up to 42%, of stock thickness--have resulted in increased die life (see the article "Piercing of Low-Carbon Steel" in this Volume).

Cutting edges should be carefully aligned, sharp, clean, and free of burrs. The importance of sharpness of cutting edges cannot be overemphasized.

Deburring. Generally, stainless steel does not shear clean, but leaves a rough work-hardened edge that is dangerous to handle and may adversely affect subsequent operations. Flat pieces can be rolled or pressed between dies adjusted exactly to the thickness of the stock, or the burrs can be removed by grinding, stoning, or filing.

Lubrication. The blanking and piercing of stainless is often done dry, but the lubricants indicated in Table 2 are sometimes used to prolong die life. Lubricants containing sulfur or chlorine are the most effective for this purpose. Emulsions are used for high-speed work.

Dimensions. Pierced holes should not be smaller than the thickness of the stock. Holes larger than 3.18 mm ($\frac{1}{8}$ in.) should be spaced so that the distance between centers is not less than $1\frac{1}{2}$ times the hole diameter. Small holes should have a distance between centers of at least $1\frac{3}{4}$ times the diameter of the holes. Holes should never be closer together than one stock thickness, nor should the edge of blanks be less than one stock thickness from the edge of the stock. For progressive-die operation, edge distances should be between $1\frac{1}{2}$ and 2 times stock thickness.

Nibbling. In some applications, an irregular contour is cut out by punching a series of overlapping holes along the contour. This process is called nibbling. A variety of unusual shapes can be cut at 300 to 900 strokes per minute by a press equipped with either a round or a rectangular punch.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Press-Brake Forming

All of the austenitic stainless steels in the soft condition can be bent 180° over one stock thickness, but need up to 50% more power to form than that required by low-carbon steel. Springback is more severe with austenitic stainless steels than with low-carbon steel, and it must be allowed for. Work-hardened austenitic steel can be press-brake formed only to a very limited degree. If austenitic stainless steel is heated to about 65 °C (150 °F), it can be formed with appreciably less power than that required when it is cold and yet can be handled easily.

The straight-chromium grades of stainless steels vary in their response to press-brake forming. The low-carbon stainless steels containing 12 to 17% Cr bend readily but, like the austenitic steels, need more power for bending than that required for low-carbon steel. High-chromium low-carbon types, such as 446, bend better when heated to 175 to 205 °C (350 to 400 °F). The heating of these high-chromium low-carbon grades tends to lower the yield strength, but can simultaneously aid in allowing the forming to be done above the brittle-to-ductile transition temperature. For these alloys, that temperature can be at or above room temperature, depending on thickness. In room-temperature forming, the highly alloyed ferritic stainless steels have been known to benefit from slower bend speeds, which minimize the possibility of an impactlike load and resultant brittle fracture. High-carbon heat-treatable stainless steels are not recommended for press-brake forming, even if in the annealed condition.

Typical bending limits for the major stainless steels are shown in Table 3. A completely flat bend can generally be made in the 18-8 and similar alloys.

Table 3 Typical bending limits for six commonly formed stainless steels

Type	Minimum bend radius		
	Annealed to 4.75 mm (0.187 in.) thick (180° bend)	Quarter hard, cold rolled	
		To 1.27 mm (0.050 in.) thick (180° bend)	1.30-4.75 mm (0.051-0.187 in.) thick (90° bend)
301, 302, 304	$\frac{1}{2}t$	$\frac{1}{2}t$	**1*t***
316	$\frac{1}{2}t$	1*t*	**1*t***
410, 430	**1*t***	. . .	. . .

t, stock thickness

Dies. Press brakes can use dies with cross sections such as those shown in Fig. 4 for forming stainless steel in sheets up to 0.89 mm (0.035 in.) thick. Adjustable dies, such as that shown in Fig. 5, can be used for forming 180° bends in stainless steel sheet 0.30 to 0.46 mm (0.012 to 0.018 in.) thick.

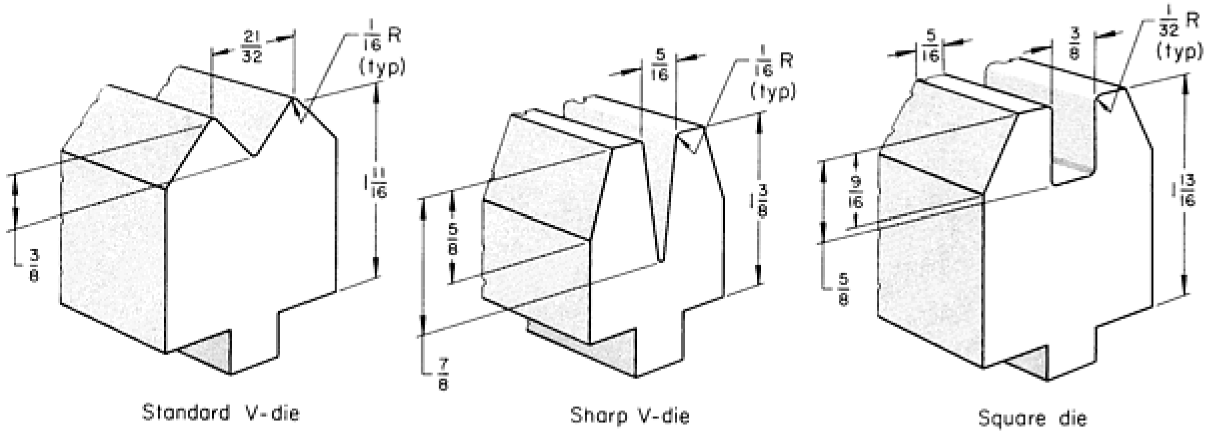


Fig. 4 Typical dies for the press-brake forming of stainless steel sheet up to 0.9 mm (0.035 in.) thick. Dimensions in figure given in inches.

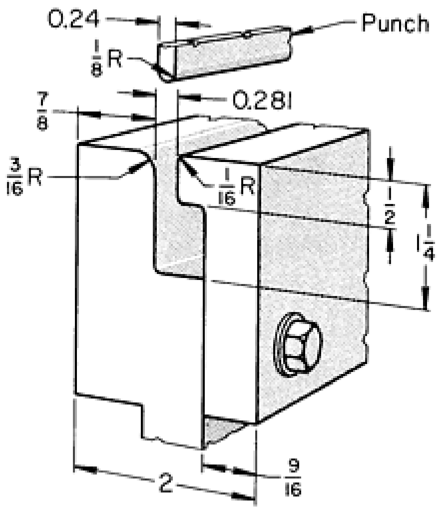


Fig. 5 Adjustable press-brake die for forming 180° bends in stainless steel sheet. Setup can be used for forming bends to 3.2 mm ($\frac{1}{8}$ in.) inside radius in sheet 0.30 to 0.46 mm (0.012 to 0.018 in.) thick, and it will produce 4.0 mm ($\frac{5}{32}$ in.) radius bends in half-hard stainless steel. Detachable side of die can be shimmed for bending thicker sheet or for bending with larger-radius punches. Dimensions in figure given in inches.

Springback is a function of the strength of the material, the radius and angle of bend, and the thickness of the stock; the thicker the stock, the less severe the problem. Table 4 shows the relationship between radius of bend and springback for three austenitic stainless steels. Ferritic steels usually exhibit less springback than austenitic steels, because the rate of work hardening of ferritic steels is lower. As a practical guide, the amount of springback is normally proportional to (0.2YS + UTS)/2.

Table 4 Springback of three austenitic stainless steels bent 90° to various radii

Steel and temper	Springback for bend radius of:		
	1*t*	**6*t***	20*t*
302 and 304, annealed	2°	4°	**15°**
301, half-hard	**4°**	**13°**	**43°**

Springback can be controlled by reducing the punch radius, by coining the line of bend (if the shape of the die is such that bottoming is feasible), and by overbending. For overbending, it is sometimes necessary only to make the punch angle smaller than the desired final angle of the workpiece, as in the following example.

Example 2: Setting a Flange Angle in a Press Brake.

The bracket shown in Fig. 6 was preformed in a U-die from a developed blank of type 302 stainless steel, half-hard, 1.0 mm (0.040 in.) thick. Only the punch angle needed to be reduced to set the angle on the flange.

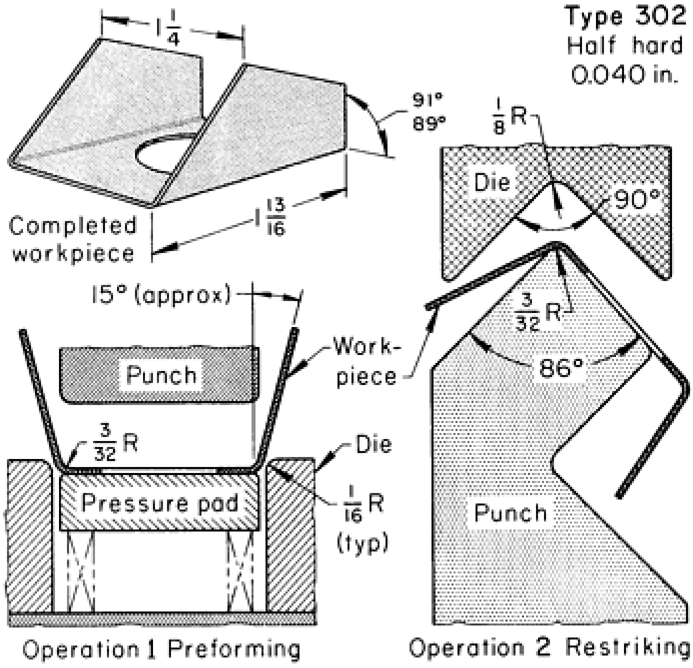


Fig. 6 Production of a U-shaped bracket from a developed blank by preforming, and restriking to set flange angles, in a press brake. Dimensions given in inches.

As the bracket came from the U-die, the springback in each flange was 15°. To correct this spread, the piece was put in a restrike die in a press brake, which set each angle separately. The restrike die angle was 90° with a 3.2 mm ($\frac{1}{8}$ in.) radius. The restrike punch was made to an angle of 86° with a 2.4 mm ($\frac{3}{32}$ in.) radius to coin the bend, so that the flanges would form to 90° ± 1°.

The lubricant was a water-soluble pigmented drawing compound. The workpiece was degreased after forming.

Lubricants. For ordinary press-brake operations (chiefly, bending and simple forming), lubricants are not used as frequently as with higher-speed press operations. Convenience of use is a major factor in selecting lubricants for this type of press-brake forming. Pigmented lubricants are not favored, and cooling effectiveness is of little significance at low production rates. For severe forming and for operations that would ordinarily be done in a press, if available, the recommendations in the "Press forming" column in Table 2 apply.

Applications of press-break forming are described in the following examples. Repetitive bends, as in corrugated stock, are frequently made one at a time in a press brake if the quantity of production is not sufficient to warrant a special die, as in the example below.

Example 3: Press-Brake Forming of Corrugations.

The corrugated sheet shown in Fig. 7 was formed from 0.41 mm (0.016 in.) thick full-hard type 302 stainless steel. The finished sheets, after bending, were 419 mm (16 $\frac{1}{2}$ in.) long, as shown, but the width, *w,* varied according to the use of the piece.

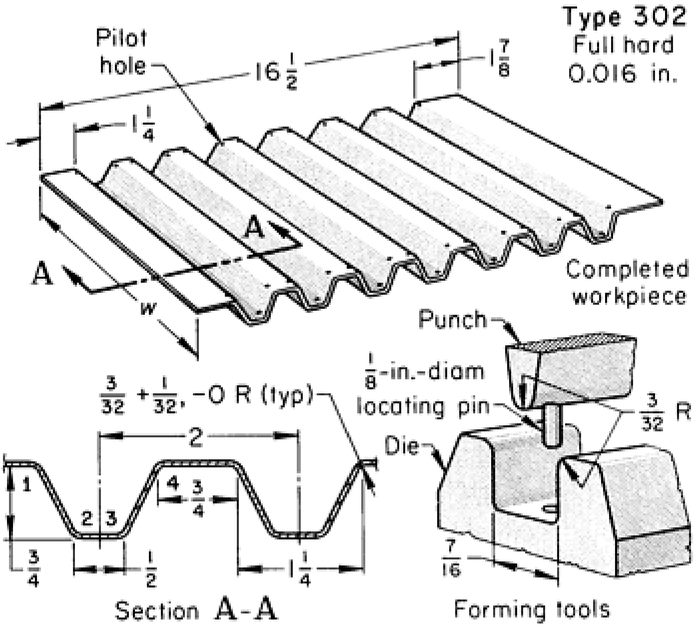


Fig. 7 Corrugated sheet in which corrugations were formed one at a time in a press brake, using tools shown. Dimensions given in inches.

The corrugations were made one at a time by air bending in the tooling shown at lower right in Fig. 7. Pilot holes in the workpiece and locating pins in the punch helped to keep the workpiece aligned. Deviation from flatness in the pieces was corrected by restriking some of the bends.

Irregular contours on long, narrow parts are conveniently produced by bending in a press brake. Because of the strength of stainless steels, the forming often must be divided among several successive operations, as in the example below.

Example 4: Forming of Stainless Steel Handrails in a Press Brake.

Figure 8 illustrates the shapes produced in five successive operations that were required for forming a handrail from 1.57 mm (0.062 in.) thick type 304 stainless steel. Because of flatness requirements and the resistance of the metal to bending, a 3600 kN (400 tonf) press brake was used.

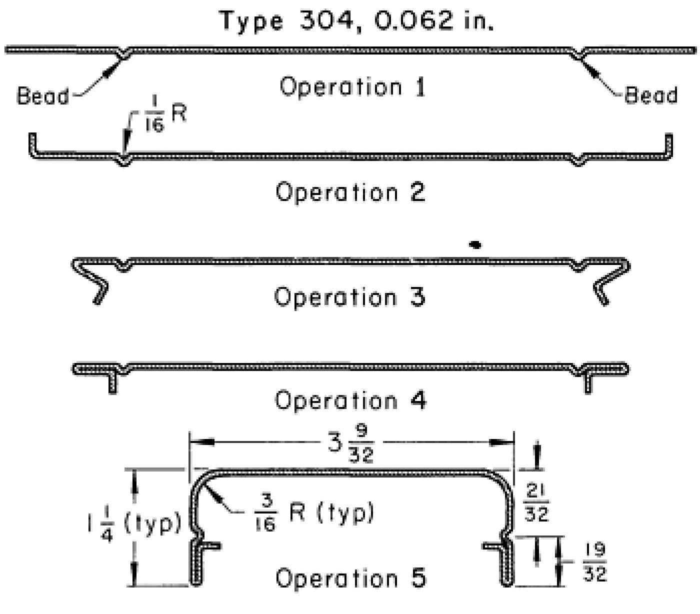


Fig. 8 Shapes progressively produced in the five-operation forming of a handrail in a 3600 kN (400 tonf) press brake. Dimensions given in inches.

Forming the 1.6 mm ($\frac{1}{16}$ in.) radius beads (operation 1, Fig. 8) was particularly troublesome because of the difficulty in retaining flatness. A force of 5300 kN (600 tonf), which exceeded the rating of the press brake, was used to form the beads.

The second and third operations presented no problems, but the fourth operation was difficult because the workpiece had to be held without marring the polished surface. Similar parts were produced from low-carbon steel without difficulty.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Press Forming

Stainless steels are press formed with the same kind of equipment as that used in the forming of low-carbon steel. However, although all stainless steels are not the same in strength or ductility, they all need more power to form than carbon steels do. In general, presses should have the capacity for 100% more ram force than that needed for equivalent work in low-carbon steel, and frames should have the rigidity and bulk necessary to withstand this greater force.

Dies. In addition to wearing out faster, dies may fracture more readily when used with stainless steel than when used with low-carbon or medium-carbon steel. This is because of the greater forces needed for the working of stainless steel.

For the longest service in mass production, the wearing parts of the dies should be made of carbide, D2 tool steel, or high-strength aluminum bronze. Carbide can last ten times as long as most tool steels, but it is more expensive and does not have the shock resistance of tool steels and aluminum bronze. Tool steels such as D2 are preferred when resistance to both shock and wear is required.

Aluminum bronze offers the most protection against galling and scuffing of the workpiece. An oil-hardening tool steel such as O2 can be used for short production runs.

Austenitic Alloys. Workpieces can be stretched by applying high blankholder pressures to the flange areas to prevent metal from flowing into the die. This causes severe thinning, but work hardening may cause the thinned metal to be as strong as or stronger than the thicker unworked sections. Figure 9 shows a section of an automobile wheel cover made of type 301 stainless steel; the central portion was purposely thinned and work hardened by stretching. In the following example, one of the principal reasons for stressing the workpiece to the limits of formability was to work harden it for increased strength.

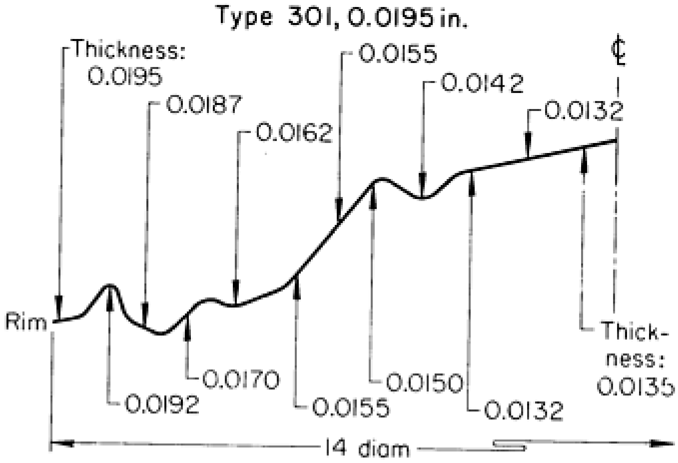


Fig. 9 Profile of a press-formed automobile wheel cover showing thinning purposely produced by severe stretching. Dimensions given in inches.

Example 5: Severe Forming for Intentional Work Hardening.

The material for a muffler header (Fig. 10) for a small aircraft engine was intentionally stressed nearly to the limits of formability to increase rigidity and to impart the necessary fatigue strength. The headers were made in two operations in a 530 kN (60 tonf) open-back-inclinable mechanical press having a 127 mm (5 in.) stroke. Each operation used a tool steel die hardened to 59 to 62 HRC. Production was 400 pieces per month.

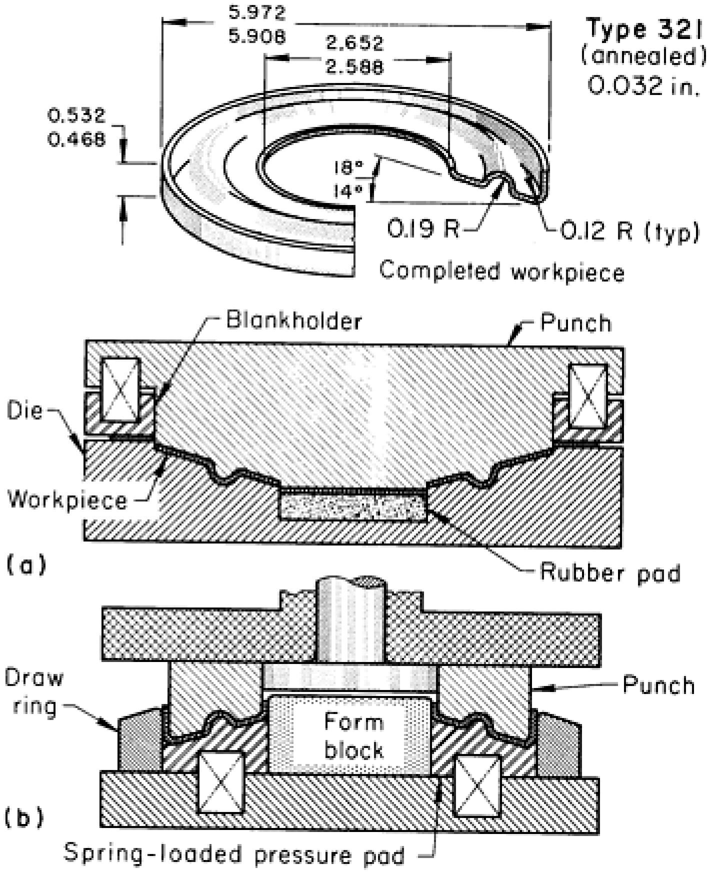


Fig. 10 Severe forming of an austenitic stainless steel aircraft muffler header to produce work hardening that would increase the rigidity and fatigue strength of the part. Dimensions given in inches.

The first die (Fig. 10a) was a compound die that formed the dish of the part, formed the bead in the dish, and blanked the inside and outside diameters. The blankholder at the outer edge of the workpiece was spring loaded, and a rubber pad supported the inner surface of the workpiece against the center blanking punch. The sequence was programmed so that the forming was completed before the outside and inside diameters were blanked, thus making the flange dimensions more accurate and concentric than would have otherwise been possible. Die life was approximately 20,000 pieces.

The second die (Fig. 10b) formed both the inner (stretch) flange and the outer (compression) flange. A spring-loaded pressure pad maintained the correct gripping pressure against the muffler-header body during this operation.

The blank was annealed type 321 stainless steel 0.81 mm (0.032 in.) thick, sheared to 216 mm (8 $\frac{1}{2}$ in.) square. The bead formed in the first die was used as a locating surface in the second die. The dies were brushed with oil between pieces.

The production rate for both operations was seven pieces per minute. Setup time for the first operation was 0.17 h; for the second operation, 0.31 h.

Stretching. Stainless steel has high ductility but wrinkles easily in compression. Therefore, if there is a choice in the direction of metal flow during forming, a better part is likely to be produced by stretching than by compression, as in the following example.

Example 6: Use of Clamping Plates and Bead to Control Metal Flow.

The dome section shown in Fig. 11 was formed from a tapered blank of annealed type 302 stainless steel in a 2200 kN (250 tonf) double-action hydraulic press. It was desirable to maximize metal flow from the narrow end of the blank in order to cause stretching rather than contraction in the metal and thus avoid wrinkles.

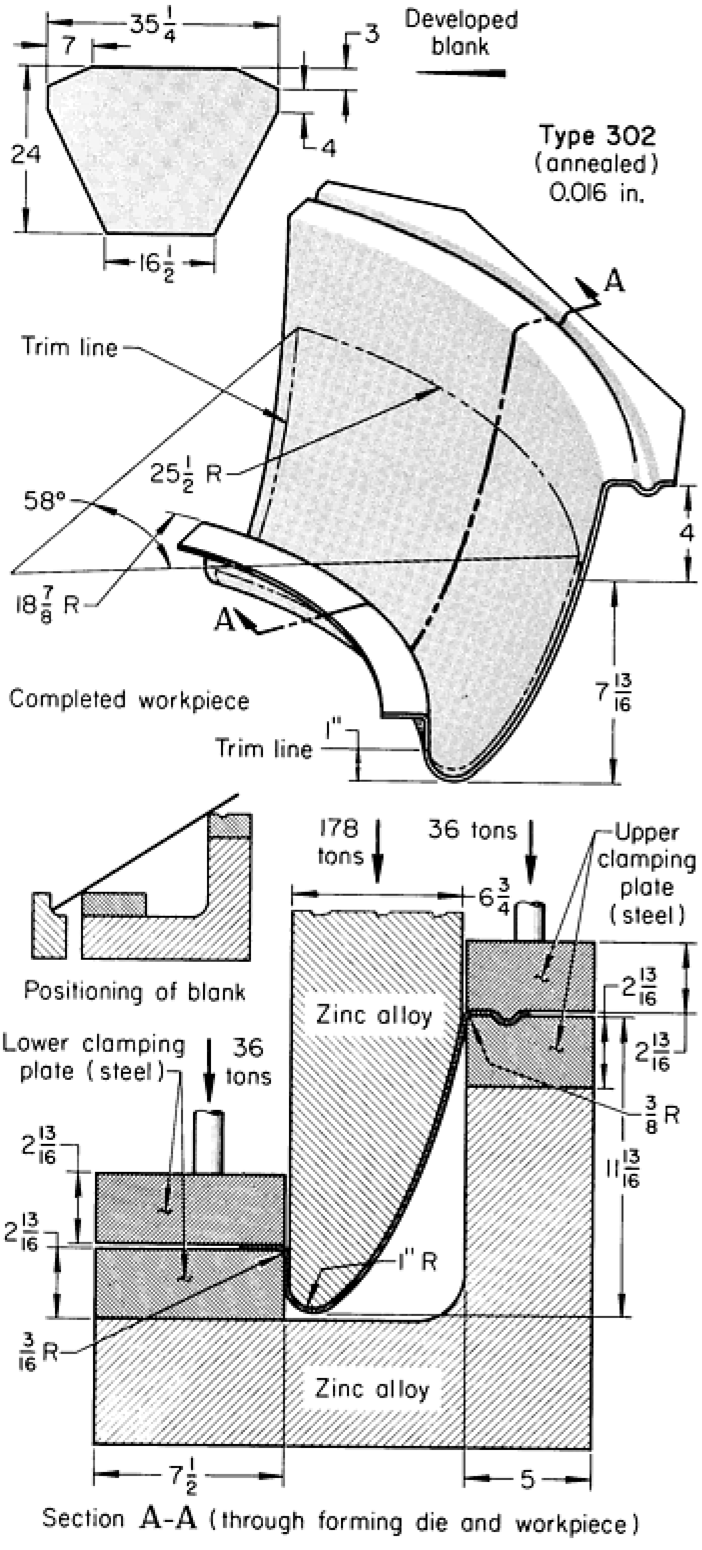


Fig. 11 Tools and clamping plates for controlling metal flow in press forming the part shown. Dimensions given in inches.

The dies could not be oriented to let the blank lie flat, because of the necessity for forming the reentrant angle next to the lower clamping plates. Both the upper and the lower edges of the blank were held between steel plates during forming. The clamping force on each pair of plates was 320 kN (36 tonf). Because the upper plates were twice as long as the lower and two-thirds as wide, the clamping force was distributed over a larger area and could have permitted most of the metal flow from the larger end, with attendant wrinkling of the work metal. The addition of a bead to the upper clamping plates improved holding at that end and caused most of the metal flow to occur at the small end of the blank. The application of a fatty acid type, nonpigmented drawing compound to the lower plates further encouraged metal flow from the small end of the blank. Scrap loss because of tearing over the relatively sharp lower die radius was 3%.

Ferritic Alloys. The formability of ferritic stainless steels, particularly the higher-chromium types, can be improved by warm forming at 120 to 200 °C (250 to 400 °F), rather than cold forming. The metal is more ductile at the higher temperatures, and less power is needed in forming. Some pieces that cannot be made by cold forming can be successfully made by warm forming.

Lubrication. The lubricant used most often in the press forming of stainless steel is the chlorinated type. It has unexcelled chemical EP activity, and the ability to adjust this activity and viscosity independently over an extremely wide range makes it the most versatile lubricant for this purpose. All chlorinated lubricants are readily removable in degreasers or solvents, and emulsifiers can be added to them for easy removal in water-base cleaners.

As shown in Table 2, pigmented pastes, sulfurized or sulfochlorinated oils, and dry wax or soap-borax films are also highly effective lubricants for press forming, but are less convenient to use. Heavy-duty emulsions, because of their superior characteristics as coolants, are preferred for high-speed operations. In the following example, high chlorine content and high viscosity were needed to produce acceptable parts (see also Example 15, in which a low-viscosity chlorine-base lubricant replaced a viscous mineral oil).

Example 7: Increase in Chlorine Content and Viscosity of Lubricant That Improved Results in Forming.

A wheel cover was made from a type 302 stainless steel blank, 457 mm (18 in.) in diameter by 0.71 mm (0.028 in.) thick, in two operations: draw, then trim and pierce. At first, a lightly chlorinated oil (10% Cl) of medium viscosity (1500 SUS, Saybolt Universal seconds, at 40 °C, or 100 °F) was used in drawing. Even though the draw was shallow, 12% of the wheel covers were rejected for splits and scratches.

A change was made to a highly chlorinated oil (36% Cl) of much higher viscosity (4000 SUS at 40 °C, or 100 °F). As a result, the rejection rate decreased to less than 1%. After forming, the wheel covers were vapor degreased.

Revised by Joseph A. Douthett, Armco Inc.

Combined Operations in Compound and Progressive Dies

The use of compound and progressive dies for the mass production of parts that require many operations or for an operation that is too severe to be done economically in a single-operation die is discussed in the article "Press Forming of Low-Carbon Steel" in this Volume. The same principles apply to their use on stainless steel for blanking, piercing, bending, forming, drawing, coining, embossing, or combinations of these operations.

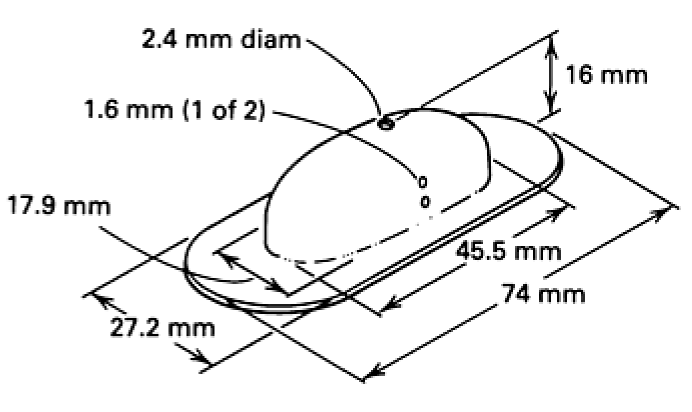
Both compound and progressive dies must be made of die materials that are hard enough to withstand the most severe demands of blanking and are tough enough for the most severe forming or coining operations. The lubricant must have enough body for the most severe draw, yet must be light enough not to interfere with the production of coined or embossed details or to gum up cutting edges. In a compound die in a double-action press, two draws can be made in stainless steel if the press capacity is not exceeded. The following example demonstrates the near-maximum severity of forming that can be achieved in a blank-and-draw compound die.

Example 8: Blanking and Severe Drawing in One Operation in a Compound Die.

The shell illustrated in Table 5 was blanked and drawn in a severe forming operation in a compound die at the rate of 16,000 pieces per year. The die was used in a 400 kN (45 tonf) mechanical press with an air cushion. The formed piece was restruck in the same die to sharpen the draw radius and to flatten the flange within 0.15 mm (0.006 in.). The die was made of A2 tool steel and had a life of 50,000 pieces per grind. An emulsified chlorinated concentrate was used as a lubricant.

Table 5 Production rates and labor time for making a severely drawn shell

Operation	**Production, pcs/h**	Labor per 100 pcs, h
Blank and draw[(a)]	922	**0.108**
Restrike[(a)]	1429	**0.070**
Pierce center hole; trim[(b)]	845	**0.118**
Pierce side holes[(c)]	786	**0.127**



(a) In a compound die, in a 400 kN (45 tonf) mechanical press with an air cushion.

(b) In a 200 kN (22 tonf) mechanical press.

(c) In a horn die in a 200 kN (22 tonf) mechanical press

After forming, the piece was then moved to a 200 kN (22 tonf) mechanical press, in which the 2.4 mm (0.093 in.) diam hole was pierced and the flange was trimmed to an oval shape. A second piercing operation, in a horn die in a 200 kN (22 tonf) mechanical press, pierced two 1.6 mm (0.062 in.) diam holes in the side of the shell. Air ejection was used in all operations except the final piercing, where the piece was picked off.

The material was type 302 stainless steel, 0.94 mm (0.037 in.) thick and 57 mm ($2\frac{1}{4}$ in.) wide, which had been annealed. Table 5 lists the production rate and labor time for each of the four operations.

Small, complex parts that must be made in large quantities are well suited to production in progressive or transfer dies. A transfer die uses a minimum of material and can accept coil stock, loose blanks, or partially formed parts. Scrap removal problems are lessened. A progressive die is preferred when the piece can remain attached to the strip.

In the following example, piercing, blanking, and forming were combined in a seven-stage progressive die. Although progressive, it was hand fed--a rather unusual combination.

Example 9: Producing a Small Bracket in a Progressive Die With Hand Feeding.

The small bracket shown in Fig. 12 was made in a seven-station progressive die from 9.5 mm ($\frac{3}{8}$ in.) wide stock that was hand fed into the 50 kN (6 tonf) press. Hand feeding was done because close operator attention was required to prevent jamming, which would have damaged the frail dies. The sequence of operations was as follows:

- Feed strip to finger stop; pierce
- Feed to notch-die opening; pierce
- Notch and trim lugs
- Form lugs
- Form and cut off
- Unload by blast of air

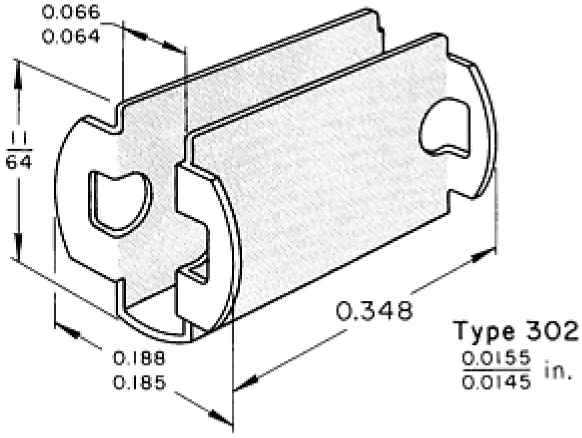


Fig. 12 Bracket that was made by hand feeding stock into a progressive die to avoid the jamming that would have been likely from automatic feeding. Dimensions given in inches.

The parts were barrel finished to remove burrs and to provide a smooth finish and high luster. The production rate was 2175 pieces per hour. Annual production was 2 million pieces. A chlorinated and inhibited oil was used as a lubricant.

Progressive Dies Versus Simple Dies. There is often a choice as to whether a stainless steel piece is to be made in a progressive die or in a series of single-operation (simple) dies. Deep forming usually presents difficulties in designing and constructing efficient and long-life progressive dies. The cost and delay involved in developing progressive tooling was justified for producing the frame described in the following example in quantities of 100,000 or more per year.

Example 10: Use of Progressive Dies for High-Quantity Production of Frames.

The frame shown in Fig. 13 was made of 0.56 mm (0.022 in.) thick type 430 stainless steel coil stock, 95.3 mm (3 $\frac{3}{4}$ in.) wide. The maximum hardness was 83 HRB.

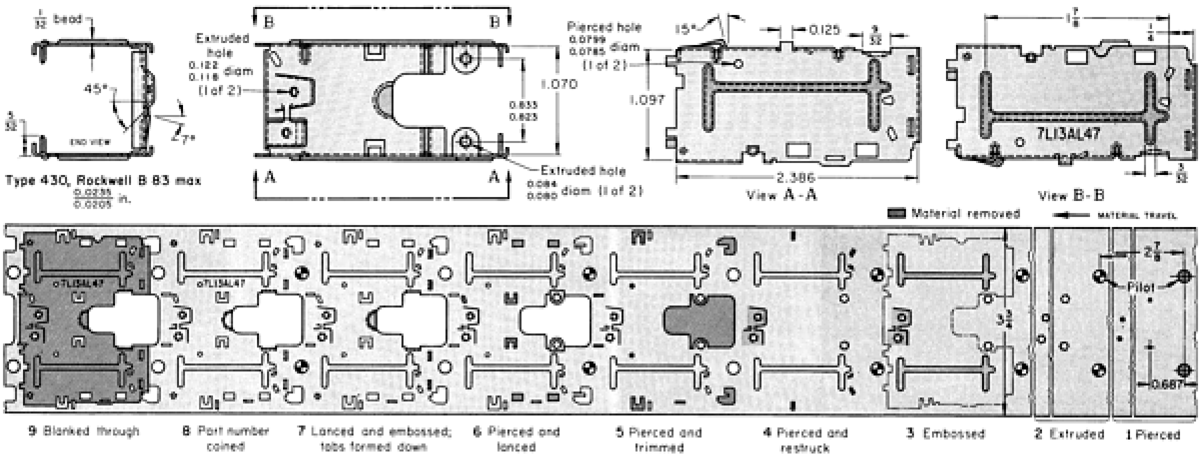


Fig. 13 Frame produced in a nine-station progressive die in the sequence of operations indicated on the strip development shown. Final forming was done in separate dies. Dimensions given in inches.

A nine-station progressive die was used to pierce and flange the holes, to emboss the stiffening beads on the two legs, to trim and form the tabs, to coin identification data, and to blank the part from the strip. Stops were then lanced and formed, and bottom flanges were formed in a forming die. A final forming die was used for the deep side flanges.

The progressive die was run in a 670 kN (75 tonf) mechanical press at a rate of 5000 pieces per hour. The first and second forming dies were run in a 270 kN (30 tonf) press at speeds of 984 and 936 pieces per hour, respectively.

Annual production was 90,000 frames, and demand was expected to increase. This, in addition to the short press time (0.2284 h per 100 pieces, as against an estimated 0.6665 h per 100 pieces if produced in eight separate dies) and the greater accuracy obtainable in the progressive die, justified the higher tooling cost for the progressive-die method (60% higher when compared to separate dies).

The dies were made of A2 tool steel and had a life of 50,000 to 75,000 pieces between regrinds. The lubricant was an emulsifiable chlorinated oil concentrate.

Revised by Joseph A. Douthett, Armco Inc.

Multiple-Slide Forming

Small high-production stainless steel parts can sometimes be formed in multiple-slide machines with the same kinds of tools as those used for the forming of low-carbon steel. Additional information is available in the article "Forming of Steel Strip in Multiple-Slide Machines" in this Volume. The following example describes the forming of a link for a flexible expanding wristband.

Example 11: Multiple-Slide Forming of a Wristband Link.

The workpiece shown in Fig. 14, a link for an expanding wristband, was formed in a multiple-slide machine from stainless steel strip 0.25 mm (0.010 in.) thick by 8.99 mm (0.354 in.) wide, and it was locked in shape by bent lugs. The production rate was 6000 pieces per hour.

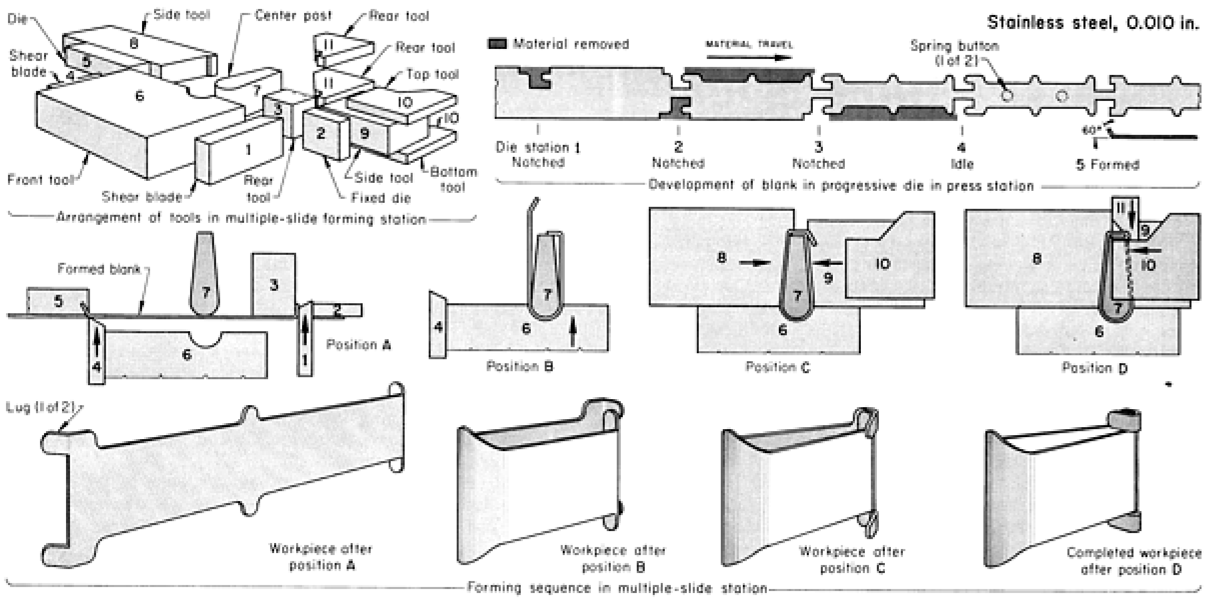


Fig. 14 Operations in the forming of a wristband link in a multiple-slide machine.

The blank for the link was made in a five-station progressive die mounted in the press station of the machine. As shown in the upper right corner of Fig. 14, the strip was notched in stations 1, 2, and 3 by four small heeled punches. For support against side thrust, the heels entered the die before engaging the stock. An air blast entering through holes in the punches removed the scrap in order to protect the die and the feed mechanisms. In the fifth die station, two lugs on the blank were bent 60°. Spring-actuated lifters stripped the blank from the bending section of the die after the lugs were bent. The workpieces were held together by a narrow strip of stock that was left to index the workpiece through the stations of the progressive die.

The blank was then fed to the forming station so that it was edge-up between the center post (7) and the front tool (6), as shown in position A in Fig. 14. As the blank entered the forming station, the center post moved upward into the forming position. The shear blade (1) then moved forward against the fixed die (2) to trim off the joining strip. The shear blade (1) also bent the end of the blank against the auxiliary rear tool (3), which then retracted. The other end of the blank was cut off by the shear blade (4) against the die (5).

After the blank was cut off by the shear blade (4), the front tool (6) bent the workpiece around the center post (7), as shown in position B in Fig. 14.

In position C, the workpiece was formed on the center post by the side tools (8 and 9) while still being held by the front tool (6). The front tool was wide enough to form the full width of the workpiece, including the lugs, but the side tools (8 and 9) were narrower, leaving exposed the top and bottom lugs that had been formed in the last press-die station.

In position D, the front and side tools (6, 8, and 9) held the part against the center post (7), while the rear tools (11) flattened the top and bottom lugs against the center post. The center post was then lowered from the workpiece. The side tool (9), which was spring loaded, slid between the top and bottom tools (10), permitting them to advance to form the top and bottom lugs into a U-shape. The side tool (9) held the workpiece against the front and side tools (6 and 8), while the top tools (10) tucked in the lugs.

With all the other tools holding the closed position against the workpiece, the rear tools (11) moved slightly to press the lugs closed against the top tools (10). As the tools opened, the completed link was then ejected by an air jet.

Deep Drawing

The percentages of reduction obtainable in deep drawing range from 40 to 60% for the chromium-nickel (austenitic) stainless steels of best drawability and from 40 to 55% for the straight-chromium (ferritic) grades (percentage of reduction = $[(D - d)/D] \times 100$, where D is the diameter of the blank and d is the inside diameter of the drawn piece). The amount of reduction obtainable varies greatly with the radius of the die and to a lesser extent with the radius of the punch nose. As the die radius decreases, the drawability decreases, as shown in Table 6 for austenitic stainless steel. Typically used punch and die radii are five to ten times metal thickness. With the ferritic grades, the drawability and ductility usually decrease with increasing chromium content. To offset this, steels with high chromium content are often warmed moderately before drawing.

Table 6 Effect of die radius on percentage of reduction obtainable in the deep drawing of austenitic stainless steel

Percentage of reduction = $[(D - d)/D] \times 100$, where D is the diameter of blank, and d is the inside diameter of the drawn piece

Die radius[(a)]	Reduction in drawing, %
15*t*	**50-60**
10*t*	**40-50**
5*t*	**30-40**
2*t*	**0-10**

(a) *t*, stock thickness

Presses used for the deep drawing of stainless steel differ only in power and rigidity from those used for low-carbon steel. Because of the higher work-hardening rate of stainless steel and its inherent higher strength, presses used for the deep drawing of stainless steel often need 100% more ram force and the necessary frame stiffness to support this greater force.

Dies for drawing stainless steel must be able to withstand the high force and resist galling. For ordinary service, D2 tool steel dies give a good combination of hardness and toughness. On long runs, carbide draw rings have exceptionally long life. Where friction and galling are the principal problems, draw rings are sometimes made of high-strength aluminum bronze. The following example describes an application in which the selection of tool material was critical in order to avoid scoring of the workpiece and to obtain acceptable die life in drawing.

Example 12: Use of a Carbide Blank-and-Draw Ring.

An orifice cup, 25 mm (1 in.) in diameter by 11 mm ($\frac{7}{16}$ in.) deep, was blanked and drawn in one operation. A 1.35 mm (0.053 in.) diam orifice was pierced in the cup in a second operation. The specifications called for the sides of the cup to be free of score marks from the die. The blank was 40.0 mm (1.575 in.) in diameter, cut from 0.97 mm (0.038 in.) thick type 302 stainless steel strip 50 mm (2 in.) wide.

The blank-and-draw tooling shown in Fig. 15 was originally made of tool steel of a grade no longer used. It produced fewer than 50 pieces without scoring the workpieces. The combination blanking punch and draw ring was chromium plated in an attempt to increase its durability. Adhesion of the plating was not satisfactory; the chromium started to peel after 180 pieces had been produced. A draw ring of graphitic tool steel was then tried, but this also scored the workpieces.

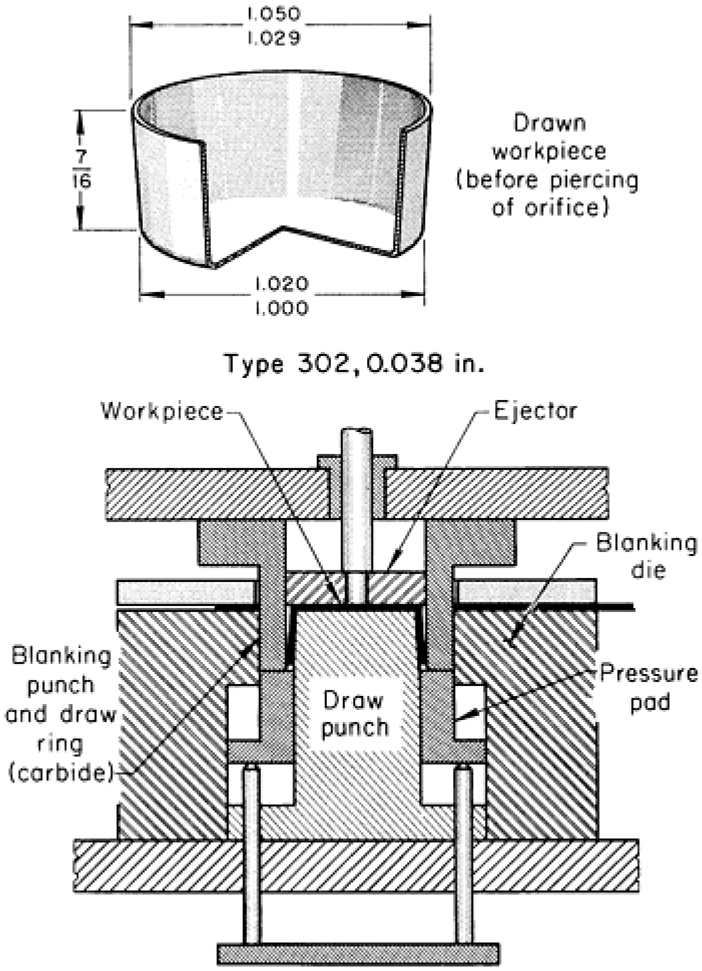


Fig. 15 Forming an orifice cup in a blank-and-draw die with a carbide punch and draw ring. Orifice was pierced in a second operation. Annual production was 60,000 pieces. Rate of blanking and drawing was 670 pieces per hour. Rate of piercing was 153 pieces per hour. Dimensions given in inches.

Finally, a new draw ring was made of sintered carbide consisting of 81% tungsten carbide, 15% Co and 4% Ta--a composition especially recommended for draw dies. The new ring, used with a chlorinated oil-base lubricant, withstood the heat and pressure generated by the severe blank-and-draw operation and produced mar-free parts. Maintenance was negligible, and after 3 years the carbide draw ring had produced 180,000 pieces, with little evidence of wear.

The blanking punch-to-die clearance was 0.05 mm (0.002 in.) per side. Drawing punch-to-die clearance was 0.851 mm (0.335 in.) plus 3° taper per side on the draw punch. The punch nose radius was 0.38 mm (0.015 in.), and the draw radius was 2.4 mm (0.093 in.).

Effect of Worn Draw Rings. The following example shows how the gradual wear of carbide draw rings in severe drawing affected the outside diameter of drawn shells.

Example 13: Effect of Wear of a Carbide Draw Ring on the Diameter of a Drawn Shell.

The carbide draw ring used in deep drawing a shell for pens and pencils made more than 225,000 pieces before it was replaced. Measurements of the pieces were made at production intervals, as shown in Fig. 16.

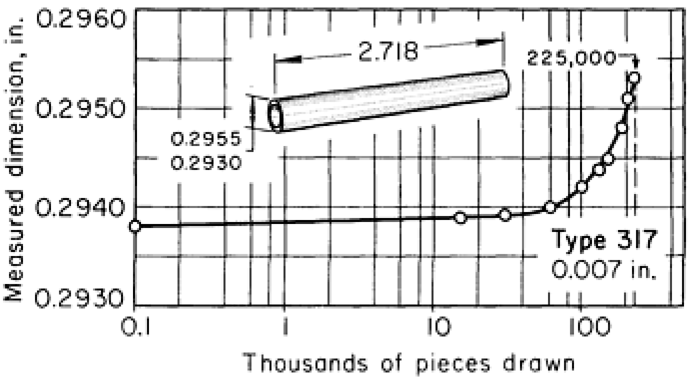


Fig. 16 Variation in diameter of a deep-drawn shell that resulted from wear of the carbide draw ring used. Dimensions given in inches.

Shortly after 225,000 pieces had been drawn, shells began to be produced that would no longer enter the "go" ring gage freely, because of wear on the draw ring. The worn draw ring, which permitted excessive springback, was replaced before the beginning of the next production run.

The shell was drawn from a blank of type 317 stainless steel 48.4 mm (1.906 in.) in diameter and 0.18 mm (0.007 in.) thick to a finished depth of 69.0 mm (2.718 in.) using chromium-plated punches. The shell was made in eight single-station dies, seven drawing and one end forming, at a rate of 600 per hour. The punches had a 2.29 mm (0.090 in.) nose radius, and the draw dies had a 90° conical entrance angle with a 1.52 mm (0.060 in.) radius blending the corners. A mixture of three parts inhibited hydraulic oil and one part chlorinated oil was used as lubricant.

Die clearance for heavy draws is 35 to 40% greater than the original metal thickness for austenitic alloys. For the ferritic alloys, which thicken less, 10 to 15% is generally adequate.

Figure 17 shows a profile of an austenitic stainless steel drawn part that illustrates the thickening pattern observed in drawing a cup from this material. If the process is one of stretching more than of drawing, the clearances do not have to compensate for natural thickening.

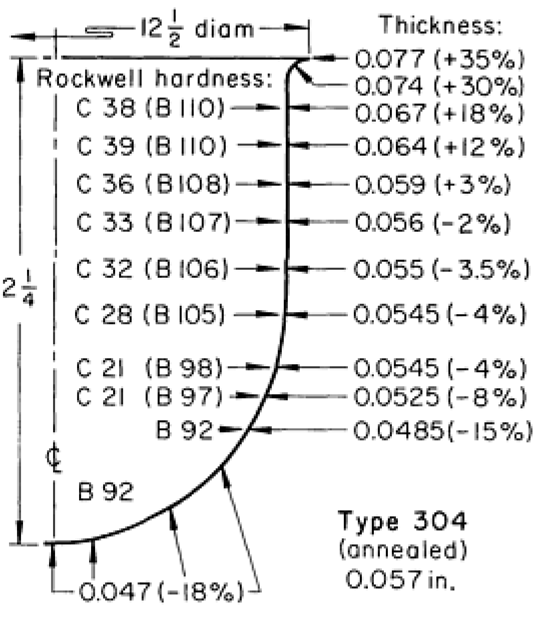


Fig. 17 Profile of a shell drawn from an austenitic stainless steel showing variations in hardness and thickness produced by drawing. Dimensions given in inches.

Clearances of less than the metal thickness are generally not used with stainless steel, because they result in ironing (squeezing of the metal between the punch and die). The austenitic stainless steels are not suited to ironing, because their high rate of work hardening promotes scoring and rapid wear of the dies. In addition, any substantial ironing in the drawing of austenitic stainless steels greatly increases the likelihood of fracturing the workpiece.

The example below describes an application in which the work metal was changed from galvanized carbon steel to a thinner ferritic stainless steel without a revision of die clearance. The resultant problems were solved by substituting an austenitic stainless steel that was better suited to the original clearance even though it had the same thickness as the ferritic steel.

Example 14: Matching Work Metal to Die Clearance.

Using the tooling shown in Fig. 18, basins were made at the rate of 10,000 to 15,000 pieces per year from galvanized carbon steel, 1.27 mm (0.050 in.) thick. The press was an 8900 kN (1000 tonf) hydraulic press with an air-over-oil pressure pad and a draw rate of 152 mm (6 in.) in 5 s. The punch, draw ring, and pressure pad for the drawing die (Fig. 18a) were hardened cast iron. Carbide inserts were used as cutting edges on the trimming punch and die (Fig. 18b). The locator on the trimming die was molded plastic, and the die plate was cast iron. Both dies were used side-by-side in the press at the same time because it had enough capacity to draw and trim in one stroke. Therefore, a finished piece was produced with each stroke of the press, using manual transfer.

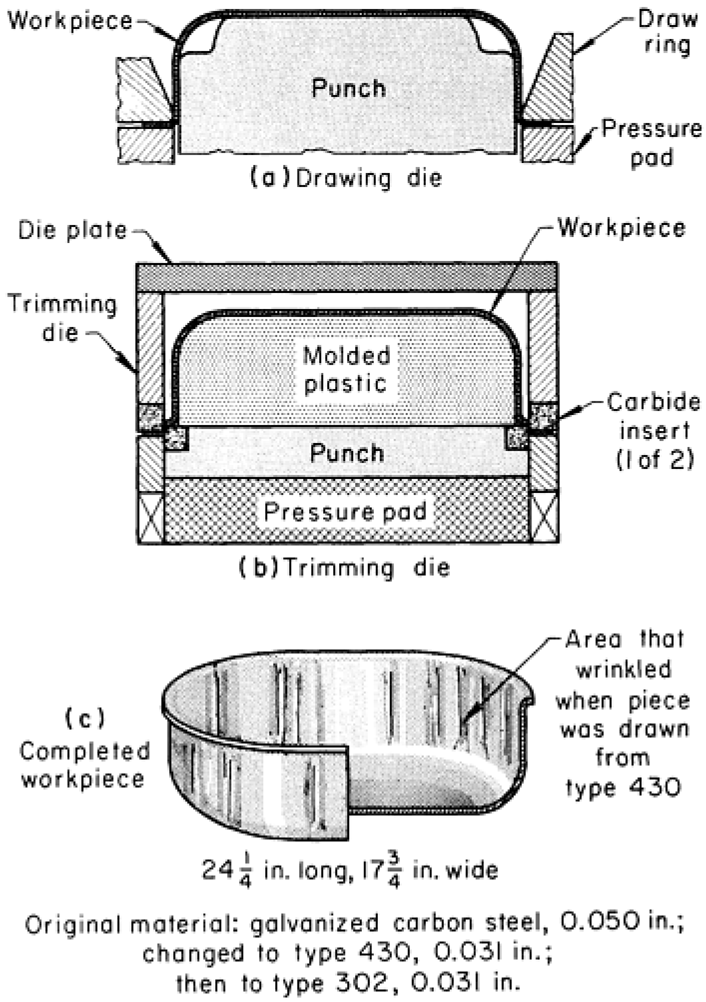


Fig. 18 Setups for drawing and trimming a basin. Die clearance (1.40 mm, or 0.055 in., per side) and drawing radius (6.4 mm, or $\frac{1}{4}$ in.) were not changed when 0.79 mm (0.031 in.) thick type 430 was substituted for 1.27 mm (0.050 in.) thick galvanized carbon steel as the work metal, and wrinkles resulted in drawing. Dimensions in figure given in inches.

To produce a more corrosion-resistant basin, type 430 stainless steel was substituted for the galvanized carbon steel. The type 430 was only 0.79 mm (0.031 in.) thick in order to minimize the increase in material costs; however, the same tooling was used because the relatively low annual quantity did not warrant the cost of retooling. Because the hold-down forces were not suitable for the ferritic stainless steel, several hundred pieces out of the first run were fractured in drawing.

When the hold-down pressure was adjusted to a level suitable for a ferritic stainless steel, contraction wrinkles formed where the material entered the throat of the die (Fig. 18c) because the die clearance was too great. The corners of the blank were cropped, the viscosity of the lubricant was changed, and the hold-down pressures were more closely adjusted in an effort to control wrinkling.

The data given in Table 7 for noncylindrical draws provide an explanation for the difficulties encountered in changing the work metal as well as guidance for the selection of a suitable stainless steel. According to these data, the thickness of type 430 stock that could best be formed by the die would be the same as that of the carbon steel previously formed. In addition, if the stock thickness were reduced, an austenitic steel such as type 302 could be used. The die clearance was 1.4 mm (0.055 in.) per side, and the draw ring radius was 6.4 mm ($\frac{1}{4}$ in.). Therefore, the die was suited for the 1.27 mm (0.050 in.) thick carbon steel, but not for the 0.79 mm (0.031 in.) thick type 430. However, 0.79 mm (0.031 in.) thick type 302 would be closely matched to the die capacity.

Table 7 Effect of die clearance and draw ring radius on noncylindrical draws of stainless steel

Stock thickness, *t*		Die clearance per side					
		Carbon steel		**Type 430**		Types 302 and 304	
mm	**in.**	**mm**	**in.**	**mm**	**in.**	**mm**	in.
1.27	0.050	1.40	0.055	1.40	0.055	2.29	**0.090**
0.76	**0.030**	**0.84**	**0.033**	**0.84**	**0.033**	**1.37**	**0.054**

Stock thickness, *t*		Draw ring radius				
		Carbon steel		**Type 430**		Types 302 and 304
mm	**in.**	**mm**	**in.**	**mm**	**in.**	
1.27	0.050	6.4-9.5	$\frac{1}{4}$ - $\frac{3}{8}$	6.4-9.5	$\frac{1}{4}$ - $\frac{3}{8}$	**4*t* min**

A change was made to type 302 stainless steel, 0.79 mm (0.031 in.) thick, with no further difficulty. A change to 1.27 mm (0.050 in.) thick stock of type 430 might have been successful.

Speed of drawing has an important bearing on the success of the draw. A rate of 6 to 7.5 m (20 to 25 ft) per minute is a good compromise between the rate of work hardening and the uniform distribution of stress. With proper forming techniques, the rate of fracture at this speed is often less than 2%.

Lubricants. Ordinarily, both sides of the workpiece need to be lubricated for each draw. If too little lubricant is used, tools may accumulate enough heat during a production run to cause the work metal to fracture because of galling. In tests with a minimum of lubricant, failures occurred after 25 draws.

The chemical type and the viscosity of the lubricant are both important. Either chemical or mechanical EP activity (see the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume) is needed for the severe deep drawing of stainless steel.

Viscosity or pigment loading must not be too high or too low. Too thick a lubricant can cause wrinkling of compressed metal; too thin, seizing or galling. The ability to remove a lubricant readily is also important. In general, the higher the viscosity, the more difficult the lubricant is to apply and remove.

The same characteristics that make chlorinated oils and waxes useful for the press forming of stainless steel (see the section "Press-Brake Forming" in this article) also make them useful for the deep drawing of these alloys. Table 2 lists other lubricants used in the deep drawing of stainless steel. Pigmented pastes and dry films are also effective (and in some cases superior) in deep drawing.

In Example 15, changing from a viscous mineral oil to a low-viscosity mineral oil blend of a chlorinated wax eliminated wrinkling and galling. Sometimes, however, there is no substitute for the physical separation and equalization of pressure provided by pigments, as in Example 16.

Example 15: Effect of Reducing Viscosity and Adding Chlorinated Wax to Mineral Oil Lubricant in Deep Drawing.

A coffeepot was deep drawn from a type 302 stainless steel blank, 355 mm (14 in.) in diameter by 0.81 mm (0.032 in.) thick, in two deep draws and one bulging operation. At first, the blanks were lubricated by brushing both sides with mineral oil having a viscosity of 6000 SUS at 40 °C (100 °F). The workpiece wrinkled in the first draw, and it galled in the second draw and in bulging.

The lubricant was replaced with a thinner mineral oil (viscosity: 500 SUS at 40 °C, or 100 °F) that was fortified with a chlorinated wax. The lubricant was brushed on, as before. Not only did the use of the modified lubricant eliminate the wrinkles in the first draw, but enough lubricant remained on the surface to prevent galling in the two other operations. Even though a fluid of much lower viscosity was used, the tenacity imparted by the chlorinated wax permitted the retention of sufficient lubricant for the subsequent bulging and deep-drawing operations.

Example 16: Pigmented Paste Versus Chlorinated Oil for Deep Drawing.

For easy cleaning in a vapor degreaser, highly fortified oils were specified for the deep drawing of a rectangular shell from 0.89 mm (0.035 in.) thick type 304 stainless steel. Chlorinated and sulfochlorinated oils with viscosities of 4000 to 20,000 SUS at 40 °C (100 °F) failed to eliminate welding to the dies and splitting of the workpiece at the corners. The shell, a well for a steam table, was deep drawn from a rectangular blank measuring 760 × 585 mm (30 × 23 in.) with corners trimmed at 45°. The shell was drawn in one operation, and the flange was then trimmed. Interior dimensions of the drawn shell were 510 × 305 × 150 mm (20 × 12 × 6 in.). Bottom corners had 16 mm ($\frac{5}{8}$ in.) radii; vertical corners, 29 mm ($1\frac{1}{8}$ in.) radii; and the flange, a 6.4 mm ($\frac{1}{4}$ in.) radius. The shell had approximately 3° taper on each side. The clearance between the punch and die was equal to the stock thickness.

The oil-type lubricant was replaced with a highly pigmented water-miscible fatty paste, diluted with two parts of water, which was applied to both sides of each blank by rollers. This lubricant eliminated the welding and allowed enough metal flow to prevent splitting. The drawn parts were cleaned with hot alkaline solution in a soak tank.

Lubricant Location. The location of the lubricant on the blank is also critical in the successful fabrication of a drawn part. Because all draws are made up of a combination of stretching and deep drawing, the lubricant location often depends on which type of forming is dominant. In a stretch condition, lubricant should especially be applied on the steel surface contacting the punch so that friction is minimized and the steel slips over the punch surface during stretching and thinning. Under deep-draw conditions, the steel surface contacting the die is definitely lubricated in order to allow ease of movement into the die cavity. However, whether stretch or deep drawing dominates, some lubricant is necessary on both steel surfaces to minimize the galling tendencies of stainless alloys.

Drawing Cylindrical Parts. When a part is made in several drawing operations, the amount of reduction in redrawing is related to the condition of the metal in the first drawing operation (cupping). If the material is highly stressed because of excessive blankholder pressure or because of small die radius, very little reduction can be made in the second operation.

General practice on the more formable grades of austenitic stainless steel is to allow 40 to 45% reduction in the first operation, followed by a maximum of 30% in the second operation, if the workpiece is not annealed between draws. With an anneal, the second reduction is usually 30 to 40%. On some parts, it may be preferable to spread the reduction over four draws before annealing--for example, successive reductions of 35, 30, 20, and 10%.

There is usually a decrease in drawability upon redrawing, and the greatest total reduction in a two-draw operation is most often produced by having the first-stage reduction as large as possible. During redrawing, it is advisable to use a tapered or rounded-end internal blankholder or sleeve to allow easy flow of metal into the die, as indicated in the article "Deep

Drawing" in this Volume. An internal blankholder with small-radius 90° corners causes the metal to be bent severely through two 90° bends before flowing into the die.

Optimal drawability is available at ram speeds of not more than 6 to 9 m (20 to 30 ft) per minute. Because of the strain-rate sensitivity of most stainless steels, work hardening of these alloys is minimized by slow forming.

The following example describes an application in which small shells were deep drawn in several steps to reduce the amount of work done in a single operation. Because production quantities were small, individual dies were more economical than a transfer die.

Example 17: Seven-Step Deep Drawing of a Fountain-Pen Cap.

Fountain-pen caps of various closely related designs were made on the same production line by one blanking and cupping operation and six redraws. A flat blank of type 302 stainless steel having a hardness of 83 to 88 HR15-T was used. The first five draws were usually the same for any of the caps made on the line; therefore, to set up for a different size of cap, only the compound blank-and-cup die and the last die (or, for some caps, the last two dies) needed to be changed. As a result, the changeover time was only about 45 min.

In the first operation, which was done in a 160 kN (18 tonf) mechanical press, a compound blank-and-cup die equipped with a rubber die cushion was used to cut circular blanks from 0.267 to 0.279 mm (0.0105 to 0.0110 in.) thick strip and to draw them into a cup. To make a typical cap 90 mm ($3\frac{1}{2}$ in.) long by 8.55 to 8.57 mm (0.3365 to 0.3375 in.) in outside diameter, a blank 55.9 mm (2.200 in.) in diameter was cut from stock 57 mm ($2\frac{1}{4}$ in.) wide and was drawn into a cup 19 mm ($\frac{3}{4}$ in.) deep by 31.8 mm (1.250 in.) in diameter--a 43% reduction in diameter. Reductions in the subsequent redraws were 27, 22, 18, 18, 16, and 15%, respectively. All except the last redraw were done in 35 kN (4 tonf) hydraulic presses with 152 mm (6 in.) strokes. The final redraw was made in a 55 kN (6 tonf) hydraulic press with a 305 mm (12 in.) stroke.

The draw dies were carbide inserts 13 to 16 mm ($\frac{1}{2}$ to $\frac{5}{8}$ in.) thick. The die openings had a 4.8 mm ($\frac{3}{16}$ in.) radius blending with a 1.6 mm ($\frac{1}{16}$ in.) wide land. There was a 2° relief per side below the land. The high-speed steel punches had a 2.4 mm ($\frac{3}{32}$ in.) nose radius and were chromium plated for smoothness and wear characteristics. The workpiece was pushed through the die and stripped from the punch by a split stripper plate under the draw die. The strippers were closed by cam action from the press stripper rod.

Because production quantities of any one part were small, this technique was preferable to making a transfer die for each of the several caps produced on this line. Operations were set up in machines in the line as they were needed and as the machines became available.

The final draw, which was the deepest, governed the final production rate of 575 pieces per hour. However, when there was a backlog of pieces, this operation was set up on two machines at the same time.

The blank-and-cup die made about 45,000 pieces before resharpening. The draw rings were used for 150,000 to 200,000 pieces before wear was too great. Dies in the first few draws were allowed to wear over a fairly wide range. As the die opening increased, clearance was maintained by increasing the thickness of the chromium plating on the punch. When the die openings were 0.10 to 0.13 mm (0.004 to 0.005 in.) oversize, the dies were replaced, and punches were returned to the original size by stripping, polishing, and replating.

The lubricant was a mixture of one part sulfur-free chlorinated oil with three parts inhibited hydraulic oil having a viscosity of 250 SUS at 40 °C (100 °F). This lubricant was furnished to all presses through a central pumping system.

Critical tolerances on these fountain-pen caps were ±0.02 mm (±0.001 in.) on outside diameter and ±0.01 mm (±0.0005 in.) on inside diameter. Holding the clearance between the draw die and punch to 10% greater than stock thickness helped to maintain these tolerances.

Steel Drawing Forces. Estimates of the maximum drawing forces necessary to form cups from an austenitic stainless steel, a ferritic stainless steel, and low-carbon steel are compared in Table 8. These drawing forces, in tons of force, are based on the formula $S\pi Dt$, where S is the tensile strength of the metal in tons of force per square inch, D is the cup diameter in inches, and t is the metal thickness in inches.

Table 8 Force required for drawing two stainless steels and low-carbon steel of 1.27 mm (0.050 in.) thickness to various diameters

Diameter of piece		Approximate drawing force required					
		Austenitic stainless steel, type 18-8		**Ferritic stainless steel, 17% Cr**		Low-carbon steel	
mm	**in.**	**kN**	**tonf**	**kN**	**tonf**	**kN**	tonf
125	5	350	39	180	20	160	**18**
255	10	700	78	520	59	350	**39**
510	**20**	**1400**	**157**	**1040**	**117**	**700**	**78**

Blankholding pressures for the austenitic alloys must be much higher than those for the ferritic types or low-carbon steels. For austenitic alloys, the pressure, P, on the metal under the blankholder is usually about 6.9 MPa (1.0 ksi); for the ferritic alloys, 1.4 to 3.4 MPa (0.2 to 0.5 ksi). Thinner material and larger flange areas generally require greater pressure.

Drawing Hemispherical Parts. The drawing of hemispherical, or dome-shaped, parts demands special attention to blankholder pressure to prevent wrinkling because so much of the metal surface is not in contact with any die surface for most of the draw. Only the very tip of the punch is in contact with the work at the start of the stroke, and the surface between the tip and the blankholder draws or stretches free until the punch descends far enough to contact it. An undersize punch can sometimes be used to draw or stretch the blank into a preform before the dome-shaped punch makes the final draw, as in the following example.

Example 18: Two-Stage Drawing of a Stepped-Diameter Hemisphere.

One of the critical points in the production of the vacuum-bottle top shown in Fig. 19 was the forming of the shoulder at the large end of the dome-shaped top. The stepped inside diameter of this shoulder had to be an exact fit with the body of the vacuum-bottle jacket. The pierced hole at the small end also had to be accurately formed to conform to the mouth of the inner container.

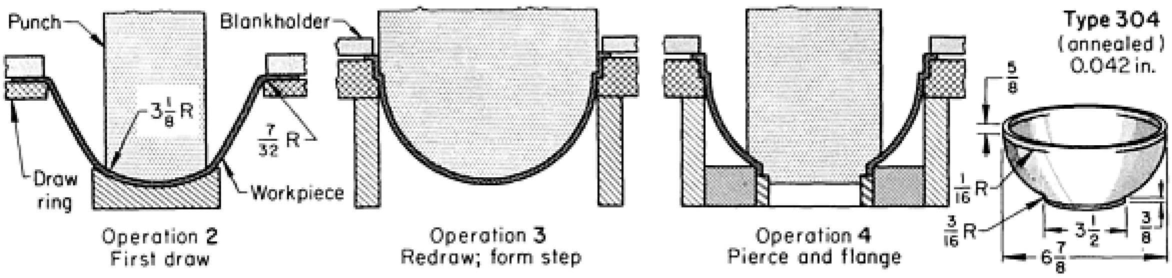


Fig. 19 Production of a stepped-diameter flanged hemisphere, in which a narrow punch was used in predrawing

the dome. The piece was drawn from a 280 mm (11 $\frac{1}{8}$ in.) diam blank produced in operation 1 (not shown). Dimensions in figure given in inches.

The stock was 289 mm (11.375 in.) wide annealed type 304 stainless steel strip, 1.1 mm (0.042 in.) thick. A single-action mechanical press with a spring-loaded pressure pad was used to cut 280 mm (11 $\frac{1}{8}$ in.) diam blanks from the strip, leaving 3.2 mm ($\frac{1}{8}$ in.) minimum scrap on each side of the strip.

The first draw was made in a 2200 kN (250 tonf) double-action mechanical press. The punch was 83 mm (3 $\frac{1}{4}$ in.) in diameter; therefore, much of the surface of the dome was drawn free (operation 2, Fig. 19). This required careful control of the blankholder pressure to prevent puckers and wrinkles. Blankholder pressure had to be adjusted for every lot of steel; it varied from 5.5 to 6.9 MPa (0.8 to 1.0 ksi). The die radius also had to be held closely (5.2 times the stock thickness). The first draw produced a cup 175 mm (6 $\frac{7}{8}$ in.) in diameter with a 235 mm (9 $\frac{1}{4}$ in.) diam flange.

The second draw was also made in the 2200 kN (250 tonf) double-action press. The punch for the second draw was shaped to the required inner contour of the part, including the step at the base of the dome, which was formed as the press bottomed at the end of the second draw stroke (operation 3, Fig. 19). This operation formed the dome shape of the bottle top by reshaping (mostly by stretching) the cup formed in the first draw. The metal for the cylindrical area above the step was drawn from the flange metal remaining after the first draw.

In the fourth operation, the hole in the top of the dome was pierced, and an internal stretch flange was formed around the hole. This was done with a spring-loaded piercing die, which gave sufficient resistance to let the piercing punch shear the material and then retreat under pressure from the flange-forming part of the punch. Both ends of the part were later trimmed in a lathe.

Drawing Rectangular Parts. During the deep drawing of a box-shaped part, the metal in the corners of the part and in the flange around the corner undergoes a change much like that which takes place when a round shell is drawn from a circular blank. Metal is compressed at the corners, and significant thickening occurs where the metal flows into the corners. The sides of the box undergo essentially no thickening, because there is no compression of the metal in the flange areas as it flows or bends over the die radius.

Clearances in the sides between the punch and die are ordinarily about 10% greater than the metal thickness to compensate for gage variations and to allow for metal flow. At the corners, punch-to-die clearances are similar to those used for cylindrical parts to allow for thickening.

Blankholding devices are almost always used in producing deeply recessed box-shaped parts in order to control the metal movement, particularly in the corners. The corners are under severe strain because of the intense compression of the flange metal, and most fracturing, if it does occur, takes place in the lower wall corner sections.

Punch and die radii are generally the same for rectangular draws as for circular draws. Some fabricators prefer to make the punch and die radii at the corners larger than along the sides in order to equalize the stress in the metal at the corners. The top surface of the draw die and the draw radii should be polished smooth (free of grind marks and well blended) to prevent localized retardation of metal flow with resultant uneven drawing of the metal. Burrs and bent edges on the blank often restrict metal flow or movement along the blankholder surface to such a degree that vertical wall fractures can occur.

Semideveloped blanks usually produce better results than rectangular ones. There are a number of patterns for trimming the corners, ranging from a simple 45° trim to patterns with a carefully developed area containing the optimal volume and area of metal.

The economic success of the run is related to tool wear and scrap rate. The following example describes a combination of tool materials that has given satisfactory performance in terms of parts or draws per regrind and redress. The same tooling can be used for both drawing operations, with the draw ring reversed to present a different radius for the second draw, as in the following example.

Example 19: Use of a Reversible Two-Radius Draw Ring for Drawing and Redrawing of a Flanged Rectangular Shell.

The flat-flanged single-sump kitchen sink shown in Fig. 20 was formed in four operations: blank, draw, redraw, and trim. Forming of the part was a combined draw-and-stretch operation. Because several different models, with drain holes in various locations, were made from the same drawn part, the drain hole was not pierced in the trimming operation but was made separately. The production rate was 50,000 to 100,000 pieces per year.

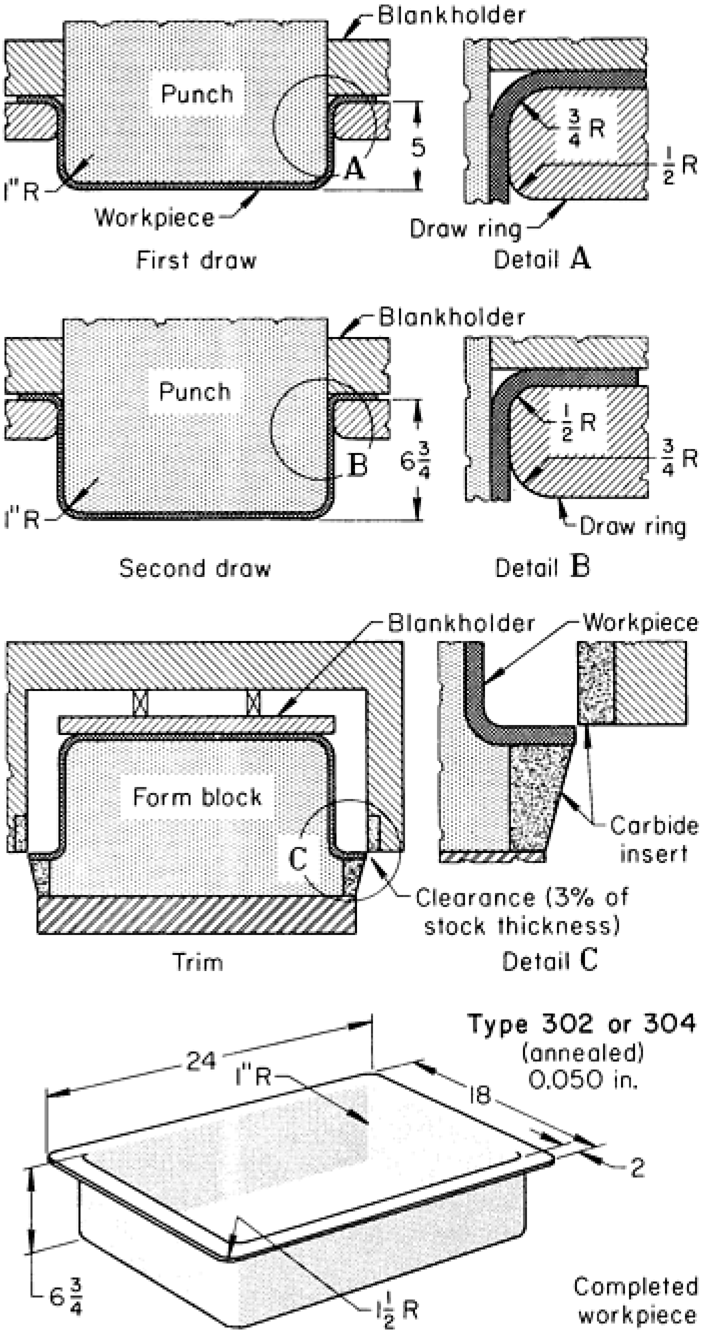


Fig. 20 Production of a flat-flanged sink basin by drawing and redrawing (using a two-radius reversible draw ring) and trimming. Dimensions given in inches.

The material was annealed type 302 or 304 stainless steel coil stock 735 mm (29 in.) wide and 1.27 mm (0.050 in.) thick, with a No. 2D sheet finish. Blanks 635 mm (25 in.) long were sheared from the coil at the rate of 40 per minute in a single-action mechanical press. Corners of the blanks were trimmed at 45°, removing 50 mm (2 in.) from each edge of the blank at each corner. Clearance for this trimming was kept at less than 5% of metal thickness to minimize edge distortion and burrs.

The draws were made in a 3600 kN (400 tonf) double-action mechanical press with 2200 kN (250 tonf) available for blank holding. The draw punch was made of alloy tool steel, and the blankholder was made of alloy cast iron. The reversible draw ring (Fig. 20) was made of hard aluminum bronze and had a 19 mm ($\frac{3}{4}$ in.) draw radius on one side for the first draw and a 13 mm ($\frac{1}{2}$ in.) draw radius on the other side for the redraw. The workpiece was annealed in an inert atmosphere at 1065 °C (1950 °F) between the first and second draws and then air cooled rapidly to room temperature.

The depth of the sink after the first draw was 127 mm (5 in.); after the second draw it was 170 mm (6 $\frac{3}{4}$ in.). Draws were made at a punch speed of approximately 6.4 m (21 ft) per minute, with less than 2% of the workpieces fracturing.

A similar 3600 kN (400 tonf) press was used to trim the piece. Carbide inserts provided shearing edges for the trimming operation. The sink was held on a form block of molded plastic or cast iron for trimming.

The second draw operation sharpened the bottom and flange corner radii and stretched the bottom surface and the side walls to remove any loose metal. Little or no metal was drawn into the part from the flange during the second draw.

Revised by Joseph A. Douthett, Armco Inc.

Spinning

Stainless steel parts such as cups, cones, and dished heads can be readily formed by manual or power spinning, although more power is required than that needed for the spinning of low-carbon steel. Equipment and techniques for these processes are described in the article "Spinning" in this Volume.

Manual Spinning. The amount of thinning that occurs during manual spinning is related to the severity of the formed shape. A cross section is shown in Fig. 21 of a manually spun piece that thinned out to such a degree that it often fractured. This piece was excessively worked, and the mid-center area was work hardened beyond the capacity of the material, causing the workpiece to fracture. The piece was later made by press drawing the dome-shaped cup and spinning the broad flared flange.

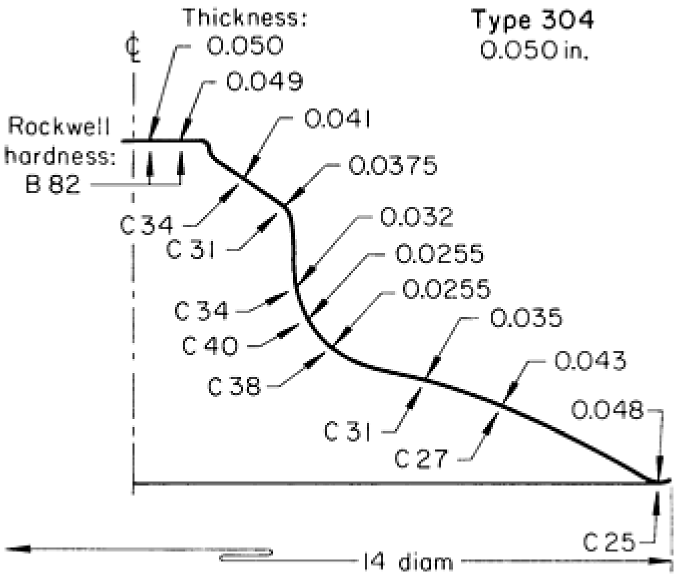


Fig. 21 Profile of shape, hardness, and thickness of a manually spun part that often fractured in its thinnest section. Dimensions given in inches.

The approximate limits of stretch in manual spinning are given in Table 9. These are for 1.57 mm (0.062 in.) thick fully annealed stock. The second stretch after annealing is about 8% less than the first. The amount of stretch is not necessarily uniform over the entire part; it varies with the severity of the form.

Table 9 Approximate limits of stretch in the manual spinning of stainless stools 1.57 mm (0.062 in.) thick

Type	Stretch (max), %
305	**45**
302	**40**
304	**40**
302B	**35**
316	**35**
316L	**35**
321	**35**
309	**30**
310	**30**

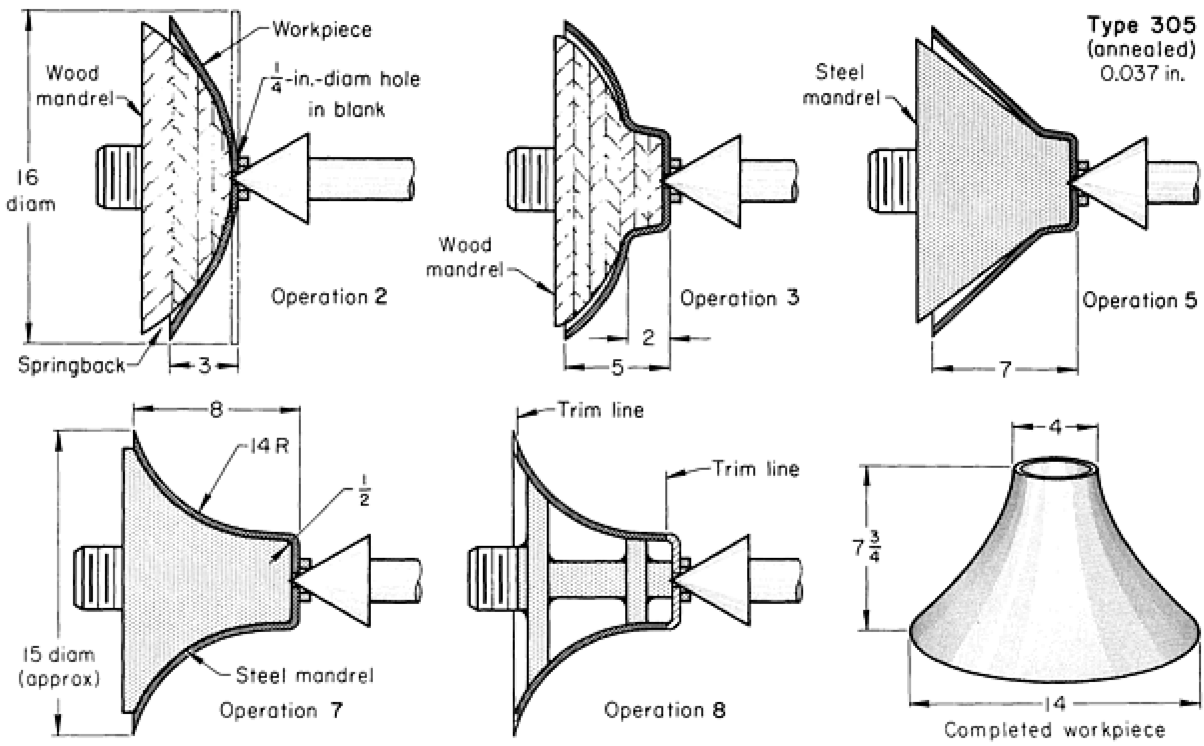
317	**30**
430	**30**
201	**25**
202	**25**
301	**25**
405	**25**
446	**25**
403	**20**
410	**20**

These limits are for stretching during one spinning pass; after being annealed, the metal can be respun to 8% less than the first stretch.

Although 300-series stainless steels can be formed by spinning, 302, 304, and 305 can be spun to greater reductions than other stainless steels before intermediate annealing becomes necessary. All anneals must be followed by pickling to remove oxides, thus restoring the clean, smooth surface. The No. 1 strip or 2D sheet finish is best for severe applications because the metal is in the softest stress-free condition and will take the greatest amount of working. The following example demonstrates the spinnability of type 305 stainless steel.

Example 20: Four-Pass Manual Spinning of a Cone From Type 305 Stainless Steel.

The 355 mm (14 in.) diam cone shown in Fig. 22 was produced in eight operations, including four manual spinning passes, from a 405 mm (16 in.) diam blank of 0.94 mm (0.037 in.) thick annealed type 305 stainless steel that had a No. 2D sheet finish or a No. 1 strip finish. Other types of austenitic stainless steel could have been used, but the reduction per pass would have been lower, in proportion to the increase in rate of work hardening.



Sequence of operations

Drill a 6.4 mm ($\frac{1}{4}$ in.) diam center hole in a 405 mm (16 in.) diam blank 0.94 mm (0.037 in.) thick.
Spin to 75 mm (3 in.) depth on a laminated hardwood mandrel at 300 rpm, applying manual pressure on lever and roller.
Spin to 125 mm (5 in.) depth on a second laminated hardwood mandrel to within 25 mm (1 in.) of edge.
Anneal in hydrogen atmosphere at 1040 °C (1900 °F); air cool.
Spin to 178 mm (7 in.) depth on a steel mandrel to within 25 mm (1 in.) of edge.
Anneal as in operation 4.
Spin to 205 mm (8 in.) depth and final shape on a steel mandrel.
Lathe-trim top and bottom ends to 195 mm (7 $\frac{3}{4}$ in.) final height of cone.

Fig. 22 Production of a stainless steel cone by four-pass manual spinning. Dimensions in figure given in inches.

As shown in Fig. 22, the mandrels for spinning were made of wood or steel. The spinning roller was made of hardened steel. Pressure was applied to the entire blank in the first spinning pass. In the three other passes, the outer 25 mm (1 in.) of the blank was not spun. This caused the edge to thicken to 1.78 mm (0.070 in.) and helped hold the outer shape. Thinning was greatest at the middle of the cone, to about 0.69 mm (0.027 in.) wall thickness (28% reduction). The surface area of the piece was increased 40%. The drastic working that accompanied the thinning and the increase in area made two anneals necessary (see sequence of operations, Fig. 22). Annual production quantity was 500 pieces.

The 400-series stainless steels, because of their relatively low ductility, do not adapt readily to manual spinning, especially when the deformation is severe. The high pressure of the forming tool causes wear of the work metal, resulting in early thinning and fracturing. Figure 23 shows forming speeds used for manual spinning of 400-series stainless steels.

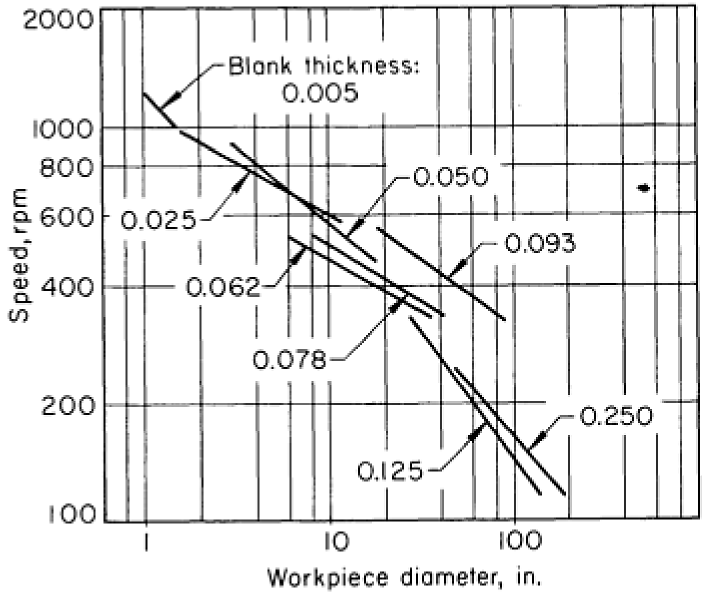


Fig. 23 Effect of workpiece diameter and blank thickness on rotational speed for the manual spinning of austenitic stainless steel. Dimensions given in inches.

The surface of severely spun parts is often very rough because of the action of the tools on the metal, and the production of a buffed or highly polished finish on a part spun from a 400-series stainless steel can be expensive. It is generally necessary to rough grind the material to smooth out the irregularities before polishing and buffing.

Typical stock thicknesses of stainless steel for manual spinning are 0.30 to 3.18 mm (0.012 to 0.125 in.), although stainless steels as thin as 0.13 mm (0.005 in.) and as thick as 6.35 mm (0.250 in.) have been spun by hand. The corner radius should be at least five times the thickness of the work metal. Allowance must be made in the size and shape of mandrels for springback and for heat-induced dimensional changes.

Power spinning is used for severe reductions and for work that cannot be done by hand. Stainless steels in both the 300 and 400 series are readily formed by power spinning, but the low-work-hardening types 302 and 305 are superior. Much larger reductions of type 430 can be made by power spinning than by manual spinning.

Spinning can be done hot or cold, although the severe reduction accompanying power spinning may cause so much heat that the spinning that began cold becomes warm spinning. Hot spinning, done only above 790 °C (1450 °F), is commonly used for work 4.8 to 13 mm ($\frac{3}{16}$ to $\frac{1}{2}$ in.) thick. The need for careful control of the temperature makes it difficult to hot spin metal that is less than 6.4 mm ($\frac{1}{4}$ in.) thick. Thicker stainless steel can be hot spun as easily as low-carbon steel.

Cracking at the edge is the main problem in the power spinning of austenitic stainless steels. The edge of the blank may need to be ground smooth to prevent cracking. A generous trim allowance is helpful so that the cracked edge can be cut off. Cracking and distortion can be prevented by keeping a narrow flange on the work. If the size of the spun piece is not correct after it cools (because of springback and heat expansion), the piece can be annealed and spun to size while it is still above 150 °C (300 °F).

Considerable thinning can be produced by power spinning, as indicated by the cross section of a deeply spun vessel shown in Fig. 24. The thickness of the vessel was reduced from 1.90 to 0.66 mm (0.075 to 0.026 in.) in one spinning operation. A preformed cup 152 mm (6 in.) in diameter and 75 mm (3 in.) deep, drawn on a conventional press, was used as the starting shape. The top of the vessel is much thicker than the wall. Thickening of the rim occurred during drawing, and it remained thick because there was essentially no deformation in this region during spinning.

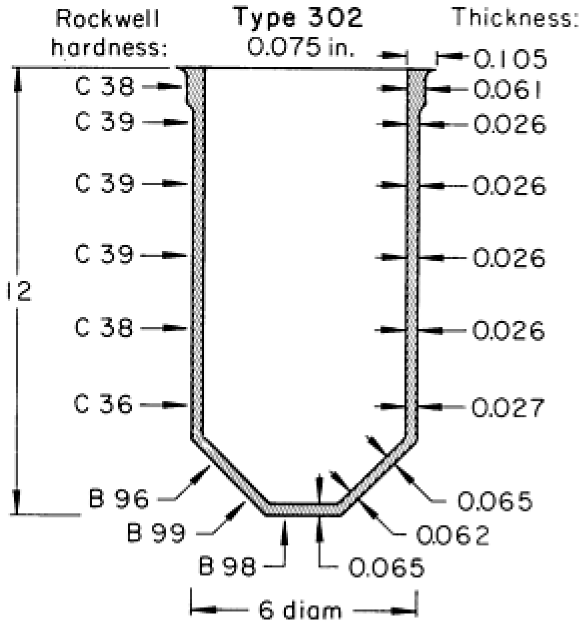


Fig. 24 Variations in hardness and thickness of a shell that was power spun from a preform drawn from stainless steel 1.9 mm (0.075 in.) thick. Dimensions in figure given in inches.

The surfaces of power-spun pieces are rough, and extensive finishing is required to make them smooth and bright. The spun surface is rough because the roller usually imparts a spiral or helical groove to the surface as the roller is fed into the metal while it rotates. Except for this disadvantage, power spinning is an excellent way of forming pieces from stainless steel.

Lubricants (see Table 2) are used to reduce friction, to minimize galling and tool drag, and to provide cooling. For manual spinning, firmly adherent lubricants are preferred; for power spinning, coolant action is more important. Lubricants containing sulfur or chlorine are usually avoided; they are difficult to remove completely and have harmful effects on heated stainless surfaces.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Rubber-Pad Forming

Annealed austenitic stainless steels--types 301, 302, 304, 305, 321, and 347--are rubber-pad formed in thicknesses to 1.3 mm (0.050 in.). Most of the operations are straight flanging, especially in thicker workpieces. With auxiliary devices, such as wedges or rollers, pieces up to 2.0 mm (0.078 in.) thick can be formed. Flanges must be wide enough to develop adequate forming force from the unit pressure on their surface. For annealed stainless steels, the following minimum flange widths beyond the bend radius are recommended for successful forming:

Thickness	Flange width

mm	**in.**	**mm**	in.
0.41	0.016	6.35	**0.250**
0.51	0.020	6.86	**0.270**
0.64	0.025	7.37	**0.290**
0.81	0.032	8.38	**0.330**
1.02	0.040	9.14	**0.360**
1.30	0.051	10.0	**0.410**
1.63	0.064	12.2	**0.480**
1.83	**0.072**	**13.0**	**0.510**

In quarter-hard temper, types 301 and 302 up to 0.81 mm (0.032 in.) thick can be flanged if the flange is at least 16 mm ($\frac{5}{8}$ in.) wide.

The rubber-pad forming of contoured flanges in stainless steel requires more powerful equipment than that used for flat flanges. Most forming of contoured flanges is done on annealed stainless steel, but a limited amount is done on quarter-hard stock.

Stretch flanges are readily formed on annealed stainless steel up to 1.3 mm (0.050 in.) thick. Rubber-pad-formed stretch flanges of thin metal are generally smoother and more accurately formed than those formed by single-action dies. Die-formed flanges often curl outward, requiring considerable hand work for correction.

The hydraulic presses used in the Guerin process develop forming pressures to 34.5 MPa (5 ksi). Narrow stretch flanges that require pressures greater than 34.5 MPa (5 ksi) are formed with the aid of auxiliary devices, such as traps and wedge blocks, that raise the forming pressure locally (see the article "Rubber-Pad Forming" in this Volume).

Thin metal can be formed by means of a simple form block, but if the web is narrow, the workpiece should be protected by a cover plate to avoid distortion. The following example demonstrates the limits of rubber-pad forming of stretch flanges in stainless steel:

- The stock was quarter-hard type 302
- The workpiece had a narrow web and therefore required the use of a cover plate in forming
- The workpiece had external hole flanges
- The stretch flange was only 7.9 mm ($\frac{5}{16}$ in.) wide
- The curved workpiece was nearly 965 mm (38 in.) long

If a stainless steel part of this shape is more than 610 mm (24 in.) long, it is almost impossible to prevent the springback of the flange material from bowing the part unless curved dies are used.

Example 21: Use of a Curved Die With Cover Plates in Rubber-Pad Forming.

The strut shown in Fig. 25 had a 7.9 mm ($\frac{5}{16}$ in.) wide stretch flange and external 65° flanges on two lightening holes at the large end. It was rubber-pad formed from a quarter-hard type 302 stainless steel blank 0.41 mm (0.016 in.) thick.

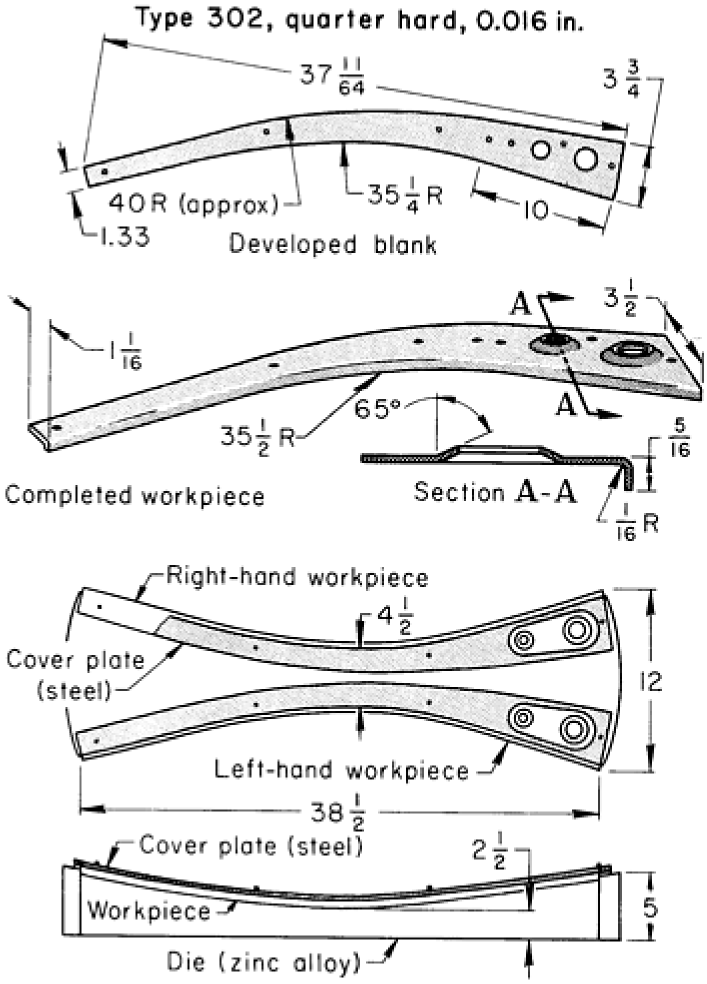


Fig. 25 Long narrow strut with a contoured stretch flange that was made by rubber-pad forming in a curved die with cover plates to prevent springback. Dimensions given in inches.

The zinc alloy die used was made with a curve to offset the springback of the flange (Fig. 25). Right-hand and left-hand pieces were flanged at the same time in the same die. A steel cover plate protected the thin web of each piece from distortion during forming. No lubricant was used. The pressure developed by the rubber was 10.3 MPa (1.5 ksi).

Deep Drawing. By the rubber-pad and rubber-diaphragm processes, stainless steels in both the 300 and the 400 series can be deep drawn to greater reductions than can be achieved with conventional methods. For extremely deep sections, the lower-work-hardening austenitic types 302 and 305 are recommended.

Two characteristics of rubber-pad methods make this great depth of draw possible. The first is controlled, continuously adjustable pressure on the blankholder or hold-down mechanism, and the second is the continuously variable draw ring radius. There is no draw ring as such, but the rubber that forms around the workpiece functions as a draw ring and conforms to the radius that will apply equal pressure to the entire surface of the workpiece. This minimizes both thinning at the punch radius and work hardening as the flange metal is drawn into the cup.

Figure 26 illustrates the relatively uniform wall thickness that can be produced by drawing using the rubber-pad method. For comparison, Fig. 17 shows the much greater variation in wall thickness produced by conventional deep drawing.

Additional information on deep drawing by rubber-pad techniques is available in the article "Rubber-Pad Forming" in this Volume.

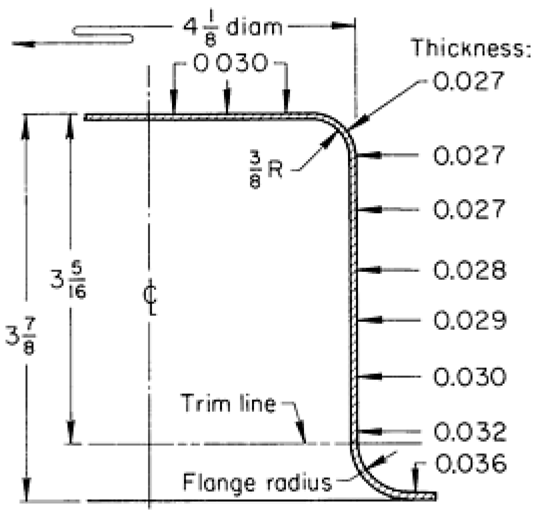


Fig. 26 Profile of a shell that was deep drawn from 0.76 mm (0.030 in.) thick stainless steel by the rubber-pad method showing the relatively uniform wall thickness obtained. Dimensions in figure given in inches.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Drop Hammer Forming

A wide variety of sizes and shapes can be formed in thin stainless steel by drop hammer forming. The advantages of this method include high impact energy (which often means that a piece can be formed by one blow, as compared to four or five by other processes) and suitability to low-volume and experimental production.

Dies. Die material for drop hammer forming is less critical than for press forming. The dies are made of steel, plastic, zinc alloy, and lead. Zinc alloy is widely used.

Punches are often made of lead because it can be cast directly on the lower die and because its weight adds energy to the stroke of the drop hammer. Although the lead is reusable, the number of pieces that can be made from each cast punch is small--about 200. Plastic punches and dies impart a finish to formed parts that would otherwise be difficult to obtain. Steel dies are used for high production and for coining and sizing (see the article "Coining" in this Volume).

Die designs are generally similar to those for press forming, with the same punch and die radii to reduce stress on the work metal. Die design for the forming of beads and methods of relieving entrapment to ensure good metal flow in drop hammer forming are also similar to those used in press forming. A trapped-rubber technique somewhat similar to the Guerin process is described in the article "Rubber-Pad Forming" in this Volume.

Quality of Product. The dimensions of workpieces formed in a drop hammer are less consistent than those made by other processes, because the degree of impact is subject to operator skill and because the punch can shift under localized high loads. However, springback is less pronounced in drop hammer forming than in other forming methods because of the high impact and forming speed.

Lubrication. The lubricants that can be used in drop hammer forming are listed in Table 2. If working is severe enough to require annealing between stages, contaminants such as graphite or sulfur (from the lubricant) or zinc or lead (from the die) must be removed from the work surface. If these contaminants are left on the surface of the stainless steel when it is heated, they can cause serious surface deterioration.

Comparison With Press Forming. Press forming, although done rapidly, is inherently an operation in which ram speed and holding pressures can be closely controlled; however, in drop hammer forming, the only way to form a part is by sudden impact. In some applications, production difficulties are overcome by the high rate of energy release in a drop hammer. In others, especially those in which blankholder pressure is critical, press forming produces better parts more economically if the die is properly made, as in the following example.

Example 22: Change From Drop Hammer to Press Forming That Eliminated Wrinkling and Reduced Cost.

The tailpipe half shown at the top in Fig. 27 was originally produced in a drop hammer, using the tooling setup shown at the lower left in Fig. 27. The operation was unsatisfactory, however, because wrinkles occurred at the intersection of the 30° risers, and six operations totaling nearly 2 min per piece were required to complete each piece.

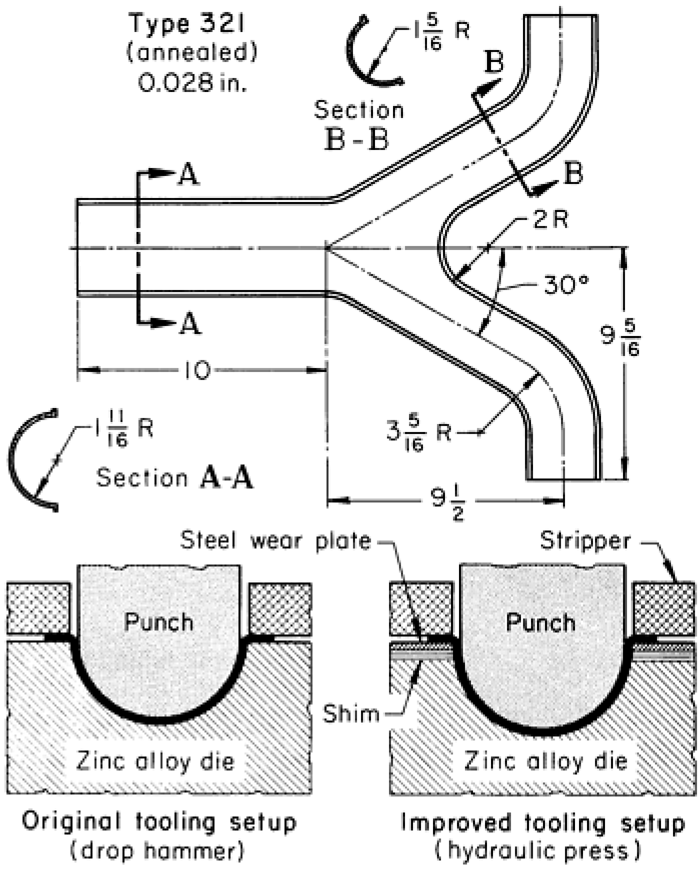


Fig. 27 Aircraft tailpipe half that was formed by the drop hammer and hydraulic press tooling setups shown. Dimensions given in inches.

The tools were redesigned for use in a 4400 kN (500 tonf) hydraulic press (lower right, Fig. 27). The zinc alloy die used in the drop hammer was reused in the press; to make it resistant to the abrasion of press forming with stainless steel, the die was faced off, and a low-carbon steel wear plate was installed. The 43 mm ($1\frac{11}{16}$ in.) radius had formed well in the drop hammer with very little springback, but springback in the press made it necessary to deepen the die. This was done by inserting shims between the die and the wear plate.

The press produced pieces that were completely free of wrinkles at the rate of two pieces per minute. This was a $1 \frac{1}{2}$ -min savings per piece.

The blank for both methods was annealed type 321 stainless steel measuring 510 × 610 mm (20 × 24 in.) and 0.71 mm (0.028 in.) thick. No lubricant was required for drop hammer forming; a wax emulsion was used for the press operation. Trimming after forming was done in a second press.

A drop hammer is ordinarily used for prototypes, and a press, using the prototype or improved dies, is used for mass production. If the quality of the prototype die is good, the drop hammer can be used for low production.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

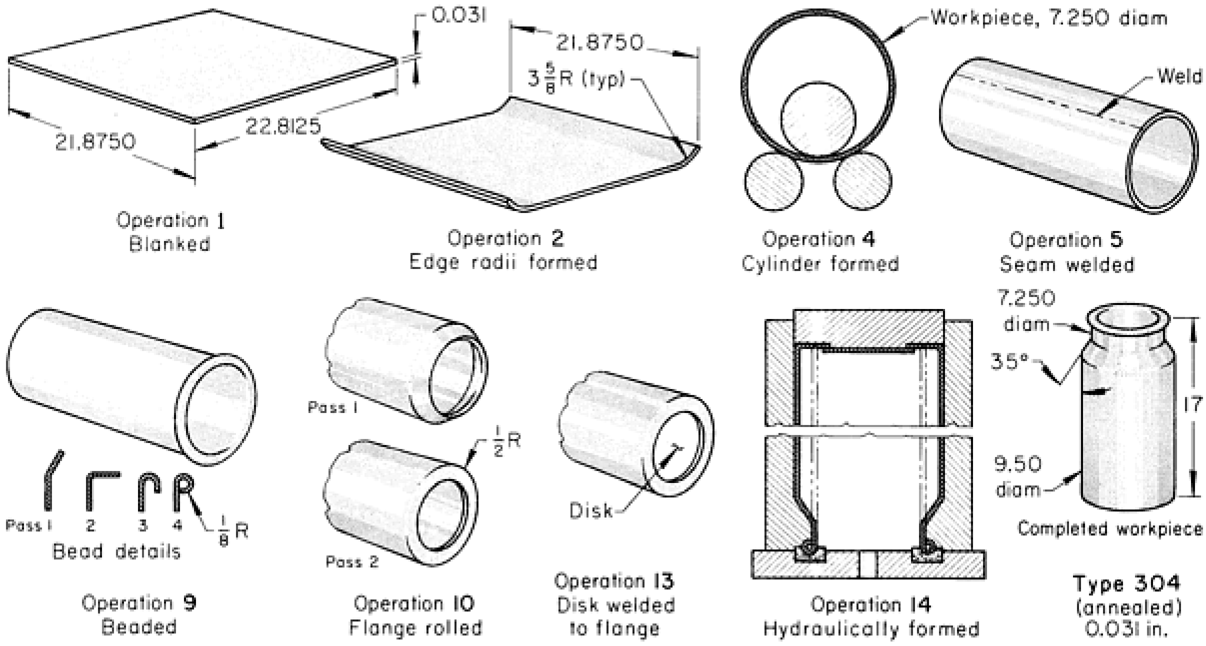
Three-Roll Forming

The three-roll forming of stainless steel is, in general, similar to the three-roll forming of other metals (see the article "Three-Roll Forming" in this Volume). Springback is a major problem with austenitic stainless steels, primarily because of the large radii involved and work hardening. It is important that the equipment be set up so that the desired curvature can be made in one pass. Because of the high rate of work hardening of austenitic stainless steels, subsequent passes are sometimes difficult to accomplish and control unless heavy equipment is used. The response of annealed ferritic stainless steel to three-roll forming is quite similar to that of hot-rolled low-carbon steel.

Three-roll and two-roll formers can be put in sequence with contour roll formers to make a cross-sectional shape and to bend or coil it, all in one production line. The following example describes an application in which three-roll forming was combined with press forming and hydraulic expansion forming.

Example 23: Use of Three-Roll Forming in the Production of a Container for Liquid.

Figure 28 shows eight of the 14 operations entailed in the production of a container for liquids by press forming and hydraulic expansion forming of a welded cylinder made from a radiused flat blank by three-roll forming in pyramid-type rolls. The six other operations are identified in the table that accompanies Fig. 28. These containers were produced in annual quantities of 10,000 to 100,000 pieces from annealed type 304 stainless steel coil stock 0.79 mm (0.031 in.) thick and 585.8 mm (23.0625 in.) wide.



Sequence of operations

Blank in die, in single-action press.
Form edge radii on blank, in a press brake.
Vapor degrease, to remove lubricant used in operations 1 and 2.
Roll cylinder, in three-roll former.
Weld cylinder seam, in automatic Heliarc setup using starting and stop-off tabs.
Trim tabs.
Hammer weld to induce compressive stress, using an air hammer at 310 kPa (45 psi).
Restore roundness of cylinder by rerolling several times in three-roll former.
Form bead on one end of cylinder, in four passes in an edger.
Roll flange on opposite end of cylinder, in two passes.
Trim flange.
Vapor degrease.
Weld (Heliarc) disk to inside of flange.
Expand and form to final shape (30% reduction in wall thickness), in a hydraulic expansion die (final pressure: 4800 kPa, or 700 psi).

Fig. 28 Use of three-roll forming in conjunction with press forming and hydraulic expansion forming, in the 14-operation production of a container for liquids. Dimensions given in inches.

Blanking the rectangular sheets for three-roll forming gave the workpiece the uniform square edges needed for maintaining the welded seam of the tube in axial alignment. The blanking tools were hardened high-carbon high-chromium tool steel; clearance was 0.08 mm (0.003 in.) per side. The stock was lubricated for blanking and edge radiusing, but the blanks were vapor degreased before three-roll forming.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Contour Roll Forming

Stainless steel is ordinarily contour roll formed in the annealed condition. Types 410 and 430 are usually roll formed on equipment similar to that used for carbon steel, with a No. 2 finish generally specified. Speeds are usually in the range of

7.6 to 30 m (25 to 100 ft) per minute, with the heavier gages and more difficult sections being roll formed at the slower speeds.

Stainless steels in hard tempers, such as quarter-hard and half-hard type 301, are also frequently roll formed. Increased power over that used for forming the same steels in the annealed condition is necessary because of the higher initial strength of the strip. Springback must be compensated for by adequate overbending. Longitudinal cracking can be a problem with the hard tempers if adequate radii are not included in the design of the part.

Distortion or warpage of straight sections causes the greatest problem in roll forming the 300-series steels, particularly when the steel is thick. The distortion can be minimized by using more sets of rolls, or more passes, for greater control during each stage of bending. However, the skill of the operator is all-important in controlling distortion. Various straightening devices are usually attached or used on the last pass as the section emerges from the machine. In some applications, sections are deliberately curved.

With the chromium-nickel stainless steels, pickup on the rolls and galling of the strip sometimes occur. Highly polished rolls or bronze rolls are used with lubrication to minimize this problem when high pressure is needed. Heavy-duty emulsions containing chlorine offer the best combination of chemical EP and coolant activity (Table 2). Chlorinated oils or waxes are easy to use, but are less effective as coolants. For severe forming, the cushioning effect of pigments is sometimes needed (as in the next example), as well as efficient cooling.

Example 24: Nine-Station Contour Roll Forming of Annealed 304 Stainless Steel.

Figure 29 shows the sectional shapes progressively produced in the nine-station roll forming of a sheave track from annealed type 304 stainless steel strip 67.3 mm (2.648 in.) wide by 0.79 mm (0.031 in.) thick, with a No. 2 finish. In a tenth station, the formed track was straightened. As the track left the tenth station, it was clamped to a moving table that conveyed it to an abrasive wheel for cutoff into lengths of 3 to 6 m (10 to 20 ft). The material weighed 0.414 kg/m (0.278 lb/ft); annual production was 180 Mg (400,000 lb).

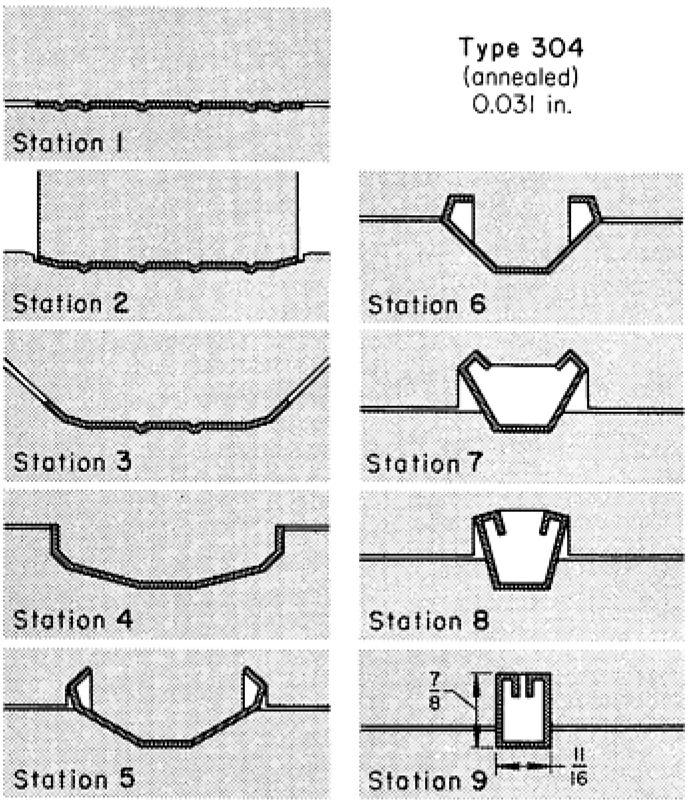


Fig. 29 Contour roll forming of a sheave track in nine stations. Dimensions given in inches.

During forming, the developed width of the section measured along the neutral axis increased only 1.02 mm (0.040 in.) to 68.2 mm (2.688 in.)--corresponding to only 1.5% stretch. The stretch was limited because the metal was restrained by the six pinch beads that were rolled into the strip before it was bent (stations 1, 2, and 3, Fig. 29). Each bead, 1.6 mm ($\frac{1}{16}$ in.) wide by 0.8 mm (0.030 in.) deep, permitted a sharp bend at that point without tearing or breaking the steel. The 50 to 55% elongation property of austenitic 304 stainless steel made it unlikely that the metal would break in bending. The strip was rolled with the slitting burr down so that the burr was flattened by the shoulders of the bottom roll of station 2.

The forming rolls were made of hardened steel, and the straightening rolls of hard bronze, for a good finish. Rolling speed was 17 m (55 ft) per minute. The lubricant was a pigmented water-soluble oil.

Plastic protective coatings are sometimes applied to the strip to minimize or prevent scratches and scuffing when high pressures are used and surface finish requirements are critical. On light-gage material (especially type 430), such protection is generally unnecessary if the fabricator is experienced in processing stainless steel. A detailed discussion of the equipment and techniques employed in contour roll forming is available in the article "Contour Roll Forming" in this Volume.

Revised by Joseph A. Douthett, Armco Inc.

Stretch Forming

The tools and techniques for stretch forming described in the article "Stretch Forming" in this Volume are applicable to stainless steel. Machines used for the stretch forming of stainless steel require 60 to 100% more power than that needed for similar operations on low-carbon steel of the same thickness.

Because of the abrasiveness of stainless steels, forming tools must be especially abrasion resistant. Wiping dies, wiping shoes, mandrels, and wear plates must be made of wear-resistant tool steel, carbide, or a bearing grade of bronze in order to avoid galling and welding.

Although the 300-series stainless steels are especially suitable for stretch forming because of their high work-hardening rate and ability to take large elongations, the 400-series steels are usable only for shallow stretched shapes. Type 301 is the austenitic steel that is best suited to stretch forming. Because of its high rate of work hardening, forming should be done slowly to derive maximum benefit from the ductility of type 301.

Maximum percentages of stretch for one-directional forming of various kinds of austenitic stainless steels are as follows:

- Annealed types 301, 302, 304, 305, 316, 321, and 347: 20% typical; possibly 30% on symmetrical and solid sections
- Quarter-hard types 301 and 302: 15% typical; possibly 20% on optimum sections
- Half-hard types 301 and 302: 5% typical; possibly 10% on optimum sections
- Full-hard type 301: possibly 2% on optimum sections

These figures should not be confused with permissible stretch in bending, nor are they the limits to which these stainless steels will stretch (which are considerably greater). Instead, these percentages, which determine the possible curvature of stretch-formed sectional shapes of stainless steel, are based on the distortion susceptibility of severely stretched stainless steel.

The upper limits can be extended by very slow stretching and forming, especially with hardened metal. In addition, to obtain maximum stretch from the harder tempers, workpieces should be carefully deburred. Automatic programming is valuable in applying continuously increasing tension to overcome the continuously increasing strength as work hardening takes place during stretch forming.

Lubricants. If there is little or no movement after contact between workpiece and form block, as in stretch wrapping or single-die draw forming, little or no lubricant need be used except when deformation is severe. A low-viscosity chlorinated oil or wax provides excellent chemical EP action and convenience of use. If there is considerable movement of the work metal against the dies (such as against the wiper shoe in radial-draw forming), pigmented lubricants are sometimes used. The following example describes an application in which no lubricant was used in the stretch forming of a sharply contoured part.

Example 25: Dry Stretch Forming of an Airfoil Leading Edge.

The leading edge of an airfoil was stretch formed dry from a type 302 stainless steel blank, 0.20 mm (0.008 in.) thick, 115 mm (4 $\frac{1}{2}$ in.) wide, and 5.5 to 6.7 m (18 to 22 ft) long, that had been roll formed to the airfoil contour shown in section A-A in Fig. 30. The blank had been annealed before roll forming, and it was stretch formed, without further annealing, to a 7.6 m (25 ft) radius with the heel of the contour pointing out (Fig. 30).

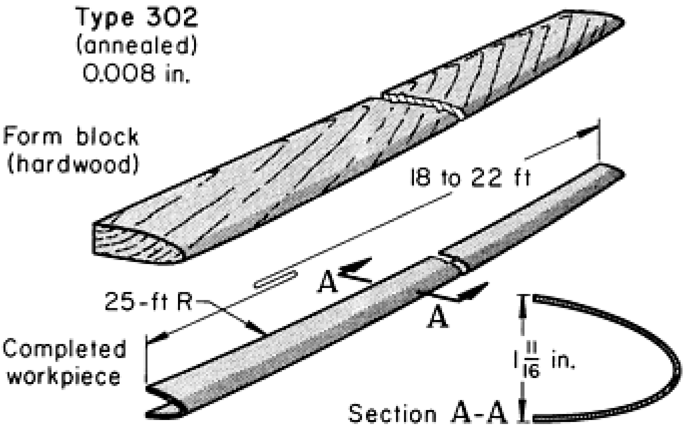


Fig. 30 Airfoil on which the leading edge was stretch formed to a long convex shape without lubricant in a radial-draw former.

The airfoil was stretch formed in a radial-draw former over a hard-maple form block with the airfoil contour carved into its surface (Fig. 30). Lubricant was not used, because it had previously caused local variations in friction. Time for forming was 10 min per piece with three men working. Setup time was 2 h. A typical production lot was 100 pieces.

The rolled contour had to be held within ±0.1 (±0.005 in.) after stretch forming. The envelope tolerance on the stretch-formed shape was 0.76 mm (0.030 in.).

Springback. In sharply contoured pieces that have a relatively deep, wide cross section, some springback cannot be avoided, even in annealed metal. During severe stretch forming, considerably higher strength, and therefore appreciably higher elastic recovery, is developed in the more highly stressed convex surface.

Springback in regular, symmetrical sections can usually be offset by overbending the piece. Dimensional variations in workpieces are primarily caused by variations in springback, which are in turn caused by variations in mechanical properties from sheet to sheet.

If the workpiece is irregular in cross section, if preformed flanges are to be held to a certain angular position, or if the curve of the form varies in severity, springback may cause twist or irregular distortion of the workpiece. Various methods of blocking, pretwisting, or overforming are used to prevent or correct this distortion. In the following example, an asymmetrical cross section was twisted during forming to offset the twist caused by springback.

Example 26: Use of Twisting to Compensate for Springback in Stretch Forming.

The curved channel section shown in Fig. 31 was stretch formed from quarter-hard type 302 stainless steel strip, 1.07 mm (0.042 in.) thick, that had been preformed in a press brake. Although the channel fit closely in the groove of the form block, springback caused considerable twist in the finished piece.

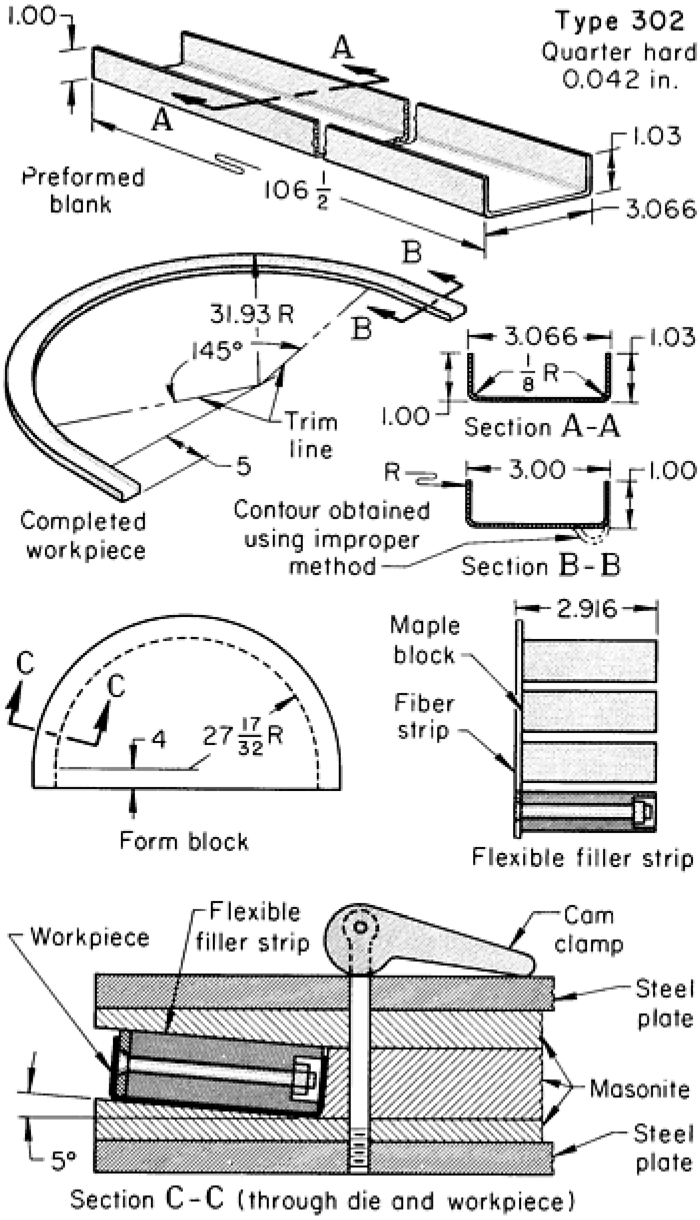


Fig. 31 Channel section that was stretch formed from a preform produced in a press brake, and details of tooling used in stretch forming, which provided reverse twist to compensate for springback. Dimensions given in inches.

Elastic recovery of the outer flange and the metal near the outer edge of the web caused buckling and twisting in the part as forming tension was released. To overcome this, the part was canted by the form block, and tension on the part was gradually increased during forming.

To establish a compensating initial reverse twist in the workpiece, spacers were added to the built-up form block to wedge the section to a 5° angle, as shown in Fig. 31. At the same time, a fiber filler strip with maple filler blocks was closely fitted into the channel to hold the cross-sectional contour. Details of the tooling are shown in Fig. 31.

The applied tension during stretch forming was 83.2 kN (18,700 lbf) at the start, 87.0 kN (19,550 lbf) at 45° bend, 90.7 kN (20,400 lbf) at 90°, 94.5 kN (21,250 lbf) at 135°, and 98.3 kN (22,100 lbf) upon completion of the bend. A nonpigmented fatty acid was used as the forming lubricant. After forming, the workpiece was trimmed to a 145° arc with a band saw.

Equalizing Stretch. In the stretch forming of sheets to a curvature in two directions (especially in stretching tempered material when the limits of stretch are very close), the quality of the product can be controlled much better if the stretch is uniform across the workpiece. One means of obtaining uniform stretch is to provide compensating contours (which are later trimmed off) at the end of the form block.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Bending of Tubing

Austenitic stainless steel tubing can be bent to a centerline radius of 1 $\frac{1}{2}$ times tube diameter. As the ratio of tube diameter to wall thickness, *D*/*t*, increases, it becomes increasingly necessary to provide both internal and external support to keep the tube from collapsing as it is bent. When *D*/*t* is greater than 30, the tube is classed as a thin-wall tube. Interlocked tooling, as well as bending machines of a greater capacity than that required for thick-wall tubes, is strongly recommended for thin-wall tubing (see the article "Bending and Forming of Tubing" in this Volume).

For the bending of stainless steel tubing, wiper dies and mandrels are often made of aluminum bronze or a chromium-plated tool steel. Lubricants for the mandrel should be fairly heavy. Viscous or pigmented oil-base lubricants containing emulsifiers for ease of removal are used. Only the very lightest of lubricants should be used between the wiper die and the tube. A thin application of very light chlorinated mineral oil can be used in some bending operations without causing wrinkling. The following example describes techniques used in the bending of stainless steel tubing.

Example 27: Bending Difficult-to-Form Tubing Into an Aerospace Component.

The bent tube shown in Fig. 32, used in an aerospace assembly, was difficult to form within the specified tolerances (dimensions within 0.25 mm, or ±0.010 in.; angles within ± $\frac{1}{2}$ °; and flattening of the tube at bends not more than 0.05 mm, or 0.002 in.). The piece was produced from type 304 stainless steel tubing in nine operations in the following sequence (times shown are for the production of 100-piece lots):

- Cut tubing into lengths of 160 mm (6 $\frac{1}{4}$ in.) with an abrasive cutoff wheel; deburr roughly (3 h)
- Fill each workpiece with low-melting alloy (8 h)
- Make 160° bend in powered draw bender; gage the bend (5 h)
- Make 24° bend in hand bender; gage the bend (5 h)
- Trim ends to length in a cutoff fixture using an abrasive wheel (3 h)
- Melt out the filler (6 h)
- Deburr by hand, using a grinder and a drill (3 h)
- Passivate in a chemical dip (1 h)
- Inspect 100% with gage and by rolling an accurate ball through the completed part (2 h)

Springback in bending, about 5°, was corrected by overbending to a degree established in trial bends.

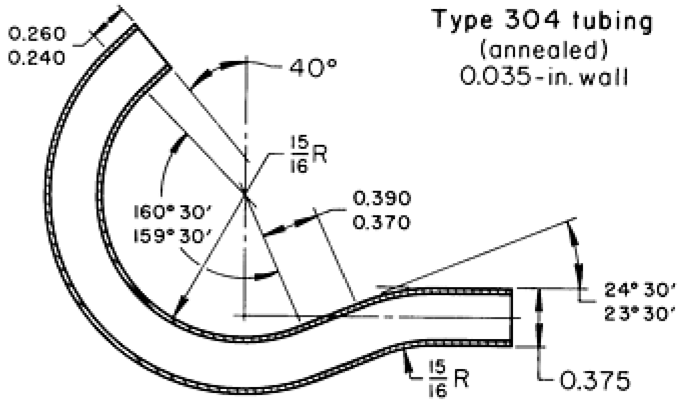


Fig. 32 Aerospace component that was bent from stainless steel tubing with the use of a low-melting alloy as a filler during bending. Dimensions given in inches.

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Other Forming Operations on Tubing

Stainless steel tubing can be easily flared to increase the diameter 25 to 30% if it is annealed. The diameter can be reduced by rotary swaging, or it can be increased by bulging or beading. Rubber punches are often used for this purpose, as described in the article "Rubber-Pad Forming" in this Volume.

Tubing of austenitic stainless steel can be hot formed by heating to 1175 to 1260 °C (2150 to 2300 °F). Work should be halted when the tube has cooled to 925 °C (1700 °F), and the tube should then be cooled rapidly to minimize the precipitation of carbides.

Because austenitic stainless steel tubing is stronger than carbon steel tubing and work hardens rapidly, warm forming (below the recrystallization temperature) is also used on this material. The temperature for warm forming should be kept below 425 °C (800 °F) to prevent the formation of carbides.

Tubing of ferritic stainless steels, such as types 430 and 446, is less easily formed than similar tubing of austenitic stainless steels. Ferritic tubing is hot formed at 1035 to 1095 °C (1900 to 2000 °F), and forming is stopped when the tubing cools to 815 °C (1500 °F). For best results, the range from 815 to 980 °C (1500 to 1800 °F) should be avoided, because ductility and notch toughness are progressively impaired as the tube cools through that range. Hot shortness may be encountered in the upper part of the range. Tubing of ferritic stainless steels is warm formed at 120 to 205 °C (250 to 400 °F).

Steel producers have studied the cold formability of 11% Cr (409) and 17% Cr (439 or 18% Cr-Nb) tubing materials, primarily because of requests from the automotive industry to use Ti or Ti + Nb stabilized ferritic alloys in exhaust systems. Such alloys are normally used in high-frequency welded or gas tungsten arc (GTA) welded (autogenous) and annealed tubing. Traditionally, the GTA welded and annealed tubes had more formability because of the elimination of the 8 to 15% cold work induced in forming the tube.

As these ferritic alloys were subjected to the demands of high-speed vector bending, particularly in making tubular exhaust manifolds, breakage rates increased to over 50%. In response, stainless steel producers borrowed technology from low-carbon steel production practices and developed a line of high-performance ferritic alloys with improved elongations and higher *r* values (>1.5). Additional information on the determination of *r* values is available in the article "Formability Testing of Sheet Metals" in this Volume.

Such alloys have permitted the greater use of high-frequency welded and unannealed tubes for thin-wall bends with a centerline bend radius less than twice the tube diameter. Furthermore, such bends can be made at room temperature, although care should be exercised in cold weather not to fabricate sub-room-temperature tubing. Finally, through tighter control of both melt chemistries and processing parameters, ferritic tube alloys with excellent welding and bending reproducibility from heat to heat have been developed.

Forming of Stainless Steel

Revised by Joseph A. Douthett, Armco Inc.

Forming Versus Machining

Although forming ordinarily requires expensive tooling and bulky equipment, it is a high-speed process, and for most parts that can be formed from sheet, it is more economical than machining for mass production. The following example shows how production techniques can vary with the size of the production lot to make the best use of each technique.

Example 28: Influence of Change in Quantity of Production Method and Product Design.

A threaded cap was made of type 347 stainless steel by three different methods. Each method involved a change in design, as illustrated in Fig. 33.

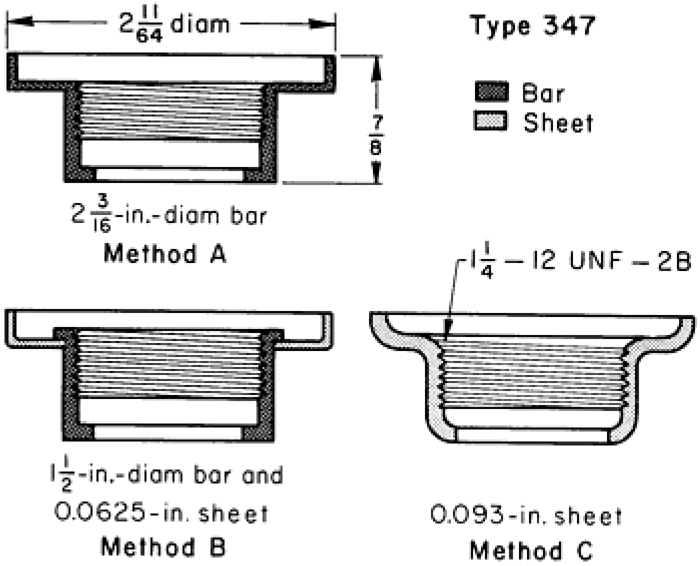

Fig. 33 Machining versus press forming for producing a cap. In method A, the cap (illustrated above) was completely machined from bar stock. In method B, the cap (redesigned) combined components that were press formed from sheet and machined from bar stock. In method C, the entire cap (again redesigned) was press formed from sheet and then partly machined. Dimensions given in inches.

The original order was for 100 caps, with an anticipated design change on future orders. The quickest and most economical production method was to machine the cap in one piece from bar stock (method A, Fig. 33).

The next order was for 1000 caps. The design and manufacturing methods were revised so that the cap was produced as an assembly of two components--one press formed from sheet and the other machined from bar stock (method B, Fig. 33).

When requirements increased to 5000 caps, a cost reduction was essential to obtain the order against a competitor's bid. The part was redesigned for production entirely from sheet by press forming and partial machining (method C, Fig. 33). Overall cost was reduced nearly 50% as compared to methods A and B.

The press-formed part of method B was made in a 400 kN (45 tonf) open-back-inclinable mechanical press at a rate of 200 to 250 pieces per hour. The die was made of oil-hardening tool steel. Method C used an air-hardening tool steel die and a 530 kN (60 tonf) open-back-inclinable mechanical press that made 300 to 350 pieces per hour. Mineral oil was used as a lubricant in both methods.

Forming of Heat-Resistant Alloys

Revised by S.K. Srivastava and E.W. Kelley, Haynes International

Introduction

WROUGHT HEAT-RESISTANT ALLOYS can be classified as iron-base, nickel-base, or cobalt-base alloys. Depending on the specific alloy, one or more of the following strengthening mechanisms can be used: solid-solution strengthening, precipitation hardening, and dispersion strengthening. The use of these various strengthening mechanisms leads to a wide range of microstructural and compositional variations. Despite this, heat-resistant alloys can be formed by techniques similar to those employed for the forming of AISI 300-series stainless steels, although the forming of heat-resistant alloys is more difficult. All heat-resistant alloys work harden rapidly. Figure 1 compares the degree of work hardening of several nickel-base alloys to that experienced in a cobalt-base alloy, an iron-base alloy, AISI type 304 stainless steel, and a low-carbon ferritic steel.

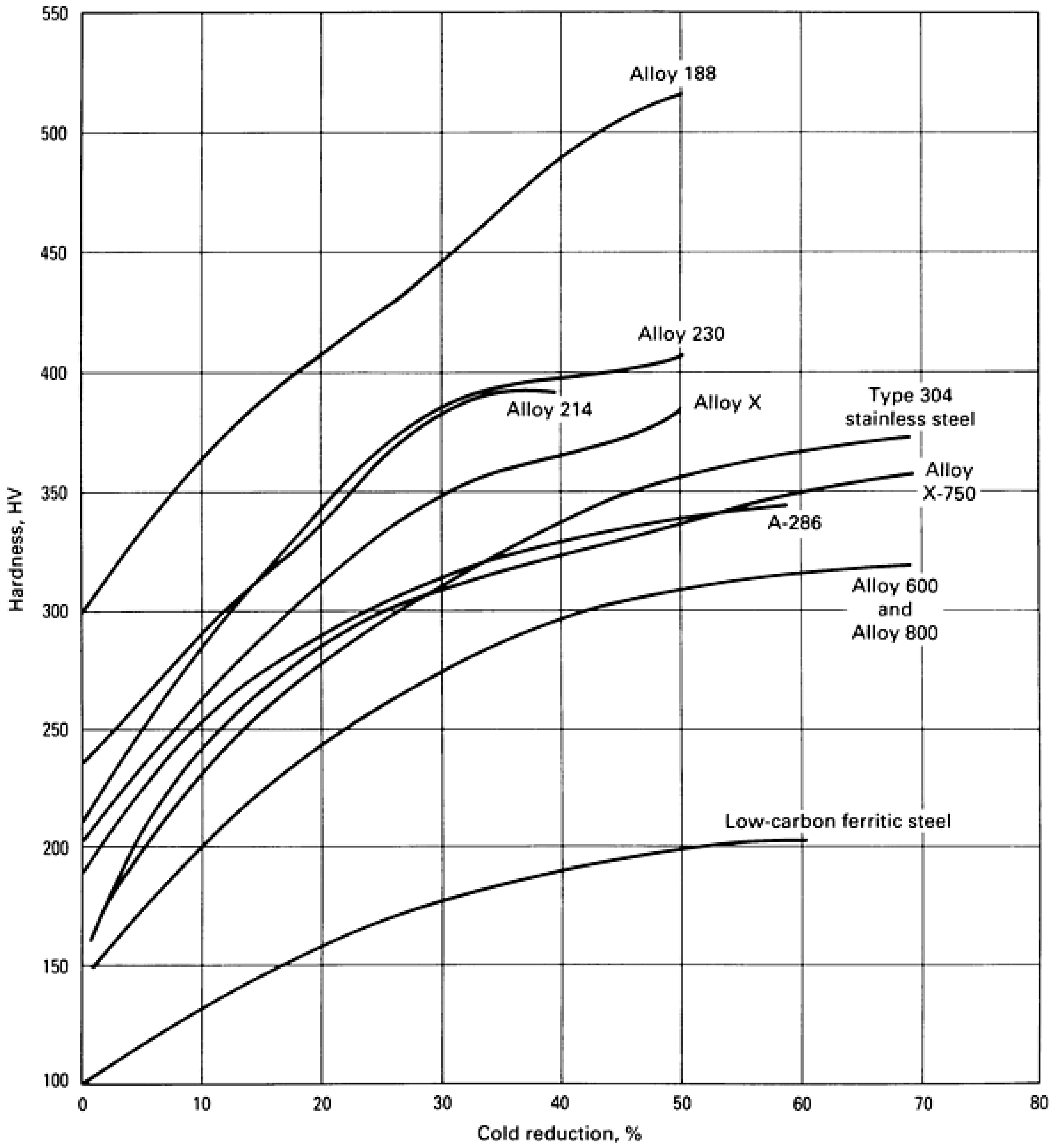

Fig. 1 Effect of cold reduction on the hardness of several heat-resistant alloys, type 304 stainless steel, and a low-carbon ferritic steel.

The differences in composition of the various heat-resistant alloys cause differences in their formability. Alloys that contain the greatest amount of cobalt, such as alloy 25 (Co-0.1C-20Cr-15W-10Ni) and alloy 188 (UNS R30188), require a greater magnitude of force to form than iron- or nickel-base alloys. Most alloys that contain substantial amounts of molybdenum or tungsten for strengthening, such as alloy 230 or alloy 41 (UNS N07041), are harder to form than alloys containing lesser amounts of these elements. Alloys that contain aluminum and titanium are strengthened by precipitation of the γ' phase. The volume fraction γ' depends strongly on the amounts of aluminum and titanium present and on overall composition. Examples of alloys that contain γ' include alloy 80A (UNS N07080), WASPALOY alloy (UNS N07011), and alloy 214. These typically contain 15, 20, and 33% γ', respectively. Many precipitation-hardened alloys require complex production steps to produce satisfactory components. Most of the iron- and nickel-base alloys contain less than 0.15% C; more carbon than this causes excessive carbide precipitation, which can severely reduce ductility. Small amounts of boron are used in some of the heat-treatable nickel-base alloys, such as alloy 41 and U-700 (Ni-18.5Co-15Cr-5.2Mo-3.5Ti-0.02B), to prevent carbide precipitation at grain boundaries; too much boron, however, can cause cracking during forming.

Sulfur causes hot shortness of nickel-base alloys. Silicon content should be below 0.60%, and preferably less than 0.30%. More than 0.60% Si causes cracking of cold-drawn alloys and may cause weld cracking in others. Silicon at levels of less than 0.30% usually does not contribute to difficulties in forming.

Cold forming is preferred for heat-resistant alloys, especially in thin sheets. Most of these alloys can be hot formed effectively only in a narrow temperature range (between about 925 and 1260 °C, or 1700 and 2300 °F). Intermediate annealing between cold-forming operations is usually preferred to hot forming.

Forming of Heat-Resistant Alloys

Revised by S.K. Srivastava and E.W. Kelley, Haynes International

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Effect of Alloy Condition on Formability

For the fine grain structure that is best for cold forming, heat-resistant alloys must be cold worked (reduced) beyond a critical percentage reduction and then annealed. The critical amount of cold work varies with the alloy and with the annealing temperature, but is usually 8 to 10%. Reheating metal that is only slightly cold worked can result in abnormal grain growth, which can cause orange peel or alligator hide effects in subsequent forming.

For example, an alloy X (UNS N06002) workpiece, partly formed, stress relieved, and then given the final form, had severe orange peel on much of its surface. The partial forming resulted in about 5% cold working, and during stress relief, an abnormally coarse grain structure developed. The difficulty was corrected by making certain that the metal was stretched 10% or more before it was stress relieved. In addition, stress relieving was done at the lowest temperature and shortest time that could be used, because higher temperatures and longer times increased grain growth. Optimal time and temperature were determined by hardness testing.

Severely cold-formed parts should be fully annealed after final forming. If annealing causes distortion, the work can be formed within 10% of the intended shape, annealed, pickled, and then given the final forming.

Solution annealed products are usually soft enough to permit mild forming. If the solution annealed alloy is not soft enough for the forming operation, an annealing treatment must be used that will remove the effects of cold work and dissolve the age-hardening and other secondary phases. Some control of grain size is sacrificed, but if cooling from the annealing temperature is very rapid, the age-hardening elements will be retained in solution. Further annealing after forming can be done at a lower temperature to decrease the risk of abnormal grain growth. Several process anneals may be required in severe forming, but the high-temperature anneal need not be repeated. Annealing should be performed at a temperature that produces optimal ductility for the specific metal, as shown in the following example.

Example 1: Change in Heat Treatment to Eliminate Cracking.

A large manifold was made by welding together two drawn halves into a doughnut shape. Each half was drawn to a depth of 127 mm (5 in.) from 3.5 mm (0.25 in.) thick alloy 41 that had been solution treated at 1175 °C (2150 °F) and water quenched. Drawing of the plate stock on a 31,000 N (7000 lbf) drop hammer produced severe work hardening, and cracking occurred frequently. To eliminate the cracking, forming was done in three steps, and the parts were annealed at 1080 °C (1975 °F) before the second and the third step.

The forming characteristics of the alloy 41 plate were greatly improved by modifying the solution treatment. The revised treatment consisted of first soaking the alloy at 540 °C (1000 °F), transferring it to a gantry furnace, and holding it at 1080 °C (1975 °F) for 30 min. The work was then lowered rapidly through the bottom of the furnace into a salt bath at 205 to 260 °C (400 to 500 °F). Thus, the elapsed time between leaving the high-temperature zone and entering the quench was kept to 4 or 5 s, the alloy was in the precipitation range (595 to 1010 °C, or 1100 to 1850 °F) for a minimum time, and minimum hardness (16 to 21 HRC) was obtained. The salt bath provided a more uniform quench and a more ductile alloy than the original water quench. The better ductility of the alloy allowed forming of the manifold halves in two operations.

Forming of Heat-Resistant Alloys

Revised by S.K. Srivastava and E.W. Kelley, Haynes International

Formability Indicators

Formability refers to the ease with which sheet metal can be formed. Material formability is difficult to measure. There is no single index for predicting specific material formability for all processing conditions. The deformation modes in the forming of most sheet metal components are complex and consist of bending, unbending, stretching, and deep drawing. Materials, the process, and the final shape all interact in the forming and therefore should be considered simultaneously; this makes material formability an elusive factor to quantify. Forming technology depends a great deal on practical experience. Material characteristics such as tensile ductility, strain-hardening exponent, and anisotropy parameters can act as guides to the nature of formability and can be used for comparing materials.

In any forming operation, the useful ductility of material is that amount up to the point of necking. Greater ductility at peak load and a large separation between yield and tensile strengths are desirable. A measure of stretchability is provided by the strain-hardening exponent (*n* value). Plastic deformation in a tensile test can be related to true stress in the following manner:

$$\text{True stress} = K\,(\text{true strain})^{n} \quad \textbf{(Eq 1)}$$

where *K* is a strength constant. Most heat-resistant alloys possess *n* values in excess of 0.4; a high strain-hardening capacity results in spreading the strain away from any local region in the presence of a stress gradient.

The rolling and rerolling of a metal during its manufacture may cause alignment of individual grains. This imparts anisotropic plastic properties to the sheet. Recrystallization during annealing will tend to restore isotropy. The plastic strain ratio *R* (the ratio of the width strain to thickness strain in a uniaxial tensile test) is a measure of normal anisotropy, that is, the variation of properties in the plane of the sheet relative to those perpendicular to the sheet surface. The average *R* value is given by:

$$R = \frac{R_L + 2R_D + R_T}{4} \quad \textbf{(Eq 2)}$$

where the subscripts refer to tensile test measurements made in the longitudinal, diagonal, and transverse orientations of rolled sheet, respectively. The variation of the properties in the plane of the sheet is termed planar anisotropy and is given by:

$$\Delta R = \frac{R_L + R_T - 2R_D}{2} \quad \textbf{(Eq 3)}$$

For an isotropic material $R = 1$ and $\Delta R = 0$. A material with a high *R* value resists localized necking in the thickness direction; therefore, deep drawability is high. There are various correlations between deep drawability and *R* value. Planar anisotropy causes uneven flow of metal, resulting in earing of drawn cups. Some typical forming characteristics of several heat-resistant alloys are given in Table 1.

Table 1 Forming characteristics of some heat-resistant alloys

Alloy	**UNS No.**	**Thickness**		**Anisotropy**		**Olsen cup depth**		Ericksen cup depth	
		mm	**in.**	***R***	**Δ*R***	**mm**	**in.**	**mm**	in.

Alloy 80A	N07080	0.9	0.035	0.91	-0.02	. . .	. . .	12.5	**0.492**
Alloy 263	N07263	0.9	0.035	0.86	0.01	. . .	. . .	12.8	**0.504**
Alloy P16	. . .	0.9	0.035	0.98	-0.40	. . .	. . .	10.5	**0.413**
Alloy 188	R30188	1.2	0.047	0.94	0.13	. . .	. . .	12.6	**0.496**
Alloy 188	R30188	0.63	0.025	0.95	-0.024	12.5	0.492	. . .	**. . .**
Alloy 230	. . .	0.76	0.030	0.93	-0.059	11.0	0.433	. . .	**. . .**
Alloy 625	N06625	0.61	0.024	0.97	-0.139	11.7	0.461	. . .	**. . .**
Alloy X	**N06002**	**0.61**	**0.024**	**0.95**	**-0.105**	**10.2**	**0.402**	**. . .**	**. . .**

Source: Ref 1, 2

Forming limit curves are increasingly used to predict the formability of materials. The forming limit curve is experimentally constructed for combinations of strain paths to describe strain to necking (or fracture). More information on tests for formability is available in the article "Formability Testing of Sheet Metals" in this Volume.

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Forming of Heat-Resistant Alloys

Revised by S.K. Srivastava and E.W. Kelley, Haynes International

Effect of Rolling Direction on Formability

Depending on the size, amount, and dispersion of secondary phases, the age-hardenable alloys show greater directional effects (Fig. 2) than alloys that are not age hardenable. However, vacuum melting and solution annealing serve to reduce directional effects (anisotropy). As shown by data for press-brake bending in Fig. 2, directional effects contribute erratically to cracking and surface defects. The following example shows how directionality seriously affected the forming characteristics of iron-base alloy A-286 (UNS S66286).

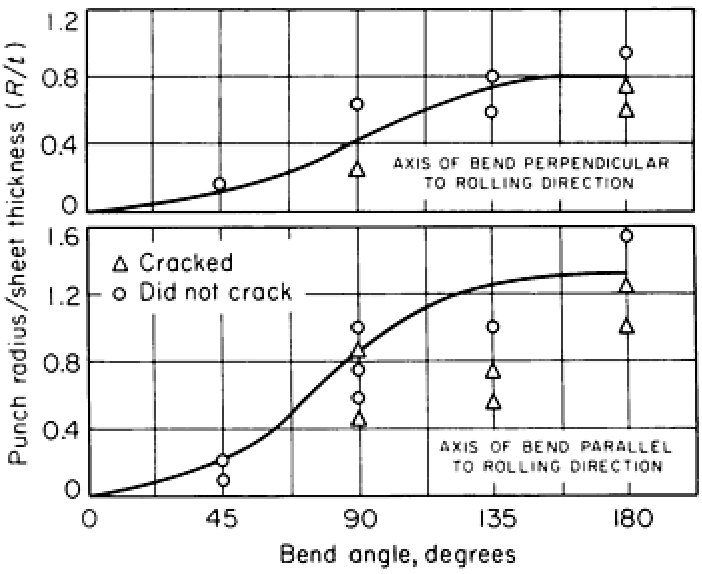

Fig. 2 Effect of forming direction relative to rolling direction on the formability of alloy 41 sheet 0.5 to 4.75 mm (0.02 to 0.187 in.) thick in press brake bending.

Example 2: Effect of Directionality in Bulging A-286.

A contoured exhaust cone (Fig. 3a) was made by cutting a flat blank from mill-annealed A-286 sheet, rolling and welding a cone from the blank, and then bulging the cone into final shape. Developed blanks for two cones were cut from one sheared rectangle (Fig. 3b) with little waste of stock.

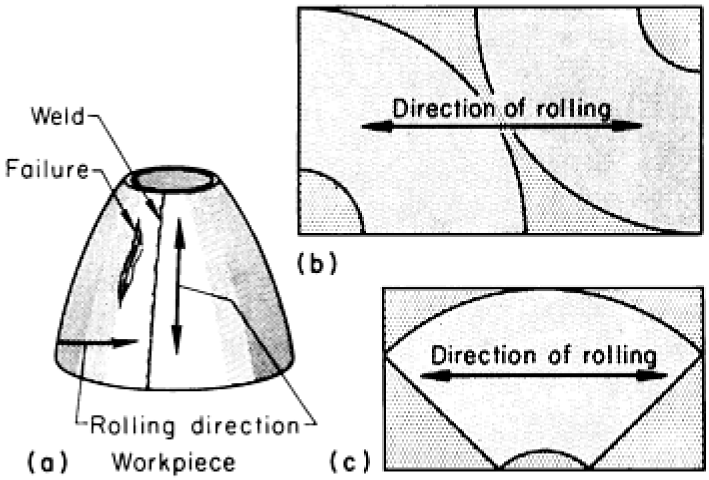

Fig. 3 A-286 exhaust cone that cracked in bulge forming, and the two layouts used in cutting the cone blanks from 1.0 to 1.3 mm (0.04 to 0.05 in.) thick sheet.

Several lots of A-286 produced good parts, but one lot of material cracked in bulging. As shown in Fig. 3(a), cracks occurred in the cone adjacent to the weld at the location where the forming stresses were perpendicular to the rolling direction (which was also the direction of minimum elongation). The good and inferior lots of A-286 were compared as to elongation with and across the rolling direction, and the inferior lot showed substantially greater difference in elongation between the two test directions.

	Good A-286	Inferior A-286
Elongation, %		
Perpendicular to rolling direction	41.0	**43.5**
Parallel to rolling direction	38.5	**37.2**
Difference	**2.5**	**6.3**

Annealing the welded cones before bulging reduced the number of cracked cones, but not by a satisfactory percentage. A higher percentage of acceptable cones resulted when the blanks were cut with their edges oriented to the rolling direction as shown in Fig. 3(c). Cones made from these blanks had less abrupt change in the forming direction relative to rolling direction on each side of the weld, and the forming stresses were never perpendicular to the rolling direction; however, there was more scrap material from cutting the blank. When a revision of production techniques at the mill reduced the elongation difference in the two directions of stress, it was possible to use the more economical blank layout shown in Fig. 3(b).

Forming of Heat-Resistant Alloys

Revised by S.K. Srivastava and E.W. Kelley, Haynes International

Effect of Speed on Formability

The speed at which a metal is deformed affects its formability. In general, each metal has a critical speed of forming. In some cases, the ductility increases until this critical speed is reached, after which it decreases sharply with increasing speed, as indicated by the curve in Fig. 4. This curve has a plateau of maximum strain where ductility is greatest. This plateau seems to be broad for most heat-resistant alloys. The breadth of the plateau depends on the use of biaxial or triaxial loading of the material during forming. Table 2 gives optimal speeds for three heat-resistant alloys and three forming operations.

Table 2 Recommended forming speeds for three heat-resistant alloys

Alloy	**UNS No.**	Forming speed for:					
		Tensile forming		**Bulge forming**		Draw forming	
		m/s	**ft/s**	**m/s**	**ft/s**	**m/s**	ft/s
A-286	S66286	15 to >84	100 to >425	0 to >213	0 to >700	0-236	**0-775**
Alloy 41	N07041	0 to >107	0 to >350	0 to >213	0 to >700	0-229	**0-750**

Alloy 25	**. . .**	**30 to >130**	**50 to >275**	**. . .**	**. . .**	**198-251**	**650-825**

Source: Ref 3

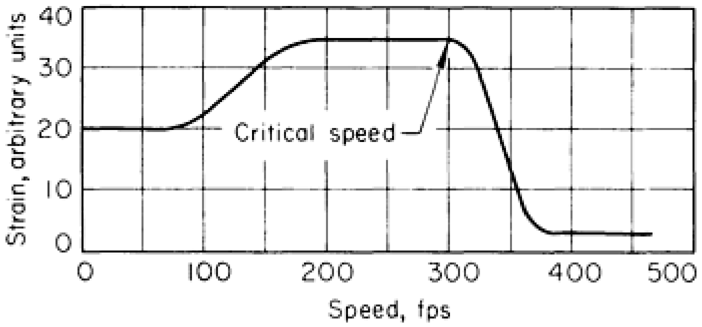

Fig. 4 Effect of forming speed on ductility. fps, feet per second. Source: Ref 3.

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Forming of Heat-Resistant Alloys

Revised by S.K. Srivastava and E.W. Kelley, Haynes International

Methods and Tools

Few applications in forming heat-resistant alloys involve quantities that warrant the use of high-production methods and tools. Usually, only a few to a few hundred parts are needed. Therefore, methods that require a minimum of tooling, such as press-brake forming, drop hammer forming, spinning, and explosive forming, have been used more than other methods. Presses or other machines are the same as those used for forming steel, but more power is needed to form heat-resistant alloys because of their higher strength. The power required to form a given workpiece is from 50 to 100% more for heat-resistant alloys than for low-carbon steel.

Safety in Explosive Forming. Operations involving explosives and pressure vessels are governed by state, county, and municipal regulations. The requirements and restrictions of these regulations should be taken into account in tool design and operational setup for explosive forming (see the article "Explosive Forming" in this Volume).

Tools used for forming heat-resistant alloys are usually the same as those used for forming stainless steel in similar quantities (see the article "Forming of Stainless Steel" in this Volume). Clearance between punch and die is generally the same as that for stainless steel. Heat-resistant alloys also resemble stainless steels in that they are likely to adhere to dies or mandrels, resulting in galling or tearing of the dies and workpieces. Steel dies, punches, or mandrels can be plated with approximately 5 to 13 μm (0.2 to 0.5 mils) of chromium in order to minimize adherence. However, small production quantities seldom justify this practice. Cast iron has proved adequate and nongalling for many low-production forming tools. If a heat-treatable grade of iron is used, areas in which high wear is anticipated can be locally hardened.

Lubrication

Some lubrication is usually required for optimal results in drawing, stretch forming, or spinning. Lubrication is seldom needed for the press-brake forming of V-bends, but will greatly improve results if a square punch is used. Mild forming operations--for example, those no more severe than a 10% reduction--can usually be accomplished successfully with unpigmented mineral oils and greases. Polar lubricants, such as lard oil, castor oil, and sperm oil, are preferred for mild forming. They will usually produce acceptable results and are easily removed. For more severe forming, metallic soaps or extreme-pressure (EP) lubricants, such as chlorinated, sulfochlorinated, or sulfurized oils or waxes, are recommended. They can be pigmented with a material such as mica for extremely severe forming.

Lubricants that contain white lead, zinc compounds, or molybdenum disulfide are not recommended, because they are too difficult to remove before annealing or before high-temperature service. At high temperatures, any sulfur or lead on the surface of the alloys can be harmful. Sulfurized or sulfochlorinated oils can be used if the work is carefully cleaned afterward in a degreaser or an alkaline cleaner. Work that has been formed in zinc alloy dies should be flash pickled in nitric acid before heat treatment to prevent the possibility of zinc embrittlement.

Lubricants used for spinning operations must cling tenaciously; otherwise, they will be thrown off the workpiece by centrifugal force. Metallic soap or wax applied to the workpiece before spinning is usually satisfactory. In power spinning, a coolant should also be used during the process (see the article "Spinning" in this Volume).

Occasionally, it is advantageous to use two kinds of lubricant in the same operation. In one stretch-forming application, the strain at the middle of the work was 3 to 4%, but near the ends, where the metal pulled tangentially to the die, the strain was 10 to 12%. A light coat of thin oil was adequate for most of the work, but an EP lubricant was used at the ends. More information on lubricants for forming is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Forming Practice for Iron-Base Alloys

Alloy A-286 has work-hardening characteristics similar to those of type 304 stainless steel (Fig. 1) and has slightly lower formability. Most other iron-base heat-resistant alloys are somewhat less formable. Typical forming practice is described in the following examples.

Example 3: Forming A-286 Tube by Spinning.

The tube shown at the top of Fig. 5 was backward spun from a roll forging that had been solution annealed at 980 °C (1800 °F). A starting groove had been machined into the tube in a previous operation. Spinning was performed in three passes on a machine capable of spinning a part 1065 mm (42 in.) in diameter and 1270 mm (50 in.) in length. Backward spinning was used in preference to forward spinning because:

- The finished workpiece was longer than the mandrel
- Forward spinning would have required a change in workpiece design to permit hooking over the mandrel
- Backward spinning is faster than forward spinning

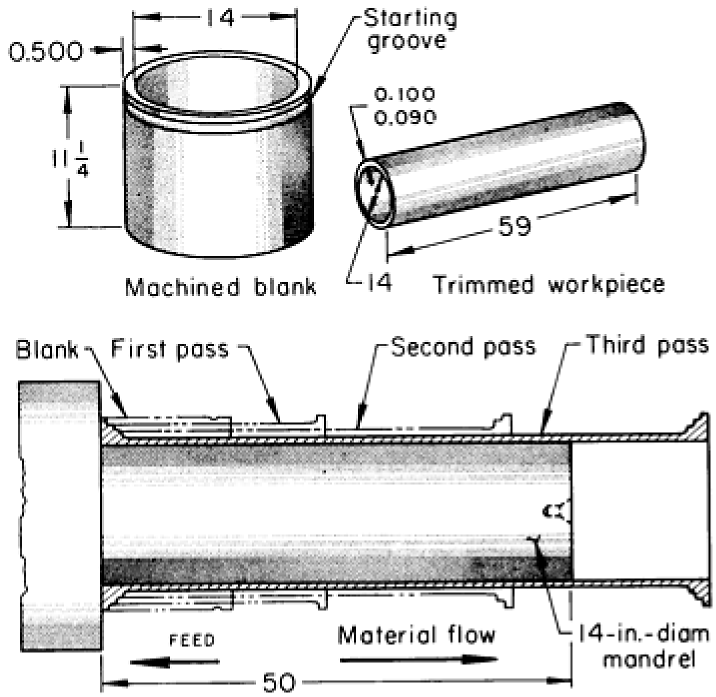

Fig. 5 Backward spinning of A-286 roll-forged tube (hardness: 200 HB max). Dimensions given in inches.

It was convenient to leave flanges at both ends and to trim these off later. The flanges prevented bell-mouthing and permitted trimming of the portions likely to have small radial cracks.

Example 4: Explosive Forming of A-286.

A tubular workpiece was explosively formed inside a die (Fig. 6) to produce a part having an internal flange. If this part had been produced by other methods, such a flange would have had to be welded on. A-286 sheet was rolled into a round cylinder 405 mm (16 in.) in diameter, welded, solution treated, and descaled. Tolerance on the diameter was ±0.75 mm (±0.03 in.).

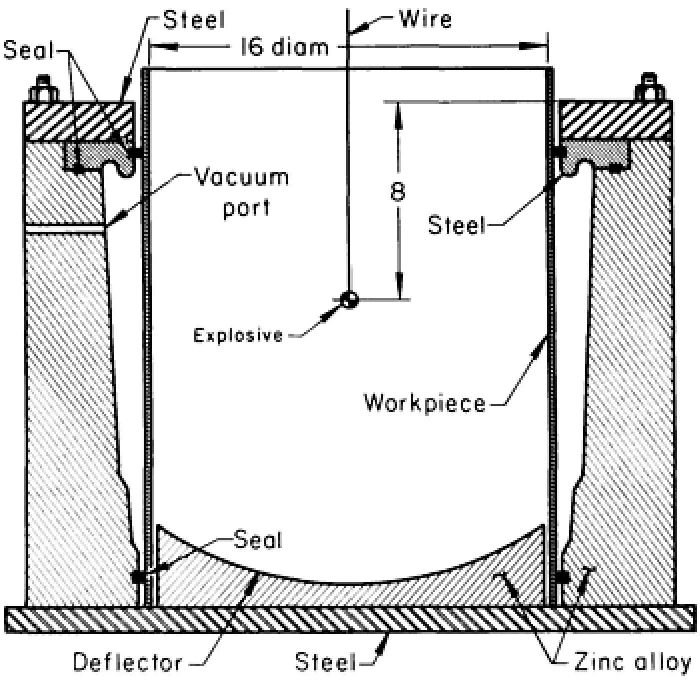

Fig. 6 Explosive forming a case from 1.5 mm (0.060 in.) thick A-286. Dimensions given in inches.

The explosive used was gel dynamite. Six shots were used to form the workpiece. For shots 1 and 2, 15 g of explosive was used; for shot 3, 18 g; for shot 4, 20 g; and for shots 5 and 6, 25 g. After three shots, the workpiece was solution annealed and descaled.

Forming of Heat-Resistant Alloys

Revised by S.K. Srivastava and E.W. Kelley, Haynes International

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Forming Practice for Nickel-Base Alloys

Two types of annealing treatments are used to soften the age-hardenable nickel-base alloys for forming, based on the ductility needed for forming and, if subsequent welding is required, on the avoidance of adverse metallurgical effects during and after welding. A high-temperature anneal is used to obtain maximum ductility and when no welding will be done on the formed part. A lower-temperature anneal, resulting in some sacrifice in ductility, is used when the will be welded.

For example, solution annealing of alloy 41 at 1175 °C (2150 °F) followed by quenching in water gives maximum ductility. However, parts formed from sheet annealed in this way should not be welded; during welding or subsequent heat treatment, they are likely to crack at the brittle carbide network developed in the grain boundaries. A lower annealing temperature, preferably 1065 to 1080 °C (1950 to 1975 °F), results in less sensitization during welding and decreases the likelihood of grain-boundary cracking. Formability is reduced by 10 to 20%, but is adequate for most forming operations. Typical practice for forming nickel-base alloys is described in the following examples.

Example 5: Forming and Slotting Alloy X.

The 88 flutes in the workpiece shown in Fig. 7 were finish formed and slotted one at a time with hand indexing in a 450 kN (50 tonf) mechanical press at the rate of one piece every 14.6 min, including setup. Slots were required to be within 0.5 mm (0.02 in.) of true position.

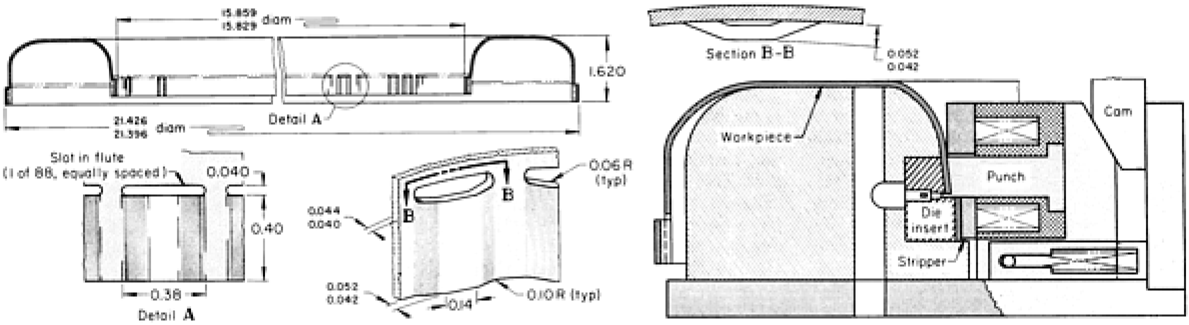

Fig. 7 Finish forming of flutes and piercing of slots one at a time in an alloy X workpiece using a mechanical press. Hardness of the workpiece was 74.5 to 81.5 HR30T. Dimensions given in inches.

The work metal was 1 to 1.1 mm (0.04 to 0.044 in.) alloy X sheet. Before the mechanical press operations, the sheet had been formed by a rubber-diaphragm process, electrolytically cleaned, annealed to 74.5 to 81.5 HR30T, pickled, restruck in the forming press, and trimmed. The flutes were partially formed in this series of operations.

In choosing a method of finish forming, it was decided that the only way to form the flutes to the required shape was to use a solid tool. The rubber-diaphragm forming process, however, was the best way to form the main contours of the part. The flutes could not be fully formed by a conventional die alone, because the percentage elongation exceeded the limits for alloy X (38 to 42% elongation in 50 mm, or 2 in.). By making use of the natural tendency of the blank to form wrinkles, the flutes were preformed during rubber-diaphragm forming, but pressures were only enough to form them 75% complete. However, the amount of elongation needed in the final die-forming operation was lowered, and definite locations for flutes were provided; therefore, each flute could be produced in one stroke of the mechanical press. The tooling (right side, Fig. 7) consisted of a die and a camactuated punch of high-carbon high-chromium tool steel hardened to 58 to 60 HRC, as well as die inserts, stripper, and cam sections of lower-alloy air-hardening tool steel. The punch pierced the slot and flattened the bulge above the flute. The stripper formed the flute when struck by the punch holder.

Example 6: Explosive Forming of Alloy 718.

Fully annealed alloy 718 (UNS N07718) sheet was used to make the flame deflector shown in Fig. 8. The sheet was rolled onto a cylinder, with the grain direction at right angles to the long axis. A 115 mm (4.5 in.) outside diameter by 815 mm (32 in.) long tube was gas tungsten arc welded from the cylinder using alloy 41 filler rod. The weld was made flush on the inside, and the outside was ground flush to +0.13 mm (+0.005 in.). The tube was spun to the dimensions shown in Fig. 8, fully annealed at 955 °C (1750 °F), and grit blasted. An outstanding characteristic of this alloy is its slow response to age hardening, which enables it to be welded and annealed with no spontaneous hardening unless cooled slowly. Explosive forming of the flame deflector was accomplished by three successive charges in a split die, and the workpiece was fully annealed after explosive forming.

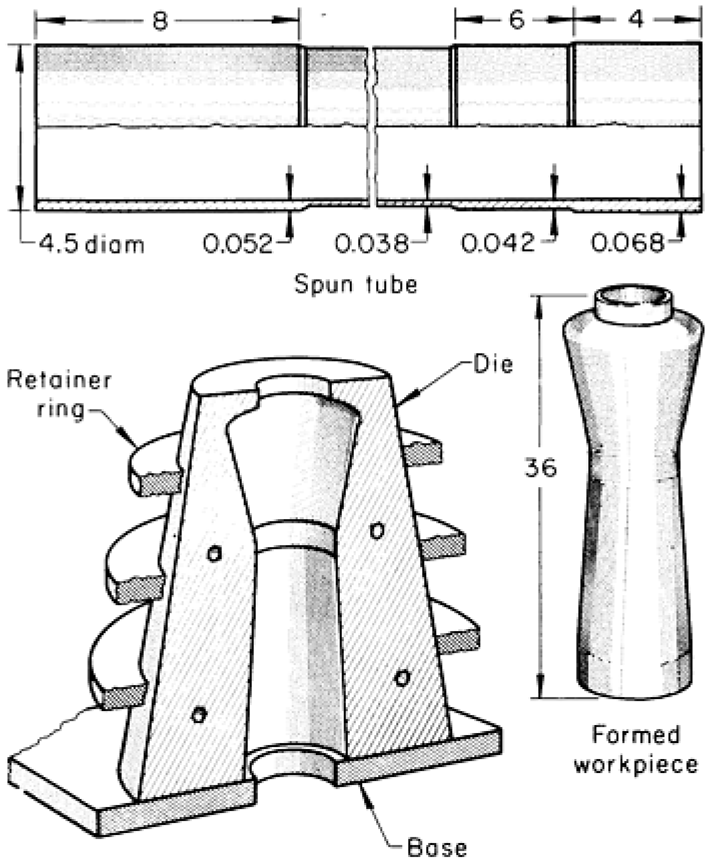

Fig. 8 Alloy 718 flame deflector (original sheet thickness: 1.8 mm, or 0.072 in.) produced by explosive forming in three successive charges. Dimensions given in inches.

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Forming Practice for Cobalt-Base Alloys

Forming the cobalt-base alloys requires more force because they are usually stronger than the iron-base and nickel-base alloys. The cobalt-base alloys with less than 20% Ni, such as Elgiloy alloy (UNS R30003) and alloy 25, are more difficult to form. Alloy N-155 (UNS R30155), a more formable alloy, has a tensile strength of 828 MPa (120 ksi), a 0.2% yield strength of 414 MPa (60 ksi) and elongation of 40%. These alloys, like most of the nickel-base alloys, are age hardened for elevated-temperature service. The practice used in forming HS-25 and N-155 parts is described in the following examples.

Example 7: Explosive Forming of Alloy 25.

Figure 9 shows the setup used for the explosive forming of a tail-pipe ball from alloy 25 sheet. The sheet was gas tungsten arc welded (butt) into a cylinder, and the shape was formed by three explosive charges. No annealing was done between welding and the first two shots of explosive forming, but after the first two shots (50 g of dynamite for each), the workpiece was withdrawn from the die, annealed at 1175 °C (2150 °F), and descaled. The workpiece was returned to the die for further forming. The third explosive charge used 62 g of dynamite. Tolerance on diameters was maintained within ±0.25 mm (±0.01 in.).

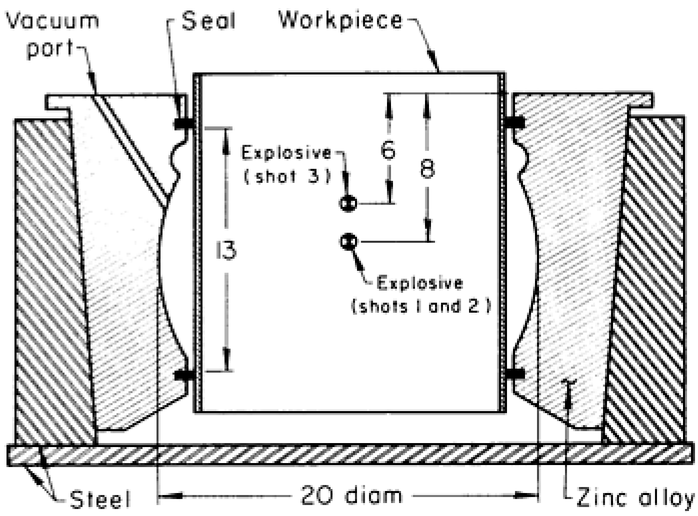

Fig. 9 Alloy 25 (sheet thickness: 1.7 mm, or 0.066 in.) welded cylinder in position for explosive forming. Dimensions given in inches.

Explosive forming was preferred over forming on an expanding mandrel. This is because the mandrel left flats on the wall of the workpiece and explosive forming did not.

Example 8: Alloy N-155 Exit Nozzle Produced by Tube Spinning and Explosive Forming.

The exit nozzle shown in Fig. 10 was produced from fully annealed 3.4 mm (0.135 in.) thick alloy N-155 sheet. The sheet was rolled into a cylinder, with grain direction at right angles to the long axis, and was gas tungsten arc welded. The weld was ground flush on both the inside and outside, after which the cylinder was tube spun to the various wall thicknesses shown in Fig. 10. The workpiece was then placed in a die and explosively formed to the shape shown at right in Fig. 10.

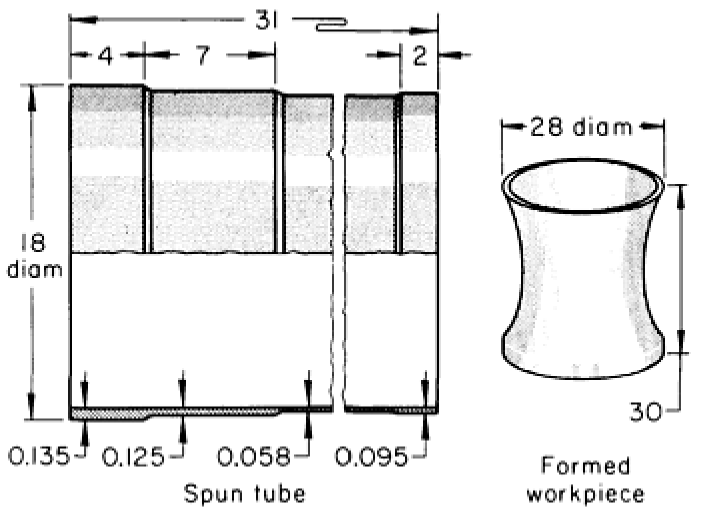

Fig. 10 Alloy N-155 exit nozzle produced by tube spinning and explosive forming. Dimensions given in inches.

The underwater explosive-forming technique was used, with a vacuum of 3 kPa (0.03 atm) between the workpiece and the die. The explosive charge was equal to 620 g of TNT and was placed at an average distance of 190 mm (7 $\frac{1}{2}$ in.) from the workpiece walls. The first shot produced approximately 90% of the final shape. A second shot, using the same size charge, completed the workpiece, after which it was fully annealed.

Forming of Heat-Resistant Alloys

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Forming of Refractory Metals

Revised by Louis E. Huber, Jr., Cabot Corporation

Introduction

REFRACTORY METALS are generally worked in small quantities. Production rates are low, each piece is handled separately, and the forming process is closely controlled.

Table 1 shows the composition of refractory alloys available as sheet. Typical conditions for bending 0.5 to 1.3 mm (0.020 to 0.050 in.) thick sheet are given in Table 2. Tensile-forming parameters for sheet materials are summarized in Table 3.

Table 1 Nominal compositions of refractory alloys available as sheet

Commercially pure niobium, tantalum, molybdenum, and tungsten sheets are also available.

Alloy	Composition, %				
	Zr	**Ti**	**Hf**	**W**	Other
Niobium alloys					
Nb-1Zr	1.0	. . .	. . .	. . .	**. . .**
FS-85	1.0	. . .	. . .	11.0	**28.0Ta**
C-103	. . .	1.0	10.0	. . .	**. . .**
C-129Y	. . .	. . .	10.0	10.0	**0.10Y**
Nb-752	2.5	. . .	. . .	10.0	**. . .**

Tantalum alloys					
Ta-2.5W	. . .	. . .	. . .	2.5	**. . .**
Ta-10W	. . .	. . .	. . .	10.0	**. . .**
Ta-8W-2Hf	. . .	. . .	2.0	8.0	**. . .**
Molybdenum alloys					
Mo-0.5Ti	. . .	0.5	. . .	. . .	**0.03C**
TZM	**0.1**	**0.5**	**. . .**	**. . .**	**0.03C**

Table 2 Conditions for the press-brake forming of refractory metal sheet 0.5 to 1.3 mm (0.020 to 0.050 in.) thick

Formed to a 120° bend angle in a 60° V-die at a ram speed of 254 to 3050 mm/min (10 to 120 in./min)

Metal or alloy	**Forming temperature, °C (°F)**	**Minimum bend radius[(a)]**		Springback, degrees
		Test data	**Preferred**	
Niobium alloys (annealed)				
C-103, C-129Y	Room	<1*t*	1*t*	**2-6**
Tantalum alloys (annealed)				
Tantalum	Room	<1*t*	1*t*	**. . .**
Ta-10W	Room	<1*t*	2*t*	**1-5**
Molybdenum alloys (stress-relieved)				
Mo-0.5Ti, TZM	150 (300)	2*t*-5*t*	5*t*	**3-8**
Tungsten (stress-relieved)				

(a) *t*, sheet thickness

Table 3 Elongation and true strain in forming refractory metal sheet of various thicknesses and grain

directions

Results are based on testing of one heat of material for each alloy.

Alloy	Condition[a]	Thickness		Forming temperature, °C (°F)	Grain direction[b]	Elongation, % in		True strain[c]	
		mm	in.			25 mm (1 in.)	50 mm (2 in.)	ϵ_m	ϵ_c
Niobium alloys									
C-103	SR	0.76	0.030	Room	L T	18.0 6.0	14.0 4.0	0.122 0.041	**0.145** **0.046**
	A	0.76	0.030	Room	L T	30.0 26.0	24.0 21.0	0.152 0.150	**0.232** **0.197**
Tantalum alloys									
Ta-10W	A	1.0	0.040	Room	L T	39.0 38.0	30.5 30.0	0.180 0.197	**0.283** **0.282**
Molybdenum alloys									
Mo-0.5Ti	SR	0.51	0.020	Room	L T	19.0 11.0	15.0 9.0	0.102 0.052	**0.164** **0.089**
TZM	SR	0.89	0.035	Room Room	L L	19.0 16.0	15.0 11.5	0.074 0.060	**0.130** **0.075**
Tungsten									
Tungsten	**SR**	**0.89**	**0.035**	**595 (1100)**	**T**	**4.0**	**2.5**	**0.019**	**0.022**

(a) A, annealed; SR, stress relieved.

(b) L, longitudinal; T, transverse.

(c) ε_m, true strain at maximum load; ε_c, maximum true strain at maximum true stress

Forming of Refractory Metals

Revised by Louis E. Huber, Jr., Cabot Corporation

Formability

Niobium and tantalum alloys are usually formed at room temperature in the annealed (recrystallized) condition, although the stress-relieved alloys are sufficiently ductile for most forming operations. Work hardening, especially of the stronger alloys, often necessitates annealing after severe forming.

Strong alloys of niobium and tantalum, which are made in limited quantity, are not listed in Table 1. These alloys have varying degrees of brittleness at low temperature, but can be formed by the same procedures used for molybdenum.

Molybdenum and tungsten are more difficult to form than niobium and tantalum, but if they are heated and certain precautions are taken, even complex parts can be formed. The greatest difficulty in forming these metals is their tendency toward brittle fracture (cracks and ruptures that occur with little or no plastic deformation) and delamination (a type of brittle behavior that produces cracks or ruptures parallel to the plane of the sheet). Tungsten can be hot formed only; it is brittle at room temperature.

At slow strain rates in tension and in bending, molybdenum and TZM alloys are ductile at room temperature, becoming brittle at lower temperatures. However, because of the high variable strain rates and triaxial stresses produced in the usual forming processes, these metals are usually hot formed in order to decrease the probability of brittle fracture. Molybdenum and tungsten blanks must have prepared edges to prevent cracking and splitting during forming.

Molybdenum and tungsten are generally supplied in stress relieved condition. Recrystallization increases the ductile-to-brittle transition temperature.

Effects of Composition on Embrittlement. Niobium and tantalum are severely embrittled by oxygen, nitrogen, and hydrogen, even in minute amounts. However, the usual melting and processing techniques keep the metals pure enough for good formability.

Some niobium alloys are more resistant to grain growth at high temperature than high-purity niobium. Alloys such as Nb-1Zr and C-103 are high-strength materials that resist grain growth at high temperature. These alloys have fine grain structure and elongate uniformly for forming and drawing operations.

Surface Contamination. The most common causes of surface contamination are failure to clean the surface properly and failure to provide the proper atmosphere in heat treatment. Niobium and tantalum are usually acid pickled, and they are heat treated in a vacuum or an inert gas atmosphere. Additional information on the heat treating of refractory metals is available in the article "Heat Treating of Refractory Metals and Alloys" in *Heat Treating*, Volume 4 of the *ASM Handbook.*

Generally, the high-strength alloys are more severely embrittled by surface contamination than the lower-strength alloys. Molybdenum and tungsten are much less susceptible to surface contamination by oxygen and nitrogen than niobium and tantalum.

Forming of Refractory Metals

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Factors That Affect Mechanical Properties

The major variables that affect mechanical properties and formability are working temperature, temperature of anneals between operations, percentage of reduction after the final anneal, and temperature of final heat treatment.

Rolling. Refractory metal sheet is generally made by hot forging or extruding billets to make sheet bars, which are rolled to sheet at high temperature. The final rolling of niobium and tantalum alloys is done below 540 °C (1000 °F), often at room temperature. The cold-worked sheet is given a final recrystallization anneal to improve formability and ductility. The finish rolling of molybdenum and tungsten is done at high temperature, and final heat treatment is usually for stress relief only. Cross-rolled sheet is generally more formable, because cross rolling makes ductility almost equal in all directions.

Heat Treatment. In the annealed (recrystallized) condition and in the ductile range, refractory metals behave much like steel. For example, recrystallized tungsten, although brittle at low temperature, has 35% uniform elongation and 50% total elongation at 400 °C (750 °F). Cold working strengthens molybdenum and tungsten and makes them less formable. Although molybdenum and tungsten are given a final stress relief, they retain their cold-worked structure.

Figure 1 shows the effect of heat treatment and strain hardening on the ductility and the ductile-to-brittle transition temperature range of unalloyed molybdenum. The curves in Fig. 1 show that the ductile-to-brittle transition for unalloyed molybdenum is between -18 and -45 °C (0 and -50 °F) for the stress-relieved condition, between 25 and -25 °C (80 and -10 °F) for the stretch-strained condition, and at approximately 25 °C (80 °F) for the recrystallized condition.

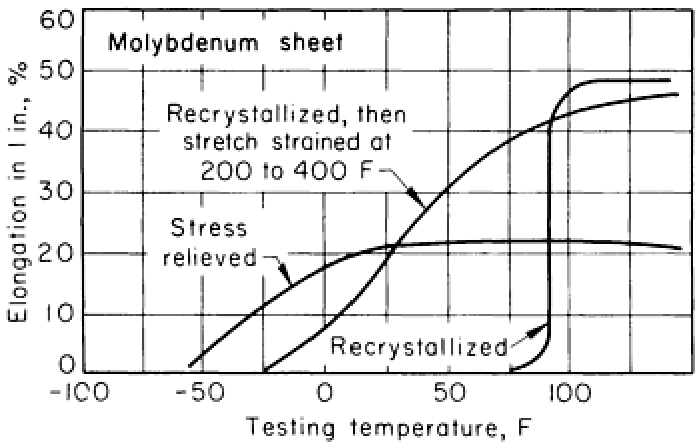

Fig. 1 Effect of heat treatment and strain hardening on the ductility and ductile-to-brittle transition temperature range of unalloyed molybdenum sheet as determined in tensile tests. The ductile-to-brittle transition occurs in the temperature range in the steep portion of the ductility curves.

Transition Temperature. Niobium, tantalum, and their most frequently used alloys are readily formable, and they are ductile at temperatures as low as -195 °C (-320 °F). Molybdenum and TZM, in the stress-relieved condition, have transition temperatures just below room temperature. Molybdenum may fracture or delaminate at room temperature under the high deformation rates and stresses generally encountered in forming practice. Therefore, molybdenum alloys are generally formed at moderate-to-high temperatures. Stress-relieved tungsten has a transition temperature of 150 to 315 °C (300 to 600 °F), so that all forming of tungsten must be done at high temperatures.

Figure 2 shows how temperature changes the strength and elongation of a typical high-strength niobium alloy with good formability. A slight increase in temperature reduces yield strength but also reduces ductility. The ductility is lowest at about 650 °C (1200 °F) and then increases with temperature. This reduced ductility is caused by strain aging, which is characteristic of body-centered cubic metals.

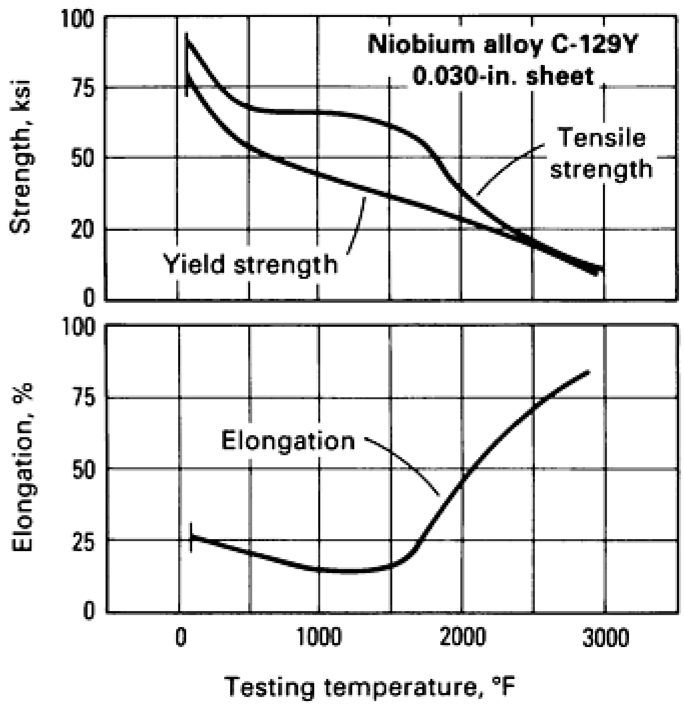

Fig. 2 Effect of temperature on strength and elongation of vacuum-annealed (recrystallized) niobium alloy sheet

Figure 3 shows how temperature changes the ductility of four typical refractory metals. The ductility minimums lie between 540 and 1095 °C (1000 and 2000 °F). The tantalum and niobium alloys in Fig. 3 were annealed (recrystallized), and the molybdenum alloy and tungsten were stress relieved. Tests above 260 °C (500 °F) were conducted in a vacuum.

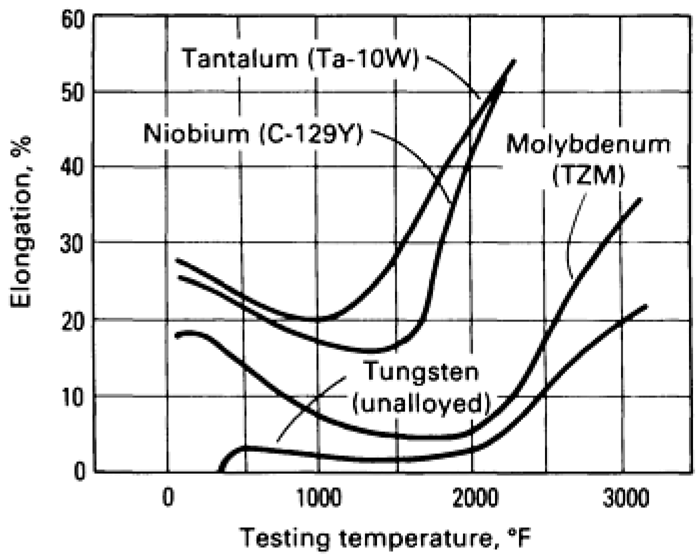

Fig. 3 Effect of temperature on the ductility of four refractory metals

Effect of Temperature on Formability

Annealed niobium and tantalum alloys are formed at room temperature. Heating these alloys would reduce their formability because of strain aging and would cause oxidation and possible surface contamination.

Tungsten is brittle at room temperature. Therefore, thin tungsten sheet is formed at 315 to 540 °C (600 to 1000 °F), and thicker sheet or complex shapes are formed at 540 to 815 °C (1000 to 1500 °F) after stress relief.

Molybdenum and molybdenum alloys, in thin sheets, can be cold formed to some extent, but heating helps to prevent fracture and delamination. As shown in Fig. 3, the TZM alloy is most ductile at 95 °C (200 °F). Further increases in temperature lessen ductility, because of strain aging. Most molybdenum is formed at 95 to 315 °C (200 to 600 °F), but thicker metal or complex shapes are formed at 315 to 650 °C (600 to 1200 °F).

Figure 4 shows the effect of heating on the bending of TZM sheet. This material is most formable at 95 to 205 °C (200 to 400 °F). Forming at lower temperatures may crack it.

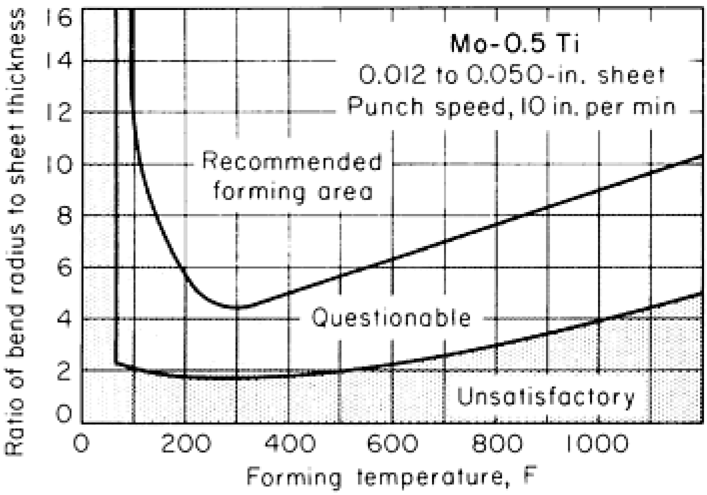

Fig. 4 Effect of temperature on the formability of Mo-0.5Ti sheet as indicated by the ratio of bend radius to sheet thickness

In one case, two sheets of TZM required bending. One sheet shattered when formed at room temperature, but the second sheet formed well in severe forming at 150 °C (300 °F). Erickson cup ductility tests indicated the two sheets to be of equal formability at room temperature.

Forming of Sheet

All of the common sheet-forming methods are used for refractory metals. However, the necessity of using elevated temperature in forming molybdenum and tungsten usually precludes the stretch forming and rubber-pad forming of these two metals.

Niobium and Tantalum. Almost all forming of niobium and tantalum is done at room temperature using conventional tools. A backup sheet is frequently used in a press brake to reduce galling or to provide support, so that the part will more closely follow the punch radius.

Niobium alloy C-103 is more ductile than type 310 stainless steel; it can be power spun to 60% reduction and deflects the rolls only half as much as 310 stainless. Tantalum can be spun in thicknesses as great as 15.75 mm (0.620 in.).

Tantalum, niobium, and Nb-1Zr sheet can be readily drawn into cups, tubes, or other shapes amenable to drawing methods. However, these materials exhibit a serious tendency toward galling to tool surfaces at contact pressures that are almost always exceeded in the drawing process. The tendency toward galling increases with each redraw, but can be significantly reduced or eliminated through careful attention to tool geometry, material surface condition, and workpiece lubrication.

The following rules should be observed when designing parts for deep drawing in these materials:

- Parts should have uniform wall thickness
- A 25% thinning allowance should be made on tight corners or extreme reductions in diameter; thinning can be effectively controlled through careful tool design
- The overall length should be less than nine times the smallest diameter in most cases, unless intermediate annealing is considered
- Bends of up to 90° can be made on inside radii of one-half the material thickness; bends over 90° should have an inside radius of at least one wall thickness
- Resultant surface finish is a function of grain size, severity of cold working, and original surface finish; it is difficult to "iron" to improve the surface finish because of galling

One method used to reduce galling consists of oxidizing the material surface by heating in an open furnace to temperatures as high as 650 °C (1200 °F) for tantalum or as high as 625 °C (1155 °F) for niobium or Nb-1Zr. The thickness of the oxide produced is related to the length of time at temperature and the surface condition of the material. A soak of 1 to 2 min produces a surface with greatly reduced tendency to gall. In most cases, this oxide must be removed from completed workpieces by acid etching or other means. The oxide is quite stable and is strongly abrasive to draw tooling. Serious reduction in tool life can be a problem when oxides are used.

Standard chlorinated drawing compounds are appropriate lubricants for the drawing of these materials. Spray or flood-type lubrication systems help to ensure adequate lubrication for parts requiring multiple redraws. Particular care must be taken to lubricate die surfaces, because dry spots will initiate galling.

Trimming or blanking operations should be conducted with a minimum punch-to-die clearance. This reduces metal pickup on tool surfaces. Burr-free or nearly burr-free results can be achieved.

Molybdenum and Tungsten. All of the common sheet-metal forming methods except rubber-pad forming and stretch forming are used for molybdenum and tungsten. These metals are formed at high temperature to prevent the cracking and delamination that occur when forming at room temperature. Stretch forming has not been successful, because of the difficulties in adapting high temperatures to the process.

Proper preparation of the edges of blanks is necessary in the forming of molybdenum and tungsten. All edges in tension during forming must be rounded or polished to prevent fracture. Shearing and sawing may cause edge cracking and delamination, which must be removed before forming.

Power and manual spinning are extensively used to work tungsten sheet. Tungsten can be power spun in machines that are capable of power spinning steel. Complex contoured or deeply recessed parts are often produced by drop hammer forming. All work is done with heated tools and with work metal temperatures ranging from 595 to 1095 °C (1100 to

2000 °F). Many failures in forming tungsten are caused by the stressing of edge defects in the starting blanks. These defects originate in sawing, shearing, or blanking and are difficult to detect visually.

Tools and workpiece blanks are heated by electrical-resistance elements, heat lamps, and gas torches. An allowance for the difference in thermal expansion between steel dies and a tungsten or molybdenum workpiece is required for all parts whose shape or dimensions will be out of tolerance because of forming at high temperature. Hot-work tool steels are satisfactory die materials. A bronze facing is recommended for steel dies if galling becomes a problem. Aluminum tooling is not recommended, because of its high thermal expansion.

Localized deformation and wrinkling of complex parts formed from molybdenum and tungsten generally result from poor die design and operation. These problems can be avoided by proper die clearance, staging and contours, and mechanical support. A steel backup sheet is sometimes used to ensure that the part more closely follows the punch or to reduce galling.

Forming of Preformed Blanks

Refractory metals that are preformed and welded into shaped blanks, cones, or cylinders can be formed by the same process used for unwelded blanks of common sheet metals. The welds must be of high quality to avoid defects or embrittlement. Chemical blanking, electrical discharge machining, abrasive cutting, and milling are preferred for making blanks. The following sequence of operations is generally used in preforming:

- Form intermediate shape
- Weld by the gas tungsten arc method
- Grind weld flush and inspect
- Stress relieve or anneal
- Form to final shape

Weldments of molybdenum and tungsten are generally formed at temperatures 95 to 150 °C (200 to 300 °F) higher than unwelded sheet of the same metals, and the weldments are usually stress relieved before forming. In some extreme applications, parts are stress relieved before and after welding, and after forming.

Forming of Refractory Metals

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Lubricants

The types of lubricants used in the forming of refractory metals include oils, extreme-pressure lubricants, soaps, waxes, silicones, graphite, molybdenum disulfide, copper plating, and an acrylic enamel coating made by suspending powdered copper in acrylic resin.

Ordinary oils and greases are commonly used in the forming of niobium and tantalum, because these metals are generally formed at room temperature. Petrolatum is frequently used for severe forming operations. Solid lubricants and suspensions of suitable pigments, such as molybdenum disulfide with or without colloidal graphite, are used in the hot forming of molybdenum and tungsten. Chlorinated lubricants and others that decompose upon heating to form toxic or noxious fumes must not be used without proper safety precautions.

Introduction

ALUMINUM and its alloys are among the most readily formable of the commonly fabricated metals. There are, of course, differences between aluminum alloys and other metals in the amount of permissible deformation, in some aspects of tool design, and in details of procedure. These differences stem primarily from the lower tensile and yield strengths of aluminum alloys, and from their comparatively low rate of work hardening. The wide range of compositions and tempers of aluminum alloys also affects their formability. This article emphasizes those aspects of commercial forming processes and equipment that apply specifically to aluminum alloys. More general information on the forming of metals is given in other articles in this Volume.

General Formability Considerations

The formability of a material is the extent to which it can be deformed in a particular process before the onset of failure. Aluminum alloy sheet usually fails during forming either by localized necking or ductile fracture. Necking is governed largely by material properties such as work hardening and strain-rate hardening and depends critically on the strain path followed by the forming process. In dilute alloys, the extent of necking or limit strain is reduced by cold work, age hardening, gross defects, large grain size, and the presence of alloying elements in solid solution. Ductile fracture occurs as a result of the nucleation and linking of microscopic voids at particles and the concentration of strain in narrow shear bands. Fracture usually occurs at larger strains than does localized necking and therefore is usually important only when necking is suppressed. Common examples where fracture is encountered are at small radius bends and at severe drawing, ironing, and stretching near notches or sheared edges (Ref 1, 2).

Considerable advances have been made in the development of alloys with good formability, but, in general, an alloy cannot be optimized on this basis alone. The function of the formed part must also be considered, and improvements in functional characteristics, such as strength and ease of machining, often tend to reduce the formability of the alloy.

Effects of Alloying Elements. The principal alloys that are strengthened by alloying elements in solid solution (often coupled with cold work) are those in the aluminum-magnesium (5*xxx*) series, ranging from 0.5 to 6% Mg. These alloys often contain small additions of transition elements such as chromium or manganese, and less frequently zirconium to control the grain or subgrain structure and iron and silicon impurities that are usually present in the form of intermetallic particles. Figure 1 illustrates the effect of magnesium in solid solution on the yield strength and tensile elongation for most of the common aluminum-magnesium commercial alloys. Note the large initial reduction in tensile elongation with the addition of small amounts of magnesium.

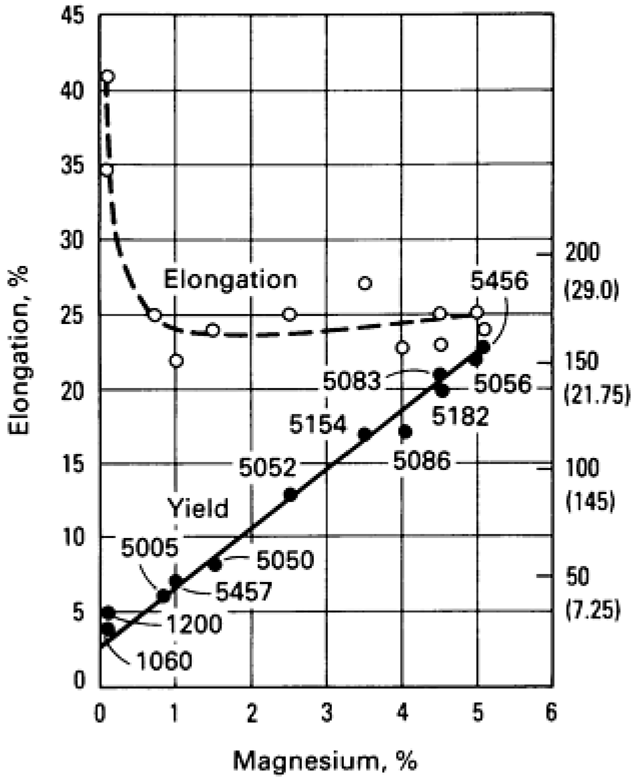

Fig. 1 Correlation between tensile yield strength, elongation, and magnesium content for some commercial aluminum alloys in the annealed temper. Source: Ref 3

The reductions in the forming limit produced by additions of magnesium and copper appear to be related to the tendency of the solute atoms to migrate to dislocations (strain age). This tends to increase work hardening at low strains, where dislocations are pinned by solute atoms, but it also decreases work hardening at large strains. Small amounts of magnesium or copper also reduce the strain-rate hardening, which in turn reduces the amount of useful diffuse necking that occurs after the uniform elongation. Zinc in dilute alloys has little effect on work hardening or necking and it does not cause strain aging.

Elements that have low solid solubilities at typical processing temperatures, such as iron, silicon, and manganese, are present in the form of second-phase particles and have little influence on either strain hardening or strain-rate hardening and thus a relatively minor influence on necking behavior. Second-phase particles do, however, have a large influence on fracture. The addition of magnesium promotes an additional reduction in fracture strain, because the higher flow stresses aid in the formation and growth of voids at the intermetallic particles. Magnesium in solid solution also promotes the localization of strain into shear bands, which concentrates the voids in a thin plane of highly localized strain.

Precipitation-strengthened alloys are usually formed in the naturally aged (T4) condition or in the annealed (O) condition, but rarely in the peak strength (T6) condition, where both the necking and fracture limits are low. Figure 2 shows the effect of a wide range of precipitate structures on some of the forming properties of alloy 2036 (Al-2.5Cu-0.5Mg). Curves similar in shape can be drawn for most of the precipitation-strengthened alloys in the 2*xxx* and 6*xxx* series.

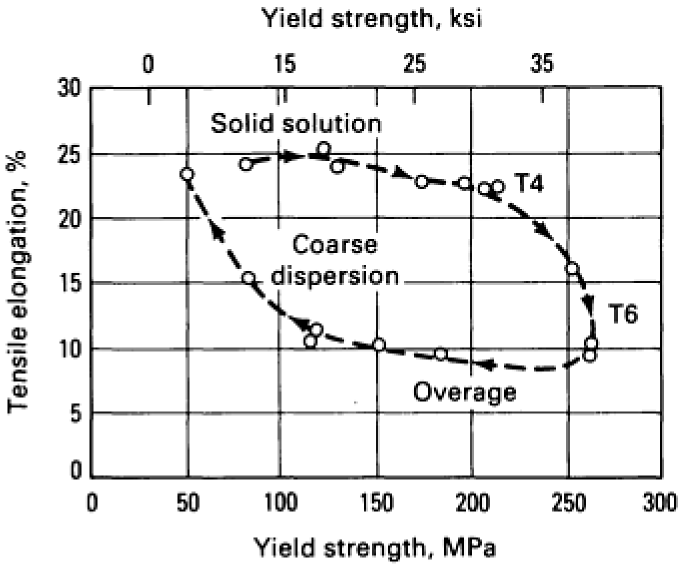

Fig. 2 Effect of precipitation on yield strength and elongation of aluminum alloy 2036. Source: Ref 3

The properties in Fig. 2 were obtained from sheet tensile specimens first solution heat treated, then aged at temperatures ranging from room temperature to 350 °C (660 °F). This produced a full range of structures from solid solution (as-quenched) through T4 and T6 tempers to various degrees of averaging and precipitate agglomeration.

Material Properties and Testing. To conduct a complete analysis of a formed part, the required mechanical properties, as determined by several standard tests, must be considered. These properties include those determined by tension testing and by other tests designed to simulate various production forming processes, including cup tests and bend tests. More information on the test methods briefly described here is available in the article "Formability Testing of Sheet Metals" in this Volume.

Tension testing is used to determine the commonly reported properties--(ultimate) tensile strength, (tensile) yield strength, and (total) elongation--as well as two properties especially important in forming (Ref 1), that is, the strain-hardening exponent n and the plastic-strain ratio r.

The strain-hardening exponent n of a material is determined from the true stress-true strain curve for that material using the formula:

$$\sigma = K\epsilon^n \quad \textbf{(Eq 1)}$$

where σ is true stress, ϵ is true strain, and K is a constant of proportionality.

The plastic-strain ratio r describes the resistance of the material to thinning during forming operations and is the ratio of the true strain in the width direction (ϵ_w) to the true strain in the thickness direction (ϵ_t) of plastically strained sheet:

$$r = \frac{\epsilon_w}{\epsilon_t} \quad \textbf{(Eq 2)}$$

A standard method for determining r using a tension specimen is given in ASTM E 517.

The tensile properties (as well as other mechanical properties) of many aluminum sheet alloys in medium and hard tempers exhibit directional sensitivity. The test direction should be reported along with test results. Directional sensitivity is important in analysis of forming operations that involve bending, flange stretching, or plane straining, all of which are encountered in the forming of ribs and troughs. Orientation of the rolling direction of the sheet relative to the direction of critical strain in the part often can mean the difference between producing a good art and producing scrap.

The Olsen cup test is a biaxial-stretch-forming test that has been used since the early 1900s (Ref 4). A specimen is stretched over a 22.2 mm ($\frac{7}{8}$ in.) diam ball lubricated by a small disk of oiled polyethylene. The flange of the specimen is tightly clamped. Maximum cup height is measured when necking occurs. The value reported is the ratio of cup height to cup diameter.

In the Swift cup test, a deep-drawn cup is used to determine the limiting draw ratio (LDR) of blank size to cup diameter. It is obtained with a 51 mm (2.0 in.) diam flat-bottom punch and a draw die appropriate for the thickness of the specimen. A circular blank is cut to a diameter smaller than the expected draw limit. Lubrication is provided by two oiled polyethylene disks, one on each side of the blank. The blank is drawn to maximum punch load, which occurs before the cup is fully formed. Successively larger blanks are drawn until one fractures before being drawn completely through the die. The diameter of the largest blank that can be drawn without fracturing, divided by cup diameter, determines the LDR.

In the bend test, strips of material are bent around mandrels having different tip radii. Often, mandrels are in the form of pins or rods. The mandrel is forced against one side of the specimen strip, and the other side is simply supported at the end points. The value reported is the minimum radius of mandrel, in multiples of the material thickness *t* around which the material can be bent 180° without cracking. The direction of the bend relative to the rolling (or extrusion) direction should be recorded with the test results.

Correlations Among Test Results. It has been found that results of simulative forming tests correlate quite well with results of tension tests. Specifically, the results of cup ductility tests, such as the Olsen and Swift tests, show good correlation with values of tensile elongation, strain-hardening exponent, and plastic-strain ratio. Olsen cup values correlate well with tensile elongation, and Swift cup values correlate with plastic-strain ratio.

Forming-limit diagrams, also known as forming-limit curves, are direct and useful representations of the formability of aluminum sheet. These diagrams illustrate the biaxial combinations of strain that can occur without failure.

To construct forming-limit diagrams, an array of circles, which often are 2.5 mm (0.1 in.) in diameter, is first imprinted by photoprinting, photoetching, or electroetching on the surface of the sheet metal before forming. The individual circles become ellipses wherever deformation occurs, except in areas where pure biaxial stretching occurs. The major and minor axes of the ellipses are compared with the circles of the original grid to determine the major and minor strains at each location. The areas immediately adjacent to failures are of particular concern in evaluating the forming capabilities of the metal. Failure can be defined by several criteria, but the onset of visible necking is the most widely used. The loci of strain combinations that produce failures define the forming-limit curve. The area below this curve encompasses all the combinations of strain that the metal can withstand.

Forming-limit diagrams for a variety of aluminum alloys and tempers are shown in Fig. 3, 4, and 5. More information on the construction and use of forming limit diagrams is available in the article "Process Modeling and Simulation for Sheet Forming" in this Volume.

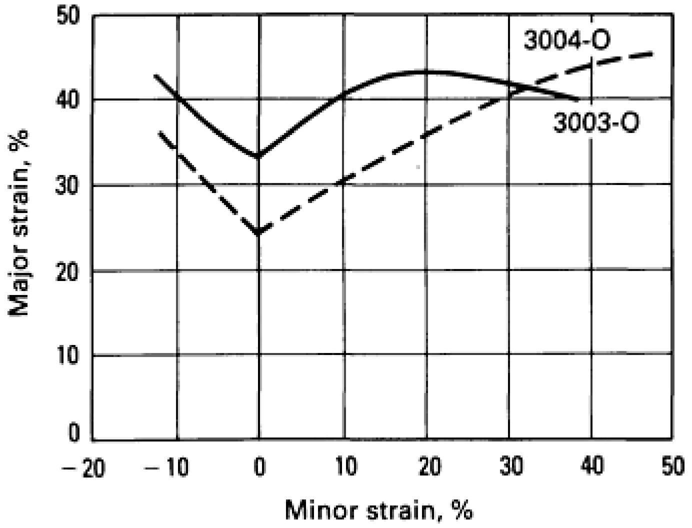

Fig. 3 Forming-limit diagrams for two 3*xxx* series aluminum alloys. Source: Ref 5

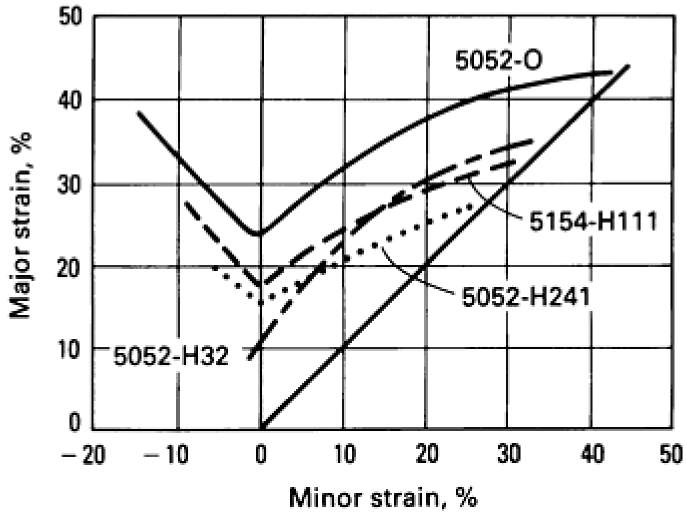

Fig. 4 Forming-limit diagrams for four 5*xxx* series aluminum alloys. Source: Ref 5

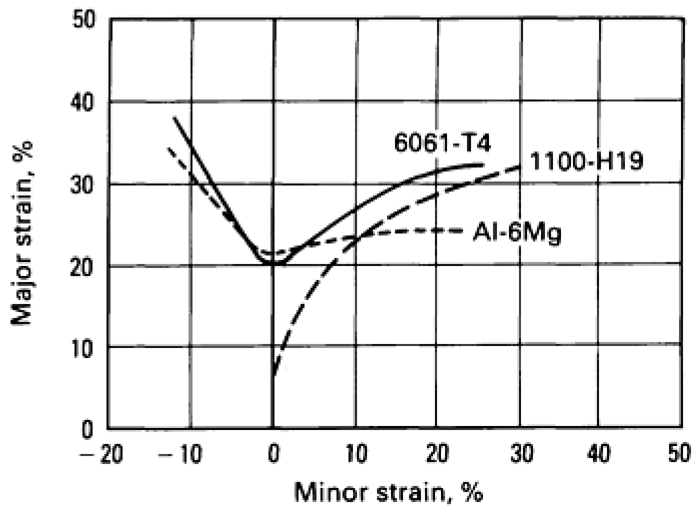

Fig. 5 Forming-limit diagrams for aluminum alloys 1100-H19 and 6061-T4 and for Al-6Mg. Source: Ref 5

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Equipment and Tools

Most of the equipment used in the forming of steels and other metals is suitable for use with aluminum alloys. Because of the generally lower yield strengths of aluminum alloys, however, required press capacities are usually lower than for comparable operations on steel, and higher press speeds can be used. Similarly, equipment for roll forming, spinning, stretch forming, and other forming operations on aluminum need not be as massive or rated for such heavy loading as for comparable operations on steel.

Tools. Total wear on tools used in forming aluminum is somewhat less than when forming steel. This results in part from the lower force levels involved and in part from the smoother surface condition that is characteristic of aluminum alloys. Accordingly, tools can sometimes be made from less expensive materials, even for relatively long runs.

However, a higher-quality surface finish is generally required on tools used with aluminum alloys, to avoid marking. The oxide film on the surface of aluminum alloys is highly abrasive, and for this reason many forming tools are made of hardened tool steels. As a rule, these tools, even if otherwise suitable, should not be used interchangeably to form steel parts, because this could destroy the high-quality finish on the tools.

Most aluminum alloys require smaller clearances between punches and dies in blanking and piercing than do steels. On drawing tools, they require larger clearances but about the same radii, to allow the free flow of metal and avoid excessive stretching.

The amount of springback in forming aluminum alloys is generally less than it is in forming low-carbon steel, and this must be considered in tool design. The amount of springback is roughly proportional to the yield strength of the metal. Additionally, the lower rate of work hardening of aluminum alloys permits a greater number of successive draws than is usually possible with steel. More information on equipment for sheet forming is available in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" in this Volume.

Lubricants

Lubricants must be specifically selected for their compatibility with aluminum alloys and their suitability for the particular forming operation in question. A lubricant suitable for use on a steel part will not necessarily be suitable for use in the forming of a similar aluminum alloy part.

The proper formulation of lubricants for the forming of aluminum alloys must take into account the special requirements of regulation of moisture content in nonaqueous systems, corrosion inhibitors, and pH control, in order to prevent staining or corrosion and to make duration of contact with the workpiece less critical.

The lubricants most widely used in the forming of aluminum alloys are listed below in approximate order of increasing effectiveness:

- Kerosene
- Mineral oil (viscosity of 40 to 300 SUS a 40 °C, or 100 °F)
- Petroleum jelly
- Mineral oil plus 10 to 20% fatty oil
- Tallow plus 50% paraffin
- Tallow plus 70% paraffin
- Mineral oil plus 10 to 15% sulfurized fatty oil and 10% fatty oil
- Dried soap films or wax films
- Fat emulsions in aqueous soap solutions with finely divided fillers
- Mineral oil with sulfurized fatty oil, fatty oil, and finely divided fillers

The use of various special-purpose lubricants is discussed in sections of this article that deal with individual forming processes. The article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume contains more information on lubricants for sheet forming.

Blanking and Piercing

Blanking and piercing of aluminum alloy flat stock are ordinarily done in punch presses because of their high production rates and ability to maintain close tolerances. Press brakes are sometimes used, particularly for experimental or short-run production.

The generally lower shear strength of aluminum alloys usually dictates the use of presses or press brakes of lower capacity than those used for comparable operations with steel. Total required shearing force can be calculated as the product of shear strength, total length of cut, and metal thickness, but allowance must be made for different alloys, for dulling of the cutting edges of punches and dies, and for variation in clearance between punch and die. The shear strengths of the commonly used aluminum alloys range from 62 to 338 MPa (9 to 49 ksi), whereas the shear strength of a typical low-carbon steel is 241 to 462 MPa (35 to 67 ksi).

Tool Materials. A discussion of materials for blanking and piercing dies is given in the article "Selection of Material for Press-Forming Dies" in this Volume. Aluminum alloys are classed with other soft materials, such as copper and magnesium alloys. In general, for a given tool material, tool life is longer for blanking and piercing aluminum alloys than for blanking and piercing steel.

In some applications, a less expensive die can be used than is true for steel parts, particularly for relatively short runs. Cast zinc dies, which cost only about one-fifth as much as tool steel dies, are used for runs of up to about 2000 parts. Steel-rule dies and template dies also reduce tooling costs for short runs or moderate-length runs. For example, an aluminum alloy blank 495 by 305 mm (19.5 by 12 in.) by 1 mm (0.040 in.) in thickness was made in a steel-rule die having an expected life of 150 pieces. For the production quantity, burr height did not exceed 0.127 mm (0.005 in.). Punches and die buttons for seven pierced holes of 3.9, 4.8, and 6.4 mm ($\frac{5}{32}$, $\frac{3}{16}$, and $\frac{1}{4}$ in.) diameter were incorporated in the die.

Low-carbon steel or cast iron dies sometimes replace hardened tool steel dies, even for long runs. Punches are usually made from annealed or hardened tool steel, depending on the size and complexity of the part and the length of the run. Cemented carbide tools are seldom required, even for extremely long runs.

Tolerances. A tolerance of ±0.127 mm (0.005 in.) is normal in the blanking and piercing of aluminum alloy parts in a punch press. Using a press brake, it is possible to blank and pierce to a location tolerance of ±0.25 mm (0.010 in.) or less, although tolerances for general press brake operations usually range from ±0.51 to ±0.76 mm (0.020 to 0.030 in.).

For economy in tool cost, specified tolerance should be no less than is actually necessary for the particular part. A tolerance of ±0.127 mm (0.005 in.) would probably require that the punch and die be jig ground, adding 30 to 40% to their cost. A tolerance of ±0.05 mm (0.002 in.) may require the addition of a shaving operation. In addition to the cost of an extra die, labor costs would be increased by the added operation.

For extremely accurate work, an allowance must be made for the shrinkage of holes and expansion of blanks resulting from the elasticity of the stock. This allowance, made to both punch and die, does not change the clearance between them, and is primarily a function of stock thickness. For large sizes and normal tolerances, this correction is not very important.

Clearance between punch and die must be controlled in blanking and piercing in order to obtain a uniform shearing action. Clearance is usually expressed as the distance between mating surfaces of punch and die (per side) in percentage of work thickness.

Correct clearance between punch and die depends on the alloy as well as the sheet thickness. Suggested punch-to-die clearances in terms of percentage of sheet thickness *t* for blanking and piercing aluminum alloys in various tempers are listed in Table 1.

Table 1 Punch-to-die clearances for blanking and piercing aluminum alloys

Alloy and temper	**Clearance per side, %t[(a)]**
1100	
O	5.0
H12, H14	6.0
H16, H18	7.0
2014	
O	6.5
T4, T6	8.0
2024	
O	6.5
T3, T36, T4	8.0
3003	
O	5.0
H12, H14	6.0
H16, H18	7.0
3004	
O	6.5
H32, H34	7.0
H36, H38	7.5
5005	
O	5.0
H12, H14, H32, H34	6.0
H36, H38	7.0
5050	
O	5.0
H32, H34	6.0
H36, H38	7.0
5052	
O	6.5
H32, H34	7.0
H36, H38	7.5
5083	
O	7.0
H323, H343	7.5
5086	
O, H112	7.0
H32, H34, H36	7.5
5154	
O, H112	7.0
H32, H34, H36, H38	7.5
5257[(b)]	
O	5.0
H25	6.0
H28	7.0
5454	
O, H112	7.0
H32, H34	7.5
6061	
O	5.5
T4	6.0
T6	7.0
7075	

O	6.5
W, T6	8.0
7178	
O	6.5
W, T6	8.0

(a) *t*, sheet thickness.

(b) Also alloys 5357, 5457, 5557, and 5657

The character of the shearing action also depends on the sharpness of the tools. Dull cutting edges on punch and die have effects similar to those of excessive clearance, with the effect on burr size being particularly pronounced.

With proper clearance, the fractures proceeding from the punch surface and from the die surface of the work meet cleanly without secondary shearing and excessive plastic deformation. Secondary shearing indicates that the clearance is too small; a large radius or dished contour at the sheared edge and a stringy burr indicate that the clearance is too large.

For additional information on punch-to-die clearances, see the article "Piercing of Low-Carbon Steel" in this Volume.

Die Taper. The walls of die openings in blanking or piercing dies are often tapered $\frac{1}{2}$° from the vertical, to minimize sticking of the blank or slug in the die. A straight, vertical section of at least 3.2 mm ($\frac{1}{8}$ in.) or equal to the metal thickness for stock thicker than 3.2 mm ($\frac{1}{8}$ in.) is usually left at the upper end of the die opening, to provide for sharpening without changing the clearance. Tapered die relief is usually more suitable for piercing aluminum than is counterbore design relief.

Stripping force of 3 to 20% of the total capacity needed for blanking and piercing is used for aluminum alloys. The force needed depends on the alloy, temper, and stock thickness. Sharpness of cutting edges on punch and die, lubrication, and uniformity of application of stripper-plate pressure also affect stripping force.

Lubricants are normally used in blanking or piercing aluminum alloy parts to reduce sticking of slugs or blanks in the die opening and to facilitate clean stripping from the punch without buckling. Lower tool maintenance costs and smoother edges on blanks or holes can be obtained with suitable lubrication.

Forming of Aluminum Alloys

Press-Brake Forming

The press-brake forming techniques used with aluminum alloys are similar to those used with steel and other metals, differing only in some details of tool design (see the article "Press-Brake Forming" in this Volume).

Tolerances in press-brake forming are larger than those in punch press operations. For simple shapes that are relatively long and narrow, a tolerance of ±0.8 mm ($\frac{1}{32}$ in.) can usually be maintained. On larger parts of more complex cross section, the tolerance may be as much as ±1.6 mm ($\frac{1}{16}$ in.).

Springback, or partial return to the original shape upon removal of the bending forces, occurs in most bending operations. The amount of springback depends on the yield strength and thickness of the material and on the bend radius. Table 2 shows the effects of these variables, giving springback allowances in degrees of overbending that have been used for high-strength aluminum alloys 2024 and 7075.

Table 2 Springback allowances for 90° bends in 2024 and 7075 aluminum alloy sheet

Sheet thickness		Springback allowance, in degrees, for bend radius, mm (in.) of:							
mm	**in.**	**2.4 ($\frac{3}{32}$)**	**3.2 ($\frac{1}{8}$)**	**4.8 ($\frac{3}{16}$)**	**6.4 ($\frac{1}{4}$)**	**7.9 ($\frac{5}{16}$)**	**9.5 ($\frac{3}{8}$)**	**11.1 ($\frac{7}{16}$)**	12.7 ($\frac{1}{2}$)
2024-O and 7075-O									
0.51	0.020	3	4	$5\frac{1}{2}$	$7\frac{1}{2}$	$8\frac{1}{2}$	9	$9\frac{1}{2}$	**12**
0.64	0.025	$2\frac{3}{4}$	$3\frac{3}{4}$	$5\frac{1}{2}$	$6\frac{1}{2}$	8	$8\frac{1}{4}$	$8\frac{3}{4}$	**$10\frac{3}{4}$**
0.81	0.032	$2\frac{1}{4}$	3	$4\frac{3}{4}$	6	$6\frac{3}{4}$	7	$7\frac{1}{2}$	**$9\frac{1}{2}$**
1.02	0.040	2	3	4	5	6	$6\frac{1}{4}$	$6\frac{3}{4}$	**$8\frac{3}{4}$**
1.29	0.051	2	$2\frac{1}{2}$	$3\frac{1}{2}$	4	5	$5\frac{1}{4}$	$5\frac{3}{4}$	**$7\frac{1}{2}$**
1.63	0.064	$1\frac{1}{2}$	2	$2\frac{3}{4}$	$3\frac{3}{4}$	$4\frac{1}{2}$	5	$5\frac{1}{2}$	**$6\frac{3}{4}$**
2.06	0.081	1	$1\frac{1}{2}$	2	$2\frac{1}{2}$	$3\frac{1}{4}$	$3\frac{1}{2}$	4	**$4\frac{3}{4}$**
2.39	0.094	. . .	. . .	$1\frac{3}{4}$	$2\frac{1}{2}$	3	$3\frac{1}{4}$	$3\frac{3}{4}$	**$4\frac{1}{2}$**
3.18	0.125	. . .	. . .	$1\frac{1}{2}$	2	$2\frac{1}{4}$	$2\frac{3}{4}$	3	**$3\frac{3}{4}$**
2024-T3									
0.51	0.200	10	12	$15\frac{1}{2}$	19	$22\frac{1}{2}$	24	$27\frac{1}{4}$	**$33\frac{1}{2}$**
0.64	0.025	$8\frac{3}{4}$	$10\frac{1}{2}$	14	$16\frac{3}{4}$	$17\frac{3}{4}$	21	23	**$28\frac{1}{2}$**
0.81	0.032	$7\frac{3}{4}$	$8\frac{3}{4}$	12	$14\frac{1}{2}$	$16\frac{3}{4}$	$17\frac{3}{4}$	$19\frac{1}{4}$	**24**
1.02	0.040	$7\frac{1}{4}$	$8\frac{1}{4}$	$10\frac{3}{4}$	$12\frac{3}{4}$	$14\frac{1}{2}$	$15\frac{1}{4}$	17	**$20\frac{1}{2}$**
1.29	0.051	. . .	. . .	9	$10\frac{1}{2}$	$12\frac{1}{4}$	13	$14\frac{1}{2}$	**$16\frac{3}{4}$**
1.63	0.064	. . .	. . .	8	$9\frac{3}{4}$	$11\frac{1}{4}$	12	$12\frac{3}{4}$	**15**
2.06	0.081	. . .	. . .	. . .	. . .	$9\frac{1}{2}$	$10\frac{1}{2}$	$11\frac{1}{4}$	**13**
2.39	**0.094**	**. . .**	**. . .**	**. . .**	**. . .**	**$8\frac{3}{4}$**	**$9\frac{3}{4}$**	**$10\frac{1}{2}$**	**12**

The springback allowance, or number of degrees of overbending required, ranges from 1 to 12° for 2024-O and 7075-O (yield strength of 76 MPa, or 11 ksi, min), and from $7\frac{1}{4}$ to $33\frac{1}{2}$° for 2024-T3 (yield strength of 345 MPa, or 50 ksi). The allowance increases with increasing yield strength and bend radius, but varies inversely with stock thickness. The allowance for bends of other than 90° can be estimated on a proportional basis. For bend angles of less than 90°, the springback may be greater unless the bend radius is decreased, because the metal in the bend area may not have been stressed beyond its yield point.

Radii to which bends can be made depend on the properties of the metal and the design, dimensions, and condition of the tools. For most metals, the ratio of minimum bend radius to sheet thickness is approximately constant, because ductility is the primary limiting factor on minimum bend radius. This is not true for aluminum alloys, for which the ratio of bend radius to sheet thickness increases with the thickness.

With special tooling, aluminum alloys can be bent to smaller radii than those indicated in standard tables. Bottoming dies and dies that combine bottoming with air bending are used for this purpose. Hydraulic forming, forming with rubber-pad dies, and high-energy-rate forming also produce good small-radius bends.

Sometimes it is possible to take advantage of the grain direction in the work metal: The most severe bends can be made across the direction of rolling. If similar bends are made in two or more directions, it is recommended that, if possible, all bends be made at an angle to the direction of rolling. Local heating along the bend lines can sometimes be used to produce small bend radii without fracture; this is particularly useful in bending plate.

The maximum temperature that can be used without serious loss in mechanical properties is 150 to 205 °C (300 to 400 °F) for cold-worked material. Reheating of naturally aged aluminum alloys 2014 and 2024 is not recommended unless the part is to be artificially aged. Generally, any reheating sufficient to improve formability will lower the resistance to corrosion to an undesirable degree, except with alclad sheet.

Blank Development. For relatively simple parts, particularly those for which close tolerances are not required, the blank layout can be developed directly by using bend-allowance tables or equations. As a rule, the initial calculated blank layout and die design are developed into final form by successive trial and modification.

Lubricants are needed for nearly all press-brake forming of aluminum alloys. The light protective film of oil sometimes present on mill stock is often adequate for mild bending operations, but when this is not sufficient, a lubricant is usually applied to the working surfaces of the tools and the bend area of the workpiece to prevent scoring and metal pickup.

Tools. The bending, forming, piercing, and notching dies used in press brakes for aluminum alloys are much the same as those used for low-carbon steel. To prevent marring or scratching of the workpiece, tools used for bending steel should be carefully cleaned and polished before being used for aluminum alloys. Rubber pads used in press-brake dies, when clean, will not scratch the surface of an aluminum sheet.

Because of the differences in tensile strength and springback, shut height settings for aluminum alloys may be different from those for low-carbon steel.

Contour Roll Forming

Aluminum alloys are readily shaped by contour roll forming, using equipment and techniques similar to those used for steel (see the article "Contour Roll Forming" in this Volume). Operating speeds can be higher for the more ductile aluminum alloys than for most other metals. Speeds as high as 245 m/min (800 ft/min) have been used in mild roll forming of 0.8 mm ($\frac{1}{32}$ in.) thick alloy 1100-O sections 15 to 30 m (50 to 100 ft) long. Power requirements for roll forming of aluminum alloys are generally lower than is the case for comparable operations on steel, because of the lower yield strength of most aluminum alloys.

Tooling. The design of rolls and related equipment, as well as the selection of tool materials, is discussed in the article "Contour Roll Forming" in this Volume. The most commonly used material is L6 tool steel, a low-alloy nickel-chromium grade with excellent toughness, wear resistance, and hardenability. For extremely severe forming operations or exceptionally long runs, a high-carbon high-chromium grade such as D2 is preferred because it has superior resistance to galling and wear. These tool steels are hardened to 60 to 63 HRC. The tools are highly polished and are sometimes chromium plated to prevent scratching and to minimize the pickup of chips when surface finish of the work is critical.

For short runs and mild forming operations, rolls can be made of turned and polished gray cast iron (class 30 or better) or low-carbon steel. For light-gage metals, tools made of plastics reinforced with metal powder, or of specially treated hardwood, have occasionally been used. For some applications in the roll forming of light-gage alloys when quality of surface finish is the primary concern, use has been made of cast zinc tools, at the cost of shorter tool life.

Extremely close tolerances are required on tool dimensions. Allowance for springback must be varied with alloy and temper, as well as with material thickness and radius of forming, as indicated in Table 2. Final adjustments must be made on the basis of production trials.

Tolerances of ±0.127 mm (0.005 in.) are common in contour roll forming, and ±0.05 mm (0.002 in.) can be maintained on small, simple shapes formed from light-gage metals. One or two final sizing stations may be required for intricate contours or when springback effects are great.

Lubricants are required in nearly all contour roll forming of aluminum alloys. For high-speed or severe forming operations, the rolls and workpiece may be flooded with a liquid that functions as both a lubricant and a coolant. A soluble oil in water is preferred for this type of operation. When a more effective lubricant is required, a 10% soap solution or an extreme-pressure (EP) compound may be used. These are better suited for minimizing tool wear and producing a high-quality finish, but are more difficult to remove.

Applications. Roll-formed aluminum alloy parts made from sheet or coiled strip include furniture parts, architectural moldings, window and door frames, gutters and downspouts, automotive trim, roofing and siding panels, and shelving.

Tubing in sizes ranging from 19 to 203 mm ($\frac{3}{4}$ to 8 in.) in outside diameter and from 0.64 to 3.9 mm (0.025 to 0.156 in.) in wall thickness is made in a combined roll-forming and welding operation (see the article "Contour Roll Forming" in this Volume). Linear speeds of 9 to 60 m/min (30 to 200 ft/min) are used in this process. Applications include irrigation pipe, condenser tubing, and furniture parts.

Other applications of contour roll forming include the forming of patterned, anodized, or pre-enameled material. Such applications impose stringent requirements on tool design and maintenance, and lubrication sometimes cannot be used because of the nature of the coating or because of end-use requirements.

Deep Drawing

Equipment, tools, and techniques used for deep-drawing aluminum and aluminum alloys are similar to those used for other metals, and are described in more detail in the article "Deep Drawing" in this Volume. This section deals with those aspects of deep drawing that are specific to aluminum alloys, and is restricted to procedures using a rigid punch and die. Other procedures are described in subsequent sections of this article.

Equipment. Punch presses are used for nearly all deep drawing; press brakes are sometimes used for experimental or very short runs. Presses used for steel are also suitable for aluminum.

Capacity requirements, determined by the same method used for steel, are generally lower for comparable operations because of the lower tensile strength of aluminum alloys.

Press speeds are ordinarily higher than they are for steel. For mild draws, single-action presses are usually operated at 27 to 43 m/min (90 to 140 ft/min). Double-action presses are operated at 12 to 30 m/min (40 to 100 ft/min) for mild draws, and at less than 15 m/min (50 ft/min) for deeper draws with low and medium-strength alloys. Drawing speeds on double-action presses are about 6 to 12 m/min (20 to 40 ft/min) with high-strength alloys.

Tool Design. Tools for deep drawing have the same general construction as those used with steel, but there are some significant differences. Aluminum alloy stock must be allowed to flow without undue restraint or excessive stretching. The original thickness of the metal is changed very little. This differs from the deep drawing of stainless steel and brass sheet; each of which may be reduced by as much as 25% in thickness in a single draw.

Clearances between punch and die are usually equal to the metal thickness plus about 10% per side for drawing alloys of low or intermediate strength. An additional 5 to 10% clearance may be needed for the higher-strength alloys and harder tempers.

With circular shells, metal thickening occurs with each draw, therefore clearance is usually increased with each successive draw. The restrictions imposed on the drawing of rectangular shells by metal flow at the corners make equal clearances for each draw satisfactory. The final operation with tapered or rectangular shells serves primarily to straighten walls, sharpen radii, and size the part accurately. Therefore, the clearance for these operations is equal to the thickness of the stock.

Excessive clearance may result in wrinkling of the sidewalls of the drawn shell. Insufficient clearance burnishes the sidewalls and increases the force required for drawing.

Radii on Tools. Tools used for drawing aluminum alloys are ordinarily provided with draw radii equal to four to eight times the stock thickness. A punch nose radius is sometimes as large as ten times the stock thickness.

A die radius that is too large may lead to wrinkling. A punch nose radius that is too sharp increases the probability of fracture or of residual circular shock lines that can be removed only by polishing.

Nonetheless, failure by fracture can sometimes be eliminated by increasing the die radius, or by making the drawing edge an elliptic form instead of a circular arc.

Surface Finish on Tools. Draw dies and punches should have a surface finish of 0.4 μm (16 μin.) or less for most applications. A finish of 0.08 to 0.1 μm (3 to 4 μin.) is often specified on high-production tooling for drawing light-gage or precoated stock. Chromium plating may also be specified to minimize friction and prevent pickup of dirt or other particles that could damage the finish on the part.

Tool Materials. The selection of materials for deep drawing tools is discussed in the article "Selection of Material for Deep-Drawing Dies" in this Volume. Materials for small dies are chosen almost entirely on the basis of performance, but cost becomes a significant factor for large dies. Local variation in wear on tools is an important factor in tool life. A twentyfold variation in rate of wear can be observed on the die radius.

Lubricants for deep drawing of aluminum alloys must allow the blank to slip readily and uniformly between the blankholder and the die, and must prevent stretching and galling while this movement takes place.

The drawing compounds can be applied only to the areas that will be subjected to a significant amount of cold working, unless local application interferes with the requirements of high-speed operation. Uniformity of application is critical, especially to enable the maintenance of correct blankholder pressure around the periphery of the die.

Drawing Limits. The reduction in diameter that is possible in a single operation with aluminum alloys is about the same as that obtainable with drawing-quality steel. For deep-drawn cylindrical shells, reductions in diameter of about 40% for the first draw, 20% for the second draw, and 15% for the third and subsequent draws can be obtained with good practice. The part can usually be completely formed without intermediate annealing. Four or more successive draws without annealing can be performed, with proper die design and effective lubrication, on such alloys as 1100, 3003, and 5005. The amount of reduction decreases in successive draws because of the loss in workability due to strain hardening. The total depth of draw thus obtainable without intermediate annealing exceeds that obtainable from steel, copper, brass, or other common materials.

For high-strength aluminum alloys, the approximate amount of permissible reduction is 30% for the first draw, 15% for the second draw, and 10% for the third draw.

Local or complete annealing is usually necessary after the third draw on alloys such as 2014 and 2024. Alloys 3004, 5052, and 6061 are intermediate in behavior.

The rate of strain hardening is greatest for the high-strength alloys and least for the low-strength alloys. Table 3 shows the changes in mechanical properties that result from successive draws with alloys 3003 and 5052. The major portion of the change is accomplished in the first draw. The rate of strain hardening is more rapid with high-strength heat-treatable alloys such as 2014 and 2024.

Table 3 Effect of drawing on mechanical properties of aluminum alloys 3003 and 5052

Number of draws	**Tensile strength**		**Yield strength**		Elongation in 50 mm (2 in.), %
	MPa	**ksi**	**MPa**	**ksi**	
Alloy 3003					
0	110	16	41	6	**30**
1	131	19	117	17	**11**
2	152	22	145	21	**9**
3	162	23.5	152	22	**8**
4	169	24.5	155	22.5	**8**
[a]	(200)	(29)	(186)	(27)	**(4)**
Alloy 5052					
0	193	28	90	13	**25**
1	238	34.5	221	32	**6**
2	272	39.5	248	36	**6**

3	296	43	255	37	**6**
4	303	44	262	38	**6**
(a)	**(290)**	**(42)**	**(255)**	**(37)**	**(7)**

(a) Values in parentheses are typical values for these alloys in the full hard condition.

Practical limits for single-operation deep drawing of cylindrical cups and rectangular boxes have been expressed in terms of dimensional ratios, as shown in Fig. 6. (Reverse redrawing can be used to obtain a deeper shell than indicated by the limits in Fig. 6 for conventional drawing methods.)

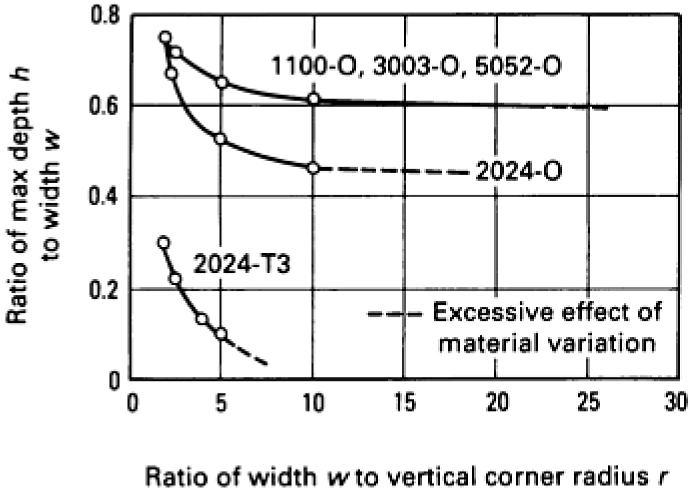

Fig. 6 Drawing limits for single-operation drawing of cylindrical cups or rectangular boxes from aluminum alloy sheet 0.66 to 1.63 mm (0.026 to 0.064 in.) thick. For cylindrical cups, width *w* equals diameter and vertical corner radius *r* equals half the diameter. For rectangular boxes, width *w* equals the square root of the projected bottom area. If length is more than three times width, drawing limits will be more severe than those shown above. For flanged boxes, the flange width must be included in depth *h*.

The relation of the metal thickness t to the blank diameter D is an important factor in determining the percentage reduction for each drawing operation. As this ratio decreases, the probability of wrinkling increases, requiring more blankholding pressure to control metal flow and prevent wrinkles from starting. Figure 7 shows the effect of this ratio on percentage reduction of successive draws, without intermediate annealing, for low-strength alloys such as 3003-O.

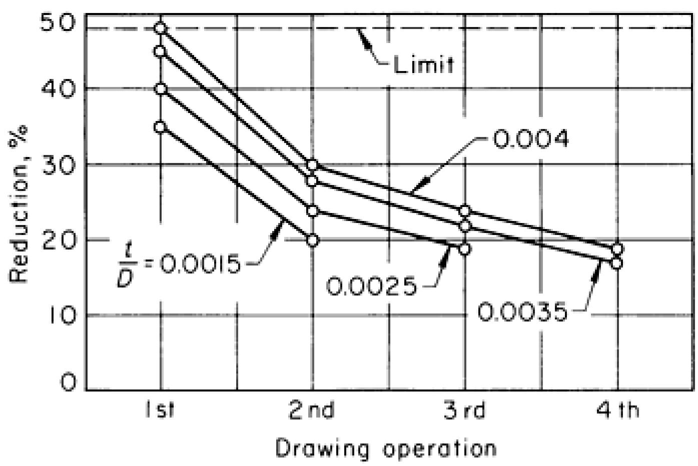

Fig. 7 Effect of thickness-to-diameter ratio on percentage of reduction for successive drawing operations without intermediate annealing for low-strength aluminum alloys such as 3003-O. *t*, metal thickness. *D*, blank

diameter

Blank development is of particular importance in the deep drawing of large rectangular and irregular shapes. Excessive stock at the corners must be avoided, because it hinders the uniform flow of metal under the blankholder and thus leads to wrinkles or fractures.

With suitable tooling and careful blank development, large rectangular and irregular shapes can often be produced economically in large quantities by deep drawing. Smaller quantities are made in sections with inexpensive tooling and then assembled by welding. Both the welding operation and the subsequent grinding and polishing of the weld areas are time consuming and costly.

Warping. The nonuniformity of stress distribution in the drawing of rectangular or irregular shapes increases the tendency toward warping. Bowing or oilcan effects on the major surfaces become more pronounced with increasing size of the part. Changes can sometimes be made in dimensional details of the drawing tools to eliminate these defects without the need for extra forming operations.

Miscellaneous Shapes. Other shapes often produced by deep drawing (besides cylindrical and rectangular shells) include hemispherical shells, flat-bottom hemispherical shells, and tapered shells.

Hemispherical shells with a final inside diameter of less than about 150 times the original metal thickness can be drawn in one operation. For inside diameters of more than 150 times thickness, two draws are usually required, to avoid wrinkles. Local thinning in the first draw must be avoided if the second draw is to be successful.

Flat-bottom hemispherical shells, unless very shallow, require at least two draws. The first draw produces a rounded shape, with a larger radius in the bottom area than on the side areas. The final draw flattens the bottom and gives the sides a uniform curvature of the radius required.

Tapered shells require more drawing operations for a given depth of draw than do most other symmetrical shapes. The number of steps required increases with the taper angle.

The bottom edges, except for the final operation, do not have the contour of a circular arc. The profile consists of essentially flat sections at an angle of about 40 to 50° from the horizontal. Stepwise reductions are made along the line of final contour, as shown in Fig. 8, and the final draw straightens out the sidewalls to the desired shape.

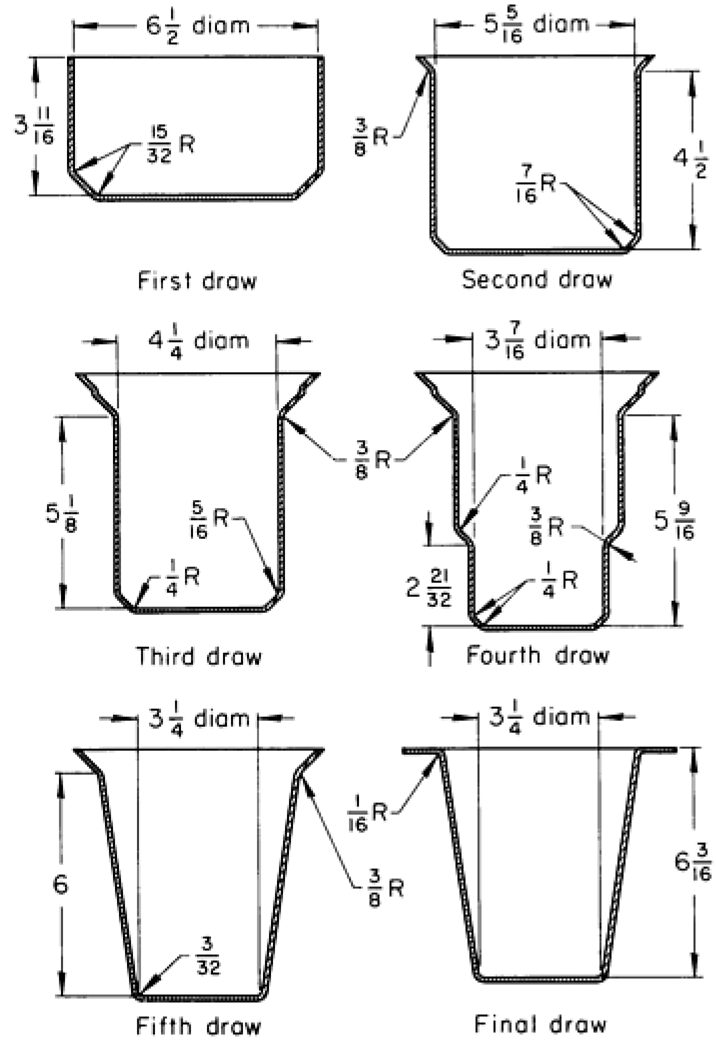

Fig. 8 Typical progression of shapes in multiple-draw forming of a tapered shell from an aluminum alloy blank 1.63 mm (0.064 in.) thick and 292 mm ($11\frac{1}{2}$ in.) in diameter. Dimensions given in inches

Each operation after the first is restricted to a shallow draw to minimize strain hardening. With alloys of low and intermediate strength, this procedure makes it possible to complete the series of draws without annealing. Contrary to normal practice, the amount of reduction per draw need not be lowered after the second draw. However, polishing or burnishing is often required on the completed shell to obtain a good-quality finish on the sidewalls.

Ironing is avoided in some deep drawing applications with aluminum alloys, but can be used to produce a shell with a heavy bottom and thin sidewalls. The shell is first drawn to approximately the final diameter. The drawing lubricant is then removed, and the shell is annealed, bringing it to temperature rapidly to minimize the formation of coarse grains in areas that have been only slightly cold worked.

The sidewalls can then be reduced in thickness by 30 to 40% in an ironing operation. By repeating the cleaning, annealing, and ironing steps, an additional reduction of 20 to 25% can be obtained, with good control over wall thickness.

A typical use of ironing is shown in Fig. 9. Here a cylindrical shell is produced with a thick bottom and thin sidewalls by a single deep draw and two successive ironing operations. The approximate final diameter and about half the final depth are obtained in the drawing operation. Wall thickness is reduced 33% in the first ironing step and 19% in the second.

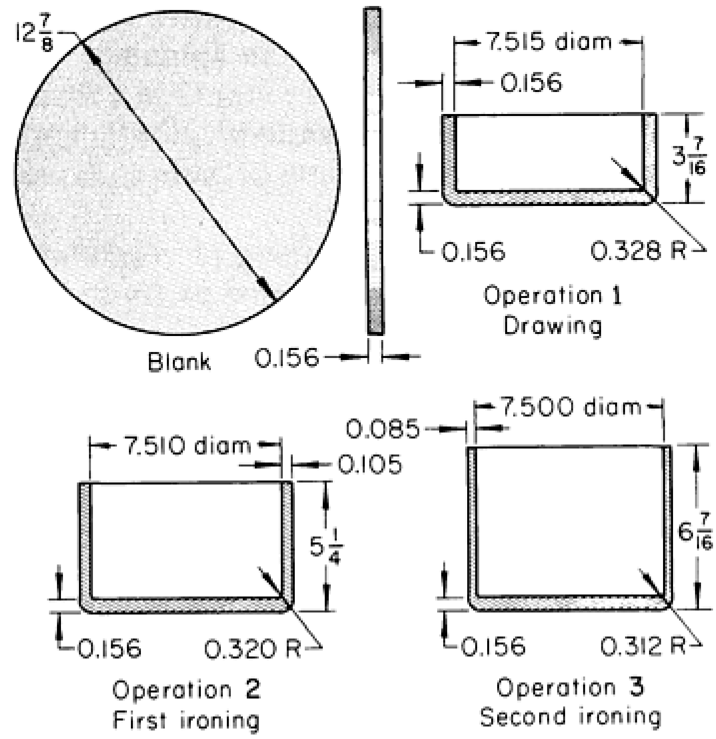

Fig. 9 Progression of shapes in production of a shell with a thick bottom and thin sides in one draw and two ironing operations. Dimensions given in inches

Hot Drawing. Severe drawing operations are often impossible to perform at room temperature on large and relatively thick shapes made from high-strength aluminum alloys. However, the lower strength and increased ductility at temperatures above the recrystallization point of the alloy make it possible to produce large and relatively thick shapes by hot drawing. There is little or no advantage when stock is less than 3.2 mm (0.125 in.) thick. Alloys frequently used in applications of this type include nonheat-treatable alloys 5083, 5086, and 5456, and heat-treatable alloys 2024, 2219, 6061, 7075, and 7178.

Heavy-duty presses and related equipment are required. Drawing temperatures range from 175 to 315 °C (350 to 600 °F). The length of time the workpiece is held at temperature is controlled to avoid excessive grain growth in areas with little strain hardening. Ordinary drawing compounds break down or burn at elevated temperature, and are not suitable for hot-drawing operations.

Graphite-containing tallow and hard yellow naphtha soap have sometimes been used as lubricants at intermediate elevated temperature. Lubricants that remain stable above 260 °C (500 °F) include graphite and molybdenum disulfide. These materials can be used in the colloidal form with a volatile vehicle, mixed with other lubricants, or applied to the die as powders.

Spinning

Spinning is often used for the forming of aluminum alloy shapes that are surfaces of revolutions. The manual lathes, automatic spinning machines, chucks, and tools used for aluminum alloys are essentially the same as those used for steel and the other metals commonly formed by spinning (see the article "Spinning" in this Volume).

Hand-spinning lathes and simple tools are suitable for forming aluminum alloy blanks 0.51 to 2.05 mm (0.020 to 0.081 in.) thick; with proper care, stock as thin as 0.10 mm (0.004 in.) can be spun. For thicker and larger blanks, auxiliary equipment is used to apply pressure to the workpiece. This equipment varies from a simple scissors arrangement to feed screws for controlling tool advance; pressure against the work is provided by air or hydraulic cylinders.

Blanks up to 6.4 mm ($\frac{1}{4}$ in.) thick can usually be spun at room temperature. For greater thicknesses, semimechanical to fully mechanical equipment is used, and the work metal is heated. Work metal 25 mm (1 in.) or more in thickness requires special heavy-duty machines and hot spinning.

Aluminum alloy parts 76 mm (3 in.) thick have been spun experimentally. Equipment is available for the spinning of parts as large as 5 m (16 ft) in diameter.

Tolerances for the spinning of aluminum alloys are essentially the same as those for other common metals.

Alloys. A number of aluminum alloys are widely used in spinning applications. Desirable properties are ductility, relatively low ratio of yield strength to ultimate strength, low rate of work hardening, and small grain size.

The alloys of low and intermediate strength that are spun most frequently include 1100, 2219, 3003, 3004, 5052, 5086, and 5154. Annealed blanks are generally used for severe forming; however, a harder temper is sometimes preferred, if it is sufficiently formable, to avoid a tendency to ball up ahead of the tool. A harder temper also may be used when forming is not severe enough to give the product its necessary strength by work hardening.

Heat-treatable alloys used for high strength in the finished part are 2014, 2024, and 6061. If the forming is extensive, these alloys often must be annealed several times during spinning, or they may be spun hot.

One method used frequently for spinning heat-treatable alloys is:

- Spin annealed blank to approximate form
- Solution heat treat and quench
- Spin to final form at once, before appreciable age hardening

If spinning to the final form cannot be done after solution heat treating and quenching, the quenched parts should be placed in a refrigerator, or packed in dry ice, and held as close to -20 °C (0 °F) as possible until they can be spun. The parts are aged to the T6 temper after spinning has been completed.

Typical spindle speeds for spinning flat blanks and drawn shells of various diameters are listed in Table 4. Rotational speed is decreased as blank diameter increases, so that peripheral speed is maintained in the same range regardless of the size of the workpiece. Peripheral speed ordinarily averages about 915 m/min (3000 ft/min) for aluminum alloys. This is somewhat faster than the speeds normally used in spinning copper, brass, stainless steel, and low-carbon steel.

Table 4 Typical spindle speeds for the spinning of aluminum alloy flat blanks

Blank diameter		Spindle speed, rpm
m	**in.**	
Flat blanks		
Up to 0.3	Up to 12	**600-1100**
0.3-0.6	12-24	**400-700**
0.6-0.9	24-36	**250-550**
0.9-1.8	36-72	**50-250**
1.8-3.0	72-120	**25-50**
3.0-4.5	120-180	**12-25**
4.5-5.3	180-210	**12**
Drawn shells		
0.25-0.35	10-14	**1000-1200**
0.35-0.50	14-20	**650-800**
0.50-0.75	20-30	**475-550**
0.75-1.0	30-40	**325-375**

1.0-1.3	40-50	**250-300**
1.3-1.8	50-70	**200-210**
1.8-2.3	**70-90**	**150-175**

Lubricants are needed in nearly all spinning operations. Beeswax, tallow, and petroleum jelly are suitable for most small parts. Hard yellow naphtha soap is an effective lubricant for larger workpieces. Colloidal graphite in kerosene, or compounds containing molybdenum disulfide, are used in hot spinning. Lubricating compounds used must be easily removable from the finished part without costly treatments.

Applications. Parts produced from aluminum alloys by spinning include tumblers, pitchers, bowls, cooking utensils, ring molds, milk cans, processing kettles, reflectors, aircraft and aerospace parts, architectural sections, tank heads, and streetlight standards.

Spinning is often selected in preference to drawing when quick delivery of small quantities is important, because the spun parts can usually be delivered before drawing tools have been made. Cones, hemispheres, tapered shapes, and parts with complex or reentrant contours (if surfaces of revolution) are often more readily formed by spinning than by other methods. Spinning is also used for very large parts when suitable press equipment and tools are not readily available or are too costly.

Spinning is not usually economical for quantities of more than 5000 to 10,000 pieces because of comparatively low production rates and resulting high unit labor costs. There are exceptions, especially in the power spinning of truncated cone-shape parts having included angles of 40° or more. Spinning is capable of producing such parts at lower cost than deep drawing, it gives a uniform wall thickness and a surface free from wrinkles, and it increases the tensile strength of the work metal by as much as 100%.

Stretch Forming

Almost all of the aluminum alloys can be shaped by stretch forming. In this process, the work metal is stretched over a form and stressed beyond its yield point to produce the desired contour (for a detailed description, see the article "Stretch Forming" in this Volume).

Typical shapes produced by stretch forming are shown in Fig. 10. These include large shapes with compound curvature formed by longitudinal and transverse stretching of sheet, and compound bends or long, sweeping bends formed from extrusions.

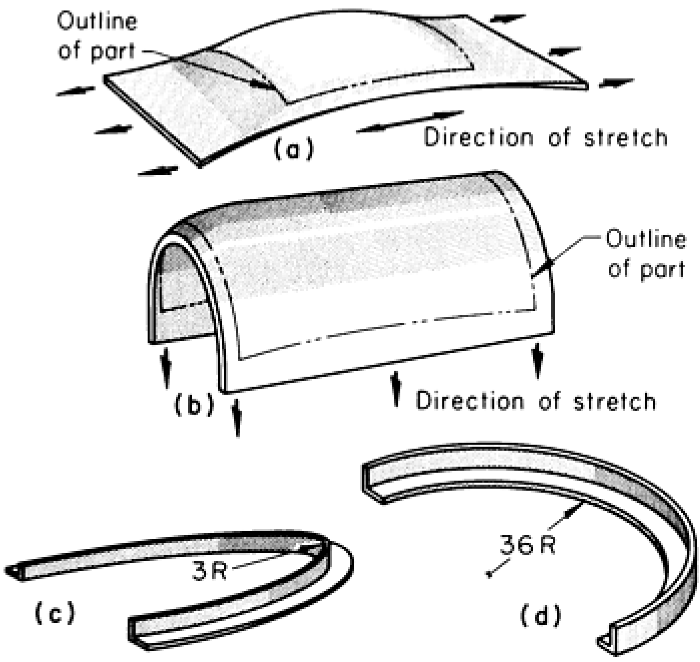

Fig. 10 Typical stretch-formed shapes. (a) Longitudinal stretching. (b) Transverse stretching. (c) Compound bend from extrusion. (d) Long, sweeping bend from extrusion. Dimensions given in inches

Alloys. Properties desirable for stretch forming are high elongation, wide forming range (spread between yield strength and tensile strength), toughness, and fine grain structure. Table 5 shows the effect of elongation and forming range on stretchability ratings for the alloys most commonly used in stretch forming. The stretchability rating varies directly with the forming range, except for 6061-W (which has somewhat higher elongation than adjacent alloys) and 7075-T6 (which has by far the lowest elongation listed). Alloys 1100-O and 3003-O, with the highest elongation shown, nevertheless are less desirable for stretch forming than are the alloys above them in the list. Their low strength and the narrow spread between yield strength and tensile strength make them particularly susceptible to local necking and premature failure in stretch forming.

Table 5 Mechanical properties and stretchability ratings for aluminum alloys most commonly used in stretch forming

Alloy	**Tensile strength**		**Yield strength**		**Forming range**[(a)]		**Elongation in 50 mm (2 in.), %**	Stretchability rating[(b)]
	MPa	**ksi**	**MPa**	**ksi**	**MPa**	**ksi**		
7075-W[(c)]	331	48	138	20	193	28	19	**100**
2024-W[(c)]	317	46	124	18	193	28	20	**98**
2024-T3	441	64	303	44	138	20	18	**95**
6061-W[(c)]	241	35	145	21	97	14	22	**90**
7075-O	221	32	97	14	124	18	17	**80**
2024-O	186	27	76	11	110	16	19	**80**
6061-O	124	18	55	8	69	10	22	**75**
3003-O	110	16	41	6	69	10	30	**75**
1100-O	90	13	35	5	55	8	35	**70**
7075-T6	**524**	**76**	**462**	**67**	**62**	**9**	**11**	**10**

(a) Tensile strength minus yield strength.

(b) Relative amount of stretch permissible in stretch forming, based on 7075-W as 100.

(c) Freshly quenched after solution heat treatment

Tools. The materials used for the form block or die depend on the production quantities required, the severity of local stress and wear on the die, and the thickness and wear properties of the alloy to be formed. Materials include wood, plastics, faced concrete, cast zinc alloys, aluminum tool and jig plate, cast iron, and (rarely) steel or chromium-plated steel.

Lubricants are recommended in the stretch forming of aluminum alloys. Water-soluble oils are commonly used, with viscosity dependent on the severity of forming. Calcium-base greases, paraffin, beeswax, and commercial waxes also are used. The application of too much lubricant can result in buckling of the workpiece.

Sometimes a layer of sheet rubber, glass cloth, or plastic between die and workpiece serves as a lubricant. Because of their inherent lubricity, zinc alloy dies require only a minimum of lubrication. Smooth-surface plastic dies may require no lubrication, because of their low coefficient of friction against aluminum.

Applications. The various stretch-forming techniques (including stretch drawing, stretch wrapping, and compression and radial drawing) are used extensively in the aerospace industry. Typical parts produced include wing-skin and fuselage panels, engine cowlings, window and door frames, and trim panels used in aerospace, automotive, architectural, and appliance industries.

Stretch draw forming of aluminum is done using both the matched-die and form block techniques. The matched-die method uses a single-action hydraulic press equipped with a means of closing and moving the jaws that grip each end of the blank. The punch is attached to the bed of the press, and the die is attached to the ram.

The alternate method uses a form block that is attached to a stationary bed or a hydraulic cylinder. With this method, the blank is gripped with jaws that hold it in tension or draw it over the form block.

Stretch wrapping uses a form block that is bolted to a rotary table. One end of the blank is clamped to the form block or to a table-mounted gripper. A hydraulic cylinder or a gripper applies tension to the other end of the blank while the form block revolves into it with the turning of the table.

Shaped form blocks that match the contour of extruded or rolled sections are used for support during forming. Filler strips, either segmented or made of low-melting alloys or strips of aluminum, are used to prevent the collapse of sections.

Radial-draw forming is a combination of stretch wrapping and compression forming. The workpiece is pressed against the form block by a roller or shoe while being wrapped around the turning form block. This method can be used, for example, to form a flange to a compound curvature while forming a leg, as in the part shown in Fig. 11.

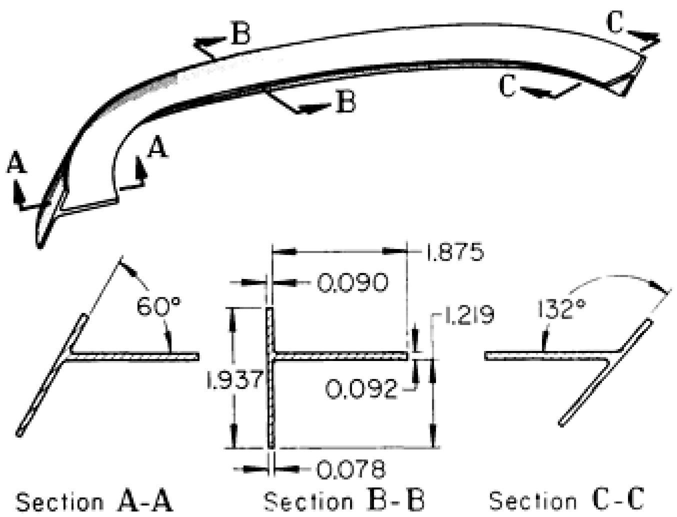

Fig. 11 Aluminum alloy 7075-O radial draw formed T-section with radical changes in angle between leg and flange. Dimensions given in inches

Rubber-Pad Forming

Aluminum alloys are formed by several techniques that can be classified as rubber-pad forming. A general description of processes, equipment, tools, and applications is given in the article "Rubber-Pad Forming" in this Volume.

Alloys for rubber-pad forming are selected on the same basis as they are selected for similar bending or deep-drawing operations. With nonheat-treatable aluminum alloys, the temper that will meet the forming requirements and give the maximum strength in unworked areas is usually chosen.

Heat-treatable aluminum alloys ordinarily are either formed in the annealed temper and then solution heat treated or formed in the freshly quenched W temper.

Tool materials are usually masonite for short runs and aluminum alloy, zinc alloy, or steel for longer runs. Several types of rubber have been used as the pad material. Certain grades of rubber have particularly good resistance to oils and forming lubricants and are available in a range of hardness, tensile strength, and deflection characteristics to meet different forming requirements.

Capabilities. A given alloy and temper can sometimes be formed more severely by rubber-pad forming than with conventional tools because of the multidirectional nature of the force exerted against the workpiece. Also, the variable radius of the forming pad assists in producing a more uniform elongation of the workpiece than in conventional forming operations.

Forming the shallow part shown in Fig. 12 with a rubber pad and a rigid female die used the variable radius to advantage. The development of wrinkles was almost eliminated, because the rubber acted as a blankholder and kept the work in contact with the flat and contoured die surfaces as the drawing progressed. A drawing compound was used on the blank.

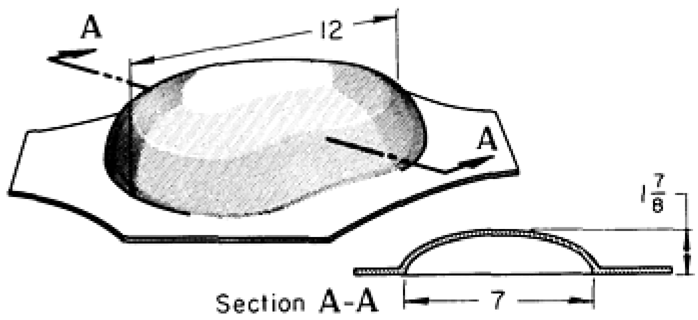

Fig. 12 Shallow part that was drawn from aluminum alloy 6061-O with a rubber pad and a rigid female steel die, in one operation. Dimensions given in inches

Limitations. The simpler types of rubber-pad forming have relatively low production rates and correspondingly high unit labor costs compared with punch-press operations. However, the hydroforming (fluid-forming) process is adaptable to automatic loading equipment and thus has fairly high production rates.

Applications. Rubber-pad forming is widely used in the aerospace industry, especially for structural parts and skin components. Products made in other industries include appliance parts, license plates, numerals, lighting reflectors, skin panels for buildings, moldings, utensils, and parts drawn from prefinished sheet.

Most rubber-pad forming is done on material 1.6 mm ($\frac{1}{16}$ in.) or less in thickness, with only a small percentage being thicker than 6.4 mm ($\frac{1}{4}$ in.). However, aluminum alloy parts 15.8 mm ($\frac{5}{8}$ in.) thick have been formed in special heavy-duty equipment of the rubber-diaphragm type.

Some bulkheads and brackets have both straight and curved flanges with joggles at both ends. The form blocks for such parts are sometimes interchangeable between the Guerin and Verson-Wheelon rubber-pad processes (see the article "Rubber-Pad Forming" in this Volume). Handwork is usually necessary to set the joggles and to smooth minor buckling in the shrink flanges.

The simultaneous blanking and piercing of flat stock can also be done with rubber-pad tooling. This type of operation is limited to aluminum alloy sheet no thicker than about 1.63 mm (0.064 in.).

The control of metal movement that can be obtained with rubber-pad forming not only permits more severe forming than do conventional tools, but also is applicable to beading operations. Beads are frequently used to obtain rigidity on large surfaces without increasing the metal thickness.

With a conventional steel punch, die, and blankholder, metal is moved from the edges of the workpiece toward the bead, making the edges somewhat concave, and sometimes producing warpage or oilcan effects. Some movement of metal toward the formed area is usually desirable, in order to prevent excessive thinning or cracking of the beads. In the forming of some parts, however, it may be necessary to restrict metal movement to the immediate vicinity of the beads.

The deep-drawing capabilities of rubber-pad processes vary with the different types of equipment. The severity of drawing possible with heavy-duty rubber-pad drawing by the Marform process (see the article "Rubber-Pad Forming" in this Volume) is compared below with that possible in conventional drawing. The comparison is based on the drawing of alloys 1100-O and 3003-O.

Drawing severity	**Reduction in diameter, %**	Ratio of depth to diameter
Rubber-pad drawing		
Typical	57	**1.1**
Maximum	72	**3.0**
Conventional drawing		
Maximum	**40**	**0.45**

Forming of Aluminum Alloys

Superplastic Forming (Ref 6)

Superplastic behavior has been demonstrated in several aluminum alloys, including the high-strength alloy 7475. The prime material requirement for superplasticity--a fine, stable grain size--can be achieved in aluminum alloys by either static or dynamic recrystallization. In static recrystallization, a deformed microstructure is allowed to undergo discontinuous recrystallization during static annealing, leading to a fine-grain microstructure at the start of superplastic forming. In dynamic recrystallization, a deformed microstructure undergoes gradual, continuous recrystallization and grain refinement in the course of superplastic forming.

The microstructures of superplastic aluminum alloys can be either dual-phase or essentially a single phase with very small amounts of second phase present. Some amount of second phase is always necessary to develop and stabilize a fine-grain structure.

Table 6 lists the nominal compositions of several superplastic aluminum alloys, their typical grain sizes, and selected mechanical properties. For comparison, the elongations and yield strengths of aluminum alloys 1100-O and 2024-T3 are also shown.

Table 6 Nominal compositions, typical grain sizes, and selected mechanical properties of several superplastic aluminum alloys

Elongation and yield strength of aluminum alloys 2024-T3 and 1100-O are shown for comparison.

Alloy	**Nominal composition, %**	**Grain size**		**Tensile elongation, %**	**Room-temperature yield strength**[b]		Reference
		μm	**μin.**		**MPa**	**ksi**	
Al-33Cu	Al-33Cu	3-4	120-160	400-1000[a]	186	27	**7**
08050	Al-5Ca-5Zn	1-2	40-80	600[a]	152	22	**8**
Al-8.5Zn-1.25Mg-0.3Zr	Al-8.5Zn-1.25Mg-0.3Zr	8	320	1500[a]	. . .	. . .	**9**
7475	Al-5.8Zn-1.6Cu-2.3Mg-0.22Cr	10-14	400-560	800-1200[a]	483	70	**10**
Supral 100	Al-6Cu-0.4Zr	2-3[c]	80-120	1000[a]	283	41	**11**
Supral 220	Al-6Cu-0.35Mg-0.1Ge-0.1Si	2-3[c]	80-120	900[a]	448	65	**12**
Aluminum-lithium	Al-2.5Li-1.2Cu-0.6Mg-0.13Zr	2-5[c]	80-200	800[a]	469	68	**13, 14**
2024-T3	Al-4.4Cu-1.5Mg-0.6Mn	. . .	. . .	18	345	50	**. . .**
1100-O	**99.00 min Al**	**. . .**	**. . .**	**35**	**90**	**13**	**. . .**

Source: Compiled from Ref 6

(a) Determined at optimal strain rate and temperature for the specific material.

(b) In aged condition whenever applicable.

(c) In dynamically recrystallized condition.

Superplasticity in Aluminum Alloy 7475. The high strength and high fracture toughness of alloy 7475 are the main reasons for examining superplasticity in this material. A number of grain refinement methods have been developed for 7*xxx* series aluminum alloys; of these, the Rockwell method (Ref 15) has been relatively easy to implement. A schematic of the Rockwell grain refinement process is shown in Fig. 13. A critical aspect of the process is the heating rate in recrystallization, which must be extremely rapid in order to activate simultaneously as many nuclei as possible.

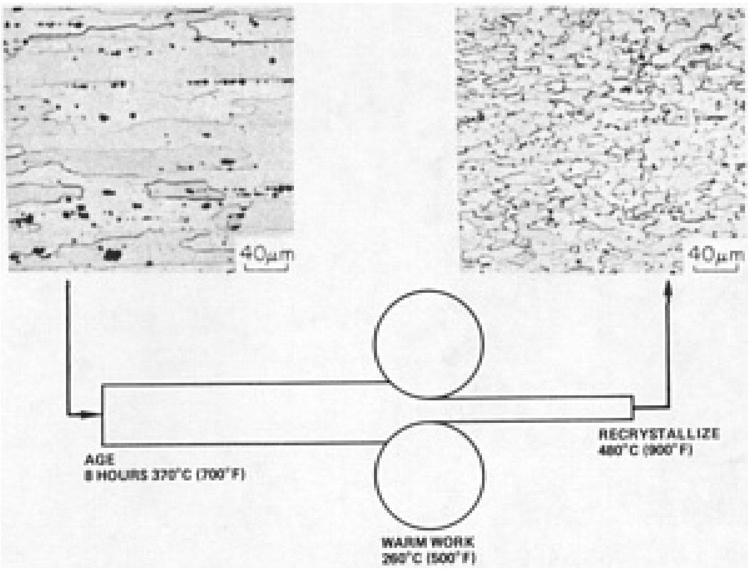

Fig. 13 Steps involved in thermal and mechanical processing to produce superplastic aluminum alloy 7475. Source: Ref 15

Stress versus strain rate plots and corresponding values of strain rate sensitivity index *m* for alloy 7475 are shown in Fig. 14. The effect of grain size on flow stress and *m* value is apparent: Flow stress increases with grain size, while *m* decreases. At a grain size of 10 to 14 μm (400 to 560 μin.) and a strain rate of 2×10^{-4} s^{-1}, flow stresses are very low (~ 690 kPa, or 100 psi). Peak *m* values are very high (~0.8). Flow stress at the same strain rate but a grain size of 40 μm (1600 μin.) is more than doubled to about 1380 kPa (200 psi); peak *m* value has decreased to approximately 0.7.

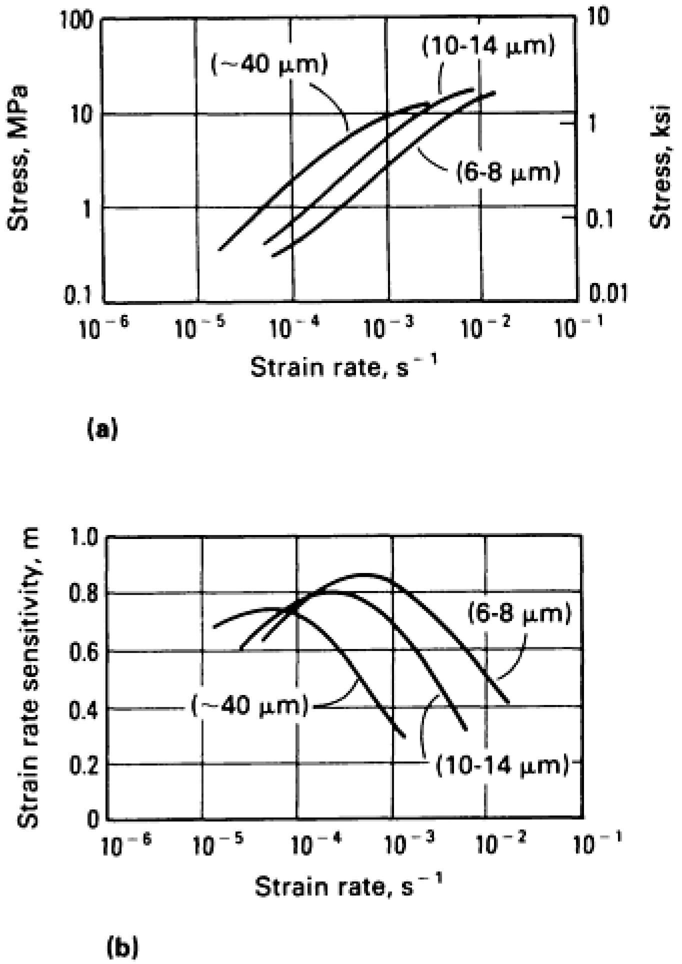

Fig. 14 Stress versus strain (a) and corresponding strain rate sensitivity *m* (b) for superplastic aluminum alloy 7475 in three different grain sizes. Tests were performed at 516 °C (960 °F), the optimal forming temperature for alloy 7475. Source: Ref 6

Superplastic Forming Processes. A number of processes are used for superplastic forming, including blow forming, vacuum forming, thermoforming, deep drawing, and dieless drawing. All of these processes are discussed in detail in the article "Superplastic Sheet Forming" in this Volume. Information on superplastic forming of titanium alloys also is discussed in the article "Forming of Titanium and Titanium Alloys" in this Volume.

Cavitation (formation of internal microvoids during superplastic forming) is a problem in most superplastic aluminum alloys. Many factors, including alloy cleanliness, grain size, flow stress, strain rate, forming temperature, and hydrostatic pressure, influence cavitation in aluminum alloys. Factors that increase the flow stress of the alloy--including large grain size (or excessive grain growth), high applied strain rate, and low forming temperature--increase the tendency toward cavitation.

Cavitation can be reduced by imposing a pressure on the back side of the sheet during forming (Fig. 15). The forming pressure must be higher than this back pressure. The same forming rates can be achieved with or without back pressure. Back pressures of 690 to 3450 kPa (100 to 500 psi) are generally suitable for suppressing cavitation. A die apparatus used to provide back pressure during forming is illustrated in Fig. 15.

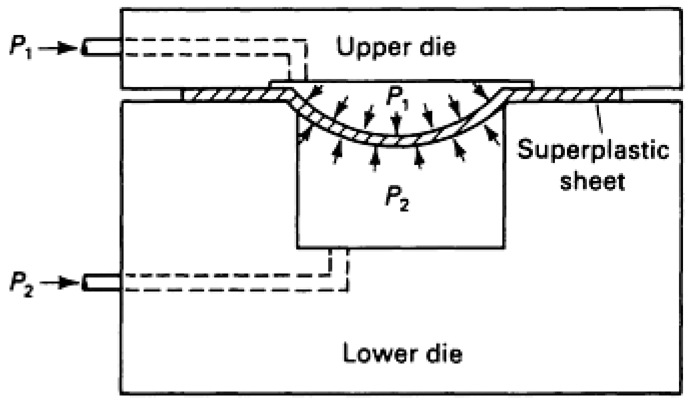

Fig. 15 Die apparatus for providing back pressure during superplastic forming to suppress cavitation. P_1, forming pressure; P_2, back pressure. Source: Ref 6

Applications. The use of superplastically formed aluminum components in the aircraft industry is increasing. Figure 16 illustrates the cost and weight savings possible when conventionally fabricated components (in this case, an airframe member) are replaced by superplastically formed parts.

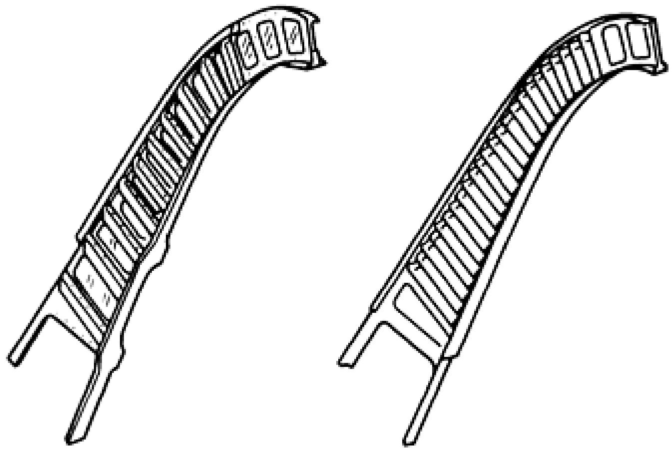

Fig. 16 Example of cost and weight savings obtainable using superplastic forming in the aircraft industry. Conventionally fabricated part (a) had 15 pieces and required 212 fasteners; the superplastically formed part (b) consists of 3 parts and requires 45 fasteners. This results in a 56% cost savings and a 13% weight savings.

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Explosive Forming

Explosive forming is one of the high-energy-rate forming (HERF) methods that are employed in the production of aluminum alloy parts, mainly in the aerospace industries. It is often used to produce parts whose size exceeds the limits of conventional equipment or whose thickness requires pressures not obtainable with conventional equipment. It is also used to form small quantities of complex parts that would be more costly to produce by conventional techniques.

Deformation velocities are several hundred feet per second, compared with 0.15 to about 6 m/s (0.5 to about 20 ft/s) for conventional forming processes. The time required for the workpiece to deform to its final shape is a few milliseconds, with working pressures of several thousand to several hundred thousand pounds per square inch. Water usually serves as the pressure medium.

Details of equipment, tools, and procedures used in explosive forming are available in the article "Explosive Forming" in this Volume.

Capabilities. Types of operations possible in explosive forming include panel forming (bending), piercing, flanging, shallow dishing, deep drawing, and cylindrical bulging. Part dimensions range from 25 mm (1 in.) to about 15 m (50 ft); work metal thicknesses range from several thousandths of an inch to about 152 mm (6 in.).

Alloys. The explosive forming process can be used with any aluminum alloy. Formability is a direct function of the ordinary tensile elongation values, but the function is different for each alloy, because of different strain-rate behavior. Alloy 1100-O is rated the most formable of all common metals by explosive forming.

Effect on Mechanical Properties. Changes in mechanical properties as a result of explosive free-forming operations are essentially the same as those observed with conventional forming techniques to produce the same part. Explosive forming in a die, however, often causes the metal to strike the die at extremely high velocity. The resulting high interface pressures can increase the yield and tensile strengths substantially. Forming capability is increased when critical forming velocities are exceeded.

Dies. Only a forming die or cavity is needed for explosive forming, because the shock wave acts as a punch. Some direction and concentration of the shock wave is obtained with suitably shaped and positioned reflectors.

Cast iron and cast steel are the most frequently used die materials. A variety of other materials and combinations of materials are used, depending on the impact of the shock wave workpiece against the die, the size of the die, dimensional tolerances on the part, and quantity of parts. These materials include low-melting cast alloys and plastics, reinforced concrete, concrete faced with plastic-glass composites, and high-impact steel.

The air between the workpiece and die cavity must be evacuated before forming, because the forming speed is so great that the air will be trapped between the workpiece and die rather than displaced, as in conventional press forming. Trapped air and excessive lubrication cause malformed areas. The vent holes for evacuating the air must be placed in noncritical areas; otherwise, marks will appear on the formed parts. In thinner parts, the forming force will pierce holes in the parts, with the vent hole acting as a piercing die. The surface finish of the die cavity is also important because it is reproduced in mirror image on the workpiece.

Lubricants, if used, are usually extreme-pressure (EP) types. Because of the high velocity and the extreme pressures of forming, excessive lubrication must be avoided. Dies of low-melting alloys or those with smooth surfaces require little or no lubrication.

Springback is of importance in die design. Increasing the explosive charge or reducing the standoff distance reduces springback. However, die wear is thereby increased and the more brittle die materials may fracture. A compromise is often required. Compensation is sometimes made for die wear by reducing charge size or increasing standoff distance to produce a controlled amount of springback and maintain dimensional tolerances.

Studies on alloy 2219 have shown springback to increase when sheet thickness decreases between 6.35 mm (0.250 in.) and 0.81 mm (0.032 in.), and also to increase substantially when a lubricant is applied. Incremental forming, on the other hand, has been observed to reduce the extent of springback. Draw radius, draw depth, and die material have shown no significant effect on springback behavior.

Examples of Applications. The forming of flat and moderately curved shapes has been one of the most useful applications of explosive forming. These have included parts ranging from small, detailed items a few square inches in area to large panels with areas in excess of 2.8 m^2 (30 ft^2).

The curved, corrugated panel shown in Fig. 17 was formed from alloy 2014 in the O, T4, and T6 tempers in a laminated epoxy-fiberglass die. The panel was formed in a single shot, using a detonating fuse as a source of energy.

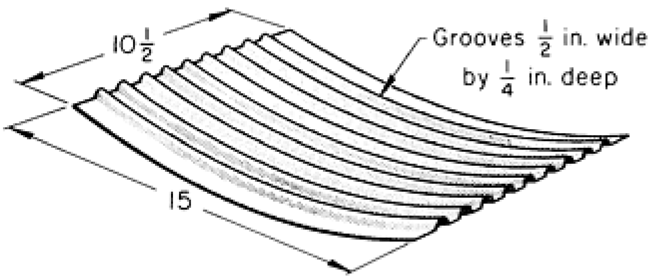

Fig. 17 Curved corrugated panel produced by explosive forming from aluminum alloy 2014 0.51 mm (0.020 in.) thick. Dimensions given in inches

Another example of an explosively formed aluminum part is the alloy 6061 instrument container shown in Fig. 18. The part was produced in a closed die using a hydraulic clamping system; tolerances were ±0.076 mm (0.003 in.).

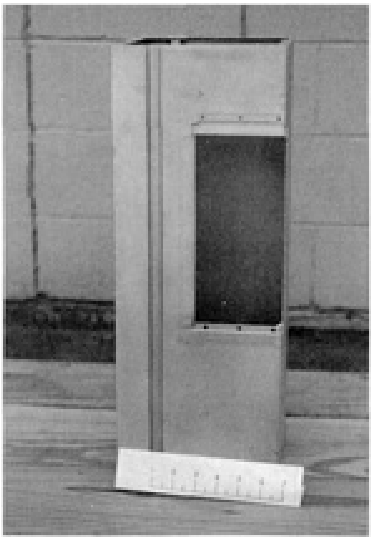

Fig. 18 Aluminum alloy 6061 instrument container fabricated from a blank by explosive forming. Courtesy of Explosive Fabricators, Inc.

Tubular parts also are readily shaped by explosive forming, using a length of detonating cord suspended along the axis of the tube.

Electrohydraulic Forming

Another high-energy-rate forming (HERF) method used in the fabrication of aluminum alloy parts is electrohydraulic forming (EHF). In this process, either a spark gap or an exploding bridgewire is employed to discharge electrical energy in water or another liquid. This generates an extremely high pressure and a shock wave similar to those produced in explosive forming. Once the energy is released in the transfer medium, the remainder of the operation is essentially the same as it is for explosive forming.

Capabilities of electrohydraulic forming differ somewhat from those of explosive forming. The spark gap method can apply programmed repetitive shock waves of varying magnitude without removal of the workpiece from the die.

The exploding-bridgewire method is less readily automated, but the shock wave can be localized and directed by the shape and placement of the wire.

Dimensional tolerances can be held to lower limits than with explosive forming, because the discharge of energy is more closely controlled. For this reason, electrohydraulic forming is sometimes used for a restrike or sizing operation after preliminary explosive forming to an approximate contour.

Commercial equipment is available that can produce about 3000 small- or medium-size pieces per week.

Examples of Applications. Electrohydraulic forming is well suited to the production of parts such as those shown in Fig. 19 and to the production of other transitional shapes in tubing. Both of the parts shown in Fig. 19 were originally fabricated by welding two drawn pieces, but the use of EHF resulted in considerable cost savings as well as in parts with closer tolerances and better surface finish.

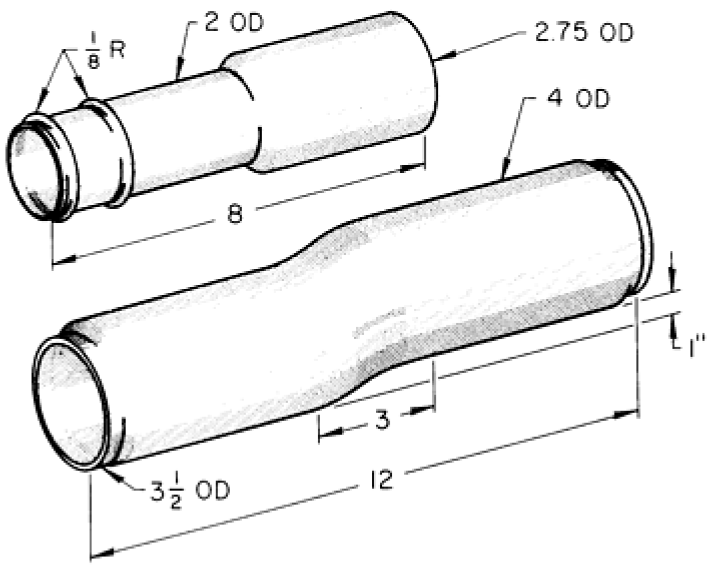

Fig. 19 Alloy 5052-O stepped tube and offset tube produced as one-piece units by electrohydraulic forming.

Electromagnetic Forming

Operations generally similar to those described for the preceding two HERF methods also can be carried out by electromagnetic forming. In this process, the discharge of a capacitor through a coil generates an intense magnetic field. This field interacts with the electric currents induced in a conductive workpiece to produce a force perpendicular to the workpiece surface.

Details of the process and of equipment, tools, and procedures are described in the article "Electromagnetic Forming" in this Volume. The method is suitable for aluminum alloys because of their formability and high electrical conductivity. Pressure-tight joints, electrically or thermally conductive joints, torque joints, and structural joints between metals can be produced by EMF techniques in a variety of shapes.

Examples of Applications. Electromagnetic forming is being used to attach an aluminum skirt to a machined bulkhead as part of an engine inlet mounting assembly for an aircraft (Fig. 20). The skirt is positioned over the bulkhead, and EMF is used to compress the skirt locally into a premachined configuration in the bulkhead.

Fig. 20 Aluminum alloy engine inlet mounting assembly for an aircraft before (right) and after (left) assembly by electromagnetic forming. Courtesy of Grumman Aircraft Systems

Welding and mechanical fasteners also were considered for this application. Welding was eliminated because of the large difference in section thicknesses being joined and the distortion that would accompany the welding operation. Mechanical fasteners were eliminated because of the increased stresses that would be caused by adding holes to the bulkhead. Joint integrity is of paramount importance because the completed assembly is used in the very front of an aircraft engine and any failure could result in ingestion of debris into the engine itself. The EMF joint meets all design requirements for the application. Another example of the use of EMF is in the joining of both ends to a tubular aluminum alloy 6063-T832 drive shaft (Fig. 21).

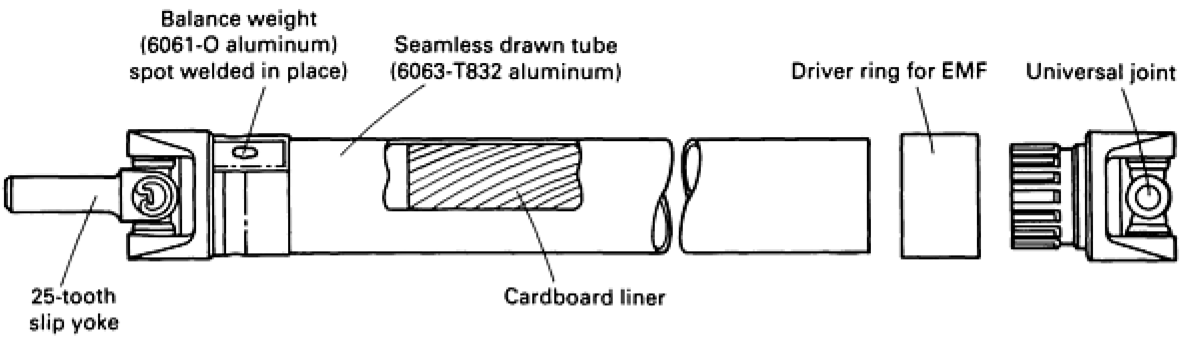

Fig. 21 Schematic of aluminum alloy 6063-T832 drive shaft with ends attached to drawn aluminum shaft by electromagnetic forming. Source: Ref 17

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Hydraulic Forming

True hydraulic forming by direct oil pressure against the surface of the workpiece has been applied to aluminum alloy flat stock. The process has been used mainly for the drawing of multiple beads on small quantities of large, flat sheets of thin material for aerospace applications. As shown in Fig. 22, a form block attached to the ram of the press holds the workpiece tightly against a selector plate, through which oil is introduced into channels at the bead locations.

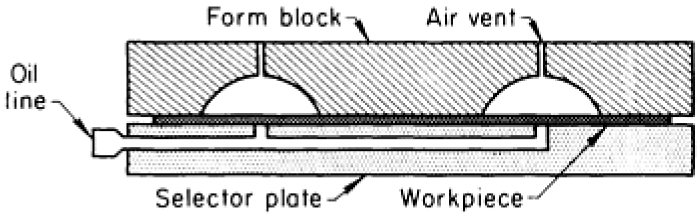

Fig. 22 Typical tooling setup for hydraulic forming of multiple beads in flat stoc.

In typical applications, up to 20 beads have been drawn in parts 510 to 760 mm (20 to 30 in.) wide by 1525 to 2030 mm (60 to 80 in.) long and about 0.30 mm (0.012 in.) thick, made from alloy 2024. Clamping force required is about 2.7 MN (300 tonf), and necessary forming oil pressure is about 6.9 MPa (1 ksi). Vents are provided in the form block to allow the escape of air from each bead cavity. The oil film left on the form block after each operation provides sufficient lubrication to draw the next part.

Forming by Shot Peening

The major application of shot peening is to increase the fatigue life of metal parts by producing a uniform compressive stress in the surface layers. Shot peening is sometimes used as a metal-forming process, and is especially useful in the forming of large, irregularly shaped parts from aluminum alloy sheet stock.

Shot. When steel shot is used to peen form aluminum alloy parts, the parts are usually treated chemically after forming to remove particles of iron or iron oxides that may be embedded in the surface.

Slugs cut from stainless steel or aluminum alloy wire are sometimes used. When peening with aluminum alloy slugs, no subsequent chemical treatment is needed, and the danger of overpeening and high localized residual stress (which sometimes occurs with steel or iron shot) is also eliminated.

Automatic or semiautomatic devices are available for the separation and removal of fines and undersize shot, and for the addition of new shot. Manual handling of shot and batch replacement may be more feasible for small-scale operations. The proportion of full-size shot in the system is usually maintained at a minimum of 85%.

Control. The effectiveness of shot peening depends on the size, shape, material, and velocity of the shot, and on the quantity of shot striking a unit area per unit time. The combined effect of these variables is known as peening intensity.

The angle at which the shot strikes the work also affects the peening intensity, which is proportional to the sine of the angle of impingement. The amount of breakdown of the shot will, of course, also affect peening intensity. The extent of surface coverage as measured by visual or instrumental techniques is often used, together with Almen test strips, to control peening operations.

Applications. One of the earliest forming applications of shot peening was the contour forming of integrally stiffened aircraft wing panels. Because of their extreme length and variable thickness, these parts are ill suited for forming by mechanical processes. Other parts formed by shot peening include honeycomb panels and large tubular shapes. Large, irregularly shaped parts are conveniently formed by this method.

If a part is deformed beyond the specified amount, the contour can be corrected by peening the reverse side. Also, peening can be used as a salvage procedure to correct the contours of bent or distorted parts.

The process is usually carried out as a free-forming technique, without dies or form blocks. Contour is checked against a template. The peening intensities and the number of passes are varied depending on the material and the severity forming required. Local areas can be subjected to the required treatment.

Drop Hammer Forming

Drop hammer forming is of value for limited production runs that do not warrant expensive tooling. For example, it is often used in experimental work to make trial parts and parts that are expected to undergo frequent design changes.

Tooling costs are low, and finished parts can be produced quickly. However, only relatively shallow parts with liberal radii can be drop hammer formed, and material thickness must be in the range of about 0.61 to 1.63 mm (0.024 to 0.064 in.). Also, wrinkling occurs frequently, and a high degree of operator skill is required.

Equipment and Tools. Air-operated hammers with sensitive and accurate control are usually preferred to hammers operated by gravity or steam.

The material is formed in a sequence of small steps. In a typical setup, several plywood or rubber spacers are stacked on the die face, and one or more are removed after each stroke to form the workpiece progressively.

In a variation of this procedure, a series of dies can be used to accomplish the progressive forming. Only the last of these dies requires close tolerances. A rubber pad several inches thick is sometimes used between workpiece and punch in all but the final step.

Dies are simple and inexpensive. Bottom dies are cast from zinc alloy. Punches can also be made from zinc alloy, but if requirements on sharpness of radii and accuracy of contour are not stringent, punches cast from lead are used for short runs. These need not be cast accurately because they deform to the shape of the bottom die in a few strokes. For longer runs, tools can be made of cast iron or cast steel. Lubrication requirements are similar to those for drawing operations.

Alloys used most frequently are 1100, 3003, 2024, 5052, 6061, and 7075. Annealed tempers permit the greatest severity of forming. Intermediate tempers of the nonheat-treatable alloys are often used for channel shapes and shallow, embossed panels. Heat-treatable alloys can be partly formed in the annealed condition and given a restrike operation after heat treatment, or they can be formed in the fresh W temper.

All processing conditions being equal, aluminum alloy stock will wrinkle more readily than the same thickness of steel sheet. For comparable results in forming, aluminum alloys must be about 40% thicker than steel. More information on this process is available in the article "Drop Hammer Forming" in this Volume.

Other Forming Methods

A number of additional conventional forming processes are applied to aluminum alloy sheet, including embossing, coining, stamping, curling, expanding or bulging, contracting or necking, hole flanging, and beading or ribbing.

Embossing, Coining, and Stamping. These three closely related methods for making shallow impressions and patterns by compression between a punch and a die are frequently combined with drawing. In these operations, the material must yield under impact and compression, and it must be ductile to avoid fracture in tension.

Uniform thickness in all areas of the workpiece generally is maintained in embossing; however, some stretching occurs. Simple designs are produced with light pressure, using a punch of the desired shape and an open female die. Complex patterns require high pressure and a closed matching female die or a rubber female die.

Coining differs from embossing in that the metal is made to flow, thus producing local differences in metal thickness. The design on the top and bottom surfaces may be different. Very high pressure is required.

Stamping produces cut lines of lettering or patterns in one side of the workpiece, to a depth of 0.51 to 1.0 mm (0.020 to 0.040 in.). The depth of penetration must be carefully controlled to minimize distortion and to prevent the design from appearing on the opposite side. Outline or open-face stamps are preferred.

Curling or false wiring can be done in a variety of machines, such as press brakes, single-action punch presses, lathes, roll-forming machines, or special beading machines. The selection of machine depends on the shape and the number of parts required. Circular parts are usually curled on spinning lathes, and rectangular parts are curled in presses. Long, relatively narrow parts can be curled in press brakes or roll-forming machines. Various types of machines have been built specifically for curling in high production quantities.

The edge to be curled should be of uniform height and free from roughness on the outside of the curl. Preferably it should be rounded slightly before beginning the operation. The minimum radius for curling should be $1\frac{1}{2}$ to 4 times the metal thickness, depending on alloy and temper.

Expanding or bulging of aluminum alloy parts can be carried out by several means including segmented mechanical dies, rubber punches, or hydraulic pressure.

Segmented mechanical expanding dies are relatively inexpensive and are capable of high production rates, but are limited to certain shapes and may produce marks on thin stock or low-strength alloys.

Rubber punches are widely used and are applicable to extremely difficult operations or those impossible to do by other means. Rubber is selected at a hardness, tensile strength, and deflection most suitable for the workpiece shape. The rubber punch or pad must be correctly shaped and located to apply pressure to the shell wall at the required points; it must be kept free from oil; and it should be lubricated with talc, pumice, or other powder-type lubricant.

Water and oil can also be used to exert pressure directly against the workpiece, but this technique requires expensive tooling and controls, and is often messy.

Contracting or necking operations reduce the diameter of a shell, usually at the open end. This entails reductions ranging in severity from the forming of a shallow circumferential groove to the forming of a bottleneck shape.

Reduction in diameter in a single operation should not exceed 8 to 15%, depending on alloy, temper, and extent of prior work hardening. The angle from the body to the necked diameter should be less than 45°, to prevent collapse of the shell. It may be necessary to anneal the workpiece locally.

Hole flanging, the forming of a flange or collar around a hole in sheet stock, can be a critical operation. The hole should be punched from the side opposite the intended flange. This prevents splitting of the severely stretched outer edge of the flange. Splitting could be initiated by the burred edge of the hole.

Shallow-flanged holes can be produced in a single pierce-and-flange operation with a stepped punch. The edges of the pierced hole should also be as smooth as possible. Low-strength ductile alloys in the annealed temper will permit forming the deepest flanges and the sharpest bend radii. More information on hole flanging is available in the article "Press Bending of Low-Carbon Steel" in this Volume.

Beading or ribbing is usually the most economical way to provide stiffness and avoid oilcan or buckling effects in large panels. Beads that extend from edge to edge of the workpiece are conveniently formed either by bending in a press brake or with corrugating rolls.

Beads that do not extend all the way across the part require a stretching or forming operation either with a rubber-pad die or in a punch press with a rigid punch and a rigid die. A double-acting die and a blank-holder can be used to prevent wrinkling at the ends of the beads, and deep, parallel beads are often made one at a time. Rubber-pad forming can also be used, as can drop hammer forming for small quantities.

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Revised by Larry A. Grant, Electrofusion Corporation

Introduction

BERYLLIUM has been successfully formed by most common sheet metal forming operations. The following are required:

- Equipment that can be controlled at slow speeds and that can withstand the use of heated dies
- Dies that can withstand the temperatures at which beryllium is commonly formed
- Facilities for preheating and controlling the temperature of dies and workpieces
- In some applications, facilities for stress relieving the work at 705 to 790 °C (1300 to 1450 °F)
- Special lubrication
- Safety precautions when grit blasting is required for cleaning after forming

Almost all beryllium currently used is produced by consolidating beryllium powder into a block by vacuum hot pressing. The powder is obtained by chipping and then mechanically or pneumatically pulverizing a vacuum-cast ingot. The hot pressed block can be warm rolled to the desired sheet thickness.

Unalloyed beryllium is available in two grades, I (instrument grade) and S (structural grade). Typical applications for instrument-grade beryllium include gyroscopes, components in inertial guidance systems, and precision satellite and airborne optical components. Structural grades find application as satellite superstructures, antenna booms, and optical support structures. Table 1 lists the compositions of four grades of vacuum hot pressed beryllium.

Table 1 Compositions of four grades of vacuum hot pressed beryllium

Grade	Composition[(a)]							
	Be, %	**BeO, %**	**Al, ppm**	**C, ppm**	**Fe, ppm**	**Mg, ppm**	**Si, ppm**	Other, ppm
S-65B	99.0 min	1.0	600	1000	800	600	600	**400**
S-200F	98.5 min	1.5	1000	1500	1300	800	600	**400**
I-220A	98.0 min	2.2	1000	1500	1500	800	800	**400**
I-400	**94.0 min**	**4.2 min**	**1600**	**2500**	**2500**	**800**	**800**	**1000**

(a) Maximum, unless otherwise indicated

Information on the production and consolidation of beryllium powder is available in the articles "Production of Beryllium Powders" and "Forging and Hot Pressing" in *Powder Metal Technologies and Applications*, Volume 7 of the *ASM Handbook;* the metallography and microstructures of unalloyed beryllium are discussed in the article "Beryllium" in *Metallography and Microstructures*, Volume 9 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

Forming of Beryllium

Revised by Larry A. Grant, Electrofusion Corporation

Formability

The formability of beryllium is low compared with that of most other metals. Beryllium has a hexagonal close-packed (hcp) crystal structure; thus, there are relatively few slip planes, and plastic deformation is limited. For this reason, all beryllium products should be formed at elevated temperature (generally 540 to 815 °C, or 1000 to 1500 °F) and at slow speeds.

Temperature, composition, strain rate, and previous fabrication history have marked effects on the results obtained in the forming of beryllium.

Effect of temperature on formability (in terms of bend angle at fracture) of two grades of powder sheet is shown in Fig. 1. Although these data show the effect of temperature on bendability, maximum strain on a 2*t* bend radius is not achieved at less than 90°. Therefore, it should not be assumed that the quantitative results shown in Fig. 1 can always be applied directly in practice.

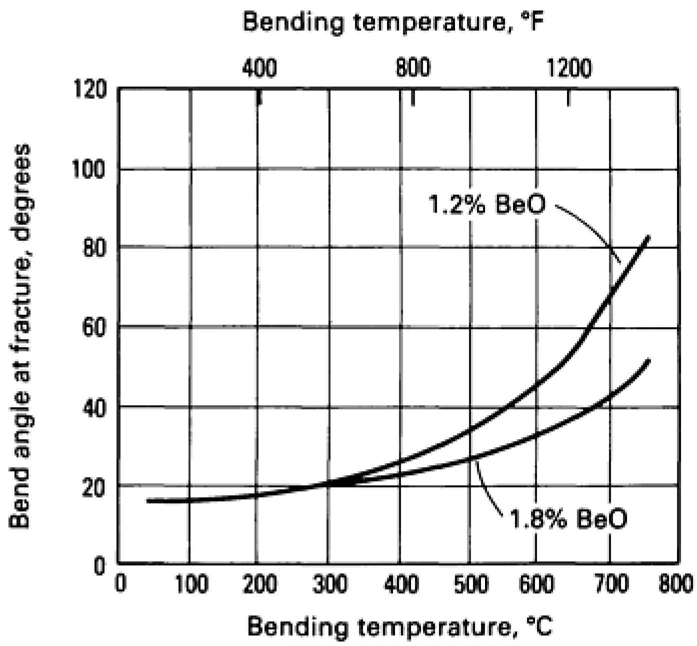

Fig. 1 Bend angle to fracture versus temperature of beryllium sheet using a 2*t* bend radius

It should be noted that Fig. 1 was generated using beryllium sheet with a guaranteed elongation of only 5%. Current beryllium sheet products have guaranteed room-temperature elongations of 10%; typical values of 15 to 20% indicate that, if the test illustrated in Fig. 1 were repeated today, improvement in results would be significant. In one case, a 90° bend with a 2*t* radius was achieved in 0.5 mm (0.020 in.) thick beryllium sheet.

Effect of Composition. The oxide content of ingot and powder sheet has a significant effect on formability, as shown by the curves in Fig. 1. As the oxide content increases, yield strength increases and ductility decreases.

Effect of Strain Rate. Strain rate greatly influences the formability of beryllium. For instance, the stroke of a press brake is too fast for making sharp bends in hot beryllium. Slow bending, by means of equipment such as a hydraulic or air-operated press, is usually used. Minimum bend limits for the press-brake method and the slower-press method are compared in Fig. 2 for bending of cross-rolled powder sheet.

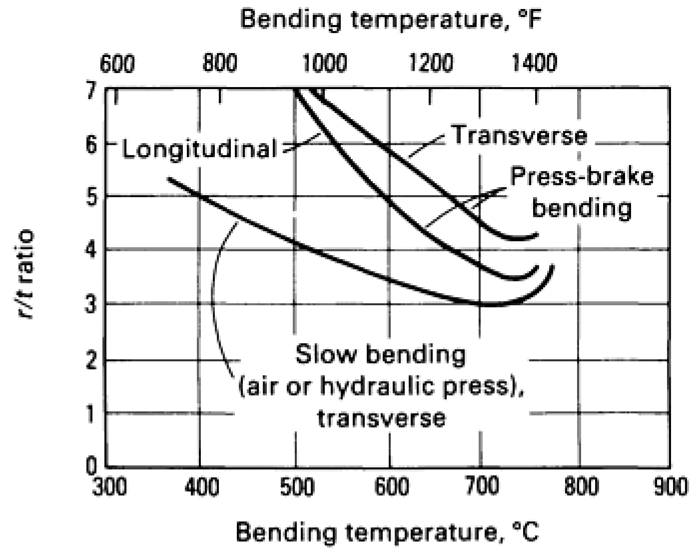

Fig. 2 Minimum bending limits for press-brake versus slower (hydraulic) bending of beryllium sheet in transverse and longitudinal directions. ***r*****, bend radius;** ***t*****, sheet thickness**

In a laboratory, a radius of $2\frac{1}{2}t$ was bent in beryllium sheet at the rate of 50 mm/min (2 in./min), and a radius of $5\frac{1}{2}t$ at 305 mm/min (12 in./min). Forming temperature in both cases was 745 °C (1375 °F).

Effect of Fabrication History. Beryllium products consolidated by vacuum hot pressing have low ductility, even at a theoretical density of 100%. The ductility of hot pressed beryllium can be increased by hot mechanical working.

Equipment and Tooling

Presses operated by air or hydraulic systems are usually used for forming beryllium, because of the slow speeds required. Standard mechanical presses or other fast forming presses are not suitable.

Critical components of the equipment must be protected against damage by the heat of forming. This protection usually is achieved by means of simple insulation.

Tooling. Because the tools used for forming beryllium will be heated, allowances must be made for thermal expansion, high-temperature strength, and oxidation when selecting tool material and designing tools. Tooling requirements for forming beryllium are similar to those for hot forming titanium (see the article "Forming of Titanium and Titanium Alloys" in this Volume). When only a few pieces are required, mild steel is usually used for dies. However, mild steel oxidizes rapidly at elevated temperatures, and when more than a few identical pieces are to be formed, the best practice is to make dies from hot work die steels, stainless steel, or one of the nickel-base or cobalt-base heat-resistant alloys.

Heating Dies and Workpieces

In most forming applications, both the die and the workpiece must be preheated. Dies are specially constructed to permit heating; heat may be supplied by either electrical elements or gas burners. Although sometimes torches are satisfactory for heating the work (as when heating sheet for spinning), usually a furnace is preferred. No specially prepared atmosphere is needed.

At the maximum temperature used for forming beryllium, surface oxidation is usually negligible. However, if desired, to prevent surface discoloration (hard oxide layer), the workpiece can be coated with a film of commercial heat-resistant oil. After forming, the film of oil can be removed by wet blasting, or by degreasing with an agent such as trichloroethylene.

In the forming of thin sheet (less than ~1 mm, or 0.040 in., thick), cooling of the work between the furnace and the forming equipment is often a problem. Overheating to compensate for this heat loss is not recommended. One satisfactory solution is to "sandwich" thin sheets of beryllium between two sheets of low-carbon steel. This sandwich is retained throughout heating and forming.

Stress Relieving

Stress relieving between stages of forming, or after forming is completed, is needed only in the forming of relatively thick sheet or in severe forming. For some finish-formed parts, stress relieving has proved an effective means of counteracting "oil canning" or excessive warpage. When stress relieving is used, regardless of whether it is an intermediate step or a final operation, holding at 705 to 760 °C (1300 to 1400 °F) for 30 min is recommended. No specially prepared atmosphere is needed.

Forming of Beryllium

Revised by Larry A. Grant, Electrofusion Corporation

Lubrication

Lubrication or coating of some type is needed in most beryllium forming operations. For less severe operations, such as bending, powdered mica has been used.

For operations such as joggling, forming in matched dies, or deep drawing, colloidal graphite in oil is commonly used. The role of lubrication is especially critical in deep drawing, and is discussed in more detail in the section "Deep Drawing" in this article.

Forming of Beryllium

Revised by Larry A. Grant, Electrofusion Corporation

Safety Practice

No special precautions or safety measures are required in forming of beryllium because no fines or oxide dust is created in forming; and the maximum temperature (815 °C, or 1500 °F) used for preheating causes the formation of only a thin film of hard oxide, which under normal operating conditions will not harm personnel. Extreme caution should be used, however, and safety equipment should be available in the event of a furnace overrun.

However, if parts require cleaning after forming and if grit blasting is used, the wet method is recommended. Wet blasting minimizes the possibility that beryllium oxide dust will contaminate the surrounding atmosphere. Adequate ventilation must be provided if parts are processed by chemical etching after forming.

The usual precautions observed in working with beryllium must be taken. Details on protection can be obtained from the publication "Health Protection in Beryllium Facilities," which is available from the U.S. Atomic Energy Commission. Also, a video tape, "Beryllium: Safe Handling," is available through Brush Wellman Inc.

Forming of Beryllium

Revised by Larry A. Grant, Electrofusion Corporation

Deep Drawing (Ref 1)

Deep drawing is the forming of deeply recessed (cuplike) parts by means of plastic flow of the material (see the article "Deep Drawing" in this Volume). Tooling consists of a punch and a suitable die or draw ring. Normally, the deformation in deep drawing is actually a combination of deep drawing and stretching.

There are two parameters that must be under control during any successful deep-drawing operation: friction and hold-down pressure. Both can be controlled by proper die design and lubricant selection, as discussed below.

Lubrication is required to prevent galling between the beryllium workpiece and the die. A lubricant film must be maintained over that portion of the blank surface making contact with the drawing surfaces of the die throughout the entire draw. Because elevated temperatures (595 to 675 °C, or 1100 to 1250 °F, for the workpiece; 400 to 500 °C, or 750 to 930 °F, for the dies) are required to deep draw beryllium, conventional lubricants applied directly to the blank and die will burn off, causing galling between workpiece and die at high-pressure areas such as the draw ring. The solution to this problem is best achieved by using die materials that are self-lubricating, such as graphite or an overlay of colloidal suspension of graphite on an asbestos paper carrier.

The technique of using consolidated graphite as a self-lubricating die material was initially developed for forming small, thin-walled parts to finished size. This technique has evolved to the point that very deep drawing of 6.35 mm (0.25 in.) thick blanks over a graphite draw ring is routine. The disadvantage is that such draw rings have short service life.

Organic emulsified suspensions of powdered graphite, aluminum, and copper have all been used successfully to lubricate punches to facilitate part stripping. These materials can also be applied to the draw ring to improve lubricity of the drawing surface under the graphite-impregnated paper.

Blank development for deep drawing of beryllium generally follows the same rules as for other metals. Blanks too thin to support themselves during the early stages of drawing will buckle or wrinkle. A restraining force is required to prevent this.

There are numerous factors involved in determining whether blank restraint is required during any drawing operation. The two most important are the ratio of blank diameter d to blank thickness t and the percentage of reduction from one draw to the next.

The relationship between reduction R and d/t is shown in Fig. 3 for cylindrical parts, whether they are flat bottomed or hemispherical cups. The areas under the curves were determined experimentally, with the curves themselves being the normal limit of formability for a given reduction at a given d/t ratio.

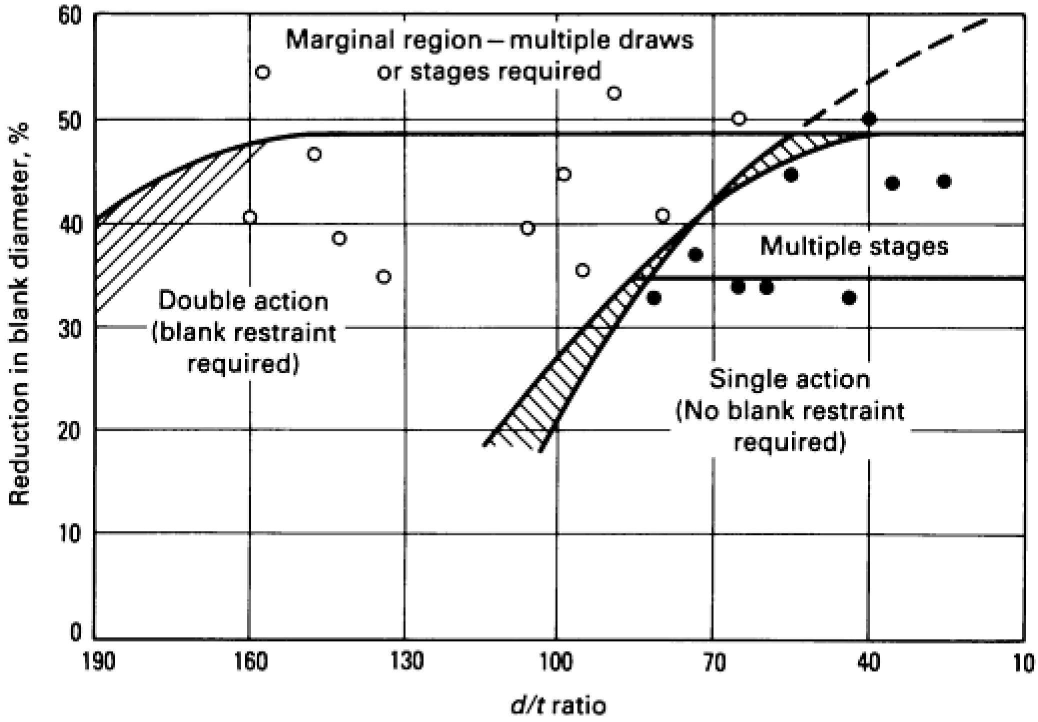

Fig. 3 Percent reduction in deep-drawing versus diameter-to-thickness (*d*/*t*) ratio for deep drawing of cylindrical beryllium shells. X and O, experimental observations used to derive the curve limits; *d,* blank diameter; *t,* blank thickness; shaded areas, marginal. Source: Ref 1.

The curves in Fig. 3 describe formability limits; therefore, some consideration should be given during design to avoid borderline cases. Reductions of more than 50% are possible but will require partial drawing followed by several anneals, usually with a high failure rate. Several stages of tooling requiring smaller reductions is a more practical approach.

Tool Design. There are many different tool designs for deep drawing sheet metal parts. Two general types will be described here. One type does not apply blank restraint to prevent wrinkling and is referred to as single action. The other type does apply blank restraint and is referred to as double action.

Single-action tooling should be used to form parts that fall in the no-restraint section of Fig. 3. Double-action tooling, or tooling which applies blank restraint to avoid wrinkling, was developed in two forms. In one system, the lower cushion ram in a hydraulic press is used as the second action for blank restraint (Fig. 4). The other type of double-action tooling used to deep draw beryllium are described in detail in Ref 1.

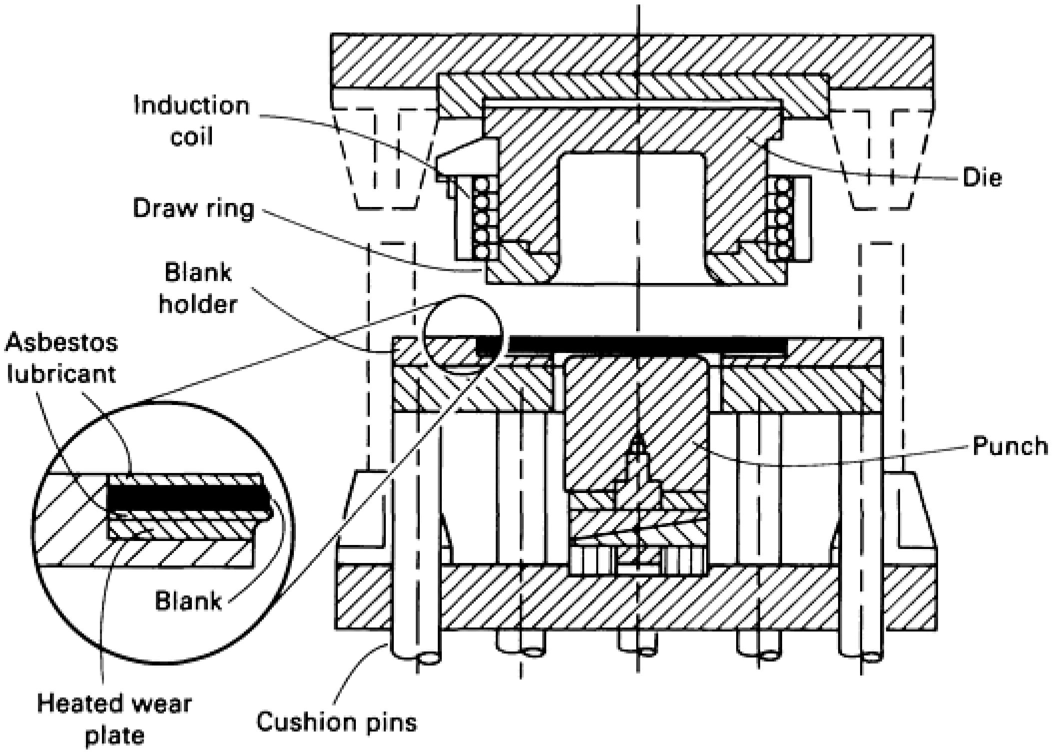

Fig. 4 A double-action tool for deep drawing of beryllium that uses the action of the lower press action for blank restraint. Lubrication with this type of tooling is best achieved using asbestos paper impregnated with colloidal graphite (see inset). Source: Ref 2.

Materials used for beryllium deep-drawing tooling need not be exotic. Gray cast iron is satisfactory for most punch and die applications. Drawing surfaces are usually made from a free-machining tool steel or, in the case of very large dies, low-carbon steel that has been carburized after machining.

Strain rates during deep drawing of beryllium may vary widely, depending on the severity of the draw. For deep drawing simple hemispherical shells, punch speeds of 760 to 1270 mm/min (30 to 50 in./min) are commonly used. With optimal die clearance and lubrication, strain rates in excess of 2500 mm/min (100 in./min) have been observed in successful deep draws.

Applications. Numerous shapes have been deep drawn from beryllium. Considerable material savings may be achieved by deep drawing rather than machining thin-walled parts. The process lends itself to cup-shaped parts that have a slightly thicker wall at the equator than at the pole because of thickening in this area during forming.

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2. J.L. Frankeny and D.R. Floyd, "Ingot Sheet Beryllium Fabrication," RFP-910, Rocky Flats Division, Dow Chemical Company, Feb 1968

Revised by Larry A. Grant, Electrofusion Corporation

Three-Roll Bending (Ref 1)

Three-roll bending is a process for shaping smoothly contoured, large-radius parts by applying three-point bending forces progressively along the part surface (see the article "Three-Roll Forming" in this Volume). Usually one or more of the forming rolls is driven. The process has been used to form curved panel sections and full cylinders from beryllium. As in all forming operations for beryllium, it is necessary to heat the blank to achieve the necessary ductility to avoid cracking.

Applications. Three-roll bending has been used to form precision beryllium cylinders. The cylinders were joined by an electron beam fusion weld and are round within 0.5 mm (0.02 in.) total indicator reading on the diameter.

Panels for the Agena spacecraft also have been formed to a 762 mm (30 in.) radius of curvature. There were two sizes of panels formed, 635 × 635 mm (25 × 25 in.) and 559 × 355 mm (22 × 14 in.), at thicknesses of 1.4 and 1.88 mm (0.055 and 0.074 in.), respectively, from cross-rolled beryllium powder sheet. The flat beryllium sheet was heated to about 427 °C (800 °F), placed on a stainless steel sheet somewhat longer than the beryllium, and manually rolled to contour. The stainless steel sheet was used to "lead-in" the beryllium and reduce the flat end inherent to roll forming. The rolled panels were stress-relieved at 732 °C (1350 °F) for 20 min.

Reference cited in this section

1. J.J. Blakeslee, chapter 7, Metalworking IV: Forming, in *Beryllium Science and Technology,* Vol 2, D.R. Floyd and J.N. Lowe, Ed., Plenum Press, 1979, p 107-124

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Stretch Forming (Ref 1)

Stretch forming is the shaping of a sheet or part, usually of uniform cross section, by first applying suitable tension or stretch, then wrapping it around a die of desired shape (see the article "Stretch Forming" in this Volume). When applying this technique to beryllium, the wrapping operation usually takes place quite slowly.

Tooling. Two commonly used types of tooling used to stretch form beryllium are generally described as open-die and closed-die tooling. Open die, the most common, consists of a male die with the desired contour and some means of forcing the blank to assume that contour. Tension is not normally required for beryllium because the high modulus resists buckling and wrinkling.

Closed die tooling has male and female counterparts. The male die is used to force the blank into the female die, thereby causing the blank to assume the contour of the male die. This type of tooling lends itself well to beryllium forming because both portions of the die may be heated to facilitate maintenance of the heat necessary in the blank to avoid cracking. Friction forces on the female die can help to restrain the part and cause stretching.

Reference cited in this section

1. J.J. Blakeslee, chapter 7, Metalworking IV: Forming, in *Beryllium Science and Technology,* Vol 2, D.R.

Floyd and J.N. Lowe, Ed., Plenum Press, 1979, p 107-124

Spinning

Beryllium sheets up to 5.1 mm (0.200 in.) thick have been successfully formed by spinning. For sheets less than about 1 mm (0.040 in.) thick, a common practice is to sandwich the beryllium between two 1.5 mm (0.060 in.) sheets of low-carbon steel and heat the sandwich to 620 °C (1150 °F) for spinning. The steel sheets not only help to maintain temperature, but also help to prevent buckling. Beryllium sheets more than about 1 mm (0.040 in.) thick usually are not sandwiched between steel sheets for spinning, and are heated to 730 to 815 °C (1350 to 1500 °F).

Hemispherical shapes have been spun in as many as nine stages with no adverse effect on the properties of the beryllium. The part and mandrel often are torch-heated during spinning.

Lubrication is especially important in spinning. Colloidal graphite or glass is usually used. Wet blasting is the recommended means of cleaning the workpiece after spinning.

Figure 5 plots combinations of conditions under which parts of a variety of shapes have been successfully produced by spinning cross-rolled beryllium powder sheet. The points plotted, however, represent only limited data, and many more points would have to be established before it would be safe to designate dimensional limitations for spinning specific shapes. More information on the spinning process is available in the article "Spinning" in this Volume.

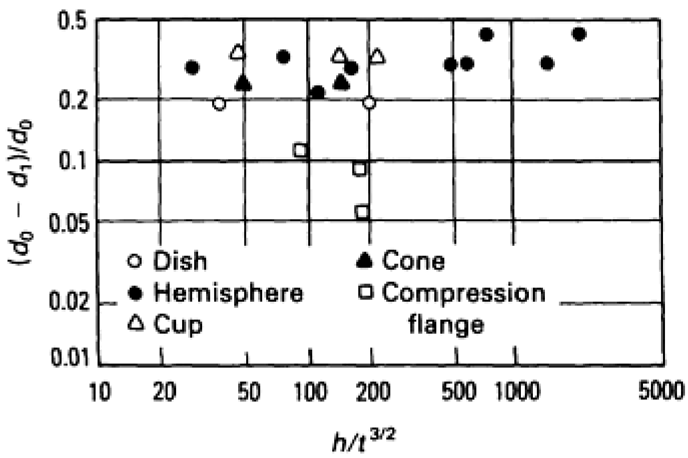


Fig. 5 Dimensional combinations for the successful spinning of beryllium sheet. d_0, blank diameter; d_1, diameter of spun part; *t*, blank thickness; *h*, height of spun part

References

1. J.J. Blakeslee, chapter 7, Metalworking IV: Forming, in *Beryllium Science and Technology,* Vol 2, D.R. Floyd and J.N. Lowe, Ed., Plenum Press, 1979, p 107-124
2. J.L. Frankeny and D.R. Floyd, "Ingot Sheet Beryllium Fabrication," RFP-910, Rocky Flats Division, Dow Chemical Company, Feb 1968

Forming of Copper and Copper Alloys

Frank Mandigo and Jack Crane, Olin Corporation

Introduction

COPPER AND MOST COPPER ALLOYS are readily formed at all sheet gages. The copper alloys commonly formed are characterized by strength and work-hardening rates between those of steel and aluminum alloys. This article will review the general characteristics of copper and copper alloys and how these characteristics affect the behavior of strip in different types of forming operations. The attempt is to provide an understanding of copper alloy formability coupled with illustrative data rather than to offer a complete single-source alloy data reference.

Acknowledgements

The authors would like to acknowledge the significant contributions to this article from John Breedis, Olin Corporation, and John Turn, Brush Wellman Inc. Without their assistance, completion of this document would not have been possible.

Forming of Copper and Copper Alloys

Frank Mandigo and Jack Crane, Olin Corporation

General Considerations

The combination of moderate-to-high strength, high electrical and thermal conductivity, modest cost, good corrosion and stress-corrosion resistance, and ease of joining, coupled with good formability, accounts for the use of copper and copper alloys in a wide range of applications. The list of typical applications given below reveals the diversity of forming operations used:

Application	Forming operations
Electrical terminals and connectors	**Bending, stretch forming, blanking, coining, drawing**
Electronic lead frames	**Bending, coining, blanking**
Hollow ware, flatware	**Roll forming, blanking**
Builder's hardware	**Shallow and deep drawing, and stretch forming operations**
Heat exchangers	**Roll forming, bending, sinking, blanking**
Coinage	**Blanking, coining, embossing**
Bellows, flexible hose	**Cupping, deep drawing, bending**
Musical instruments	**Blanking, drawing, coining, bending, spinning**
Ammunition	**Blanking, deep drawing**

These applications are illustrated in Fig. 1, 2, 3, 4, 5, 6, 7, and 8.

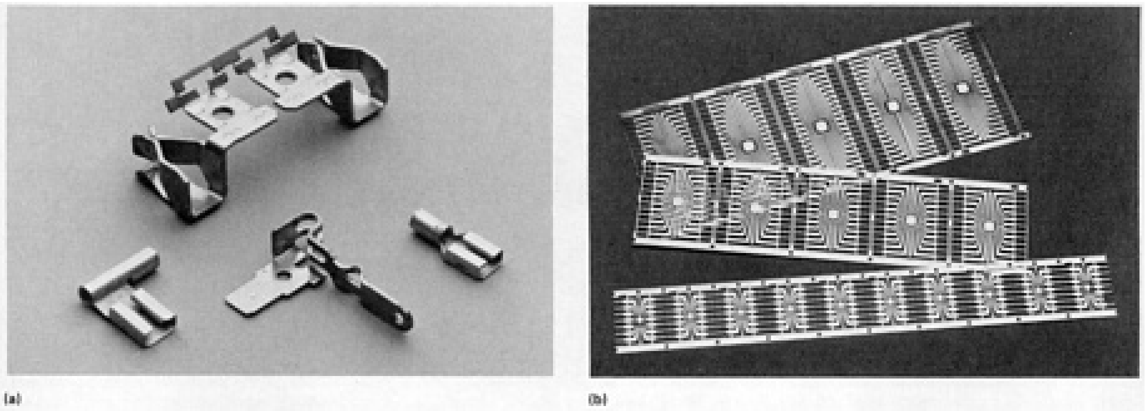


Fig. 1 Electrical and electronic applications for formed copper alloy parts. (a) Connectors used in home appliances and automotive electrical systems. (b) Copper alloy leadframe for a semiconductor device.

Fig. 2 Typical household flatware utensils formed from copper alloys.

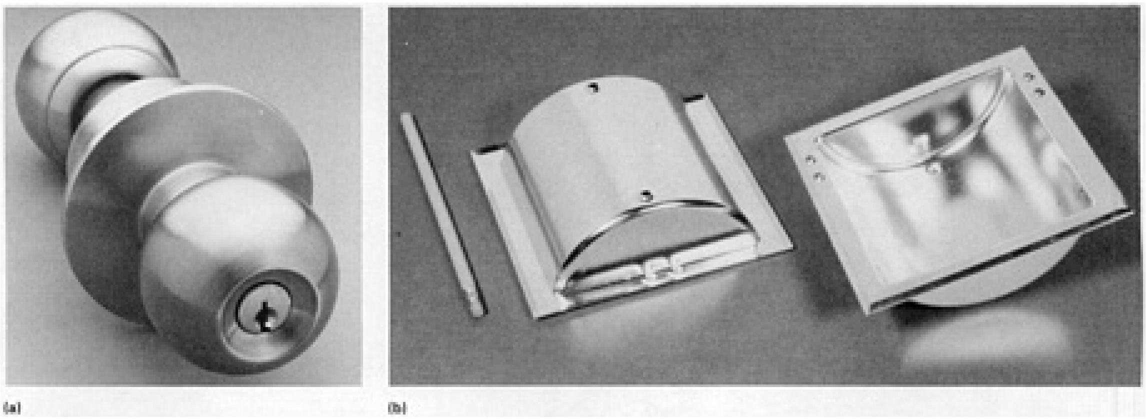


Fig. 3 Builder's hardware formed from copper alloys. (a) Doorknob fabricated by deep drawing. (b) Recessed fixture for kitchen and bathroom accessories.

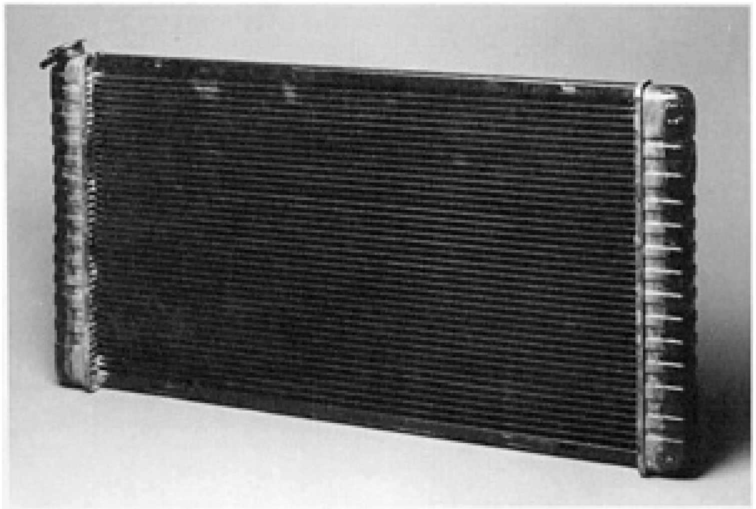


Fig. 4 Automotive radiator fabricated from several formed copper alloy components, including a deep-drawn water tank, roll-formed cooling tubes, and formed cooling fins.

Fig. 5 Copper alloy U.S. currency with heavy coining and embossing.

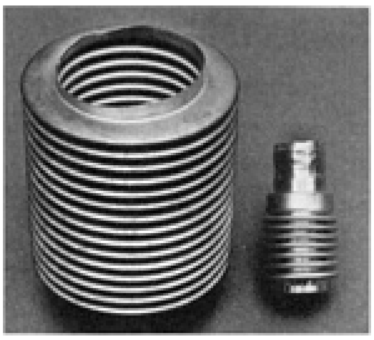


Fig. 6 Deep-drawn and corrugated copper alloy bellows.

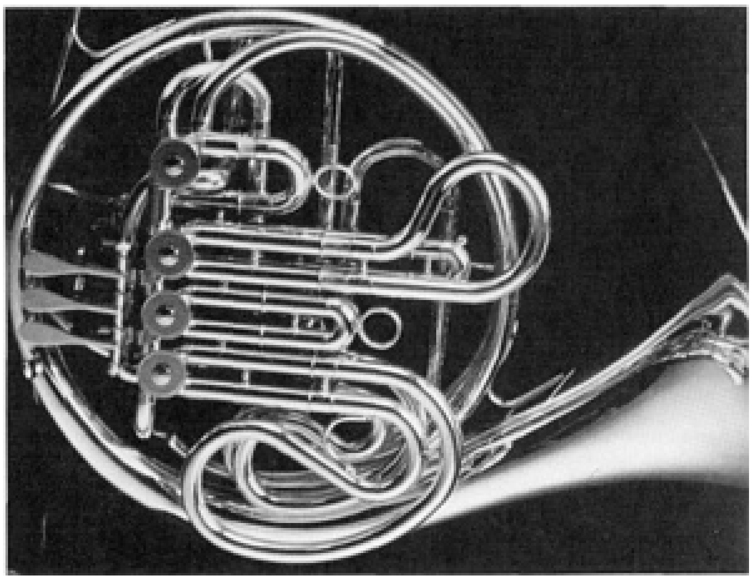


Fig. 7 French horn fabricated from copper alloys using complex bending and spinning operations.

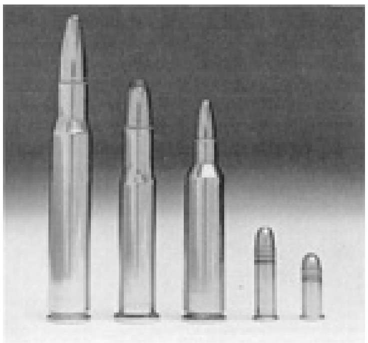


Fig. 8 Ammunition using a deep-drawn copper alloy cartridge case.

The forming of any part involves the interaction of material, tooling, and lubrication. Tooling and lubrication are discussed in the article "Presses and Auxiliary Equipment for Forming of Sheet Metal" and "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume and are noted in this article only where there are unique or specific requirements for a copper alloy or a given forming operation. The forming characteristics of copper and copper alloys will be discussed at length. All of the various major forming operations considered in this article--blanking, bending, stretch forming, drawing, and coining--depend on some optimal combination of strength, ductility, and work-hardening behavior of the sheet metal to provide the most cost-effective part. Therefore, much of this article is devoted to understanding the trade-offs in strength, work-hardening, and ductility available by selection of material composition and temper. Strain rate sensitivity m is also a factor in some forming operations. However, m is of practical significance only at elevated temperature. A more comprehensive treatment of the relationships between these materials characteristics and formability is available in the article "Formability Testing of Sheet Metal" in this Volume and in the article "Sheet Formability Testing" in *Mechanical Testing*, Volume 8 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

Other materials characteristics that reflect formability and can be determined using simple test specimens include the plastic-strain ratio r, which is a measure of sheet anisotropy; the limiting draw ratio (LDR); bulge height; and minimum bend-forming radius. These measurements are primarily used to assess drawing and stretching capacity specific to a given alloy composition, cold-work level, and texture development.

Effects of Composition, Cold Work, and Heat Treatment on Formability

Copper alloys are primarily strengthened by cold work or by alloying additions that solid solution strengthen and enhance strain hardening. A finely dispersed second phase is sometimes used as a grain refiner to maximize strength/ductility combinations and/or as a means of ensuring good surface finish after forming.

Precipitation hardening is important to a small but important class of alloys, most notably, the beryllium copper alloys. Copper-nickel-aluminum and copper-nickel-silicon alloys are also commercially important precipitation-hardenable alloys. Spinodal and/or precipitation hardening is available in the copper-nickel-tin and copper-nickel-chromium systems. Hardening by martensite transformation is available in the copper-aluminum system, but is rarely used commercially.

Copper alloys are classified using the Unified Numbering System (UNS). The designations of the Copper Development Association (CDA) are also used and correspond closely to UNS designations. Wrought copper alloys are divided in the UNS system into the following groups:

Copper and high-copper alloys	**C1*xxxx***
Zinc brasses	**C2*xxxx***
Zinc-palladium brasses	**C3*xxxx***
Zinc-tin brasses	**C4*xxxx***
Tin bronzes	**C5*xxxx***
Aluminum, manganese, and silicon	**C6*xxxx***
Copper-nickel and copper-nickel-zinc alloys	**C7*xxxx***

Copper alloys are supplied in annealed (soft) and cold-worked (hard) tempers, as defined in Table 1. These designations are only guidelines; the supplier should be consulted for specific property/temper characteristics. Temper designations for precipitation-hardened alloys are covered in the section "Precipitation Hardening and Cold Working" in this article.

Table 1 ASTM B 601 temper designations for copper and copper alloys

Temper designation	Temper name or material condition
Annealed tempers	
025	**Hot rolled and annealed**
050	**Light annealed**
060	**Soft annealed**
061	**Annealed**
065	**Drawing annealed**
068	**Deep-drawing annealed**
070	**Dead soft annealed**
080	**Annealed to temper--** $\frac{1}{8}$ **hard**
081	**Annealed to temper--** $\frac{1}{4}$ **hard**
082	**Annealed to temper--** $\frac{1}{2}$ **hard**
OS005	**Average grain size 0.005 mm**
OS010	**Average grain size 0.010 mm**
OS015	**Average grain size 0.015 mm**
OS025	**Average grain size 0.025 mm**
OS035	**Average grain size 0.035 mm**
OS050	**Average grain size 0.050 mm**
OS070	**Average grain size 0.070 mm**
OS100	**Average grain size 0.100 mm**
OS120	**Average grain size 0.120 mm**
OS150	**Average grain size 0.150 mm**
OS200	**Average grain size 0.200 mm**
Cold-worked tempers	
H00	$\frac{1}{8}$ **hard**

H01	$\frac{1}{4}$ **hard**
H02	$\frac{1}{2}$ **hard**
H03	$\frac{3}{4}$ **hard**
H04	**Hard**
H06	**Extra hard**
H08	**Spring**
H10	**Extra spring**
H12	**Special spring**
H13	**Ultra spring**
H14	**Super spring**
Cold-worked and stress-relieved tempers	
HR01	**H01 and stress relieved**
HR02	**H02 and stress relieved**
HR04	**H04 and stress relieved**
HR06	**H06 and stress relieved**
HR08	**H08 and stress relieved**
HR10	**H10 and stress relieved**
HR50	**Drawn and stress relieved**
Cold-worked and order-strengthened tempers	
HT04	**H04 and order heat treated**
HT06	**H06 and order heat treated**
HT08	**H08 and order heat treated**

Solid-Solution Strengthening and Cold Working. Solute elements provide a major means of strengthening copper, and the magnitude of strengthening depends on the type and level of addition. Table 2 lists mechanical properties resulting from various alloying additions to copper in the annealed condition. Neither tensile elongation (Table 2) nor reduction in area fully defines usable formability and should not be used to correlate formability; they can, however, offer some insight into formability. It is clear from Table 2 that strength higher than that of pure copper (Alloy C11000) can be acquired with limited or no loss of ductility by solid-solution alloying.

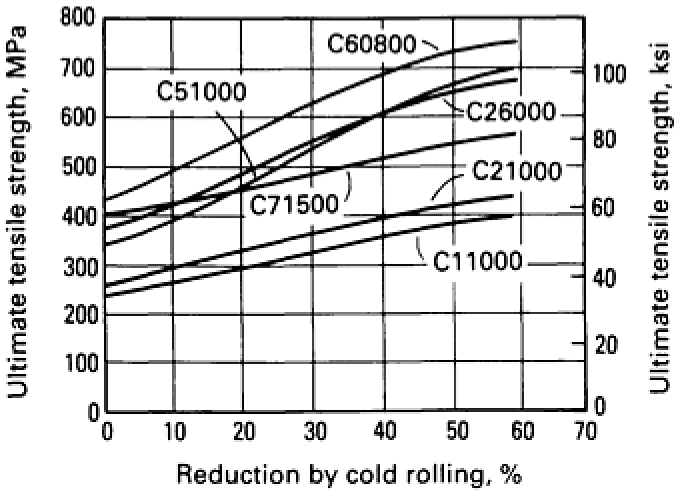
Table 2 Mechanical properties of selected solid-solution copper alloys

Grain sizes of all materials listed ranged from 0.010 to 0.025 mm (0.0004 to 0.001 in.).

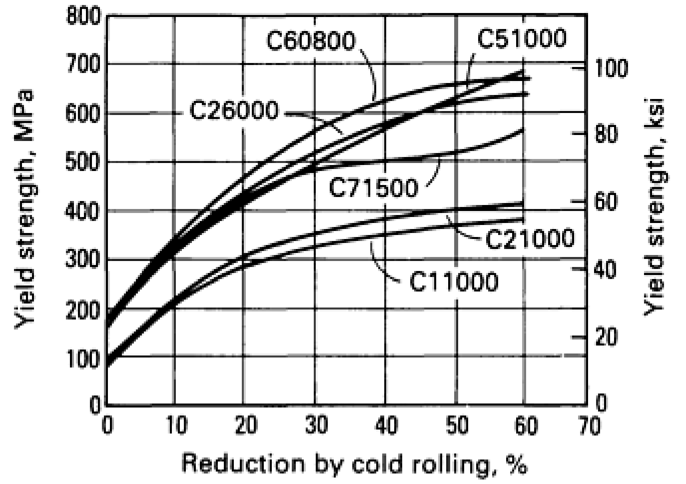
Alloy designation and common name	**Nominal composition, %**	**0.2% offset yield strength**		**Tensile strength**		Elongation, %
		MPa	**ksi**	**MPa**	**ksi**	
C11000 (Electrolytic tough-pitch)	99.90 min Cu	83	12	241	35	**48**
C21000 (Gilding, 95%)	Cu-5Zn	97	14	262	38	**45**
C23000 (Red brass, 85%)	Cu-15Zn	110	16	290	42	**45**
C26000 (Cartridge brass, 70%)	Cu-30Zn	179	26	379	55	**48**
C50500 (Phosphor bronze, 1.25% E)	Cu-1.4Sn	124	18	290	42	**47**
C51000 (Phosphor bronze, 5% A)	Cu-5Sn	165	24	345	50	**50**
C61000[(a)] (. . .)	Cu-8Al	207	30	483	70	**65**
C70600 (Copper nickel, 10%)	Cu-10Ni	124	18	317	46	**38**
C71500 (Copper nickel, 30%)	Cu-30Ni	172	25	400	58	**32**
C75200 (Nickel silver, 65-18)	**Cu-18Ni-18Zn**	**179**	**26**	**414**	**60**	**37**

(a) Available only as tube, but properties are illustrative of copper-aluminum alloy strip properties.

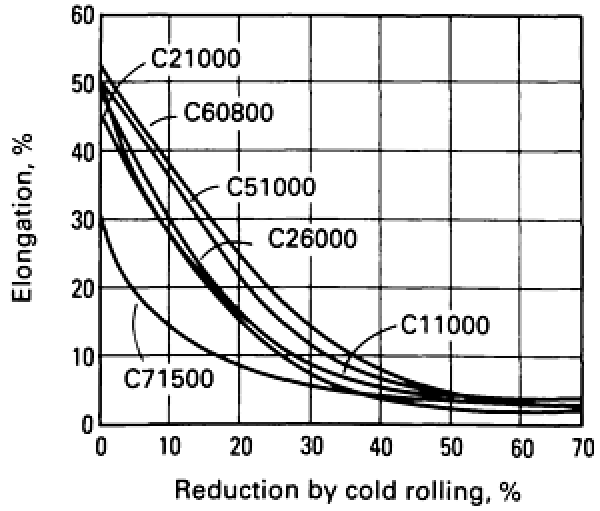
Figure 9 shows the work-hardening behavior of copper (C11000) and several copper alloys in terms of strength and ductility versus cold reduction. The relative work-hardening effects of various alloying elements are evident; the strong effect of aluminum is contrasted with the weak effect of nickel, with zinc and tin being intermediate. Ductility, as indicated by tensile elongation, decreases with cold reduction. Again, however, the combination of strength and ductility is enhanced by solid-solution additives even after cold working.



(a)



(b)



(c)

Fig. 9 Work-hardening behavior of copper and some solid-solution copper alloys. (a) Effect of cold work by rolling reduction on ultimate tensile strength. (b) Effect of cold work on yield strength. (c) Effect of cold work on

elongation.

Precipitation Hardening and Cold Working. Precipitation-hardenable alloys offer the opportunity to form parts in the maximum-ductility (solution-annealed) condition and then harden the formed part to maximum strength with a precipitation heat treatment. However, fabrication requirements may preclude this option. Alloys containing 0.15 to 2.0% Be can be strengthened by solid-state precipitation. For alloys with high beryllium content (1.8 to 2.0%), combinations of cold work and elevated-temperature aging produce material with tensile strength above 1380 MPa (200 ksi). Lower beryllium contents are used to sacrifice some strength for better thermal and electrical conductivities. Forming can precede aging or follow it; the choice is based on property and formability requirements, as well as practicality.

In many cases, volume changes that accompany aging, or other fabricating constraints, preclude aging treatment of the formed part, and the precipitation-hardened alloys are therefore provided in mill-hardened tempers. Mill-hardened alloys are either solution annealed or cold rolled before being given an aging treatment at the mill to produce a specific set of final properties.

Mill-hardened tempers are designed to balance the requirements of strength and formability. They are of particular importance for intricate parts such as electronic connectors, where elimination of customer heat treatment and cleaning steps are important to the economics and/or fabrication of the part. Parts that require sharp bends or maximum formability should be formed from the annealed or rolled tempers before final aging to reach the desired peak strength.

Mill-hardened tempers are much stronger than unaged rolled tempers, but compromise some formability compared to the rolled tempers in favor of avoiding customer aging and cleaning. The grain size of these alloys is less than 0.03 mm (0.001 in.) for gages from 0.1 to 1.27 mm (0.004 to 0.050 in.) thick. Temper designations for precipitation-hardening systems are given in Table 3; mill-hardened temper designations correspond to supplier designations.

Table 3 ASTM B 601 temper designations for precipitation-hardened copper alloys

Temper designation	Temper name or material condition
Solution-treated temper	
TB00	**Solution heat treated**
Solution-treated and cold-worked tempers	
TD00	**TB00 cold worked to $\frac{1}{8}$ hard**
TD01	**TB00 cold worked to $\frac{1}{4}$ hard**
TD02	**TB00 cold worked to $\frac{1}{2}$ hard**
TD03	**TB00 cold worked to $\frac{3}{4}$ hard**
TD04	**TB00 cold worked to full hard**
Precipitation-hardened temper	
TF00	**TB00 and precipitation hardened**
Cold-worked and precipitation-hardened tempers	
TH01	**TD01 and precipitation hardened**
TH02	**TD02 and precipitation hardened**
TH03	**TD03 and precipitation hardened**
TH04	**TD04 and precipitation hardened**
Precipitation-hardened and cold-worked tempers	
TL00	**TF00 cold worked to $\frac{1}{8}$ hard**
TL01	**TF00 cold worked to $\frac{1}{4}$ hard**
TL02	**TF00 cold worked to $\frac{1}{2}$ hard**
TL04	**TF00 cold worked to full hard**
TL08	**TF00 cold worked to spring**
TL10	**TF00 cold worked to extra spring**
TR01	**TL01 and stress relieved**
TR02	**TL02 and stress relieved**
TR04	**TL04 and stress relieved**

Mill-hardened tempers	
TM00	**AM**
TM01	$\frac{1}{4}$ **HM**
TM02	$\frac{1}{2}$ **HM**
TM04	**HM**
TM06	**XHM**
TM08	**XHMS**

The mechanical properties of four precipitation-hardenable alloys in the solution-annealed condition are given in Table 4. The work-hardening behavior of several precipitation-hardening systems in the solution-annealed condition is shown in Fig. 10. The strong effect of beryllium content on solid-solution strengthening and work hardening is evident in Fig. 10 for Alloy C17200. Table 5 lists the mechanical properties of selected tempers of mill-hardened alloys.

Table 4 Mechanical properties of precipitation-hardenable copper alloys in the annealed condition

UNS designation	**0.2% offset yield strength**		**Tensile strength**		Elongation, %
	MPa	**ksi**	**MPa**	**ksi**	
C17200	290	42	476	69	**40**
C17500	207	30	310	45	**27**
C70250	138	20	338	49	**37**

Table 5 Mechanical properties of mill-hardened copper alloys

UNS designation	**Temperature[(a)]**	**0.2% offset yield strength**		**Tensile strength**		Elongation, %
		MPa	**ksi**	**MPa**	**ksi**	
C17410	TM04	655-862	95-125	758-896	110-130	**4-15**
C17500	HTR	758-965	110-140	827-1034	120-150	**1-4**
C70250	TM00	552 min	80 min	620 min	88 min	**6 min**
	TR04	690 min	100 min	731 min	106 min	**2 min**
C17200	TM02	690-862	100-125	827-931	120-135	**12-18**
	TM04	793-931	115-135	931-1034	135-150	**9-15**
C72400	TM02	690-827	100-120	876-1000	127-145	**10-17**

(a) See Table 3.

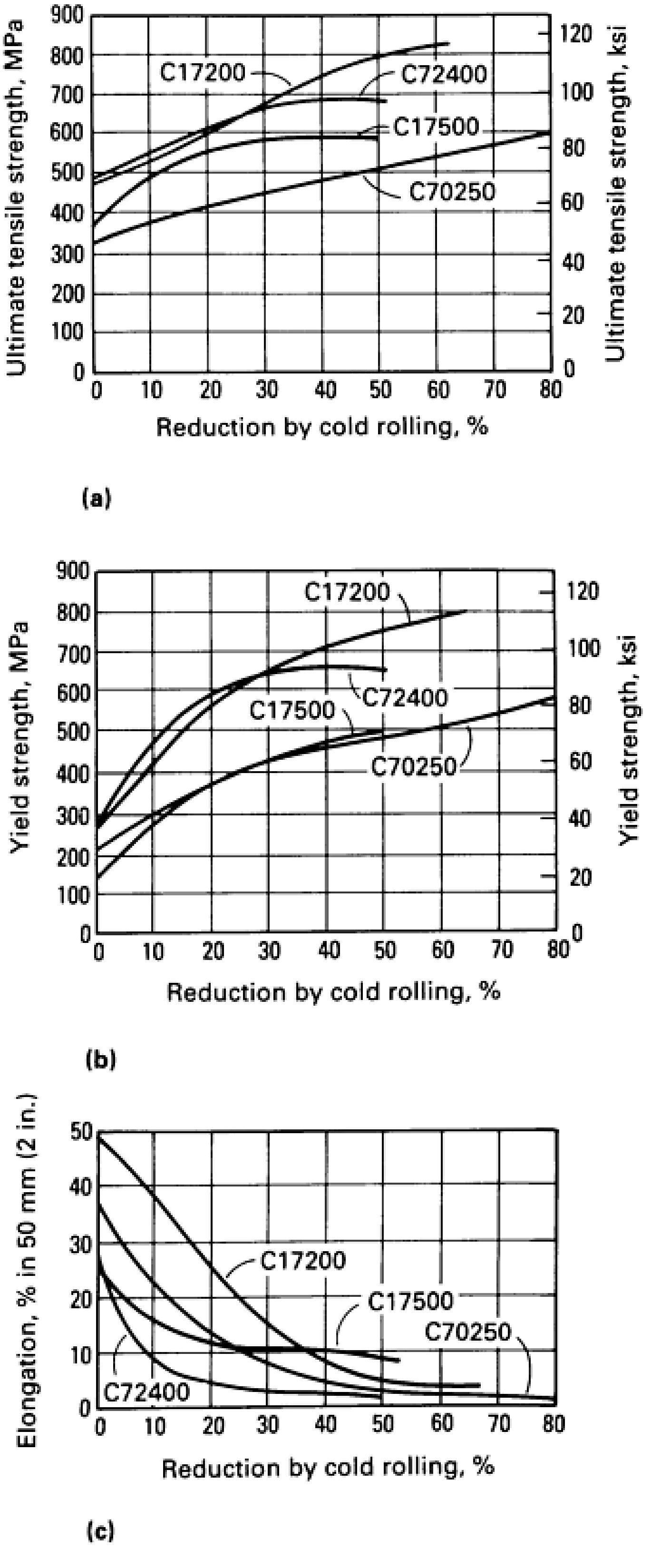


Fig. 10 Work-hardening behavior of four precipitation-hardening copper alloys in the solution-annealed condition. (a) Effect of cold work by rolling reduction on ultimate tensile strength. (b) Effect of cold work on yield strength. (c) Effect of cold work on elongation.

Postforming Heat Treatment. Heat treatments, aside from those employed to precipitation harden, are used after forming to reduce susceptibility to stress corrosion (primarily the brasses) or to increase the stiffness or stress relaxation resistance of electrical or electronic springs (mainly the brasses, aluminum bronzes, and copper-silicon alloys). These postforming treatments are performed at low temperatures.

Formability of Copper Alloys Versus Other Metals

In forming a given part, no single materials property completely defines formability. As previously noted, formability can best be rationalized in terms of the strength, work hardening, and ductility of a copper alloy, but these parameters do not allow direct correlation with formability. The problem becomes even more difficult when comparing different alloy systems--for example, ferrous and nonferrous.

Figure 11 shows the annealed ultimate tensile and yield strengths and response to cold rolling for AISI type 304 stainless steel, 1045 steel, aluminum Alloy 1100, copper Alloy C11000, and some selected copper alloys. The high work-hardening rate and strength of the austenitic stainless steel are evident. The copper alloys range from above aluminum to above low-carbon steel in strength and work-hardening rate. A comparison of limiting draw ratio with the plastic-strain ratio *r* for ferrous and nonferrous alloys is shown in Fig. 12. Increasing values of *r* and LDR reflect increasing drawability (see the section "Drawing and Stretch Forming" in this article).

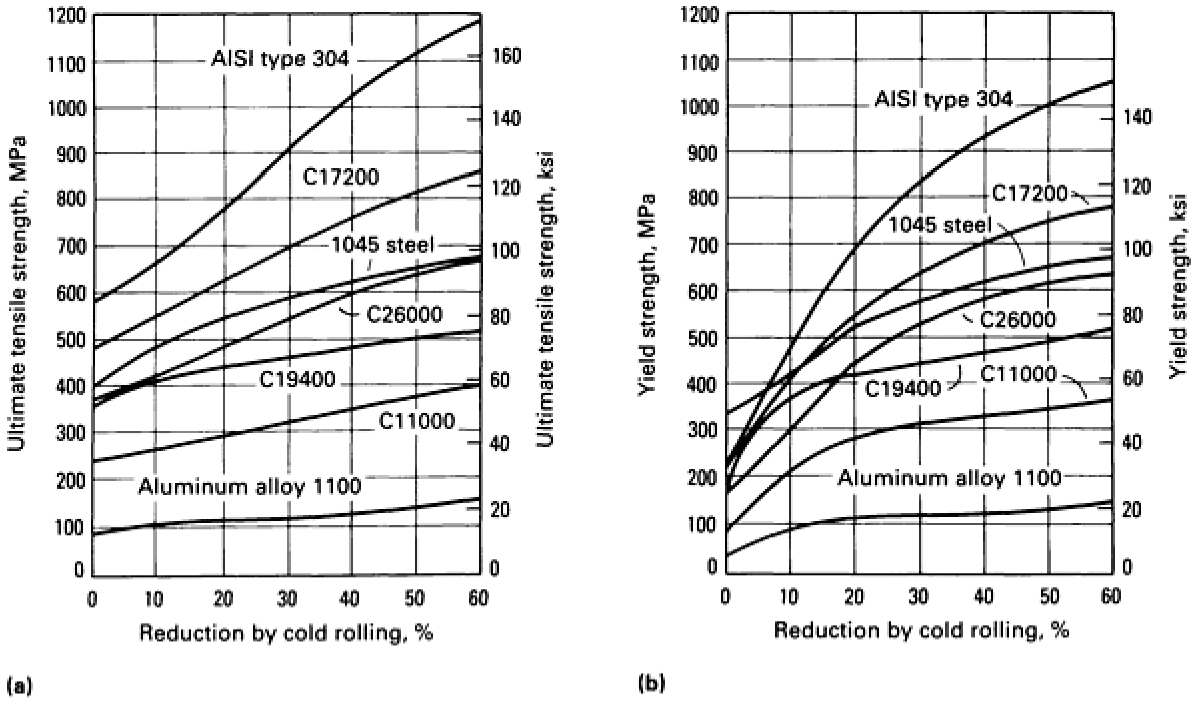


Fig. 11 Work-hardening behavior of copper alloys versus that of low-carbon steel, austenitic stainless steel, and aluminum. (a) Effect of cold work by rolling reduction on ultimate tensile strength. (b) Effect of cold work on yield strength.

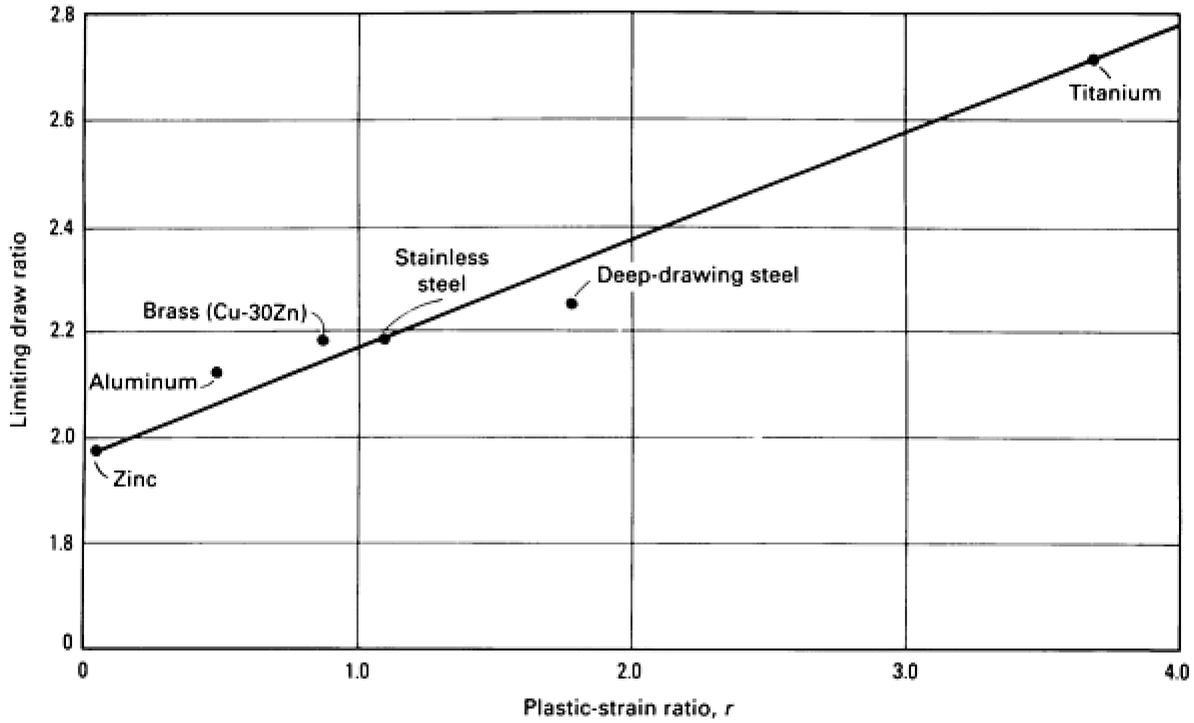


Fig. 12 Plastic-strain ratio *r* versus limiting draw ratio for different metals. Source: Ref 1

In general, copper alloys offer better strength/formability combinations than most other alloy systems. The choice of material system is usually based on economics, including material and other fabrication costs as well as properties.

In general, copper alloys offer better strength/formability combinations than most other alloy systems. The choice of material system is usually based on economics, including material and other fabrication costs as well as properties.

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Forming of Copper and Copper Alloys

Frank Mandigo and Jack Crane, Olin Corporation

Blanking and Piercing

Nature of the Operation. Blanking, piercing, and related cutting operations (trimming, notching, parting, and so on) are often used to provide parts that are subsequently formed to final shape by such operations as bending, drawing, coining, and spinning. Cutting operations are frequently conducted in the same press tooling used to form and shape the final part geometry. The principal objective of any cutting operation is to produce a workpiece that has the correct geometric shape, is free of distortion, and possesses sheared edges that are of sufficient quality to allow subsequent forming, finishing, and/or handling operations.

Materials Considerations. Copper and copper alloys can be readily blanked and pierced. The strip characteristics that directly affect the quality of the workpiece and/or final part produced by cutting operations are flatness, dimensional tolerances, (width, thickness, and so on), and shear-to-break ratio. The flatness and dimensional tolerances of copper alloy strip depend on the equipment and manufacturing expertise. The shear-to-break characteristics of strip depend on strip composition and temper.

Effects of Alloy Composition and Temper. The quality of blanked edges--shear to break, rollover, breakout angle, burr height, and so on--is determined by both die clearance and material characteristics. Burr-free and distortion-free parts can be cut from annealed copper alloy strip at die clearances to about 5% of strip thickness. Unalloyed coppers, such as C10100 and C10200, require smaller clearances (usually <5%) and less latitude in actual values to produce burr-free edges, even in rolled tempers. Copper alloys that contain second-phase particles (for example, C19400), that have high solute additions (such as C26000 or C51000), and/or that are cold rolled more than 50% generally exhibit high-quality blanked edges at die clearances in the range of 3 to 12%. Low-lead additions to brass and other copper alloys will decrease burrs and the shear-to-break ratio in blanking operations--but at some cost to formability in almost all types of forming.

Forming of Copper and Copper Alloys

Frank Mandigo and Jack Crane, Olin Corporation

Bending

Nature of the Operation. Many connectors, terminals, and spring-like components are fabricated by simple bending operations. Bending is an operation in which a blanked coupon is wrapped, wiped, or formed over a die to a specified radius and bend angle. Bend formability is usually expressed as minimum bend radius *R* in terms of strip thickness *t* (***R***/*t*). Minimum bend radius is defined as the smallest radius around which a specimen can be bent without cracks being observed on the outer fiber (tension) surface. Bend deformation is highly localized and is confined to the region of the workpiece in contact with the bending die. Workpiece thickness is not substantially reduced unless the bend radius is less than 1.0*t* or the part is coined during bending. A detailed review of bend testing is provided in the Section "Bend Testing" in *Mechanical Testing*, Volume 8 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

Materials Characteristics. Ductility is the principal materials factor that determines bend formability. The ductility factor of first order importance is the ability of a material to distribute strain in a highly localized region, that is, necking strain. The necking strain available depends on alloy composition and temper. As strength is increased by cold work, the ability of an alloy to distribute necking strain decreases. The extent to which bend formability is decreased with increasing strength is dependent on the alloy composition and the strengthening mechanism. Conventional tensile elongation cannot be used to predict bend formability, because it does not adequately account for the contribution of necking strain. However, if the tensile specimen gage length were decreased to define an area of deformation equal to that deformed during bending, comparable ductility values would be obtained.

Effect of Alloy Composition, Temper, and Orientation. Bend data for a wide range of copper alloys are summarized in Table 6. Strength-to-bend formability characteristics are dependent on alloy composition, temper, and orientation. The principal strengthening mechanism is through solute additions to increase the work-hardening rate. For example, additions of 15 and 30% Zn to copper increase the tensile-strength-to-bend properties by 220 and 290 MPa (32 and 42 ksi), respectively, for 0.25 mm (0.010 in.) thick good-way bends at a bend radius of 0.4 mm ($\frac{1}{64}$ in.). Precipitation strengthening is also an important mechanism employed to improve the strength-to-bend performance of copper alloy strip, particularly if the part is bent in a softer temper and subsequently aged to a higher strength.

Table 6 Maximum strengths required to make the indicated bends in various copper alloys

UNS designation	Maximum strength required to make bend of indicated radius in material of indicated thickness, MPa (ksi)					
	Good-way bend			Bad-way bend		
	0.25 (0.010)[(a)]	**0.50 (0.020)[(a)]**	**0.76 (0.030)[(a)]**	**0.25 (0.010)[(a)]**	**0.50 (0.020)[(a)]**	0.76 (0.030)[(a)]

	0.4 ($\frac{1}{64}$)[b]	**0.8 ($\frac{1}{32}$)[b]**	**1.2 ($\frac{3}{64}$)[b]**	**0.4 ($\frac{1}{64}$)[b]**	**0.8 ($\frac{1}{32}$)[b]**	1.2 ($\frac{3}{64}$)[b]
C11000	372 (54)	352 (51)	352 (51)	365 (53)	331 (48)	**345 (50)**
C17200[c]	896 (130)	896 (130)	896 (130)	896 (130)	896 (130)	**896 (130)**
C17500[c]	. . .	. . .	724 (105)	. . .	. . .	**724 (105)**
C15100	428 (62)	400 (58)	400 (58)	407 (59)	400 (58)	**400 (58)**
C19400	538 (78)	510 (74)	496 (72)	517 (75)	496 (72)	**490 (71)**
C19500	614 (89)	572 (83)	572 (83)	592 (86)	572 (83)	**558 (81)**
C19700	538 (78)	510 (74)	496 (72)	517 (75)	496 (72)	**490 (71)**
C23000	593 (86)	593 (86)	593 (86)	572 (83)	552 (80)	**538 (78)**
C26000	662 (96)	662 (96)	662 (96)	627 (91)	524 (76)	**524 (76)**
C35300	641 (93)	572 (83)	572 (83)	496 (72)	483 (70)	**469 (68)**
C41100	517 (75)	496 (72)	496 (72)	468 (68)	448 (65)	**434 (63)**
C42500	621 (90)	621 (90)	621 (90)	552 (80)	475 (69)	**462 (67)**
C50500	490 (71)	469 (68)	469 (68)	490 (71)	468 (68)	**469 (68)**
C51000	710 (103)	662 (96)	648 (94)	621 (90)	572 (83)	**538 (78)**
C52100	765 (111)	745 (108)	731 (106)	614 (89)	558 (81)	**552 (80)**
C63800	827 (120)	807 (117)	793 (115)	724 (105)	696 (101)	**696 (101)**
C65400	745 (108)	731 (106)	731 (106)	627 (91)	627 (91)	**627 (91)**
C66600	669 (97)	655 (95)	641 (93)	613 (89)	586 (85)	**579 (84)**
C68800	786 (114)	744 (108)	745 (108)	786 (114)	745 (108)	**731 (106)**
C70250[c]	690 (100)	655 (95)	. . .	552 (80)	517 (75)	**. . .**

C70600	524 (76)	496 (72)	496 (72)	489 (71)	483 (70)	**483 (70)**
C72400[c]	793 (115)	690 (100)	621 (90)	793 (115)	690 (100)	**621 (90)**
C72500	572 (83)	517 (75)	517 (75)	531 (77)	504 (73)	**503 (73)**
C73500	579 (84)	579 (84)	579 (84)	525 (76)	518 (75)	**517 (75)**
C74000	648 (94)	600 (87)	586 (85)	593 (86)	565 (82)	**552 (80)**
C75200	579 (84)	579 (84)	579 (84)	558 (81)	558 (81)	**558 (81)**
C77000	**807 (117)**	**751 (109)**	**717 (104)**	**758 (110)**	**696 (101)**	**676 (98)**

"Good way" and "Bad way" refer to the orientation of the bend with respect to the sheet or strip rolling direction (see Fig. 13). Note: Tensile strengths of 965 and 1103 MPa (140 and 160 ksi) are available in 0.25 and 0.5 mm (0.010 and 0.020 in.) thicknesses with specially supplied mill tempers.

Source: Ref 2

(a) Sheet thickness, mm (in.).

(b) Bend radius, mm (in.).

(c) Mill hardened to strength shown, then formed.

The practice of cold rolling to increase strip temper degrades bend formability. However, it is often used because most alloys still exhibit useful bend formability at modest cold-rolling reductions. Product applications that require both high strength and good bend performance are usually satisfied by selecting copper alloys that are precipitation and/or solute strengthened with additions that greatly increase the work-hardening rate and thus minimize cold-rolling requirements to achieve the desired strength.

Bend formability is typically dependent on bend direction with respect to strip-rolling direction (Fig. 13 and Table 6). All cold-rolled materials exhibit directionality. The extent of bend directionality varies from alloy to alloy, but always increases with increasing cold reduction. Bend directionality results from the development of strong textures during rolling. Copper alloys with low stacking fault energy, such as Alloy C26000 (cartridge brass), develop strong {110} <112> textures during rolling and can exhibit bend directionality even at approximately 30% cold-rolling reduction. Dilute copper alloys and copper-nickel alloys do not develop well-defined rolling textures, and they show less bend directionality even at high (70%) cold-rolling reductions. In general, sharper bends can be made in the good-way than in the bad-way orientations for alloys that are cold rolled and/or solute strengthened. Bend anisotropy in precipitation-hardening systems is strongly process dependent.

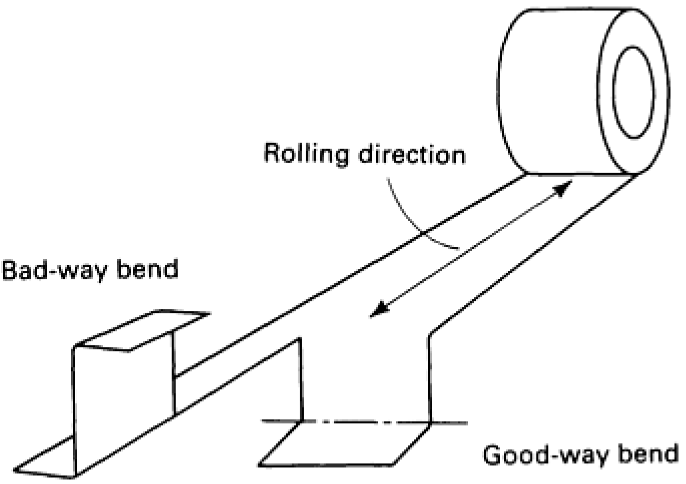

Fig. 13 Bend formability of copper alloys as a function of rolling direction. Bends with the axis transverse to the rolling direction are termed good-way bends; bends with the axis parallel to the rolling direction are bad-way bends. See also Table 6. Source: Ref 3

Figure 14 shows the effects of bend directionality on part layout. This part includes both good-way and bad-way bends. If the part were fabricated from an alloy with strong bend directionality--for example, phosphor bronze (Alloy C51000) in spring temper--the part layout would be restricted to avoid failure at bad-way bends. With alloys such as C68800 or C72500, which exhibit significantly less bend directionality, the part layout is not as restricted. It is not always possible to orient parts to minimize web scrap, regardless of the alloy selected, because tool design and part-handling and transfer costs may override the cost penalty of poor strip utilization.

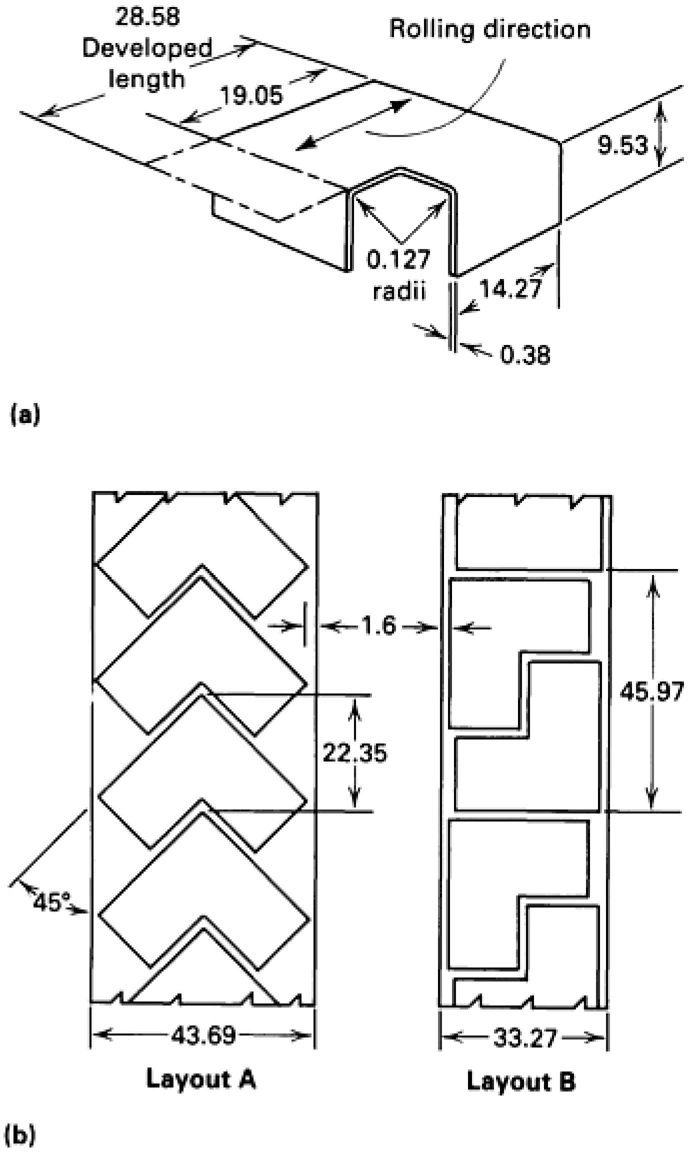

Fig. 14 Impact of bend anisotropy on part layout. (a) Hypothetical part, which has equal-radius bends at 90° orientations in the plane of the strip. Selection of the appropriate copper strip alloy for this application depends on the material strength and the bend properties in the relevant orientations. (b) Potential nesting of blanks for the part shown in (a). Layout A is required for directional alloys such as C51000 and results in 38% scrap; a nondirectional alloy such as C68800 would allow the more efficient layout B, with 23% scrap. Dimensions given in millimeters (1 in. = 25.4 mm). Source: Ref 3.

Special Considerations. The values listed in Table 6 for the minimum bend radii of various alloys as a function of temper are approximate; actual results can deviate because of tool condition and shop practice. The bend performance required also depends on the part application. For example, orange peel (surface roughening) is unacceptable if the part is to be plated or subjected to other finishing operations and if appearance is important. Often, more than one alloy is available that will meet product requirements. In the absence of other limitations, bend formability may be the deciding factor in alloy selection.

The bend performance of copper alloy strip degrades as bend angle increases; that is, a 180° bend is more severe than a 90° bend angle. The effect of bend angle on bend formability is more severe as gage increases and/or bend radius decreases. Bend performance improves as the width-to-thickness ratio of the bend region is reduced to values of less than 8 to 1. Reducing the width-to-thickness ratio can enhance bend performance by as much as three times.

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Forming of Copper and Copper Alloys

Frank Mandigo and Jack Crane, Olin Corporation

Drawing and Stretch Forming

In drawing and stretch forming, a suitably shaped blank of sheet metal is drawn or formed into a die cavity to produce a part. A clamping ring, draw beads, and/or other restraints are usually applied at the periphery of the blank to prevent wrinkling and/or tearing of the blank as it is drawn or formed into the die cavity. The complexity of the edge restraint required is usually directly proportional to the complexity of the final part.

A deep-drawn part is characterized by having a depth greater than the minimum part width. A deep-drawn part can be fabricated in a single drawing step or in multiple steps by redrawing preforms developed by deep-draw, shallow-draw, and/or stretch-forming methods. Ironing can be used during redrawing to control the wall thickness of the final part. Additional anneals may be required between redrawing steps.

A shallow-drawn part has a depth less than the minimum part width and is usually formed in one process step. It can be a final part or the preform for deep drawing.

A stretch-formed part is fabricated by pressing a punch into a blank that is fully or partially restrained at its periphery to develop positive biaxial strain on the part surface. A stretch-formed part can be a final part or the preform for drawing operations. Additional information on drawing and stretch forming is available in the articles "Deep Drawing" and "Stretch Forming" in this Volume.

Materials Characteristics and Effects of Alloy Composition and Temper

Single-Step Drawing. Copper alloys that have high *r* values will provide the largest limiting draw ratio in a single deep-draw step. The *r* value is defined as the ratio of true width strain to true thickness strain in the region of uniform uniaxial elongation during a tensile test. It measures the resistance of a material to thinning. The *r* value correlates with deep-drawing performance because it reflects the difference between the load-carrying capability of the cup sidewall and the compression load required to draw in the flange of the cup or blank during a deep-drawing operation.

The deepest single-step draws (highest LDR) can be made with Alloy C52100, followed by the brasses (in order of decreasing zinc level) and by copper. The LDR of cartridge brass (Alloy C26000) increases as its grain size increases.

Multiple-Step Deep Drawing. The number of redrawing steps and the frequency of intermediate annealing treatments required depend on the initial preform geometry, the extent of ironing required, and the work-hardening rate of the particular alloy. Fewer redrawing steps are required if the preform geometry closely matches that of the final part. The trade-offs involved in selecting a fabrication procedure for the initial preform (for example, deep drawing, shallow drawing, or stretch forming) are complex.

In contrast to single-step deep drawing, in which alloys with high work-hardening rates give the highest LDR, copper alloys with lower work-hardening rates can be redrawn and ironed more times without intermediate annealing. The curves shown in Fig. 15 suggest that Alloy C11000 (electrolytic tough-pitch, ETP, copper) will possess better redrawing and ironing characteristics and will require lower press forces than copper alloys with solute additions of zinc, tin, and/or silicon.

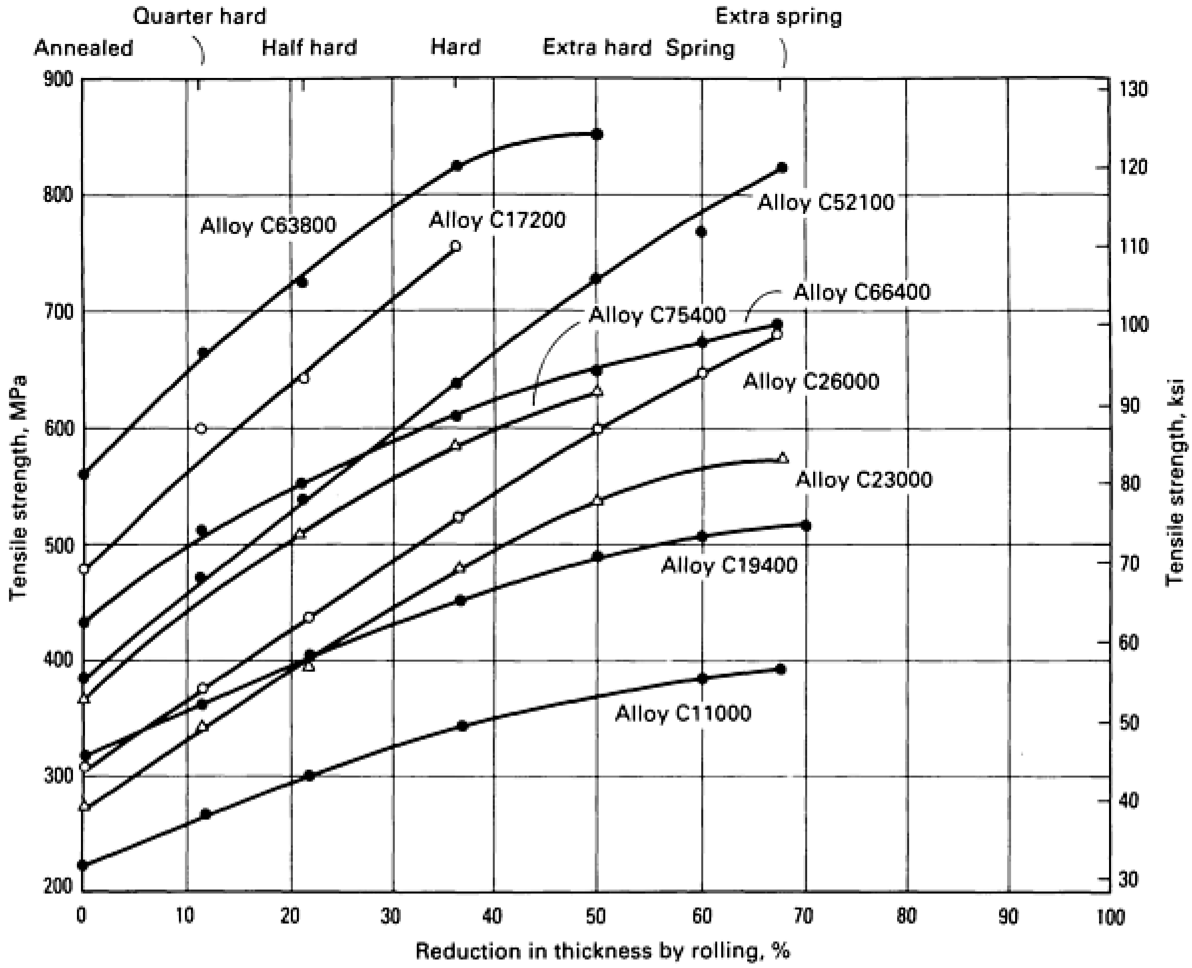

Fig. 15 Redrawing characteristics of 1.0 mm (0.040 in.) thick sheets of several copper alloys. Curves of lower slope indicate a lower rate of work hardening and therefore a higher capacity for redrawing. Source: Ref 3.

In general, successively smaller reductions are selected at each redrawing step to ensure that the punch forces required to decrease the flange circumference do not exceed the load-carrying capability of the part sidewall. The magnitude of the incremental steps of redrawing is decreased if the part sidewall is to be ironed. Ironing increases the strength of the sidewall and flange proportionally to the distance from the cup bottom. In some applications, redrawing capacity can be improved by increasing the temper of the initial strip to enhance the load-carrying capability at the junction of the part sidewall and the cup bottom. The alternative is to use a stronger alloy.

Stretch Forming. The stretch formability of copper alloys correlates with the total elongation measured in a tension test. Annealed alloys that show high work-hardening rates offer the best stretch-forming characteristics. Improved combinations of strength and stretch formability are achieved by solute elements that greatly increase the work-hardening rate. Cold rolling to increase strip temper (strength) significantly reduces stretch formability.

The variation of tensile elongation with cold-rolling reduction for copper alloys is shown in Fig. 16. These data indicate that high-tin and high-zinc alloys offer the best combinations of strength and stretch formability.

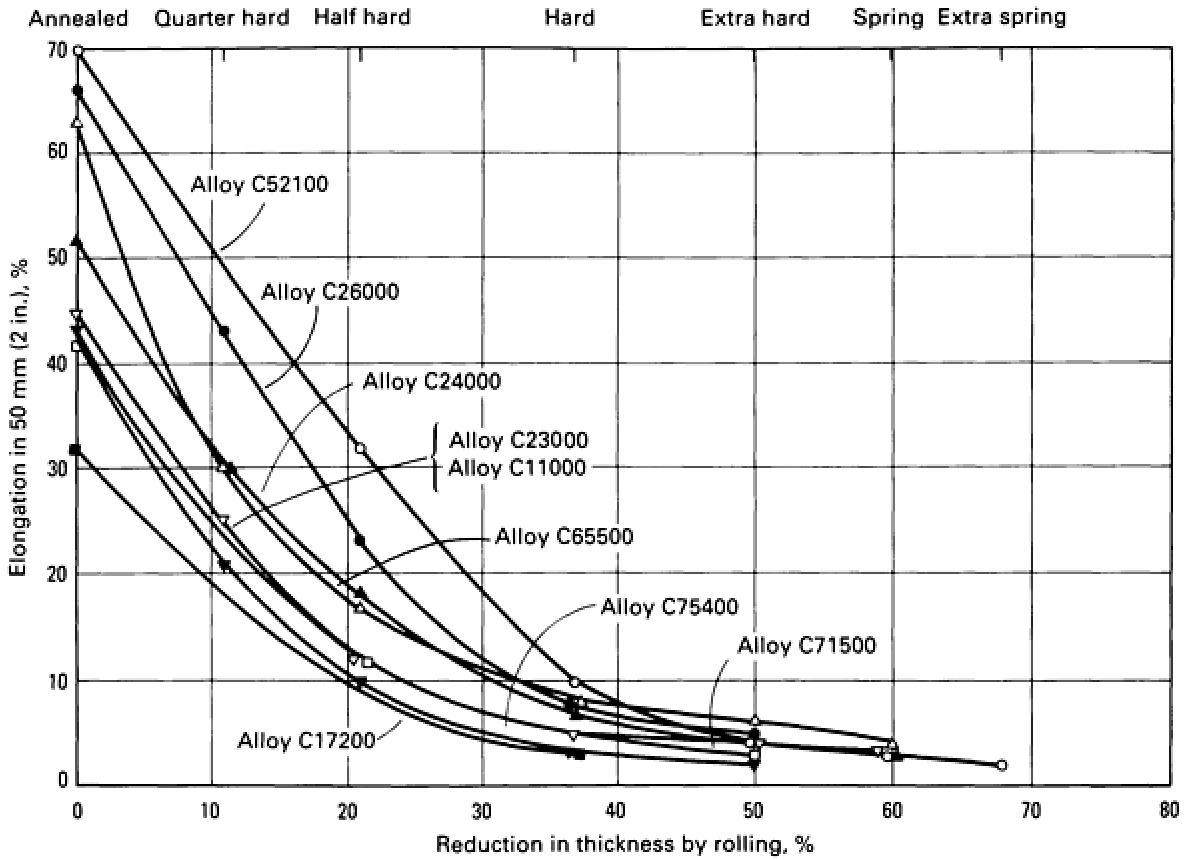

Fig. 16 Stretch-forming characteristics of 1.0 mm (0.040 in.) thick copper alloys. Elongation values for a given percentage of cold reduction indicate the remaining capacity for stretch forming in a single operation. Source: Ref 3.

Specific Characteristics of Copper Alloys. The higher-zinc brasses, such as Alloy C24000 (low brass), Alloy C26000 (cartridge brass), and Alloy C26200 (high brass), have strengths comparable to those of low-carbon steels and high ductilities. They are outstanding materials for deep drawing and stretch forming.

Many other families of copper alloys also have good deep-drawing and stretch-forming properties. Phosphor bronze A (Alloy C51000) has an excellent combination of high strength and high ductility and is used to form deep-drawn thin-wall shells that are then annealed and corrugated to produce bellows with high fatigue strength, corrosion resistance, and excellent flexibility.

The nickel silvers (copper-nickel-zinc) are white copper alloys that also have excellent deep-drawing characteristics similar to those of the high-zinc brasses. However, they have somewhat higher work-hardening rates and require more intermediate annealing for redrawing than cartridge brass. In the fully annealed condition, Alloy C63800 (Cu-3Al-2Si-0.4Co) also exhibits good deep drawability (similar to that of the nickel silvers). Annealed high-zinc leaded brasses are suitable for shallow-drawn parts, such as garden-hose coupling nuts.

Copper-zinc-tin alloys such as C40500, C41100, C42200, and C425000 respond well to drawing and redrawing operations. With regard to deep-drawing properties, C40500 and C41100 are similar to the high-copper brasses, and C42200 and C42500 are similar to C24000.

Beryllium coppers can be drawn in the solution-annealed temper and then age hardened. For example, annealed Alloy C17200 has been deep drawn to 80% reduction before annealing. Parts drawn from beryllium-copper alloys can subsequently be heat treated to produce tensile strengths to 1275 to 1380 MPa (185 to 200 ksi).

There are many other special-purpose coppers and copper alloys. By examining their compositions and mechanical properties carefully and by comparing them with standard alloys, the user can estimate how they will respond in deep-drawing applications.

Grain Size Effects. For the coppers and single-phase alloys, grain size is the basic criterion by which deep drawability and stretch forming are measured. In general, for a given alloy and sheet thickness, ductility increases with grain size, and strength decreases. However, when grain size is so large that there are only a few grains through the thickness of the sheet or strip, both ductility and strength, as measured by tensile testing, decrease. Figure 17 illustrates how elongation changes with grain size for three different thicknesses of Alloy C26000 (cartridge brass). General recommendations for the grain size of annealed strip for drawing and stretch-forming operations are provided in Table 7, along with the expected surface characteristics.

Table 7 Available grain size ranges and recommended applications

Average grain size		Type of operation and surface characteristics
mm	**in.**	
0.005-0.015	0.0002-0.0006	**Shallow forming or stamping. Parts will have good strength and very smooth surface. Also used for very thin metal**
0.010-0.025	0.0004-0.001	**Stampings and shallow-drawn parts. Parts will have high strength and smooth surface. General use for metal thinner than 0.25 mm (0.010 in.)**
0.015-0.030	0.0006-0.0012	**Shallow-drawn parts, stampings, and deep-drawn parts that require buffable surfaces. General use for thicknesses under 0.3 mm (0.012 in.)**
0.020-0.035	0.0008-0.0014	**This grain size range includes the largest average grain that will produce parts essentially free of orange peel. Therefore, it is used for all types of drawn parts produced from brass up to 0.8 mm (0.032 in.) thick**
0.010-0.040	0.0004-0.0016	**Begins to show some roughening of the surface when severely stretched. Good deep-drawing quality in 0.4-0.5 mm (0.015-0.020 in.) thickness range**
0.030-0.050	0.0012-0.002	**Drawn parts from 0.4-0.64 mm (0.015-0.025 in.) thick brass requiring relatively good surface, or stamped parts requiring no polishing or buffing**
0.040-0.060	0.0016-0.0024	**Commonly used for general applications for the deep and shallow drawing of parts from brass in 0.5-1.0 mm (0.020-0.040 in.) thicknesses. Moderate orange peel may develop on drawn surfaces**
0.050-0.119	**0.002-0.0047**	**Large average grain sizes are used for the deep drawing of difficult shapes or deep-drawing parts for gages 1.0 mm (0.040 in.) and thicker. Drawn parts will have rough surfaces with orange peel except where**

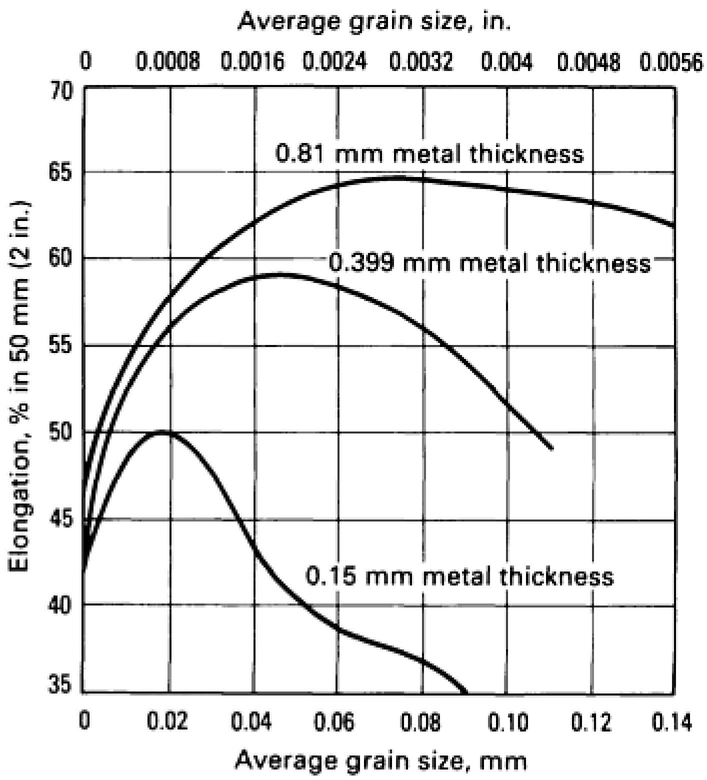

Fig. 17 Elongation versus grain size for Alloy C26000 sheets of various thicknesses. Source: Ref 3.

For optimal deep-drawing and stretch-forming properties, the grain size chosen should provide maximum elongation. With reference to Fig. 17, peak elongation for 0.15 mm (0.006 in.) thick strip occurs at an average grain size of 0.020 mm (0.008 in.). For 0.40 mm (0.0157 in.) thick brass, a range of 0.038 to 0.061 mm (0.0015 to 0.0024 in.) average grain size would provide maximum drawability. For 0.81 mm (0.032 in.) thick material, a range of 0.060 to 0.090 mm (0.0024 to 0.0035 in.) average grain size would give optimal performance.

The surface finish required on the final part is an important consideration when selecting the grain size to be used. When metal with a coarse grain size is drawn or stretch formed, the surface roughens and develops an appearance resembling orange peel. Such a surface is more difficult and costly to polish and buff. Therefore, when a part requiring a buffed surface is to be produced, much effort is expended in designing the tools and process to use brass with a fine grain size.

A classic example of this situation is the one-piece brass or bronze doorknob (Fig. 3a). Such useful and decorative articles are made by the millions, and these types of shapes are difficult to produce on draw presses. These parts are usually produced in transfer presses, and the process can include 15 to 20 operations with one intermediate anneal or partial anneal. The Alloy C26000 or C22000 strip from which these parts are made is usually about 0.76 mm (0.030 in.) thick, and the grain size is usually 0.020 to 0.035 mm (0.008 to 0.0014 in.) or 0.015 to 0.030 mm (0.0006 to 0.0012 in.) to provide sufficient ductility for the part to be drawn without surface roughening.

Special Considerations (Ref 4). A common concern in all drawing operations is the formation of ears at the top of the cup sidewall. Ears occur in preferred directions (usually 45 or 0 to 90°) relative to the strip-rolling direction. Earing reflects the crystallographic texture of the strip. In part manufacture, ears must be trimmed; therefore, nonearing grades of copper and copper alloy strip are preferred for drawn parts.

A common concern in all drawing operations is the formation of ears at the top of the cup sidewall. Ears occur in preferred directions (usually 45 or 0 to 90°) relative to the strip-rolling direction. Earing reflects the crystallographic texture of the strip. In part manufacture, ears must be trimmed; therefore, nonearing grades of copper and copper alloy strip are preferred for drawn parts.

For copper alloys, the reduction in diameter in a single draw (cupping) usually ranges from 35 to 50%, with a 50% reduction corresponding to ideal conditions. Drawing procedures vary widely in commercial practice. Reductions for successive draws of the commonly formed brasses, under favorable operating conditions and without intermediate annealing, are usually 45% for cupping; 25% for the first redraw; and 20, 16, 13, and 10% for subsequent redraws. Greater reductions are usually obtained with blank thicknesses larger than about 1.62 mm (0.064 in.); for blank thicknesses less than about 0.38 mm (0.015 in.), reductions are usually about 80% of the percentages given above. With an annealing operation before each redraw, a reduction of 35 to 45% in each successive redraw can be obtained under favorable operating conditions, assuming that the accompanying reduction in wall thickness is acceptable.

Die radius usually varies from about twenty times the metal thickness for material 0.127 mm (0.005 in.) thick to about five times the metal thickness for material 3.18 mm (0.125 in.) thick. Radii of this size prevent high stress concentrations at the die opening, which can lead to tearing in subsequent draws. Sharper radii are needed for flanged shells and for meeting special design requirements.

The punch radius, except for the final stages of drawing, is usually less than one-third of the punch diameter, or four to ten times the metal thickness. Clearance between punch and die is maintained at values that produce at least a slight amount of ironing of the sidewalls.

References cited in this section

3. J.H. Mendenhall, Ed., *Understanding Copper Alloys,* Olin Corporation, 1977
4. "Advanced Sheet Metal Forming Course," Metals Engineering Institute Home Study and Extension Course, American Society for Metals, 1979

Frank Mandigo and Jack Crane, Olin Corporation

Coining

Nature of the Operation. Coining is a cold-forming process in which the work metal is compressed between two dies so as to fill the depression of both dies in relief or to reduce the strip thickness. The most familiar coining operation is the minting of coins. However, one of the most common uses of coining is in reducing the thickness or width of electrical and electronic connectors and leadframe leads.

Materials Characteristics. The ability of a material to be coined is determined by its strength and work-hardening rate. In general, copper, the lower-zinc brasses, the lower-alloy nickel silvers, and the copper nickels, which all exhibit low work-hardening rates, exhibit good coinability (Fig. 15).

Forming of Copper and Copper Alloys

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Spinning

Nature of the Operation. Spinning is a method of forming sheet metal or tubing into seamless hollow cylinders, cones, hemispheres, or other circular shapes by a combination of rotation and force. Manual and power-automated equipment is used for spinning copper alloys. More information is available in the article "Spinning" in this Volume.

Materials Characteristics. The principal materials factors that determine the spinnability of copper alloys are plastic-strain ratio *r*, total available elongation, and work-hardening rate. In general, alloys with high *r* values, high tensile elongation, and low work-hardening rates exhibit the highest spinnability.

Effects of Alloy Composition and Temper. Tough-pitch copper (Alloy C11000) is the easiest copper material to spin and usually does not require intermediate annealing. Brasses, except for the multiphase alloy Muntz metal (C28000), are readily spun, although the higher-zinc brasses sometimes require intermediate annealing. Tin brasses containing at least 87% Cu require higher spinning pressure and more frequent annealing than brasses. Nickel silvers that contain at least 65% Cu, as well as the copper nickels, are also well suited for spinning. Phosphor bronzes, aluminum bronzes, and silicon bronzes are difficult to spin, but can be spun into shallow shapes under favorable conditions. Copper alloys that are difficult to spin include Muntz metal, nickel silvers containing 55% Cu or less, beryllium coppers, alloys containing more than about 0.5% Pb, naval brass (C46400), and other multiphase alloys.

The single-phase high-strength copper alloys can be heated for spinning to reduce the force required to permit the spinning of thicker material or to permit more severe deformation, provided the increased cost for heating is justified. The forming characteristics of Muntz metal, extra-high-leaded brass, and naval brass are also improved at elevated temperature, but special precautions must be taken to avoid even the unintentional heating of the workpiece in spinning brasses that contain 0.5% Pb or more and more than 64% Cu.

Annealed tempers are almost always used in spinning copper alloys. Larger grain sizes (lower hardnesses) are easier to spin; finer grain sizes may be needed to meet surface finish requirements.

Although stock as thin as 0.1 mm (0.004 in.) can be manually spun under special conditions, manual spinning is usually restricted to thicknesses of 0.51 to 6.35 mm (0.020 to 0.250 in.). Powered equipment is used in the upper part of this range, and stock thicknesses in excess of 25.4 mm (1 in.) can be shaped by hot power spinning.

Applications. Typical products that are spun from copper alloys include bell-mouth shapes for musical instruments, lighting fixture components, vases, tumblers, decorative articles, pressure vessel parts, and other circular parts with bulged or recessed contours.

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Contour Roll Forming

Nature of the Operation. Contour roll forming is an automated high-speed production process that is capable of producing tubular, box, angular, and folded parts of varied and complex shapes (see the article "Contour Roll Forming" in this Volume). Auxiliary operations such as notching, slotting, punching, and embossing can be combined with contour roll forming.

The materials characteristics that determine the roll-forming capability of copper alloy strip are the same as those that govern bend and stretch formability (see the sections "Bending" and "Drawing and Stretch Forming" in this article).

Alloy and Temper Effects. The bend properties given in Table 6 provide an indication of the relative suitability of copper alloys for contour roll forming. Annealed tempers are needed for complicated shapes and parts with extremely sharp bends or for severe stretch forming.

Applications. Contour roll forming is used less extensively with copper alloys than with steel and aluminum alloys because there are fewer copper alloy parts that are made in sufficient volume to be produced economically by this type of forming operation. Applications are primarily in the automotive and architectural industries.

Rubber-Pad Forming

Nature of the Operation. In this process, the rubber pad usually serves as the female die, in conjunction with an inexpensive male punch. The pad is practically incompressible, and it transmits pressure in all directions in the same manner as hydraulic fluid. Rubber-diaphragm forming uses hydraulic fluid behind the rubber pad. The most important reasons for using rubber-pad forming in preference to conventional press techniques or other production methods are improved formability, low tooling costs, and freedom from marking of workpiece surfaces. This is the most cost-effective method of fabricating one-piece doorknobs.

Deep drawing by rubber-diaphragm or Marforming techniques often permits a 65% reduction of diameter in a single draw, without producing wrinkles or surface defects that could require expensive finishing operations. More information on the Marform process and other rubber-pad forming techniques is available in the article "Rubber-Pad Forming" in this Volume.

Materials Characteristics. The materials properties of greatest importance in rubber-pad forming are the same as those that control strip performance in metal dies; that is, deep drawing is dependent on the plastic-strain ratio r, stretch forming is dependent on tensile elongation, bending is determined by strip ductility, and so on.

Effects of Alloy Composition and Temper. The same principles can be used to select the appropriate alloy composition and temper for rubber-pad die forming that are used for parts formed with conventional metal dies.

Forming of Copper and Copper Alloys

Frank Mandigo and Jack Crane, Olin Corporation

Specialized Forming Operations

Hydraulic Forming. Copper alloys are sometimes formed by applying direct hydraulic pressure to the surface of the workpiece in order to shape the workpiece against a rigid die. This procedure can be used to form grooves on large, thin, flat sheets and to shape small parts to irregular contours. Tool cost is low, but the method is ordinarily applicable only to small-lot production because of comparatively low production rates.

Embossing and swaging, which are closely related to coining (being compressive or deformation operations), are also frequently used in the cold forming of copper alloys. The principles of alloy selection described for coining apply equally to embossing and swaging. However, embossing (impressing letters, numerals, or designs into a surface by displacing metal to either side) can be done on any copper alloy, with special attention to tooling and selection of temper on the less formable alloys. Swaging is often used for the production of complicated electrical contacts from copper or brass.

Electromagnetic forming, also known as magnetic pulse forming, is a process for forming metal by the direct application of an intense, transient magnetic field. The workpiece is formed without mechanical contact by the passage of a pulse of electric current through a forming coil (see the article "Electromagnetic Forming" in this Volume).

Electromagnetic forming can be used on copper and some brasses because of their high electrical conductivity and excellent formability. Metals with a resistivity greater than about 16 $\mu\Omega\cdot$ cm are formed by the use of a copper or aluminum electromagnetic driver that is one to three times the thickness of the work metal. Thermally or electrically conductive joints and structural joints are produced in a single forming operation. Field shapers are frequently used to concentrate the forming force.

Electrical connections are made by electromagnetically swaging a copper band onto the end of stranded electrical conductor wire before insertion into a brass terminal. Optimal conductivity with 100% mechanical strength and long life under severe service conditions are obtained by using swaging forces great enough to compact the strands of the conductor so that a cross section of the joint appears to be essentially solid copper.

Special Forming Considerations for Conductive Spring Materials. Increasingly, contact designers are developing parts that rely on stepped or tapered beam thickness for optimal deflection or normal force characteristics. Some designs involve complex geometries requiring high formability in some regions (as for crimp connections) coupled with high strength in other regions (to resist permanent set in spring connections). Stepped or tapered contact beam thicknesses can be achieved by coining heavier-gage strip in progressive dies. This practice, however, rapidly work hardens copper alloys and reduces their formability. Die progressions that include the forming of contacts after a coining operation must incorporate more generous minimum bend radii than those suggested in the product literature of the supplier.

Figure 18 shows this change in formability for a mill-hardened temper of Alloy C17200 that was subjected to coining up to 50% reduction in area and simulated by cold rolling after mill hardening. To avoid this formability problem, strip can be purchased with variable gage across the slit width, which is produced by profile milling or skiving or by the longitudinal electron beam welding of dissimilar thicknesses of strip. The need for localized high formability can also be met by the longitudinal electron beam welding of dissimilar metals, combining, for example, ductile C19500 with high-strength mill-hardened C17200.

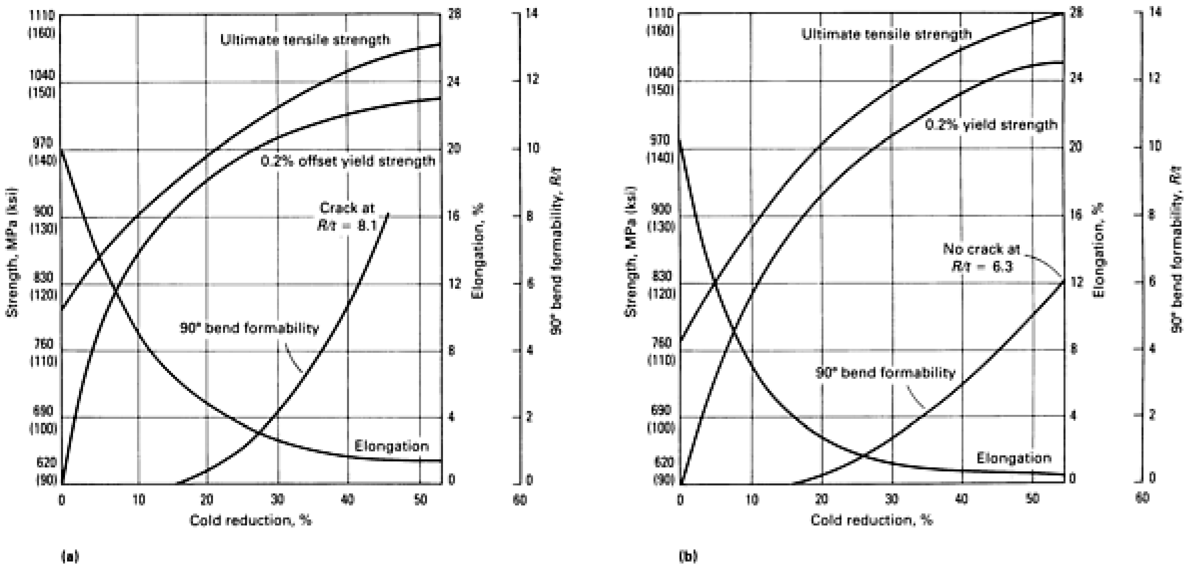

Fig. 18 Change in formability as a function of the coining of Alloy C17200 in longitudinal (a) and transverse (b) directions. The effect of coining is simulated by cold reduction. Original strip thickness in both cases was 0.41 mm (0.016 in.). Bend formability is measured as the ratio of bend radius *R* to strip thickness *t*.

An emerging electron beam application is the localized thermal softening of mill-hardened copper alloy strip to provide increased formability with no sacrifice in strength in the remainder of a contact. Examples of these unique copper alloy strip forms are shown in Fig. 19.

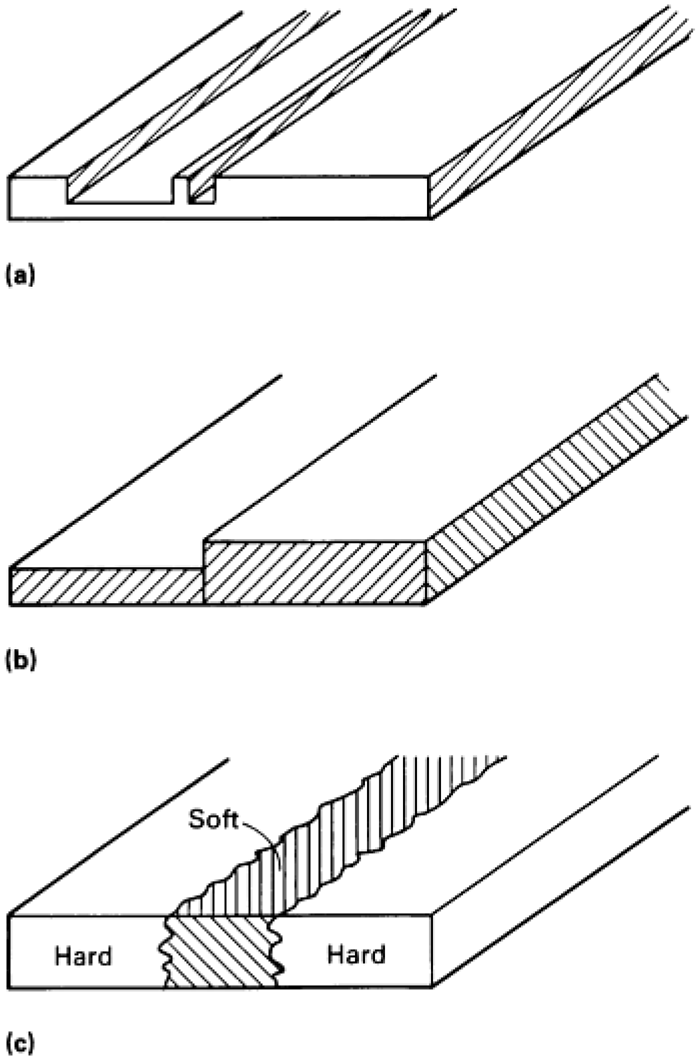

Fig. 19 Special treatment of copper alloy strip for optimized combinations of formability and spring characteristics. (a) Profile milled strip. (b) Dissimilar thicknesses longitudinally welded; this method can also be used to join dissimilar alloys. (c) Localized heat treatment (electron beam softening).

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Springback

Springback is the elastic recovery that occurs in a plastically deformed part when it is released from tooling. It causes the final part to have a geometry different from that of the press tooling. The springback that occurs in a bending operation is shown schematically in Fig. 20. Springback must be taken into account in design and materials selection.

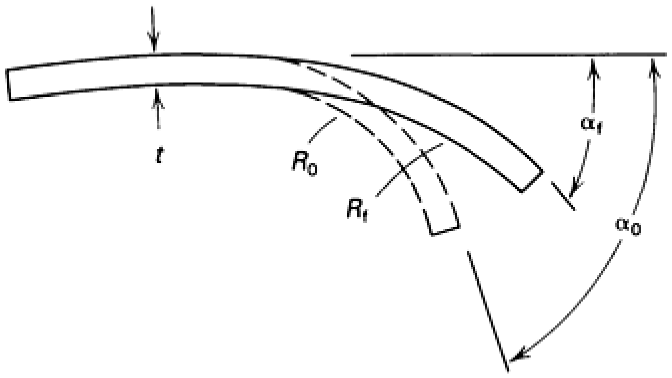

Fig. 20 Schematic of springback in a bending operation. *t* is sheet thickness, R_o and, α_o are the die radius and bend angle, and R_f and α_f are the part radius and bend angle after springback.

Springback depends on alloy, temper, thickness, bend radius, and the angle of bend. For fixed tooling and press conditions, springback increases as the strength of the copper alloy strip increases. Therefore, springback is increased by cold rolling to increase strip temper and/or by alloy additions that increase strength. The springback behavior of three copper alloys (C21000, C26000, and C35300) is shown in Fig. 21. These data indicate that springback increases with increasing bend radius and decreasing strip gage. Springback values for tempers or bend radii not shown can be interpolated from Fig. 21. Some strip suppliers provide springback data upon request.

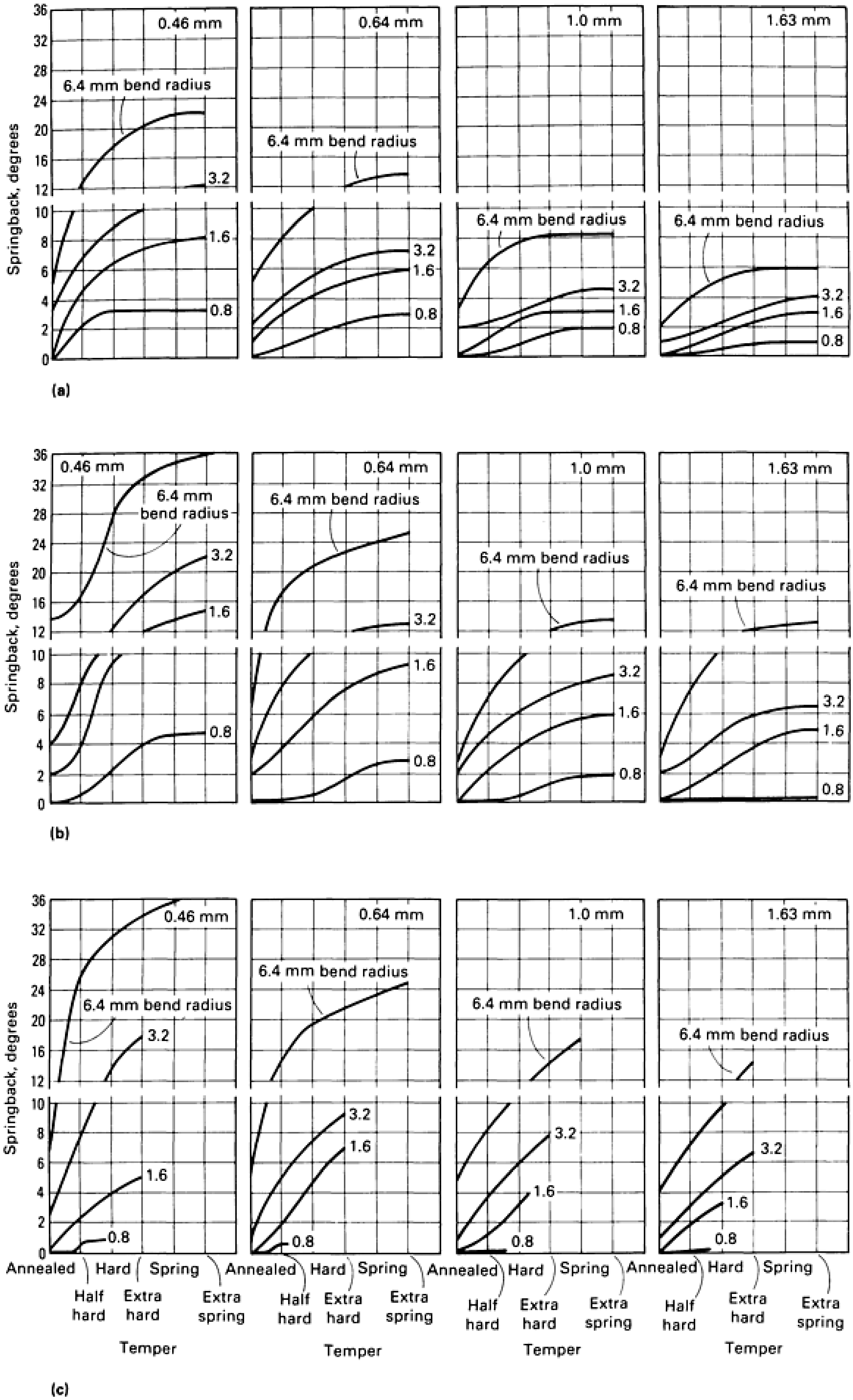
0.46 mm
0.64 mm
1.0 mm
1.63 mm
6.4 mm bend radius
3.2
1.6
0.8
Springback, degrees
0
2
4
6
8
10
12
16
20
24
28
32
36
(a)
(b)
(c)
Annealed
Half hard
Hard
Extra hard
Spring
Extra spring
Temper

Fig. 21 Springback behavior of copper alloys as a function of temper, sheet thickness, and bend radius (90° bends). (a) Alloy C21000. (b) Alloy C26000. (c) Alloy 35300.

Three techniques are commonly used to compensate for springback: overbending, restriking, and the use of special dies. Over-bending simply deforms the part to a larger bend angle so that it is at the desired value after springback. Restriking in original dies reduces springback in much the same manner as overbending, that is, by the introduction of additional plastic deformation. Special dies often use coining action at bend radii to deform the metal plastically in the bend area beyond the elastic limit. In other die modifications, the metal is pinched slightly at the bend region. When special dies are used, careful control must be exercised because excessive thinning can cause part failure during bending or can make the part susceptible to early failure in service.

Forming of Copper and Copper Alloys

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Forming Limit Analysis

Forming limit analysis provides the means to assess sheet metal formability over a wide range of forming conditions, including drawing, bending, and stretching. The amount of deformation that occurs during sheet forming, that is, the strain state, is given in terms of, or related to, major and minor strains (e_1 and e_2, respectively) measured from fiducial markings printed or etched onto strip surfaces prior to fabrication. The analysis requires two curves:

- A forming limit curve (FLC), which indicates the ability of the material to distribute localized strain
- A limiting dome height (LDH) curve, which indicates the overall ductility for forming of the material

These empirically determined curves show the biaxial strain or deformation limits beyond which failure may occur in sheet metal forming. More information on the development and use of these curves is available in the article "Formability Testing of Sheet Metals" in this Volume; the use of computers to generate such data is described in the article "Process Modeling and Simulation for Sheet Forming" in this Volume.

Forming limit and limiting dome height curves for 13 copper alloys are shown in Fig. 22 and 23. Table 8 lists UNS designations, common names, alloy compositions, and tempers for the alloys tested. These data indicate that, in annealed tempers, high-copper and copper-zinc alloys exhibit the highest FLC values, followed closely by Alloys C72500, C51000, and C74300; these materials in turn are slightly better than Alloys C19400, C75200, and C70600. Increasing the temper by cold rolling decreases forming capability, as shown in Fig. 22. The LDH data essentially follow the trend shown in FLC behavior.

Table 8 Coppers and copper alloys evaluated using forming limit analysis

See Fig. 22 and 23 for results of analysis.

UNS designation	Common name	Material conditions applicable to FLCs and LDH curves
C10200	Oxygen-free copper	**Annealed, 0.66 mm (0.026 in.) thick, 0.014 mm (0.0006 in.) grain, 234 MPa (34 ksi) UTS[(a)]**
C11000, lot 1	ETP copper	**Annealed, 0.74 mm (0.029 in.) thick, 0.016 mm (0.00063 in.) grain, 224 MPa (32.5 ksi) UTS[(b)]**

C11000, lot 2	ETP copper	**Half hard, 0.69 mm (0.027 in.) thick, 268 MPa (38.8 ksi) UTS, 20% tensile elongation[c]**
C15500	Silver copper	**Annealed, 0.71 mm (0.028 in.) thick, 0.009 mm (0.00035 in.) grain, 288 MPa (41.8 ksi) UTS**
C17200	Beryllium copper	**Annealed, 0.25 mm (0.010 in.) thick, 0.019 mm (0.00075 in.) grain, 491 MPa (71.2 ksi) UTS**
C19400	HSM copper	**Annealed, 0.69 mm (0.027 in.) thick, 319 MPa (46.3 ksi) UTS, 29% tensile elongation[d]**
C22000	Commercial bronze	**Annealed, 0.69 mm (0.027 in.) thick, 0.017 mm (0.00067 in.) grain, 234 MPa (34 ksi) UTS[d]**
C23000	Red brass	**Annealed, 0.69 mm (0.027 in.) thick, 0.024 mm (0.00094 in.) grain, 293 MPa (42.5 ksi) UTS[e]**
C26000, lot 1	Cartridge brass	**Annealed, 0.64 mm (0.025 in.) thick, 0.025 mm (0.00098 in.) grain, 345 MPa (50 ksi) UTS[f]**
C26000, lot 2	Cartridge brass	**Half hard, 0.69 mm (0.027 in.) thick, 407 MPa (59 ksi) UTS, 28% tensile elongation[e]**
C26000, lot 3	Cartridge brass	**Full hard, 0.51 mm (0.020 in.) thick, 531 MPa (77 ksi) tensile strength**
C51000	Phosphor bronze A	**Annealed, 0.69 mm (0.027 in.) thick, 0.014 mm (0.0006 in.) grain, 374 MPa (54.3 ksi) UTS**
C70600	Copper nickel, 10%	**Annealed, 0.81 mm (0.032 in.) thick, 0.016 mm (0.00063 in.) grain, 361 MPa (52.4 ksi) UTS**
C72500	Copper-nickel-tin alloy	**Annealed, 0.69 mm (0.027 in.) thick, 0.023 mm (0.0009 in.) grain, 356 MPa (51.6 ksi) UTS**
C74300	Nickel silver	**Annealed, 0.69 mm (0.027 in.) thick, 0.035 mm (0.0014 in.) grain, 387 MPa (56.1 ksi) UTS**
C75200	**Nickel silver**	**Annealed, 0.69 mm (0.027 in.) thick, 0.020 mm (0.0008 in.) grain, 405 MPa (58.7 ksi) UTS**

(a) UTS, ultimate tensile strength.

(b) LDH curves are medians based on 0.69, 0.74, and 0.79 mm (0.027, 0.029, and 0.031 in.) thickness data.

(c) LDH curves are medians based on 0.64, 0.69, and 0.79 mm (0.025, 0.027, and 0.031 in.) data.

(d) LDH curves are medians based on 0.69 and 0.74 mm (0.027 and 0.029 in.) thickness data.

(e) LDH curves are medians based on 0.69, 0.79, and 0.81 mm (0.027, 0.031, and 0.032 in.) data.

(f) LDH curves are medians based on 0.66 and 0.69 mm (0.026 and 0.027 in.) data.

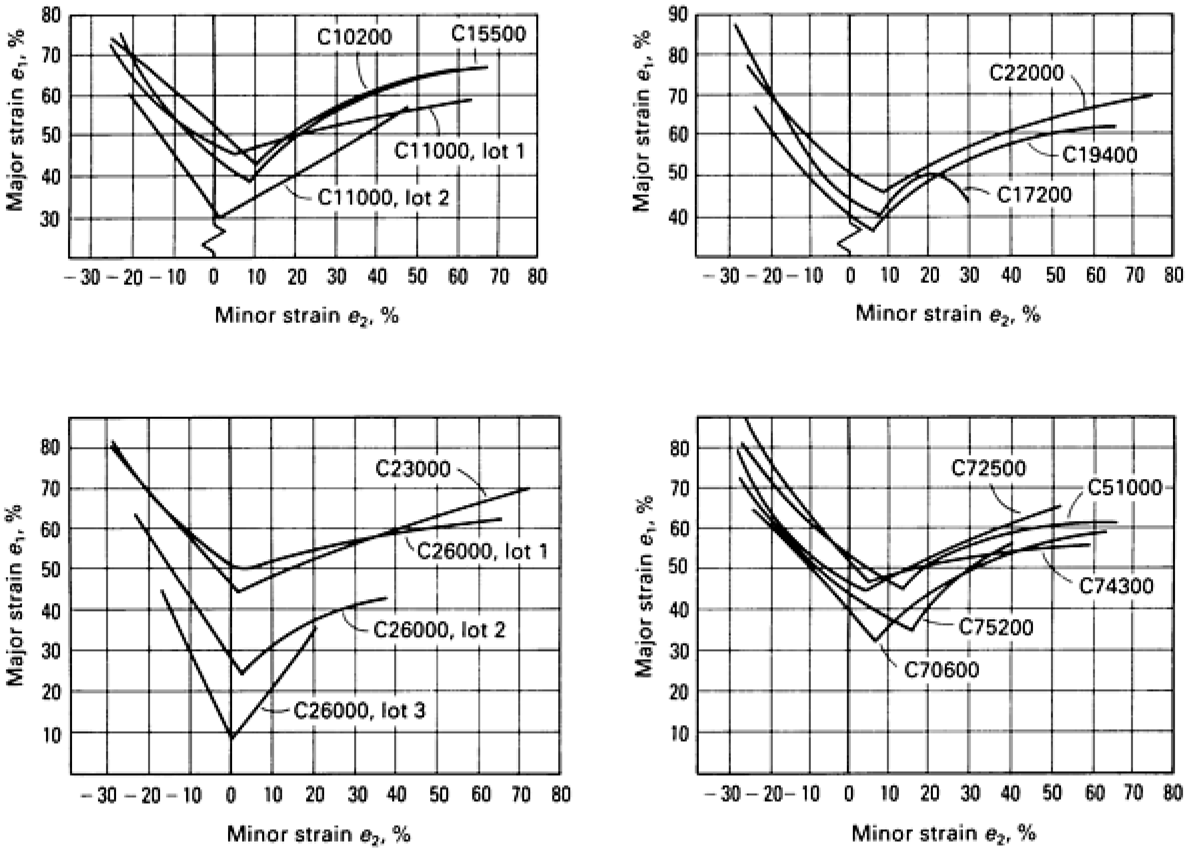

Fig. 22 Forming limit curves for selected copper alloys. FLCs reveal local ductility during forming. See Table 8 for material designations, thicknesses, and tempers. Source: Ref 5, 6.

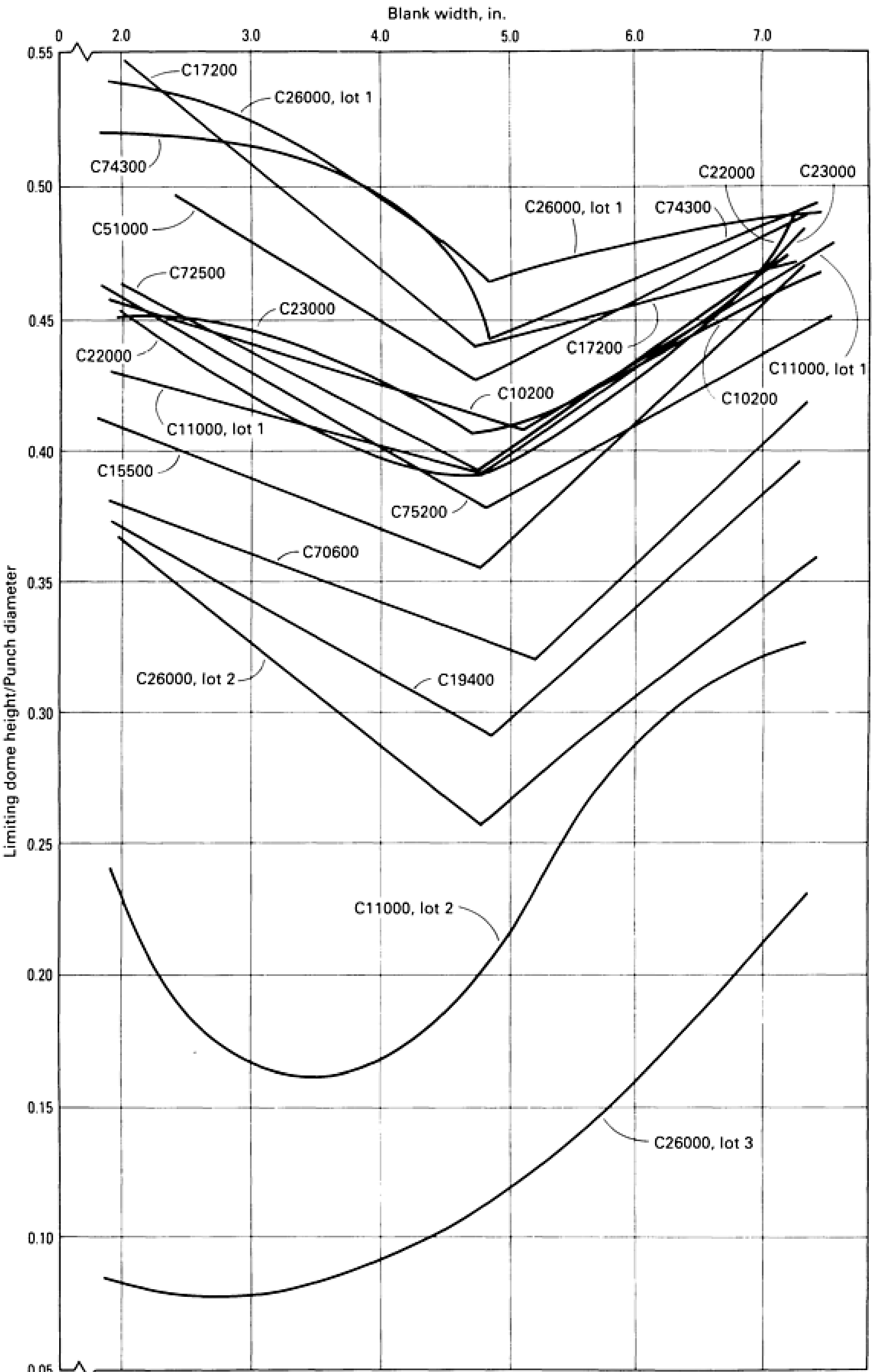
Blank width, in.
0
2.0
3.0
4.0
5.0
6.0
7.0
0.55
0.50
0.45
0.40
0.35
0.30
0.25
0.20
0.15
0.10
0.05
Limiting dome height/Punch diameter
C17200
C26000, lot 1
C74300
C51000
C22000
C23000
C26000, lot 1
C74300
C72500
C23000
C22000
C17200
C11000, lot 1
C10200
C11000, lot 1
C10200
C15500
C75200
C70600
C26000, lot 2
C19400
C11000, lot 2
C26000, lot 3
0
51
76
102
127
152
178
203
Blank width, mm

Fig. 23 Limiting dome height curves for copper and copper alloys. LDH curves illustrate the overall ductility of the coppers and copper alloys evaluated. See Table 8 for material designations, thicknesses, and tempers. Source: Ref 5, 6.

Solving Forming Problems. In addition to displaying the relative formability of one material versus that of another, forming limit and limiting dome height curves are valuable for identifying the cause of a sudden production problem that might arise from changes in tooling, lubrication, or material suppliers. This permits the forming process to be modified to maximize formability and productivity.

The most direct approach for determining if a sudden forming problem is materials related is to compare the LDH curve for the material with that of the control lot of known good material. If only one region of the part is subject to critical strains, it may be necessary only to test the blank width that will produce that critical value of minor strain. If the LDH curve of the new material is the same as that of the control lot, then tooling or lubrication are suspect. If the LDH curve of the new material is below that of the control lot, the material is the problem.

The best way to determine whether tooling or lubrication conditions have changed is to form a gridded sample under current tool conditions from a control lot held in inventory. Strain distribution and critical grid strains measured on this sample can be placed on the established forming limit curve and compared with those before the problem arose in order to establish their relationship to known, safe strain levels. If changes are detected, they can often be remedied by adjusting press conditions to change the magnitude of stretch or draw components.

This is illustrated in Fig. 24. Point A on this forming limit curve represents the strains in the critical region of a part when the part was being formed satisfactorily. Point B represents the critical strains when forming became a problem because the major and minor strains were too high. Draw beads, blank hold-down pressure, blank size, and/or lubrication can be modified to change the amount of major and minor strains. The effects on critical strain can be compared on the forming limit curve to ensure that the adjustments will indeed enable the part to be formed.

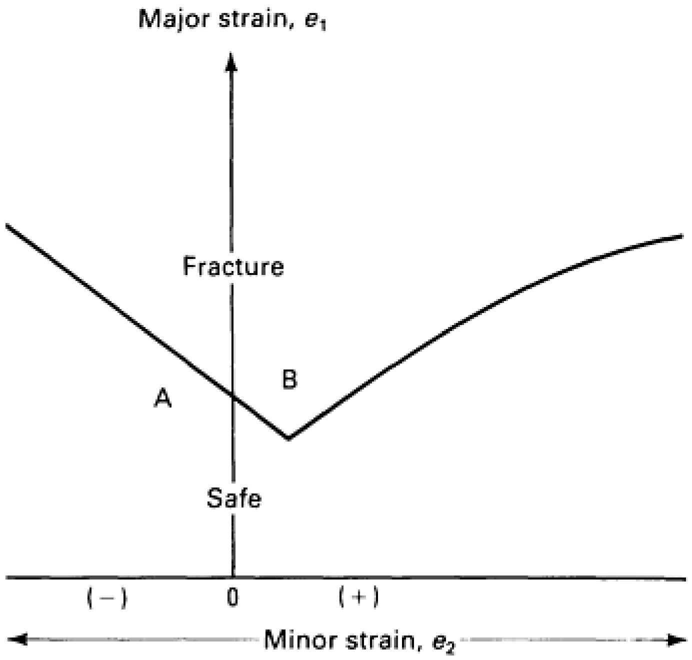

Fig. 24 Effect of tooling or lubrication on critical strain. Inadvertent changes in tooling or lubrication can shift strain from Point A to Point B, causing parts that previously were readily formed to fail during forming. Source: Ref 5, 6.

A similar approach can be used to adjust the forming operation so that a less ductile material can be formed. Figure 25, for example, shows forming limit curves for two materials (A and B) and the critical strain combination (point X) measured on a formed part. Material A forms successfully, but Material B fractures during forming, as indicated by the location of point X relative to the forming limit curve of each material.

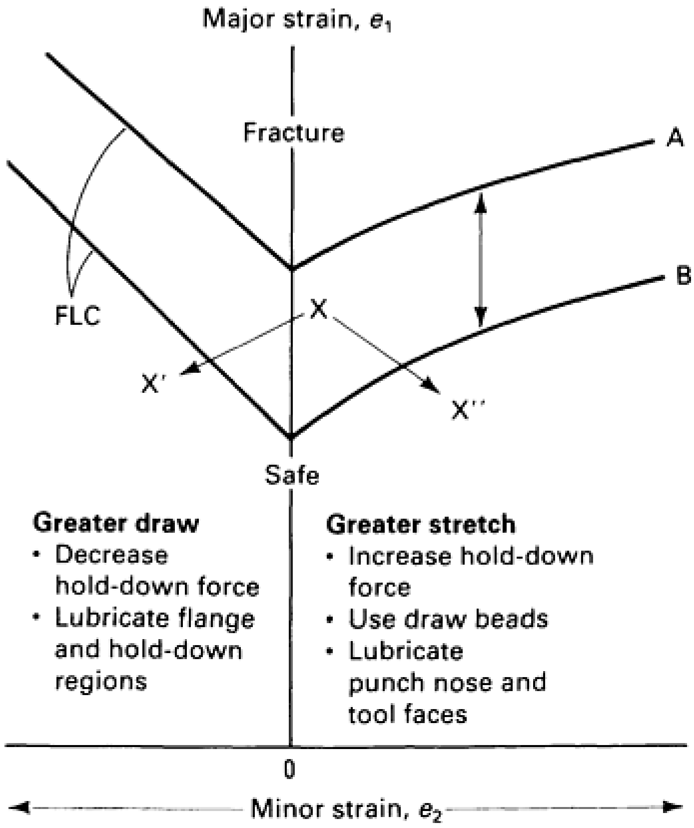

Fig. 25 Effect of changes in forming operation on critical strain. Shifting strain from X to X' by increasing draw or from X to X" by increasing stretch permits Material B to be used in place of Material A despite its lower ductility. Source: Ref 5, 6.

Because of the shape of the forming limit curves, it is possible to maintain approximately the same e_1 value for point X but to fall in the safe region by changing e_2, as indicated by points X' and X". In this case, moving toward X' requires that the draw component be increased during forming; moving toward X" requires that the stretch component be increased. Either can be accomplished by altering lubrication, tooling, and/or blank hold-down pressure, thus enabling the part to be formed in Material B.

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Forming of Copper and Copper Alloys

Frank Mandigo and Jack Crane, Olin Corporation

Property Requirements for Various Formed Products (Ref 7)

Because the selection of an alloy begins with performance requirements even before formability is considered, this section offers a brief discussion of alloy effects relative to the performance of copper alloys. When these factors are matched with the best formability considerations, optimal material selection is attained.

Electrical Conductors. High conductivity is a primary requisite for many conductors, but not all conductors require high conductivity. Strength and resistance to creep or softening can be traded for some loss of conductivity. Thermal conductivity goes hand in hand with electrical conductivity and is usually required in all conductors operating in the high-amperage range.

Terminals and connectors are used in both electronic and electrical applications. Electrical circuits require greater current-carrying ability than electronic circuits, for which low-to-moderate conductivity will often suffice. Good formability at the required strength level, well-defined load deflection characteristics, and resistance to stress relaxation are usually required for both electrical and electronic connectors. Corrosion and stress-corrosion resistance and solderability are also often demanded.

Other Electronic Applications. The primary electronic application for copper and copper alloys, other than connectors and printed circuits, is as leadframes for semiconductor devices. Transistors, diodes, and integrated circuits can be fabricated on a semiconductor chip often less than 5 mm (0.2 in.) square. The chip is bonded to a substrate or leadframe, which serves both structurally and electrically to connect it to the outside world. Where copper alloys can be used, the materials requirements are expressed in terms of cost, conductivity, strength, softening resistance, formability, low-cycle fatigue resistance, and surface characteristics (such as plating, wire bonding, and plastic molding compound adherence).

Hollow Ware, Flatware, and Decorative Applications. The production of hollow ware products requires materials with good drawability. In addition, the material must have good solderability, corrosion resistance, sufficient strength to resist denting during manufacture or use, and good buffing and plating characteristics; most hollow ware products are silver or gold plated. The least expensive alloys that meet these criteria are the 10 to 30% Zn brasses. The lower zinc levels are used for multiple-redrawing applications, and the higher zinc levels are for parts demanding higher strength and/or deep-drawing capability. Copper and phosphorus-deoxidized copper are significantly softer and are primarily used for decorative applications without plating, where the red color of the metal is considered appealing. They have adequate drawability for parts that do not require sidewall ironing and a high work-hardening rate. Phosphorus-deoxidized copper is required where brazing is needed.

Flatware items are generally produced by roll forming. In addition to good formability, flatwork alloys must have good solderability, corrosion resistance, good buffing and plating characteristics, and low cost. Embossed items use annealed tempers of materials with sufficiently low work-hardening rates to give faithful reproduction of detailed patterns. Copper-zinc and copper-nickel-zinc alloys offer the required combination of properties; the zinc content is varied to suit the work hardening needed. A copper-nickel-zinc alloy that has a silvery color is generally used for silver-plated flatware, and Cu-30Zn is used for gold-plated flatware to minimize the color contrast between base metal and plate if damage to the plate occurs.

Heat exchangers require good thermal conductivity, corrosion and stress-corrosion resistance, joinability, and strength at modest cost. These requirements vary in importance for each application. Copper and copper alloys offer good combinations of these properties. The two major heat exchanger applications are steam condenser tubing and automotive radiators.

Condenser tubing must withstand potentially corrosive cooling water as well as the volatile components carried by the steam, which condense on the tubing. Corrosion requirements are paramount, but strength at elevated temperature and thermal conductivity are also required.

The copper alloys commonly used in power utility condensers include arsenic-, antimony-, or phosphorus-inhibited brasses; aluminum bronzes; or copper-nickels, depending on corrosion and stress-corrosion requirements. For automotive radiators, corrosion resistance, thermal conductivity, and fabricability are the primary requisites. Certain applications require strength at elevated temperature. Fabricability demands the ability to solder and to braze. Resistance to both atmospheric corrosion and corrosion by heat-transfer media and their decomposition products is required. Cooling fins are made of pure copper or high-copper copper alloys.

Coinage. General requirements include low cost, attractive appearance and high density for high denominations (to give the impression of intrinsic value), tarnish and corrosion resistance, modest strength, and ability to be coined easily. Specific additional requirements for vending machine use include control of conductivity, density, magnetic permeability, and eddy-current response. Apart from those few coins that are fabricated from stainless steel, most non-precious metal coins are made of copper alloys, which can meet these requirements.

Ammunition. The cartridge case that contains the explosive powder and primer for ammunition is made by a cup and draw process; a blanked disk is cupped, drawn to extend the sidewall, and redrawn for the same purpose. The drawing process is repeated until the wall is sufficiently thinned and extended. The case can be annealed between a series of draws to permit sufficient extension without cracking. The primary materials requirements are related to fabricability, but stress-corrosion resistance and strength are also needed.

Cartridge brass (Cu-30Zn) is the most widely used copper alloy for shells and other ammunition; hence its name. Cartridge brass offers the best materials compromise for such applications, having excellent deep drawability, moderate redrawability, and sufficient strength. In press operations, its low coefficient of friction and absence of refractory oxides contribute toward low press forces, low tool maintenance, and good surfaces on the parts being formed. It is also low in cost. Formed cartridge cases must be stress-relief annealed to minimize the possibility of stress-corrosion cracking.

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Forming of Copper and Copper Alloys

Frank Mandigo and Jack Crane, Olin Corporation

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Forming of Magnesium Alloys

Introduction

THE PRINCIPAL DIFFERENCE between forming magnesium alloys and forming steel, aluminum, or copper is temperature. Although some forming of magnesium alloys can be done at room temperature, elevated temperatures are used in most applications.

Cold Forming

Cold forming of magnesium alloys is restricted to mild deformation with a generous bend radius.

Bend Radii. Cylinders and cones can be formed from magnesium alloys at room temperature by using standard power rolls. Simple flanges can be press formed at room temperature. Table 1 gives minimum radii for fast bending at room temperature, as in a press brake. Slightly smaller bend radii than those given in Table 1 may be used when forming speeds are slower, as in a hydraulic press or when proved by trial and error.

Table 1 Recommended minimum bend radii for the fast forming of magnesium alloys at room temperature

Alloy and temper	Minimum bend radius in terms of workpiece thickness, *t*
Sheet 0.51-6.3 mm (0.020-0.249 in.) thick[(a)]	
AZ31B-O	**5.5**
AZ31B-H24	**8**
HK31A-O	**6**
HK31A-H24	**13**
HM21A-T8	**9**
HM21A-T81	**10**
LA141A-O	**3**
ZE10A-O	**5.5**
ZE10A-H24	**8**
Extruded flat strip 22.2 × 2.3 mm (0.875 × 0.090 in.) thick	
AZ31C-F	**2.4**
AZ31B-F	**2.4**
AZ61A-F	**1.9**
AZ80A-F	**2.4**
AZ80A-T5	**8.3**
HM31A-T5	**11**

ZK21A-F	**15**
ZK60A-F	**12**
ZK60A-T5	**12**

(a) Minimum bend radii are based on bending a 152 mm (6 in.) wide specimen through 90°

Surface Protection. In low-production cold forming, a common method of preventing surface damage is to apply tape to critical areas of the work metal and, if feasible, to tool surfaces. It is especially important to keep the die clean and free from particles of foreign metal that can become embedded in the surface of the workpiece and impair its corrosion resistance.

Hard rubber inserts in dies are sometimes helpful in preventing damage to the work metal and in forming large radii. However, this practice is not recommended for high production, because the inserts wear rapidly and cause nonuniform workpieces.

Reworking of bends by straightening and rebending the same portion should not be done in cold forming, because of the possibility of failure.

Springback can be as much as 30° for a 90° bend in cold forming of magnesium alloys.

Effect of Bending on Length. Unlike aluminum alloys and steel, which lengthen in bending, magnesium alloys shorten, because the neutral axis moves slightly toward the tension side of the bend. For thin sheet, the extent of this shortening is small, because the axis shifts only 5 to 10%. However, in thicker sheet when several bends are made, the amount of shortening can be significant and must be allowed for in the development of the blank.

Stress Relieving. Magnesium-aluminum-zinc (AZ) alloys should be stress relieved after cold forming to prevent stress corrosion. It may be desirable to stress relieve workpieces formed from the magnesium-thorium (HM, HK) alloys, particularly if they require straightening in fixtures. Recommended temperatures and times for stress relieving the magnesium alloys that are most commonly cold formed are given in Table 2.

Table 2 Stress relief treatments for the magnesium alloys most commonly cold formed

Alloy and temper	**Temperature**		Time at temperature, min
	°C	**°F**	
Sheet			
AZ31B-O	260	500	**15**
AZ31B-H24	150	300	**60**
HK31A-H24	290	550	**30**
HM21A-T8	370	700	**30**

HM21A-T81[(a)]	370	700	**30**
Extruded flat strip			
AZ31B-F	260	500	**15**
AZ61A-F, AZ80A-F	260	500	**15**
AZ80A-T5	205	400	**60**
HM31A-T5	**425**	**800**	**60**

(a) 80 to 90% stress relief may be accomplished with a 30 min exposure at 400 °C (750 °F), but mechanical properties will be reduced.

Forming of Magnesium Alloys

Hot Forming

Magnesium alloys, like other alloys with hexagonal crystal structures, are much more workable at elevated temperatures than at room temperature. Consequently, they are usually formed at elevated temperatures. The methods and equipment used in forming magnesium alloys are the same as those commonly employed in forming other metals, except for differences in tooling and technique that are required when forming is done at elevated temperatures.

Working of metals at elevated temperatures has several advantages over cold working. Magnesium alloy parts usually are drawn at elevated temperature in one operation without repeated annealing and redrawing, thus reducing the time involved for making the part and also eliminating the necessity for additional die equipment for extra stages. Hardened dies are unnecessary for most types of forming. Hot-formed parts can be made to closer dimensional tolerances than can cold-formed parts because of less springback. Table 3 lists suggested maximum forming temperatures and times for various wrought magnesium alloys. The times given indicate the maximum time the alloy can be held at temperature without adversely affecting mechanical properties.

Table 3 Maximum forming temperatures and times for wrought magnesium alloys

Alloy	**Temperature**		Time
	°C	**°F**	
Sheet			
AZ31B-O	290	550	**1 h**
AZ31B-H24	165	325	**1 h**

HK31A-H24	345	650	**15 min**
	370	700	**5 min**
	400	750	**3 min**
Extrusions			
AZ61A-F	290	550	**1 h**
AZ31B-F	290	550	**1 h**
M1A-F	370	700	**1 h**
AZ80A-F	290	550	**30 min**
AZ80A-T5	195	380	**1 h**
ZK60A-F	290	550	**30 min**
ZK60A-T5	**205**	**400**	**30 min**

Sheet and Plate. Rolled magnesium alloy products include flat sheet and plate, coiled sheet, circles, tooling plate, and tread plate. These products are supplied in a variety of sizes.

The ability to use increased section thickness without weight penalty is of particular importance in designs that employ magnesium sheet. Thick-sheet construction provides the rigidity necessary in a structure, without the need for costly assembly of ribs and similar reinforcing members.

Rolled magnesium-alloy products can be worked by most conventional methods. For severe forming, sheet in the annealed (O temper) condition is preferred. However, sheet in the partially annealed (H24 temper) condition can be formed to a considerable extent. Because heat has significant effects on properties of hard-rolled magnesium, properties of the metal after exposure to elevated temperature must be considered in forming. The design curves shown in Fig. 1 give minimum values suitable for design use. Although the curves are based primarily on tests of sheet 1.63 mm (0.064 in.) thick or less, check tests indicate reasonable applicability for thicknesses up to 6.35 mm (0.250 in.).

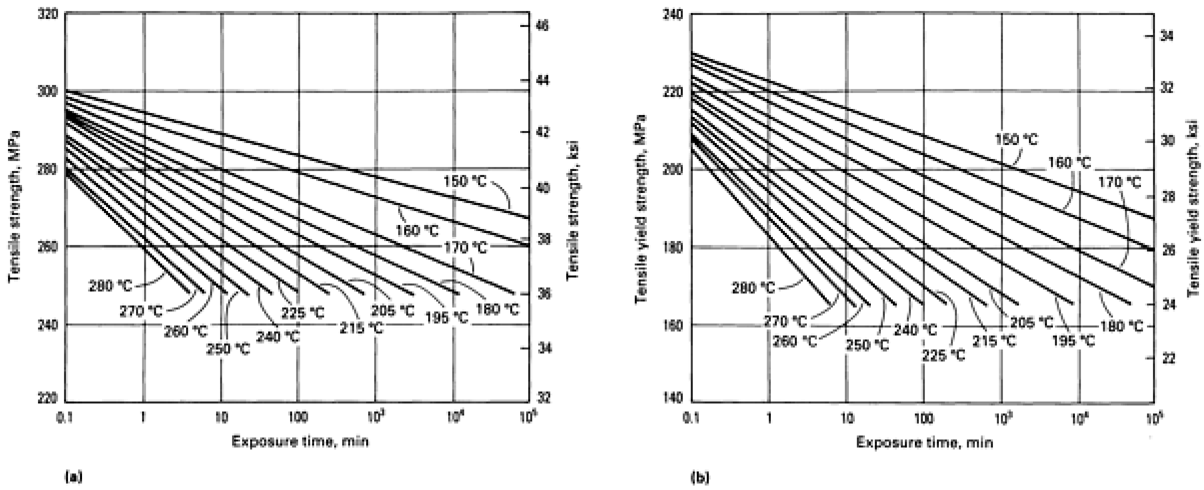


Fig. 1 Effect of exposure time at elevated temperature on the mechanical properties of AZ31B-H24 sheet at room temperature. (a) Tensile strength. (b) Tensile yield strength. Data are based on tests of sheet 1.63 mm (0.064 in.) thick, and have reasonable applicability for thicknesses up to 6.35 mm (0.250 in.).

Figure 1 shows how the properties of AZ31B-H24 change with exposure time at various temperatures. The curves have been extrapolated above the typical property levels of AZ31B-H24 sheet. Thus, if the value selected from a curve exceeds the actual property level of the material before exposure, the actual figure must be used. Also, it should be kept in mind that the effects of multiple exposures at elevated temperature are cumulative.

AZ31B-H24 sheet is commonly hot formed at temperatures below 160 °C (325 °F) to avoid annealing it to room-temperature property levels lower than the specified minimums. Annealing is a function of both time of exposure and temperature; thus, temperatures higher than 160 °C can be tolerated if exposure is carefully controlled.

Thermal Expansion. Magnesium alloys have a very high rate of thermal expansion. At 260 °C (500 °F), for example, the thermal expansion of magnesium is more than twice that of steel. Therefore, when parts made of magnesium alloys are hot formed in tool steel or cast iron dies, the difference in the thermal expansion of the tool material and the work metal must be considered.

Figure 2 shows the relation between the size of a magnesium part and the size of a steel die at 20 °C (70 °F) and at 205 °C (400 °F). The dimensional factor need not be applied when the dies are made of zinc or aluminum alloys, because the coefficients of expansion of these alloys are similar to those of magnesium alloys.

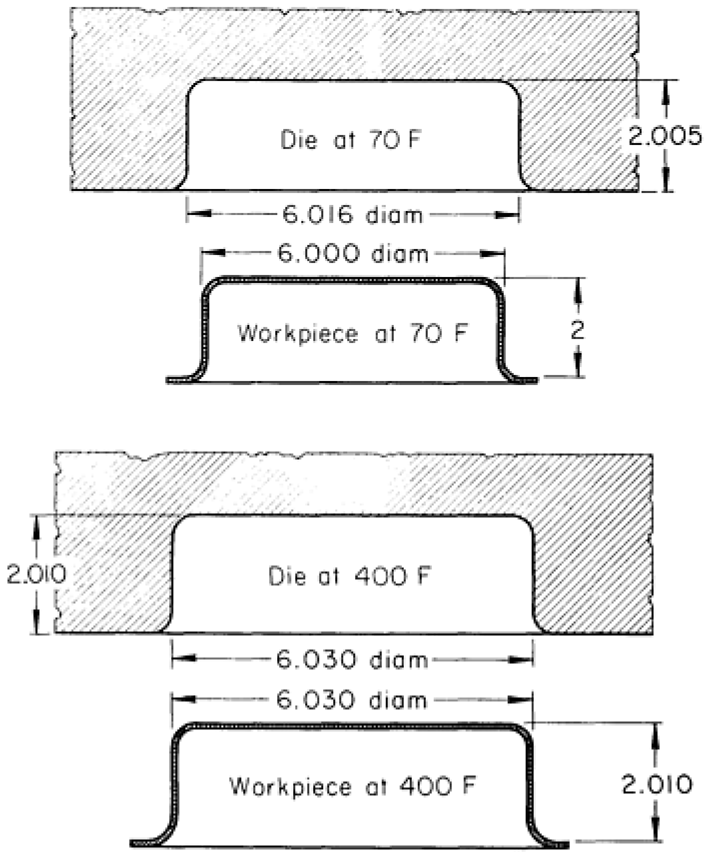


Fig. 2 Dimensional relations of a magnesium alloy workpiece and a steel die at room temperature and at a forming temperature of 205 °C (400 °F). Room-temperature dimensions of the steel die are determined by multiplying the design dimensions of the magnesium part by 1.00270. Dimensions given in inches.

Precautions. Magnesium alloy stock to be hot formed must be clean. Protective coatings, oil, dirt, moisture, or other foreign matter must be removed.

Dies, punches, and form blocks should be clean and free of scratches; tooling should be cleaned with solvent. Rust, scratches, and minor imperfections can be removed by light polishing with fine-grit abrasive cloth. However, polishing must not alter the dimensions of the tool.

In forming, the possibility of fire due to ignition of magnesium is remote. However, an ample supply of suitable fire-extinguishing material, such as dry sand or commercially available powder, must be kept in the work area.

Heating of Workpieces

In most hot-forming methods, both the tools and the magnesium alloy work metal must be heated. Heating equipment includes ovens, platen heaters, ring burners, electric heating elements, heat transfer liquids, induction heaters, and lamps and other types of infrared heating. For small lots, tools and work may be heated by hand torches. Small dies that can be handled rapidly can be heated in ovens adjacent to the forming equipment.

Heating Methods. Electric heating elements often are used for heating dies and other forming tools. Electric-resistance heating can be used for heating some dies. Low-voltage high-amperage current is passed into the die through conducting grips or clamps.

Radiant heating with electricity and gas is useful for heating dies and workpieces in some applications. Radiant heating is particularly useful for rapid heating of the workpiece and for use in rapid-action presses. Also, with this heating method, cloth covers can be used on the workpiece to minimize heat loss.

Infrared heating is also commonly used. A bank of infrared lamps is the most common method, but gas-fired units are also used.

The principal advantage of infrared heating is that only the die and workpiece are heated and not the surrounding area. Also, the cost of heating is less, and working conditions are cooler and less hazardous.

Gas heating often is advantageous, because the installation of equipment is simple and fuel cost is generally low. Burners up to 1.9 m (75 in.) long can be formed and welded from 19 mm ($\frac{3}{4}$ in.) black iron pipe; 25-mm (1-in.) pipe is suggested for burners more than 1.9 m (75 in.) long. Burners are attached to the dies so that the flames touch the die surface. Hollow punches can be heated by a burner inside the punch.

Four gas-mixing systems are used for heating tools and dies:

- Simple venturis, in which gas flows through a mixer that draws in air
- Proportional mixers, which use compressed air flowing through a venturi (or an air injector) that pulls the gas into the burner at atmospheric pressure
- A gas-air carburetor system, which uses a low-pressure turbocompressor to compress the gas-air mixture. The carburetor holds a constant fuel-to-air ratio regardless of the volume of flow
- A venturi mixing system combined with turbocompressors, which gives accurate temperature control with minimum overshoot

Heat transfer fluids are used for heating platens, form blocks, drop hammer dies, and other forming tools large enough to have passages in the die. Heating by this method is rapid and permits good temperature control. Heat transfer fluids with a working-temperature range of 150 to 400 °C (300 to 750 °F) are available. Hot oils, natural and synthetic, that can withstand temperatures up to 345 °C (650 °F) are commonly circulated in passages in the dies. Steam is readily available, and is circulated through ducts in the tools or dies, but its maximum temperature is usually about 175 °C (350 °F). Commercially available equipment for use with heat transfer fluids includes vapor generators, circulating mechanisms, and means for temperature control.

Temperature control is important. For forming a few pieces, contact pyrometers or temperature-sensitive crayons are satisfactory for determining temperature. Blue carpenter's chalk can sometimes be used. A streak of this chalk on a metal surface will turn white at approximately 315 °C (600 °F).

Automatic temperature controls are essential for most magnesium-forming operations. Radiant and infrared heat are more difficult to control than other kinds of heat. One type of infrared lamp has a control that extends or retracts the lamp when the tool or workpiece has reached the desired temperature. Another temperature control for infrared heating consists of a special radiometer that senses only the heat radiated by the surfaces being heated.

To maintain the desired temperature, controls used in gas-heating systems usually operate by adjusting a solenoid valve in the line to lower or raise the flame. Electric heating by elements, resistance heating, and heating by means of heat transfer fluids usually provide good temperature control.

Lubricants

Generally, lubrication is more important in hot than in cold forming of magnesium alloys, because the likelihood of galling increases with the increase of temperature.

Lubricants used in forming magnesium alloys include mineral oil, grease, tallow, soap, wax, molybdenum disulfide, colloidal graphite in a volatile vehicle, colloidal graphite in tallow, and thin sheets of paper or fiberglass.

Selection of a lubricant depends primarily on forming temperature. For temperatures up to 120 °C (250 °F), oil, grease, tallow, soap, and wax are generally used.

In spinning, it is essential that the lubricant cling to the work metal; otherwise the lubricant will be thrown off by centrifugal force. This is not a problem when drawing in a die or bending in a press brake.

Frequently, a lubricant that is used for other operations in the plant can be used, up to a forming temperature of 120 °C (250 °F). It is common practice to use the lubricant that can be most easily removed after forming and to apply it by roller coating or swabbing. Sometimes the lubricant is applied to both the work metal and the tools.

When forming is done at temperatures above 120 °C (250 °F), the selection of a lubricant is more limited; ordinary oil, grease, and wax are eliminated. Although colloidal graphite can be applied at any temperature that is used for forming magnesium alloys, because graphite is difficult to remove and interferes with subsequent surface treatments, its use is usually avoided.

A soap lubricant is acceptable for temperatures as high as 230 °C (450 °F). This compound is an aqueous solution and is applied to the work metal by dipping, brushing, or roller coating. After coating, the work metal blanks are dried in still or forced air. After drying, the blanks can be stored for an indefinite period for future processing because the dried lubricant is stable. Lubricant that remains after forming can be completely removed by cleaning in hot water.

When forming temperatures are higher than 230 °C (450 °F), the choice of lubricant is restricted to colloidal graphite or molybdenum disulfide. Graphite in a vehicle such as spirits (2% graphite) is widely used; for spinning, the graphite is mixed with tallow to improve adherence.

Lubricants should be cleaned from parts as soon as possible after forming, to prevent corrosion and to avoid difficulty in their removal. Colloidal graphite is particularly difficult to remove if allowed to remain on parts for any length of time.

Because for some work lubricants cannot be tolerated at any forming temperature, thin sheets of paper or fiberglass (depending on temperature) are placed between the work metal and the tools. More information on lubricants for sheet forming is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

Press-Brake Forming

The press-brake forming of magnesium alloys is the same as it is for other metals, except that the work metal and the dies are usually heated. Top and bottom dies can be made of steel, or if the workpiece permits cold forming, the steel punch can be bottomed in a rubber die held in a retaining box. Metal punches and dies should be highly polished to prevent marking of workpiece surfaces.

A preferred method of heating the punch and die for hot forming is shown in Fig. 3. When only a few workpieces are to be formed, heating with a gas torch is satisfactory; however, care must be taken to ensure that the area of the workpiece to be formed is uniformly heated. If the press-brake die is not heated, the workpiece should be heated to the maximum allowable temperature and formed quickly before the tools can cool the work metal too much.

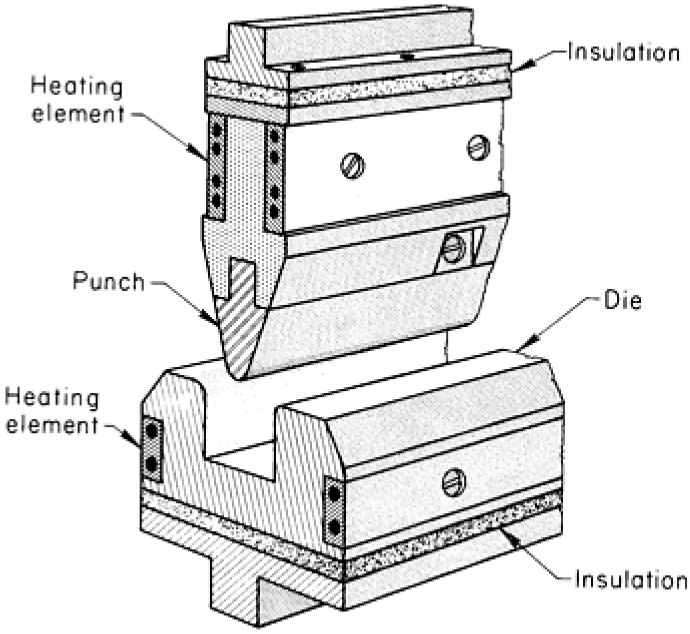


Fig. 3 Preferred method of heating a punch and die for hot forming magnesium alloys on a press brake.

Deep Drawing

Magnesium alloys can be cold drawn to a maximum reduction of 15 to 25% in the annealed condition. The cold drawability limit of alloy AZ31B-O is about 20%. The drawability, or percentage of reduction in blank diameter, is calculated by the formula:

$$\text{Percentage reduction} = \frac{D - d}{D} \times 100$$

where D is the blank diameter before drawing and d is the diameter of the punch.

Die Heating. Electric heating elements are usually used for heating the tools, although when intricate parts are drawn, gas-ring burners provide more flexibility, particularly if the work metal is likely to pucker. The base of the punch sometimes can be heated.

Another type of differential heating is used in the production of workpieces that have a slight crown. When the crown must be held to close tolerance, burners with separate controls are provided inside and outside the die. For more crown, the outside of the mating die is heated to a higher temperature than the inside. When the workpiece reaches die temperature and the outside of the sheet is hotter than the inside, the work metal retracts upon removal of the die, thus causing the crown to form. Alternatively, to eliminate crowning and obtain flatness, the inside of the die is heated to a higher temperature than the outside.

Blankholder pressures for magnesium alloys vary from the lowest obtainable to as much as 5 MPa (700 psi). In average draws, the blankholder pressure is usually 345 to 1380 kPa (50 to 200 psi). To secure proper wall thinning, blankholder pressures are obtained by trial and error.

Drawability. Annealed magnesium alloy sheet can be cold drawn to 25% reduction (blank diameter to cup diameter). With heat, drawability is greatly improved, and up to 70% reduction is possible.

Drawability is also influenced by the shape of the workpiece. A maximum drawability of 70% is for drawing a round cup. Square and rectangular boxes, for example, seldom are drawn as severely.

Effect of Speed on Drawability. Drawability at any temperature varies with the speed of drawing, which ranges from 0.6 to 405 mm/s ($1\frac{1}{2}$ to 960 in./min). Large reductions (70%, for example) require slower speeds than do moderate reductions (up to 55%).

Reductions up to about 55% can often be made on high-speed hydraulic or mechanical presses. Also, milder draws permit lower forming temperatures, and costs can be reduced, because strip feeding, blanking, lubrication, trimming, and cleaning can be simplified.

For most parts, however, depth of draw is not a primary consideration, and usually no trouble is experienced in drawing to the depth required. More trouble is encountered in keeping the metal free from puckers in parts with rounded corners or contours. Temperatures above those required for maximum drawability are often necessary to eliminate these puckers. On unusual or difficult jobs, it may be necessary to vary the procedure to obtain minimum scrap.

Redrawing. The possibilities inherent in two-step draws are illustrated by the following parts: In the first operation, 610 mm (24 in.) blanks of 0.64 mm (0.025 in.) annealed sheet were drawn to a cup 200 mm (8 in.) in diameter by 400 mm (16 in.) in depth; they were redrawn to a cup 140 mm ($5\frac{1}{2}$ in.) in diameter by 585 mm (23 in.) in depth. Starting with a rectangular blank of 1.3 mm (0.051 in.) AZ31B-O, 455 × 485 mm (18 × 19 in.), a rectangular box 111 × 273 × 165 mm ($4\frac{3}{8} \times 10\frac{3}{4} \times 6\frac{1}{2}$ in.) in depth was drawn in the first operation. This box was then redrawn into a rectangular box 89 × 254 × 171 mm ($3\frac{1}{2} \times 10 \times 6\frac{3}{4}$ in.) in depth having 5.6 mm ($\frac{7}{32}$ in.) corner radii.

Choice of die materials is chiefly influenced by the severity of the operation and the number of parts to be produced. For most applications, unhardened low-carbon steel boiler plate or cast iron is satisfactory. For runs of 10,000 parts or more, for maximum surface smoothness, or for close tolerances where no significant die wear can be allowed, hardened tool steels are recommended. Tool steels W1 or O1 are satisfactory for extremely long runs (1 million parts). For the most severe draws, however, the more abrasion-resistant tool steels, such as A2 or D2, will probably be more satisfactory and economical. For room-temperature drawing, it is usually desirable for die steels to be heat treated to obtain near-maximum hardness in service. However, for elevated-temperature drawing, the maximum temperature to which the dies will be exposed in drawing must also be considered. In this situation, the dies must be tempered slightly above the maximum service temperature, even though some hardness may be sacrificed.

Manual Spinning

Various conical and hemispherical shapes can be produced from magnesium alloys by manual spinning. Because tooling is inexpensive, manual spinning of small quantities is often more economical than press forming. When press tooling would be complex, manual spinning may be used for medium to large production quantities.

Equipment and tooling for manual spinning of magnesium alloys are essentially the same as those used for other metals (see the article "Spinning" in this Volume), except that when magnesium alloys are to be heated, the mandrels (spin blocks) should be made of metal, with provision for controlled heating of the work metal.

For spinning a few pieces, it is common practice to heat the blanks with a hand torch, using temperature-sensitive crayons to indicate the temperature. For production spinning, however, the use of a thermostatically controlled burner on the lathe is preferred.

Procedure. Annealed sheet is usually used in spinning. Manual spinning depends to a large extent on operator skill, especially when spinning magnesium alloys, which are more temperature sensitive than most metals.

Many shapes can be spun from unheated blanks by a skilled operator, especially when thin sheet is used, because friction between the spinning tool and workpiece generates a substantial amount of heat. As severity of sheet thickness increases, the work metal must be heated. Temperatures of 260 to 315 °C (500 to 600 °F) are common.

Whether spinning is done hot or cold, a lubricant should be used (see the section "Lubricants" in this article). Spindle speed should be such that the speed of the edge of the blank is about 610 m/min, or 2000 surface feet per minute (sfm) when spinning begins.

Tolerances. Typical tolerances that can be maintained in manual spinning of magnesium alloys are as follows:

Workpiece diameter		Tolerance	
mm	**in.**	**mm**	in.
<455	<18	±0.8	$\pm\frac{1}{32}$
455-915	18-36	±1.6	$\pm\frac{1}{16}$
>915	**>36**	**±3.2**	$\pm\frac{1}{8}$

Spinning Extrusions. Manual spinning can be used either to close or flare the ends of extruded round tubing.

Closing the ends of tubes is done by slowly forcing a rotating hemispherical cup over the end of the tube until the end is closed and has assumed the hemispherical shape of the cup (spinning tool). This can be done on almost any machine that can hold the workpiece and rotate the cuplike tool. A drill press is frequently used. The use of grease or soap as a lubricant is helpful in producing a better workpiece finish and prolonging the life of spinning tools. In most applications, tube ends can be closed without the use of heat.

Tube flaring is done either by inserting a stationary mandrel into the tube and pushing it out against a stationary die on the outside, or by spinning or rolling the flare against a stationary outside die with a conical, rotating, inside mandrel. The shape required for the flare usually determines the preferred procedure.

Tube-flaring machines have a stationary outside die and conical, rotating, inside spindle with adjustable eccentricity, the axis of which rotates off center at approximately 1600 rpm. The eccentric spindle forces the tube against the outer die to form the flare. In the flaring of magnesium alloy tubes, the outer die should be heated, preferably by electric heating elements in the die holder, to a preferred temperature of about 260 °C (500 °F). The tube to be flared is preheated to the same temperature. Lubrication may be required during tube flaring. The lubricants recommended for spinning can be used.

Forming of Magnesium Alloys

Power Spinning

Power spinning (shear spinning) can be used for magnesium alloys. Both cone spinning (spinning in accordance with the sine law) and tube spinning (spinning in which metal displacement is strictly volumetric) are used for magnesium alloys.

Equipment and Tooling. Special machines are used in the cone and tube spinning of magnesium alloys. However, the equipment used in power spinning is the same as that used for other metals (see the article "Spinning" in this Volume), except when hot spinning is done; then, torches or other heating equipment must be added to the machine. Consequently, the mandrels and rollers must be made from an alloy tool steel that will not be softened by heat. Tool steels such as H12 or H13 hardened to 54 to 58 HRC are used in many applications.

Procedure. Magnesium alloys are sometimes power spun without heat, but more often the major portion of the reduction is performed hot and finished cold, or is rough worked hot and then finished at a somewhat lower temperature (warm). For the most successful results, a definite procedure of alternate spinning and heating should be followed, whether the metal is finished cold or warm. Table 4 gives recommended procedures for the two methods often used for alloys HK31A and HM21A. The use of the procedures outlined in Table 4 has resulted in total wall thickness reductions as high as 80%.

Table 4 Procedures for power spinning two magnesium alloys to obtain acceptable properties[(a)]

Alloy	Procedure
Cold finishing	
HK31A	**Hot work roughly to shape at 425 ± 30 °C (800 ± 50 °F). Heat treat for 30-60 min at 455-480 °C (850-900 °F)[(b)]. Cold work to a total reduction in thickness of at least 25% using low reductions per pass. Heat 1 h at 315-330 °C (600-625 °F)**
HM21A	**Hot work roughly to shape at 455 ± 10 °C (850 ± 50 °F). Heat treat for 30-60 min at 480-510 °C (900-950 °F)[(c)]. Cold work to a total reduction in thickness of 15-25% using low reductions per pass. Heat 1 h at 370 ± 16 °C (700 ± 25 °F)**
Warm Finishing	
HK31A	**Hot work roughly to shape at 425 ± 30 °C (800 ± 50 °F) if necessary. Heat treat for 30-60 min at 455-480 °C (850-900 °F)[(b)]. Warm work at 315-370 °C (600-700 °F) to a total reduction in thickness >50% with a minimum number of passes. Heat 16 h at 205 °C (400 °F)**
HM21A	**Hot work roughly to shape at 455 ± 30 °C (850 ± 50 °F) if necessary. Heat treat for 30-60 min at 480-510 °C (900-950 °F)[(c)]. Warm work at 315-370 °C (600-700 °F) to a total reduction in thickness >50% with a minimum number of passes. Heat 16 h at 230 °C (450 °F)**

(a) Properties obtained will approach those of the H24 temper for HK31A and the T8 temper for HM21A.

(b) Fairly rapid cooling is desirable, but less critical than for HM21A.

(c) Should be cooled from the heat-treating temperature to 315 °C (600 °F) or below within 5 min

Forming of Magnesium Alloys

Rubber-Pad Forming

Hydraulic presses are generally used for the rubber-pad forming of magnesium alloys. Tooling is simple because only a form block is used (see Fig. 4). A conventional die is not needed.

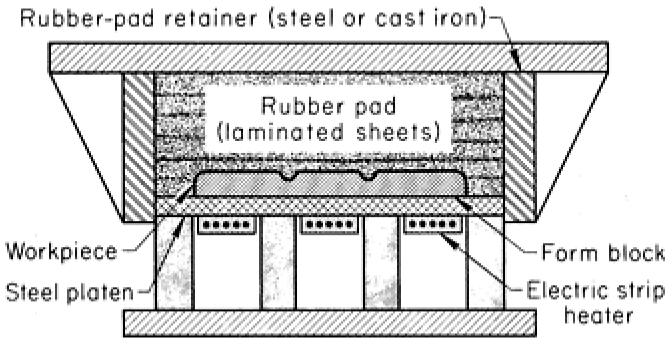


Fig. 4 Tooling and heating setup for rubber-pad forming of magnesium alloys at elevated temperature.

For forming at room temperature, particularly for limited use, form blocks can be made of wood or masonite; or for higher production runs, they can be made of aluminum, zinc, or magnesium, which are more durable than wood or masonite. However, large radii must be used in cold forming.

When rubber-pad forming at elevated temperature form blocks must be made from metal that will not creep excessively at the working temperature and pressure; magnesium, aluminum, or zinc can be used up to about 230 °C (450 °F). However, forming at temperatures higher than 230 °C (450 °F) requires steel form blocks.

Specially compounded grades of solid rubber or laminated sheets are used for the rubber pad when forming at temperatures up to 315 °C (600 °F). Hardness of the rubber is important--Durometer A 40 to 70 is the common range.

Heating. As shown in Fig. 4, the heating elements heat the steel platen, and the heat is transferred to the form block, which is not fastened to the platen, by conduction. Alternatively, the form block can be heated separately in an oven and then placed on the platen. With this method, a fireproof blanket often is placed between the heated form block and the cold platen for insulation. Usually blanks are heated in ovens situated near the press to minimize loss of heat.

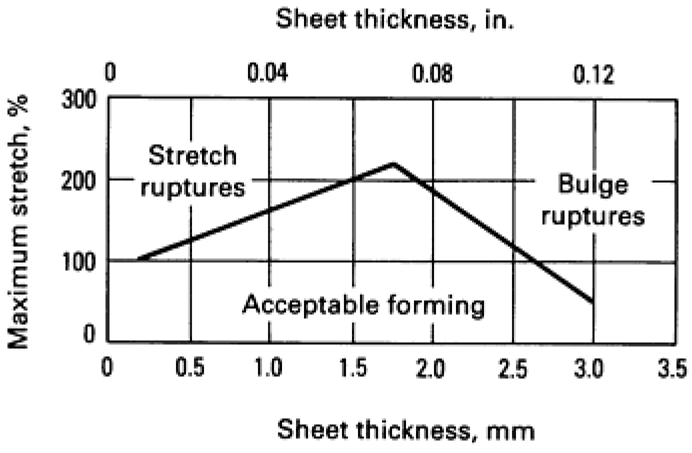
Forming Pressure. Pressure for rubber-pad forming is a function of sheet thickness and forming temperatures; 6200 kPa (900 psi) is adequate for most work.

Rubber-pad forming is generally done by shaping the blank around a form block with pressure from the rubber pad. However, when pressure must be concentrated at one point, or metal flow must start before general pressure is applied, deflector bars are used (see the article "Rubber-Pad Forming" in this Volume).

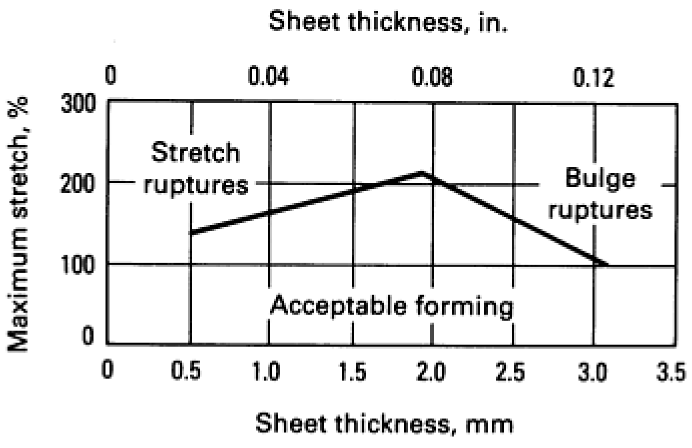
Some severe forming is done in two operations: The workpiece is partly formed, is removed from the press for hand smoothing of wrinkles, and then is returned to the press for final forming under full pressure. In one-operation forming, thin throw sheets of heat-resistant rubber can be placed over the blank, or attached to the pad, to protect the rubber.

Shrink flanges that are wrinkle-free can be made with a higher percentage of compression from magnesium alloys than from other metals, such as aluminum, of the same gage. Minor wrinkles can be hand corrected after flanging. If the part is likely to wrinkle severely, scalloped cutouts or recesses in the form blocks may correct this condition. A draw plate to iron out wrinkles during forming is often helpful.

Stretch flanges of up to 40% stretch in hard-rolled magnesium alloy sheet (H24 temper) and 70% in annealed sheet (O temper) can be made by rubber-pad forming. The ranges of stretch flange limits for various thicknesses of AZ31B-O and AZ31B-H24 alloy sheet rubber pad formed at 150 °C (300 °F) are shown in Fig. 5.



(a)



(b)

Fig. 5 Stretch flange limits for various thicknesses of magnesium alloys AZ31B-O and AZ31B-H24 rubber pad formed at 150 °C (300 °F). (a) Alloy AZ31B-O. (b) Alloy AZ31B-H24.

A minimum flange radius of 5*t* is suggested for alloy AZ31B-H24 at 160 °C (325 °F). The radius of the die should be approximately $\frac{1}{2}t$ less to compensate for springback.

Beads. In rubber-pad forming, both internal and external beads can be formed in magnesium alloy sheet. Usually external beads are easier to produce, although wrinkling is slight in both types.

Severity of forming internal and external beads is expressed as the ratio w/h where w is the width of the bead and h is the height of the bead. Beading is essentially a stretching operation, and this w/h ratio is related to the maximum percentage of stretch obtained in a given bead.

External beads can be made to equal or more severe ratios; the minimum bead margin should be 9.6 mm (0.38 in.), and beads should be separated by a minimum center-point distance of 19.3 mm (0.76 in.).

Hand forming can slightly improve the definition of a part after rubber-pad forming, while the form block and workpiece are still hot. A leather or plastic forming tool can be used to correct minor irregularities or improve flange angles. To avoid damage to the form block, hard tools should not be used.

Stretch Forming

The stretch forming of magnesium alloys is the same as it is for other metals, except that magnesium alloys are generally stretch formed at elevated temperatures. The fundamentals of stretch forming, compression forming, and radial draw forming are described in the article "Stretch Forming," in this Volume.

Dies, or form blocks, made from magnesium, aluminum, or zinc alloys are suitable for forming at temperatures up to 230 °C (450 °F). Concrete form blocks containing wire mesh heated by electrical resistance may also be used at temperatures up to 230 °C (450 °F). For temperatures higher than 230 °C (450 °F), cast iron form blocks are used.

Grippers used in the forming of magnesium alloys should not have serrated jaws, to prevent tearing of the work metal. Coarse emery paper or cloth can be placed between the work metal and the jaws to preclude tearing.

Tools and work metal can be heated by electric heating elements or by radiant heat. Proper distribution of heat is important, and units should be placed at critical forming areas.

For differential stretching of sheet over forms of low curvature, the practical maximum stretch is about 15%. The maximum is 12% if allowance (overstretch) is made for springback; however, normally little springback is encountered at elevated temperatures; therefore an addition of 1% to the total stretch usually compensates for any springback that may occur.

Although freedom from wrinkles is an advantage of stretch forming in most applications, wrinkles can be a problem when making asymmetrical low-curvature parts. Wrinkles can be controlled by including proper restraints in the die. The skill of the operator largely determines where such restraints are needed.

Drop Hammer Forming

Drop hammer forming is used for producing shallow depths and asymmetrical shapes in magnesium alloys when quantities are small and for applications requiring minimum springback. Successful results depend on operator skill. Except for heating, drop hammer forming of magnesium alloys is the same as it is for other metals, (see the article "Drop Hammer Forming" in this Volume).

Zinc alloy can be used for both punch and die. Lead punches are sometimes used, but lead pickup can cause corrosion of the sheet. For production quantities greater than about 50 pieces, however, cast iron punches and dies are recommended, because zinc alloy tools lose their shape at these quantities.

Annealed sheet is preferred for drop hammer forming. Blanks should be heated near the hammer, because the work cools rapidly--usually 16 to 25 °C (30 to 45 °F) in 5 s. Ten blows may be needed to form a part, with reheating between blows--5 min for metal thicknesses up to 1.3 mm (0.051 in.) and 9 min for thicknesses of 1.3 to 3.18 mm (0.051 to 0.125 in.).

Heat-resistant rubber pads are often used in the dies for preliminary forming and removed before final forming.

Dies can be heated in an oven near the hammer, or by torches or ring burners during operation. Small dies can be used on an electrically heated cast iron platen on the hammer bed, but this method is not practical for large dies. Heating of the punch and die by electric heating elements or by a heat transfer fluid is also used.

The elevated temperatures used in drop hammer forming of magnesium alloys can reduce or eliminate springback; therefore, the maximum practical temperature should always be used. The rate of deformation must be carefully controlled, especially when deformation of the work metal is severe, or when the metal is in the H24 temper. For workpieces that require severe forming, the punch is lowered slowly and forming is completed with subsequent blows. Tolerances of ±0.76 mm (±0.030 in.) can be maintained in production.

Impact Extrusion

Impact extrusion is used for producing symmetrical tubular workpieces, especially those with thin walls or irregular profiles for which other methods are not practical. Information on the application of this process to magnesium is described in the section "Impact Extrusion of Magnesium Alloys" of the article "Cold Extrusion" in this Volume.

Forming of Nickel-Base Alloys

R. William Breitzig, INCO Alloys International, Inc.

Introduction

THE DUCTILITY of nickel-base alloys in the annealed condition makes them adaptable to virtually all methods of cold forming. Within this group of alloys (see Table 1 for compositions), other engineering properties vary sufficiently to cause the alloys to range from moderately easy to difficult to form, compared to other materials.

Table 1 Nominal compositions of some nickel-base alloys

Alloy	**UNS designation**	Composition, %[(a)]						
		C	**Fe**	**Co**	**Cr**	**Ti**	**Mo**	Others
Nickel 200	N02200	0.10 max	0.4 max	. . .	. . .	. . .	. . .	**0.25 max Cu, 0.10 max Ti, 0.15 max Si**
Nickel 201	N02201	0.02 max	0.4 max	. . .	. . .	. . .	. . .	**0.25 max Cu, 0.10 max Ti, 0.15 max Si**
Astroloy	N13017	0.06	. . .	17.0	15.0	3.5	5.25	**4.0Al, 0.03B**
Alloy C	N10002	0.07	5.0	2.5 max	16.0	. . .	17.0	**4.0W**
Alloy C-276	N10276	0.02	5.5	2.5 max	15.5	. . .	16.0	**3.75W, 0.35V**
Alloy W	N10004	0.12 max	5.5	2.5	5.0	. . .	24.5	**0.6V**
Alloy 400	N04400	0.15 max	1.25	. . .	. . .	. . .	. . .	**31.5Cu, 0.5Si**
Alloy 600	N06600	0.1 max	8.0	. . .	15.5	. . .	. . .	**. . .**
Alloy 625	N06625	0.1 max	5.0 max	1.0 max	21.5	0.4 max	9.0	**3.65 Nb + Ta, 0.4 Al max**
Alloy 718	N07718	0.05 max	19.0	. . .	18.0	0.4 max	3.0	**5.0Nb**

Alloy 800	N08800	0.1 max	44.0	. . .	21.0	0.38	. . .	**0.75 max Cu, 1.0 Si**
Alloy X-750	N07750	0.04	7.0	. . .	15.5	2.5	. . .	**0.95 Nb + Ta, 0.7Al**
Alloy 75	N06075	0.15 max	5.0 max	. . .	19.5	0.6 max	. . .	**0.50 max Cu, 1.0 max Mn, 1.0 max Si**
Alloy 80	N07080	0.10 max	5.0 max	2.0 max	19.5	2.25	. . .	**1.13Al**
U-700	. . .	0.15 max	1.0 max	18.5	15.0	3.5	5.2	**4.25Al, 0.05B**
Waspaloy	**N07001**	**0.07**	**. . .**	**13.5**	**19.5**	**3.0**	**4.3**	**1.4Al, 0.07Zr, 0.006B**

(a) All compositions contain balance Ni.

Strain Hardening. Because strain hardening is related to the solid-solution strengthening afforded by alloying elements, strain-hardening rate generally increases with the complexity of the alloy. Accordingly, strain-hardening rates range from moderately low for nickel and nickel-copper alloys to moderately high for the nickel-chromium, nickel-chromium-cobalt, and nickel-iron-chromium alloys. Similarly, the age-hardenable alloys have higher strain-hardening rates than their solid-solution equivalents. Figure 1 compares the strain-hardening rates of six nickel alloys, in terms of the increase in hardness with increasing cold reduction, with those of four other materials. Note that the strain-hardening rates of the nickel-base alloys are greater than that of 1020 steel, and most are less than that of AISI-type 304 stainless steel.

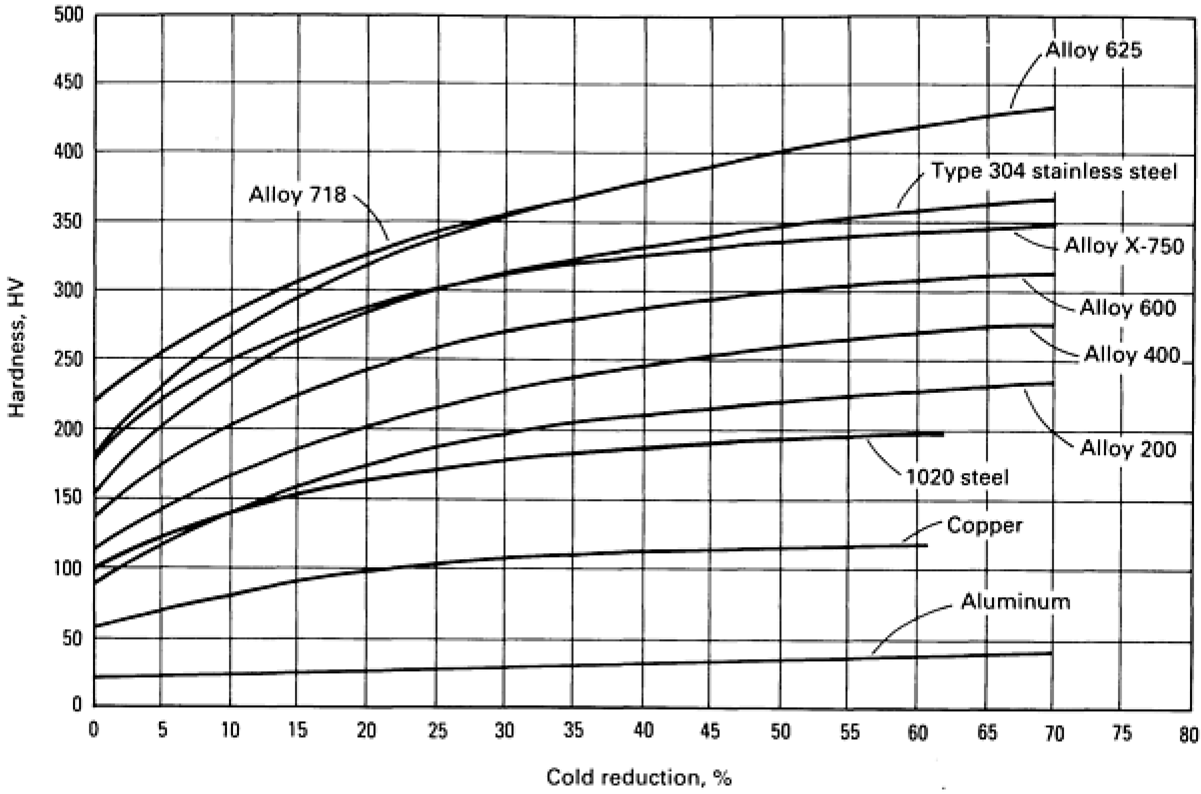


Fig. 1 Effect of cold work on the hardness of various nickel-base alloy sheet materials.

Because the modulus of elasticity of the high-nickel alloys is relatively high (similar to that of steel), a small amount of springback in cold-forming operations might be expected. However, springback is also a function of proportional limit, which can increase greatly during cold working of strain-hardenable materials. For instance, a yield strength of 170 MPa (25 ksi) of an alloy in the annealed condition might increase to 520 MPa (75 ksi), during a drawing operation. Therefore, the amount of springback for this alloy must be computed from the 520 MPa (75 ksi) flow stress, rather than from the initial value of the yield strength.

Temper. Most cold-forming operations require the use of annealed material. However, the softer alloys, such as Nickel 200, the NILO alloys, and alloy 400, are frequently used in $\frac{1}{8}$-hard and $\frac{1}{4}$-hard tempers for improved shearing and piercing. For similar reasons, alloy 400 is usually cold headed in No. 1 or 0 temper for fastener applications.

Galling. Because nickel-base alloys do not readily develop an oxide film that would present a barrier to diffusion bonding, they cold weld (gall) easily to materials of similar atomic diameter. When a cold weld is formed, the high shear strength and ductility of the alloys prevent the weld from being easily broken. For these reasons, the coefficient of friction between nickel-base alloys and other metals, including most die materials, is usually high.

Alloying with highly reactive elements that readily form oxide films, such as chromium, reduces the galling, or cold welding, propensity of nickel alloys. Accordingly, the nickel-chromium and nickel-iron-chromium alloys are less likely to gall than are the nickel and nickel-copper alloys. However, chromium oxide films are thin and brittle and provide only limited protection because they are easily broken when the substrate is deformed. The use of heavy-duty lubricants will minimize galling in most cold forming.

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Lubricants

Heavy-duty lubricants are required in most cold forming of nickel-base alloys. Although sulfur- and chlorine-containing additives can improve lubricants, they can also have harmful effects if not completely removed after forming. Sulfur will embrittle nickel-base alloys at elevated temperatures such as might be encountered in annealing or age hardening, and chlorine can cause pitting of the alloys after long exposure. Therefore, sulfurized and chlorinated lubricants should not be used if any difficulty is anticipated in cleaning the formed part. Neither are these lubricants recommended for use in spinning, as this operation may burnish the lubricant into the surface of the metal. Similarly, molybdenum disulfide is seldom recommended for use with nickel alloys because of difficulty in removal.

Pigmented oils and greases should be selected with care, because the pigment may be white lead (lead carbonate), zinc oxide, or similar metallic compounds that have low melting points. These elements can embrittle nickel alloys if the compounds are left on the metal during heat treatment. Inert fillers such as talc can be used safely.

Metallic Coatings. Maximum film strength can be obtained by using a coating of copper. However, because application and removal are expensive, metallic coatings are used as lubricants only in severe cold-forming operations and then only when they can be properly removed.

Ordinary petroleum greases are seldom used in forming nickel-base alloys. These greases do not necessarily have the film strength indicated by their viscosity, and they do not have a strong polar attraction for metals.

Phosphates do not form usable surface compounds on nickel-base alloys and cannot be used as lubricant carriers.

Light mineral oils and water-base lubricants have limited film strength and lubricity and can be used only in light forming operations. More information on lubricants for sheet forming is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

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Tools and Equipment

Nickel-base alloys do not require special equipment for cold forming. However, the physical and mechanical properties of these materials frequently necessitate modification of tools and dies used for cold forming other metals. These modifications are discussed in this section. Information applying to specific forming operations is presented in the sections covering those operations.

Die materials used in forming austenitic stainless steels (see the article "Forming of Stainless Steel" in this Volume) are suitable for similar operations on nickel-base alloys. Soft die materials such as aluminum bronze, nickel-aluminum bronze, and zinc alloys are used when superior surface finishes are desired. However, these materials have a relatively short service life. Parts formed with zinc alloy dies should be flash-pickled in dilute nitric acid to remove any traces of zinc picked up from the dies during forming. Zinc can cause embrittlement of nickel alloys during heat treatment or high-temperature service. For similar reasons, parts formed with brass or bronze dies should be pickled if the dies impart a bronze color to the workpiece.

Tool Design. Because nickel-base alloys are likely to gall, and because of the high pressures developed in forming, tooling should be designed with liberal radii, fillets, and clearances. The radii and clearances used in the cold forming of nickel-base alloys are usually larger than those for brass and low-carbon steel, and about equal to those for the austenitic stainless steels.

Because nickel-base alloys, particularly the nickel-chromium alloys, have higher yield strengths and strain-hardening rates, they require stronger and harder dies and more powerful forming equipment than does low-carbon steel. Generally, 30 to 50% more power is required for nickel-base alloys than is needed for low-carbon steel.

Equipment Operation. The strain-rate sensitivity and frictional characteristics of nickel-base alloys dictate that all forming operations be performed at relatively slow speeds. For instance, the slide speed in shearing, deep drawing, and press-brake bending is usually 9 to 15 m/min (30 to 50 ft/min). Cold heading, piercing, and similar operations are normally done at speeds of 60 to 100 strokes per minute.

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Shearing, Blanking, and Piercing

The optimum temper of nickel-base alloys for shearing, blanking, and piercing varies from skin hard to full hard, depending on the alloy and thickness. For instance, thin strip of Nickel 200 and the NILO alloys should be blanked in full-hard temper for maximum die life and minimum edge burr, but alloy 600 (UNS N06600) usually gives best results in skin-hard temper. Annealed temper is usually suitable for blanking of the precipitation-hardenable alloys such as alloy X-750 (UNS N07750).

Punch-to-die clearance per side should be 3 to 5% of stock thickness for thin material and 5 to 10% of stock thickness for thick ($\geq$3.2 mm, or $\frac{1}{8}$ in.) material. The clearance between the punch and the stripper plate should be as small as is practicable.

Shears should have a low-carbon steel rating of 50% greater than the size of the nickel alloy material to be sheared. For example, a shear with a low-carbon steel rating of 9.5 mm ($\frac{3}{8}$ in.) should be used to cut 6.4 mm ($\frac{1}{4}$ in.) thick alloy 400 (UNS N04400) plate.

Lubricants are not usually used in shearing but should be used in blanking and piercing. A light mineral oil fortified with lard oil can be used for material less than 3.2 mm ($\frac{1}{8}$ in.) thick. A heavier sulfurized oil should be used for material that is thicker than 3.2 mm ($\frac{1}{8}$ in.).

Procedure. In piercing, the minimum hole diameter is usually equal to or greater than the thickness of the material, depending on the thickness, temper, and specific alloy. Minimum hole diameters for given thicknesses of alloys 200, 400, and 600 are:

Sheet thickness, *t*		Minimum hole diameter
mm	**in.**	
0.46-0.86	0.018-0.034	**1.5*t***
0.94-1.78	0.037-0.070	**1.3*t***
1.98-3.56	0.078-0.140	**1.2*t***

Hole diameters equal to the thickness of the sheet have been produced in material as thin as 0.46 mm (0.018 in.), but only after considerable experience and with proper equipment.

The softer alloys, such as Nickel 200, have greater impact strength than do the harder, chromium-containing alloys. Consequently, the softer alloys are more sensitive to the condition of dies and equipment. Shear knives may penetrate 65 to 75% of the material thickness before separation occurs in shearing Nickel 200, whereas penetration may be only 20 to 30% in shearing the harder alloys.

Laboratory tests have indicated that the shear strength of nickel-base alloys in double shear averages about 65% of the tensile strength. However, these values were obtained under essentially ideal conditions using laboratory testing equipment with sharp edges and controlled clearances. Shear loads on nickel-base alloys ranged from 113 to 131% of those on low-carbon steel in production shearing based on tests using a power shear with a rake of 31 mm/m ($\frac{3}{8}$ in./ft) of blade length. More information on shearing is available in the article "Shearing of Plate and Flat Sheet" in this Volume.

Forming of Nickel-Base Alloys

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Deep Drawing

Nickel-base alloys can be drawn into any shape that is feasible with deep drawing steel. The physical characteristics of nickel-base alloys differ from those of deep drawing steel, but not so much as to require different manipulation of dies for the average deep drawing operation.

Most simple shapes can be deep drawn in nickel-base alloys using dies and tools designed for use on steel or copper alloys. However, when intricate shapes with accurate finished dimensions are required, minor die alterations are necessary. These alterations usually involve increasing clearances and enlarging the radius of the draw ring or of the punch nose.

Double-Action Drawing. In drawing and redrawing of thin stock ($\leq$1.6 mm, or $\frac{1}{16}$ in.) into cylindrical shells with no ironing, the diameter reduction should be 35 to 40% on the first operation and 15 to 25% on redraws. If the walls are held to size, the first and second operations may be the same as suggested above, but the amount of reduction should be diminished by about 5% on each successive redraw.

Although reductions of up to 50% can be made in one operation, this is not advisable because of the possibility of excessive shell breakage. Also, large reductions may open the surface of the metal and cause difficulty in finishing.

The number of redraws that can be made before annealing is necessary depends on the alloy being drawn. The alloys with the lower rates of work hardening (Fig. 1) can often be redrawn more than once without intermediate annealing. Trial runs may be needed to determine when annealing is necessary.

Single-Action Drawing. As with all metals, the depth to which nickel-base alloys can be drawn in single-action presses without some means of blank restraint is controlled by the blank-thickness-to-diameter ratio. For single-action drawing without holddown pressure, the blank thickness should be at least 2% of the blank or workpiece diameter for reductions of up to 35%. With properly designed dies and sufficiently thick material, the reduction on the first (cupping) operation with a single-action set-up may be made equal to those recommended for double-action dies--that is, 35 to 40%. Redraws should not exceed a 20% reduction.

If the shell wall is to be ironed, the increased pressure on the bottom of the shell usually necessitates a decrease in the amount of reduction to prevent shell breakage. With reductions of 5% or less, the shell wall may be thinned by as much as 30% in one draw. With medium reductions of about 12%, the thickness of the shell wall can be decreased by about 15%. If the wall is to be reduced by a large amount, the shell should first be drawn to the approximate size with little or no wall thinning and the ironing done last. If a good surface finish is desired, the final operation should have a burnishing effect with only a slight change in wall thickness.

Clearances. Because nickel-base alloys have higher strengths than do low-carbon steel of drawing quality, nickel alloys have greater resistance to the wall thinning caused by the pressure of the punch on the bottom of the shell. Consequently, greater die clearance is required than is the case for steel if the natural flow of the metal is not to be resisted. However, the clearances required for nickel-base alloys are only slightly greater than those required for steel, and if dies used for steel have greater-than-minimum clearances, they are usually satisfactory for drawing nickel-base alloys, depending primarily on the mechanical properties of the alloy.

For ordinary deep drawing of cylindrical shells, a punch-die clearance per side of 120 to 125% of the blank thickness is sufficient and will prevent the formation of wrinkles. In the drawing of sheet thicker than 1.6 mm ($\frac{1}{16}$ in.), it is general practice to have the inside diameter of the draw ring larger than the diameter of the punch by three times the thickness of the blank (150% of stock thickness per side).

Draw-Ring and Punch Radii. Because nickel-base alloys work harden rapidly, relatively large draw-ring and punch radii should be used, especially for the early operations in a series of draws. Nickel-base alloys require more power to draw than does steel; consequently, the punch imposes a greater stress on the bottom corner of the shell. Small punch radii cause thinning of the shell at the line of contact, and if such a shell is further reduced, the thinned areas will appear farther up the shell wall, and may result in visible necking or rupture. Also, buffing a shell having thinned areas will cause the shell wall to have a wavy appearance. For redraws, it is preferable to draw over a beveled edge and to avoid round-edged punches except for the final draw.

The draw-ring radius for a circular die is principally governed by the thickness of the material to be drawn and the amount of reduction to be made. A general rule for light-gage material is to have the draw-ring radius from 5 to 12 times the thickness of the metal. Insufficient draw-ring radius may result in galling and excessive thinning of the wall.

Drawing Rectangular Shells. As with other materials, the depth to which rectangular shapes can be drawn in nickel-base alloys in one operation is principally governed by the corner radius. To permit drawing to substantial depths, the corner radii should be as large as possible. Even with large corner radii, the depth of draw should be limited to from two to five times the corner radius for alloys 400, 200, and 201, and to four times the corner radius for alloy 600 and alloy 75 (UNS N06075). The permissible depth also depends on the dimensions of the shape and on whether the shape has straight or tapered sides. The depth of draw for sheet less than 0.64 mm (0.025 in.) thick should not exceed an amount equal to three times the corner radius for alloys 400, 200, and 201 and should be less than that for alloy 600.

The corner radius on the drawing edge of the die should be as large as possible--approximately four to ten times the thickness of the material. To avoid wrinkles around the top corner of the shape, it is essential that the blank not be released prematurely.

In redrawing for the purpose of sharpening the corners or smoothing out wrinkles along the sides, only a small amount of metal should be left in the corners.

Frequently, it is necessary to draw shapes on dies designed to make a deeper single draw than is practical for nickel-base alloys. With such dies, the general practice is to draw about two-thirds of the full depth, to anneal the shape after this draw, and to complete the draw to full depth on the same dies. This same practice can be used to avoid wrinkling in drawing to lesser depths.

More information on the deep-drawing process is available in the article "Deep Drawing" in this Volume.

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Spinning

Power spinning is preferred over manual spinning for nickel-base alloys (see the article "Spinning" in this Volume). However, thin material, particularly alloys 200 and 400, can be manually spun with no difficulty. Table 2 gives practical limits on blank thickness for manual spinning of seven nickel-base alloys.

Table 2 Maximum blank hardness and thickness for manual spinning of nickel-base alloys

Alloy	**Maximum hardness, HRB**	Maximum thickness	
		mm	in.
Nickel 200	64	1.57	**0.062**
Nickel 201	55	1.98	**0.078**
Alloy 400	68	1.27	**0.050**
Alloy 600	80	0.94	**0.037**
Alloy 722	94	0.94	**0.037**
Alloy X-750	94	0.94	**0.037**

Alloy 801	**88**	**0.94**	**0.037**

Tools. Except for small, light shapes, the required pressure cannot be exerted with the ordinary bar or hand tool pivoted on a fixed pin. Most shapes require the use of a tool that is mechanically adapted for the application of greater force, such as a compound-lever tool or roller tools that are operated by a screw. For small jobs, a ball-bearing assembly can be used on the end of a compound lever to make a good roller tool. Roller tools should be used whenever practical in order to keep friction at a minimum and exert maximum pressure. Roller tools should also be used to perfect contours in the spinning of press-drawn shapes.

When possible, tools used for spinning nickel-base alloys should be broader and flatter than those used for softer materials. The broader tool distributes plastic flow over a greater area and reduces overstraining. Except for this consideration, bar and roller tools should be designed the same as those used for spinning copper or brass.

Correct tool materials are essential for successful spinning. The most suitable material for bar tools is a highly polished, hard alloy bronze. Hardened tool steels are preferred for roller and beading tools. Chromium-plated hardened tool steel is recommended, as it decreases metal pickup by the tool. Tools of common brass and carbon steel, which are used for spinning softer materials, are unsatisfactory for use with nickel-base alloys.

Rotary cutting shears are preferred for edge trimming. If rotary shears are not available, hand trimming bars hard faced with cobalt-base alloy may be used, but the trimming speed must be reduced. Hand trimming bars should be ground so that they have a back-rake angle of 15° to 20° from the cutting edge, and the edge must be kept sharp. A tool shaped like a thread-cutting lathe tool can be used for trimming. This tool also has a back rake from the cutting edge. With this type of tool, the material is not sheared off the edge; instead, the tool is fed into the side of the workpiece, and a narrow ring is cut from the edge. The workpiece should be supported at the back during all trimming operation.

Mandrels. Hardened alloy cast iron and steel mandrels give longer life and better results than softer materials such as wood. Hard maple or birch mandrels may be used for intermediate operations if production quantities are small and tolerances are liberal.

Spinning nickel-base alloys over mandrels that are the same as those used for copper alloys will not necessarily result in spun shapes of exactly the same dimensions as those of the softer metal. Most nickel-base alloy shapes will have slightly larger peripheries than those of softer metals spun over the same mandrel. This is caused by the greater springback of the nickel-base alloys.

Lubricants. Heavy-bodied, solid lubricants, such as yellow laundry soap, beeswax, and tallow, are recommended for spinning. These lubricants can be manually applied to the blank as it rotates. Blanks can be electroplated with 5 to 18 μm (0.2 to 0.7 mils) of copper to improve lubrication on difficult shapes.

Procedure for spinning nickel-base alloys is essentially the same as that used for other metals (see the article "Spinning" in this Volume).

As a general rule, in laying out a spinning sequence for alloy 400, an increase in height of 25 to 38 mm (1 to $1\frac{1}{2}$ in.) on the article being spun constitutes an operation if spinning is being done in the usual way with a bar tool. Approximately twice that depth per operation may be obtained with a compound-lever or roller tool. The workpiece should be trimmed and annealed before it is spun to greater depths.

A hard-surfaced mandrel should be provided for each operation so that the metal can always be pushed firmly against the surface of the mandrel. This procedure keeps the workpiece surface smooth and dense, and ensures the best results in annealing. With an insufficient number of intermediate mandrels, the material is subjected to an excessive amount of cold working. This may result in either spinning a buckle into the material or formation of a pebbled surface. It is virtually impossible to smooth out the former by additional cold work, or to correct the latter by annealing.

Figure 2 illustrates the number of mandrels and annealing operations necessary for spinning deep cups from 0.94 mm (0.037 in.) thick alloy 200, 400, and 600 blanks using hand tools. Figure 2 also shows the amount of forming that can be done before annealing and between intermediate anneals. The spinnability of other alloys can be estimated from their relative work-hardening rates (Fig. 1) and from their tensile properties.

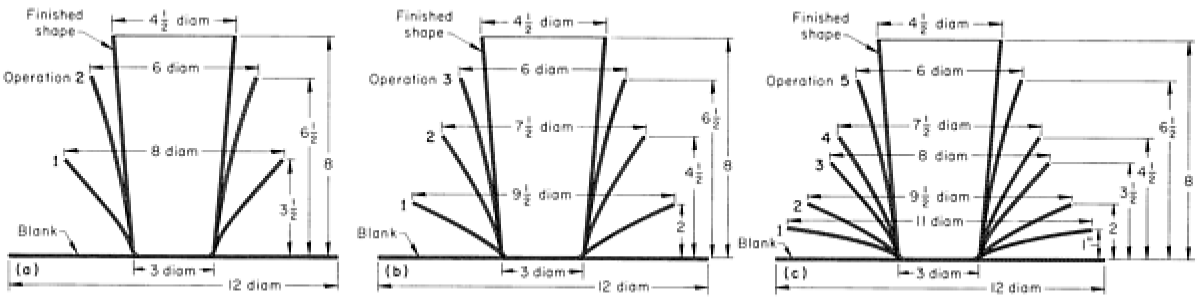

Fig. 2 Spinnability of three nickel-base alloys, as shown by the number of operations required for manual spinning of a deep cup from a 0.94 mm (0.037 in.) thick blank of each alloy. Workpieces were annealed between all operations. Dimensions given in inches. (a) Alloy 200. (b) Alloy 400. (c) Alloy 600.

In spinning, the optimum speed of the rotating blank is governed by its diameter and thickness. Small, thin blanks can be spun at greater speeds than larger or thicker pieces. Nickel-base alloys are often spun at speeds of one-half to three-fourths those normally used in spinning the same shape from softer metals. Lathe speeds of 250 to 1000 rpm are usually satisfactory. Trimming speeds must necessarily be slow; ordinarily, trimming is done at the minimum speed of the lathe.

Forming of Nickel-Base Alloys

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Bending Tube and Pipe

All common forming operations such as bending, coiling, and expanding can be performed readily on nickel-base alloy tube and pipe, using the same type of equipment as is used for other metals (see the article "Bending and Forming of Tubing" in this Volume). In general, material in the annealed condition is recommended. Alloys 400, 200, and 201 can be formed in the stress-relieved temper; however, the amount of deformation will be limited by the higher tensile strength and lower ductility. In bending, the minimum radius to which stress-relieved tubing can be bent is 25 to 50% greater than it is for annealed tubing of the same size.

The minimum radii to which nickel-base alloy tubing can be bent by various methods are given in Table 3. Depending on equipment design, tube size, and quality of the finished bend, it is possible to bend to smaller radii than those listed; however, trial bends should be made to determine whether the smaller radii are practicable.

Table 3 Minimum bend radii for nickel-base alloy tubes

Bending method	**Minimum mean bend radius[(a)]**	Maximum included angle of bend, degrees
Press bending, unfilled tube	6*D*	**120**
Roll bending, filled tube	4*D*	**360**
Compression bending		

Unfilled tube	2.5*D*	**180**
Filled tube(b)	2*D*	**180**
Draw bending		
Unfilled tube	3*D*	**180**
Filled tube(b)	**2*D***	**180**

(a) *D,* tube outside diameter.

(b) Or using mandrel

Bending Without Mandrels or Filler. When bending with no internal support, the dies should be slightly smaller than those used for bending with a mandrel or filler. Bending without use of a mandrel or filler is suitable only for tube and pipe that have a wall thickness greater than 7% of the outside diameter, or for large-radius bends. Nickel-base alloy tube in sizes within the above ratio can be bent with no mandrel or filler to a minimum mean radius of three times the outside diameter of the tube (3*D*) through 180°.

Bending With Mandrels or Fillers. Thin-wall tubing can be bent to small radii with freedom from wrinkles by use of a mandrel or filler. Thin-wall tubing of nickel alloys can be mandrel-bent through 180° to a minimum mean radius of 2*D.*

To minimize galling of the inside surface of the tube, mandrels should be made of hard alloy bronze rather than of steel. If steel mandrels are used, they should be chromium plated to reduce galling.

Mandrels must be lubricated before use; chlorinated oils with extreme-pressure (EP) additives are recommended for severe bending. For less severe bending or for ease of removal, water-soluble lubricants are used.

Any conventional filler material, such as sand, resin, and low-melting alloys, can be used. Sand is the least desirable because it is difficult to pack tightly and thus can lead to the formation of wrinkles or kinks during bending.

Low-melting alloy fillers produce the best bends. The expansion characteristics of these fillers ensure that voids are eliminated and a sound carrier is created.

Alloy fillers are removed by heating the bent tube in steam or hot water. Metallic fillers must not be removed by direct torch heating, because they contain elements such as lead, tin, and bismuth that will embrittle nickel alloys at elevated temperatures. It is imperative that all traces of metallic fillers be removed if the tube is to be subjected to elevated temperatures during subsequent fabrication or during service.

Press Bending. Press or ram bending, in which the tube is held by two supporting dies and a force is applied between the dies, is normally used only for heavy-wall tubing in which some flattening is tolerable. This method does not provide close tolerances and is applicable only to large-radius bends. The bend is limited to 120°, and the radius of the bend should not be less than six times the outside diameter of the tube (6*D*) if a smooth bend is desired. A filler material should be used for bends of radii less than 6*D.*

Pressure dies used in press bending should be at least two times longer than the outside diameter of the tube. Press bending with wing dies is used for unfilled, thin-wall, large-diameter tube.

Annealed tubing is not always preferred for press bending. Annealed tubing of low base hardness is not stiff enough to withstand deformation without excessive flattening. Consequently, nickel and nickel-copper alloys are usually press-bent in the stress-relieved temper. Nickel-chromium alloys have higher mechanical properties in the annealed condition than do nickel and nickel-copper alloys and should be press-bent in the annealed temper. Ideally, the choice of temper for a specific bend should be determined from the results of several trial bends.

Roll bending is the principal method of producing helical coils, spirals, and circular configurations because an included angle of 360° can be obtained. Bending may be done on either unfilled or filled tube. The minimum bend radius that can be attained on unfilled tube is approximately six times the outside diameter of the tube.

Compression bending uses a stationary bending form and a movable wiper shoe. This method is unsuitable for thin-wall tubing and is generally used with no mandrel support.

Compression bending can produce bend radii down to $2\frac{1}{2}D$ but is normally used only for large-radius bends. The maximum included angle that can be produced is 180°.

Draw bending is the most common bending process and the preferred method for bending nickel-base alloy tube. The process is similar to compression bending, except that the bending form revolves and the pressure die either remains stationary or slides along a straight line. The sliding pressure die is preferred, because it distributes the applied stresses more evenly.

Bends of up to 180° with a minimum radius of $2D$ can be produced by draw bending. Bending can be done with or without a mandrel. In general, a mandrel is preferred and must be used when the ratio of tube diameter to wall thickness is above the limit suitable for bending without tube wrinkling or collapsing. Various types of mandrels are used, including ball and plug types.

Hot Bending. When possible, tube and pipe should be formed by cold bending. If hot bending is necessary, it is performed by standard hot bending methods.

Hot bending is normally limited to tube and pipe larger than 2 in. schedule 80 (60.5 mm, or 2.375 in., OD and 5.54 mm, or 0.218 in., wall thickness). Thin-wall tubing should not be bent hot, because it is difficult to retain sufficient heat to make the bend.

Hot bending should be done on filled tube only. Sand is the normal filler material. The sand must be free of sulfur, because contamination of nickel-base alloys by sulfur causes cracking during bending. Sulfur can be removed from sand by heating to about 1150 °C (2100 °F) in an oxidizing atmosphere. Tubing must be cleaned thoroughly before filling or heating.

Sand-filled tube and pipe in small sizes (60.2 to 72.9 mm, or 2.37 to 2.87 in., OD) can be bent hot to a minimum mean radius of two times the outside diameter of the tube. Larger sizes require greater bend radii.

In hot bending, the metal should be worked as soon as possible after removal from the furnace, to avoid cooling before bending is completed.

Forming of Nickel-Base Alloys

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Bending of Plate, Sheet, and Strip

Table 4 lists minimum, bend diameters for hot-rolled and annealed nickel-base alloy sheet, plate, and strip. In compiling these data, a sample was judged to have passed the 180° bend test if its surface showed no ductile fracturing. Because of the effects of various surface conditions and heat treatments on bendability, the data in Table 4 should be regarded as general guidelines. Many of the materials can in fact be bent in stages to tighter radii than those suggested provided initial bending is not too severe.

Table 4 Minimum bend diameters for annealed sheet and strip and hot-rolled, annealed plate

Alloys were bent 180°; minimum bend diameters are given in terms of material thickness.

Alloy	**Product form**	**Thickness, *t***		Minimum bend diameter[(a)]
		mm	**in.**	
Nickel 200	Sheet, strip	0.30-6.35	0.012-0.250	**1*t***
	Plate	4.75-6.35	0.187-0.250	**2*t***
Alloy 400	Sheet, strip	0.30-2.77[(b)]	0.012-0.109[(b)]	**1*t***
		2.79-6.35	0.110-0.250	**2*t***
	Plate	4.75-6.35[(c)]	0.187-0.250[(c)]	**2*t***
Alloy 600	Sheet, strip	0.30-6.35	0.012-0.250	**1*t***
	Plate	4.75-6.35	0.187-0.250	**2*t***
Alloy 625[(d)]	Sheet, strip	0.30-6.35	0.012-0.250	**2*t***
	Plate	4.75-6.35	0.187-0.250	**2*t***
Alloy 718[(d)]	Sheet, strip	0.30-1.24	0.012-0.049	**1*t***
		1.27-6.35	0.050-0.250	**2*t***
Alloy X-750	Sheet, strip	0.30-1.24	0.012-0.049	**1*t***
		1.27-6.35	0.050-0.250	**2*t***
Alloy 800	Sheet, strip	0.30-6.35[(b)]	0.012-0.250[(b)]	**1*t***
	Plate	4.75-6.35[(b)]	0.187-0.250[(b)]	**2*t***
Alloy 825	Sheet, strip	0.30-6.35[(b)]	0.012-0.250[(b)]	**2*t***
	Plate	**4.75-6.35[(b)]**	**0.187-0.250[(b)]**	**2*t***

(a) Bend tests performed according to ASTM E 290 with a guide bend jig as described in ASTM E 190.

(b) Successful bending depended on surface condition of the samples, with particular regard to freedom from oxidation.

(c) Samples were descaled.

(d) Sheared edges were ground or machined.

The importance of surface condition is demonstrated by the alloys from which scale or oxides must be removed to ensure successful bending. As indicated in Table 4, scale can be removed either by chemical or mechanical means depending on the alloy.

Forming of Nickel-Base Alloys

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Expanding

Nickel-base alloy tubing can be expanded into tube sheets for heat exchanger applications by any conventional method. The oversize allowance on tube sheet holes to the nominal outside diameter of the tube should be kept to a minimum. The tube sheet hole should be 0.10 to 0.20 mm (0.004 to 0.008 in.) larger than the nominal outside diameter of the tube for tubing less than 38 mm ($1\frac{1}{2}$ in.) in outside diameter. For tubing 38 mm ($1\frac{1}{2}$ in.) or larger in outside diameter, the oversize allowance should be 0.23 to 0.25 mm (0.009 to 0.010 in.).

Procedure. Expanding may be done by drifting with sectional expanders or by rolling with three-roll expanders. Three-roll expanders are preferred. The ends of rolled-in tubing are flared in the conventional manner.

The tube sheet hole and both the outside and inside surfaces of the tube must be free of all foreign matter such as oxides, dirt, and oil. The ends of the tube should also be deburred before rolling.

Lubrication should be provided between the rollers of the tool and the inside surface of the tube. Any sulfur-free mineral oil or lard oil, either diluted or straight, can be used. Lubricants that contain embrittling or contaminating elements such as sulfur or lead should be avoided, because of the difficulty in cleaning the finished assembly.

Controlled rolling equipment should be used to prevent overexpanding, which may distort the tube sheet and deform the tube sheet ligaments, causing loose-fitting tubes. This is particularly true when the tube has a higher hardness than the tube sheet or a significantly higher rate of work hardening.

Temper. The tube sheet should be harder than the tube being rolled into it. Otherwise, springback in the tube may be greater than in the tube sheet, causing a gap between the two when the expanding tool is removed. For this reason, tube sheets are usually supplied in the as-rolled or as-forged temper and tube is supplied in the annealed temper. The need for the tube sheet to be harder than the tube is greatest when the thickness of the tube sheet is less than the outside diameter of the tube, and when the center-to-center spacing of the tubes (tube pitch) is less than $1\frac{1}{4}$ times the outside diameter of the tube or the outside diameter plus 6.4 mm ($\frac{1}{4}$ in.), whichever is greater.

Stress-relieved tubing may be slightly harder than the tube sheet but can be expanded to form a satisfactory connection if greater care is exercised in expanding. For greater assurance of pressure tightness, a seal weld may be placed around the end of the tube after expanding. The stress-relieved temper is suitable for either welding or silver brazing.

Tubing in the annealed condition is used when optimum rolling or expanding characteristics are desired or for severe cold bending and flaring.

Forming of Rod and Bar

Rod and bar in the annealed condition are preferred for cold forming. Material in other tempers may be required for some forming operations or when properties that cannot be obtained by heat treatment after forming are desired in the end product.

Bending. Rod and bar may be bent in the same manner as tubing. The possibility of collapsing or wrinkling is eliminated, because the solid section provides its own internal support.

Most nickel-base alloys have suitable mechanical properties in the hot-finished condition for moderate bending. The annealed temper should always be used for extremely small-bend radii or low radius-to-thickness ratios. Cold drawn, annealed material should be used if surface roughening (orange peeling) related to coarse grain structure is undesirable.

Coiling of rod and bar is limited almost entirely to the production of springs. Nickel-base alloy springs for high-temperature service are usually annealed or solution treated and aged after forming. Consequently, they may be produced from annealed material (or even produced by hot coiling) with no adverse effect on final properties.

If the desired properties cannot be obtained by heat treating after forming, the spring must be coiled from tempered, cold-worked material. The use of tempered material greatly increases the minimum radius to which the rod or bar can be coiled.

Pressures and speeds encountered in production coiling usually require the use of high-grade lubricants with good film strength. Wire rod is often coated with copper to reduce friction and improve retention of organic lubricants.

The severe cold forming involved in producing coils and the severe service conditions in which these products are often used demand a high-grade starting product. Centerless-ground or ground and cold-drawn material is used to obtain the necessary quality.

Forming of Nickel-Base Alloys

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Cold Heading and Cold Extrusion

Cold heading and cold extrusion are most often used in the production of fasteners and similar cold upset parts. Cold extrusion is rarely done except in conjunction with cold heading.

The high strength and galling characteristics of nickel-base alloys require slow operating speeds and high-alloy die materials. Cold heading machines should be operated at a ram speed of about 10 to 15 m/min (35 to 50 ft/min). These ram speeds correspond to operating speeds of 60 to 100 strokes per minute on medium-size equipment.

Tools should be made of oil-hardening or air-hardening die steel. The air-hardening types, such as AISI D2, D4, or high-speed steel (M2 or T1), tempered to 60 to 63 HRC, are preferred.

Material. Rod stock (usually less than 25 mm, or 1 in., in diameter) in coils is used for starting material, as cold heading is done on high-speed automatic or semiautomatic equipment. Although alloy 400 is sometimes cold headed in larger sizes, 22 mm ($\frac{7}{8}$ in.) is the maximum diameter in which alloys 400 and K-500 can be cold headed by most equipment. Limiting sizes in harder alloys are proportionately smaller, depending on their hardness and yield strength in the annealed condition. Stock sizes in excess of these limits are normally hot formed.

Cold-heading equipment requires wire rod with diameter tolerances in the range of 0.076 to 0.127 mm (0.003 to 0.005 in.). Because alloy 400 should be cold headed in the 0 or No. 1 temper to provide resistance to crushing and buckling during forming, these tolerances can normally be obtained with the drawing pass used to develop this temper. For tighter tolerances or harder alloys, fully cold-drawn material must be used.

The surface quality of regular hot-rolled wire rod, even with a cold sizing pass, may not be adequate for cold heading. Consequently, a special cold heading quality wire rod is usually recommended. Configurations that are especially susceptible to splitting, such as rivets, flat-head screws and sockethead bolts, require shaved or centerless-ground material.

Lubricants. To prevent galling, high-grade lubricants must be used in cold heading of nickel-base alloys.

Lime and soap are usually used as a base coating on alloy 400. Better finish and die life can be obtained by using copper plating 7.5 to 18 μm (0.3 to 0.7 mils) thick as a lubricant carrier. Copper plating may be used also on the chromium-containing alloys 600 and 800, but oxalate coatings serve as an adequate substitute.

Regardless of the type of carrier, a base lubricant is best applied by drawing it on in a light sizing pass to obtain a dry film of the lubricant. Any of the dry soap powders of the sodium, calcium, or aluminum stearate types can be applied this way.

If the wire rod is to be given a sizing or tempering pass before the cold-heading operations, the heading lubricant should be applied during drawing.

Lubrication for cold heading is completed by dripping a heavy, sulfurized mineral oil or a sulfurized and chlorinated paraffin on the blank as it passes through the heading stations.

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Straightening

Rod and bar in straight lengths are usually straightened by conical rolls, stretchers, or punch presses. Material in coil form is straightened with staggered-roll straighteners or rotating-die straighteners.

Like other forming equipment, straighteners require about 50% more power for nickel-base alloys than they do for low-carbon steel; a straightener having a capacity of 12.7 mm ($\frac{1}{2}$ in.) diameter in steel will be limited to about 9.5 mm ($\frac{3}{8}$ in.) diam in nickel alloys.

A lubricant should be used with rotating-die straighteners to reduce scratching and scoring and to improve die life. Spiral scoring may become quite severe on large sizes of harder alloys. If scoring cannot be held to an acceptable level by the use of lubricants, material in the softest temper available should be used.

Staggered-roll straightening involves lower contact velocities than do rotating-die types, and lubrication is less critical. Coil stock is often straightened without lubricant, for better grip on rolls.

Dies for rotary-die straighteners may be either bronze or cast iron. Cast iron dies must be used if contamination from rubbed-off bronze occurs in the end product, and no pickling is done after straightening.

Forming of Nickel-Base Alloys

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Cold-Formed Parts for High-Temperature Service

A part that is highly stressed from cold forming may require heat treatment to avoid excessive creep in service above its recrystallization temperature. Recrystallization of a specific alloy is determined largely by the extent of cold work and the temperature to which the part is exposed during service. The grain size and exact composition of the material also complicate prediction of the recrystallization temperature.

The nickel-base alloys most likely to be subjected to high temperatures in service are the iron-nickel-chromium and nickel-chromium alloys (see Table 1). These alloys are frequently used in the grain-coarsened condition at service temperatures above 595 °C (1100 °F).

Generally, cold-formed nickel-base alloys should be heat treated if they have been strained in either tension or compression by more than 10% and will be subject to service temperatures above 650 °C (1200 °F). Producers should be consulted for the proper thermal treatments.

In certain alloys, heavy cold working (for example, highly restrained bending) followed by exposure at moderate to high temperatures (for example, stress relieving or age hardening) can lead to cracking. In age-hardenable alloys, for example, the combination of high residual tensile stress and the stress associated with the aging response may exceed the stress-rupture strength of the material. In nonage-hardenable alloys, excessive cold working of coarse-grain material (grain size of ASTM No. 5 or coarser) without the recommended intermediate annealing can cause cracking during subsequent exposure at stress-relieving or annealing temperatures. Testing the material under actual conditions of forming and heating will determine its susceptibility to cracking.

Springs can be cold formed from age-hardenable alloys in the annealed or cold-drawn temper. For service at temperatures above 315 °C (600 °F), springs should be solution annealed before aging to prevent loss of strength from relaxation.

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Introduction

TITANIUM AND ITS ALLOYS can be formed in standard machines to tolerances similar to those obtained in the forming of stainless steel. However, to reduce the effect of springback variation on accuracy and to gain the advantage of increased ductility, the great majority of formed titanium parts are made by hot forming or by cold preforming and then hot sizing.

The following characteristics of titanium and titanium alloy sheet materials must be considered in forming:

- Notch sensitivity, which may cause cracking and tearing, especially in cold forming
- Galling (more severe than with stainless steel)
- Relatively poor ability to shrink (a disadvantage in some flanging operations)
- Potential embrittlement from overheating and from absorption of gases, principally hydrogen (scale and the surface layer adversely affected by the slower penetration of oxygen can be removed readily)
- Limited workability

- Higher springback than that encountered in ferrous alloys at the same strength level

However, as long as these limitations are recognized and established guidelines for hot and cold forming are followed, titanium and titanium alloys can be successfully formed into complex parts.

The mechanical properties, and therefore the formabilities, of titanium and its alloys vary widely. For example, the tensile strength of different grades of commercially pure (CP) titanium ranges from 240 to 550 MPa (35 to 80 ksi); correspondingly large differences in the minimum bend radius are obtainable at room temperature. The tensile strength and ductility of CP titanium are largely dependent on its oxygen content. Table 1 lists the common designations, compositions, and selected mechanical properties of some titanium alloys.

Table 1 Designations, nominal compositions, and selected mechanical properties of some titanium alloys

Common alloy designation	**Nominal composition, %**	**Alloy type**	**Minimum ultimate tensile strength**		**Minimum 0.2% yield strength**		Elongation, %
			MPa	**ksi**	**MPa**	**ksi**	
ASTM grade 1	Unalloyed titanium	α	240	35	170	25	**24**
ASTM grade 2	Unalloyed titanium	α	345	50	280	40	**20**
ASTM grade 3	Unalloyed titanium	α	450	65	380	55	**18**
ASTM grade 4	Unalloyed titanium	α	550	80	480	70	**15**
Ti-Pd (ASTM grade 7,11)	Ti-0.15Pd	α	345/240	50/35	275/170	40/25	**20/24**
ASTM grade 12	Ti-0.3Mo-0.8Ni	Near α	480	70	380	55	**18**
Ti-3-2.5 (ASTM grade 9)	Ti-3Al-2.5V	Near α	620	90	520	75	**22**
Ti-64 (ASTM grade 5)	Ti-6Al-4V	α-β	900	130	830	120	**17**
Ti-5Ta	Ti-5Ta	Near α	. . .	. . .	. . .	. . .	**. . .**
Ti-5-2.5	Ti-5Al-2.5Sn	α	790	115	760	110	**22**
Ti-8-1-1	Ti-8Al-1V-1Mo	Near α	900	130	830	120	**12**
Ti-6-2-4-2	Ti-6Al-2Sn-4Zr-2Mo	Near α	900	130	830	120	**15**
Ti-550	Ti-4Al-2Sn-4Mo-0.5Si	α-β	. . .	. . .	. . .	. . .	**. . .**

Ti-6-6-2	Ti-6Al-6V-2Sn-0.6Fe-0.6Cu	α-β	1030	150	970	140	**14**
Corona 5	Ti4.5Al-1.5Cr-5Mo	α-β	965	140	900	130	**12-15**
Ti-6-2-4-6	Ti-6Al-2Sn-4Zr-6Mo	α-β	1170	170	1100	160	**11**
Ti-10-2-3	Ti-10V-2Fe-3Al	Near β	1170	170	1100	160	**9**
Ti-15-3-3-3	Ti-15V-3Sn-3Cr-3Al	β	790	115	770	112	**20-25**
Ti-3-8-6-4-4	Ti-3Al-8V-6Cr-4Zr-4Mo	β	900	130	830	120	**10-15**
Ti-13-11-3	**Ti-13V-11Cr-3Al**	β	**1170**	**170**	**1100**	**160**	**18**

Note

* R. Bajoraitis, Boeing Commercial Airplane Company; G.C. Cadwell, Rohr Industries Inc.; E. Cook, Douglas Aircraft Company; K. Herbert and H. Hollenbach, Murdock Inc.; R.S. Kaneko, Lockheed-California Company; B.W. Kim, Northrop Corp.; F. Koeller, Consultant; E.E. Mild, Timet Inc.; L.J. Pionke, McDonnell Douglas; P.A. Russo, RMI Company; J. Schley, RMI Company; J.K. Solheim, Metal Bellows Division of Parker Bertea Aerospace Group; G.W. Stacher, Rockwell International; R. Witt, Grumman Aircraft Systems

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Titanium Materials

There are several grades of unalloyed titanium (see Table 1). The primary difference between the grades is in the amounts of interstitial elements (for example, oxygen and nitrogen) and iron. Grades of higher purity (lower interstitial content) are lower in strength, hardness, and transformation temperature than those higher in interstitial content. The high solubility of the interstitial elements oxygen and nitrogen makes titanium rather unique among metals and creates problems that are not of concern in most other metals. For example, heating titanium in air at high temperature results not only in oxidation but also in solid-solution hardening of the surface as a result of inward diffusion of oxygen. A surface-hardened zone (α case) is formed. This layer is usually removed by machining, chemical milling, or other mechanical means prior to placing a part in service because the presence of a case reduces fatigue strength and ductility.

Alloy Ti-6Al-4V is the most widely used titanium alloy, accounting for about 60% of total titanium production. Unalloyed grades constitute about 20% of production, and all other alloys make up the remaining 20%. Selection of an unalloyed grade of titanium, an α or near-α alloy, an α-β alloy, or a near-β or β alloy depends on desired mechanical properties, service requirements, cost considerations, and the other factors that enter into any material selection process.

Commercially pure titanium is usually selected for its excellent corrosion resistance, especially in applications in which high strength is not required. The yield strengths of CP grades (Table 1) vary from less than 170 to more than 480

MPa (25 to 70 ksi) simply as a result of variation in the interstitial and impurity levels. Oxygen and iron are the primary variants in these grades; strength increases with increasing oxygen and iron contents.

Alpha and Near-Alpha Alloys. Alpha alloys that contain aluminum, tin, and/or zirconium are preferred for high-temperature and cryogenic applications. Alpha-rich alloys are generally more resistant to creep at high temperature than α-β or β alloys. The extra-low-interstitial α alloys (ELI grades) retain ductility and toughness at cryogenic temperatures, and Ti-5Al-2.5Sn-ELI has been extensively used in such applications.

Unlike α-β and β alloys, α alloys cannot be strengthened by heat treatment. Generally, α alloys are annealed or recrystallized to remove residual stresses induced by cold working.

Alpha alloys that contain small additions of β stabilizers (for example, Ti-8Al-1V-1Mo or Ti-6Al-2Nb-1Ta-0.8Mo) are sometimes classed as near-α alloys (Table 1). Although they contain some retained β phase, these alloys consist primarily of α and behave more like conventional α alloys than α-β alloys.

Alpha-beta alloys contain one or more α stabilizers or α-soluble elements plus one or more β stabilizers. These alloys retain more β phase after final heat treatment than near-α alloys; the specific amount depends on the amount of β stabilizers present and on heat treatment.

Alpha-beta alloys can be strengthened by solution treating and aging. Solution treating is usually done at a temperature high in the two-phase α-β field and is followed by quenching in water, oil, or other suitable quenchant. As a result of quenching, the β phase present at the solution-treating temperature may be retained or may be partly transformed during cooling by either martensitic transformation or nucleation and growth. The specific response depends on alloy composition, solution-treating temperature (β-phase composition at the solution temperature), cooling rate, and section size. Solution treatment is then followed by aging, usually at 480 to 650 °C (900 to 1200 °F).

Solution treating and aging can increase the strength of α-β alloys 30 to 50%, or more, over the annealed or overage condition. Response to solution treating and aging depends on section size; alloys relatively low in stabilizers (Ti-6Al-4V, for example) have poor hardenability and must be quenched rapidly to achieve significant strengthening. For Ti-6Al-4V, the cooling rate of a water quench is not rapid enough to cause significant hardening of sections thicker than about 25 mm (1 in.). Hardenability increases as the content of β stabilizers increases.

Beta alloys are richer in β-phase stabilizers and leaner in α stabilizers than α-β alloys. They are characterized by high hardenability, with β phase completely retained upon the air cooling of thin sections or the water quenching of thick sections. Beta alloys in sheet form can be cold formed more readily than high-strength α-β or α alloys. An example of this is the Ti-15V-3Sn-3Cr-3Al alloy, which is formed almost exclusively at room temperature. After solution treating, β alloys are aged at temperatures of 450 to 650 °C (850 to 1200 °F) to partially transform the β phase to α. The α forms as finely dispersed particles in the retained β, and strength levels comparable to or superior to those of aged α-β alloys can be attained.

In the solution-treated condition (100% retained β), β alloys have good ductility and toughness, relatively low strength, and excellent formability. Solution-treated β alloys begin to precipitate α phase at slightly elevated temperatures and are therefore unsuitable for elevated-temperature service without prior stabilization or overaging treatment.

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

General Formability

Titanium and titanium alloy sheet is strain hardened by cold forming. This normally increases tensile and yield strengths and causes a slight drop in ductility. Titanium metals exhibit a high degree of springback in cold forming. To overcome this characteristic, titanium must be extensively overformed or, as is done most frequently, hot sized after cold forming.

Hot forming does not greatly affect final properties. Forming at temperatures ranging from 595 to 815 °C (1100 to 1500 °F) allows the material to deform more readily and simultaneously stress relieves the deformed material; it also minimizes springback. The net effect in any forming operation depends on total deformation and actual temperature during forming. Because titanium metals tend to creep at elevated temperature, holding under load at the forming temperature (creep forming) is another alternative for achieving the desired shape without the need to compensate for extensive springback.

The Bauschinger Effect. In all forming operations, titanium and its alloys are susceptible to the Bauschinger effect--a drop in compressive yield strength subsequent to tensile straining in the same or another direction. The Bauschinger effect, unlike the strain-hardening behavior observed in other metals, involves stress-strain asymmetry that results in hysteretis stress-strain loops such as those shown schematically in Fig. 1. The Bauschinger effect is most pronounced at room temperature; plastic deformation (1 to 5% tensile elongation) at room temperature always introduces a significant loss in compressive yield strength, regardless of the initial heat treatment or strength of the alloys. At 2% tensile strain, for example, the compressive yield strength of Ti-6Al-4V drops to less than one-half the value for solution-treated material. Increasing the temperature reduces the Bauschinger effect; subsequent full thermal stress relieving completely removes it.

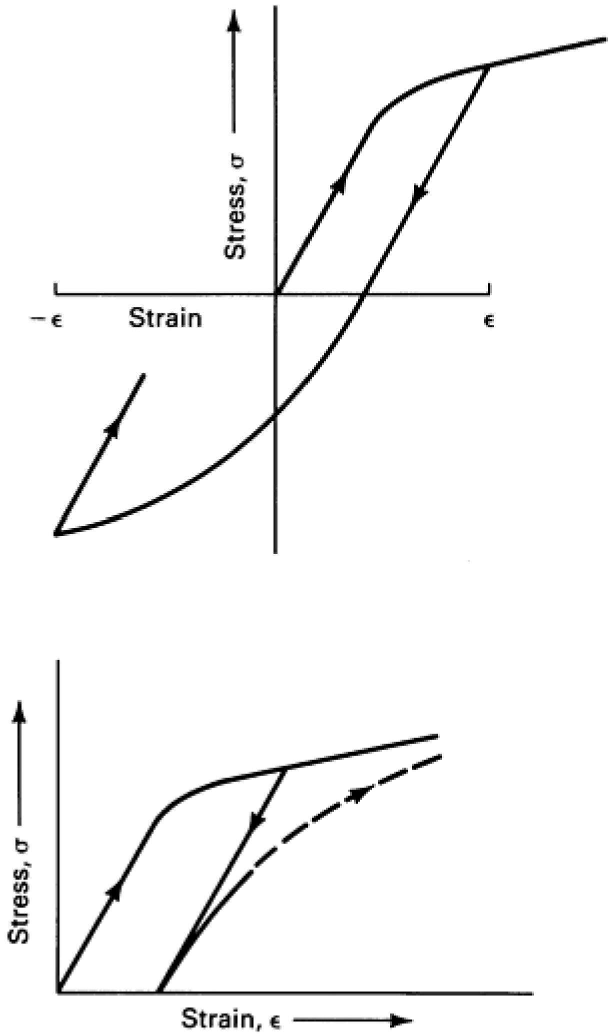

Fig. 1 Schematics showing two types of hysteresis stress-strain loops resulting from the Bauschinger effect in titanium alloys. Source: Ref 1.

Temperatures as low as the aging temperature will remove most of the Bauschinger effect in solution-treated titanium alloys. Heating or plastic deformation at temperatures above the normal aging temperature for solution-treated Ti-6Al-4V will cause overaging; as a result, all mechanical properties will decrease.

Reference cited in this section

1. E.W. Collings, *The Physical Metallurgy of Titanium Alloys,* American Society for Metals, 1984, p 151

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Preparation of Sheet for Forming

Before titanium sheet is formed, it should be inspected for flatness, uniformity, and thickness. Some plants test incoming material for hardness, strength, and bending behavior.

Critical regions of titanium sheet should not be nicked, scratched, or marred by tool or grinding marks, because the metal is notch sensitive. All scratches deeper than the finish produced by 180-grit emery should be removed by sanding the surface. Edges of the workpieces should be smooth, and scratches, if any, should be parallel to the edge of the blank to prevent any concentration of stress that could cause the workpiece to break. To prevent difficulty in forming, as by increased notch sensitivity, surface oxide or scale should be removed before forming.

Cleaning. Grease, oil, stencils, fingerprints, dirt, and all chemicals or residues that contain halogen compounds must be removed from titanium before any heating operation. Salt residues on the surface of the workpiece can cause hot-salt cracking in service or in heat treating; even the salt from a fingerprint can cause problems. Therefore, titanium is often handled with clean cotton gloves after cleaning and before hot forming, hot sizing, or heat treatment.

Ordinary cleaners and solvents such as isopropyl alcohol and acetone are used on titanium. Halogen compounds, such as trichlorethylene, should not be used, unless the titanium is pickled in acid after cleaning.

Titanium that has been straightened or formed with tools made of lead or low-melting alloy should be cleaned in nitric acid. Detailed information on the cleaning of titanium is given in the article "Surface Engineering of Titanium and Titanium Alloys" in *Surface Engineering,* Volume 5 of the *ASM Handbook.*

Removal of Tool Marks. Tool and grinding marks in titanium can be moderated in an aqueous acid bath containing (by volume) 30% concentrated nitric acid and not more than 3% hydrofluoric acid. Failure to keep the ratio of nitric to hydrofluoric acid at 10 to 1 or greater (to suppress the formation of hydrogen gas during pickling), or the use of any pickling bath that produces hydrogen, can result in excessive hydrogen pickup. The acid bath should remove 0.025 to 0.075 mm (0.001 to 0.003 in.) of thickness from each surface to eliminate the marks made by abrasives. Titanium should be washed or cleaned before it is immersed in acid.

Removal of Scale. Heavy gray and black scale and similar hard oxides that form on titanium at temperatures of 540 °C (1000 °F) and higher can be removed chemically or by wet or dry mechanical methods that use fine abrasives. Wire brushing and coarse abrasives are generally not used, because they can leave stress-raising marks; if these techniques are used, the damaged surface layer can be removed by pickling in nitric-hydrofluoric acid, as described above.

Thin oxides that form at temperatures below 540 °C (1000 °F) can be removed by acid pickling. Very tenacious oxides may require gritblasting prior to pickling.

Tool Materials and Lubricants

Tool materials for forming titanium are chosen to suit the forming operation, forming temperature, and expected quantity of production. The cost of tool material is generally only a small fraction of the cost of tools, unless forming temperature is such that heat-resistant alloy tooling is required.

Cold forming can be done with epoxy-faced aluminum or zinc tools. Hot-forming tools are fabricated from ceramic, cast iron, tool steel, stainless steel, and nickel-base alloys.

Tool materials for the superplastic forming of titanium alloys are a special case (see the section "Superplastic Forming" in this article). They must be able to withstand the high temperatures (870 to 925 °C, or 1600 to 1700 °F) required for superplastic forming, but must not contain more than about 6% Ni, because of the possibility of nickel migration into the work metal at superplastic forming temperatures. Cast ceramics, 22-4-9 stainless steel (Fe-0.5C-22Cr-9Mn-4Ni), and 49M steel are used for this purpose.

Lubricants. Galling is the most severe problem to be overcome in hot forming. Lubricants may react unfavorably with titanium when it is heated, although molybdenum disulfide suspended in a volatile carrier, colloidal graphite, and graphite-molybdenum disulfide mixtures have been successfully used. Boron nitride slurries also are used. If the lubricant reacts with oxidation products to produce a tenacious surface coating, it must be removed by sandblasting with garnet grit or 120-mesh aluminum oxide, followed by acid pickling.

Boron nitride is the preferred temperature-resistant lubricant because of its higher lubricity, as well as ease of application and removal. Other lubricants used for hot forming have a graphite or molybdenum disulfide base. Zinc phosphate conversion coatings are sometimes first produced on the work metal surface to aid in the retention of lubricants during severe forming.

Lubricants for the cold forming of titanium are generally similar to those used for the severe forming of aluminum alloys (see the articles "Forming of Aluminum Alloys" and "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume). Tool materials and lubricants for the cold and hot forming of titanium alloys are given in Table 2.

Table 2 Tool materials and lubricants used for forming titanium alloys

Operation(s)	**Tool materials**	Lubricants
Cold forming		
Press forming, drawing, drop hammer forming	Cast zinc die or lead punch with stainless steel caps	**Graphite suspension in a suitable solvent**
Press-brake forming	4340 steel (36-40 HRC)	**Graphite suspension in a suitable solvent**
Contour roll forming, three-roll forming	AISI O2 tool steel	**SAE 60 oil**
Stretch forming	Epoxy-faced cast aluminum, cast zinc, cast bronze	**Grease-oil mixtures, wax; 10:1 wax-graphite mixture**

Hot forming		
Press forming, drawing, drop hammer forming	High-silicon cast iron, stainless steels, heat-resistant alloys	**Graphite suspension, boron nitride**
Sizing	Low-carbon steel, high-silicon gray or ductile iron, AISI H13 tool steel, stainless steels, heat-resistant alloys	**Graphite suspension, boron nitride**
Press-brake forming	AISI H11 or H13 tool steel, heat-resistant alloys	**Graphite suspension, boron nitride**
Contour roll forming, three-roll forming	AISI H11 or H13 tool steel	**Graphite suspension, boron nitride**
Stretch forming	Cast ceramics, AISI H11 or H13 tool steel, high-silicon gray iron	**Graphite suspension, 10:1 wax-graphite mixture, boron nitride**
Superplastic forming	**Ceramics, 22-4-9 stainless steel, 49M heat-resistant steel**	**Boron nitride**

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Blank Preparation

Most blanking of titanium alloy sheet 6.4 mm ($\frac{1}{4}$ in.) thick or less is done in a punch press. As with other metals, maximum blank size depends on stock thickness, shear strength, and available press capacity.

Dies must be rigid and sharp to prevent cracking of the work metal. Hardened tool steel must be used for adequate die life.

In one application, holes 6.35 mm (0.25 in.) in diameter were punched in 1.02 to 3.56 mm (0.040 to 0.140 in.) thick annealed alloy Ti-6Al-4V sheet to within ±0.051 mm (±0.002 in.) of diameter and with surface roughness of less than 1.3 μm (50 μin.). The best holes were produced with flat-point punches having 0.025 mm (0.001 in.) die clearance.

Shearing. Titanium sheet up to 3.56 mm (0.140 in.) thick can generally be sheared without difficulty; with extra care, titanium sheet as thick as 4.75 mm (0.187 in.) can be sheared. Shears intended for low-carbon steel may not have enough hold-down force to prevent titanium sheets from slipping. A sharp shear blade in good condition with a capacity for cutting 4.8 mm ($\frac{3}{16}$ in.) thick low-carbon steel can cut 3.2 mm ($\frac{1}{8}$ in.) thick titanium sheet. Cutters should be kept sharp to prevent edge cracking of the blank.

Sheared edges, especially on thicker work metal, can have straightness deviations of 0.25 to 5 mm (0.01 to 0.20 in.), usually because the shear blade is not stiff enough. Shearing can cause cracks at the edges of some titanium sheet thicker than 2.0 mm (0.080 in.). If cracks or other irregularities develop in a critical portion of the workpiece, an alternative method of cutting should be used, such as band sawing, abrasive waterjet cutting, or laser cutting (see the articles "Abrasive Waterjet Cutting" and "Laser Cutting" in this Volume).

Slitting of titanium alloy sheet can be done with conventional slitting equipment and with draw-bench equipment. Slitting shears are capable of straight cuts only; rotary shears can cut gentle contours (minimum radius: ~250 mm, or 10 in.). The process can be used for sheet thicknesses to 2.54 mm (0.100 in.). However, an individual machine must be restricted to titanium thicknesses of low-carbon steel for which the machine is rated.

Band sawing prevents cracking at the edges of titanium sheet but causes large burrs. Band sawing is generally used to cut titanium sheet that is 3.18 mm (0.125 in.) or more in thickness.

Nibbling can be used to cut irregular blanks of titanium, but most blanks need filing or grinding after nibbling.

Edge Preparation. All visual evidence of a sheared or broken edge on a part should be removed by machining, sanding, or filing before final deburring or polishing. All rough projections, scratches, and nicks must be removed. Extra material must be allowed at the edges of titanium blanks so that shear cracks and other defects can be removed. On sheared parts, a minimum of 0.25 mm (0.010 in.) must be removed from the edge; on punched holes, 0.35 mm (0.014 in.). On parts cut by friction band sawing or abrasive sawing, 6.35 mm (0.25 in.) or one thickness of sheet should be removed, whichever is the smaller.

The lay of the finish on the edges of sheet metal parts should be parallel to the edge surface of the blank, and sharp edges should be removed. Edges of shrink flanges and stretch flanges must be polished before forming. To prevent scratching the forming dies, edges of holes and cutouts should be deburred on both sides and should be polished where they are likely to stretch during forming.

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Cold Forming

Commercially pure titanium and the most ductile titanium alloys, such as Ti-15V-3Sn-3Cr-3Al and Ti-3Al-8V-6Cr-4Zr-4Mo, can be formed cold to a limited extent. Alloy Ti-8Al-1Mo-1V sheet can be cold formed to shallow shapes by standard methods, but the bends must be of larger radii than in hot forming and must have shallower stretch flanges. The cold forming of other alloys generally results in excessive springback, requires stress relieving between operations, and requires more power. Titanium and titanium alloys are commonly stretch formed without being heated, although the die is sometimes warmed to 150 °C (300 °F). For the cold forming of all titanium alloys, formability is best at low forming speeds.

To improve accuracy, cold forming is generally followed by hot sizing. Hot sizing and stress relieving are ordinarily needed to reduce stress and to avoid delayed cracking and stress corrosion. Stress relief is also needed to restore compressive yield strength after cold forming. Hot sizing is often combined with stress relieving, with the workpiece being held in fixtures or form dies to prevent distortion. Stress-relief treatments for CP titanium and some titanium alloys are given in Table 3. Detailed information on the heat treatment of titanium alloys is available in the article "Heat Treating of Titanium and Titanium Alloys" in *Heat Treating*, Volume 4 of the *ASM Handbook.*

Table 3 Recommended stress-relief treatments for titanium and some titanium alloys

Alloy	**Temperature**		Time
	°C	**°F**	
CP titanium (all grades)	480-595	900-1100	**15 min-4 h**
α or near-α alloys			

Ti-5Al-2.5Sn	540-650	1000-1200	**15 min-4 h**
Ti-8Al-1Mo-1V	595-705	1100-1300	**15 min-4 h**
Ti-6Al-2Sn-4Zr-2Mo	595-705	1100-1300	**15 min-4 h**
Ti-6Al-2Nb-1Ta-0.8Mo	595-650	1100-1200	**15 min-2 h**
Ti-0.3Mo-0.8Ni (ASTM grade 12)	480-595	900-1100	**15 min-4 h**
α-β alloys			
Ti-6Al-4V	480-650	900-1200	**1-4 h**
Ti-6Al-6V-2Sn (Cu + Fe)	480-650	900-1200	**1-4 h**
Ti-3Al-2.5V	540-650	1000-1200	**30 min-2 h**
Ti-6Al-2Sn-4Zr-6Mo	595-705	1100-1300	**15 min-4 h**
β or near-β alloys			
Ti-13V-11Cr-3Al	705-730	1300-1350	**5-15 min**
Ti-11.5Mo-6Zr-4.5Sn (Beta III)	720-730	1325-1350	**5-15 min**
Ti-3Al-8V-6Cr-4Zr-4Mo (Beta C)	705-760	1300-1400	**10-30 min**
Ti-10V-2Fe-3Al	675-705	1250-1300	**30 min-2 h**
Ti-15V-3Al-3Cr-3Sn	**790-815**	**1450-1500**	**5-15 min**

The only true cold-formable titanium alloy is Ti-15V-3Sn-3Cr-3Al. Hot sizing is usually not used for this alloy; however, properties must be developed with an aging treatment (8 h at 540 °C, or 1000 °F, is typical). Because of the high springback rates encountered with this alloy, more elaborate tooling must be used.

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Hot Forming

Heating titanium increases formability, reduces springback, takes advantage of a lesser variation in yield strength, and allows for maximum deformation with minimum annealing between forming operations. Severe forming must be done in hot dies, generally with preheated stock.

The greatest improvement in the ductility and uniformity of properties for most titanium alloys is at temperatures above 540 °C (1000 °F). At still higher temperatures, some alloys exhibit superplasticity (see the section "Superplastic Forming" in this article). However, contamination is also more severe at the higher temperatures. Above about 650 °C (1200 °F), forming should be done in vacuum or under a protective atmosphere, such as argon, to minimize oxidation.

As shown in Table 4, most hot-forming operations are done at temperatures above 540 °C (1000 °F). For applications in which the utmost in ductility is required, temperatures below 315 to 425 °C (600 to 800 °F) are usually avoided.

Table 4 Temperatures for the hot forming of titanium and some annealed titanium alloys

Alloy	Forming temperature	
	°C	°F
CP titanium (all grades)	480-705	**900-1300**
α and near-α alloys		
Ti-8Al-1V-1Mo	790 ± 15	**1450 ± 25**
Ti-5Al-2.5Sn	620-815	**1150-1500**
α-β alloys		
Ti-6Al-6V-2Sn	790 ± 15	**1450 ± 25**
β alloy		
Ti-13V-11Cr-3Al	**605-790**	**1125-1450**

Source: Ref 2

Temperatures generally must be kept below 815 °C (1500 °F) to avoid marked deterioration in mechanical properties. Superplastic forming, however, is performed at 870 to 925 °C (1600 to 1700 °F) for some alloys, such as Ti-6Al-4V. At these temperatures, care must be taken not to exceed the β transus temperature of Ti-6Al-4V. Heating temperature and time at temperature must be controlled so that the titanium is hot for the shortest time practical and the metal temperature is in the correct range.

Scaling and Embrittlement. Titanium is scaled and embrittled by oxygen-rich surface layers formed at temperatures higher than 540 °C (1000 °F). Generally, for heating in air, 1 h is the longest time at 705 °C (1300 °F) that should be permitted, and 20 min at 870 °C (1600 °F) should be the limit; these times are cumulative and include all time that the metal is at that temperature for all the operations on a given workpiece. The subsequent removal of scale and embrittled surface, or a protective atmosphere, should be considered for any heating above 540 °C (1000 °F). Argon gas is a commonly used atmosphere for superplastic forming.

Aging. Some hot-forming temperatures are high enough to age a titanium alloy. Heat-treatable β and α-β alloys generally must be reheat treated (solution annealed) after hot forming. Alpha-beta alloys should not be formed above the β transus temperature.

Because of aging, scaling, and embrittlement, as well as the greater cost of working at elevated temperatures, hot forming is ordinarily done at the lowest temperature that will permit the required deformation. When maximum formability is required, the forming should be done at the highest temperature practical that will retain the mechanical properties and serviceability required of the workpiece.

Tools. Titanium alloys are often formed hot in heated dies in presses that have a slow, controlled motion and that can dwell in the position needed during the press cycle. Hot forming is sometimes done in dies that include heating elements or in dies that are heated by the press platens. Press platens heated to 650 °C (1200 °F) can transmit enough heat to keep the working faces of the die at 425 to 480 °C (800 to 900 °F). Other methods of heating include electrical-resistance heating and the use of quartz lamps and portable furnaces.

Accuracy. Hot forming has the advantage of improved uniformity in yield strength, especially when the forming or sizing temperature is above 540 °C (1000 °F). However, care must be taken to limit the accumulation of dimensional errors resulting from:

- Differences in thermal expansion
- Variations in temperature
- Dimensional changes from scale formation
- Changes in dimensions of tools
- Reduction in thickness from chemical pickling operations

Reference cited in this section

2. "Fabrication Practices for Titanium and Titanium Alloys," Lockheed Corporate Process Specification LCP70-1099, Revision B, Lockheed-California Company, Oct 1983

Hot Sizing

Hot sizing is used to correct inaccuracies in shape and dimensions in cold preformed parts. Hot sizing uses the creep-forming principle to force irregularly shaped parts to assume the correct shape against a heated die by the controlled application of horizontal and vertical forces over a period of time. Buckles and wrinkles can be removed from preforms in this way. A combination of creep and compression forming is used when reducing bend radii by hot sizing.

Hot sizing is used to correct for springback in parts formed by other methods. The correction of springback depends on time and temperature; the higher the temperature, the shorter the time for processing. However, the effect of temperature on the properties of the metal limits the maximum useful temperature. The pressure applied to the part during hot sizing should be high enough to keep the part firmly against the fixture or die. Any additional pressure above the clamping requirement has no effect on the part and can cause deformation of the tooling.

Hot platen presses are commonly used for the hot sizing of titanium. The tooling is designed for the hot sizing of preforms; that is, it must only hold the workpiece to the required shape for the necessary time at temperature. Hot sizing in hot platen presses is done in the following sequence of operations:

- The preformed parts are loaded on hot form blocks that are heated by the platen in the press

- The press is closed, and it heats the parts without applying the forming force
- Force is applied by the upper platen and auxiliary side rams, and is held as long as necessary to complete the forming

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Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Superplastic Forming

The superplastic forming of titanium is currently being used to fabricate a number of sheet metal components for a range of aircraft and aerospace systems. Hundreds of parts are in production, and significant cost savings are being realized through the use of superplastic forming. Other advantages of superplastic forming over other forming processes include the following:

- Very complex part configurations are readily formed
- Lighter, more efficient structures are possible
- It is performed in a single operation, reducing fabrication labor time
- Depending on part size, more than one piece can be produced per machine cycle
- The force needed for forming is supplied by a gas, resulting in the application of equal amounts of pressure to all areas of the workpiece

The limitations of the process include:

- Heat-resistant tool materials that contain minimal amounts of nickel are required
- Equipment requirements are extensive
- Long preheat times are necessary to reach the forming temperature
- A protective atmosphere, such as argon, is required

Several processes are used in the superplastic forming of titanium alloys. Among these are blow forming, vacuum forming, thermo-forming, deep drawing, and superplastic forming/diffusion bonding (see the section "Superplastic Forming/Diffusion Bonding" in this article). All of these processes are discussed in more detail in the article "Superplastic Sheet Forming" in this Volume.

Superplastic Titanium Alloys

The workhorse superplastic titanium alloy is Ti-6Al-4V, and the state-of-the-art in titanium superplastic forming is largely based on this alloy. However, a number of titanium alloys, especially the α-β alloys, exhibit superplastic behavior. Many of these materials, such as Ti-6Al-4V, are superplastic without special processing. Table 5 illustrates the superplastic behavior of some titanium alloys and lists the characteristics used to describe superplastic properties in engineering alloys: strain rate sensitivity factor *m* and tensile elongation. The *m* value is a measure of the rate of change of flow stress with strain rate; the higher the *m* value of an alloy, the greater its superplasticity. Titanium alloys that have exhibited superplasticity but are not listed in Table 5 include Ti-3Al-2.5V (ASTM grade 9), Ti-4.5Al-1.5Cr-5Mo (Corona 5), and Ti-0.3Mo-0.8Ni (ASTM grade 12).

Table 5 Superplastic characteristics of titanium alloys

Alloy	**Test temperature**		**Strain rate, s^{-1}**	**Strain rate sensitivity factor, *m***	Elongation, %
	°C	**°F**			
CP titanium	850	1560	1.7×10^{-4}	. . .	**115**
α-β alloys					
Ti-6Al-4V	840-870	1545-1600	1.3×10^{-4} to 10^{-3}	0.75	**750-1170**
Ti-6Al-5V	850	1560	8×10^{-4}	0.70	**700-1100**
Ti-6Al-2Sn-4Zr-2Mo	900	1650	2×10^{-4}	0.67	**538**
Ti-4.5Al-5Mo-1.5Cr	870	1600	2×10^{-4}	0.63-0.81	**>510**
Ti-6Al-4V-2Ni	815	1500	2×10^{-4}	0.85	**720**
Ti-6Al-4V-2Co	815	1500	2×10^{-4}	0.53	**670**
Ti-6Al-4V-2Fe	815	1500	2×10^{-4}	0.54	**650**
Ti-5Al-2.5Sn	1000	1830	2×10^{-4}	0.49	**420**
Near-β and β alloys					
Ti-15V-3Sn-3Cr-3Al	815	1500	2×10^{-4}	0.50	**229**
Ti-13Cr-11V-3Al	800	1470	. . .	. . .	**<150**
Ti-8Mn	750	1380	. . .	0.43	**150**
Ti-15Mo	**800**	**1470**	**. . .**	**0.60**	**100**

Source: Ref 3

Metallurgical variables that affect superplastic behavior in titanium alloys include grain size, grain size distribution, grain growth kinetics, diffusivity, phase ratio in α-β alloys, and texture (Ref 3). Alloy composition is also significant and can have a pronounced effect on α-β phase ratio and on diffusivity.

Grain size is known to have a strong influence on the superplastic behavior of Ti-6Al-4V (Ref 4, 5). This is illustrated in Fig. 2, which shows flow stress and strain rate sensitivity factor *m* as a function of strain rate for Ti-6Al-4V materials with four different grain sizes. Increasing grain size increases the flow stress, reduces maximum *m* value, and reduces the strain rate at which maximum *m* is observed.

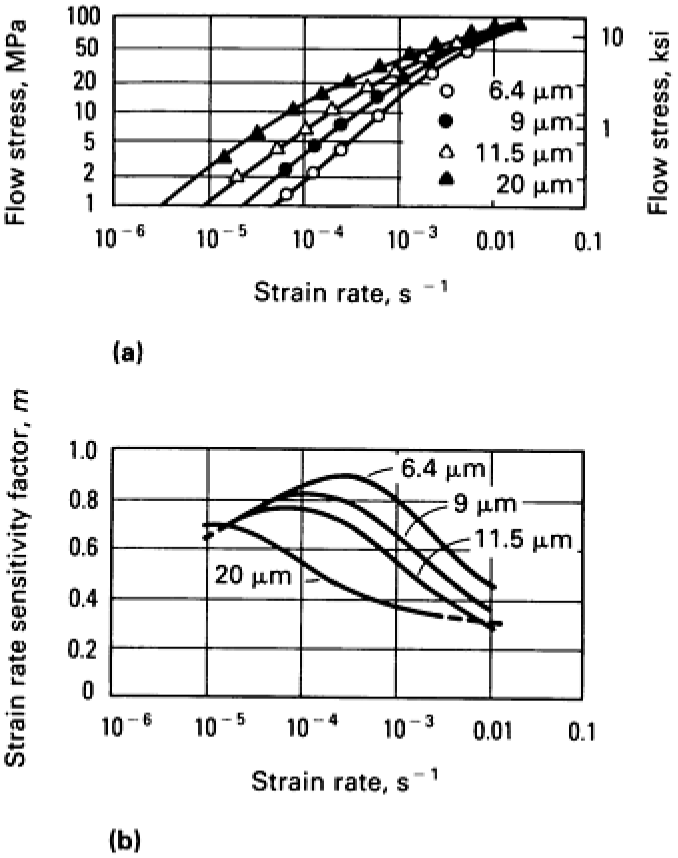

Fig. 2 Flow stress (a) and strain rate sensitivity factor *m* (b) versus strain rate for Ti-6Al-4V materials with four different grain sizes. Test temperature: 927 °C (1700 °F). Source: Ref 5.

Grain Size Distribution. Figure 3 shows flow stress versus strain rate for Ti-6Al-4V alloys with two different grain size ranges. The material with the smaller grain size distribution (lot A) exhibits significantly lower flow stresses than the material with the larger grain size distribution (lot B). Maximum *m* value is also higher for the lot A material.

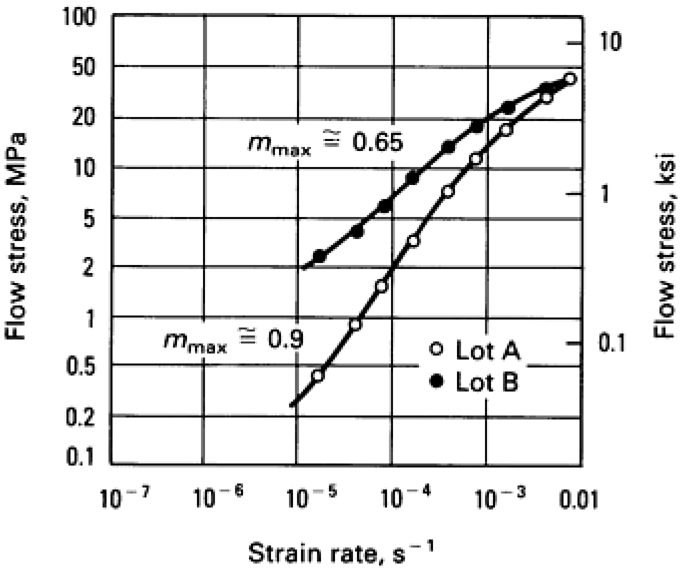

Fig. 3 Effect of grain size distribution on flow stress versus strain rate data for Ti-6Al-4V at 927 °C (1700 °F). Lot A, average grain size of 4 μm and grain size range of 1 to 10 μm; lot B, average grain size of 4.6 μm but grain size range of 1 to > 20 μm. Source: Ref 6.

Grain growth kinetics affect superplastic behavior in direct relation to the grain size developed in the material. A study of grain growth effects on Ti-6Al-4V found that the flow hardening observed during constant strain rate superplastic flow was the direct result of grain growth (Ref 5). It was also observed that grain growth accelerated with increasing strain rate. This grain growth causes an increase in flow stress and a decrease in maximum *m* value.

Diffusivity is an important quantity in the superplastic flow of titanium alloys (and other engineering materials). The best indicator of diffusivity is usually activation energy *Q*, which can be determined from the change in strain rate with temperature (Ref 3). Values of *Q* have been determined for several titanium alloys and for the α and β phases of titanium alloys. As indicated in Table 6, the activation energies determined from superplastic data are consistently higher than those for self-diffusion. It has been suggested that the higher *Q* values seen in superplastic alloys are due to the fact that the volume fraction of β phase in the alloys investigated increases with temperature, exaggerating the strain rate increase and resulting in falsely high *Q* values. This complicates efforts to establish specific deformation mechanisms.

Table 6 Activation energies for superplastic deformation and self-diffusion in titanium alloys

Alloy	**Temperature range**		**Activation energy (*Q*), kcal/mol**	Ref
	°C	**°F**		
Ti-5Al-2.5Sn	800-950	1470-1740	50-65	**4**
Ti-6Al-4V	800-950	1470-1740	45	**7**
Ti-6Al-4V	850-910	1560-1670	45-99	**8**
Ti-6Al-4V	815-927	1500-1700	45-52	**9**

Ti-6Al-2Sn-4Zr-2Mo	843-900	1550-1650	38-58	**10**
Self-diffusion, α phase	. . .	. . .	40.4	**11**
Self-diffusion, β phase	. . .	. . .	36.5	**12**
Self-diffusion, β phase	**. . .**	**. . .**	**31.3**	**13**

Phase Ratio Effects. Table 5 shows that the two-phase (α-β) titanium alloys seem to exhibit greater superplasticity than other titanium alloys. The α and β phases are quite different in terms of crystal structure (hexagonal close-packed for α, and body-centered cubic for β) and diffusion kinetics. Beta phase exhibits a diffusivity approximately two orders of magnitude greater than that of α phase. For this reason alone it should be expected that the amount of β phase present in a titanium alloy would have an effect on superplastic behavior.

Figure 4 shows elongations and *m* values for several titanium alloys as a function of the volume fraction of β phase present in the alloys. It can be readily seen that elongation values reach a peak at approximately 20 to 30 vol% β phase (Fig. 4a), while *m* values peak at β contents of about 40 to 50 vol% (Fig. 4b). Because *m* is usually considered to be a good indicator of super-plasticity, this discrepancy in the location of maxima of the curves in Fig. 4 may be surprising. It is believed that the difference stems from a grain growth effect during superplastic deformation. Beta phase is known to exhibit more rapid grain coarsening than α, and the maximum ductility may be the result of a balance between moderated grain growth (due to the presence of α phase) and enhanced diffusivity (due to the presence of β).

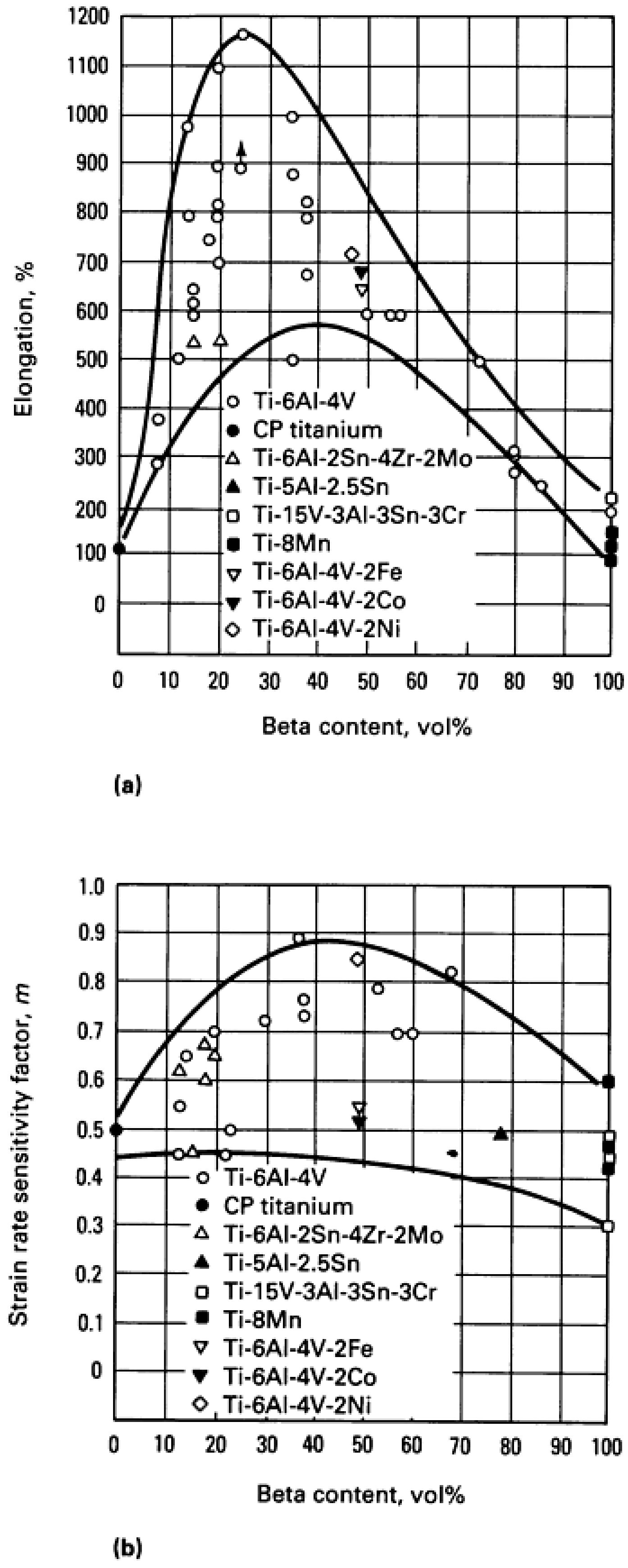

Fig. 4 Elongation (a) and *m* value (b) as a function of β-phase content for several titanium alloys. See text for details.

Crystallographic Texture. Although some studies (Ref 6, 8) did not report a noticeable effect of crystallographic texture on the superplastic behavior of titanium alloys, a more recent study (Ref 14) indicated that strongly textured Ti-6Al-5V alloy exhibited significantly higher total elongations (up to 200% difference) than the weakly textured material did. This was especially true at intermediate temperatures and in certain crystallographic directions (with the rolling direction and 45° to the rolling direction). No noticeable difference was observed normal to the rolling direction.

Superplastic Forming/Diffusion Bonding (SPF/DB)

The versatility of the superplastic forming process for titanium can be enhanced by combining it with diffusion bonding (solid-state joining). Both processes require similar conditions, that is, heat, pressure, clean surfaces, and an inert environment. The combined process is referred to as superplastic forming/diffusion bonding. Diffusion bonding is carried out simultaneously with superplastic forming, thus eliminating the need for welding or brazing for complex parts.

The SPF/DB process has greatly extended the applicability of superplastic forming. Using SPF/DB, a sheet can be formed onto pre-placed details and diffusion bonded, or two or more sheets can be formed and bonded at selected locations. Figure 5 illustrates the SPF/DB process for three-sheet parts.

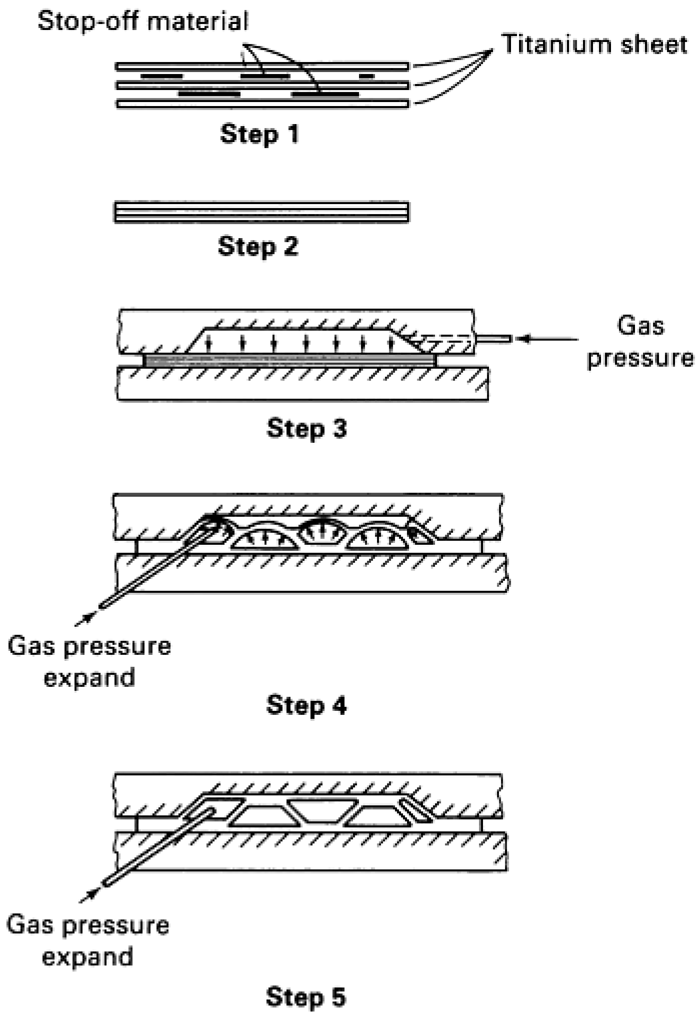

Fig. 5 Schematic showing the sequence of operations for SPF/DB of three-sheet titanium parts.

Diffusion bonding can be applied only to selected areas of a part by using a stop-off material (Fig. 5) that is placed between the sheets at locations where no bonding is desired. Suitable stop-off materials depend on the alloy being bonded and the temperatures employed; yttria and boron nitride have been successfully used.

Applications

Superplastic forming and SPF/DB are rapidly gaining acceptance in the aircraft/aerospace industry. Figure 6 shows the increase in applications for superplastically formed titanium parts in four military aircraft since 1980; applications for commercial aircraft and in the aerospace industry also are increasing.

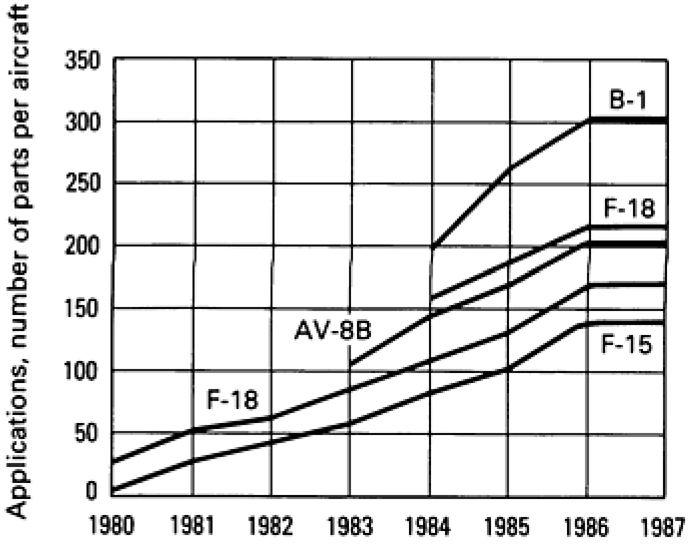

Fig. 6 Applications of superplastically formed titanium parts in military aircraft. Source: Ref 15.

Applications range from simple clips and brackets to airframe components and other load-bearing structures. Figures 7 and 8 show current applications for superplastically formed parts and illustrate the cost and weight savings that can be realized by using superplastic forming.

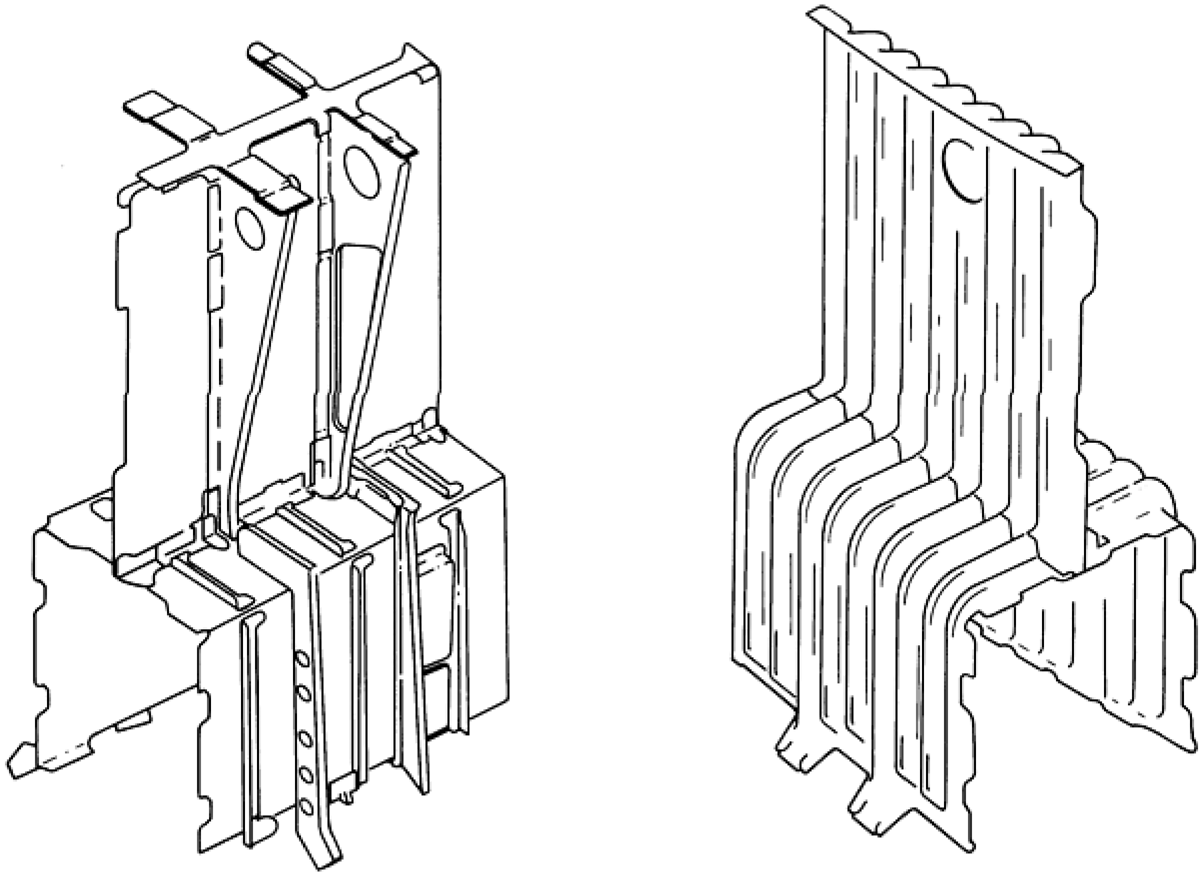

Fig. 7 Original keel design (left) and superplastically formed titanium keel section (right) for F-15 fighter aircraft. The change to the SPF part resulted in a 58% cost savings and a 31% weight savings. Source: Ref 15

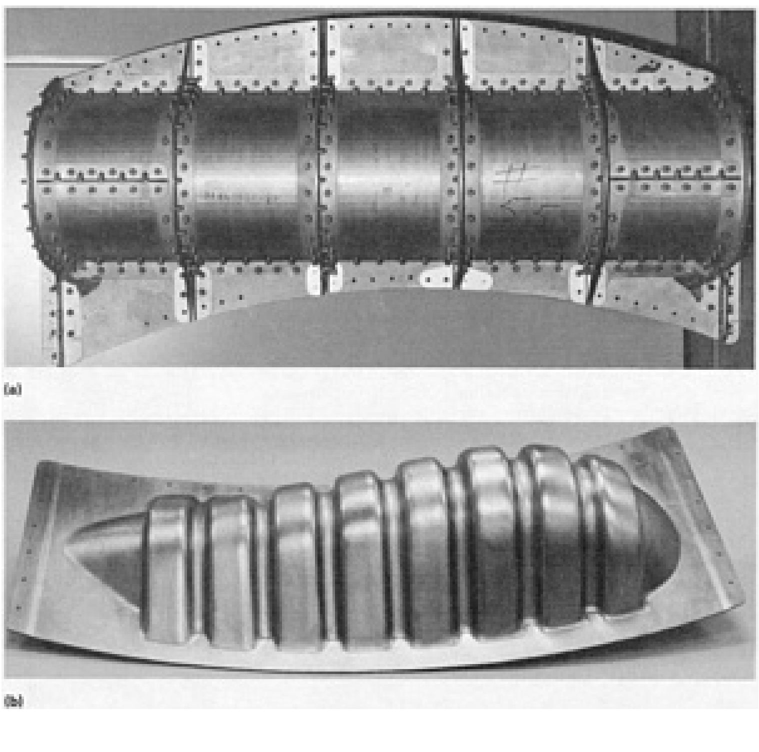

Fig. 8 Ti-6Al-4V engine nacelle component for the Boeing 757 aircraft. (a) Part as previously fabricated required 41 detail parts and more than 200 fasteners. (b) Superplastically formed part is formed from a single sheet. Courtesy of Metal Bellows Division of Parker Bertea Aerospace Group.

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Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Press-Brake Forming

Titanium alloys cold formed in a press brake behave like work-hardened stainless steel, except that springback is considerably greater (see the article "Forming of Stainless Steel" in this Volume). If bend radii are large enough, forming can be done cold. However, if bend radii are small enough to cause cracking in cold forming, either hot forming or the process of cold forming followed by hot sizing must be used.

The setup and tooling for press-brake air bending are relatively simple because the ram stroke determines the bend angle. The only tooling adjustments are the span width of the die and the radius of the punch. The span width of the die affects the formability of bend specimens and is determined by the punch radius and the work metal thickness, as shown in Fig. 9. Acceptable conditions for dies in press-brake forming are shown as the shaded area between the upper and lower limits in Fig. 9.

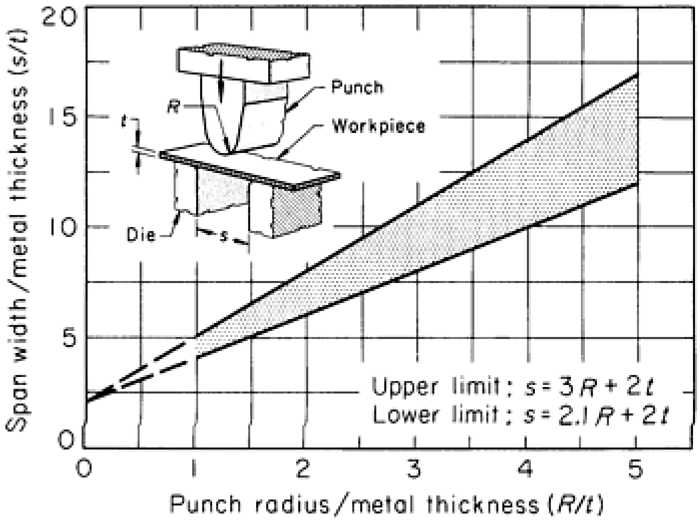

Fig. 9 Optimal relationships among span width of die, punch radius, and work metal thickness in the press-brake forming of titanium alloys. Shaded area indicates acceptable forming limits.

The minimum bend radius obtainable in press-brake forming depends on the alloy, work metal thickness, and forming temperature (Table 7). Springback in press-brake forming depends on the ratio of punch radius (bend radius) to stock thickness and on forming temperature, as shown in Fig. 10 for alloy Ti-6Al-4V (Fig. 10 is not to be used for minimum bend radii).

Table 7 Minimum bend radii obtainable in the cold press-brake bending of annealed or solution-treated titanium alloys

Alloy	Minimum bend radius as a function of sheet thickness, *t*	
	***t*< 1.75 mm (0.069 in.)**	1.75 mm (0.069 in.) < *t* < 4.76 mm (0.1875 in.)
CP titanium		
ASTM grade 1	2.5	**3.0**
ASTM grade 2	2.0	**2.5**
ASTM grade 3	2.0	**2.5**
ASTM grade 4	1.5	**2.0**
α alloys		
Ti-5Al-2.5Sn	4.0	**4.5**

Ti-5Al-2.5Sn ELI	4.0	**4.5**
Ti-6Al-2Nb-1Ta-0.8Mo	. . .	**. . .**
Ti-8Al-1Mo-1V	4.5[a]	**5.0[b]**
α-β alloys		
Ti-6Al-4V	4.5	**5.0**
Ti-6Al-4V ELI	4.5	**5.0**
Ti-6Al-6V-2Sn	4.0	**4.5**
Ti-6Al-2Sn-4Zr-2Mo	4.5	**5.0**
Ti-3Al-2.5V	2.5	**3.0**
Ti-8Mn	6.0	**7.0**
β alloys		
Ti-13V-11Cr-3Al	3.0	**3.5**
Ti-11.5Mo-6Zr-4.5Sn	3.0	**3.0**
Ti-3Al-8V-6Cr-4Mo-4Zr	3.5	**4.0**
Ti-8Mo-8V-2Fe-3Al	3.5	**3.5**
Ti-15V-3Cr-3Sn-3Al[c]	**2.0**	**2.0**

Source: Ref 17.

(a) 4.0 in transverse direction.

(b) 4.5 in transverse direction.

(c) Source: Ref 16

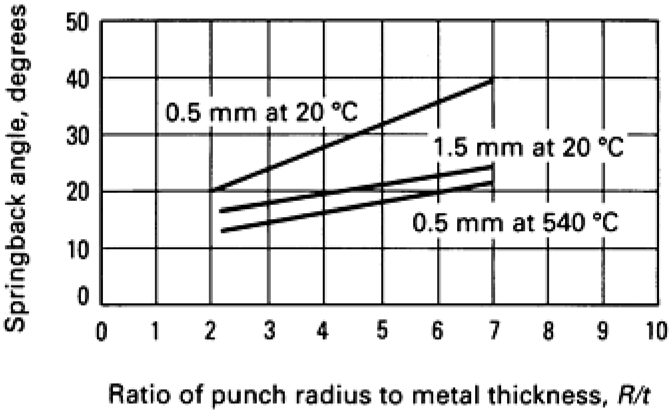

Fig. 10 Effect of ratio of punch radius to work metal thickness on springback in the press-brake bending of Ti-6Al-4V at two temperatures.

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Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Deep Drawing

The deep drawing of titanium alloys has been largely replaced by the superplastic forming process. However, general guidelines for the deep drawing of titanium alloy dome shapes at room temperature are:

- The edges of the blank should be smooth to prevent cracking during forming
- The flange radius should be at least 9.5 to 12.7 mm ($\frac{3}{8}$ to $\frac{1}{2}$ in.)
- The workpiece should be clean before each forming operation
- An overlay can be used to prevent wrinkles
- Severe forming and localized deformation should be avoided; forming pressure should be applied slowly
- The punch should be polished to prevent galling, regardless of lubrication

The deep drawing of dome and hemisphere shapes has also been accomplished at room temperature in a rubber-diaphragm press. A detailed description of rubber-diaphragm forming is available in the article "Rubber-Pad Forming" in this Volume. Deep drawing is discussed in more detail in the article "Deep Drawing" in this Volume.

Hot Drawing. Titanium can be drawn deeper when hot, and more difficult forming can be done, than at room temperature. Generally, depth of draw depends on composition, workpiece shape, required radii, forming temperature, die design, die material, and lubricant. Temperature to 675 °C (1250 °F) have been used.

Power (Shear) Spinning

Most titanium alloys are difficult to form by power spinning. Alloys Ti-6Al-4V and Ti-13V-11Cr-3Al and some grades of CP titanium are the most responsive to forming by this method.

Tooling. Most tools for the power spinning of titanium are made of high-speed steel and hardened to 60 HRC. Mandrels are heated for hot spinning, although the work metal can also be heated by torches. Tube preforms can be heated by radiation. The hot power spinning of titanium is done at 205 to 980 °C (400 to 1800 °F), depending on the alloy and the operation.

Lubricants for the power spinning of titanium depend on the forming temperature used. At temperatures up to 205 °C (400 °F), heavy drawing oils, graphite-containing greases, and colloidal graphite are used. Colloidal graphite and molybdenum disulfide are employed at temperatures to 425 °C (800 °F); above this temperature, colloidal graphite, powdered mica, and boron nitride are used. More information on power spinning is available in the article "Spinning" in this Volume.

Rubber-Pad Forming

The cold forming of titanium in a press with tooling that includes a rubber pad is used mostly for flanging thin stock and for forming beads and shallow recesses. The capacity of the press controls the range in size, strength, and thickness of blanks that can be formed. Within this range, however, additional limits will be set by buckling and splitting.

Auxiliary devices, such as overlays, wiper rings, and sandwiches, are usually needed in rubber-pad forming to improve the forming and to reduce the amount of wrinkling and buckling. Rubber-pad forming is generally done at room temperature or with only moderate heat. Forming is almost always followed by hot sizing to remove springback, to sharpen radii, to smooth out wrinkles and buckles, and to complete the forming. Hand work is sometimes needed to complete the forming. The cold-formed workpiece should be stress relieved or hot sized within 24 h after forming.

Sharp bends can be made at higher forming pressures. Figure 11 shows the effect of pad pressure on bend radius for two titanium alloys.

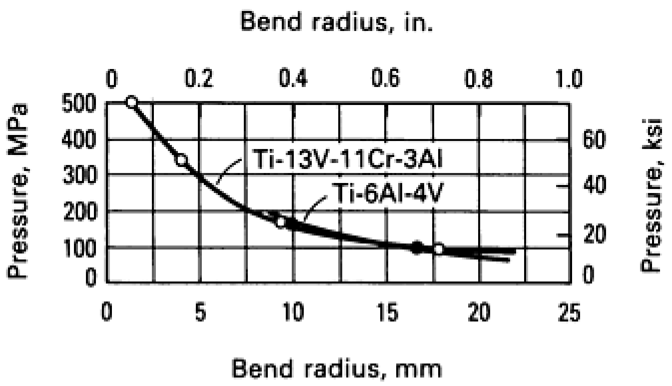

Fig. 11 Effect of pad pressure on radii formed in 1.60 mm (0.063 in.) thick titanium alloy sheets at room temperature.

Springback behavior of titanium and its alloys in rubber-pad forming differs somewhat from that observed in other methods of forming. In general, springback in forming titanium varies directly with the ratio of bend radius to work metal thickness, and inversely with forming temperature. Springback is also inversely proportional to forming pressure.

Beads can be formed to a limited extent in titanium alloy sheet by rubber-pad forming. However, beads are readily formed by superplastic forming, and this process is preferred. Additional information on rubber-pad forming is available in the article "Rubber-Pad Forming" in this Volume.

Stretch Forming

Tooling that is generally used for the stretch forming of stainless steel is also suitable for the cold stretch forming of titanium, when used with a high clamping force that will prevent slipping and tearing. Titanium may exhibit irregular incremental stretch under tension loads; therefore, optimal results are obtained when titanium is stretch formed at slow strain rates. The rate of wrapping around a die should be about 205 mm/min (8 in./min).

In the stretch forming of angles, channels, and hat-shaped sections, deformation occurs mainly by bending at the fulcrum point of the die surface; compression buckling is avoided by applying enough tensile load to produce about 1% elongation in the inner fibers. The outer fibers elongate more, depending on the curvature of the die and on the shape of the workpiece. It is sometimes preferable or required (especially if sufficient forming power is not available) to stretch wrap at elevated temperature. Again, the wrapping speed must be slow to prevent local overheating or necking.

Formability limits can be extended by permitting small compression buckles to occur at the inner fibers and removing them later by hot sizing. The buckled region represents a condition of overforming and should be limited to the amount that can be effectively removed by hot sizing.

Compression buckling is not a problem when sheet is stretch formed to produce single or compound curves. The ductility of sheet varies with orientation and is generally better in the direction of rolling. In the stretch forming of compound curves, the stretching force should be applied in the direction of the smaller radius. The rate of wrapping around the die should be about 205 mm/min (8 in./min).

Stretch forming is being replaced in many applications by superplastic forming. Additional information on the stretch forming process is available in the article "Stretch Forming" in this Volume.

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Three-Roll Forming

Three-roll forming is an economical method of forming titanium alloy sheet into aircraft skins, cylinders, or parts of cylinders. The sheet should be flat within 0.15 mm (0.006 in.) for each 51 mm (2 in.) of length. The corners of the sheet should be chamfered to prevent marking of the rolls.

The upper roll of the three-roll assembly can be adjusted vertically. The radius of the bend is controlled by the roll adjustment. Premature failure will occur if the contour radius is decreased too rapidly; however, too many passes through the rolls may cause excessive work hardening of the work metal. Several trial parts must sometimes be made in a new material or shape to establish suitable operating conditions.

Three-roll forming is also used to form curves in channels that have flanges of 38 mm (1.5 in.) or less. Figure 12 shows the use of the process for curving a channel with the heel in. Transverse buckling and wrinkling are common failures in the forming of channels. The article "Three-Roll Forming" in this Volume contains more information on this process.

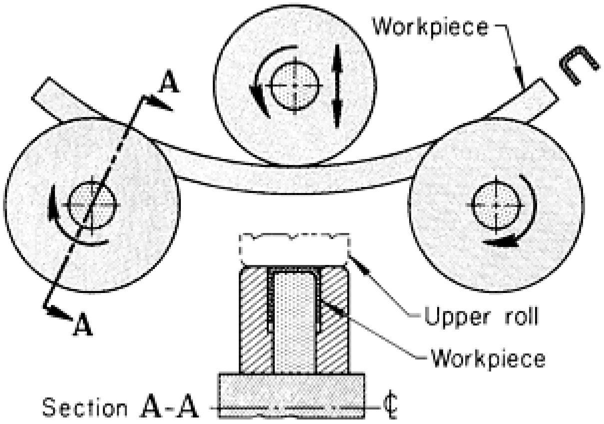

Fig. 12 Use of three-roll forming to produce a curve in a U-section channel.

Contour Roll Forming

Titanium sheet can be contour roll formed like any other sheet metal, but with special consideration for allowable bend radius and for the greater springback that is characteristic of titanium. Springback is affected to some extent by roll pressure. Often, hot rolling must be done on heated work metal with heated rolls. Additional information is available in the article "Contour Roll Forming" in this Volume.

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Creep Forming

In creep forming, heat and pressure are combined to cause the slow forming of titanium sheet into various shapes, such as double-curve panels, channel sections, Z-sections, large rings, and small joggles. The metal flows plastically at a stress below its yield strength. At low temperature, creep rates are ordinarily very low (for example, 0.1% elongation in 1000 h), but the creep rate of titanium accelerates sharply with increasing temperature.

Creep forming can be done by three different methods:

- A blank is clamped at the edges, as for stretch forming, and a heated male tool is loaded to press against

the unsupported portion of the blank; the metal yields under the combination of heat and pressure and slowly creeps to fit the tool
- A set of dies containing heating elements or coils is used in a hydraulic press in a manner similar to hot sizing
- A heated female die is used with a vacuum diaphragm, as in vacuum forming (see the section "Vacuum Forming" in this article)

Temperatures for creep forming are the same as those used in hot forming (Table 4). Generally, titanium must be held at the creep-forming temperature for 3 to 20 min per operation; creep forming sometimes takes as long as 2 h.

Vacuum Forming

Large panels (some as much as 18 m, or 60 ft, long) for aircraft are sometimes vacuum formed from titanium alloy sheet. Vacuum forming, however, has been largely replaced by superplastic forming. For vacuum forming, the blank is laid on a die of heated concrete, ceramic, or metal, and a somewhat larger flexible diaphragm is laid on top of the blank to provide a seal around its edges. After the blank has been heated to forming temperature, the air is pumped out from between the blank and the die so that atmospheric pressure is used to form the work. This method, a kind of creep forming, cannot bend the work to sharp radii.

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Drop Hammer Forming

Titanium should not be permitted to rub against lead, zinc, or other low-melting metals that contaminate titanium. Drop hammer tools can be capped with sheet steel, stainless steel, or nickel alloy, depending on the expected tool life. Nickel-base alloys, in thicknesses of 0.635 to 0.813 mm (0.025 to 0.032 in.), have the longest life.

As shown in Table 4, severe forming of most titanium alloys, which includes drop hammer forming, is done at about 500 to 800 °C (900 to 1500 °F). Thermal expansion of the dies must be considered in the design. The approximate rate of expansion for steel dies is 0.006 mm/mm (0.006 in./in.) as temperature is increased from 20 to 540 °C (70 to 1000 °F).

Multistage tools can be used if the part shape is complex and cannot be formed in one blow. Workpieces are then finished by hot sizing.

The minimum thickness of titanium sheet for drop hammer forming is 0.635 mm (0.025 in.); thicker sheet is used for complex shapes. Total tolerance on parts formed in drop hammers is usually 1.6 mm ($\frac{1}{16}$ in.). More information on the drop hammer forming process is available in the article "Drop Hammer Forming" in this Volume.

Joggling

Joggling is frequently done on titanium alloy sheet. A joggle is an offset in a flat plane, consisting of two parallel bends in opposite directions at the same angle (Fig. 13). Generally, the joggle angle is less than 45°.

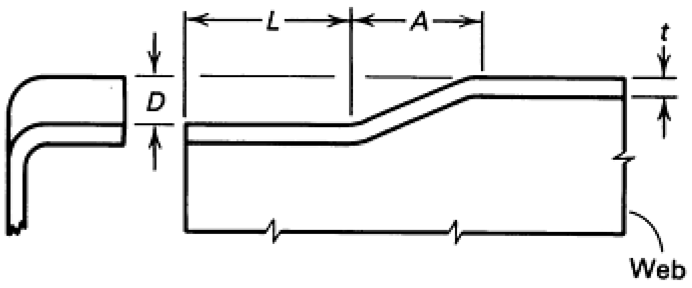

Fig. 13 Details of a joggle. See Table 8 for room-temperature joggle limits of several titanium alloys. *t*, sheet thickness, *D*, joggle height; *L*, joggle length; *A*, joggle allowance. Source: Ref 2.

Depending on joggle depth, joggles can be either formed completely at room temperature or at elevated temperature in press brakes and mechanical or hydraulic presses. Table 8 lists room-temperature joggle limits for several titanium alloys. Common practice is to preform at room temperature and then hot size ("set" the joggle) in a heated die. The sizing operation is usually done under conditions that result in stress relieving or aging.

Table 8 Room-temperature joggle limits for several annealed titanium alloys

See Fig. 13 for definitions of joggle dimensions given here, and Table 7 for minimum bend radii.

Alloy	Sheet thickness, *t*		***A*, minimum**	***D*, maximum**	*L*, minimum
	mm	**in.**			
CP titanium[(a)]	Up to 4.75	Up to 0.187	6*D*	3*t*	**5*D***
CP titanium[(b)]	Up to 4.75	Up to 0.187	4*D*	4*t*	**5*D***
Ti-8Al-1Mo-1V	Up to 2.29	Up to 0.090	8*D*	2.5*t*	**6*D***
Ti-6Al-4V	Up to 2.29	Up to 0.090	8*D*	2.5*t*	**6*D***
Ti-6Al-6V-2Sn	Up to 2.29	Up to 0.090	8*D*	2*t*	**6*D***
Ti-5Al-2.5Sn	Up to 3.18	Up to 0.125	6*D*	3*t*	**6*D***

Ti-13V-11Cr-3Al	Up to 4.75	Up to 0.187	6*D*	3*t*	**6*D***
Ti-15V-3Cr-3Sn-3Al	**Up to 2.29**	**Up to 0.090**	**4*D***	**4*t***	**5*D***

Source: Ref 2

(a) Minimum yield strength: 483 MPa (70 ksi).

(b) Minimum yield strength: <483 MPa (70 ksi).

In press-brake formed or stretch-formed angles and channels, and in machined extrusions, joggles with radii smaller than the minimum bend radii for the metal at room temperature, or joggles with length-to-depth ratios of less than about 6 to 1, arc more successfully formed at elevated temperature. Forming temperature varies between 315 and 650 °C (600 and 1200 °F), depending on the alloy and its heat-treated condition. Annealed alloys are joggled at 315 to 425 °C (600 to 800 °F). Heat-treated or partly heat-treated alloys are joggled at, or near, their aging temperature.

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Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys[*]

Dimpling

Dimpling produces a small conical flange around a hole in sheet metal parts that are to be assembled with flush or flathead fasteners. Dimpling is most commonly applied to sheets that are too thin for countersinking. Sheets are always dimpled in the condition in which they are to be used because subsequent heat treatment may cause distortion of the holes or dimensional changes in the sheet.

The hot ram-coin dimpling process is generally used, although dimples have been produced at room temperature by swaging. In hot ram-coin dimpling, force in excess of that required for forming is applied to coin the dimpled area and to reduce the amount of springback.

Titanium is dimpled at up to 650 °C (1200 °F) with tool steel dies. If higher temperatures are required, heat-resistant alloy or ceramic tooling is needed in order to prevent deformation of the dies during dimpling. The work metal is usually heated by conduction from the dimpling tools, which are automated to complete the dimpling stroke at a predetermined temperature.

Pilot holes must be drilled, rather than punched, and must be smooth, round, cylindrical, and free of burrs. Because of the notch sensitivity of titanium, care must be taken in deburring the holes.

The amount of stretch required to form a dimple varies with the head and body diameters of the fastener and the bend angle. If the metal is not ductile enough to withstand forming to the required shape, cracks will occur radially in the edge of the stretch flange, or circumferentially at the bend radius. Circumferential cracks are more common in thin sheet; radial cracks are more common in thick stock.

Explosive Forming

Within the limits set by its mechanical properties, titanium can be explosive formed like other metals. Explosive forming is most commonly used for cladding titanium to other metals. Titanium is explosive formed using techniques similar to those used for other metals and alloys (see the article "Explosive Forming" in this Volume).

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

Bending of Tubing

Round tubing of commercially pure titanium and alloy Ti-3Al-2.5V can be formed at room temperature in ordinary draw bending machines. When hot bending is required, the equipment is modified by adding some means of heating the tools. Minimum and preferred conditions for bending tubing of commercially pure titanium at room temperature and at elevated temperatures are given in Table 9. As shown in Table 9, tubing up to 63.5 mm (2.5 in.) in diameter ordinarily is bent at room temperature, while larger sizes are bent at temperatures of 175 to 205 °C (350 to 400 °F). In either case, bend radius is limited chiefly by tubing diameter, but maximum bend angle is affected by both diameter and wall thickness.

Table 9 Limits on radii and angles in banding of CP titanium

Tube OD		**Wall thickness**		**Minimum bend radius**		Bending conditions			
						Maximum angle[(a)]	**Preferred minimum bend radius**		Preferred maximum angle[(a)]
mm	**in.**	**mm**	**in.**	**mm**	**in.**		**mm**	**in.**	
Room-temperature bending									
38.1	1.5	0.41	0.016	57.2	2.25	90°	75	3	**120°**
		0.51	0.020	57.2	2.25	100°	75	3	**160°**
50.8	2.0	0.41	0.016	76.2	3.00	80°	100	4	**110°**
		0.51	0.020	76.2	3.00	100°	100	4	**150°**
63.5	2.5	0.41	0.016	95.3	3.75	70°	127	5	**100°**

		0.89	0.035	95.3	3.75	110°	127	5	**180°**
Elevated-temperature bending (175 to 205 °C, or 350 to 400 °F)									
76.2	3.0	0.41	0.016	114.3	4.50	90°	150	6	**120°**
		0.89	0.035	114.3	4.50	130°	150	6	**180°**
88.9	3.5	0.41	0.016	133.4	5.25	90°	178	7	**120°**
		0.89	0.035	133.4	5.25	130°	178	7	**180°**
101.6	4.0	0.41	0.016	152.4	6.00	110°	203	8	**160°**
		0.89	0.035	152.4	6.00	120°	203	8	**180°**
114.3	4.5	0.41	0.016	171.5	6.75	130°	229	9	**140°**
		0.89	0.035	171.5	6.75	140°	229	9	**140°**
127.0	5.0	0.51	0.020	254.0	10.00	. . .	254	10	**110°**
152.4	**6.0**	**0.51**	**0.020**	**304.8**	**12.00**	**. . .**	**305**	**12**	**100°**

(a) Maximum bend angles are based on the use of a clamp section three times as long as the diameter of the tubing and on maximum mandrel-ball support of the tubing.

Commercially pure titanium deforms locally if tension is not applied evenly. Bending should be slow; rates of $\frac{1}{4}$° to 4° per minute are suitable. A lubricant should be used.

Tools used in bending titanium and titanium alloy tubing are shown in Fig. 14. In this type of apparatus, the tubing is gripped between the clamp and the straight portion of the rotating form block tightly enough to prevent axial slipping during bending. The clamped end of the tubing is supported by a plug. The cleat insert in the clamp and that attached to the end of the plug (see Fig. 14) are used only in bending the larger sizes of tubing that have thin walls, for which greater gripping power is needed.

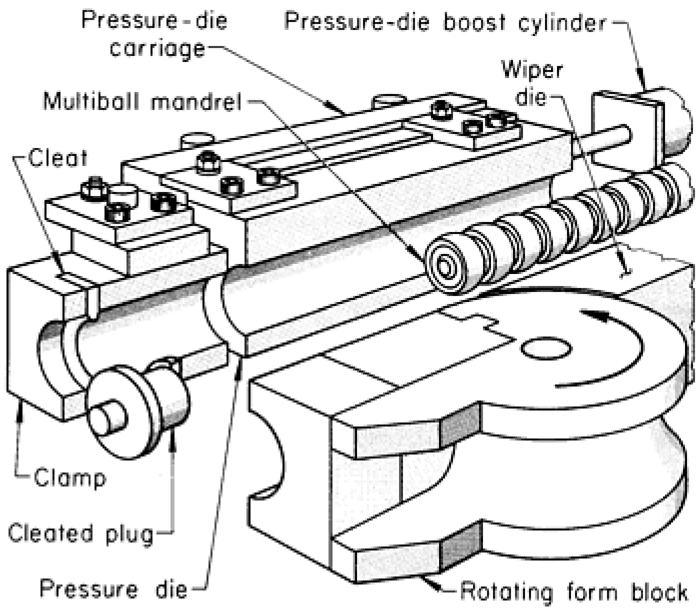

Fig. 14 Tools used for bending titanium tubing. The cleats on the clamp and plug are used only for bending of large-diameter tubing with thin walls. For hot bending, the pressure die and mandrel are integrally heated.

Computers are also being applied to titanium tube bending, especially at large aircraft and aerospace companies. Computer measurement systems are used during bending, and software packages are available that can design bend geometries. Completely automated precision bending can be performed using computers and numerically controlled (NC) bending equipment. More information on automated tube bending is available in the article "Bending and Forming of Tubing" in this Volume.

Lubrication. Drawing oils are used as lubricants for forming commercially pure titanium tubing at room temperature. Grease with graphite is used as a lubricant for the hot bending of commercially pure titanium tubing, but is not recommended for temperatures above 315 °C (600 °F). Phosphate conversion coatings are sometimes used for hot bending of titanium tubing.

Forming of Titanium and Titanium Alloys

Revised by the ASM Committee on Forming of Titanium Alloys*

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Introduction

FOUR of the platinum metals--platinum, palladium, rhodium, and iridium--have face-centered cubic crystal structures. This crystal structure is usually associated with ductility. However, only platinum and palladium can be cold worked from the cast condition. Rhodium must be broken down at a high temperature before it can be cold worked, and iridium can be cold worked, with difficulty, only after a fibrous structure has been imparted by careful hot working.

Ruthenium and osmium have hexagonal close-packed crystal structures. Osmium is completely unworkable and ruthenium very nearly so.

In general, the only problems that are peculiar to the working of the platinum metals are those resulting from surface contamination derived from rolls, swaging dies, and other tools. Base metal impurities such as iron, which may be smeared on the surface or picked up as slivers or fine dust during hot working or annealing, will alloy with the surface layers and diffuse inward. Therefore, physical characteristics such as electrical resistivity are affected and surface cracking may develop.

The platinum metals do not scale during hot working. Nevertheless, cracks usually are not easily welded or healed, probably because of the slight, inevitable contamination by iron, iron oxide, or even films of adsorbed gas. In the following sections, working procedures for each of the platinum group metals are considered separately.

Working of Platinum Group Metals

Platinum

Hot Working. Platinum ingots are normally broken down by hot forging or rolling. Ingots are heated to 1205 to 1510 °C (2200 to 2750 °F), usually in a gas-fired furnace, supported on high-grade alumina.

In forging, particular care is taken to keep the anvil surfaces smooth and bright. After the first few blows, the forging is cooled, and any surface cracks or folds are carefully gouged out with a chisel. The work is then reheated, and forging is continued to the finished size. The freedom of platinum from scaling is not without disadvantages; surface imperfections do not oxidize and flake away, but persist.

Platinum is hot rolled to sheet in simple slab rolls. Rod is hot rolled between grooved rolls, which may be provided with half-round sections throughout or, more frequently, with half-round sections for the finishing passes only. The early passes are formed with gothic sections alternating with oval sections.

Cold Working. Platinum responds readily to cold working and can be reduced 98% or more by rolling or wiredrawing. The rate of work hardening is slow, as shown in Fig. 1 and in Table 1.

Table 1 Influence of cold work on the hardness of platinum, palladium, and the more important platinum alloys, with recommended annealing temperatures

Values for hardness and annealing temperature will vary, because of differences in working procedures and degree of purity of the alloy.

Reduction of area, %	Metal/alloy											
	Pt	**Pd**	**10Rh-90Pt**	**20Rh-80Pt**	**40Rh-60Pt**	**10Ir-90Pt**	**ZGS[(a)] Pt**	**ZGS 10Rh-90Pt**	**ZGS 5Au-95Pt**	**20Ir-80Pt**	**25Ir-75Pt**	10Ru-90Pt
Brinell hardness												
0	53	48	110	128	130	116	61	114	110	192	220	**190**
10	70	80	145	176	236	136	89	160	140	226	270	**242**
20	80	88	165	190	264	154	108	184	159	242	286	**265**
30	86	96	178	200	284	168	115	196	171	252	298	**280**
40	93	100	185	212	292	176	124	208	176	259	308	**286**
50	99	106	190	222	308	180	134	218	180	264	316	**295**
60	103	110	195	234	320	182	141	229	186	272	324	**310**
70	112	120	200	244	334	185	147	245	196	284	332	**325**
80	122	135	220	260	356	195	153	271	219	300	339	**335**
Recommended annealing temperature, °C (°F)	**1000 (1830)**	**850 (1560)**	**1100 (2010)**	**1100 (2010)**	**1250 (2280)**	**1100 (2010)**	**1000 (1830)**	**1200 (2190)**	**1200 (2190)**	**1100 (2010)**	**1200 (2190)**	**1100 (2010)**

(a) ZGS, zirconia grain stabilized

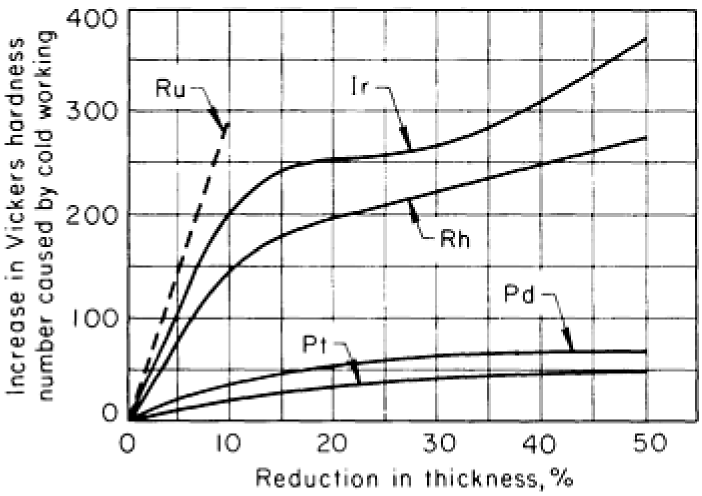


Fig. 1 Effect of cold work in increasing the hardness of the platinum group metals

In cold rolling, the response of platinum is similar to that of copper, and similar rolling programs are followed. It is seldom desirable to interpose annealing stages.

Foil thinner than about 0.038 mm (0.0015 in.) is sometimes made in small amounts by book rolling. A sheet of copper about 1.6 mm ($\frac{1}{16}$ in.) thick is folded back on itself, a platinum sheet about 0.25 mm (0.01 in.) thick is slipped into the fold, and the "book" is then rolled down as far as required, with the copper and platinum being reduced together. The finish of book-rolled strip (when separated) is poor, but the method requires no special equipment. For good surfaces, direct rolling in a Sendzimirtype mill is preferred.

In wiredrawing, platinum is handled in almost exactly the same manner as copper. Solid lubricants are used for drawing to about 2.4 mm ($\frac{3}{32}$ in.); for smaller diameters, water-base lubricants of the soluble-oil type are suitable. Diamond dies, with profiles similar to those used for drawing copper wire, are used below about 3.2 mm ($\frac{1}{8}$ in.), and platinum wires are drawn direct to sizes down to 0.01 mm (0.0004 in.) in diameter.

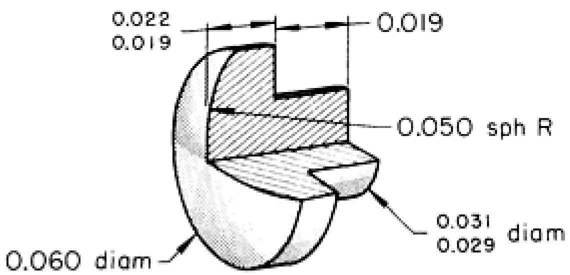
Wires smaller in diameter than 0.01 mm (0.0004 in.) can be made by the Wollaston process. In this process, a platinum rod is sheathed with a thick-wall closely fitting cylinder of silver about ten times its diameter. The composite is drawn without annealing. In the finished wire, the average diameter of the platinum core is nearly in the same ratio to that of the outer silver coating as in the original assembly, although the platinum core is not quite round or uniform. Wollaston wire is usually supplied without removing the silver coating, which is dissolved in dilute nitric acid after the wire has been mounted for use.

The usual press operations of blanking, piercing, bending, and deep drawing are used to form platinum, palladium, and their commercial alloys through the use of techniques and tools that are similar in all respects to those for cartridge brass or silver. Production methods have not been used to form the other platinum group metals. These other metals are more difficult to fabricate, and the demand for press-formed parts is small.

When steel tools are used, tool life may be shortened by cold welding between the platinum metal and the working surface of the tool. In blanking and cutting operations, the cold welding impairs tool edges and necessitates frequent regrinding. In cold-heading operations, it may cause pickup on the hammer heads, which must be removed by grinding, as shown in the following example.

Example 1: Life of Steel Hammers for Cold Heading Silver and Platinum.

The electrical contact illustrated in Fig. 2 was formed from both platinum and silver wires in the same cold-heading machine, using identical heading tools made from a conventional die steel containing 2% C and 12% Cr and hardened to 62 to 63 HRC. The platinum was lubricated with a smear of kerosene; the silver was run dry. About 15,000 parts could be headed from silver between tool regrinds, and only 5000 could be headed from platinum, because the steel tools picked up platinum, which had to be removed by grinding. Total tool life was 150,000 parts from platinum and 1 million from silver.



	Number of parts formed	
	Platinum	Silver
Between tool repolishings	5,000	**15,000**
During total tool life	**150,000**	**1,000,000**

Fig. 2 Electrical contact that was produced from platinum wire and silver wire by cold heading using steel punches. Dimensions given in inches

Pickling. Platinum is often pickled before annealing to remove surface contaminants that might otherwise alloy with and diffuse into the metal. A hot 10% solution of sulfuric acid in water is usually used. A 10% solution of hydrochloric acid is occasionally preferred if iron contamination alone is suspected. More rarely, hot aqua regia solution is used, particularly if slight surface alloying is suspected.

Annealing does not require a protective atmosphere and is done at about 1000 °C (1830 °F). The platinum should be supported on clean refractories. Silica refractories can be used if fully oxidizing conditions are maintained in the heating chamber, but alumina is preferred. Flash annealing techniques are often used for platinum wire and sheet to minimize grain growth.

Palladium

Hot Working. Palladium can be hot forged or rolled at 1200 to 1400 °C (2190 to 2550 °F). It deforms less readily than platinum but appreciably more readily than low-carbon steel. It should be quenched in water from above 815 °C (1500 °F) in order to retain a bright surface. A tarnish film of PdO forms on palladium in air between 400 to 850 °C (750 to 1560 °F), and this film is therefore found on metal that has cooled slowly through this temperature range. The oxide decomposes above 850 °C (1560 °F); metal can be cleaned from tarnish by heating above 850 °C (1560 °F) and quenching.

Palladium is readily cold worked. Small ingots are frequently reduced to the finished size by cold working, although larger ingots are usually broken down by hot working.

Cold Working. In cold rolling and wire-drawing, palladium behaves much like platinum, although the rate of work hardening of palladium is slightly higher, as shown in Fig. 1 and Table 1.

Annealing. Palladium can be annealed in air at about 850 °C (1560 °F), but to retain a bright surface, it must be quenched in water, as described above. Palladium is more often annealed in a protective atmosphere.

Hydrogen can be used as a protective atmosphere. Although palladium absorbs very large quantities of hydrogen at room temperature, with a notable increase in volume, the solubility at 850 °C (1560 °F) is low and remains low if the metal is quenched from the annealing temperature. A 95:5 mixture of nitrogen and hydrogen is, however, equally effective in preventing tarnish and is usually preferred. Vacuum annealing of palladium is done to a limited extent, and flash annealing techniques are used to minimize grain growth.

Pickling. Tarnish films of PdO on palladium that has been slowly cooled or slack quenched can be removed by a hot 10% solution of sulfuric acid or by a 10% tartaric acid solution. Hot dilute sulfuric acid dissolves palladium slowly and can therefore be used to remove contaminated surface layers.

Rhodium

Hot Working. Ingots cast from melted rhodium, as well as sintered powder metallurgy compacts, can be worked by hot forging at high temperature. Above 1300 °C (2370 °F), the metal is soft and malleable. The small ingots are usually heated to about 1500 °C (2730 °F), either in an electric resistance furnace in an atmosphere of hydrogen, in a gas-fired furnace, or even in a blowpipe; the ingots are frequently reheated during the early stages of reduction. A tarnish film of RhO_2 begins to form on rhodium when it is heated above about 500 °C (930 °F), and this film persists to much higher temperatures than the film on palladium. The metal must be quenched from above about 1400 °C (2550 °F) to be entirely bright and free from oxide. After preliminary forging, rhodium can be hot rolled to sheet or hot swaged to rod.

Cold Working. Once the cast structure of polycrystalline rhodium has been broken down by hot working, it becomes amenable to further reduction by cold working. However, the rate of hardening by cold work is high, as shown in Fig. 1.

In both cold rolling and wiredrawing, it is necessary to anneal frequently. For the first few reductions, annealing should follow each reduction in area of 10 to 20%; as the workpiece size decreases, the reduction in area between anneals can be gradually increased to 30 to 40%. Single crystals of rhodium, made by vertical floating-zone melting, can be readily cold swaged, rolled, or drawn, and they respond as readily as pure nickel. However, if this cold-worked rhodium is then recrystallized by full annealing, intercrystalline brittleness develops, and further cold working becomes as difficult as with normal polycrystalline rhodium. Partial softening, with reasonable ductility, can be achieved by stress-relief annealing at 595 to 815 °C (1100 to 1500 °F) and is sometimes helpful.

Annealing. Rhodium is usually annealed in hydrogen at about 1205 °C (2200 °F) and cooled to below about 200 °C (390 °F) in hydrogen in order to retain its bright surface. Rhodium wire is preferably continuously annealed in a tube furnace that has a cooling extension and is fed with hydrogen. Rhodium must always be bright annealed; no satisfactory method of chemical or electrochemical pickling is available.

Working of Platinum Group Metals

Iridium

Hot Working. Iridium, either as argonarc cast ingots or as powder metallurgy compacts, can be hot forged, like rhodium, but with greater difficulty. The tarnish film of IrO_2 forms at about 400 °C (750 °F) and persists in the temperature region of 400 to 1120 °C (750 to 2050 °F). Above 1120 °C (2050 °F), it dissociates. The surface remains bright when the metal is quenched from temperatures above 1120 °C (2050 °F).

Ingots that have been broken down by forging can be hot rolled by small reductions to sheet or can be hot swaged to rod if precautions are taken to keep the material hot until it enters the swaging die. After hot forging, iridium, like tungsten, can be hot drawn through heated dies; the wire is heated to about 700 °C (1290 °F) just before it enters each die. After working has started, iridium is embrittled if it is heated above its recrystallization temperature of 1350 °C (2460 °F).

Cold Working. Iridium that has been broken down by hot working to a fibrous structure and not recrystallized will withstand only a very small amount of cold work, such as that imparted by a planishing pass. Single crystals of iridium (made by vertical-zone melting), like single crystals of rhodium, can be cold worked to reductions of about 50%. They cannot then be softened by annealing, because intercrystalline brittleness develops upon recrystallization.

Ruthenium

Hot Working. Small ingots of ruthenium can be deformed by small amounts by careful hot working at about 1500 °C (2730 °F), but dense fumes of the oxide are evolved and cracks invariably develop. Ruthenium powder compact and cast ingots have been successfully hot rolled at 1205 °C (2200 °F) after enclosure in an envelope of stainless steel. The resulting sheet has little ductility.

Platinum Alloys

The alloys of platinum with up to about 40% Rh, 30% Ir, or 10% Ru constitute those of chief industrial use. All are worked by the same general methods used for platinum; allowance is made for the greater stiffness and hardness of the alloys.

Hot Working. The alloys can be forged, hot rolled, and hot swaged, usually at temperatures higher than those for platinum. The platinum-ruthenium alloys give off fumes of ruthenium oxide above about 1095 °C (2000 °F) in air and are preferably heated in a protective atmosphere.

Cold Working. All of the alloys respond to cold working by rolling, swaging, and wiredrawing. The effects of cold work on the hardness of some typical alloys are given in Table 1.

Annealing. The alloys can be annealed in air, but all need to be quenched to prevent tarnishing by the oxide film of the alloying metal. Wires are usually continuously annealed and cooled in a hydrogen atmosphere. Annealing temperatures for the alloys are shown in Table 1.

Grain-stabilized platinum and platinum alloys--zirconia grain-stabilized (ZGS) platinum, ZGS Pt-10Rh, and ZGS Pt-5Au--have been developed for the glass industry for applications requiring prolonged component life. Temperatures for these alloys are about 200 °C (360 °F) greater than those for the corresponding nonstabilized alloys. Data on hardness and recommended annealing temperatures are included in Table 1.

Superplastic Sheet Forming

C.H. Hamilton, Washington State University; A.K. Ghosh, Rockwell International

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Introduction

SUPERPLASTICITY is a term used to indicate the exceptional ductility that certain metals can exhibit when deformed under proper conditions. The term is most often related to the ductile tensile behavior of the material; however, superplastic deformation has the characteristic of easy deformation under low pressures, and compression deformation characteristics are also described as superplastic. The tensile ductility of superplastic metals typically ranges from 200 to 1000% elongation, but ductilities in excess of 5000% have been reported (Ref 1). Elongations of this magnitude are one to two orders greater than those observed for conventional metals and alloys, and they are more characteristic of plastics than metals.

Because the capabilities and limitations of sheet metal fabrication are most often determined by the tensile ductility limits, it is clear that there are significant advantages potentially available for forming such materials, provided the high-ductility characteristics observed in the tensile test can be used in production forming processes. This is of course being done, and the number of applications of parts formed by these methods is increasing each year. This article will discuss many of the processes and related considerations involved in the forming of superplastic sheet metal parts.

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Superplastic Sheet Forming

C.H. Hamilton, Washington State University; A.K. Ghosh, Rockwell International

Requirements for Superplasticity

Before discussing the details of the superplastic forming (SPF) processes, it is necessary to review the more important aspects of superplastic material behavior because some of the specific forming parameters are determined by this behavior. There are several different types of superplasticity in terms of the microstructural mechanisms and deformation conditions, including the following (Ref 2, 3):

- Micrograin superplasticity
- Transformation superplasticity
- Internal stress superplasticity

At this time, only the micrograin superplasticity is of importance in the fabrication of parts, and the discussion will be limited to this type. For micrograin superplasticity, the high ductilities are observed only under certain conditions, and the basic requirements for this type of superplasticity are:

- Very fine grain size material (of the order of 10 μm, or 400 μin.)
- Relatively high temperature (greater than about one-half the absolute melting point)
- A controlled strain rate, usually 0.0001 to 0.01 s^{-1}

Because of these requirements, only a limited number of commercial alloys are superplastic, and these materials are formed using methods and conditions that are different from those used for conventional metals.

Characteristics of Superplastic Metals. For a superplastic metal that is tensile tested under proper conditions of temperature, the observed ductility is seen to vary substantially with strain rate, as shown in Fig. 1 for a zinc-aluminum eutectoid alloy (Ref 4). As shown, there is a maximum in ductility at a specific strain rate, with significant losses in ductility as the strain rate is increased or decreased relative to this maximum. It is well known that the primary factor related to this behavior is the rate of change of flow stress with strain rate, usually measured and reported as *m,* the strain rate sensitivity exponent:

$$m = \frac{\partial \ln \sigma}{\partial \ln \dot{\epsilon}} \quad \textbf{(Eq 1)}$$

where σ is the flow stress and $\dot{\epsilon}$ is the strain rate.

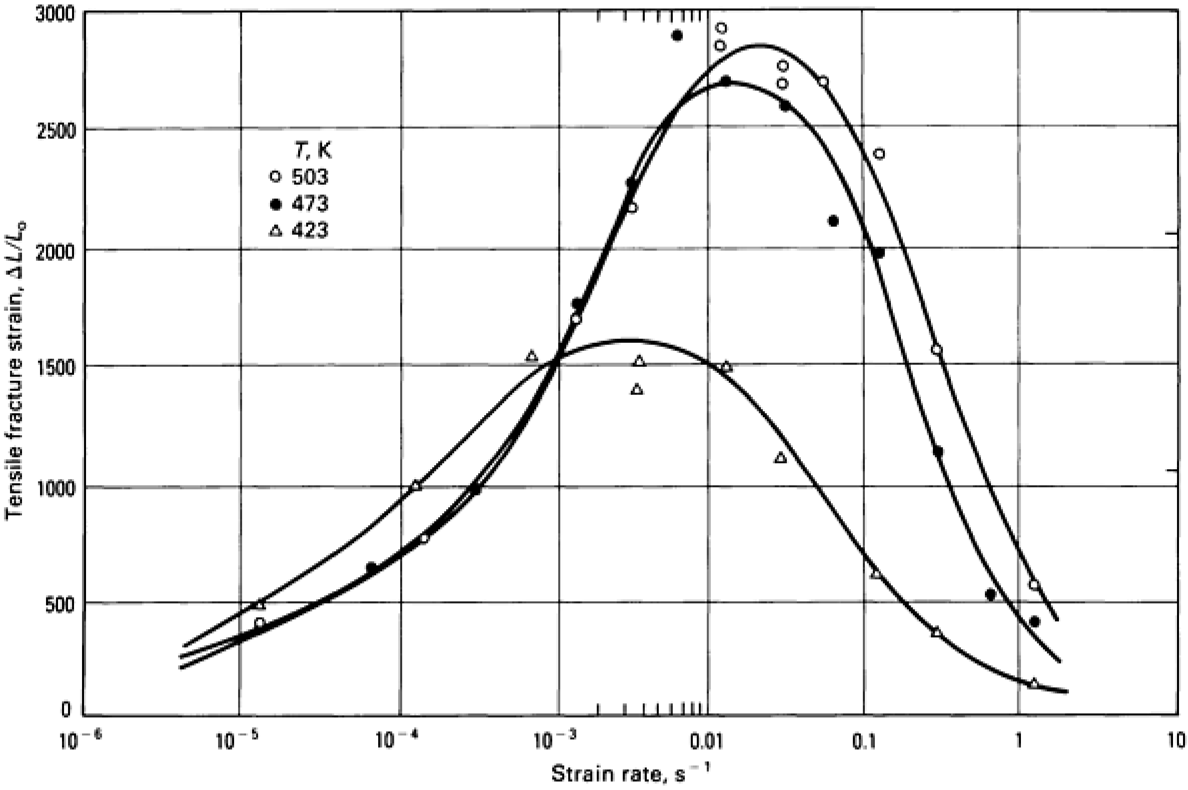

Fig. 1 Tensile fracture strain versus initial strain rate for a Zn-22Al alloy having a grain size of 2.5 μm (100 μin.) tested at temperatures ranging from 423 to 503 K.

The above characteristics of a superplastic alloy indicate that unusual forming capability should be possible with superplastic alloys but that control of the forming process parameters is important to obtain the full potential of this class of material. Such process controls are more demanding than corresponding requirements for conventional forming processes, and the superplastic forming of sheet metals is a new technology that is different from the conventional processes. However, superplastic forming offers advantages over other fabrication methods for a number of applications as a result of its unique capability of fabricating complicated components in a single step.

Superplastic Alloys. Because of the stable grain size requirement for a superplastic metal, not all commercially available alloys are superplastic. In fact, very few such alloys are superplastic. Many materials have been produced with laboratory or pilot-plant processing, but very few of these have been produced commercially (Ref 3). Nonetheless, there are some alloys that can be obtained (or are expected to be available in the future). As SPF technology develops, it is anticipated that additional alloys will be produced specifically for this process.

A summary of several superplastic alloys is presented in a Table 1, along with some of their characteristics. Particularly noteworthy are Ti-6Al-4V, aluminum alloy 7475, and the Supral alloys, which are quite superplastic and are commercially available. The titanium alloys have been found to be superplastic as conventionally produced, and there has not been a need to develop alloy modifications nor special mill-processing methods to make them superplastic. However, this has not been the case with the aluminum alloys, and either special processing (Ref 5) or alloy development (Ref 6) has been necessary to produce superplastic materials. The Zn-22Al alloy has been the focus of substantial research because it can be readily processed into the superplastic condition; this alloy has also been made available commercially by several different suppliers.

Table 1 Superplastic properties of several aluminum and titanium alloys

Alloy	**Test temperature**		**Strain rate, s^{-1}**	**Strain rate sensitivity, *m***	Elongation, %
	°C	**°F**			
Aluminum					
Statically recrystallized					
Al-33Cu	400-500	752-930	8×10^{-4}	0.8	**400-1000**
Al-4.5Zn-4.5Ca	550	1020	8×10^{-3}	0.5	**600**
Al-6 to 10Zn-1.5Mg-0.2Zr	550	1020	10^{-3}	0.9	**1500**
Al-5.6Zn-2Mg-1.5Cu-0.2Cr	516	961	2×10^{-4}	0.8-0.9	**800-1200**
Dynamically recrystallized					
Al-6Cu-0.5Zr (Supral 100)	450	840	10^{-3}	0.3	**1000**
Al-6Cu-0.35Mg-0.14Si (Supral 220)	450	840	10^{-3}	0.3	**900**
Al-4Cu-3Li-0.5Zr	450	840	5×10^{-3}	0.5	**900**
Al-3Cu-2Li-1Mg-0.2Zr	500	930	1.3×10^{-3}	0.4	**878**
Titanium					
α/β					
Ti-6Al-4V	840-870	1545-1600	1.3×10^{-4} to 10^{-3}	0.75	**750-1170**
Ti-6Al-5V	850	1560	8×10^{-4}	0.70	**700-1100**
Ti-6Al-2Sn-4Zr-2Mo	900	1650	2×10^{-4}	0.67	**538**
Ti-4.5Al-5Mo-1.5Cr	871	1600	2×10^{-4}	0.63-0.81	**>510**
Ti-6Al-4V-2Ni	815	1499	2×10^{-4}	0.85	**720**

Ti-6Al-4V-2Co	815	1499	2×10^{-4}	0.53	**670**
Ti-6Al-4V-2Fe	815	1499	2×10^{-4}	0.54	**650**
Ti-5Al-2.5Sn	1000	1830	2×10^{-4}	0.49	**420**
β and near β					
Ti-15V-3Cr-3Sn-3Al	815	1499	2×10^{-4}	0.5	**229**
Ti-13Cr-11V-3Al	800	1470	. . .	. . .	**<150**
Ti-8Mn	750	1380	. . .	0.43	**150**
Ti-15Mo	800	1470	. . .	0.60	**100**
α					
CP Ti	**850**	**1560**	**1.7×10^{-4}**	**. . .**	**115**

Additional information on superplastic alloys is available in the articles "Forming of Aluminum Alloys," "Forming of Titanium and Titanium Alloys," and "Isothermal and Hot-Die Forging" in this Volume. Superplastic ferrous alloys are discussed in the Appendix to this article.

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Superplastic Sheet Forming

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Characterization of Superplastic Alloys

The characteristic flow properties of a superplastic metal are exemplified in Fig. 2 for a Ti-6Al-4V alloy tested at 927 °C (1701 °F). The very strong dependence of *m* on strain rate at various grain sizes (Fig. 3) is typical of superplastic metals, and there is a good relationship between the *m* value and superplastic ductility. This relationship is demonstrated most clearly in Ref 7 in a graph of data for a large number of alloys in which the *m* value is graphed as a function of elongation. Although the total elongation can also be affected by fracture, the strain rate sensitivity is a first-order effect. The influence of *m* on the ductility is understood through mechanics to be due to the stabilizing effect of the strain rate sensitivity of flow stress on the diffuse necking process (Ref 8, 9, 10). The strong effect temperature has on superplastic deformation is demonstrated most clearly in Ref 11, in which the ductility of a titanium alloy is graphed as a function of temperature. The elongation of the titanium alloy rises and falls rapidly over a relatively short temperature range, and outside the limits of this temperature span, the ductility is quite modest and within the range of conventional material behavior.

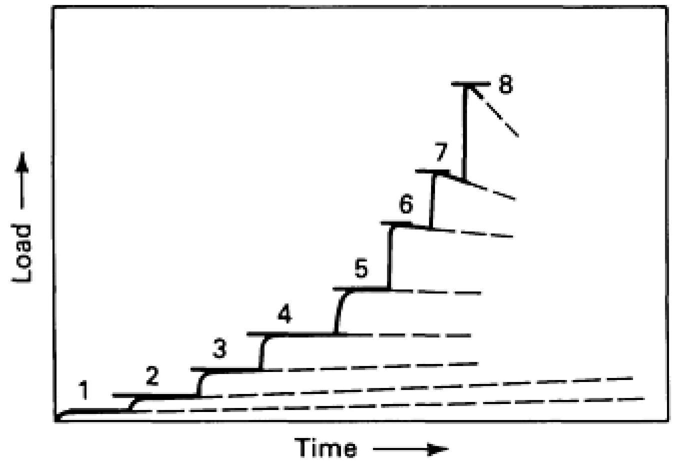


Fig. 2 Schematic plot of load versus time for the tensile deformation of a superplastic Ti-6Al-4V alloy at 927 °C (1701 °F) and at progressively increasing crosshead velocities (step strain rate test)

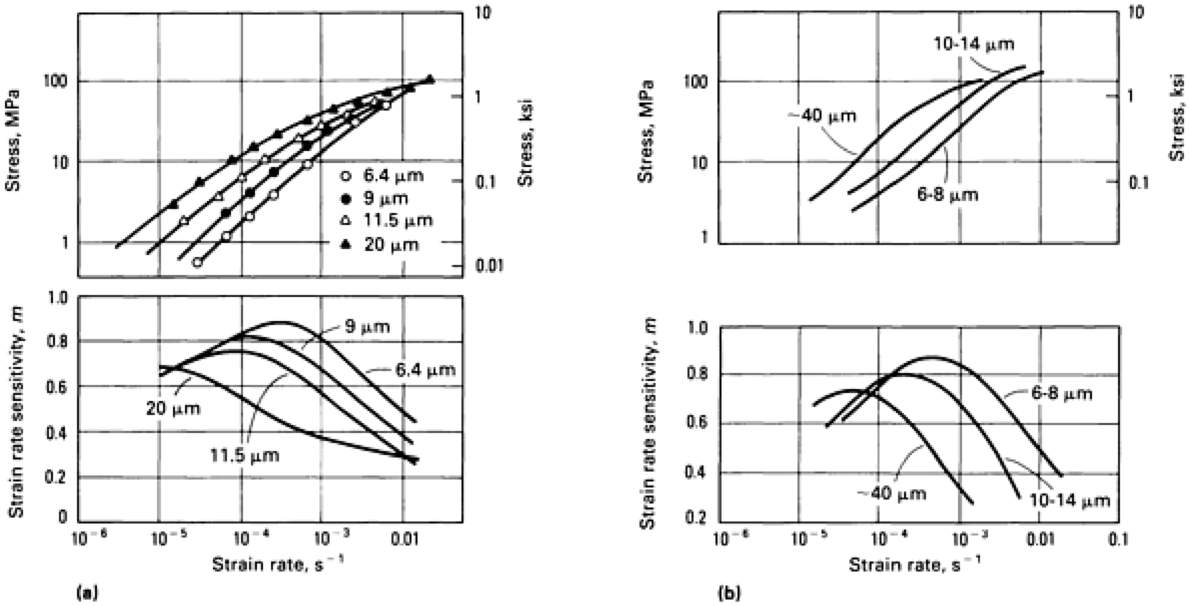


Fig. 3 Stress-strain rate plots and the corresponding strain rate sensitivity plots from step strain rate tests for Ti-6Al-4V at 927 °C (1701 °F) (a) and 7475 aluminum alloy at 516 °C (961 °F) (b), which are both at superplastic temperature

The characterization of superplastic behavior includes the characterization of plastic flow, internal cavitation, and fracture behavior. The processing variables needed for an overall characterization of superplastic behavior are introduced in this section. The parameter that is commonly selected as a measure of superplastic formability is the tensile elongation at the optimum superplastic temperature and strain rate. Because this is a highly strain rate sensitive property and because real components can experience significant variations in strain rate during forming, tensile elongation is measured as a function of strain rate. Although this is somewhat time consuming, an alternative is to determine the strain rate sensitivity of the flow stress, *m*, which has been shown to correlate well with tensile elongation for different classes of materials (Ref 7, 8). Measurements of flow stress and strain rate sensitivity of flow stress can be conducted in a single test and can be used to determine the optimum strain rate for superplastic forming (where *m* is a maximum). Although strain rate sensitivity is the dominant parameter in superplastic forming, more recent results show that a significant amount of hardening can occur as a function of superplastic strain even at a constant strain rate (Ref 9, 10). This type of strain hardening is believed to be related primarily to the grain growth that occurs during superplastic forming. At higher strain rates, strain hardening is associated with dislocation cell formation in the classical manner.

Forming temperature is just as important a variable in superplastic forming as the strain rate. Temperature variation in a forming die is a primary source of localized thinning. Characterization of material behavior should therefore include not only determination of the optimum superplastic temperature but also the sensitivity of flow stress and elongation to temperature. A large temperature sensitivity of flow stress is not desirable, because local hot spots will lead to severe strain localization. When strain localization and necking are the dominant modes of failure, it has been shown that tensile elongation is related to the *m* value in a predictable manner (Ref 11). However, when fracture intervenes, the *m* value does not provide sufficient quantitative characterization, although within the same alloy system it still provides a qualitative comparison. Fracture is therefore an important consideration in most superplastic materials of engineering application. An exception is materials with anomalously high diffusivity (for example, Ti-6Al-4V alloys), which do not show any evidence of internal cavitation or fracture. Superplastic materials that exhibit cavitation at inclusions, triple points, and second-phase particles generally fail by the interlinking of growing cavities.

Stress-strain rate behavior is usually characterized by a step strain rate test, in which strain rate is increased in successive steps and an attempt is made to measure the corresponding steady (or saturated) flow stress. A constant flow stress indicates a negative loading rate, which occurs at a point somewhat beyond the load maximum. However, even if the load maximum is used as a criterion for calculating these stresses, it can be shown that the error is negligible. Various arguments have been put forward for the proper selection of flow stress from transient loading response (Ref 12, 13, 14). However, because of the changing plastic-strain rate during this test, selection of data at the elastic limit from the rapidly rising portion of the load curve is thought to be inappropriate.

Figure 2 shows a schematic load versus time plot during a step strain rate test of a typical superplastic alloy. The interesting features are:

- At the low crosshead speeds, the load does not reach a maximum but continues to show a gradual rise
- At some intermediate speed, load reaches a constant plateau
- At higher speeds, it peaks and begins to show a sharp drop

The load increase at the low strain rates, in spite of a decrease in applied strain rate, indicates hardening of the material with imposed strain. A part of this hardening is due to a rise in plastic-strain rate, which occurs gradually when crosshead speed is low. However, the extent of hardening observed is considerable and does not saturate even after significant plastic strain, which suggests other possible sources of hardening. This type of hardening has been observed in other superplastic materials, such as aluminum-copper eutectic, and is generally attributed to grain growth occurring during deformation (Ref 15, 16).

The criticism against the use of strain rate jump tests and the selection of maximum load points for obtaining stress values is that the strain automatically becomes a variable along the σ-$\dot{\epsilon}$ curve. This could be avoided if load relaxation test results were used to derive σ-$\dot{\epsilon}$ curves. However, more complex transient effects might be associated with load relaxation tests, and the results might not be meaningful for a forming application in which strain rate generally increases with accompanying strain. The step strain rate test is therefore believed to be a logical test method for use in superplastic forming applications, provided the data are obtained with very little strain accumulation.

The σ-$\dot{\epsilon}$ data for Ti-6Al-4V alloy and 7475 aluminum alloy deformed in the superplastic temperature range are presented in Fig. 3 for a variety of grain sizes. The total accumulated strain is generally less than 0.25 in these tests. At lower strain rates ($<5 \times 10^{-4}$ s^{-1}), strain hardening does not permit the establishment of a load maximum. To characterize flow stress free from grain-growth hardening effects, stresses must be selected soon after the elastic portion. If the load rises slowly, the plastic-strain rate, $\dot{\epsilon}_p$, can be obtained from:

$$\dot{\epsilon}_p = \dot{\epsilon}_t - \frac{1}{E}\left[\frac{\dot{P}}{A} + \frac{P}{A}\dot{\epsilon}_t\right] \quad \textbf{(Eq 2)}$$

where $\dot{\epsilon}_t$ is the applied strain rate, E is Young's modulus, P is the load, $\dot{P}$ is the loading rate, and A is the instantaneous area.

The second term within the brackets in Eq 2 can generally be neglected, and even the first term can be neglected for most metals where $\dot{P}/A$ < 0.015 MPa s^{-1} (0.0022 ksi s^{-1}). Therefore, for most purposes, $\dot{\epsilon}_p = \dot{\epsilon}_t$ is a reasonable assumption as long as the loading rate is low (it need not be zero). The stress versus strain rate data plotted on the basis of such small strain accumulation serve as the initial behavior of the superplastic material. Deformation produces changes in this behavior, however, and a stress versus strain rate plot taken after a considerable plastic strain exhibits a higher stress level compared with the initial curve (Ref 9).

The proper method for determining *m* from step strain rate test results is to obtain the slope of the curve of best fit through the log σ versus log $\dot{\epsilon}$ data. The determination of *m* from two consecutive strain rate jumps assumes a constant *m* over that strain rate range and introduces an error that is dependent on the size of the range of jump strain rates. Figure 3 shows that grain size has a strong effect on flow stress and *m* value in the superplastic range. Because of the sigmoidal shape of the stress-strain rate curves, *m* values exhibit a maximum at an intermediate strain rate, and the *m* peak shifts to higher strain rates with decreasing grain size. In the titanium and aluminum alloys, the value of the *m* peak is typically in the range of 0.7 to 0.9 and increases with decreasing grain size. The value of *m* also exhibits a maximum as a function of temperature. The effects of grain size and temperature are closely tied to the diffusional creep contribution during superplastic flow (Ref 17, 18). In the power-law creep regime (at higher strain rates), however, the stress values tend to converge as the grain size dependence decreases.

Grain Size Distribution Effects on Stress-Strain Rate. Grain size has a profound influence on the superplasticity of metals. When the grain size is fine, the flow stress is low, the value of *m* is generally high, and the tensile elongation is greater. Characterization of grain size is therefore important in the overall characterization of superplasticity. However, because polycrystalline aggregates, in general, possess a distribution of grain size, it is not very meaningful to assign a fixed grain size to metals. The nature of the distribution has also been shown to influence the stress-strain rate curve (Ref 19, 20). A few coarse grains in an otherwise fine grain structure can control the strain rate range over which *m* is high, and may in some cases cause the appearance of a threshold stress. The important effect of grain size distribution in real materials is to produce a relatively high *m* (*m* > 0.5) over almost three decades in strain rate; this is a transition region between power-law creep and diffusional creep (Fig. 4).

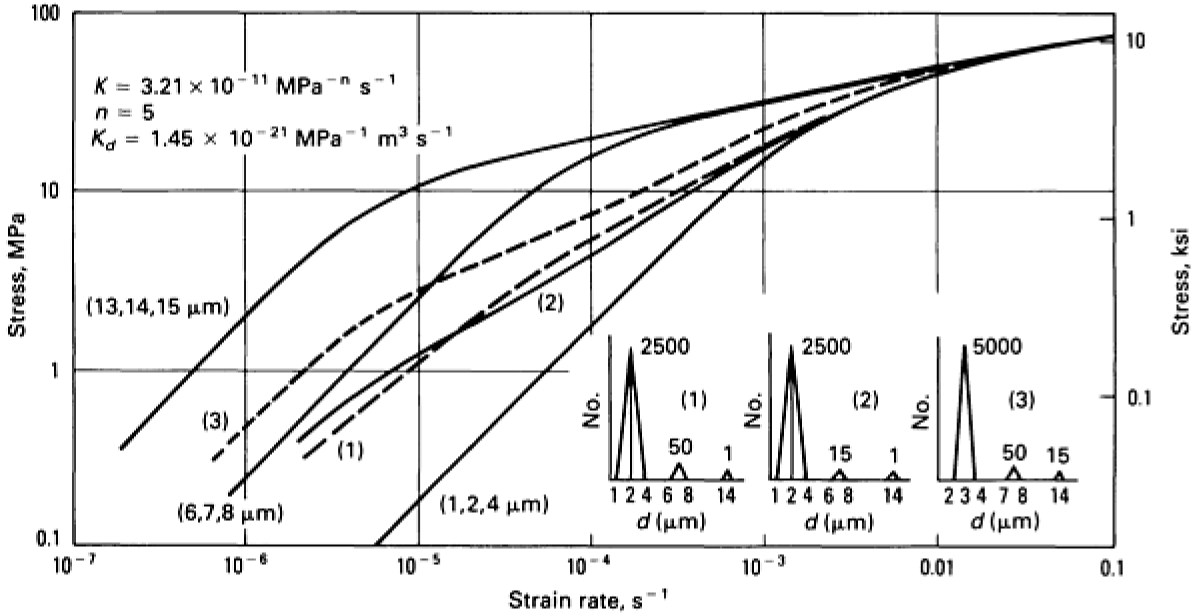


Fig. 4 Influence of different grain size distributions on the steady-state stress-strain rate curves. The solid lines

are for essentially singular grain sizes. Distribution in grain size yields a gradual transition in stress-strain rate curves.

Stress-Strain Behavior. Superplastic metals are generally regarded as ideally rate sensitive; that is, no strain hardening occurs during deformation. However, grain growth induced strain hardening has been observed, and it can be quite significant in some cases (Ref 9, 10, 15, and 16). In testing superplastic materials, constant crosshead speed leads to a decreasing strain rate within the specimen gage length, particularly at large tensile strains. An effort to maintain constant strain rate requires that:

- The specimen fillet region be reduced to a minimum to reduce its contribution to the overall extension
- The crosshead speed be programmed to increase with specimen elongation in order to maintain a constant strain rate (assuming the elongation to arise uniformly out of the gage length)

An on-line computer serves this function by altering the crosshead speed, *v*, in the following manner:

$$v = l_o \dot{\epsilon}_t \exp(\dot{\epsilon}_t t) \qquad \textbf{(Eq 3)}$$

where l_o is the initial gage length, $\dot{\epsilon}_t$ is the applied strain rate, and t is the time from the start of test. Temperature control during the test also must be accurate (within 2 °C, or 4 °F) in order to avoid any localized deformation.

Figure 5 shows the stress-strain curves for titanium and aluminum alloys obtained at various constant strain rates. The extent of hardening is quite large and appears to produce a linear stress-strain behavior. In diffusional creep, one can expect $\sigma \sim d^2$; if grain growth kinetics are such that $d \propto t^p$, where t is time, p is the exponent, and d is grain size, then $\sigma \propto t^{2p}$. When p is about 0.5, a linear hardening behavior would be expected. If diffusional creep due to grain-boundary transport is considered, $\sigma \propto d^3$, and p may be as low as 0.33, thus causing the appearance of linear hardening. In reality, the value of the hardening exponent, 2*p* or 3*p*, may be somewhat less than unity. A small component of strain rate dependent increase of flow stress also influences these experimental results because strain rate does not remain absolutely constant during the tests. The last portion of the stress-strain curves (shown dashed or with stress drop) is where nonuniformity of deformation within the gage length makes stress measurements incorrect.

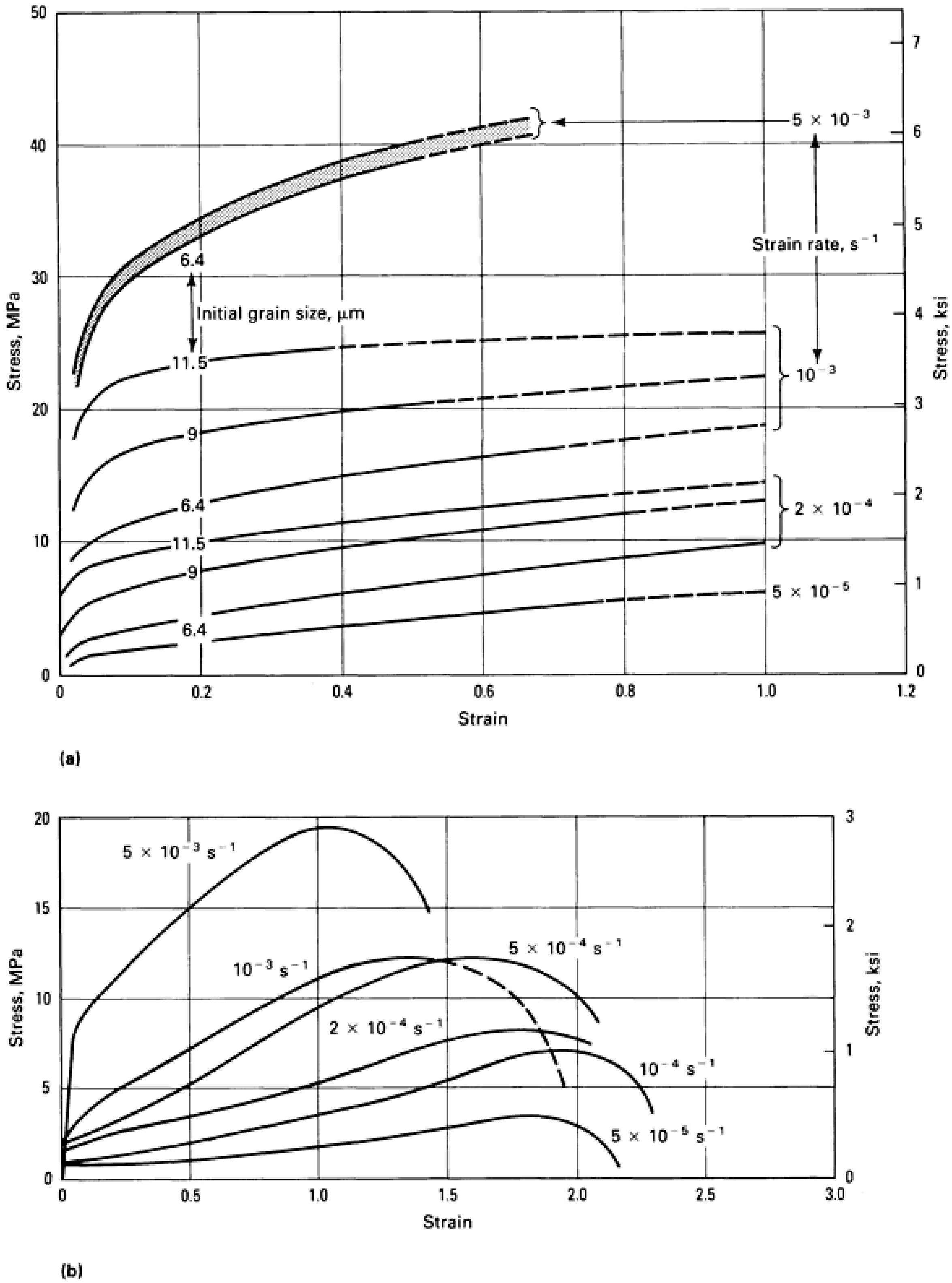
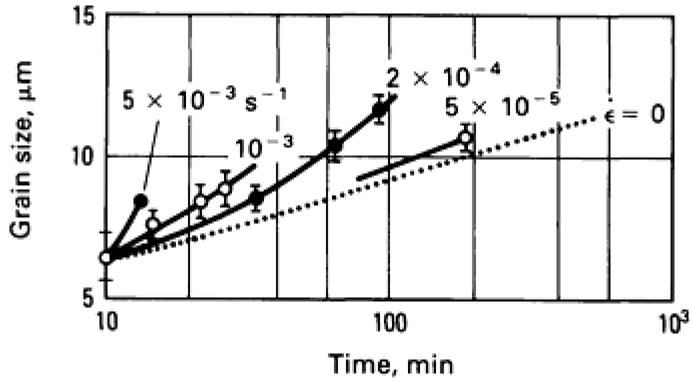
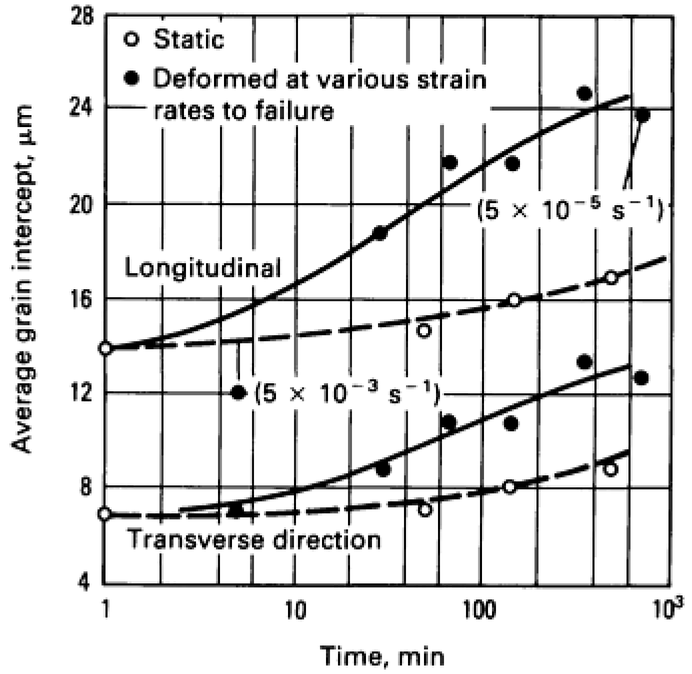


Fig. 5 Stress-strain curves at various constant strain rates for Ti-6Al-4V at 927 °C (1701 °F) (a) and 7475 aluminum alloy at 516 °C (961 °F) (b). Initial grain size: ~10 to 14 μm

The significant strain hardening observed in Fig. 5 is believed to be due to concurrent grain growth, the evidence for which is shown in Fig. 6. In the case of Ti-6Al-4V, the grain growth kinetics appear to some degree to be a function of strain rate as well. It is clear, however, that slower strain rates produce the largest grain sizes, particularly because of the long exposure times, and in all cases, dynamic grain growth is significantly greater than static growth.



(a)



(b)

Fig. 6 Grain growth kinetics at four different tensile strain rates compared with static kinetics for Ti-6Al-4V at 927 °C (1701 °F) with 6.4 μm (250 μin.) initial grain size (a) and 7475 aluminum at 515 °C (959 °F) (b)

Concurrent grain growth effects can also influence the stress-strain rate data measured at the low strain rates. This can be understood from the consideration of a constitutive equation of the form:

$$\dot{\epsilon} = \frac{A\Omega D_{eff}}{kTd^3}\sigma + K\sigma^n \qquad \textbf{(Eq 4)}$$

where A is a constant, Ω is the atomic volume, D_{eff} is the effective diffusion coefficient, k is Boltzmann's constant, T is absolute temperature, K is the constant for power-law creep (containing dependencies on temperature and shear modulus), and n is the power-law creep exponent. Designating ($A\Omega D_{eff}/kT$ by A', and if dynamic grain growth is given by $d \sim d_o t^p$, the diffusional creep portion can be rewritten as:

$$\dot{\epsilon}_d = \frac{A' \sigma}{d_o^3 \, t^{3p}} \qquad \textbf{(Eq 5)}$$

where d_o refers to the initial grain size. During the step strain rate test with constant strain rate segments, if a stress of σ_1 is obtained at a strain rate $\dot{\epsilon}_1$ and a strain of ϵ_1, and σ_2 at $\dot{\epsilon}_2$ and ϵ_2, from Eq 4, then:

$$\frac{\sigma_2}{\sigma_1} = \left(\frac{\epsilon_2}{\epsilon_1}\right)^{3p} \left(\frac{\dot{\epsilon}_2}{\dot{\epsilon}_1}\right)^{(1 - 3p)} \qquad \textbf{(Eq 6)}$$

because $t = \epsilon/\dot{\epsilon}$ for a constant strain rate test. The strain rate sensitivity *m* can be given by:

$$m = \frac{\log\left(\frac{\sigma_2}{\sigma_1}\right)}{\log\left(\frac{\dot{\epsilon}_2}{\dot{\epsilon}_1}\right)} = (1 - 3p) + 3p \frac{\log\left(\frac{\epsilon_2}{\epsilon_1}\right)}{\log\left(\frac{\dot{\epsilon}_2}{\dot{\epsilon}_1}\right)} \qquad \textbf{(Eq 7)}$$

Therefore, the amount of strain accumulated in each strain rate step will influence the value of *m* through its influence on dynamic grain growth kinetics. If p = 0.25 (Ref 9), then in the limit, *m* could vary between 0.25 and 1 and might be responsible for the apparent threshold stress-like behavior at low strain rates. The concurrent grain growth induced hardening thus makes it difficult to describe superplastic flow behavior accurately.

Step strain rate tests conducted with minimum strain accumulation are used as the initial stress-strain rate behavior of the materials, and the strain-hardening component is measured and added separately. The rationale for this is seen in Fig. 7, in which two step strain rate tests conducted with intermediate superplastic deformation clearly show that the hardening effect is not negligible. Although it may not be obvious from the slopes of these plots that *m* is also influenced by strain, a drop in *m* would be expected on the basis of grain growth (Fig. 3). The strain dependence of *m* can also be determined during constant strain rate tests by making incremental strain rate changes (Fig. 8a) of a small magnitude. With strain rate changes of 25 to 40% maintained over a 2 to 3% plastic strain, the microstructure may not be altered significantly after return to the original strain rate. Figure 8(b) shows the decrease in *m* measured from these tests. Similar results have also been observed in aluminum alloys (Ref 21). The drop in *m* is usually more rapid at the higher strain rates and may be related to classical hardening due to dislocation buildup, which is also faster at high strain rates. Even though strain hardening has important implications for superplastic flow, the rate sensitivity of strain hardening is usually small, and *m* is still the parameter of greater interest.

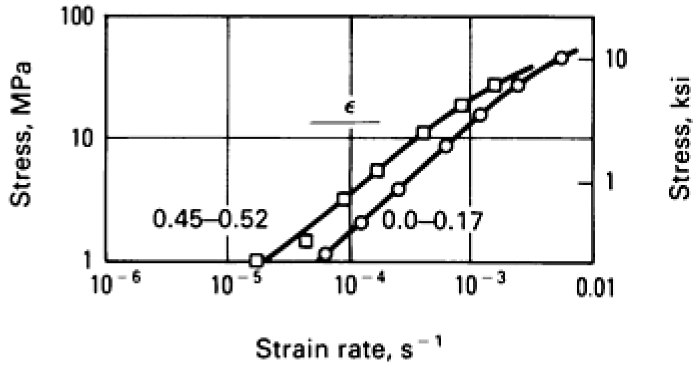


Fig. 7 Stress versus strain rate plots for 6.4 μm (250 μin.) grain size Ti-6Al-4V at 927 °C (1701 °F) initially (that is, up to ϵ = 0.17) and after a strain of 0.45 at a rate of 2 × 10^{-4} s^{-1} showing the hardening contribution due to the deformation.

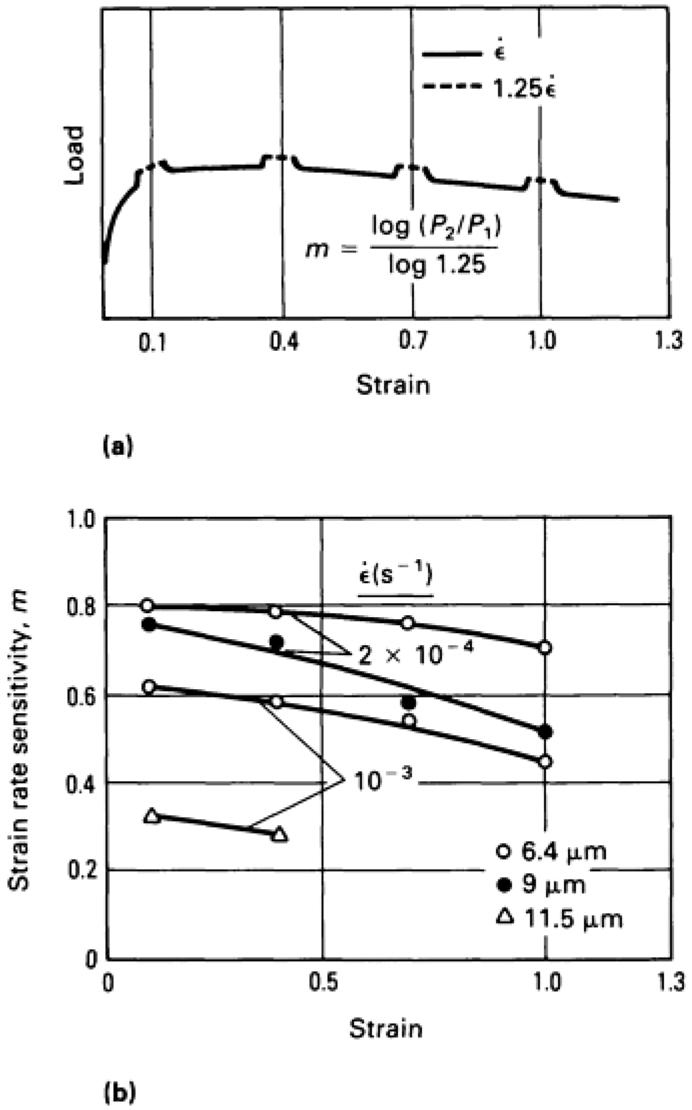


Fig. 8 Schematic representation (a) showing how instantaneous measurements of *m* can be made at periodic intervals during the tensile test by strain rate increments of 25%. (b) Corresponding *m* value as a function of strain for Ti-6Al-4V at 927 °C (1701 °F)

Important considerations in the selection and use of a superplastic alloy are the total elongation capability, the stability of the superplastic microstructure at high temperature, the latitude of the temperature and strain rate range over which superplasticity is observed, and the rate of development of cavitation during superplastic deformation. All of these factors can change from lot to lot of the material, and it is generally advisable to check each lot for the superplastic properties as well as design properties. Information on superplasticity in specific materials is available in the articles "Forming of Aluminum Alloys" and "Forming of Titanium and Titanium Alloys" in this Volume.

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Superplastic Sheet Forming

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SPF Processes

A number of methods and techniques have been reported for forming superplastic materials, each of which has a unique capability and develops a unique set of forming characteristics (Ref 3, 22). The following are forming methods that have been used with superplastic alloys:

- Blow forming
- Vacuum forming
- Thermo-forming
- Deep drawing
- Superplastic forming/diffusion bonding (DB)
- Forging
- Extrusion
- Dieless drawing

Only those processes that relate to sheet metal forming will be described in this section. Superplasticity as related to bulk forming operations is discussed in the Appendix to this article and in the article "Isothermal and Hot-Die Forging" in this Volume.

Blow forming and vacuum forming are basically the same process (sometimes called stretch forming) in that a gas pressure differential is imposed on the superplastic diaphragm, causing the material to form into the die configuration (Ref 3, 22, 23, and 24). In vacuum forming, the applied pressure is limited to atmospheric pressure (that is, 100 kPa, or 15 psi), and the forming rate and capability are therefore limited. With blow forming, additional pressure is applied from a gas pressure reservoir, and the only limitations are related to the pressure rating of the system and the pressure of the gas source. A maximum pressure of 690 to 3400 kPa (100 psi to 500 psi) is typically used in this process.

The blow forming method is illustrated in Fig. 9, which shows a cross section of the dies and forming diaphragm. In this process, the dies and sheet material are normally maintained at the forming temperature, and the gas pressure is imposed over the sheet, causing the sheet to form into the lower die; the gas within the lower die chamber is simply vented to atmosphere. The lower die chamber can also be held under vacuum, or a back pressure can be imposed to suppress cavitation if necessary. The use of back pressure to control or prevent cavitation is discussed in the sections "Pressure Profiling" and "Cavitation and Cavitation Control" in this article.

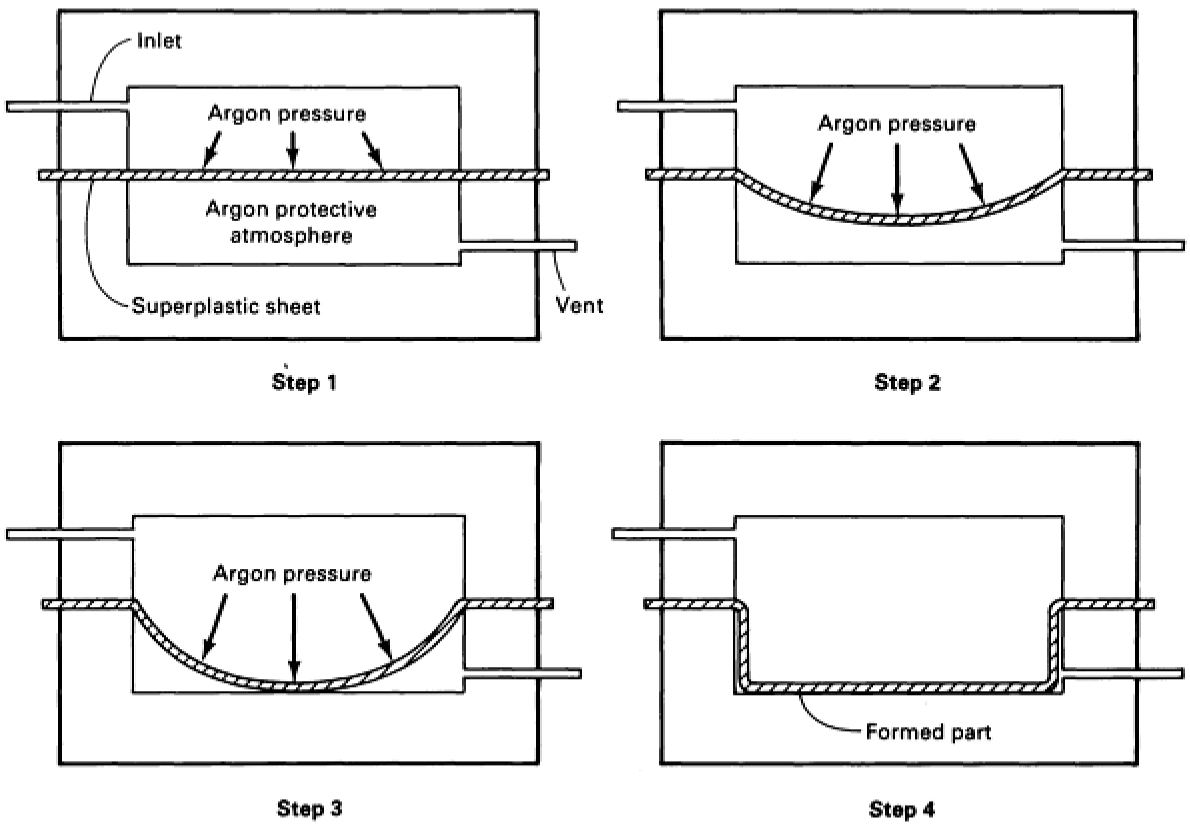

Fig. 9 Schematic of the blow forming technique for superplastic forming

The rate of pressurization is normally established such that the induced strain rates in the forming sheet are maintained in the superplastic range. The rate of pressurization is determined by trial and error or by the application of analytical modeling methods (Ref 25, 26, 27, and 28). This pressure is generally applied slowly rather than abruptly in order to prevent too rapid a strain rate and consequent rupturing of the part.

The periphery of the sheet is held in a fixed position and does not draw-in, as would be the case in typical deep-drawing processes. It is common to use a raised land (seal bead) machined into the tooling around the periphery as shown in Fig. 10 to secure the sheet from slippage and draw-in and to form an airtight seal in order to prevent leakage of the forming gas. Therefore, the sheet alloy stretches into the die cavity, and all of the material used to form the part comes from the sheet overlying the die cavity. This results in considerable thinning of the sheet for complex and deep-drawn parts, and it can also result in significant gradients in the thickness in the finished part.

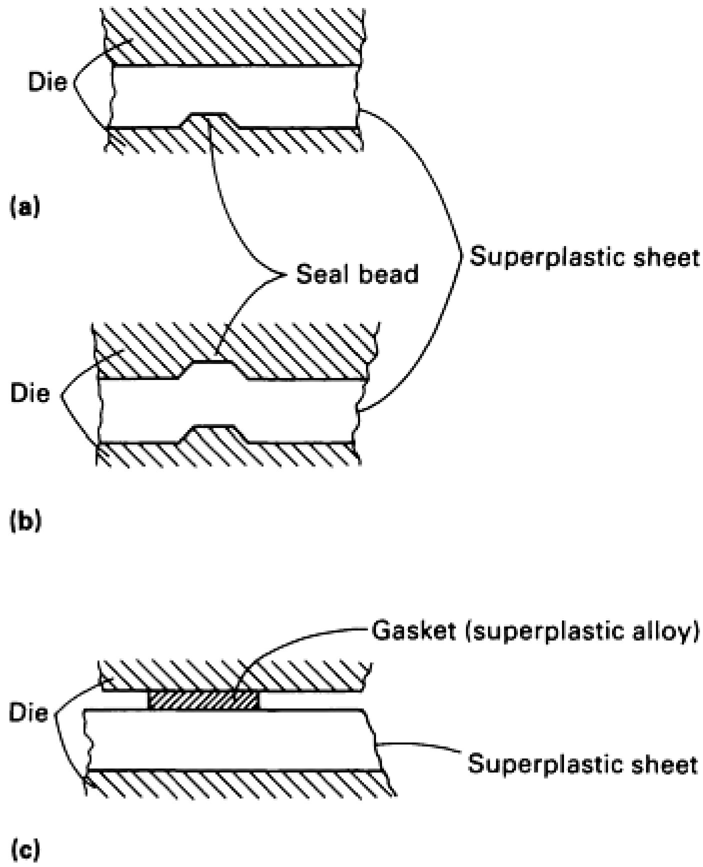

Fig. 10 Various sealing methods that have been used around the sheet to provide a pressure seal suitable for containing the gas pressure during forming. Sections (a) and (b) utilize seal beads machined into the tooling, and (c) shows the use of a superplastic frame used as a soft gasket.

This process is being increasingly used to fabricate structural and ornamental parts from titanium, aluminum, and other metals. An example of the process applied to the forming of a titanium aircraft nacelle frame is illustrated in Fig. 11 (Ref 29). In this case, the forming is conducted at about 900 °C (1650 °F), and inert gas (argon) is used on both sides of the sheet to minimize oxidation and related detrimental surface degradation due to the reactivity of titanium. The use of such protective gases is not usually necessary for aluminum alloys.

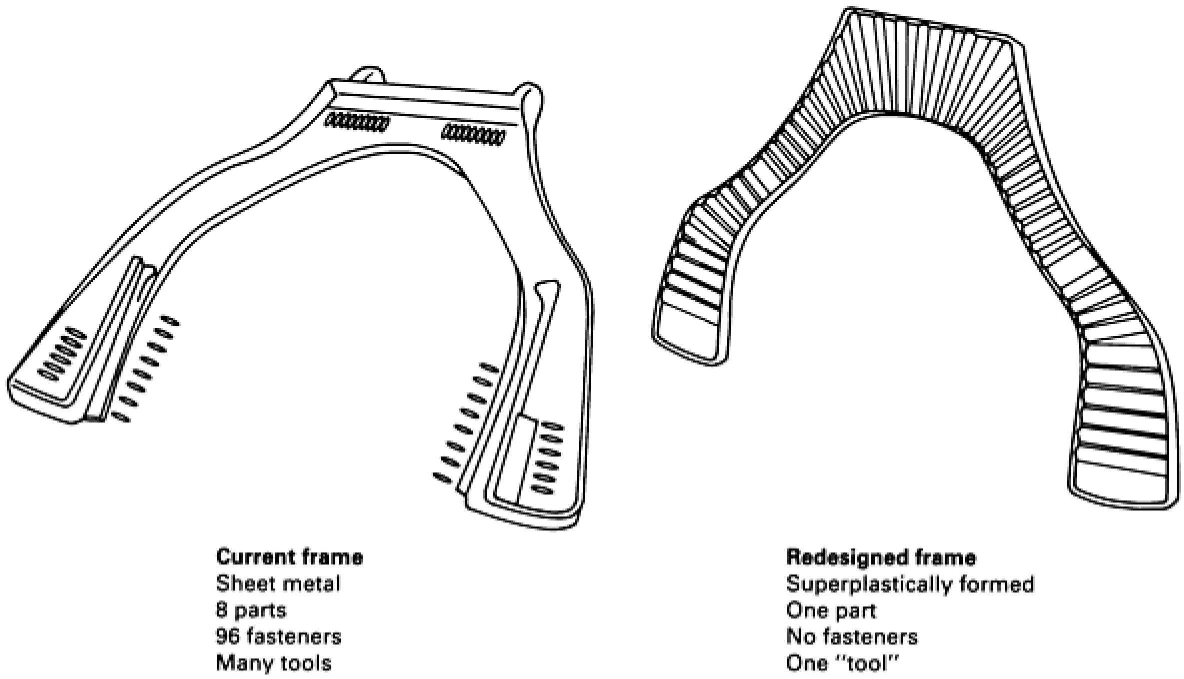

Fig. 11 Ti-6Al-4V aircraft nacelle frame that was redesigned from a conventional configuration to one suitable for superplastic forming having fewer parts and fasteners. The redesigned version of this B-1B aircraft component, having 0.161 m^2 (250 $in.^2$) plan view area, resulted in a 33% weight savings and a 55% cost savings over a conventional multiple-piece assembly.

Large, complex parts can be readily formed by this method; it has the advantage of no moving die components (that is, no double-acting mechanisms) and does not require mated die components. Multiple parts can be formed in a single process cycle, thus permitting an increase in the production rate for some parts.

Thermo-forming methods have been adopted from plastics technology for the forming of superplastic metals, and these methods sometimes use a moving or adjustable die member in conjunction with gas pressure or vacuum (Ref 23, 24, 30). Figure 12 shows two examples of thermo-forming methods. In Fig. 12(a), an undersize male die punch is used to stretch form the superplastic sheet, followed by application of gas pressure to force the sheet material against the configurational die to complete the shaping operation. In Fig. 12(b), the first step involves blowing a bubble in the sheet away from the tool. The male tool is then moved into the bubble, and the pressure is reversed to cause the bubble to conform to the shape of the tool.

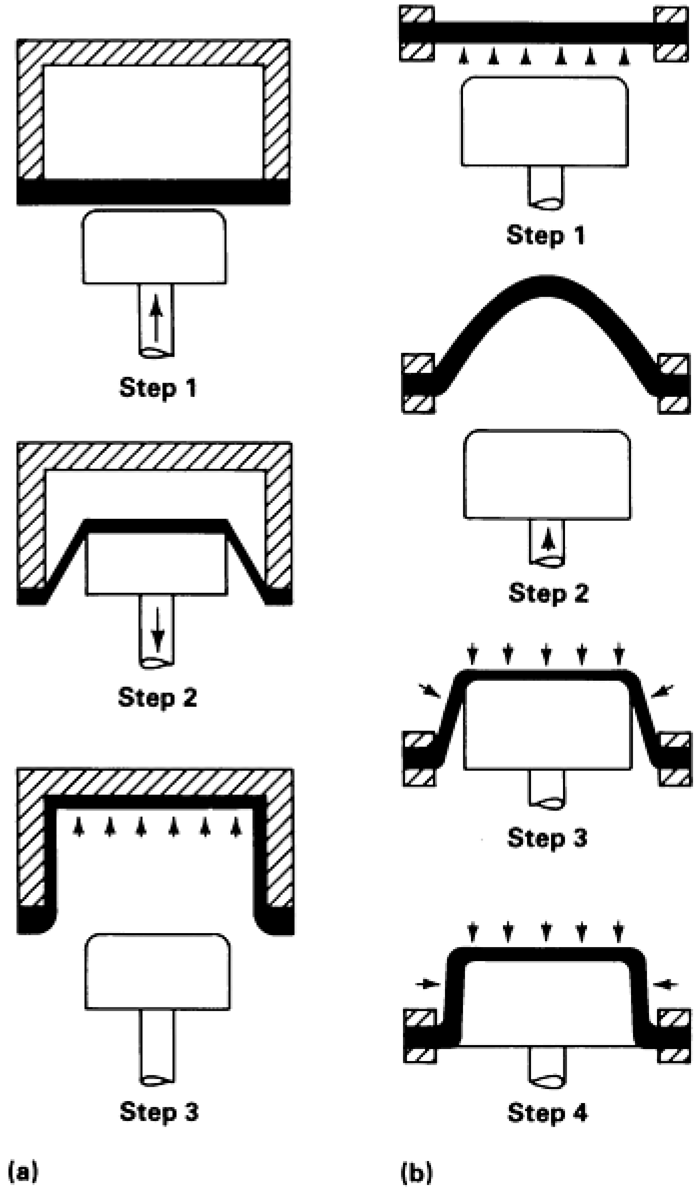

Fig. 12 Examples of thermo-forming methods used for superplastic forming. (a) Plug-assisted forming into a female die cavity. (b) Snap-back forming over a male die that is moved up into the sheet

Figure 13 illustrates two other methods that employ a movable die member that aids in prestretching the sheet material before gas pressure is applied. In this case, the gas pressure is applied from the same side of the sheet as the moving die. These techniques provide ways of producing different shapes of parts and are effective for controlling the thinning characteristics of the finished part.

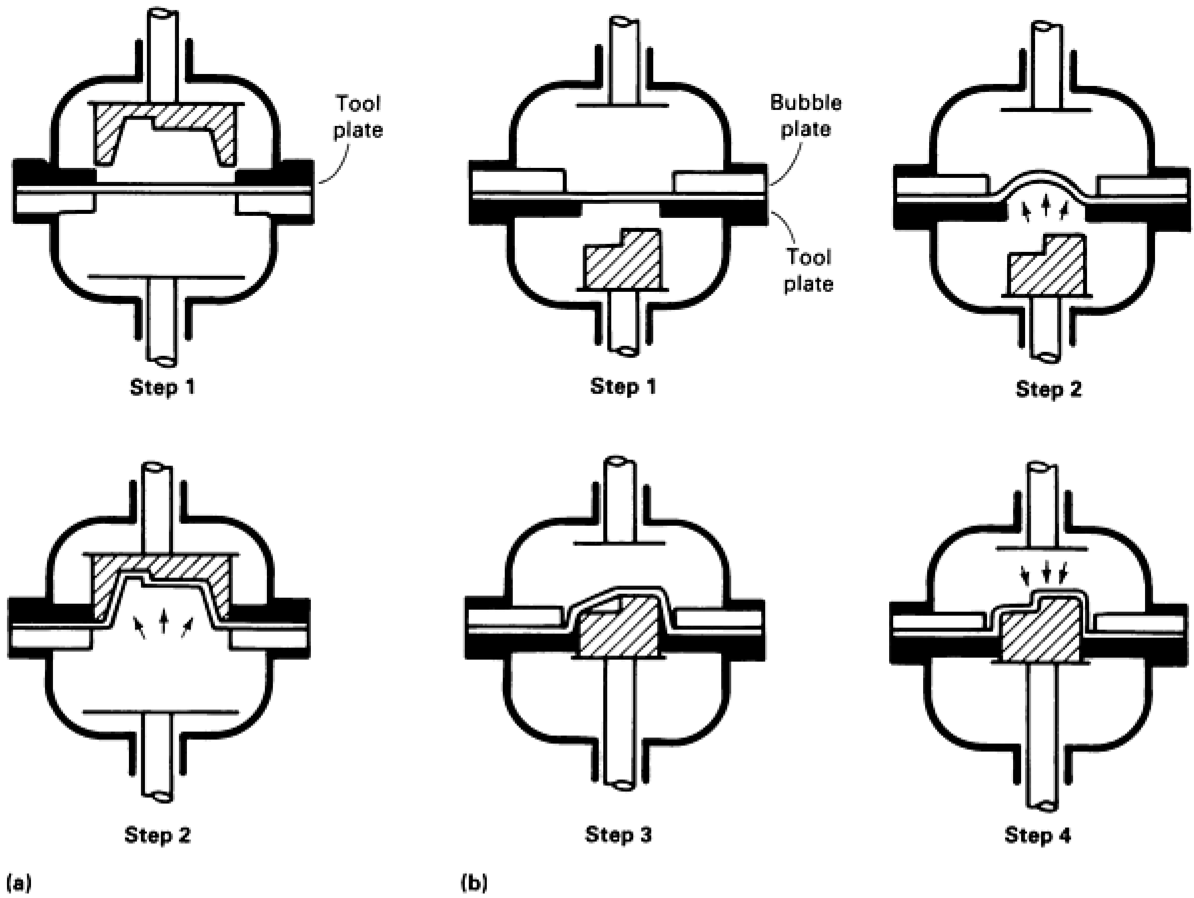

Fig. 13 Thermo-forming methods that use gas pressure and movable tools to produce parts from superplastic alloys. (a) Female forming. (b) Male forming

Deep Drawing. Although deep-drawing studies have been conducted with superplastic metals, this process does not appear to offer many significant advantages in the forming of superplastic materials. Deep drawing depends on strain hardening to achieve the required formability and to prevent thinning and rupture during forming. Superplastic materials do not strain harden to any great extent, but they depend on the high strain rate hardening for their forming characteristics, and this property seems to offer little aid to deep drawing.

The difficulty is that, in order to draw-in the flange, the material in contact with the punch nose, as well as that in the sidewall, must work harden to carry the increasing stresses required to draw-in increasing amounts of the flange. At superplastic temperatures, no significant work hardening occurs, and the punch typically pierces the blank, or the blank fails in the cup walls if the frictional constraint between the punch and the blank is high. However, in studies on the zinc-aluminum alloy, a maximum draw ratio of 2:1 has been developed under optimized conditions (Ref 31).

A technique that tends to improve the drawability of superplastic alloys is discussed in Ref 32. This method (Fig. 14) uses a punch cooled to a temperature below that of the forming blank, while the hold-down tooling is maintained at the forming temperature. It was demonstrated that this differential temperature technique permitted an increase in the limiting draw ratio from less than 2.4:1 for isothermal conditions to more than 3.75:1 for the differential temperature method. The thinning characteristics for this process are also shown in Fig. 14. Slight thinning can be seen to occur over the (cold) punch nose, and substantial thinning is seen in the material adjacent to the punch. The extent of the thinning in the material adjacent to the punch depends on the blank hold-down load, but increases with increasing blank diameter (draw ratio) and decreasing punch speed. Because ironing was not used in these forming tests, thickness increases were observed at the greater distances from the pole of the cup where substantial draw-in of the material occurred.

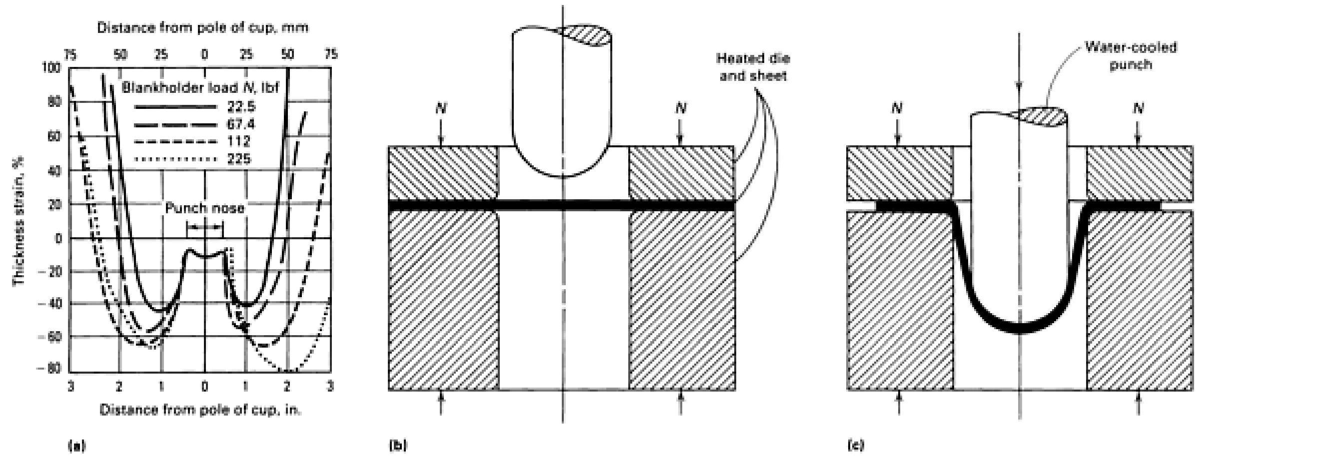

Fig. 14 Punch setup for deep drawing a superplastic sheet. (a) Plot showing thinning characteristics of a 59.9 mm (2.36 in.) diam Zn-21Al-1Cu-0.1Mg heated sheet that was formed using the 160 mm (6.3 in.) diam water-cooled punch setup illustrated in (b) and (c). *N*, blankholder load. Both the die and zinc alloy sheet were heated to 230 °C (445 °F). Punch speed was 33.0 mm/min (1.3 in./min), and maximum punch load was 2150 N (483 lbf). Draw ratio was 3.75. See text for discussion.

Another concept evaluated to explore the deep-drawing capability is discussed in Ref 33. This method uses high-pressure oil around a blank periphery to aid in the drawing. It is actually a combined extrusion and drawing process. In this study, a tin-lead eutectic was used, which permitted processing at ambient temperature. Good control of wall thickness was achieved, but the applicability of the process to alloys requiring high temperatures has yet to be demonstrated.

SPF/DB Processes. Recent developments have demonstrated that a number of unique processes are available if joining methods, such as diffusion bonding, can be combined with superplastic forming; these processes are generally referred to as SPF/DB processes (Ref 22, 34, 35). Although diffusion bonding is not a sheet metal process, it complements and enhances superplastic forming to such an extent that the two processes must be discussed together.

The SPF/DB processes have evolved as natural combinations of the SPF and DB processes because the process temperature requirements of both are similar. The low flow properties characteristic of the superplastic alloys aid the DB pressure requirements, and it has been found that many superplastic alloys can be diffusion bonded under pressures in the same low range as that used for SPF processing (that is, of the order of 2100 to 3400 kPa, or 300 to 500 psi). The SPF method used with SPF/DB to date is that of blow forming.

The resulting SPF/DB process consists of the following variations:

- Forming of a single sheet onto pre-placed details, followed by diffusion bonding (Fig. 15)
- Diffusion bonding of two sheets at selected locations, followed by the forming of one or both into a die (Fig. 16); the reverse sequence can also be used
- Diffusion bonding of three or more sheets at selected locations under gas pressure, followed by expansion under internal gas pressure, which forms the outer two sheets into a die; in the process, the center sheet(s) is stretched into a core configuration (Fig. 17)

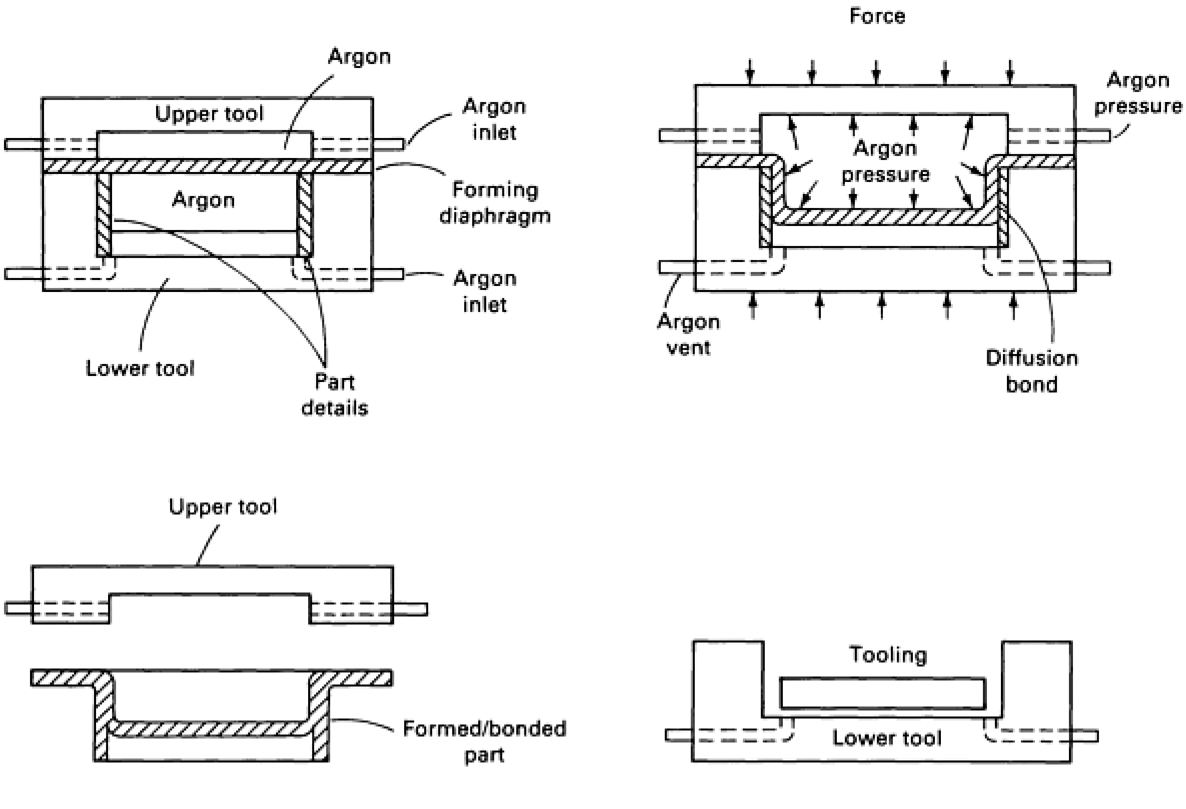

Fig. 15 Cross section of the SPF process combined with diffusion bonding (SPF/DB). The process shown utilizes pre-placed details to which the superplastic sheet is bonded.

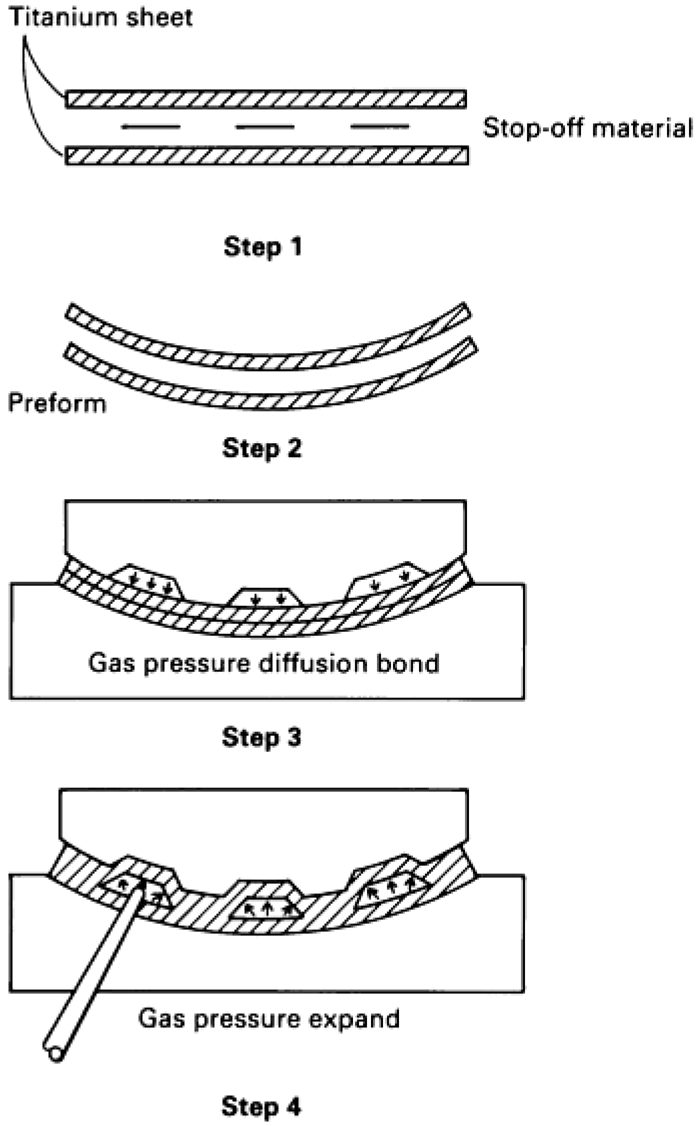

Fig. 16 Operations required for joining two sheets of superplastic alloy using the SPF/DB process.

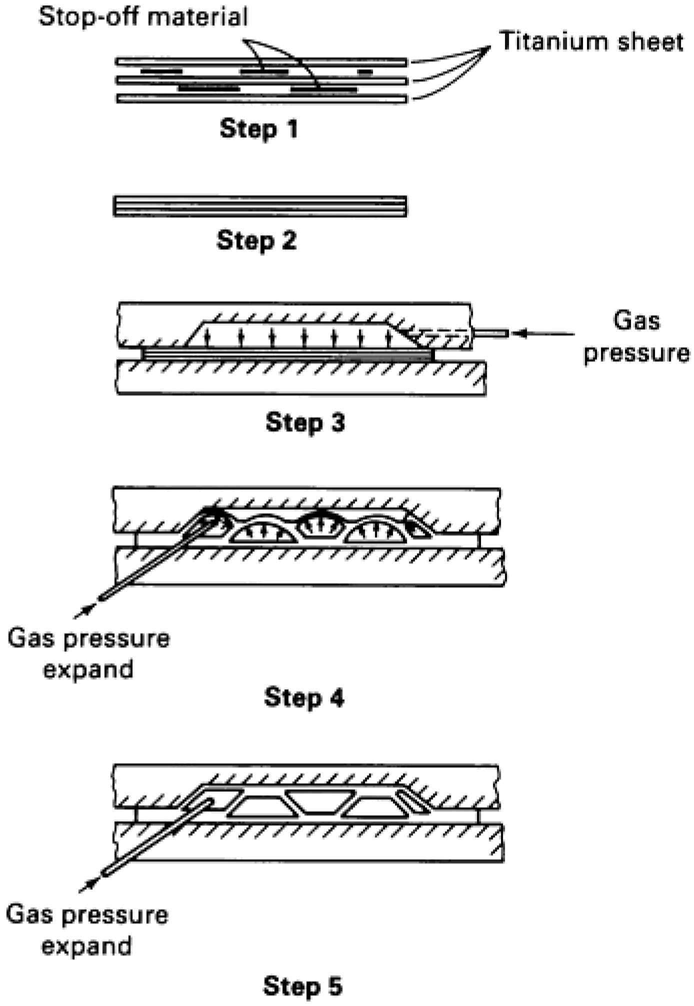

Fig. 17 Operations required for joining three sheets of superplastic alloy using the SPF/DB process

A few techniques have been used to develop diffusion bonds in predetermined local areas. One of these involves the use of a parting agent, or stop-off material, between the sheets in the local areas where no bonding is desired. Suitable stop-off materials may depend on the alloy being bonded and the temperature being used. For example, yttria or boron nitride has been successfully employed to stop-off titanium alloys processed to temperatures of at least 930 °C (1705 °F). Such stop-off materials can be suspended in an appropriate binder, such as acrylic. After a DB operation, the area of the stop-off pattern is not bonded, and gas can be applied internally along this pattern, thus causing the external sheets to be separated and formed by expanding into a surrounding die.

A variation of the above method involves the use of a minimum of four sheets to make a sandwich panel. The external (for example, skin) sheets are expanded, the inner two sheets are then bonded (or welded) to define the core structure, and finally the core is expanded to bond with the external sheets and to complete formation of the sandwich structure. This sequence is shown in Fig. 18.

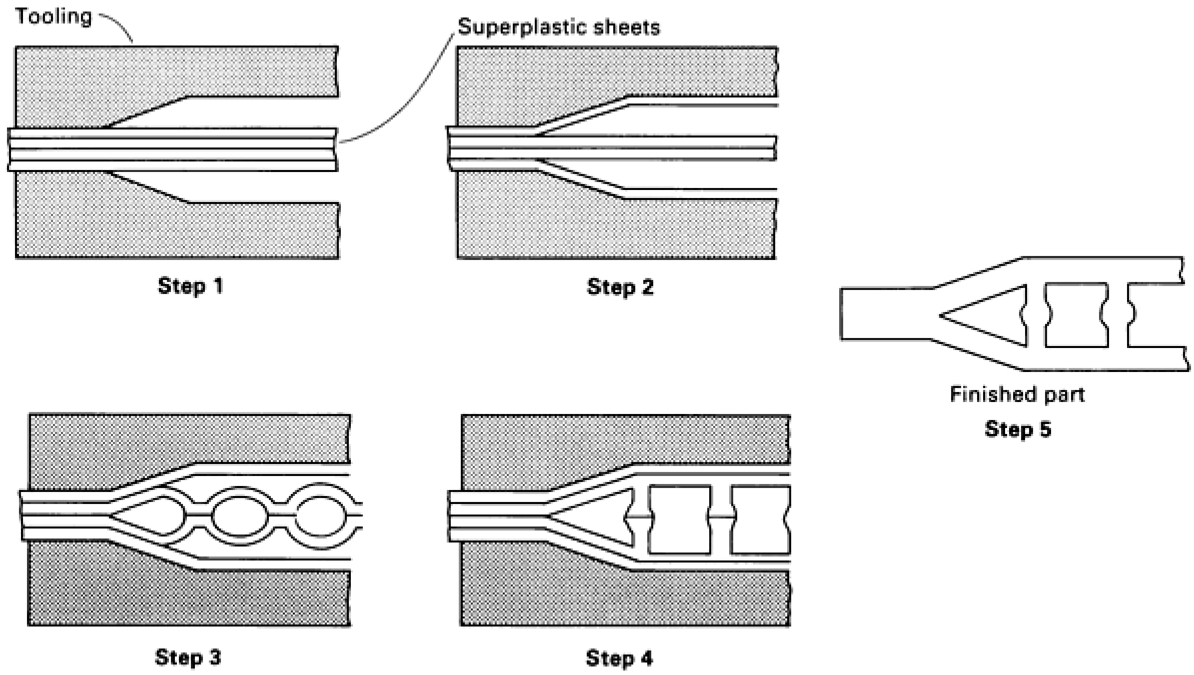

Fig. 18 Example of a four-sheet SPF/DB process in which the outer sheets are formed first and the center sheets are then formed and bonded to the outer two sheets.

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Forming Equipment and Tooling

The forming of superplastic sheet materials involves methods that are generally different from those used in other, more conventional sheet forming processes. The forming environmental conditions are also different. Therefore, the equipment and tooling used are generally different.

Forming Equipment. For the blow forming and vacuum forming methods, there is a need to provide constraint to the forming tools in order to counteract the forming gas pressure. In addition, a seal is generally required at the interface between the sheet and the tool around the periphery in order to prevent leakage of the gas pressure. A press is typically used to meet these requirements. Hydraulic presses and mechanical clamping systems have been used, and each has advantages and disadvantages. The hydraulic press can be loaded and unloaded fairly rapidly, but it requires a significant capital investment. The mechanical clamping systems are much less expensive, but are more cumbersome to load and unload. Recently, robotic systems have been coupled with a hydraulic press to aid the loading and unloading, and this type of advanced system is especially beneficial for high-temperature forming operations such as titanium alloy SPF processing.

The hydraulic presses used include both single-action and multiple-action systems (Ref 22, 23, 30). In the single-action press, the press applies the constraining pressure only. In the multiple-action press, the press can also move dies into the forming sheet and effectively aid in the control of the thinning gradients (Fig. 12 and 13).

The heating system used must be tailored to the temperature required and the allowable thermal gradients. The most common heat source is electrical heating, in which resistance heating elements are embedded in ceramic or metal pressure plates placed between the tooling and the press platens. This allows for good control of the temperature and provides a clean source of energy. The heating platens can be arranged in sections of heating elements, and each section can be controlled by independent temperature controllers to minimize thermal gradients in the forming die assembly. Significant thermal gradients can lead to excessive thinning or rupture of the sheet during forming.

Tooling Materials. The tooling used in the SPF process is generally heated to the forming temperature, and it is subjected to internal gas pressure and pressing clamping loads. The internal gas pressure is typically less than about 3400 kPa (500 psi), and this is usually not the critical design factor for SPF tools. More important are the clamping loads and thermal stresses encountered during heat-up and cool-down and the environmental conditions. The thermal stresses can cause permanent distortions in the die, and this is controlled by selection of a material that has good strength and creep resistance at the forming temperature. Slow heating and cooling of the tooling can reduce the thermal stresses. Materials with a low coefficient of thermal expansion and those that do not undergo a phase transformation during heating and cooling are preferred for the high-temperature SPF processes.

The environmental conditions can be severe for the forming of high-temperature materials, such as the titanium alloys, iron alloys, nickel alloys, and other high-temperature metals. Oxidation can alter the surface condition of the tooling, thus affecting the surface quality of the SPF part produced and eventually affecting the dimensional characteristics.

Another important environmental factor is the compatibility between the superplastic sheet and the tooling, and the compatibility of these with the stop-off materials that may be used. Interdiffusion at the tooling/sheet interface can result in the degradation of both of these materials. Reactive metals, such as titanium alloys, are especially prone to this type of problem. Tooling materials that have been found to be successful with titanium alloys are the Fe-22Cr-4Ni-9Mn alloy and similar materials. Parting, or stop-off, agents are also helpful in minimizing the interaction, and materials such as boron nitride and yttrium oxide have been successfully used. Generally, materials with a low solid solubility in the sheet are good candidates for compatibility.

A variety of materials have been used for SPF tooling, including metals and alloys, ceramics, and graphite (Ref 36). Metal tools are preferred for large production quantities, such as 100 parts or more. Graphite tools are suitable for about 100 parts, and they are readily hand worked, although there is a problem with shop cleanliness with graphite. Ceramics can be cast into the desired shape and are therefore inexpensive for a variety of large parts. Because the ceramic is subject to cracking and rapid degradation, it requires frequent repair. Ceramic tools are considered for small production quantities, usually less than about 10 parts.

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Superplastic Sheet Forming

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Thinning Characteristics

To take advantage of the very high elongations possible with superplastic metals, it is necessary to accept the accompanying significant thinning in the sheet material. This thinning is a natural consequence of the deformation conditions. For superplastic deformation, elastic strains are negligible; therefore, constancy of volume can be assumed. From this consideration, the sum of the plastic strains is 0, and tensile strain in one direction must be balanced by compressive (negative) strains in another. The strains are:

$$\epsilon_1 + \epsilon_2 + \epsilon_3 = 0 \quad \textbf{(Eq 8)}$$

where ϵ is the strain, and the subscripts indicate the principal directions. For example, in a sheet forming operation under plane-strain conditions, $\epsilon_2 = 0$ and $\epsilon_3 = -\epsilon_1$. In this case, the thinning strain (for example, ϵ_3) is equal and opposite to the longitudinal tensile strain, and the thinning will therefore match the tensile deformation. For large tensile strains, the thinning will be correspondingly large. Accordingly, as the thinning increases, the tendency to develop thinning gradients also increases.

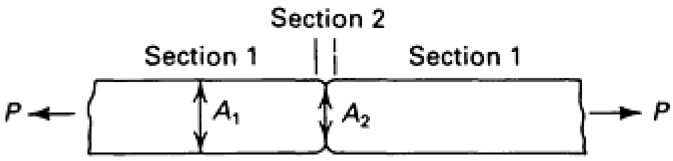
Although the superplastic materials are effective in resisting the necking process, they nonetheless do neck (in relation to the *m* value), and thinning gradients do develop. Therefore, in the design and processing of SPF parts, it is important that the thinning be understood and considered.

Uniaxial Tensile Test. It has been shown that superplastic deformation occurs when *m* is large and that under these conditions the deformation process is predominately postuniform, in contrast to conventional metal tensile behavior. In most cases, virtually all deformation is nonuniform, and the issue in the tensile behavior is the extent of this nonuniformity. The thinning in the tensile specimen can be assumed to be the result of a preexisting inhomogeneity, which can grow under the imposed deformation (Ref 37, 38).

The rate of thinning in the tensile specimen is therefore determined not only by the size of the inhomogeneity but also by the *m* value. This has been demonstrated analytically for an idealized tensile specimen (Fig. 19) containing a geometric inhomogeneity, *f* (for example, a machining defect) (Ref 9). This analysis follows the strain development both inside and outside the inhomogeneity, assuming that the applied load is fully transferred along the length of the specimen and that the material obeys the following constitutive equation:

$$\sigma = K \epsilon^n \dot{\epsilon}^m \quad \textbf{(Eq 9)}$$

where *n* is the strain-hardening exponent (*n* is small in this case). The results of calculations using Eq 9 are shown in Fig. 20, in which the strain in the inhomogeneity is graphed as a function of the strain outside the inhomogeneity for a number of different *m* values. The extent of the thinning in the tensile specimen is shown to be strongly related to the *m* value, although thinning gradients will develop at all *m* values if the strain is sufficiently large. It will be seen that this is also the case for sheet forming in which the inhomogeneity is caused by stress gradients resulting from the part geometry and tool interactions. The inhomogeneities in tensile specimens have also been found to relate to the *m* value (Ref 39), as shown in Fig. 21 for the Zn-22Al eutectoid alloy. In Fig. 21, the results are presented for the same alloy tested at different strain rates, for which the *m* values are known to differ.



$$f = \frac{A_1 - A_2}{A_1}$$

$$\sigma_1 = \frac{P}{A_1};\ \sigma_2 = \frac{P}{A_2} = \frac{P}{A_1(1 - f)}$$

Fig. 19 Geometric inhomogeneity, *f*, in a tensile specimen

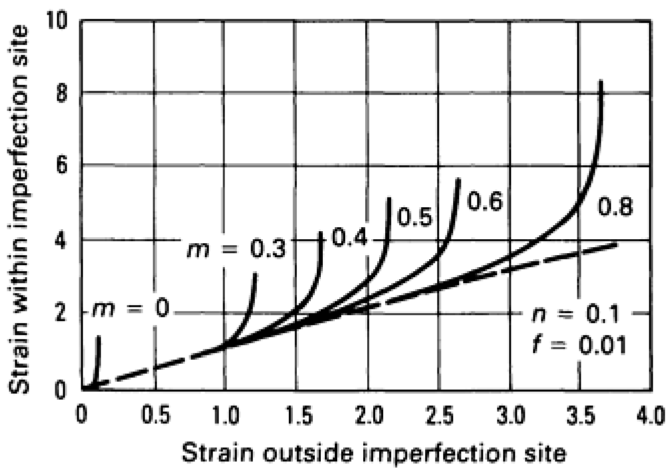


Fig. 20 Calculated strains inside and outside an inhomogeneity in a tensile specimen, such as that shown in Fig. 19, for various *m* values

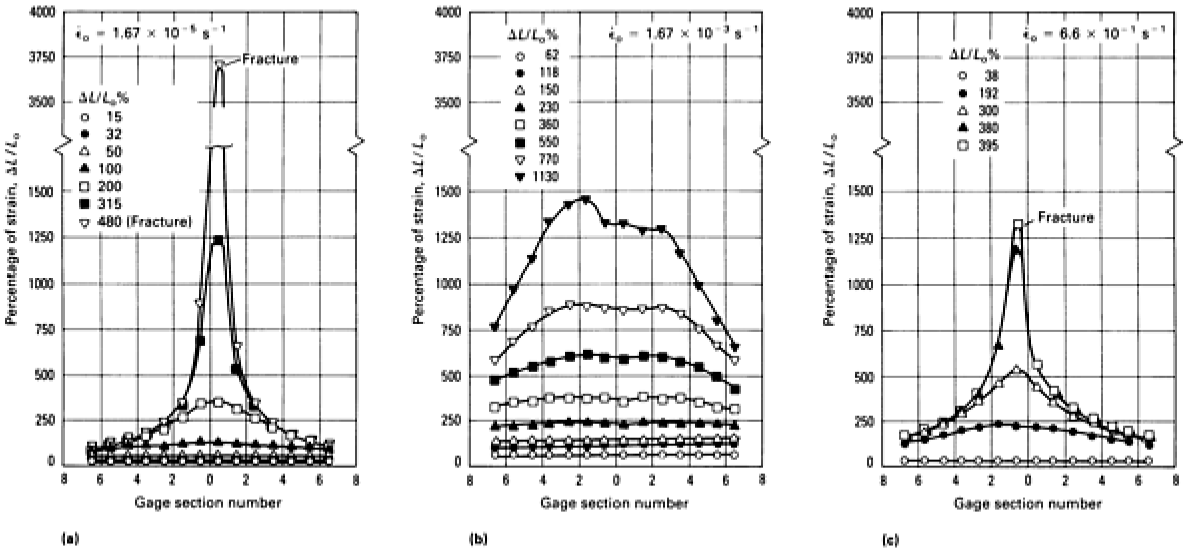


Fig. 21 Local elongation gradients in tensile specimens of Zn-22Al alloy at *T* = 473 K with initial gage length L_o of 12.7 mm (0.500 in.) that were tested within the superplastic strain rate range (b) and outside the superplastic strain rate range (a) and (c). The total percentage of strain at each termination point is given by $\Delta L/L_o$, where ΔL is the overall increase in gage length. The percentage of strain in each of the 14 individual segments of the gage length is given by $\Delta l/l_o$ %, where Δl is the increase in length of each small segment of the specimen. The initial strain rate was $\dot{\epsilon}_o$.

Spherical Domes. Although the thinning in superplastic tensile test specimens is the result of geometric inhomogeneities, the corresponding thinning in biaxially formed parts is usually the result of local stress state differences, which subsequently lead to the development of geometric inhomogeneities. In all of these cases, however, the difference in the local stresses leads to strain rate gradients, and the strain rate gradients develop directly into thickness gradients. A major difference between the tensile specimen and the part configuration is that, in the former, the stress gradients can be varied (that is, reduced) by dimensional control during machining. In the part forming, however, the configuration determines the stress state, and the stress state is not adjustable without changing the geometry.

The concept of thinning during SPF processing is perhaps best understood in terms of the bulging of a sheet (Ref 25, 26, 30, 40, 41, 42, 43, 44, 45, and 46). In this geometry, there is a stress state gradient from the pole of the dome to the edge, as shown in Fig. 22. If the dome is assumed to develop into a part of spherical symmetry, the stress state can be readily described. At the pole, the orthogonal stresses are equal, and the stress state is that of equibiaxial tensile. At the edge of the dome, there is constraint around the periphery, leading to a plane-strain stress state. Because the flow behavior of superplastic metals has been found to obey the von Mises criterion (Ref 47), it is helpful to examine the effective stress, $\overline{\sigma}$, which will determine the corresponding strain rate:

$$\bar{\sigma}_p = \frac{1}{\sqrt{2}}[(\sigma_\theta - \sigma_\phi)^2 + (\sigma_\phi - \sigma_t)^2 + (\sigma_t - \sigma_\theta)^2]^{1/2} \quad \textbf{(Eq 10)}$$

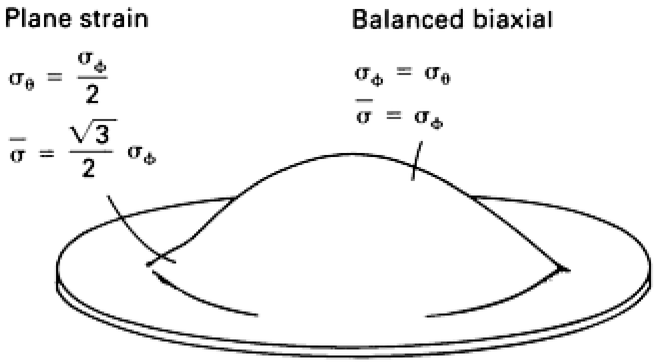


Fig. 22 Illustration of a spherical dome indicating the range of stress states existing between the pole and the edge

If it is assumed that the through-thickness stress is small with respect to the in-plane stresses, the effective stresses at the pole and the edge can be expressed in terms of the meridional stress, $\sigma\theta$, as follows:

$$\overline{\sigma}_p = \sigma\theta$$

and

$$\bar{\sigma}_e = \frac{\sqrt{3}}{2}\sigma_\theta = 0.87\sigma_\theta \quad \textbf{(Eq 11)}$$

where the subscripts *p* and *e* indicate the pole and the edge, respectively, of the dome. Therefore, the pole is experiencing a 15% higher flow stress than the edge, resulting in a higher strain rate, the initial magnitude of which depends on the *m* value.

The stress state difference between the pole and the edge of the dome is roughly the equivalent of the tensile specimen, which has a local geometric inhomogeneity, *f*, of 0.13. The initial strain rate difference between these two areas is dependent on the *m* value, the larger the *m* value, the smaller the strain rate difference and the less the tendency to develop a thickness gradient. For example, the ratio of $\dot{\epsilon}_e/\dot{\epsilon}_p$ is 0.87 for m = 1, and the value is 0.5 for m = 0.2--both for the same initial effective stress difference.

Therefore, the stress gradient in a forming dome causes a more rapid thinning rate at the pole, and it may be expected that the thinning difference will accelerate with time, leading to a thickness gradient in the formed dome. There are abundant experimental results to show that this is the case and that the thinning gradient is a function of the *m* value. Profiles of thickness for bulge-formed sheets are shown in Fig. 23 for *m* values of 0.57 and 0.23 (Ref 40). The thickness gradient is in agreement with expectations, and the effect of the high *m* value in impeding localized thinning at the pole can be seen. Other results for a titanium alloy and a stainless steel for which *m* values are 0.75 and 0.4, respectively, are shown in Fig. 24, in which the thickness strain is plotted as a function of the position along the dome cross section (Ref 21). The position along the dome is measured as the fractional height, h/h_o, where h_o is the full height of the dome and h is the height on the dome at which the thickness measurement is made.

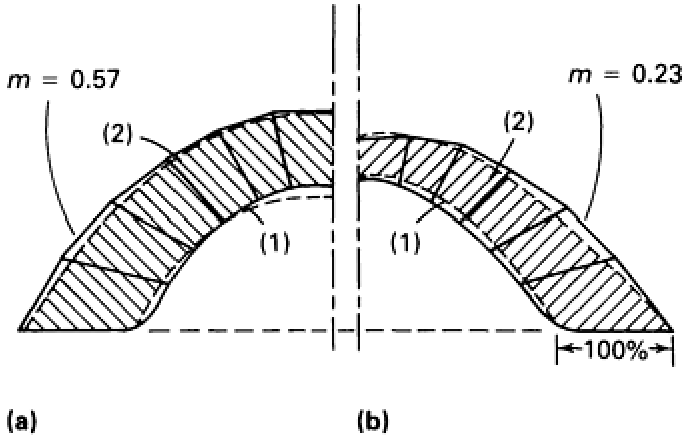


Fig. 23 Experimentally observed thickness profiles for a hemispherical dome formed from materials with two different *m* values (solid lines). The smaller, broken outlines confined mainly within the experimental data silhouette represent bulge profiles (1) and sheet thickness distributions (2) that were calculated using *m* = 0.50 (a) and *m* = 0.20 (b).

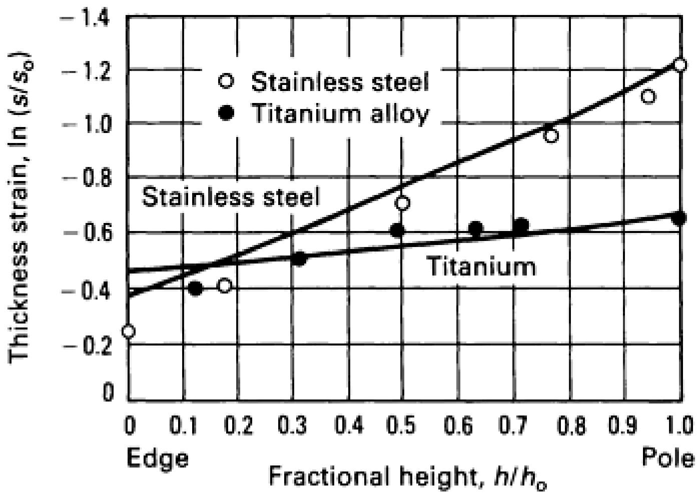


Fig. 24 Thickness strain as a function of the fractional height for dome-shaped parts formed from a stainless steel with an *m* value of 0.4 and a titanium alloy with an *m* value of 0.75. The values *s* and s_o in ln(s/s_o) represent dome thickness and initial sheet thickness, respectively.

A number of analytical developments have been reported that predict the thinning for the superplastic forming of this type of geometry (Ref 26, 30, 42, 44, 45, and 46). These models result in relations for thicknesses that are not closed-form, but require numerical integration of strain increments. The models predict the thinning characteristics reasonably well, as can be seen by the comparison of experimental and analytical data shown in Fig. 24 and 25.

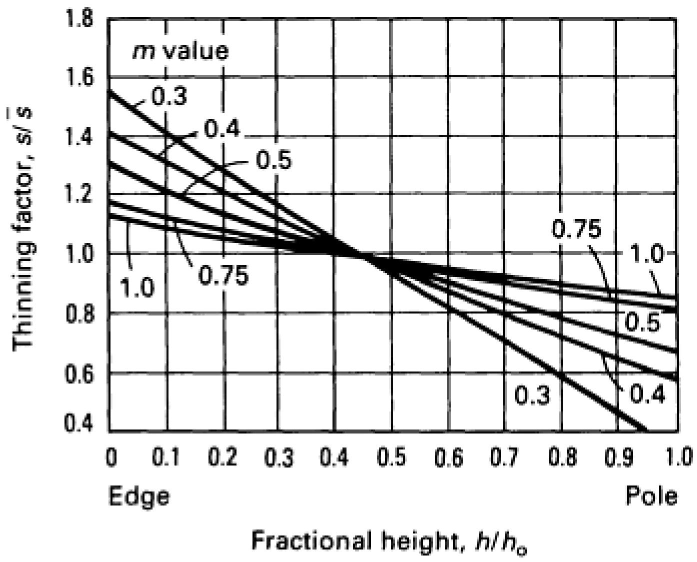


Fig. 25 Theoretical relations for a hemisphere showing the thinning factor as a function of the fractional height for a range of *m* values from 0.3 to 1.0

The theoretical predictions can be used to show the influence of the strain rate sensitivity of flow stress on the thinning gradient. For example, the thinning for a hemisphere formed from materials of differing *m* values is illustrated in Fig. 25. In Fig. 25, the thinning factor $s/\bar{S}$ is plotted as a function of the fractional height, where s is the local thickness and $\bar{S}$is the average dome thickness. The maximum thinning occurs at the pole because of the stress state, as mentioned previously, and the strain rate sensitivity is a crucial parameter in determining not only the initial strain rate difference but also the subsequent rate of thinning, as shown in Fig. 26, in which the thinning factor at the pole can be seen to be increasingly influenced by *m* as the dome height is increased.

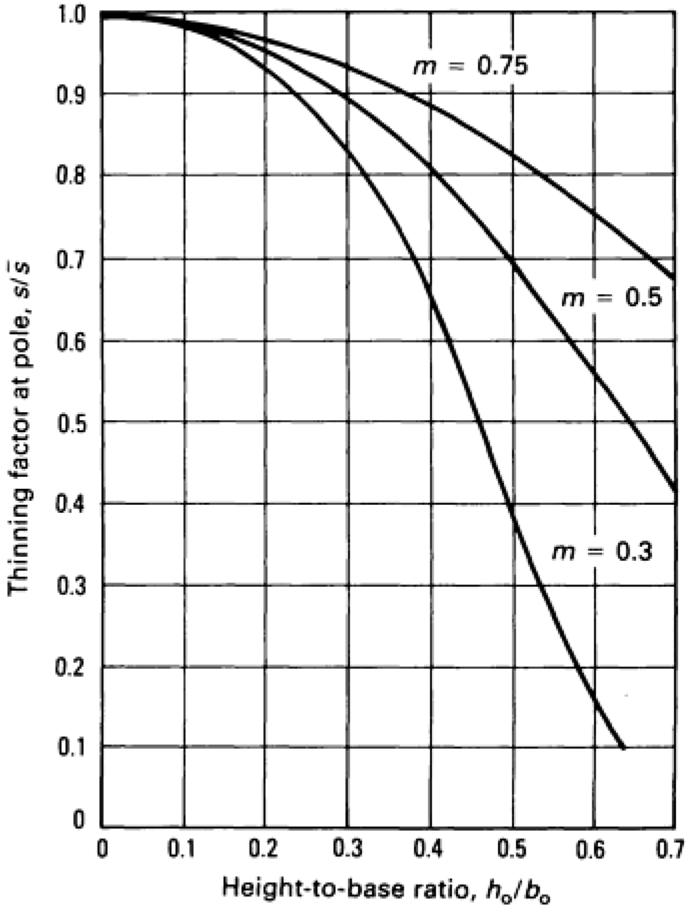


Fig. 26 Theoretical curves showing the thinning factor at the pole as a function of the bulge height-to-base ratio for *m* = 0.3, 0.5, and 0.75

The initial stress state differences and the corresponding strain rate differences along the meridian of a forming dome lead to a predictable thinning gradient in this type of geometry. The magnitude of the thinning gradient, however, is determined by the strain rate sensitivity, *m*, and the height to which the part is formed.

Rectangular Shapes. The factors that contribute to the thinning characteristics in rectangular parts, as well as other shapes, are the same as those for the spherical dome-shaped parts discussed in the previous section in this article. It is the specific geometry that determines the initial stress state gradients, and different geometries will be expected to develop different stress states in the forming part.

The rectangular shape is one that is common to many parts or sections of parts; therefore, it has been studied by experimental and analytical methods similar to those used for the spherical dome (Ref 22, 27, 47). For the long rectangular shape, there is a plane-stress state throughout the width of the sheet; for the case in which the die entry radius does not cause a significant stress concentration, the sheet will not experience an initial stress state gradient. This case is very similar to that of the tensile test, in which only thickness variations or material inhomogeneities will cause local stress differences leading to localized thinning. Because these are small in comparison to the magnitude of the stress variation in the forming dome, it may be expected that the thinning gradients would be less pronounced. Experimental results for the free-forming cylindrical section show this to be the case, as illustrated in Fig. 27, and virtually no thinning gradient is seen for hemicylindrical shapes.

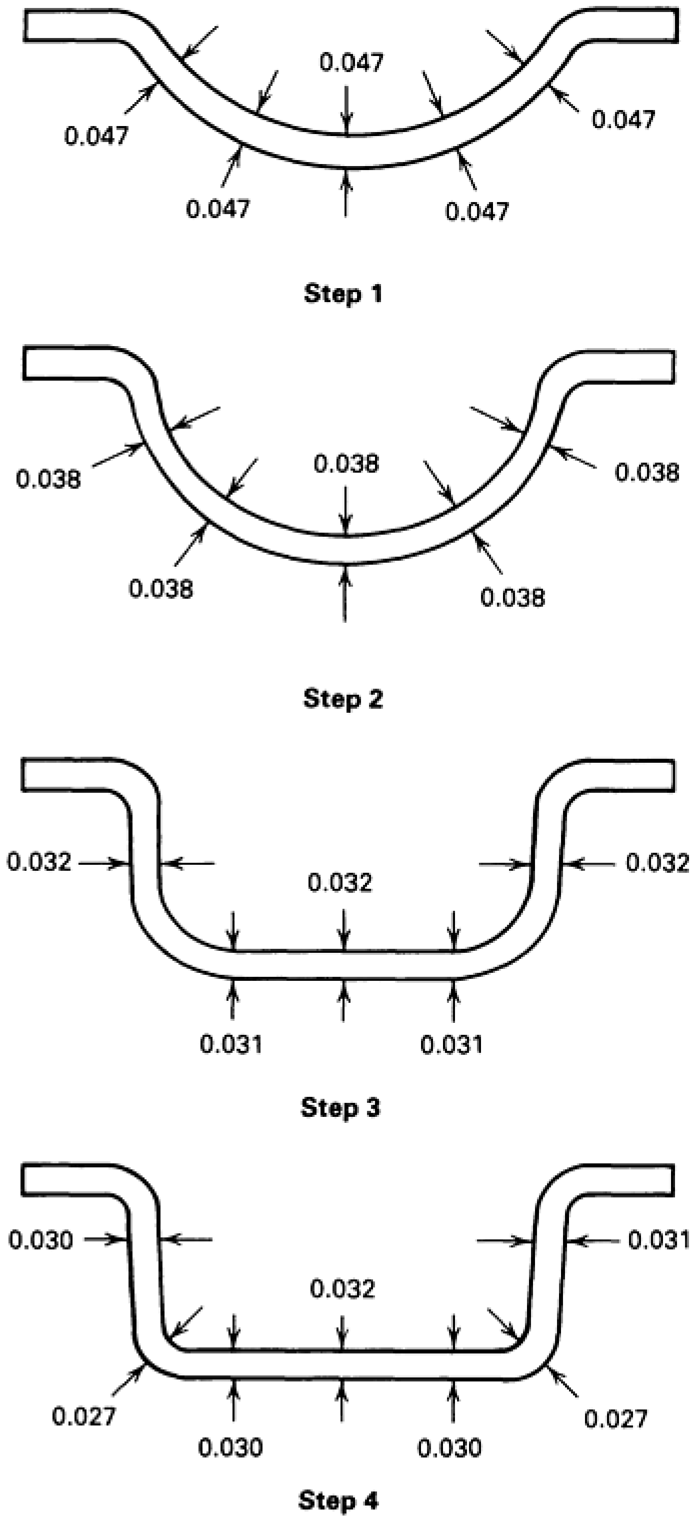


Fig. 27 Thinning development in a 1.37 mm (0.054 in.) thick superplastic formed Ti-6Al-4V part having a rectangular cross section and semi-infinite length. Formed at 870 °C (1600 °F) using a boron nitride lubricant, the sheet required 20 min to fabricate at an average strain rate of 5.8 × 10^{-4}. Dimensions given in inches

Interactions with the tooling do, however, cause local stress variations that can lead to thinning gradients, as shown in Fig. 27. This effect can be considered as two different types resulting from different areas of the die--the die surface at the bottom and sidewall, and the die entry radius.

Die Bottom and Sidewall. Ignoring the die entry effects, the die surface can be considered to restrict deformation in the forming sheet where contact has been made and where friction is non-zero. If the friction is large, the forming characteristic is as illustrated in Fig. 28. When the sheet makes contact with the die wall surface, the deformation in that contact area is restricted, and thinning is localized in the noncontact areas, leading to a greater degree of thinning in the last area to contact the die than in the first areas to make contact (Fig. 28). This results in a thickness gradient, as shown in Fig. 29, for a titanium alloy part formed in a die with no lubricating compounds present.

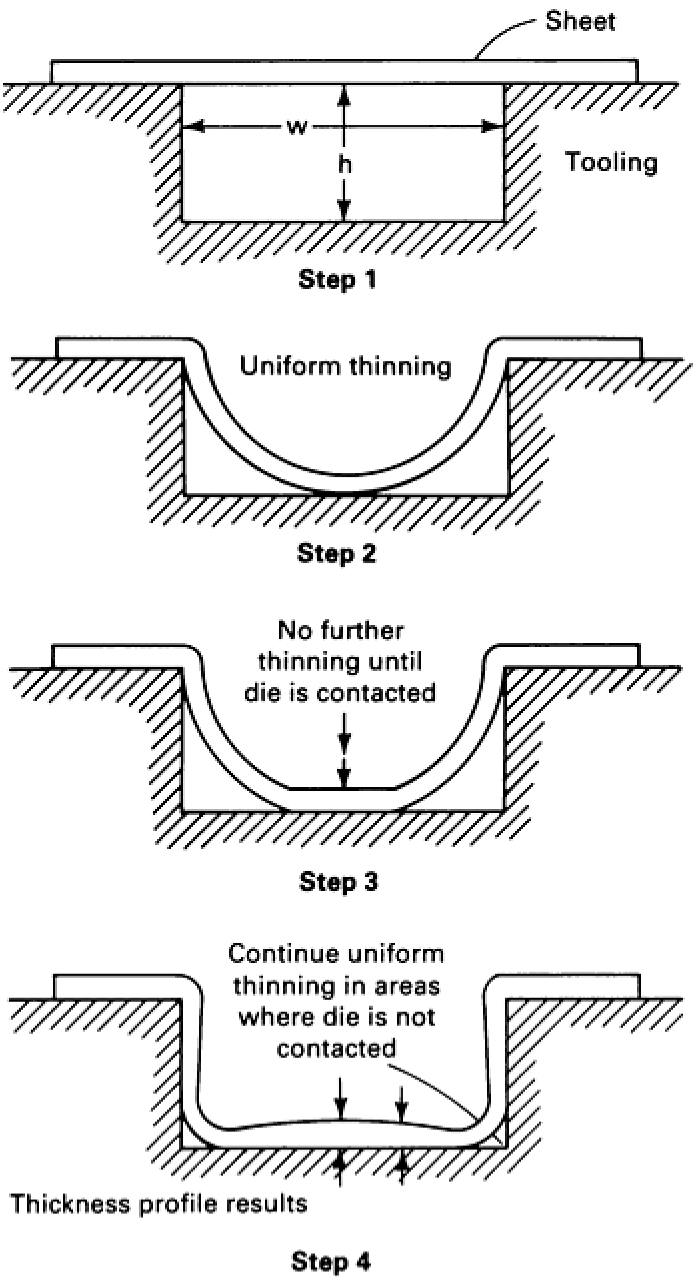


Fig. 28 Illustration of thinning characteristics in the blow forming of an unlubricated part of rectangular cross section and semi-infinite length

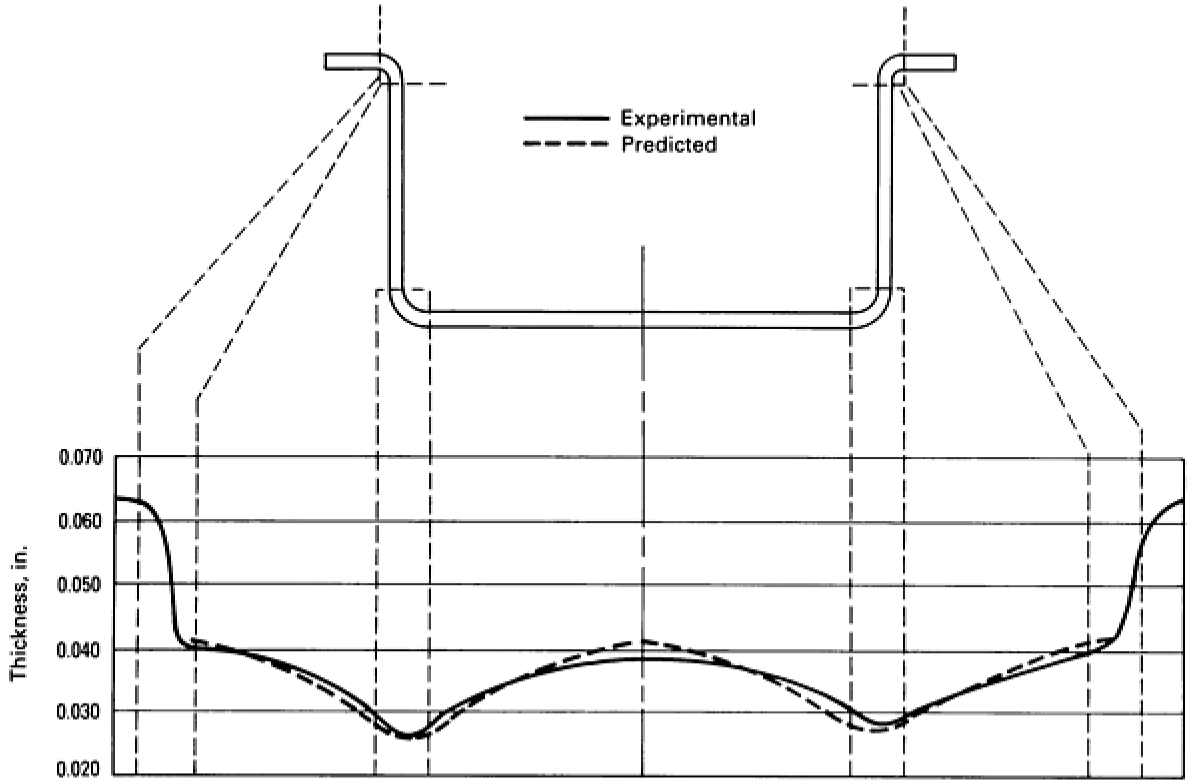


Fig. 29 Observed and predicted thinning profiles in an unlubricated blow-formed Ti-6Al-4V alloy part of rectangular cross section. Forming of the sheet, which had an initial thickness of 1.68 mm (0.066 in.), required 20 min at 925 °C (1700 °F).

This type of thinning is readily predicted analytically if it is assumed that the sheet sticks to the die surface after contact is made by using an incremental method (Ref 27). Results of this type of model show that there are a variety of thinning variations corresponding to various width and depth ratios of the rectangular shape, as shown in Fig. 30. It is apparent from Fig. 30 that the narrow and deep parts develop the greatest amount of thinning. In this specific case, the thickness profiles can be predicted quite well without referring to the strain rate sensitivity, *m*. This is the result of the dominant effect of the die friction coupled with the uniform initial stress state.

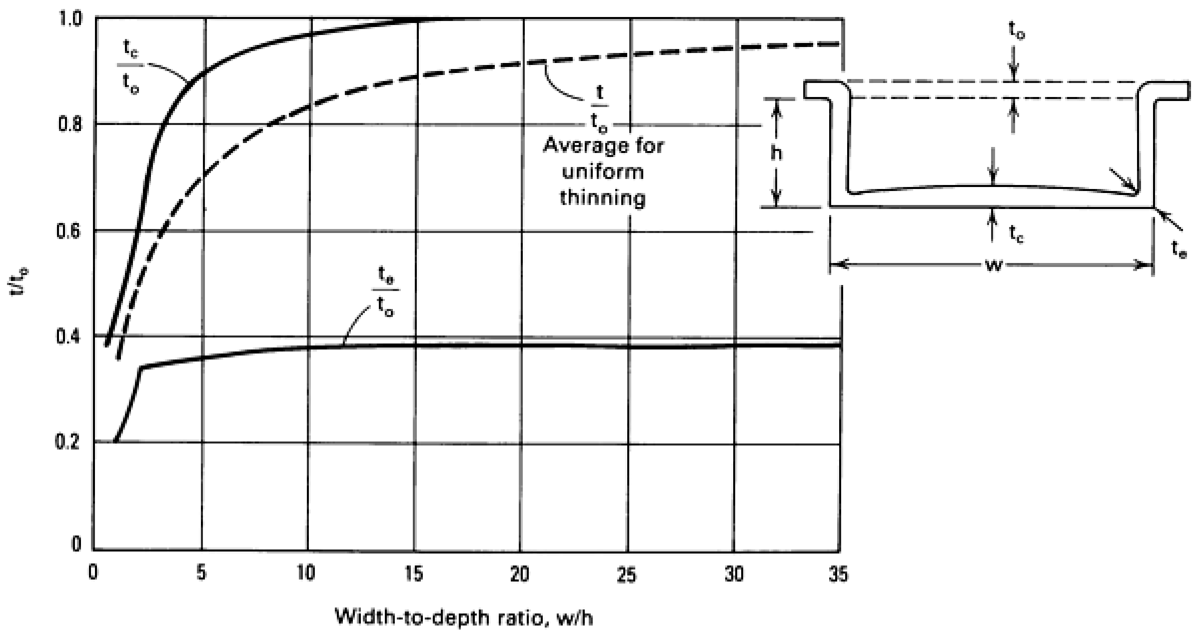


Fig. 30 Predicted minimum thicknesses as function of the width-to-depth (*w/h*) ratio for unlubricated blow-forming part of rectangular cross section.

If the interfacial friction is reduced, the thinning gradient will be reduced in the sidewall and bottom areas because continued deformation after die contact is possible. An example of the thinning in a formed rectangular titanium part is shown in Fig. 31, for which forming was conducted with a boron nitride solid lubricant (Ref 22, 27).

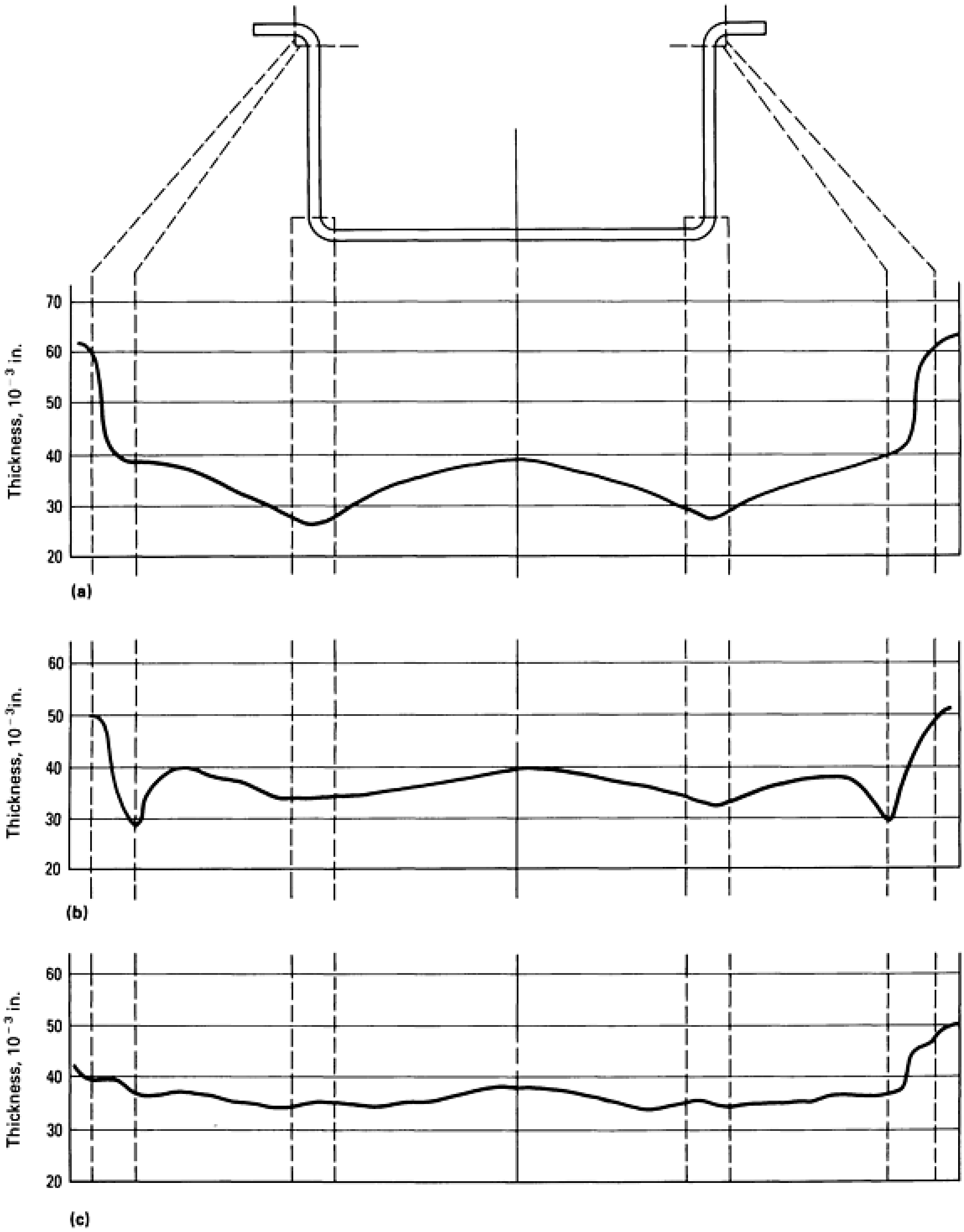


Fig. 31 Observed thickness profiles for 1.68 mm (0.066 in.) thick Ti-6Al-4V blow-formed parts of rectangular cross section formed under different average strain rates, $\bar{\dot{\varepsilon}}_t$, and with different lubrication conditions. (a) $\bar{\dot{\varepsilon}}_t$ = -7 × 10^{-4} s^{-1} no lubricant. (b) $\bar{\dot{\varepsilon}}_t$ = -5.6 × 10^{-4} s^{-1}; boron nitride lubricant. (c) $\bar{\dot{\varepsilon}}_t$ = -5.4 × 10^{-5} s^{-1}; boron nitride lubricant

A die entry radius causes a local stress concentration in the forming sheet, which then creates a stress state gradient in the forming sheet, and this can lead to localized thinning, especially if the ratio of die radius to sheet thickness is small and if the surface is lubricated. The source of the stress concentration is the back pressure exerted by the die radius on the forming sheet and the gas pressure on the opposite side of the sheet from the die. The pressure exerted by the die radius has been shown to be (Ref 27):

$$P_r = \frac{\sigma_w h}{R_1} \qquad \textbf{(Eq 12)}$$

where P_r is the pressure of the die entry radius, σ_w is the in-sheet stress in the width direction, h is the sheet thickness, and R_1 is the die entry radius. This and the applied gas pressure, g, develop an average through-thickness stress σ_h of:

$$\sigma_h = \frac{g + P_r}{2} \qquad \textbf{(Eq 13)}$$

The magnitude of σ_w will be dependent on the local friction coefficient, μ_r, and the position on the radius, so that the effective stress will vary around the die entry radius. A detailed analytical model of this somewhat complex condition is available in Ref 27, but it has been shown that a local stress increase is developed in this area, causing a tendency toward local thinning. If the friction is sufficiently low, the initially thinned section can continue to thin after die contact is made. Therefore, significant localized thinning can occur, and even rupture may take place if the conditions are sufficiently severe.

Thinning over the die entry radius is the result of stress gradients; therefore, the strain rate sensitivity *m* is an important parameter in determining the extent of thinning that will develop. The influence of these variables is illustrated in Fig. 32 for a titanium alloy part formed under the indicated conditions of lubrication and strain rate. The strain rate variations resulted in corresponding variations in the *m* value during the respective forming process. The thinning for the unlubricated part is in agreement with that expected from the discussion in the previous section in this article. For this case where lubricant is used, the strain rate, which determines the corresponding *m* value, is a factor in determining the extent of thinning over the die entry radius. The average *m* value corresponding to the die entry radius was higher for the forming process that developed the lower average strain rate, resulting in a significantly reduced tendency toward local thinning in that area.

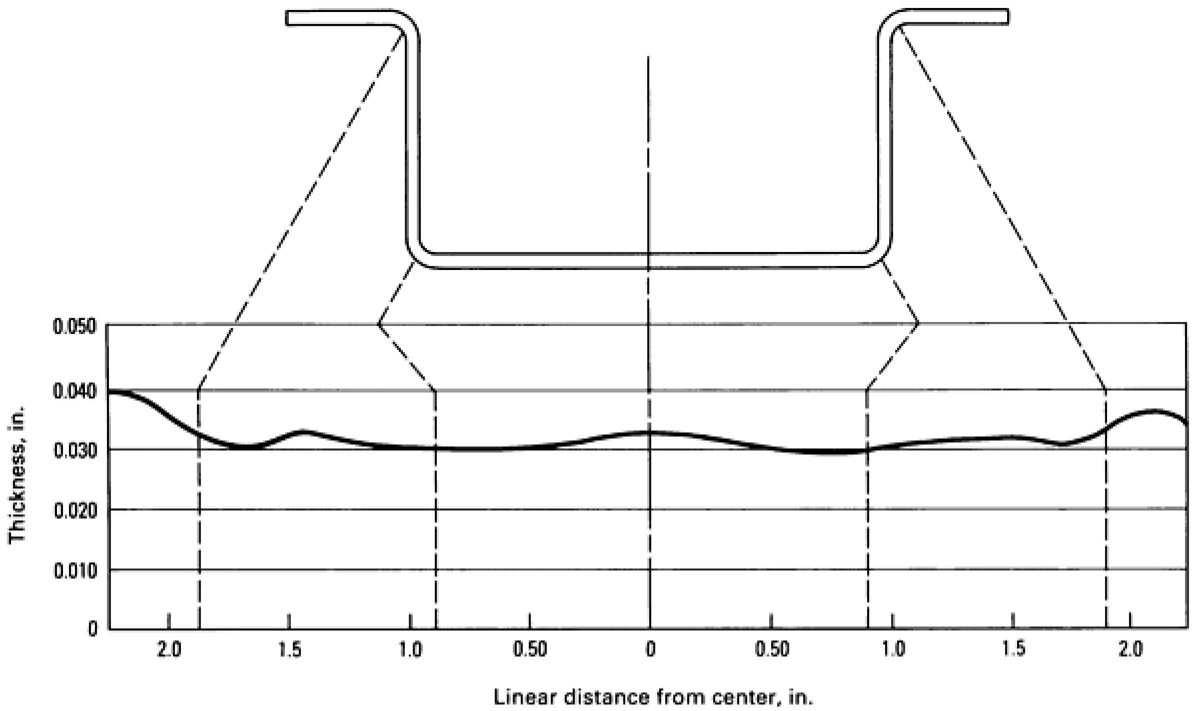


Fig. 32 Observed thickness distribution for Ti-6Al-4V parts with 1.14 mm (0.045 in.) initial thickness at 927 °C (1700 °F) formed at 10^{-3} s^{-1} with boron nitride lubrication

Thinning Control. Because SPF parts are typically stretched to very large elongations, the thickness variations are potentially large for a part. Therefore, it is often important to control the thickness variations in order to meet part tolerance requirements. Although it is seldom possible to prevent thickness variations, there are techniques that can be utilized to control this problem. In addition to such methods, the designer can often accommodate variations in thickness if he knows in advance what they might be. This latter approach is an important and viable one, but will not be addressed in this article because it is a very specialized field of metallurgy.

The methods used to control thinning are:

- Processing of the superplastic material to achieve a high *m* value
- Use of surface lubrication, as discussed above
- Use of thermo-forming methods to control the localized deformation
- Modification of the die or part design to minimize local stress concentrations
- Forming a thickness-profiled sheet
- Application of pressure in a controlled and profiled manner to control strain rate to a value corresponding to a high *m* value

Because the raw sheet material is generally obtained from a commercial supplier, the material superplastic properties are under the control of the mill. However, it may be judicious for the forming plant to obtain material under the control of an appropriate specification. The effect of lubrication is discussed in the section "Die Entry Radius" in this article, along with the effect of the die entry radius, which may, in some parts, be increased to minimize the thickness gradients.

The thermo-forming method has been shown to offer effective techniques that can control the thinning gradients in single-pocketed deep-drawn parts (Ref 24, 30). With these methods, a movable tool is usually used to contact the forming sheet before the finished shape is produced, causing the local friction to minimize deformation in some locations while free-forming sections continue to deform.

The use of thermo-forming techniques was demonstrated using apparatuses such as those shown in Fig. 33 and 34 (Ref 30). The test rig used consisted of two cylindrical chambers, each 190 mm (7.5 in.) in inside diameter by 178 mm (7 in.) deep, and a hydraulic ram, positioned in the bottom chamber, that was capable of moving up and down. The material used was Zn-22Al-1.5Cu sheet 1.27 mm (0.050 in.) thick. For the convex upward die, the deformation was restricted in the center of the sheet and concentrated at the outer area, resulting in a strain and thickness profile, as shown in Fig. 35, in which the top center is thicker than the adjacent areas. This thickness profile was substantially modified by the use of a concave upward die, as shown in the Fig. 36. In this case, the superplastic diaphragm was formed down into the concave die by gas pressure, and the die was slowly withdrawn until it reached the bottom. The preformed diaphragm was then formed into the upper cylindrical chamber in the same manner as that of the previous figure. The resulting profile is seen to be considerably more uniform across the top of the part.

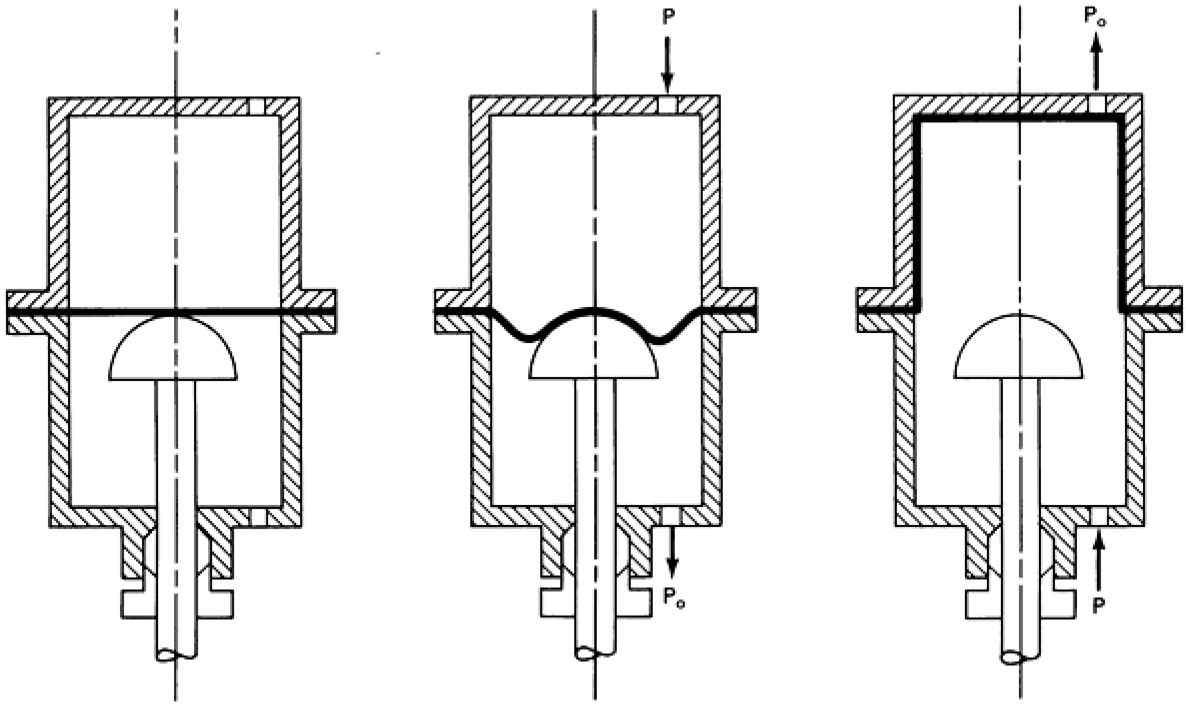


Fig. 33 Apparatus for thermo-forming superplastic sheet materials using a convex die member to control thinning in forming of a hat configuration

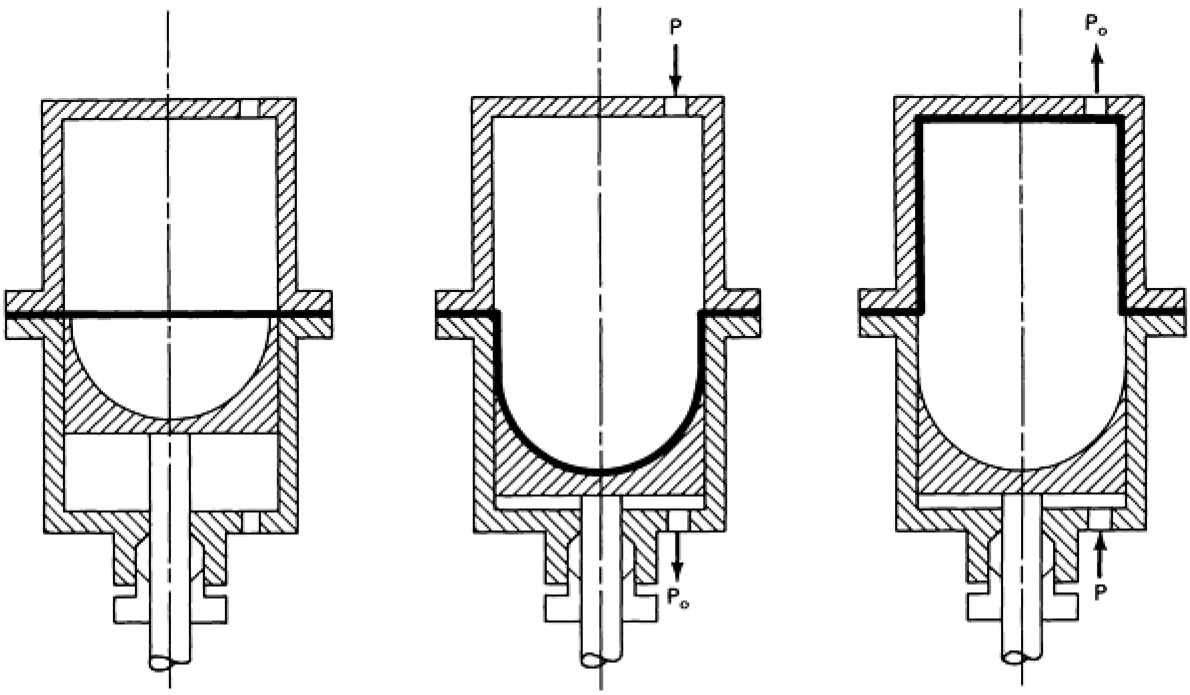


Fig. 34 Apparatus for thermo-forming superplastic sheet materials using a concave die member to control thinning in forming of a hat configuration

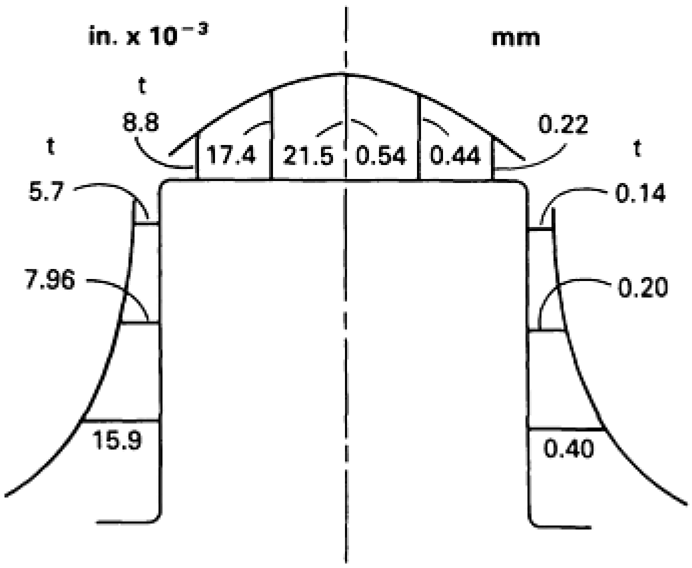


Fig. 35 Thickness profile for hat configuration formed with a convex die member, as shown in Fig. 33. Material formed is Zn-22Al-0.15Cu at a forming temperature of 250 °C (480 °F).

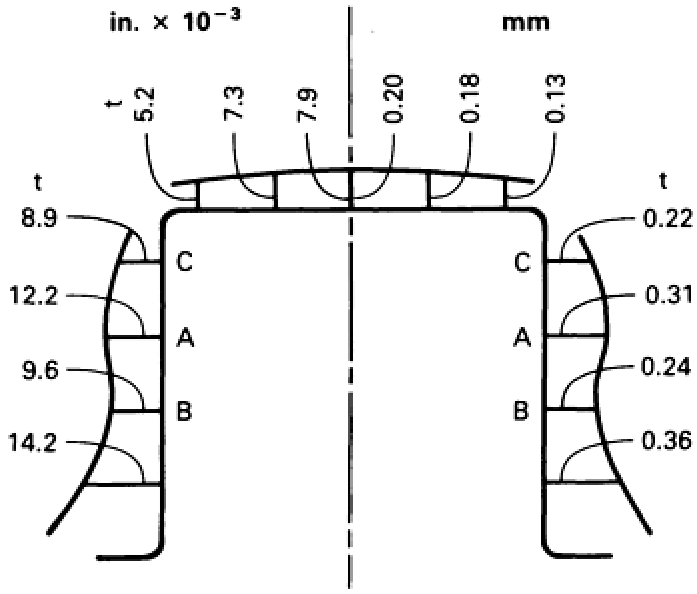


Fig. 36 Thickness profile for hat configuration formed with a concave die member, as shown in Fig. 34. Material formed is Zn-22Al-0.1Cu at a forming temperature of 250 °C (480 °F).

The use of thickness-profiled sheet has been suggested to control the thickness in the final part (Ref 44). The concept considers that the initial thickness variations can be used to offset the subsequent variations resulting from the stress state and part geometry effects on the thinning, as shown in Fig. 37. If areas that will thin excessively are thicker than surrounding areas, it is possible to develop finished part thickness profiles that are more uniform than those formed of constant thickness sheet.

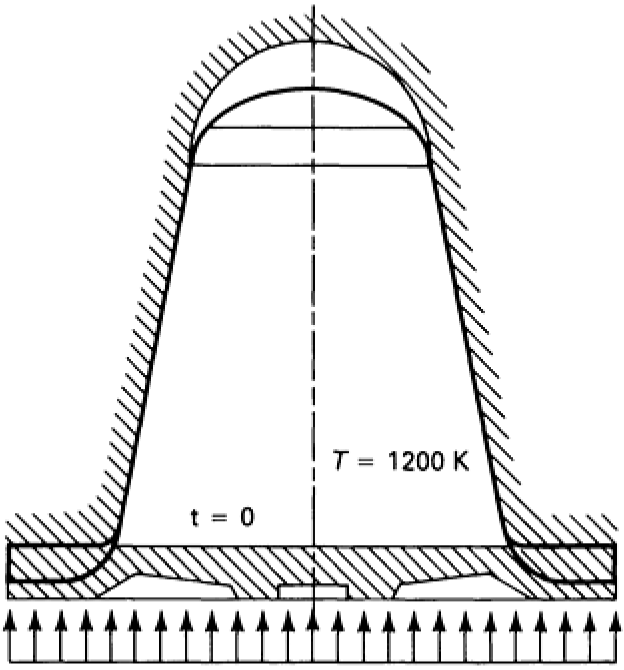


Fig. 37 Schematic of the concept of forming a sheet material that has a thickness profile before forming

Pressure Profiling. It is now well recognized that the *m* value for superplastic alloys will vary with strain rate and that it will also often vary with strain. The strain rate imposed during the forming process will therefore determine the *m* value, and if the strain rate varies during the forming process, the corresponding *m* value and related thinning uniformity will also vary. For example, the simplest pressurization concept for SPF processing is that of constant pressure. The resulting strain rate variation for a spherical dome part configuration has been shown to be as much as three orders of magnitude (Ref 25). A graph of the predicted strain rate for a spherical dome that will be generated under constant pressure for such a part is shown in Fig. 38. The strain rate drops to 0.001 of the initial value when the part becomes a hemisphere. The *m* value for a typical superplastic alloy can vary from a maximum *m* to a value of less than 0.2 over strain rate ranges of this magnitude. The consequence of forming a part under these conditions is that excessive thinning or part rupture during forming is likely. The initial strain rate corresponds to a large *m* value, and good thinning resistance is observed. However, this would be transient; other strain rates would be encountered corresponding to low *m* values, and poor resistance to localized thinning would be present.

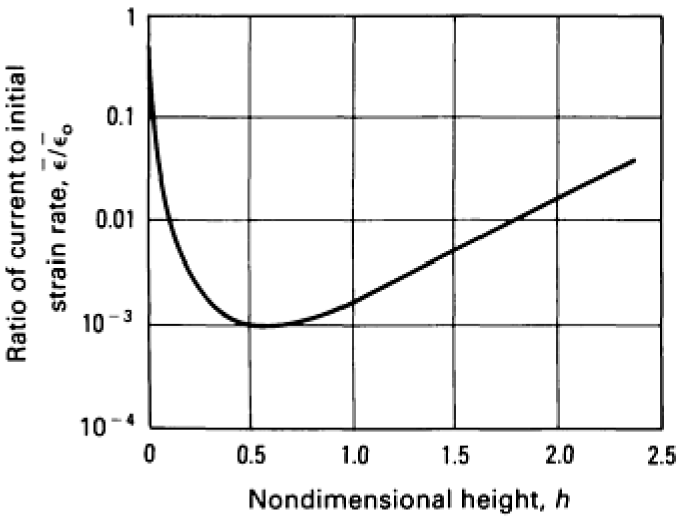


Fig. 38 Ratio of the current to initial strain rate as a function of nondimensional height for a constant pressure

application in the forming of a hemispherical configuration

This condition can be rectified if the strain rate can be maintained at a constant level corresponding to a suitably high *m* value. Because the constant pressure is seen to develop a variable strain rate, it is apparent that a variable forming pressure would be required to develop a constant strain rate. Such pressure profiles have been established analytically for the spherical dome (Ref 25, 26) and the rectangular (Ref 27) part configurations. Because most of the analytical models of the SPF process use applied gas pressure to establish the current stress and strain rate conditions, it is possible to utilize these same models to adjust the current gas pressure to develop the desired stress and strain rate.

The resulting pressure profiles for the constant strain rate forming of the spherical dome and the rectangular parts are illustrated in Fig. 39 and 40, respectively. It is typical that the pressure initially rises rapidly, followed by decrease. The rapid initial rise is due to a rapid decrease in the radius of curvature with little change in thickness, and the subsequent decrease is the result of thinning that is more rapid at this stage than the change in radius of curvature. The depth to which a part is formed will also affect the pressure profile for constant strain rate control, as shown in Fig. 41 for a rectangular part formed with no lubrication. For a deep part (w = 305 mm, or 12 in.), the applied pressure never reaches a maximum, but continues to rise during the forming process. For a shallow part (w = 50 mm, or 2 in.), the pressure is decreased for a significant time period before being increased to high levels.

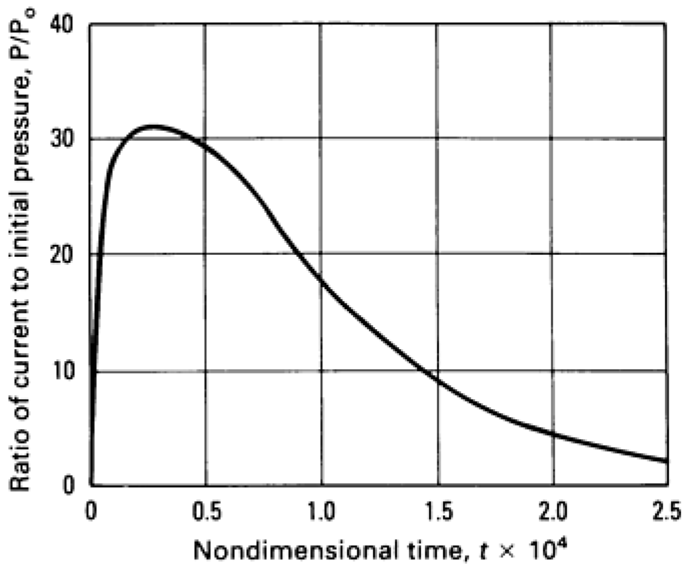


Fig. 39 Ratio of current to initial pressure as a function of a time parameter for forming a spherical configuration under constant strain rate conditions

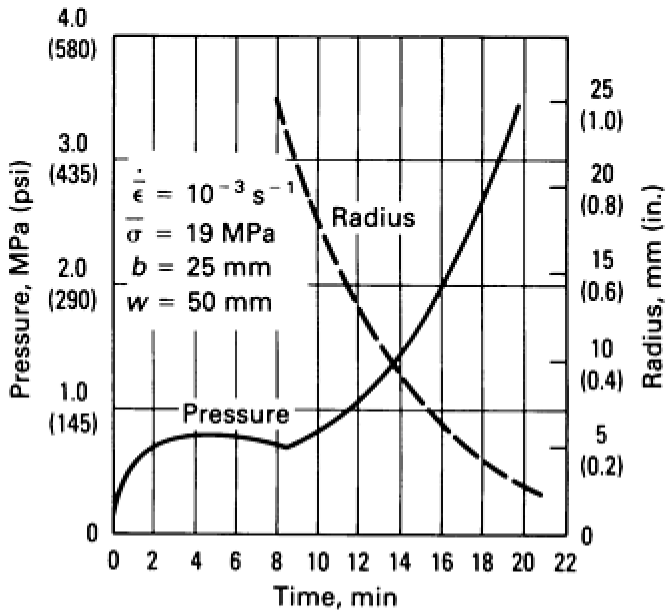


Fig. 40 Analytically predicted pressure versus time for a constant strain rate in the forming of a long, rectangular, 1.27 mm (0.050 in.) thick Ti-6Al-4V part fabricated at 870 °C (1600 °F)

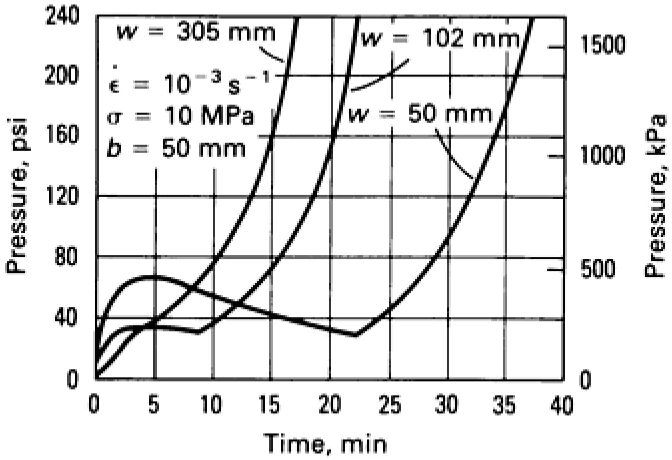


Fig. 41 Analytically predicted pressure profiles for the constant strain rate forming of long rectangular parts of different cross section width, *b,* and height, *w,* dimensions

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Superplastic Sheet Forming

C.H. Hamilton, Washington State University; A.K. Ghosh, Rockwell International

Cavitation and Cavitation Control

Most superplastic alloys tend to form voids, or cavities, at intergranular locations during the superplastic deformation. This process is termed cavitation. Cavitation can lead to the degradation of strength and other design properties, and it is dealt with in one of two ways:

- Establishing reduced design properties
- Utilizing a back pressure technique to control cavitation

Typical cavitation as a function of strain is shown for an aluminum alloy in Fig. 42. It can be seen that the absolute amount of cavitation in terms of the volume fraction is not large but depends on the strain imposed. The use of the back pressure concept imposes a hydrostatic pressure on the sheet during forming, and if this pressure is of the order of the flow stress, cavitation can be reduced or completely suppressed. An example of the effect of back pressure on the rate of development of cavitation is shown in Fig. 43.

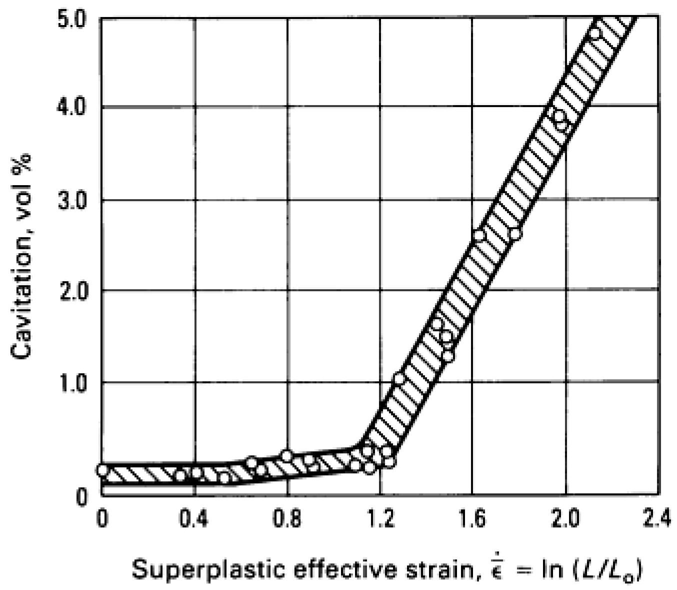

Fig. 42 Development of cavitation with uniaxial tensile strain in a 7475 aluminum alloy specimen of 0.8 cm^2 ($\frac{1}{8}$ in.^{2}) cross-sectional area deformed at 516 °C (961 °F) under a constant strain rate of 2 × 10^{-4} s^{-1}

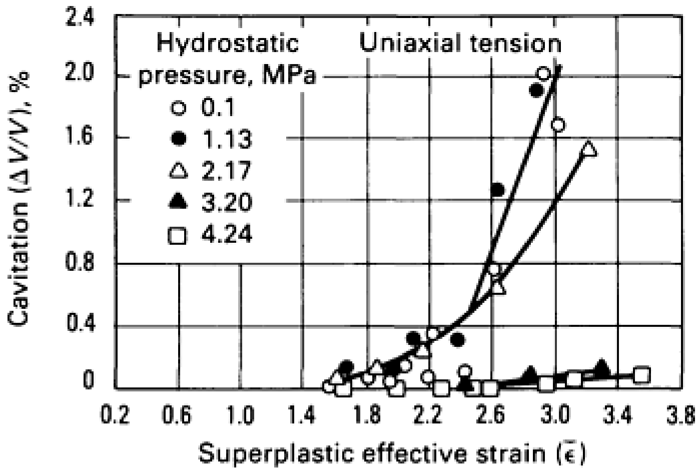

Fig. 43 Effect of hydrostatic pressure on the suppression of cavitation in a 7475 aluminum alloy specimen superplastically deformed at 516 °C (961 °F)

In practice, the back pressure is achieved by imposing a pressure on the back side of the sheet to oppose the forming pressure and by sustaining this pressure during the forming cycle. The forming pressure must be higher than the back pressure, and the same forming rates can be achieved with or without back pressure if the pressure differential is the same. For example, if a pressure profile is desired, such as that shown in Fig. 39 or 40, the pressure profile is simply raised in magnitude by an amount equal to the back pressure. Because the back pressure is normally of the order of the material flow properties, pressures of about 690 to 3400 kPa (100 to 500 psi) are generally suitable for suppressing cavitation.

C.H. Hamilton, Washington State University; A.K. Ghosh, Rockwell International

Manufacturing Outlook

The SPF process is unique in terms of the complexity of parts that can be produced and the methods that can be used to shape such a material. A number of processing methods are currently being used, most of which involve significant stretching of sheet material. The high ductility that can be achieved with these types of materials also has a consequence that must be understood and dealt with, namely, thinning gradients. The thinning gradients are a natural consequence of the stress gradients that develop in the various die configurations, and the superplastic property of the strain rate sensitivity of the flow stress then determines the subsequent thinning gradient that will result in the part. Control of the forming process, die configuration, and material characteristics are all factors that can affect the thinning.

The SPF processes are being increasingly used for a wide range of structural and nonstructural applications. The availability of superplastic alloys is considered to be a major impediment to the broader use of these processes, but as the use of the technology increases, more and perhaps better superplastic alloys can be expected to become available.

Superplastic Sheet Forming

C.H. Hamilton, Washington State University; A.K. Ghosh, Rockwell International

Appendix: Superplasticity in Iron-Base Alloys

Oleg D. Sherby, Stanford University; Jeffrey Wadsworth, Lockheed Missiles and Space Company; Robert D. Caligiuri, Failure Analysis Associates

Superplasticity is the ability of certain polycrystalline metallic materials to extend plastically to large strains when deformed in tension. Strains to failure in superplastic materials range from several hundred to several thousand percent. In general, superplastic materials also exhibit low resistance to plastic flow in specific temperature and strain rate regions. These characteristics of high plasticity and low strength are ideal for the manufacturer who needs to fabricate a material into a complex but sound body with a minimum expenditure of energy.

The phenomenon of superplasticity was first observed over 70 years ago (Ref 48), but active research in this field did not begin until 1962 (Ref 49). Since that time, superplastic effects have been reported in over 100 alloy systems (Ref 49). The micro-mechanisms that permit extraordinary elongations in these materials are still under investigation (Ref 50), but it is generally accepted that a major microstructural requirement for superplasticity is the development of a stable, very fine grain size (typically <10 μm, or 400 μin.) (Ref 51). This requirement also usually (but not always) leads to the necessity for a uniform distribution of a fine, second phase to inhibit matrix grain growth. Furthermore, the grain boundaries should be high angled (that is, disordered), equiaxed, mobile, and must resist separation under tensile stresses (Ref 52). To prevent cavitation around the second-phase particles, the strengths of the two phases should be similar at the temperature of deformation (Ref 52). These micro-structural characteristics can lead to a flow stress that is highly sensitive to changes in strain rate, which in turn resists the formation of instabilities during tensile deformation.

Even though superplasticity has been observed in many metallic alloy systems, only a relatively small number of them are iron-base. This is because of the difficulty in generating microstructures in these systems with the above characteristics. As a result, there have been relatively few industrial applications involving the superplastic bulk forming of ferrous alloys. Those iron-base systems in which the microstructural requirements have been met include:

- Hypoeutectoid and eutectoid plain carbon steels (ferrite-carbide two-phase system)
- Hypereutectoid plain carbon steel (ferrite-carbide and austenite-carbide two-phase systems) and white cast iron (ferrite-carbide two-phase system)
- Low-to-medium alloy steels (ferrite-austenite two-phase system)

- Microduplex stainless steels (ferrite-austenite two-phase system)

Of these, only the hypereutectoid steels have been extensively investigated for applications involving superplastic forming.

The development and existence of superplasticity in all of the above categories of iron-base alloys have been reviewed in detail (Ref 53, 54). This article will describe the key characteristics of superplastic behavior in each of these iron-base systems, with emphasis on hypereutectoid plain carbon steels, which currently have the most industrial potential.

Hypoeutectoid and Eutectoid Plain Carbon Steels

Early attempts at making superplastic plain carbon steels were not particularly successful (Ref 53). In these early efforts, extensive heat treatments were used to produce microstructures of fine ferrite grains pinned by spheroidized cementite particles in compositions containing between 0.2 and 1.0% C. Even though the overall grain structure was correct, elongations of only about 130% were observed, primarily because the grain boundaries were low-angle dislocation boundaries. The flow stress of materials with low-angle grain boundaries is usually not sensitive to changes in strain rate; therefore, superplasticity was not observed. However, later work showed that thermally cycling such materials across the eutectoid transformation temperature after thermomechanical processing changes the grain boundaries from low angle to high angle; tensile elongations of the order of 1000% have been achieved in thermally cycled, hypoeutectoid plain carbon steels (Ref 51).

Hypereutectoid Plain Carbon Steels and White Cast Irons

Hypereutectoid plain carbon steels, also known as ultrahigh-carbon (UHC) steels, will exhibit a microstructure consisting of a continuous network of proeutectoid carbide (at prior-austenite grain boundaries) surrounding pearlite colonies when slowly cooled from a temperature in the single-phase austenite region. This carbide network in turn imparts poor mechanical properties to these steels. However, work conducted in the 1970s showed that, with proper thermomechanical processing, microstructures can be achieved in UHC steels that consist of ultrafine, equiaxed grains of ferrite and a uniform distribution of fine, spherical, discontinuous proeutectoid carbide particles (Ref 55). Such microstructures in UHC steels have led to superplastic behavior; tensile elongations up to 1500% have been reported (Ref 56).

The requisite thermomechanical processing of UHC steels to obtain the desired fine spheroidized microstructure usually involves two steps. In the first step, the UHC ingot or billet is solution heat treated at typically 1150 °C (2100 °F), followed by hot and warm working during cooling down to 750 °C (1380 °F). This step refines the austenite grains, uniformly distributes the proeutectoid carbides at prior-austenite grain and subgrain boundaries, and forms pearlite in the matrix of the prior-austenite grains. The second step incorporates a divorced eutectoid transformation in which the UHC steel is heated to just above the eutectoid transformation temperature and air cooled (with or without accompanying deformation). This step transforms the fine pearlite from step 1 into a fully spheroidized fine structure. The divorced eutectoid transformation is discussed in more detail in Ref 57.

Although not yet fully commercialized, the industrial potential of UHC steels has been thoroughly investigated. One promising application is in the press forging of gears. Figure 44 illustrates a bevel gear that was warm forged from a fine-grain 1.25% C UHC steel in a single operation. An additional advantage of using superplastic UHC steels is that, because of their high carbon content, the carburization step in normal gear production is eliminated. A second example of the superplastic press forming of fine-grain UHC steels is the aft closure for a guided missile shown in Fig. 45. In this case, a 1.6% C steel casting was liquid atomized, and the resulting fine-grain powders were warm pressed into a billet at 800 °C (1470 °F). A third application for superplastically formed fine-grain UHC steels is in the manufacture of die components by superplastic hobbing operations (Ref 58).

Fig. 44 Warm precision forging of a 1.25% C UHC steel billet into a bevel gear. Forging temperature was 650 °C (1200 °F).

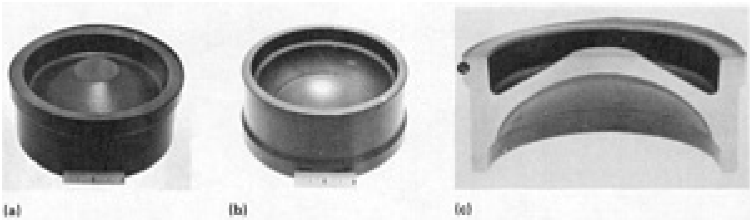

Fig. 45 Aft closure, for a guided missile, that was superplastically formed at 815 °C (1500 °F) from a 1.6% C UHC steel. The processing procedure consisted of warm pressing (800 °C, or 1470 °F) liquid-atomized powders into a billet and forging the resulting billet into plate. The plate was then superplastically formed to the final shape (a and b). (c) Cross section of the part

The original work on UHC steels demonstrated that, given the proper microstructure, superplasticity could be developed over the composition range of about 0.8 to 2.1% C and the temperature range of about 650 to 800 °C (1200 to 1470 °F). Therefore, superplastic UHC steels include both austenite-carbide as well as ferrite-carbide two-phase structures. Ultrahigh-carbon steels are generally not susceptible to cavitation, because the ferrite, austenite, and carbide all have about the same strength in the intermediate temperature range in which superplasticity occurs.

Furthermore, in the original work, superplasticity in UHC steels was limited to intermediate strain rates (that is, forming rates): 10^{-5} to 10^{-3} s^{-1}. The upper limits on strain rate and temperature are primarily related to the destruction of the fine uniform carbide structure. Commercialization of superplastic UHC steels has in fact been hampered by these somewhat limited strain rate and temperature ranges. However, more recent work has shown that these ranges could be substantially extended by careful alloying additions of silicon or aluminum (Ref 59). These elements enhance superplasticity in UHC steels because they:

- Increase the eutectoid transformation temperature (increase the stability of ferrite)
- Inhibit carbide coarsening at high temperatures (increase the activity of carbon in ferrite)
- Increase the volume fraction of proeutectoid carbides
- Do not form active sites for cavitation to occur

Ultrahigh-carbon steels containing 3% Si or 1.6% Al have exhibited superplasticity at strain rates to 10^{-2} s^{-1} at a temperature of 800 °C (1470 °F). Superplasticity has been projected to occur in a UHC 12% Al alloy at strain rates to 3 × 10^{-1} s^{-1} at 950 °C (1740 °F); this would make superplastic forming of UHC steels both economical and feasible for many operations.

An additional benefit of adding aluminum or silicon to UHC steels is a low, fairly constant (14 ± 1.5 MPa, or 2.00 ± 0.22 ksi) flow stress over the temperature range of 750 to 925 °C (1380 to 1700 °F). This suggests that thin-sheet UHC steels containing the proper amounts of aluminum or silicon can be readily blow formed over a wide temperature range.

Hypereutectoid White Cast Irons. Application of rapid solidification processing to UHC steels has also helped to extend the compositional range for superplasticity well into the white cast irons. Such techniques can create very high carbon content powders that, upon annealing at intermediate temperatures (595 to 700 °C, or 1100 to 1290 °F), exhibit the requisite fine microstructure (Ref 60). These powders are then readily consolidated into fully dense compacts at temperatures below the subcritical annealing temperature (A_1) (Ref 61). White cast irons processed in this manner have been shown to exhibit superplasticity at intermediate temperatures; a maximum tensile elongation of 1410% has been observed in a Fe-3.0C-1.5Cr (Ref 62). The shaded region on the iron-carbon phase diagram shown in Fig. 46 illustrates the range of carbon content and temperature over which superplasticity has been documented in fine-grain UHC steels and cast irons.

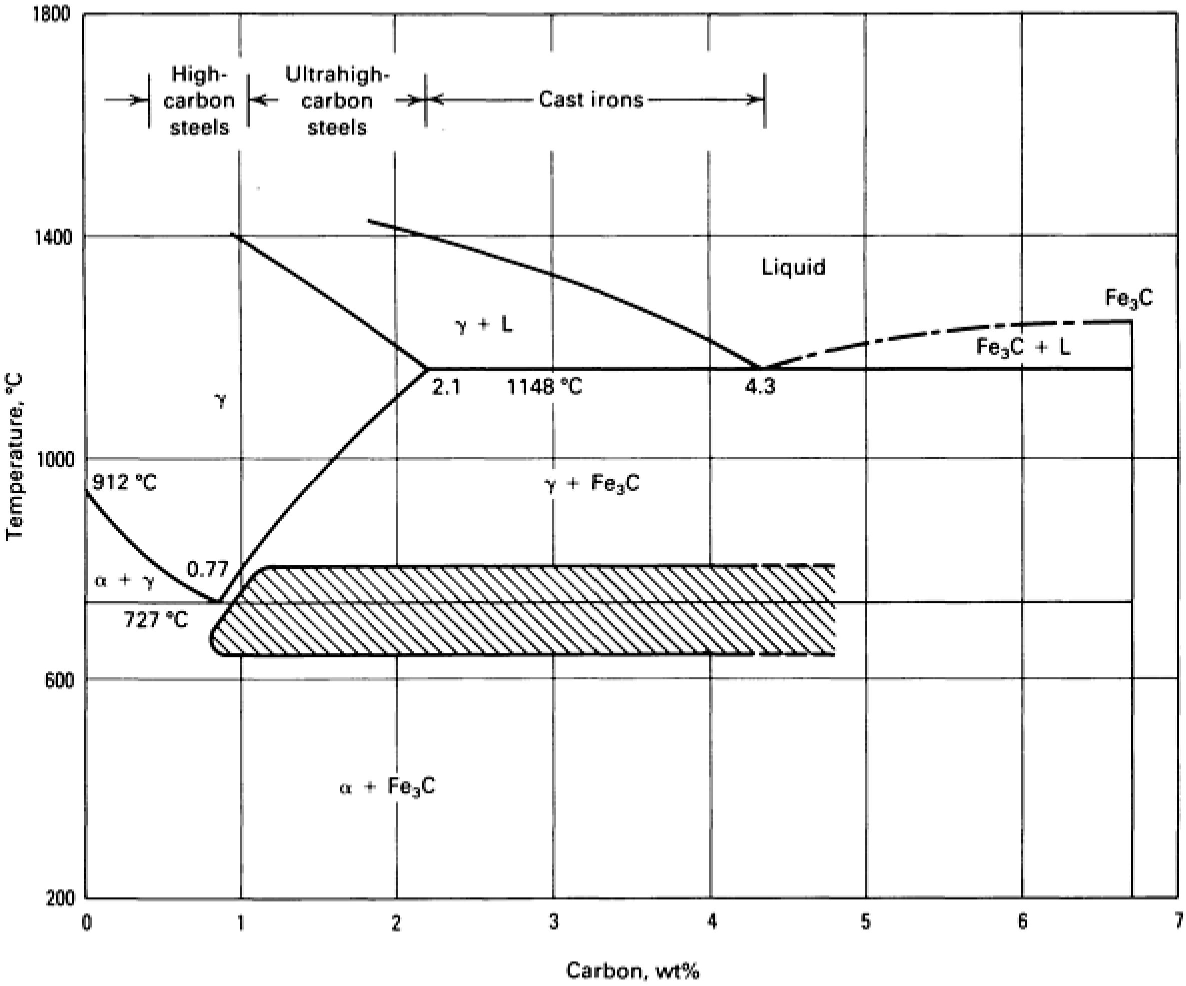

Fig. 46 Iron-carbon phase diagram. The shaded area illustrates the temperature and composition over which UHC steels and white cast irons have been made superplastic.

Hypereutectoid Steel Properties. Because of their carbon content, UHC steels, after superplastic forming, can be heat treated to very high hardness levels (65 to 68 HRC). This can be important in a wide variety of applications, including gears, tool bits, abrasion-resistant surfaces, and military vehicle armor. However, because of their very fine grain size, plain carbon UHC steels also exhibit poor hardenability. This hardenability problem can be alleviated by dilute alloying additions of hardenability-enhancing elements, such as chromium, manganese, and molybdenum. The dramatic effect of alloying additions on the hardenability of UHC steels is shown in Table 2.

Table 2 Hardenability of UHC steels as a function of alloying additions

Material composition	Austenitizing temperature (A_1 + 50)		Critical brine temperature to achieve 62 HRC, T_c		Critical cylinder diameter, D_c		M.F.[a]
	°C	°F	°C	°F	mm	in.	
1.25C-0.5Mn	773	1425	66	151	6.9	0.27	**1.0**
1.25C-0.5Mn-0.2P	795	1465	72	162	10.9	0.43	**1.6**
1.25C-1Cr-0.5Mn-0.25Mo	790	1455	73	163	11.4	0.45	**1.7**
1.25C-3Si-1.4Cr-0.5Mn	825	1520	78	172	15.5	0.61	**2.2**
1.25C-1.6Al-1.5Cr-0.5Mn	860	1580	93	199	22.4	0.88	**3.2**
1.25C-2Mn-1Cr	**795**	**1465**	**>100**	**>212**	**>23.1**	**>0.91**	**>3.3**

(a) M.F., multiplying factor = $D^{c}_{\mathrm{UHC steel alloy}} / D^{c}_{\mathrm{UHC steel}}$

The room-temperature tensile properties of fine-grain UHC steels have been extensively studied (Ref 63). As expected, the properties are very sensitive to heat treatment. Figure 47 compares the tensile properties of UHC steels in two different heat treatment conditions with the properties of other plain carbon and low-alloy structural steels. In addition, if properly heat treated to achieve an extremely fine (optically unresolvable) martensite, fully hardened UHC steels will exhibit a surprisingly high strain to failure in compression. As shown in Fig. 48, a 1.3% C steel that was water quenched from 770 °C (1420 °F) (Steel A) will exhibit a compression fracture strength of 4.5 GPa (650 ksi) and a strain to failure of 10%. However, austenitizing the same steel at 1100 °C (2010 °F) prior to water quenching from 770 °C (1420 °F) (Steel B) will coarsen the resulting martensite and reduce the compression strain to failure to less than 2%.

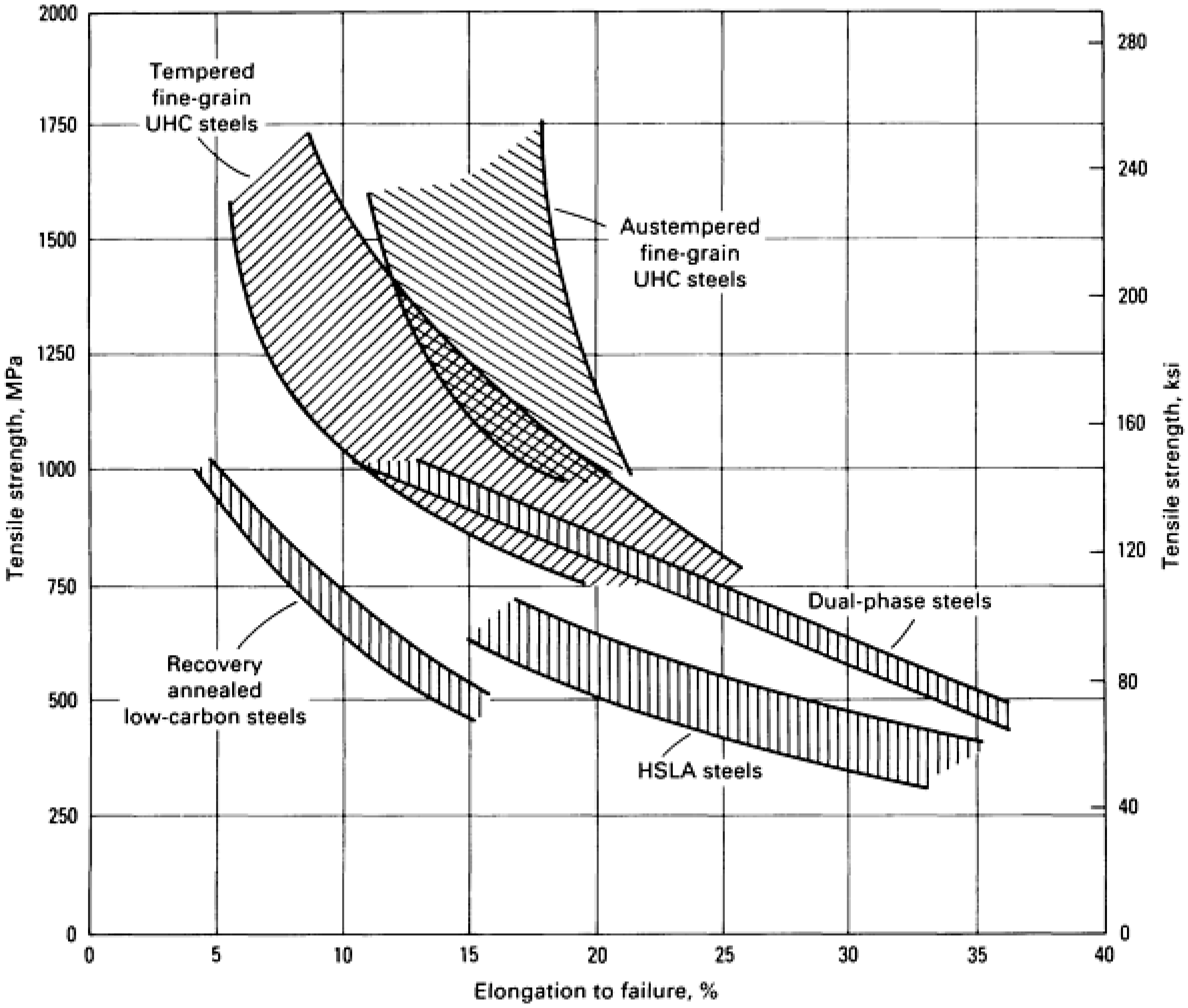

Fig. 47 Tensile strength versus elongation to failure of heat-treated fine-grain UHC steels compared to low-carbon steel, high-strength low-alloy (HSLA) steels, and dual-phase steels

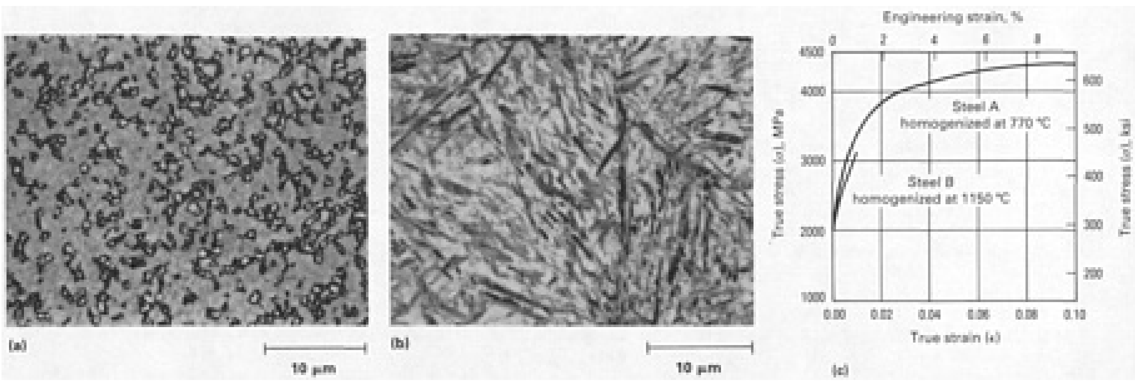

Fig. 48 Influence of prior heat treatment on the microstructure (a and b) and compression stress-strain behavior (c) of a 1.3% C UHC steel quenched from 770 °C (1420 °F)

Because they contain a high volume fraction of carbide, UHC steels exhibit only moderate impact resistance (<25 J, or 18 ft · lbf, Charpy V-notch at 25 °C, or 80 °F). The impact resistance of fine-grain UHC steels can be enhanced by laminating them to tougher (but weaker) materials. Fine-grain UHC steels can be readily solid state bonded to themselves or other ferrous materials, partly because of their superplastic nature (Ref 64). The lamination procedure can consist of conventional roll bonding, press bonding, or explosive techniques as long as a good metallurgical bond results. The effect of such lamination on the impact properties of UHC steels is illustrated in Fig. 49. The impact properties of a UHC steel/1020 steel laminated composite are clearly superior to those of either of the monolithic UHC steel or the monolithic 1020 low-carbon steel. Most UHC steel-base laminates will also exhibit superplasticity over limited temperature and strain rate ranges and therefore can be superplastically formed (Ref 65, 66). The room-temperature tensile strength of a UHC steel laminate will of course be reduced relative to that of a monolithic UHC steel by an amount governed by the rule of mixtures; however, this loss in strength can be partially compensated for by heat treating the laminate to selectively harden the UHC steel component to very high strength levels.

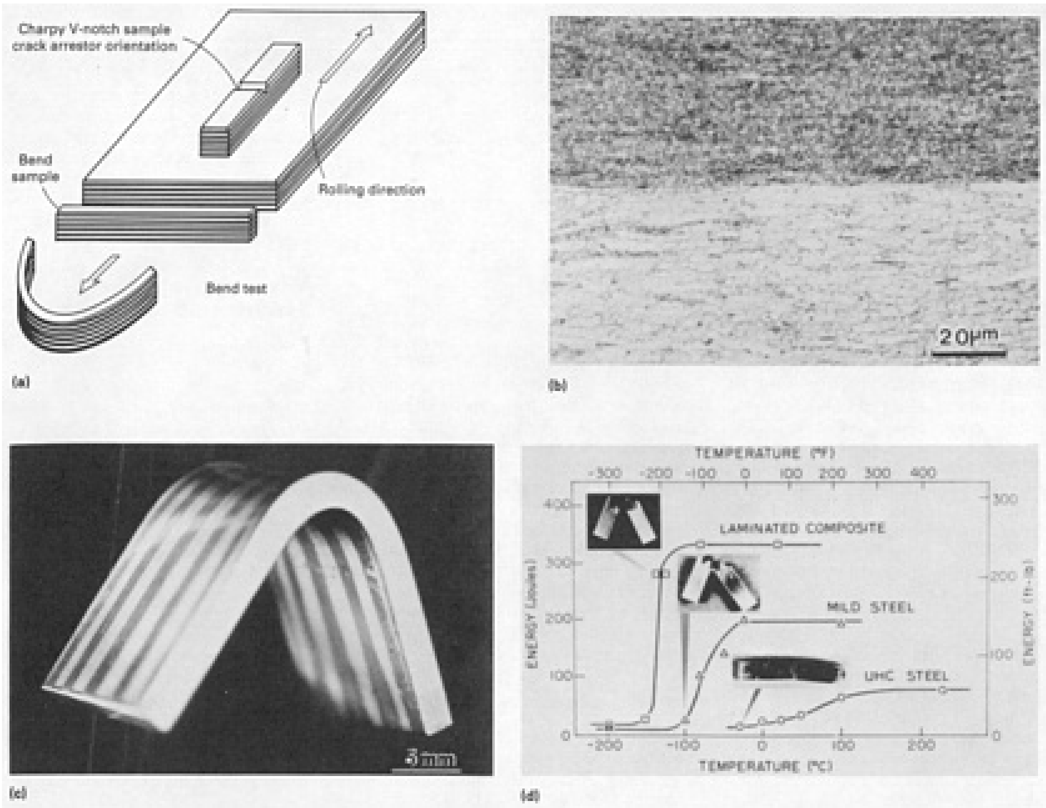

Fig. 49 UHC steel/1020 steel laminated composite to improve impact resistance of fine-grain UHC steels. (a) Orientation of mechanical test samples taken from a laminated composite of UHC steel and 1020 steel. (b) Optical photomicrograph of interface in laminated composite of UHC steel and 1020 showing an excellent metallurgical bond. (c) Bend test sample of the laminated composite. (d) Impact properties of UHC steel, 1020 steel, and UHC steel/1020 steel laminated composite including photographs of the tested samples

Low-to-Medium Alloy Steels

Superplastic studies have been conducted on several alloy steels (Ref 53). Compositions and thermomechanical processing procedures are generally chosen to generate fine-grain (1 to 2 μm, or 40 to 80 μin.) microstructures consisting of roughly equal amounts of ferrite and austenite. These phases will coexist only in a narrow temperature range, but within that range each phase will inhibit the other from growing, and superplasticity will occur. Elongations of 300 to 500% have been achieved in steels containing 1 to 2% Mn and 0.1 to 0.4% C, and elongations of up to 600% have been achieved in Fe-4Ni-3Mo-1-2Ti alloys when tested at temperatures in the dual-phase region. However, the potential of superplastic forming these alloys has not been exploited, because of the narrow temperature range over which superplastic flow occurs and because of the rapid growth that can occur even in the two-phase region at high temperatures.

Microduplex Stainless Steels

Microduplex stainless steels are so called because their stable microstructure consists of both ferrite and austenite in fine grain size form--about 2 to 3 μm (80 to 120 μin.). They typically contain 18 to 26% Cr and 5 to 8% Ni and can also contain molybdenum, titanium, copper, silicon, manganese, carbon, and diatomic nitrogen. Elongations to failure in these alloys are commonly in excess of 500% at strain rates between 10^{-3} and 10^{-2} s^{-1} and at temperatures between 900 and 1000 °C (1650 to 1830 °F) (Ref 66). Although microduplex stainless steels have found a number of commercial applications in the chemical industry because of their high strength and corrosion resistance, their superplastic forming potential has not been exploited. This is because microduplex stainless steels are susceptible to cavitation during superplastic flow due to the strength differential between the ferrite and the austenite at these high temperatures (Ref 67).

Outlook for Superplastic Ferrous Alloys

The number of iron-base alloys exhibiting superplasticity is relatively limited. This is primarily because of the difficulty in satisfying all of the microstructural requirements generally required for superplasticity in ferrous materials. Of the alloys identified to date for possible superplastic forming, the hypereutectoid (or UHC) steels show the most promise. These steels can be stretched without cavitation over 1000% at reasonable forming rates and intermediate temperatures. Ultrahigh-carbon steels are heat treatable to high hardnesses, exhibit good room-temperature mechanical properties, and can be readily laminated to other ferrous materials to enhance toughness and impact resistance. Future efforts in the superplastic forming of iron-base alloys will undoubtedly focus on this class of steels.

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Introduction

SHEET METAL FORMING is the process of converting a flat sheet of metal into a part of desired shape without fracture or excessive localized thinning. The process may be simple, such as a bending operation, or a sequence of very complex operations such as those performed in high-volume stamping plants. In the manufacture of most large stampings, a sheet metal blank is held on its edges by a blankholder ring and is deformed by means of a punch and die. The movement of the blank into the die cavity is controlled by pressure between the upper and lower parts of the blankholder ring.

This control is usually increased by means of one or more sets of drawbeads. These consist of an almost semicylindrical ridge on the upper part of the blankholder and a corresponding groove in the lower part (the positions are sometimes reversed). The drawbeads force the periphery of the blank to bend and unbend as it is pulled into the die; this increases the restraining force considerably. Presses with capacities to 17.8 MN (2000 tonf) are commonly used for the manufacture of large stampings, and presses to 26.7 MN (3000 tonf) are used for heavy-gage parts.

Sheet metal forming operations are so diverse in type, extent, and rate that no single test provides an accurate indication of the formability of a material in all situations. However, knowledge of material properties and careful analysis of the various types of forming involved in making a particular part are indispensable in determining the probability of successful part production and in developing the most efficient process.

Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Types of Forming

Many forming operations are complex, but all consist of combinations or sequences of the basic forming operations--bending, stretching, drawing, and coining (see the Section "Forming Processes for Sheet, Strip, and Plate" in this Volume).

Bending is the most common type of deformation, and it occurs in almost all forming operations. Bending around small radii can lead to splitting in the early stages of a forming process because it localizes strain and prevents its distribution throughout the part. Ideally, strain should be distributed as uniformly as possible to maximize the amount of deformation that can be obtained. Even a slight increase in the radius in a given location can sometimes significantly improve strain distribution. Frequently, designs specify smaller radii than necessary, which results in manufacturing problems and increased costs.

Lubrication is not recommended when bending over a sharp radius, because die friction reduces strain localization by restricting metal movement away from the radius. The orientation of the sheet in relation to the rolling direction can also be important in a bending operation. During rolling, inclusions and other defects become elongated in the rolling direction, producing lines of weakness. When the axis of bending is in this direction, there is a tendency toward splitting along the lines of weakness. This lowers resistance to fracture compared to when the axis is inclined to the rolling direction. Detailed information on sheet metal lubrication is available in the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume.

The outer and inner panels of a part are frequently assembled by bending (hemming) the edges of the outer panel around the inner panel. This requires material that can be easily bent over very small radii. In the absence of other types of deformation, bending produces tensile stresses on the outside surface. These decrease to zero at an interior level known as the neutral axis. These stresses then become compressive on the inside of the bend. They can cause springback (shape distortion) upon removal of the applied forces. If tensile deformation is also present, the compressive stresses may be reversed, but through-thickness stress and strain gradients will generally still exist.

Many forming operations involve pulling metal over a die radius so that it is initially bent and subsequently straightened. The net strain resulting from this process may be quite small, depending on the size of the die radius and the tensile forces involved. However, the bending and straightening process cold works the metal, particularly at the surfaces, and reduces its subsequent formability.

Stretching is caused by tensile stresses in excess of the yield stress. These forces produce biaxial stretching when they are applied in perpendicular directions in the plane of the sheet. Balanced biaxial stretching occurs when the perpendicular forces are equal. Much higher levels of deformation, as measured by an increase in area, can be reached in balanced biaxial stretching than in any other forming mode.

Many forming operations involve stretching of some areas within the stamping. Automotive outer body panels are typical examples of parts formed primarily by stretching. Parts with regions containing domes, ribs, and embossments also involve stretching.

Plane-strain stretching results in elongation in one direction and no dimensional change in the perpendicular direction. It frequently occurs when a wide, flat area of sheet metal is stretched longitudinally--for example, in the sidewall of a stamping. In this case, strain in the transverse direction is prevented by the adjacent metal. Plane-strain stretching is an important type of deformation because most materials fracture at a lower level of strain in plane strain than in any other condition. Many of the fractures that occur in stamping operations are in the plane-strain region.

Drawing of sheet metal causes elongation in one direction and compression in the perpendicular direction. The simplest example is the drawing of a flat-bottomed cylindrical cup. In this process, a circular disk is held between two flat annular dies and impacted in the center by a flat-bottomed punch. This draws (pulls) the edges of the disk inward to form the wall of the cup. The metal is stretched radially by the tensile forces produced by the punch, but it is compressed circumferentially as its diameter decreases. Many other forming operations involve substantial drawing.

Coining occurs when metal is compressed between two die surfaces. It is extensively used for making coins and parts with similar surface features, for flattening, and for reducing springback upon removal of parts from a die. In many stretching and drawing operations, coining is undesirable, because it restricts metal movement, localizes strain, and produces surface damage. Much of the die preparation for these operations concentrates on locating and eliminating coining.

Combinations of Types of Forming. Most forming operations involve combinations of different types of forming. Figure 1 illustrates a part design that requires drawing in the corners; biaxial stretching in the dome; bending, straightening, and plane-strain stretching in the walls; and bending and plane-strain stretching at the tops and bottoms of the walls.

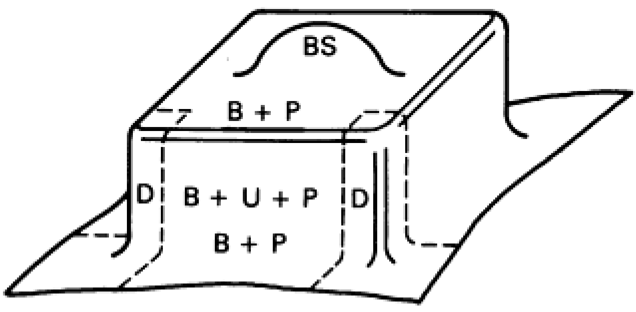

Fig. 1 Part design requiring a combination of types of forming. B, bending; BS, biaxial stretching; D, drawing; P, plane-strain stretching; U, unbending (straightening). Source: Ref 1.

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Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Formability Problems

The major problems encountered in sheet metal forming are fracturing, buckling and wrinkling, shape distortion, loose metal, and undesirable surface textures. The occurrence of any one or a combination of these conditions can render the sheet metal part unusable. The effects of these problems are discussed below.

Fracturing occurs when a sheet metal blank is subjected to stretching or shearing (drawing) forces that exceed the failure limits of the material for a given strain history, strain state, strain rate, and temperature. In stretching, the sheet initially thins uniformly, at least in a local area. Eventually, a point is reached at which deformation concentrates and causes a band of localized thinning known as a neck, which ultimately fractures. The formation of a neck is generally regarded as failure because it produces a visible defect and a structural weakness. Most current formability tests are concerned with fracture occurring in stretching operations.

In shearing, fracture can take place without prior thinning. The most common examples of this type of fracture occur in slitting, blanking, and trimming. In these operations, sheets are sheared by knife-edges that apply forces normal to the plane of the sheet. Shearing failures are sometimes produced in stamping operations by shearing forces in the plane of the sheet, but they are much less common than stretching failures.

Buckling and Wrinkling. In a typical stamping operation, the punch contacts the blank, stretches it, and starts to pull it through the blankholder ring. The edges of the blank are pulled into regions with progressively smaller perimeters. This produces compressive stresses in the circumferential direction. If these stresses reach a critical level characteristic of the material and its thickness, they cause slight undulations known as buckles. Buckles may develop into more pronounced undulations or waves known as wrinkles if the blankholder pressure is not sufficiently high.

This effect can also cause wrinkles in other locations, particularly in regions with abrupt changes in section and in regions where the metal is unsupported or contacted on one side only. In extreme cases, folds and double or triple metal may develop. These may in turn lead to splitting in another location by preventing metal flow or by locking the metal out. Therefore, increasing the blankholder pressure often corrects a splitting problem.

Shape Distortion. In forming operations, metal is deformed elastically and plastically by applied forces. Upon removal of the external forces, the internal elastic stresses relax. In some locations, they can relax completely, with only a very slight change in the dimensions of the part. However, in areas subjected to bending, through-thickness gradients in the elastic stresses will occur; that is, the stresses on the outer surfaces will be different from those on the inner surfaces.

If these stresses are not constrained or "locked in" by the geometry of the part, relaxation will cause a change in the part shape known as shape distortion or springback. Springback can be compensated for in die design for a specific set of material properties, but may still be a problem if there are large material property or process variations from blank to blank.

Loose metal occurs in undeformed regions and is undesirable, because it can be easily deflected. A phenomenon usually referred to as oil canning, in which a local area can be either concave or convex, can also be encountered. In stampings with two or more sharp bends of the same sign in roughly the same direction, such as a pair of feature lines, a tendency exists for the metal between them to be loose because of the difficulty involved in pulling metal across a sharp radius.

It is sometimes possible to avoid the problem by ensuring that the metal is not contacted by both lines at the same time; thus, some stretching can occur before the second line is contacted (see the article "Press Forming of Coated Steel" in this Volume). There is a tendency for loose metal to occur toward the center of large, flat, or slightly curved parts. Increasing the restraining forces on the blank edges usually improves this condition.

Undesirable Surface Textures. Heavily deformed sheet metal, particularly if it is coarse grained, often develops a rough surface texture commonly known as orange peel (see the article "Press Forming of Coated Steel" in this Volume). This is usually unacceptable in parts that are visible in service. Another source of surface problems occurs in metals that have a pronounced yield point elongation, that is, materials that stretch several percent without an increase in load after yielding. In these metals, deformation at low strain levels is concentrated in irregular bands known as Lüders lines (or bands) or stretcher strains.

These defects disappear at moderate and high strain levels. However, almost all parts have some low-strain regions. These defects are unsightly and are not concealed by painting. Aged rimmed steels and some aluminum-magnesium alloys develop severe Lüders lines.

In some cases, zinc-coated steels exhibit surface defects known as spangles. This phenomenon occurs only in hot-dipped products and is caused by the development of a coarse grain size in the galvanic coating, which makes the individual grains clearly visible. This problem can be corrected in the coating process. In addition to the above occurrences, handling damage, dents caused by dirt or slivers in the die, and scoring or galling caused by a rough die surface or inadequate lubrication sometimes produce unacceptable surfaces.

Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Measurement of Deformation

The principal methods of measuring deformation are gage marks, strain gages, optical extensometers, and thickness and shape measurements.

Gage Marks. The most widely used method for measuring deformation is to mark the sheet by etching or scribing or by means of ink, dye, or paint and to measure the changes in the separations of the marks caused by the deformation. Rectangular grid markings and arrays of small-diameter circles (for example, 2.5 mm, or 0.1 in.) are frequently used.

In most production forming operations and in the later stages of tensile testing, deformation varies rapidly with location, which can lead to large differences in strain measurements made over different gage lengths. Therefore, small gage lengths, such as the diameters of small, closely spaced circles, are commonly used. Circular markings provide an additional advantage in that it is easy to identify the directions of the maximum (or major) and minimum (or minor) strains and thus to measure their values. Upon deforming, the circles change into ellipses with their major axes in the direction of the maximum strain and their minor axes in the direction of the minimum strain.

This information is essential in determining how close the local strain state is to the maximum the material can withstand without fracturing, which depends on the ratio of the strains. It is also useful in determining how the geometry of a die must be modified when the formability limits of the work material are exceeded.

Strain Gages and Extensometers. In some cases, a strain gage or a strain gage extensometer is attached to the sheet or test sample. Accurate strain measurements are thus obtained continuously during a forming operation or test. Optical extensometers, which are particularly effective at high strain rates, can also be used.

Thickness and Shape Measurements. Thickness measurements, which can be made rapidly by ultrasonic methods, can sometimes be used to determine strains. In practice, this method is limited to situations in which the ratio of the major and minor strains is known from previous measurements, because many different combinations of strains can lead to the same change in thickness.

Part shape is measured by using templates, checking fixtures, or shadowgraphs or by using a profile meter that employs a stylus to contact the surface. Profile meters may give two- or three-dimensional digital representations of the part. Noncontacting surface digitizers and systems for measuring deformation by locating grid markings in three dimensions are also used.

Brian Taylor, General Motors Corporation

Representation of Strain

The most common method of representing strain defines the engineering strain, *e*, as the ratio of the change in length, ΔL, to the original length, L_o:

$$e = \frac{\Delta L}{L_o} = \frac{L - L_o}{L_o} = \frac{L}{L_o} - 1 \qquad \textbf{(Eq 1)}$$

The second method defines the true strain, ε, in the region of uniform elongation as the integral of the incremental change in length, *dL*, divided by the actual (instantaneous) length, *L*:

$$\epsilon = \int_{L_o}^{L} \frac{dL}{L} = \ln\left(\frac{L}{L_o}\right) = \ln(1 + e) \qquad \textbf{(Eq 2)}$$

The engineering strain is easier to calculate and is satisfactory for many applications. The true strain is used in the theoretical analysis of formability and is advantageous in that successive strains can be added to give the cumulative strain.

The strain state of a deformed sheet is frequently represented graphically by plotting the maximum or major strain, e_1, on the vertical axis and the minimum or minor strain, e_2, which can be positive or negative, on the horizontal axis. This is illustrated in Fig. 2, which shows five strain paths, each leading to the same major strain of 40% but with minor strains ranging from -40 to +40%. The ellipses shown were originally circles (shown dashed) in the undeformed sheet.

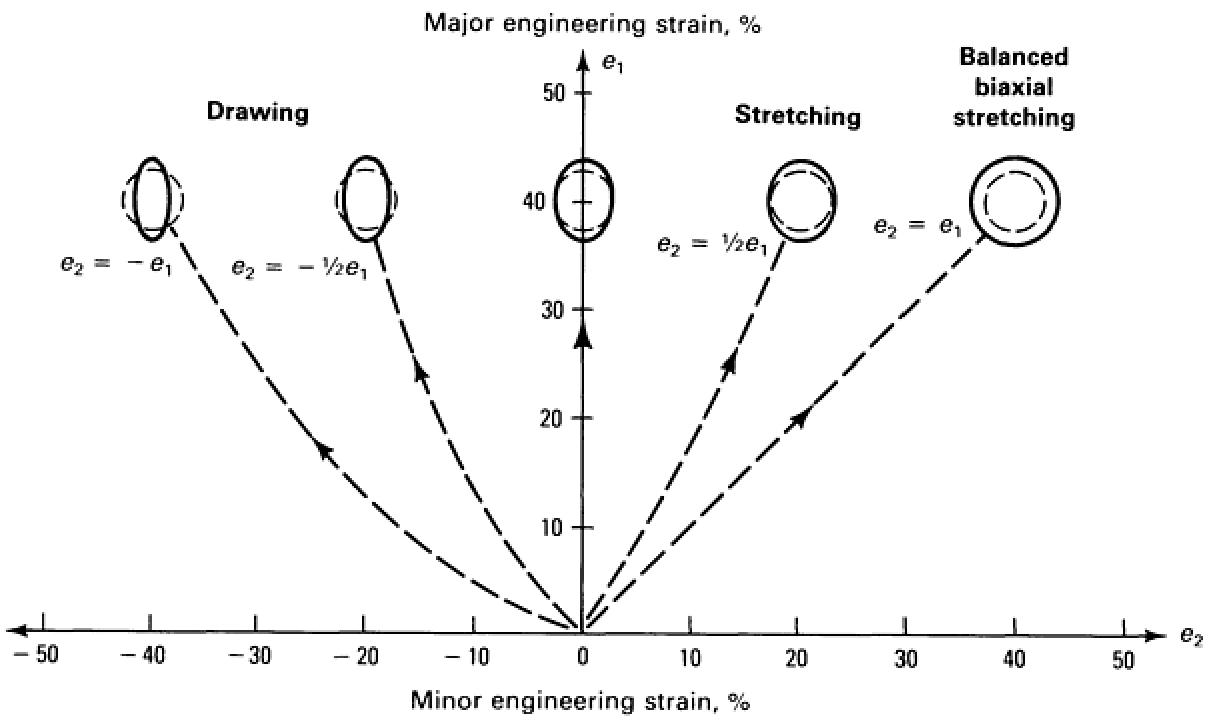

Fig. 2 Schematic of several major strain/minor strain combinations.

On the right side of Fig. 2, the circles have transformed into ellipses that are larger in all directions than the original circles. This is the region of biaxial stretching and, in the diagonal (45°) direction, of balanced biaxial stretching. In this direction, the circles have expanded without changing shape.

On the left side of Fig. 2, the circles have transformed into ellipses, which are larger in one direction but smaller in the perpendicular direction than the original circles. This is the region of drawing and is the strain state developed in the tensile test. On the vertical axis, the ellipses are larger in one direction, but unchanged dimensionally from the original circles in the perpendicular direction. This is the region of plane strain.

Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Effect of Material Properties on Formability

The properties of sheet metals vary considerably, depending on the base metal (steel, aluminum, copper, and so on), alloying elements present, processing, heat treatment, gage, and level of cold work (see the Sections "Forming of Stainless Steel and Heat-Resistant Alloy Sheet Materials" and "Forming of Nonferrous Sheet Materials" in this Volume). In selecting material for a particular application, a compromise usually must be made between the functional properties required in the part and the forming properties of the available materials. For optimal formability in a wide range of applications, the work material should:

- Distribute strain uniformly
- Reach high strain levels without necking or fracturing
- Withstand in-plane compressive stresses without wrinkling
- Withstand in-plane shear stresses without fracturing
- Retain part shape upon removal from the die
- Retain a smooth surface and resist surface damage

Some production processes can be successfully operated only when the forming properties of the work material are within a narrow range. More frequently, the process can be adjusted to accommodate shifts in work material properties from one range to another, although sometimes at the cost of lower production and higher material waste. Some processes can be successfully operated using work material that has a wide range of properties. In general, consistency in the forming properties of the work material is an important factor in producing a high output of dimensionally accurate parts.

Strain Distribution

Three material properties determine the strain distribution in a forming operation:

- The strain-hardening coefficient (also known as the work-hardening coefficient or exponent) or n value
- The strain rate sensitivity or m value
- The plastic strain ratio (anisotropy factor) or r value

The ability to distribute strain evenly depends on the n value and the m value. The ability to reach high overall strain levels depends on many factors, such as the base material, alloying elements, temper, n value, m value, r value, thickness, uniformity, and freedom from defects and inclusions.

The *n* value, or strain-hardening coefficient, is determined by the dependence of the flow (yield) stress on the level of strain. In materials with a high *n* value, the flow stress increases rapidly with strain. This tends to distribute further strain to regions of lower strain and flow stress. A high *n* value is also an indication of good formability in a stretching operation.

In the region of uniform elongation, the *n* value is defined as:

$$n = \frac{d \ln \sigma_T}{d \ln \epsilon} \quad \textbf{(Eq 3)}$$

where σ_T is the true stress (load/instantaneous area). This relationship implies that the true stress-strain curve of the material can be approximated by a power law constitutive equation proposed in Ref 2:

$$\sigma_T = k\varepsilon^n \quad \textbf{(Eq 4)}$$

where *k* is a constant known as the strength coefficient.

Equation 4 provides a good approximation for most steels, but is not very accurate for dual-phase steels and some aluminum alloys. For these materials, two or three *n* values may need to be calculated for the low, intermediate, and high strain regions.

When Eq 4 is an accurate representation of material behavior, $n = \ln (1 + e_u)$, where e_u is the uniform elongation, or elongation at maximum load in a tensile test. By definition, $\ln (1 + e_u)$ is identical to ε_u which is the true strain at uniform elongation.

Most steels with yield strengths below 345 MPa (50 ksi) and many aluminum alloys have *n* values ranging from 0.2 to 0.3. For many higher yield strength steels, *n* is given by the relationship (Ref 3):

$$n \simeq \frac{70}{\text{(yield strength in MPa)}} \quad \textbf{(Eq 5)}$$

A high *n* value leads to a large difference between yield strength and ultimate tensile strength (engineering stress at maximum load in a tensile test). The ratio of these properties therefore provides another measure of formability.

The *m* value, or strain rate sensitivity, is defined by:

$$m = \frac{d \ln \sigma_T}{d \ln \dot{\epsilon}} \quad \textbf{(Eq 6)}$$

where $\dot{\epsilon}$ is the strain rate, $d\varepsilon/dt$. This implies a relationship of the form:

$$\sigma_T = f(\varepsilon) \cdot \dot{e}^{\,m}$$

or

$$\sigma_T = k\,\varepsilon^n \cdot \dot{e}^{\,m} \quad \textbf{(Eq 7)}$$

where Eq 7 incorporates Eq 4 between stress and strain.

A positive strain rate sensitivity indicates that the flow stress increases with the rate of deformation. This has two consequences. First, higher stresses are required to form parts at higher rates. Second, at a given forming rate, the material resists further deformation in regions that are being strained more rapidly than adjacent regions by increasing the flow stress in these regions. This helps to distribute the strain more uniformly.

The need for higher stresses in a forming operation is usually not a major consideration, but the ability to distribute strains can be crucial. This becomes particularly important in the post-uniform elongation region, where necking and high strain concentrations occur. An approximately linear relationship has been reported between the *m* value and the post-uniform elongation for a variety of steels and nonferrous alloys (Ref 4). As *m* increases from -0.01 to +0.06, the post-uniform elongation increases from 2 to 40%.

Metals in the superplastic range have high *m* values of 0.2 to 0.7, which are one to two orders of magnitude higher than typical values for steel. At ambient temperatures, some metals, such as aluminum alloys and brass, have low or slightly negative *m* values, which explains their low post-uniform elongation.

High *n* and *m* values lead to good formability in stretching operations, but have little effect on drawability. In a drawing operation, metal in the flange must be drawn in without causing fracture in the wall. In this case, high *n* and *m* values strengthen the wall, which is beneficial, but they also strengthen the flange and make it harder to draw in, which is detrimental.

The *r* value, or plastic strain ratio, relates to drawability and is known as the anisotropy factor. This is defined as the ratio of the true width strain to the true thickness strain in the uniform elongation region of a tensile test:

$$r = \frac{\epsilon_w}{\epsilon_t} = \frac{\ln\left(\frac{w}{w_o}\right)}{\ln\left(\frac{t}{t_o}\right)} \quad \textbf{(Eq 8)}$$

The *r* value is a measure of the ability of a material to resist thinning. In drawing, material in the flange is stretched in one direction (radially) and compressed in the perpendicular direction (circumferentially). A high *r* value indicates a material with good drawing properties.

The *r* value frequently changes with direction in the sheet. In a cylindrical cup drawing operation, this variation leads to a cup with a wall that varies in height, a phenomenon known as earing (Fig. 3). It is therefore common to measure the average *r* value, or average normal anisotropy, r_m, and the planar anisotropy, Δr.

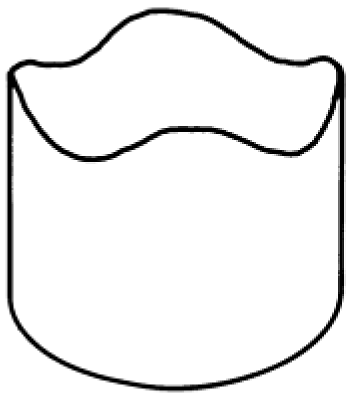

Fig. 3 Drawn cup with ears in the directions of high *r* value.

The property r_m is defined as $(r_0 + 2r_{45} + r_{90})/4$, where the subscripts refer to the angle between the tensile specimen axis and the rolling direction. The value Δr is defined as $(r_0 - 2r_{45} + r_{90})/2$. It is a measure of the variation of *r* with direction in the plane of a sheet. The value r_m determines the average depth (that is, the wall height) of the deepest draw possible. The value Δr determines the extent of earing. A combination of a high r_m value and a low Δr value provides optimal drawability.

Hot-rolled low-carbon steels have r_m values ranging from 0.8 to 1.0; cold-rolled rimmed steels range from 1.0 to 1.4, and cold-rolled aluminum-killed (deoxidized) steels range from 1.4 to 2.0. Interstitial-free steels have values ranging from 1.8 to 2.5, and aluminum alloys range from 0.6 to 0.8. The theoretical maximum r_m value for a ferritic steel is 3.0; a measured value of 2.8 has been reported (Ref 5).

Maximum Strain Levels: The Forming Limit Diagram

Each type of steel, aluminum, brass, or other sheet metal can be deformed only to a certain level before local thinning (necking) and fracture occur. This level depends principally on the combination of strains imposed, that is, the ratio of major and minor strains. The lowest level occurs at or near plane strain, that is, when the minor strain is zero.

This information was first represented graphically as the forming limit diagram, which is a graph of the major strain at the onset of necking for all values of the minor strain that can be realized (Ref 6, 7). Figure 4 shows a typical forming limit diagram for steel. The diagram is used in combination with strain measurements, usually obtained from circle grids, to determine how close to failure (necking) a forming operation is or whether a particular failure is due to inferior work material or to a poor die condition (Ref 8).

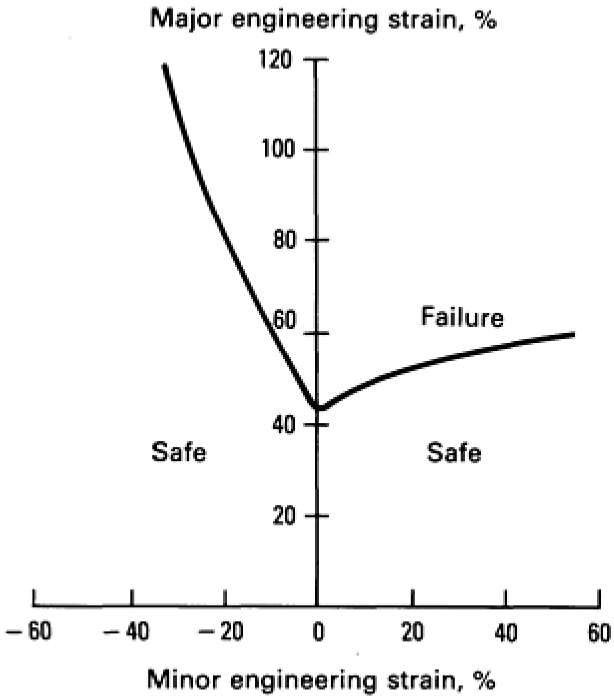

Fig. 4 Typical forming limit diagram for steel.

For most low-carbon steels, the forming limit diagram has the same shape as the one shown in Fig. 4, but the vertical position of the curve depends on the sheet thickness and the *n* value. The intercept of the curve with the vertical axis, which represents plane strain and is also the minimum point on the curve, has a value equal to *n* in the (extrapolated) zero thickness limit. The intercept increases linearly with thickness to a thickness of about 3 mm (0.12 in.).

The rate of increase is proportional to the *n* value up to $n = 0.2$, as shown in Fig. 5. Beyond these limits, further increases in thickness and *n* value have little effect on the position of the curve. The level of the forming limits also increases with the *m* value (Ref 4).

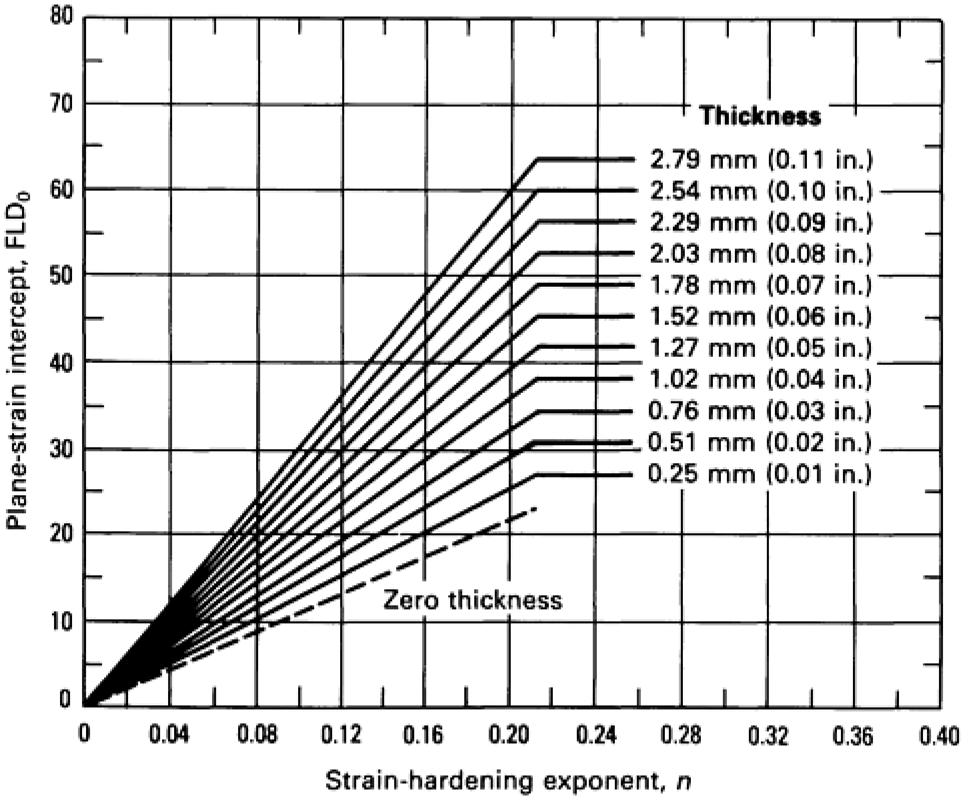

Fig. 5 Effect of thickness and *n* value on the plane-strain intercept of a forming limit diagram. Source: Ref 9.

The shape of the curve for aluminum alloys, brass, and other materials differs from that in Fig. 4 and varies from alloy to alloy within a system. The position of the curve also varies and rises with an increase in the thickness, *n* value, or *m* value, but at rates that are generally not the same as those for low-carbon steel.

The forming limit diagram is also dependent on the strain path. The standard diagram is based on an approximately uniform strain path. Diagrams generated by uniaxial straining followed by biaxial straining, or the reverse, differ considerably from the standard diagram. Therefore, the effect of the strain path must be taken into account when using the diagram to analyze a forming problem.

Material Properties and Wrinkling

The effect of material properties on the formation of buckles or wrinkles is the subject of extensive research. In drawing operations, there is general agreement, based primarily on experiments with conical and cylindrical cups, that a high r_m value and a low Δr value reduce buckling in both flanges and walls (Ref 10, 11, 12). In addition to the above correlations, a low flow-stress-to-elastic-modulus ratio (σ_F/E) decreases wall wrinkling (Ref 13). The *n* value has an indirect effect. When the binder force is kept constant, the *n* value has no effect. However, high *n* values enable higher binder forces to be used, which reduces buckling.

In stretching operations, the situation appears to be different. A close correlation between the formation of buckles at low strain levels and the yield-strength-to-tensile-strength ratio (YS/TS) has been reported, as well as an inverse correlation with the low strain *n* value and an absence of correlation with the r_m value and uniform elongation (Ref 14). Some of the differences between these results may be attributed to the fact that the experiments with cups involved high strains and high compressive stresses, while the stretching experiments were conducted at low strain and low compressive stress levels. In both situations, the problem becomes significantly more severe as the sheet thickness decreases.

Material Properties and Shear Fracture

Shear fractures due to in-plane shear stresses are more prevalent in high-strength cold-worked materials, particularly when internal defects such as inclusions are present. Typical strain combinations that cause shear fracture are shown on the forming limit diagram in Fig. 6. For this material, Fig. 6 shows that, at high strain levels in the regions close to $\varepsilon_2 = \pm\varepsilon_1$, failure occurs by shearing before the initiation of necking.

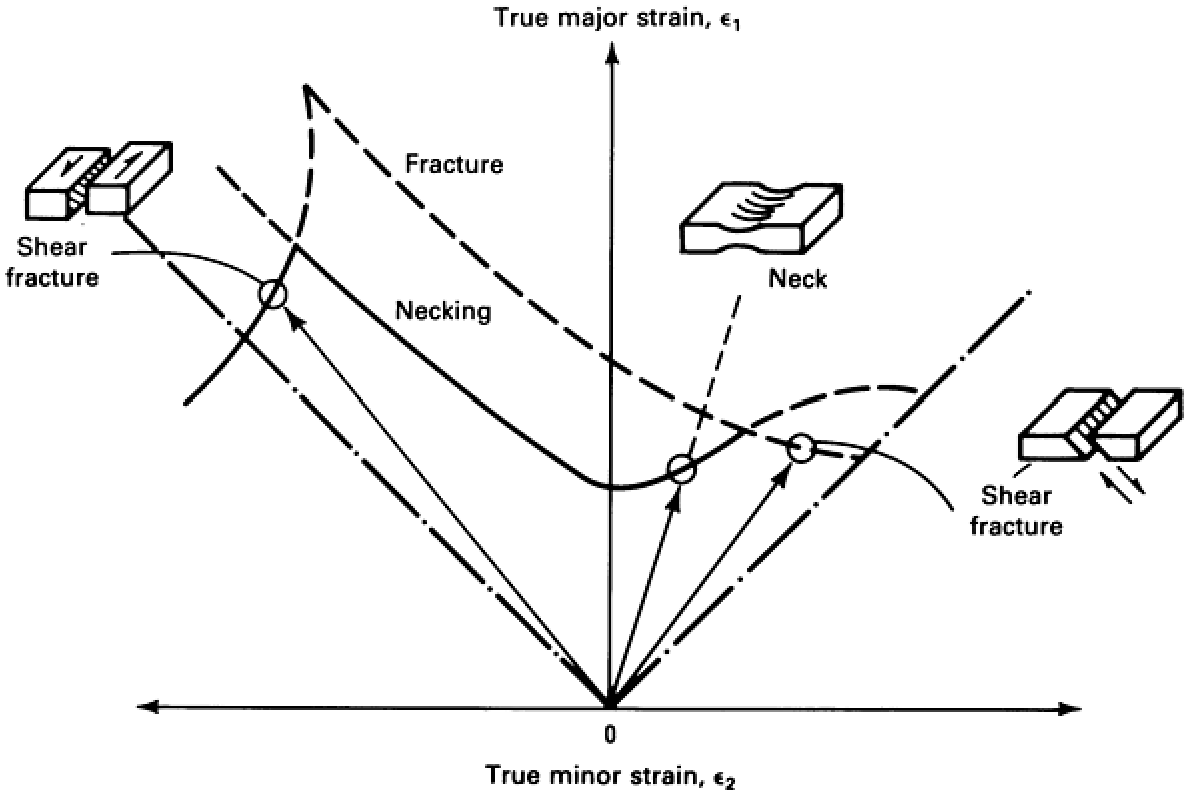

Fig. 6 Forming limit diagram including shear fracture. Source: Ref 15.

The position and shape of the shear fracture curve depends on the material, its temper, and the type and degree of prestrain or cold work (Ref 15, 16, 17). Limited data are available on shear fracture.

Material Properties and Springback

Material properties that control the amount of springback that occurs after a forming operation are:

- Elastic modulus, E
- Yield stress, σ_y
- Slope of the true stress/strain curve, or tangent modulus, $d\sigma_T/d\varepsilon$

Springback is best described by means of three examples involving a rectangular beam: elastic bending below the yield stress, simple bending with the yield stress exceeded in the outer layers of the beam, and combined stretching and bending. In an actual part, springback is determined by the complex interaction of the residual internal elastic stresses, subject to the constraints of the part geometry.

Elastic Bending Below the Yield Stress. Tensile elastic stresses are generated on the outside of the bend. These stresses decrease linearly from a maximum at the surface to zero at the center (neutral axis). They then become compressive and increase linearly to a maximum at the inner surface. Upon removal of the externally applied bending forces, the internal elastic forces cause the beam to unbend as they decrease to zero throughout the cross section (Fig. 7a).

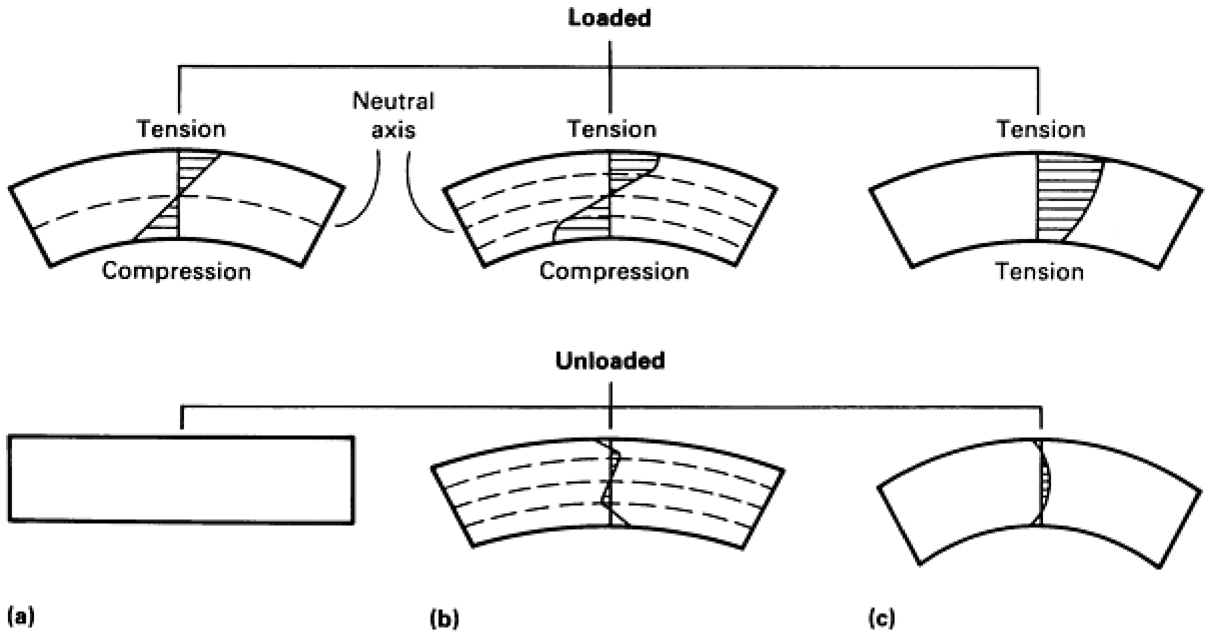

Fig. 7 Springback of a beam in simple bending. (a) Elastic bending. (b) Elastic and plastic bending. (c) Bending and stretching.

The maximum amount of elastic deflection that can be produced without entering the plastic range is proportional to the yield stress divided by the elastic modulus. The strain at the yield point is equal to σ_y/E ($E = \sigma/\varepsilon$). The springback moment for a given deflection is therefore proportional to the elastic modulus ($\sigma = E\varepsilon$).

Simple Bending. In this example, the yield stress is exceeded in the outer layers of the beam. The outer layers deform plastically, and their stored elastic stresses continue to increase, but at a much lower rate that is proportional to the slope of the true stress-strain curve, or tangent modulus, $d\sigma_T/d\varepsilon$, instead of the elastic modulus. Figure 7(b) illustrates this condition for a beam bent so that 50% of its volume is in the plastic range.

Upon removal of the externally applied bending forces, the stored elastic stresses cause the beam to unbend until their combined bending moment is zero. This produces compressive stresses at the outer surface and tensile stresses at the inner surface.

The springback in this case is less than for a material whose yield strength is not exceeded at the same strain level. This can result from either a higher yield stress or a lower elastic modulus. It is also apparent that higher values of the tangent modulus cause greater springback when the yield strength is exceeded.

In actual conditions, the neutral axis moves inward upon bending because the outer part of the beam is stretched and becomes thinner and because the inner part is compressed and becomes thicker. This effect is analyzed in detail in Ref 18.

Combined Stretching and Bending. In this case, the entire beam can be plastically deformed in tension by as little as 0.5% stretching. However, a stress gradient still exists from the outer to the inner surface (Fig. 7c). Upon removing the external forces the internal elastic stresses recover.

This causes unbending, but to a lesser extent than in the previous cases. As the level of stretching is increased, the amount of springback decreases because the tangent modulus and therefore the stress gradient through the beam decrease at higher strains. The yield strength ceases to be a factor in springback once all regions are plastically deformed in tension.

In the bending of wide sheets, the metal is deformed in plane strain, and the plane-strain properties (elastic modulus, yield stress, and tangent modulus) should be used. The effects of a low elastic modulus and a high yield stress and tangent modulus in increasing springback have been experienced in forming operations. Springback is more severe with aluminum alloys than with low-carbon steel (1 to 3 modulus ratio). High-strength steels exhibit more springback than low-carbon steels (~2 to 1 yield strength ratio), and dual-phase steels spring back more than high-strength steels of the same yield strength (higher tangent modulus).

The effect of stretching in reducing springback to very low levels has also been reported (Ref 19). Springback is also greatly influenced by geometrical factors, and it increases as the bend angle and ratio of bend radius to sheet thickness increase.

Surface Quality

The previously mentioned conditions that lead to undesirable surface textures can be minimized or prevented. The formation of orange peel in heavily deformed regions can be minimized by using a fine-grain material. The development of Lüders lines in rimmed steels can be prevented by temper rolling to 0.25 to 1.25% extension or by flex rolling, which produces mobile dislocations for a limited period of time, until they are trapped by nitrogen atoms. This also reduces elongation slightly. This problem is becoming less common with the increased use of continuous casting, which requires killed steels. These steels have less free nitrogen to interact with the dislocations and do not develop Lüders lines. Similar treatments can be applied to aluminum-magnesium alloys to prevent this defect.

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Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Effect of Temperature on Formability

A change in the overall temperature alters the properties of the material, which thus affects formability. In addition, local temperature differences within a deforming blank lead to local differences in properties that affect formability.

At high temperatures, above one-half of the melting point on the absolute temperature scale, extremely fine-grain aluminum, copper, magnesium, nickel, stainless steel, steel, titanium, zinc, and other alloys become superplastic. Superplasticity is characterized by extremely high elongation, ranging from several hundred to more than 1000%, but only at low strain rates (usually below about $10^{-2}/s^{-1}$) at high temperatures.

The requirements of high temperatures and low forming rates have limited superplastic forming to low-volume production. In the aerospace industry, titanium is formed in this manner. The process is particularly attractive for zinc alloys because they require comparatively low temperatures (~270 °C, or ~520 °F).

At intermediate elevated temperatures, steels and many other alloys have less ductility than at room temperature (Ref 20, 21). Aluminum and magnesium alloys are exceptions and have minimum ductility near room temperature. Alloys of these metals have been formed commercially at slightly elevated temperatures (~250 °C, or ~480 °F). The strain rate sensitivity (*m* value) and post-uniform elongation for aluminum-magnesium alloys have been found to increase significantly in this temperature range (Ref 22).

Low-temperature forming has potential advantages for some materials, based on their tensile properties, but practical problems have limited application. Local increases in temperature occur during forming because of the surface friction and internal heating produced by the deformation. Generally, this is detrimental because it lowers the flow stress in the area of greatest strain and tends to localize deformation.

A method of improving drawability by creating local temperature differences has been developed and is being used commercially (Ref 23). It involves water cooling the punch in a deep-drawing operation. This lowers the temperature of the blank where it contacts the punch, which is the principal failure zone, and increases the local flow stress. Heating the die in order to lower the flow stress in the deformation zone at the top of the draw wall has also been found to be beneficial. The combination of these procedures has produced an increase of over 20% in the drawability of an austenitic stainless steel.

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Types of Formability Tests

Formability tests are of two basic types: intrinsic and simulative. Intrinsic tests measure the basic characteristic properties of materials that can be related to their formability. Simulative tests subject the material to deformation that closely resembles the deformation that occurs in a particular forming operation.

Intrinsic tests provide comprehensive information that is insensitive to the thickness and surface condition of the material. The most important and extensively used intrinsic test is the uniaxial tensile test, which provides the values of many material properties for a wide range of forming operations. Other commercially important intrinsic tests are the plane-strain tensile test, the Marciniak stretching and sheet torsion tests, the hydraulic bulge test, the Miyauchi shear test, and hardness tests.

Simulative tests provide limited and specific information that is usually sensitive to thickness, surface condition, lubrication, and geometry and type of tooling. This information usually relates to only one type of forming operation. Many simulative tests, such as the Olsen and Swift cup test, have been extensively used for many years with good correlation to production in specific cases. Several simulative tests are described later in this article.

Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Uniaxial Tensile Testing

The most widely used intrinsic test of sheet metal formability is the uniaxial tensile test. A specimen such as that illustrated in Fig. 8 is used; its sides are accurately parallel over the gage length, which is usually 50.8 mm (2.00 in.) long and 12.7 mm (0.50 in.) wide. The specimen is gripped at each end and stretched at a constant rate in a tensile machine until it fractures, as described in ASTM E 8. The applied load and extension are measured by means of a load cell and strain gage extensometer.

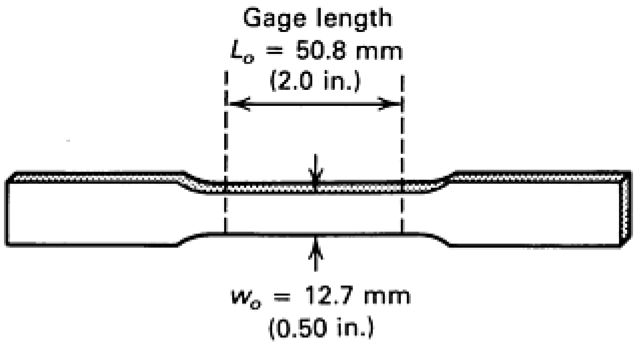

Fig. 8 Sheet tensile test specimen.

The load extension data can be plotted directly. However, data are usually converted into engineering (conventional) stress, σ_E (load/original cross section), and engineering strain, *e* (elongation/original length), or to true stress, σ_T (load/instantaneous cross section), and true strain, ε(natural logarithm of strained length/original length).

In addition, for formability testing, it is common practice to measure the width of the specimen during the test. This is done either intermittently by interrupting the test at preselected elongations to make measurements manually or continuously by means of width extensometers. From these measurements, the plastic strain ratio (anisotropy factor), or *r* value, can be determined.

During the rolling process used to produce metals in sheet form and the subsequent annealing, the grains and any inclusions present become elongated in the rolling direction, and a preferred crystallographic orientation develops. This causes a variation of properties with direction. Therefore, it is common practice to test specimens cut parallel to the rolling direction and at 45 and 90° to this direction. These are known as longitudinal, diagonal, and transverse specimens, respectively. This also enables the values of r_m and Δr to be calculated. Because the mechanical properties and elongation tend to be lower in the transverse direction, tests in this direction are often used as the basis for specifications.

The rate at which the test is performed can have a significant effect on the end results. Two methods are commonly used to determine this effect. In the first method, replicate samples are tested at different rates, and the results are influenced by variations between the samples. In the second method, the test rate is alternated between two levels. This approach avoids the problem of variation between samples, but it cannot be used at very high rates and is complicated by transients, which occur each time the rate is changed. The strain rate sensitivity, or *m* value, can be calculated from these tests.

Figure 9 shows a typical engineering stress-strain curve and the corresponding true stress-strain curve for a material that has a smooth transition between the very low strain (elastic) and the higher strain (plastic) regions of the curve. When the load is removed in the elastic region, the sample returns to its original dimensions. When this is done in the plastic region, the sample retains permanent deformation.

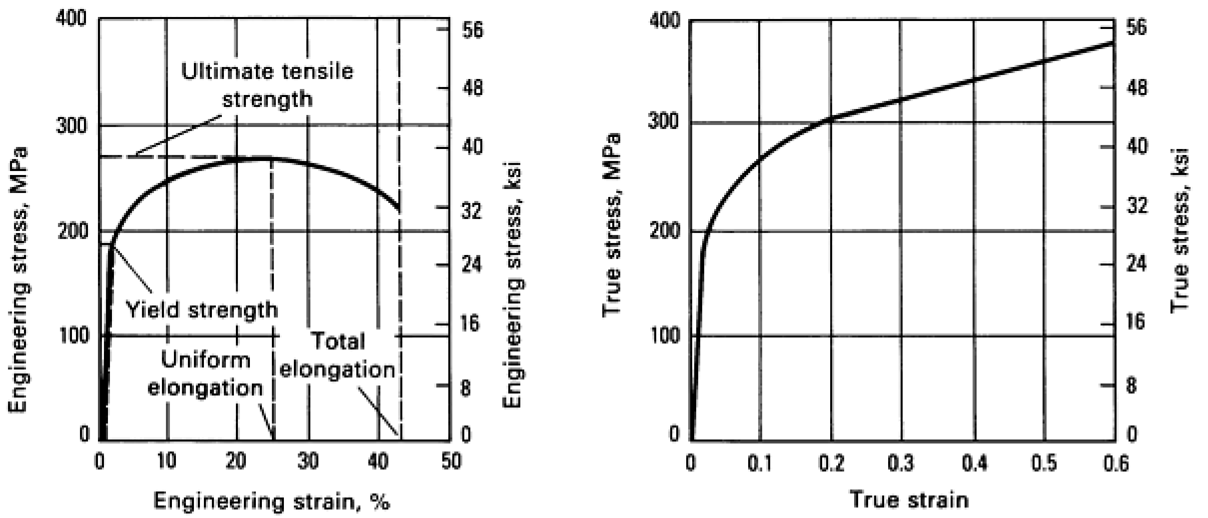

Fig. 9 Typical engineering and true stress-strain curves.

In the tensile test, the load increases to a maximum value and then decreases prior to fracture. The decrease is due to the localization of the deformation, which causes a reduction in cross section. This reduction has a greater effect than the opposing increase in flow stress due to strain hardening.

Some materials such as aged rimmed steels do not have a smooth transition between the elastic and plastic regions of the stress-strain curve. The load they can support decreases at the beginning of the plastic region and remains approximately constant for up to about 7% elongation. Subsequently, the load increases to a maximum and then decreases again at high elongations. This type of stress-strain curve is shown in Fig. 10. With the increasing use of continuous casting, which requires killed steels (steels deoxidized by small additions of aluminum, for example), rimmed steels are becoming less common.

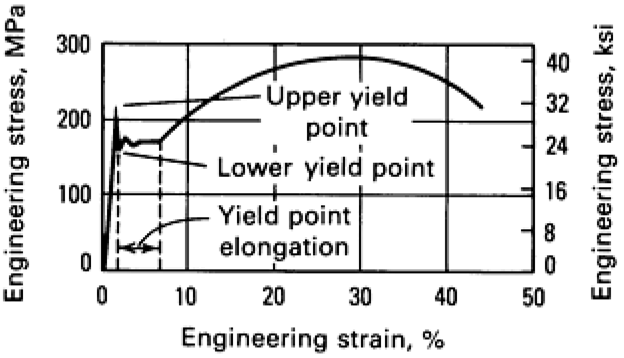

Fig. 10 Engineering stress-strain curve for rimmed steel.

Test Procedure

For accurate and reproducible results, uniaxial tensile testing must be performed in a carefully controlled manner. The main steps in the procedure are discussed in detail in the Section "Tension Testing" in *Mechanical Testing*, Volume 8 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.* These procedures are summarized below.

Specimen Preparation. The surfaces of the specimen should be free from scratches or other damage that can act as stress raisers and cause early failure. The edges should be smooth and free from irregularities. Care should be taken not to cold work the edges, or to ensure that any cold work introduced is removed in a subsequent operation, because this changes mechanical properties and lowers ductility.

It is common practice to mill and grind the edges, but other procedures such as fine milling, nibbling, and laser cutting are also used. When a new method is used, initial tests should be performed to compare the results with those obtained by conventional methods.

The width of a nominally 12.7 mm (0.50 in.) wide specimen should be measured to the nearest 0.025 mm (0.001 in.), and the thickness for specimens in the range of 0.5 to 2.5 mm (0.02 to 0.1 in.) should be measured to the nearest 0.0025 mm (0.0001 in.). If this is impractical because of surface roughness, the thickness should be measured to the nearest 0.025 mm (0.001 in.).

The tensile test is sensitive to variations in the width of the specimen, which should be accurately controlled. For a specimen 12.7 mm (0.50 in.) wide, the width of the reduced section should not deviate by more than ±0.25 mm (±0.01 in.) from the nominal value and should not differ by more than ±0.05 mm (±0.002 in.) from end to end.

Some investigators intentionally taper the reduced section slightly toward the center to increase the probability that fracture will occur within the gage length. In this case, the center should not be narrower than the ends by more than 0.10 mm (0.004 in.).

Alignment of Specimens. The specimen should be accurately aligned with the centerline of the grips. The effect of small displacements (10% of the specimen width) of one or both ends from the centerline has been calculated (Ref 24). It has been determined that the latter case is the more serious, but both strongly affect the strain in the outermost fibers. It has also been concluded that the calculated stress-strain curve is not significantly affected at strains above 0.3%.

Measurement of Load and Elongation. The applied load is measured by means of a load cell in the test machine, for which the usual calibration procedures must be followed (ASTM E 4). Elongation is usually determined by using a clip-on strain gage extensometer (ASTM E 83). In addition, small scratches are often scribed across the specimen at the ends of the gage length so that the total elongation can be determined from the broken specimen.

Circle grids are sometimes etched or printed on the specimen. These can be used to measure the strain distribution and width strain as well as the overall strain. This can be done continuously by means of a video camera and data processing system if required. Optical extensometers are used for some applications, particularly high-speed testing. These units require well-illuminated boundaries that are clearly delineated by means of high-contrast coatings, such as black-and-white paint.

An approximate measure of elongation can be obtained from the crosshead travel. This involves errors due to elongation of the specimen outside the gage length and elastic strain in the grips, which can be compensated for to some extent. This method is used when the specimen is inaccessible, such as in nonambient testing.

The signals from the load cell and extensometer can be plotted on a chart recorder or processed by a data processing system to the required form, such as plots of stress versus strain or tables of mechanical and forming properties.

Measurement of Width and Thickness. In addition to the initial measurements of specimen width and thickness, which are required to calculate the stress, measurements can be made at intervals during the test to determine the *r* value (ASTM E 517) and to determine the reduction in area and true strain. The *r* value is measured at a specified strain level between the yield point and the uniform elongation (for example, at 15% elongation). It can be measured by stopping the test at this strain level and then measuring the width accurately (±0.013 mm, or ±0.0005 in.) at a minimum of three equally spaced points in the gage length (for a 50.8 mm, or 2.0 in., gage length). In practice, the thickness is calculated from the specimen width and length, assuming no change in volume.

Alternatively, width measurements can be made during the test using width extensometers, although this is a more complicated procedure. Attempts are underway to develop combined width and length extensometers to simplify this method.

Reduction in area is the ratio $(A_o - A)/A_o$, where A is the instantaneous cross-sectional area and A_o is the original cross-sectional area. It is used to calculate the true strain in the region of post-uniform elongation. A large reduction in area at fracture correlates with a small minimum bend radius, a high *m* value, and high energy absorption. To calculate the reduction in area, the width and thickness must be measured in the narrowest part of the necked region.

Effect of Gage Length on Elongation. In post-uniform elongation, part of the specimen is elongated uniformly, and the remainder is narrowed into a necked region of higher strain level. A change in the gage length alters the ratio of these two regions and has a significant effect on the total elongation measurement. This phenomenon is discussed in detail in Ref 25.

To obtain results that are comparable for different gage lengths, the ratio of the square root of the cross-sectional area to the length, $\sqrt{A}/L$, should be the same. When comparing samples of different thickness, this implies that the gage length or the width should be adjusted to maintain this ratio.

Rate of Testing. Most tensile tests are performed on screw-driven or hydraulic testing machines at strain rates of 10^{-5} to 10^{-2} s^{-1}. The strain rate is defined as the increase in length per unit length per second. These tests are known as low strain rate or static tests.

Most high-volume production forming operations are performed at considerably higher strain rates--in the range of 1 to 10^2 s^{-1}. To determine the tensile properties in this range, dynamic test machines, which operate at rates of 10^{-1} to 10^2 s^{-1}, are used (Ref 25). As mentioned previously, steels have higher tensile properties and lower elongations at high strain rates. The properties of aluminum alloys have little sensitivity to the strain rate.

Material Properties

The stress-strain curve determined by uniaxial tension testing provides values of many formability-related material properties. Several of these properties and methods for measurement are discussed below. Table 1 lists typical values of properties measured in tensile tests on thin (0.5 to 1.0 mm, or 0.02 to 0.04 in.) sheet materials.

Table 1 Typical tensile properties of selected sheet metals

Material	Young's modulus, E		Yield strength		Tensile strength		Uniform elongation, %	Total elongation, %	Strain-hardening exponent, n	Average normal anisotropy, r_m	Planar anisotropy, Δr	Strain rate sensitivity, m
	GPa	10^6 psi	MPa	ksi	MPa	ksi						
Aluminum-killed drawing quality steel	207	30	193	28	296	43	24	43	0.22	1.8	0.7	0.013

Interstitial-free steel	207	30	165	24	317	46	25	45	0.23	1.9	0.5	0.015
Rimmed steel	207	30	214	31	303	44	22	42	0.20	1.1	0.4	0.012
High-strength low-alloy steel	207	30	345	50	448	65	20	31	0.18	1.2	0.2	0.007
Dual-phase steel	207	30	414	60	621	90	14	20	0.16	1.0	0.1	0.008
301 stainless steel	193	28	276	40	690	100	58	60	0.48	1.0	0.0	0.012
409 stainless steel	207	30	262	38	469	68	23	30	0.20	1.2	0.1	0.012
3003-O aluminum	69	10	48	7	110	16	23	33	0.24	0.6	0.2	0.005
6009-T4 aluminum	69	10	131	19	234	34	21	26	0.23	0.6	0.1	-0.002
70-30 brass	110	16	110	16	331	48	54	61	0.56	0.9	0.2	0.001

Young's Modulus. The initial slope of the stress-strain curve, that is, the ratio of the stress to the strain in the elastic region before any plastic deformation has occurred, is the Young's modulus, *E*, of the material. This property affects springback and shape distortion at low strains. For accurate measurement of Young's modulus, a low strain rate and a high data acquisition rate should be used in the elastic region (below about 0.5% elongation), and a very stiff tension-testing machine should be used if strain is inferred from crosshead displacement.

Yield Strength. The stress at which the stress-strain curve deviates in elongation from the initial elastic slope by a specified amount, commonly 0.2%, is known as the yield strength (YS). The yield strength determines the load necessary to initiate deformation in a forming operation, which is usually a high percentage (40 to 90%) of the maximum load required.

For accurate measurement of yield strength, a rate of loading of less than 690 MPa/min (100 ksi/min) is specified. Beyond this point, the strain rate should not exceed 0.08 s^{-1}. Some materials elongate without an increase in load, or at a decreased load, at the transition between the elastic and plastic regions. The point at which this initiates is known as the yield point.

With a decrease in load, the material has an upper yield point and a lower yield point. The upper yield point is difficult to measure reproducibly. The lower yield stress usually fluctuates, and the minimum value is used.

The elongation that occurs after yielding before the load starts to increase monotonically is known as the yield point elongation. Yield point elongation leads to nonuniform deformation at low strains in forming operations. If it exceeds about 1.5%, irregular surface markings known as Lüders lines or stretcher strains may occur to an extent that is unacceptable in visible parts.

Tensile Strength. The maximum stress observed in the test is known as the tensile strength (TS), or ultimate tensile strength. Tensile strength determines the maximum load that can be usefully applied in a forming operation.

Uniform Elongation. The engineering strain at the maximum engineering stress is known as the uniform elongation, e_u. Prior to this point, the sample deforms uniformly. Subsequently, deformation concentrates--initially in a fairly large region known as a diffuse neck, and ultimately in a localized region of sharply reduced cross section known as a local neck. Deformation continues to concentrate in this region until fracture occurs.

Total Elongation. Elongation at the point of fracture is known as total elongation, e_T. It has been extensively used as an approximate indication of sheet metal formability. However, no single property is a reliable indicator of formability under all conditions.

Reduction in area, $(A_o - A)/A_o$, is calculated from measurements of actual specimen width and thickness in the narrowest part of the necked region. The true strain, which cannot be determined from length measurements in the post-uniform elongation region, is also calculated from these values.

The true strain in the necked region is equal to ln (dL/dL_o), where dL is a small element of length in this region, whose original length was dL_o. Equating the original and final volumes of this element of length gives:

$$V_o = A_o dL_o = V = AdL$$

or

$$\epsilon = \ln\left(\frac{dL}{dL_o}\right) = \ln\left(\frac{A_o}{A}\right) \quad \textbf{(Eq 9)}$$

The relationship between the reduction in area at fracture and the minimum bend radius is as follows (Ref 25). For values of reduction in area at fracture, q, below 0.2, the ratio of the minimum bend radius, R_m, to sheet thickness, t, is given by:

$$\frac{R_m}{t} = \frac{1}{2q} - 1 \quad \textbf{(Eq 10)}$$

For values of q greater than 0.2:

$$\frac{R_m}{t} = \frac{(1 - q)^2}{(2q - q^2)} \quad \textbf{(Eq 11)}$$

Strain-Hardening Exponent. The n value, $d \ln \sigma_T / d \ln\varepsilon$, is given by the slope of a graph of the logarithm of the true stress versus the logarithm of the true strain in the region of uniform elongation. For materials that closely follow the Holloman constitutive equation (Eq 4), an approximate n value can be obtained from two points on the stress-strain curve by the Nelson-Winlock procedure (Ref 26). The two points commonly used are at 10% strain and at the maximum load. The ratio of the loads or stresses at these two points is calculated, and the n value and uniform elongation can then be determined from a table or graph. The accuracy of the n value determined in this way is ±0.02.

The n value can be determined more accurately by linear regression analysis, as in ASTM E 646. For some materials, n is not constant, and initial (low strain), terminal (high strain), and sometimes intermediate n values are determined. The initial n value relates to the low deformation region, in which springback is often a problem. The terminal n value relates to the high deformation region, in which fracture may occur.

Plastic Strain Ratio. The r value, or anisotropy factor, is defined as the ratio of the true width strain to the true thickness strain in a tensile test. Generally, its value depends on the elongation at which it is measured. It is usually measured at 10, 15, or 20% elongation.

The r value is calculated from the measured width and length as:

$$\epsilon_w = \ln\left(\frac{w}{w_o}\right)$$
$$\epsilon_t = \ln\left(\frac{t}{t_o}\right) = \ln\left(\frac{L_o w_o}{L w}\right) \quad \textbf{(Eq 12)}$$

where constancy of volume ($Lwt = L_o w_o t_o$) has been used and:

$$r = \frac{\epsilon_w}{\epsilon_t} = \frac{\ln\left(\frac{w}{w_o}\right)}{\ln\left(\frac{L_o w_o}{L w}\right)} \quad \textbf{(Eq 13)}$$

The average r value, or normal anisotropy (r_m), and the planar anisotropy, or Δr value, can be calculated from the values of r in different directions using Eq 1, 2, 8, and 12.

Strain Rate Sensitivity. The m value, $d \ln \sigma_T / d \ln \dot{e}$, is determined either from duplicate tensile tests performed at different strain rates or from a single test in which the rate is alternated between two levels during the test. These methods are shown schematically in Fig. 11. The m value can be determined at various strain levels in the region of uniform elongation:

$$m = \frac{\ln\left(\frac{\sigma_1}{\sigma_2}\right)}{\ln\left(\frac{\dot{\epsilon}_1}{\dot{\epsilon}_2}\right)} \quad \textbf{(Eq 14)}$$

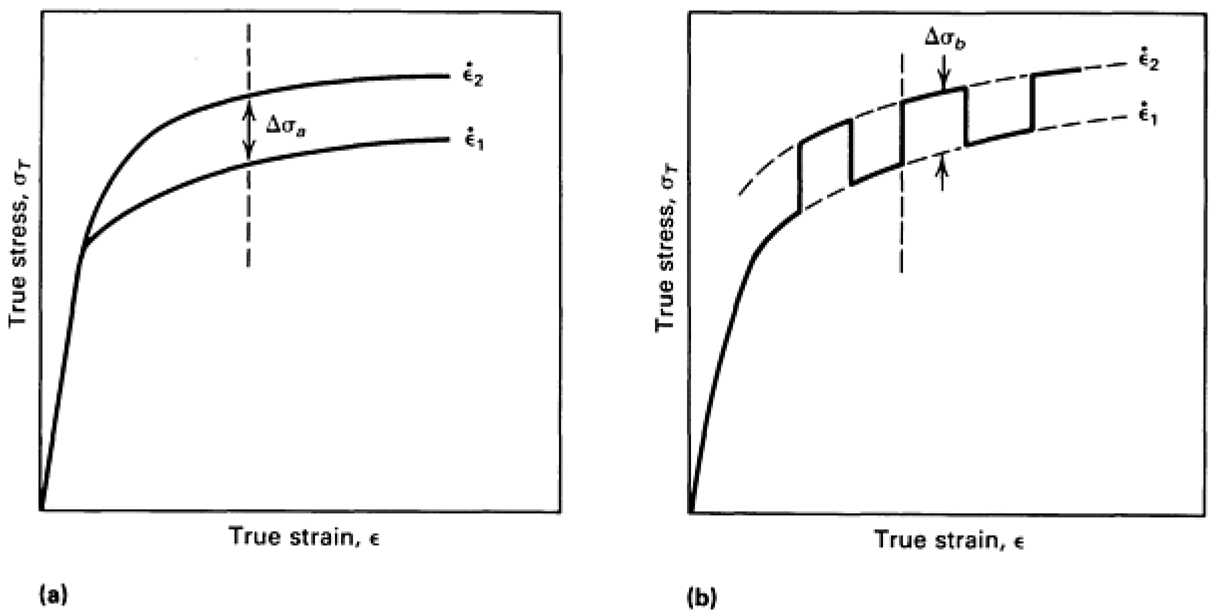

Fig. 11 Methods for determining strain-rate sensitivity (*m* value). (a) Duplicate test method. (b) Changing rate method.

In some materials, *m* is insensitive to strain (Ref 4, 27). In other materials, however, *m* is sensitive to strain and strain rate (Ref 28). In many materials, *m* increases and *n* decreases with an increase in temperature (Ref 29), sometimes to the extent that superplastic properties develop.

Determining *n* and *r* Values

The time and facilities required for sample preparation and for performing the uniaxial tensile test make it difficult to use for on-line process control. The following simplified tests for determining *n* and *r* are more suitable for this purpose. The circle arc elongation test and the rapid-*n* test utilize tensile specimens with two sections that differ in width by about 5% to determine *n* values. Fracture almost always occurs in the narrow section, but the final measurements are made on the wide section, which elongates uniformly. The *r* value can be obtained from these tests, but for ferritic steels, the Modul-*r* test is faster and easier to perform. This test actually measures the elastic modulus of the specimen and uses an empirically determined correlation between the modulus and the *r* value.

The circle arc elongation test does not require measurement of the applied loads (Ref 30). It uses a rectangular tensile specimen with a reduced width section produced by milling a pair of small circular arc notches on opposite sides. The gage length is marked in the full-width section, and the specimen is pulled to fracture, which usually occurs in the narrow section. The uniform elongation is measured in the full-width section. The value is slightly lower than that obtained in the conventional tensile test and gives a slightly lower *n* value. However, it is suitable for production control. The *r* value can be determined by the additional measurement of the change in width in the full-width section.

The rapid-*n* test provides rapid and fairly accurate measurements of yield and tensile strengths, elongation, and *n* and *r* values (Ref 31). It requires relatively simple equipment and can be performed in less than 5 min, including specimen preparation. The test is suitable for sheet metals whose properties are represented accurately by the Holloman equation, $\sigma_T = k\varepsilon^n$. It has been successfully used on low-carbon and stainless steels and on a variety of nonferrous alloys.

The test specimen, which is punched directly from the sheet sample, has the dimensions shown in Fig. 12. Generally, 25 mm (1.0 in.) gage lengths are marked on both the wide and narrow sections, and the specimen is strained to fracture in a manual or motorized load frame or in a tension-testing machine. The yield load is measured if there is discontinuous yielding, and the maximum load is measured. Yield and tensile strengths are calculated from the measured loads and the initial dimensions of the narrow section. If the yielding is continuous, the yield strength can be calculated from the tensile strength and *n* value, as indicated below.

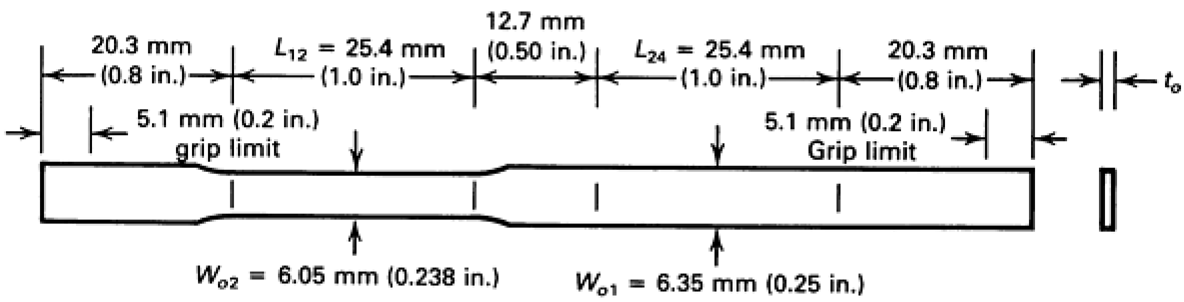

Fig. 12 Rapid-*n* test specimen. Source: Ref 31.

An empirically determined correction is applied to compensate for the effect of the sheared edges of the specimens. For steel, this correction reduces the measured yield and tensile strengths by 13.6 MPa/mm (50.0 ksi/in.) of initial sample thickness. The *n* value is calculated from:

$$n = \frac{\ln\left(\dfrac{w_{o2}t_o}{w_{f1}t_f}\right) - n}{\ln\left[\dfrac{\ln\left(w_{o1}t_o/w_{f1}t_f\right)}{n}\right]} \qquad \textbf{(Eq 15)}$$

where w_{o1} and w_{o2} are the initial widths of the wide and narrow sections, respectively; w_f is the final width of the wide section; and t_o and t_f are the initial and final thicknesses of the wide section, respectively. Equation 15 can be solved iteratively in four steps, or by means of a simple computer program, beginning with a trial value of 0.24 for *n*.

For materials that do not have a discontinuous yield point, the yield strength can be calculated as:

$$\text{YS} = \text{TS}\left(\frac{\text{C}}{n}\right)^n \quad \textbf{(Eq 16)}$$

where TS is the tensile strength and C is a constant. For low-carbon steels, C $\simeq$0.02. For other materials, C must be determined empirically. The *r* value can be computed from the initial and final dimensions of the wide section as described previously.

The Modul-*r* test measures the elastic modulus (Young's modulus) of low-carbon steel samples by determining their resonant frequencies by exciting them using an oscillating magnetic field (Ref 32). The elastic modulus is directly proportional to the square of the resonant frequency, and simple empirical relationships exist between the directionally averaged elastic modulus and the r_m value and between the planar variation of the modulus and the Δr value.

This test uses a flat 102 × 6.35 mm (4.0 × 0.25 in.) punched specimen and a specially designed, commercially available magnetostrictive oscillator. The specimen is placed inside drive and pickup coils in the oscillator. An alternating current passed through the drive coil produces an alternating magnetic field, which causes magnetostrictive oscillations in the sample. The oscillations induce an alternating current in the pickup coil. This current is used to change the frequency of the current in the drive coil to maximize the amplitude of the oscillations, that is, to obtain the resonant frequency, which is displayed digitally.

The relationships used to determine r_m and Δr from the resonant frequency, *f*, are:

$$E = 4\rho L^2 f^2 \quad \textbf{(Eq 17)}$$

$$r = \frac{4822.6}{(E_m - 267.7)^2} - 0.564 \quad \textbf{(Eq 18)}$$

$$\Delta r = 0.031 - 0.0468\,\Delta E \quad \textbf{(Eq 19)}$$

where E is the elastic modulus in gigapascals; ρ and L are the density and length of the specimen, respectively; and E_m and ΔE are defined analogously to r_m and Δr.

This test provides a more reproducible measure of r_m and Δr than the conventional tensile-testing method and is less sensitive to differences between operators. It can be performed in 5 min, including specimen preparation. When alloy steels or ferritic stainless steels are tested, a different correlation between the modulus and *r* value must be used. This must be determined experimentally. When the test is used on coated products, the coating must be removed chemically prior to testing.

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Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Plane-Strain Tensile Testing

In conventional uniaxial tensile testing, the sample is strained in the region of drawing; that is, the minor or width strain is negative. The test does not provide information on the response of sheet materials in the plane-strain state, in which the minor strain is zero. However, it can be modified to produce this strain state in part of the sample. This modification involves the use of a very wide, short sample or the use of knife-edges to prevent transverse (width) strain in part of the sample.

Wide Sample Methods. Increasing the width of the sample and decreasing the gage length changes the strain state from one with a large negative minor strain component toward the plane-strain state, in which the minor strain component is zero. In the rectangular sheet tension test, samples with length-to-width ratios of 1 to 1, 1 to 2, and 1 to 4 are used to approach the plane-strain conditions (Ref 33). Gage lengths are constrained further by reinforcements welded onto each side of the sample at both ends, thus making the samples three layers thick except in the gage length.

The minimum minor strain obtained with the 1 to 4 length-to-width ratio is -0.05 times the major strain, which is close to the plane-strain condition of zero minor strain. The in-plane strains are measured by means of grid markings on the samples, and through-thickness deformations can be observed by holographic interferometry.

A similar approach was used in testing many wide specimen designs to determine the effect of edge profile and length-to-width ratio on strain state (Ref 34, 35, 36). The specimen geometry that yielded the highest center strain at failure with a large region of plane strain is shown in Fig. 13. The plane-strain region, which is arbitrarily taken as the region where $|e_2/e_1|$ is less than 0.2, occupies about 80% of the specimen width. The outer part of the specimen deforms in a similar manner to a standard tensile test specimen.

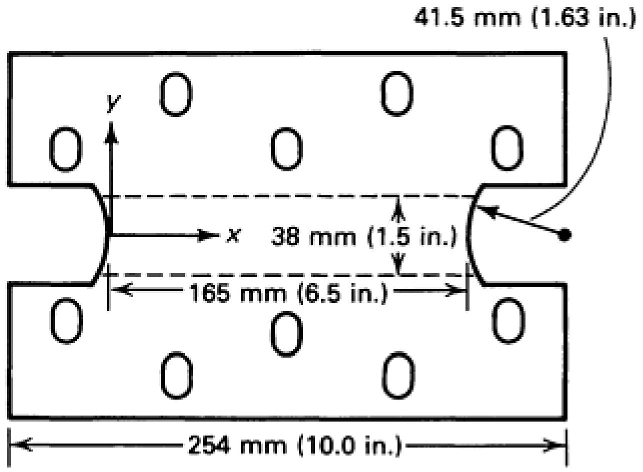

Fig. 13 Plane-strain tensile test specimen. Source: Ref 36.

Special grips were developed that exert a high clamping force at the inner contact lines. This minimizes distortion and slip-page in these regions, giving the test well-defined boundary conditions. The results of both types of wide specimen tensile tests described above correlated well with stress-strain predictions obtained by finite-element modeling using material properties obtained in the standard tensile test (Ref 34, 37).

Width Constraint Method. In the width constraint method, a rectangular sample is used that has a central gage section reduced in width by circular notches (Ref 38). The gage section is clamped between two pairs of opposing parallel knife-edges (stingers) aligned with the sample axis. The knife-edges prevent transverse (width) strain in this region. The sample is pulled to fracture in a tension-testing machine, and the plane-strain limit (necking) and fracture strains are determined from thickness measurements made on the fractured sample. This procedure is described in detail in Ref 38. The use of a spring-loaded clamp around the knife-edges makes adjustment of the clamp during testing unnecessary.

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Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Biaxial Stretch Testing

Two tests that determine the properties of sheet metals in biaxial stretching without involving surface friction effects are the Marciniak biaxial stretching test (Ref 39) and the hydraulic bulge test (Ref 40). The Marciniak test subjects the sample to in-plane biaxial stretching, but does not determine the stresses. In the hydraulic bulge test, the stresses can be determined, but the sample is deformed into a dome, which involves out-of-plane stresses and strains.

Marciniak Biaxial Stretching Test. A disk of the test material is stretched over a flat-bottomed punch of cylindrical or elliptical cross section. This creates uniform in-plane biaxial strain in the center of the sample, with a strain ratio that is determined by the ratio of the major and minor diameters of the punch. Most testing has been performed with a cylindrical punch, which produces balanced biaxial stretching.

The center of the punch is hollowed out to eliminate friction in this area, and a spacer is placed between the sample and the punch. The spacer is a disk of material similar to that under test--with the same diameter, but with a hole at the center. The experimental arrangement is shown in Fig. 14. As the disk and spacer are stretched over the punch, the hole in the spacer enlarges, and the central part of the test sample is deformed in uniform in-plane biaxial stretching.

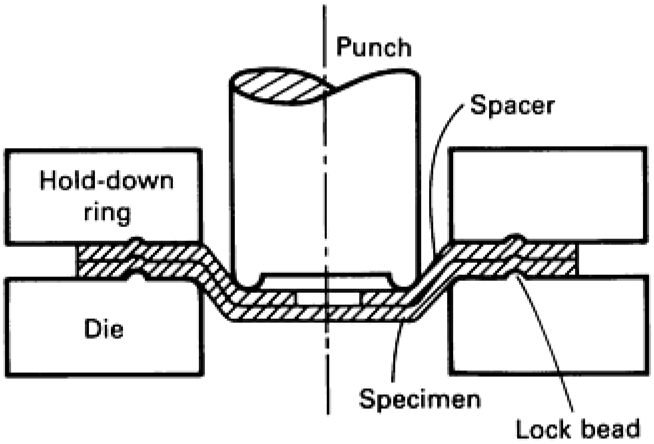

Fig. 14 Schematic of Marciniak biaxial stretching test.

The function of the spacer is to reverse the direction of the surface friction experienced by the sample. In the absence of the spacer, the surface friction opposes the movement of the sample over the punch and reduces the maximum strain level attainable. The spacer deforms more easily than the test sample because of the hole in the center, and it exerts a frictional force on the sample directed outward over the punch radius.

For the material to stretch freely, the punch and die radii must be adequate for the thickness of the material under test. A ratio of spacer hole diameter to punch diameter of 1 to 3 has been successfully used. The strains can be measured by using grid circles, squares, or other suitable markings. The test has the following applications:

- Determination of the limiting strains of materials in uniform in-plane biaxial stretching without surface friction
- Application of a carefully controlled level of uniform in-plane biaxial strain to samples with large areas to be used in other tests--for example, tests to determine the effect of different strain paths on the limiting strain levels
- Detection of defects, such as inclusions, by straining a sample of large area to a uniformly high level. Defects will cause early localized fracture, usually parallel to the rolling direction

Hydraulic Bulge Test. The periphery of a sheet metal sample is clamped between circular or elliptical die rings, and hydraulic pressure is applied on one side of the sample to deform it into a dome, as shown in Fig. 15. The edge of the sample is prevented from slipping by a lock bead placed in the die rings. This consists of a ridge with small radii on one ring and a matching groove on the other.

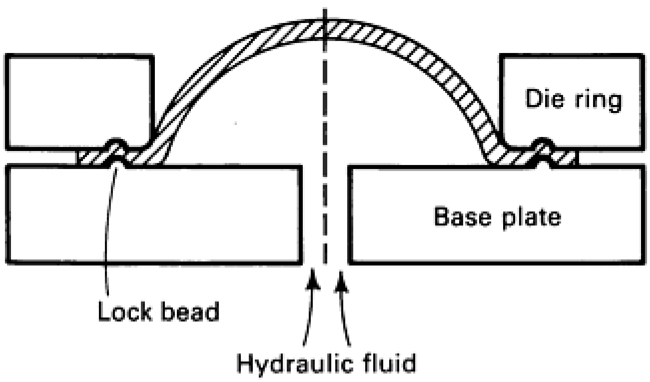

Fig. 15 Schematic of hydraulic bulge test.

With circular die rings, the center of the dome has been found to be nearly spherical (Ref 40). The stress and strain states in this region can be determined from the curvature and extension and the fluid pressure. A biaxial test extensometer has been developed that measures the extension and curvature by means of a spherometer and an extensometer that are in direct contact with the dome (Ref 41).

More recently, a system for controlling the strain rate in this test has been developed (Ref 42) because significantly different test results have been obtained (Ref 43) under conditions of constant strain rate and constant fluid flow. The system uses feedback from the extensometer signal to operate a servo-valve, which controls the flow of hydraulic oil to the bulge. This system was used to determine the strain rate sensitivity of aluminum alloys to a much higher strain level than is possible in the tensile test.

A computerized system is available that uses electronic vision to measure the principal strains and closed-loop feedback to control the strain rate (Ref 44). This system monitors the relative positions of the centers of three closely spaced white dots painted on a black background at the center of the sample. Initially, the dots form a right-angled isosceles triangle. The principal strains are computed from the change in the spacings of the dots and changes in the angle they subtend. This information is recorded and also used to maintain a constant strain rate by controlling the hydraulic pressure. Strains can be computed only once per second, which limits the maximum controllable strain rate.

A camera is mounted so that it maintains a constant distance from the top of the dome and the dots, except for the effect of the curvature of the dome, which is negligible. The stress state is determined by measuring the curvature of the dome with a contacting spherometer and by measuring the hydraulic pressure with a strain gage pressure transducer.

For thin samples, bending stresses can be neglected, and the radial (meridional) stress, σ_r, is given by:

$$\sigma_r = \frac{pR}{2t} \quad \textbf{(Eq 20)}$$

where p is the hydraulic pressure, R is the radius of curvature, and t is the instantaneous thickness. The thickness is calculated from the measured strains using constancy of volume. At the top of the dome, the sample is in balanced biaxial stretching, and the circumferential stress, σ_c, is equal to the radial stress.

For convenience, it is customary to express the results of hydraulic bulge tests in terms of the true thickness stress and strain. This is done by theoretically superimposing a hydrostatic compressive stress that does not influence deformation and that has in-plane components equal to the actual radial and circumferential tensile stresses. This converts the stress state to a simple uniaxial thickness compressive stress, as shown in Fig. 16.

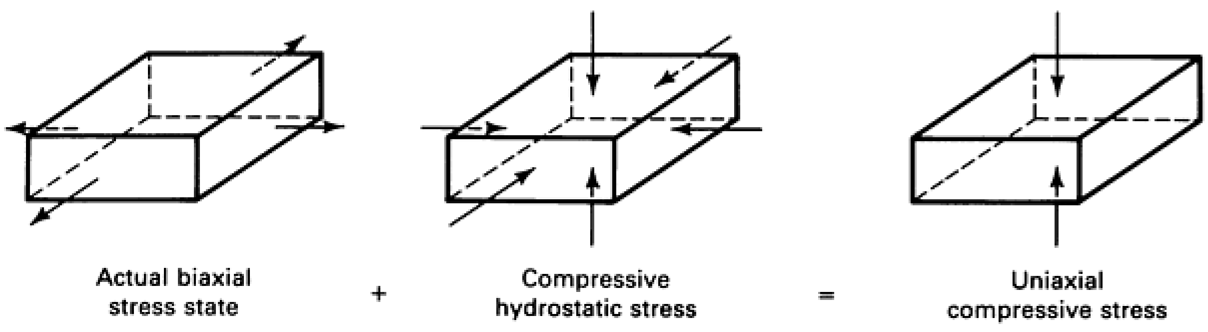

Fig. 16 Addition of compressive hydrostatic stress to biaxial tensile stress.

The true thickness strain, ε_t, can be obtained from the radial strain, ε_r, and circumferential strain, ε_c, by using the constancy of volume condition:

$$\varepsilon_t = - \varepsilon_r - \varepsilon_c \qquad \textbf{(Eq 21)}$$

This enables the results to be represented by a true compressive stress-strain curve in the thickness direction. The hydraulic bulge test has the following applications:

- Intrinsic material characterization in biaxial stretching, which is a very common strain state in production stampings
- Testing to much higher strain levels than those achievable in tensile testing (in some cases, by as much as a factor of ten), particularly for heavily cold-worked materials
- Checking the validity of plasticity theories that attempt to predict the yielding behavior of metals in all stress states from properties measured in uniaxial and plane-strain tensile testing

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Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Shear Testing

Two tests have been developed to determine the properties of sheet metals subjected to planar shear deformation: the Marciniak in-plane sheet torsion test (Ref 45) and the Miyauchi shear test (Ref 46).

Marciniak In-Plane Sheet Torsion Test. A flat 50 mm (1.97 in.) square sample is effectively divided into three zones: an inner circular zone, which is clamped; a ring-shaped middle zone, which surrounds the inner zone and is free to deform; and an outer ring-shaped zone, which is clamped. The inner zone is rotated in its plane relative to the outer zone, which deforms the middle zone in shear. The sample is deformed to fracture, and the angular rotations at two radii in the middle zone are measured by means of calibrated drums that rotate with the sheet. This is shown schematically in Fig. 17.

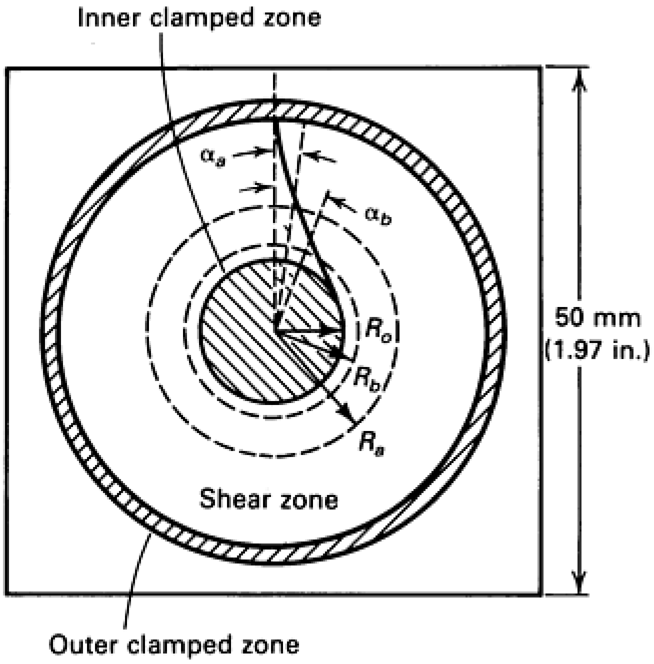

Fig. 17 Deformed Marciniak in-plane torsion test specimen.

For materials that follow the power law shear strain-hardening relationship:

$$\tau = C\gamma^n \quad \textbf{(Eq 22)}$$

where τ is the shear stress, C is a constant, and γ is the shear strain; it can be shown that (Ref 45):

$$n = \frac{2 \log \left(\frac{R_a}{R_b}\right)}{\log \left(\frac{\alpha_a}{\alpha_b}\right)} \quad \textbf{(Eq 23)}$$

where α_a and α_b are the angular displacements at radii R_a and R_b.

Shear fracture occurs where the shear stress is greatest--at the inner radius, R_o, of the deforming ring. The shear fracture strain, γ_f, is given by:

$$\gamma_f = \frac{2\alpha_a \left(\frac{R_a}{R_o}\right)^{2/n}}{n} \quad \textbf{(Eq 24)}$$

This test enables the forming properties of sheet metals to be determined at much higher strain levels than is possible in the uniaxial tensile test and in a different strain state, that is, in shear.

The Miyauchi shear test determines the properties of sheet metals in planar shear deformation by means of a modified tensile technique (Ref 46). The test uses flat, rectangular specimens whose ends are divided into three equal sections by parallel longitudinal slits, as shown in Fig. 18(a).

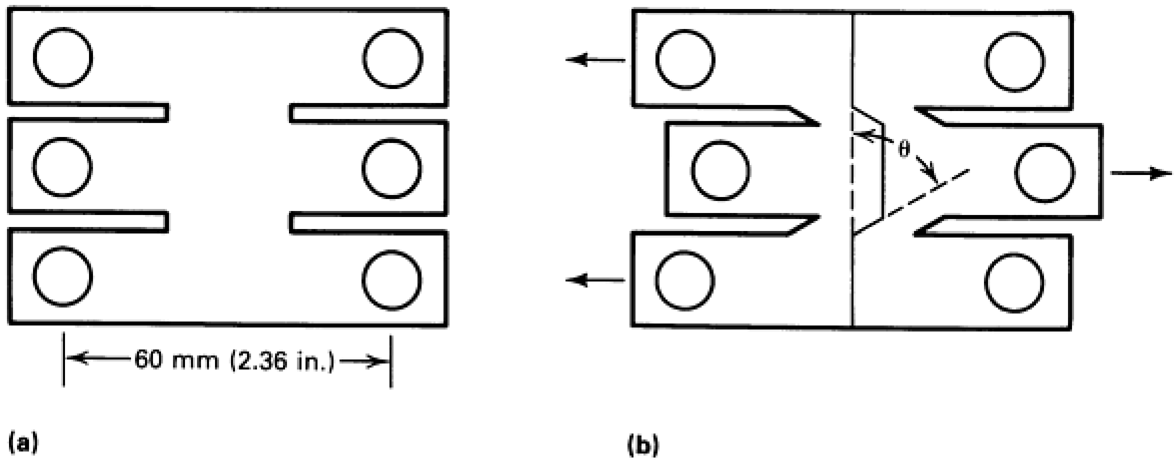

Fig. 18 Miyauchi shear test specimen. (a) Undeformed. (b) Deformed. Source: Ref 46.

The specimen is clamped in a fixture that prevents out-of-plane deformation. The inner and outer sections are then pulled in opposite directions in a tensile machine. This produces a shear stress in the regions between the inner and outer sections and deforms the specimen, as shown in Fig. 18(b). The deformation in these regions is uniform, except at the ends.

The shear strain, γ, is the tangent of angle θ, which is the change in direction of lines scribed across the specimen as they pass through the shear zone, as shown in Fig. 18(b). The strain can also be determined from the displacement of the inner section once a relationship has been established between the displacement and θ, which must be done for each type of sheet metal tested. Shear stress-strain curves are given in Ref 46 for three different steels. These curves show differences in the strain dependence of the work-hardening coefficient from that in the tensile test.

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Hardness Testing

Hardness, or the resistance to indentation by a concentrated load applied by a suitable indenter, has been used in many stamping plants as a measure of formability. Generally, formability decreases with increasing hardness, but the fine-scale correlation between these properties has not been reliable. The test can be used effectively to monitor changes in a particular grade of material caused by changes in processing that may affect formability.

For steels, hardness measurements correlate well with yield strength values (Ref 47). Therefore, hardness testing is useful in quality control to ensure that the material in use is the specified grade and has the required strength level.

The Rockwell hardness test (ASTM E 18), which is described in detail in the article "Rockwell Hardness Testing" in *Mechanical Testing*, Volume 8 of *ASM Handbook*, formerly 9th Edition *Metals Handbook*, is typically used to determine the hardness of such materials. The load and indenter must be selected for the gage and hardness range of the material according to the test specification to ensure that the indentation is the appropriate size. If the indentation is too deep in a sheet sample, the reading will be artificially high because of the influence of the supporting anvil. This becomes a more serious consideration with the use of thinner gage sheet metal in many industries. For thicker and harder materials, the Rockwell B scale is commonly used, and for thinner or softer materials, the Rockwell 30T superficial scale is used.

Hardness readings are influenced by the degree of flatness and surface conditions. In addition, the presence of cold-worked surface layers can cause unrepresentatively high hardness readings that suggest a lower formability level than actually exists.

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Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Simulative Tests

For many forming operations, tests that simulate the operation are more useful and relevant than fundamental intrinsic property measurement tests. These tests subject the work material to deformation that closely approximates the production operation, including the effects of factors not present in the intrinsic tests, such as bending and unbending and friction between the work materials and die surfaces. Because these additional factors are present, simulative tests tend to be less reproducible than intrinsic tests and must be performed under carefully controlled conditions to minimize variability in the results.

Simulative tests can be classified on the basis of the predominant forming operation involved: bending, stretching, drawing, and stretch-drawing. In addition, tests have been developed to measure wrinkling and the springback that occurs after bending or another forming operation.

Bending Tests

Two types of bending tests relate to sheet metal forming: simple bending and stretch-bending tests. Simple bending tests are useful in predicting how the sheet metal will perform when bent without tension, as in a hemming operation. Stretch-bending tests relate to the response to combined bending and stretching, as when sheet metal is pulled over a punch or die radius.

Simple bending tests can be performed in various ways (ASTM E 290). The simplest method for thin sheet material is to clamp a specimen and a bending die in a vise, as shown in Fig. 19, and to bend the specimen over the die manually or with a nonmetallic mallet.

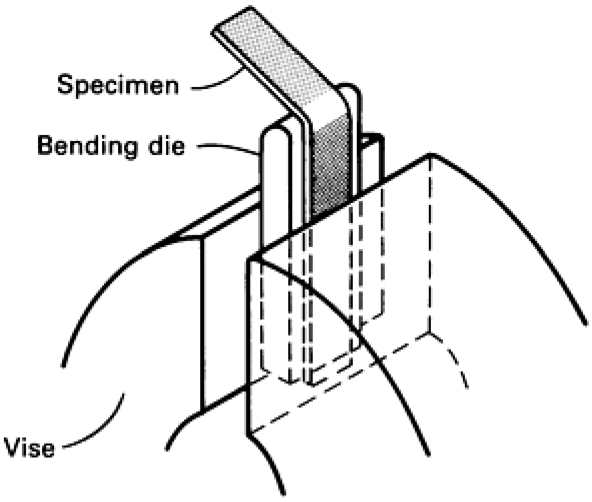

Fig. 19 Schematic of simple bending test.

If the specimen bends through 180° without fracturing or cracking, the experiment is repeated using a bending die of smaller radius. A modified test is performed for highly ductile metals that have extremely small bend radii. The specimen is initially bent at its midpoint, through less than 90°, over a small radius. The test is then completed by pressing the ends of the specimen together between flat platens without a bending die placed between the platens.

The ratio of specimen width to thickness should be greater than 8 to 1, and sheared edges should be machined, filed, or sanded to remove the heavily cold-worked metal present. The orientation of the specimen with respect to the rolling direction may be important because it affects the resistance of the specimen to fracture. Specimens cut perpendicular to the rolling direction usually require a larger bend radius and therefore provide a more conservative measure of this property.

For low-carbon sheet steels, the minimum bend radius is usually not a limiting factor. For high-strength steels and aluminum alloys, it sometimes is, and methods such as rope hemming, which increase the bend radius, have been developed to prevent cracking during the hemming of these materials.

Stretch-Bending Tests. A rectangular strip of sheet metal is clamped at its ends in lock beads and deformed in the center by a punch, as shown in Fig. 20. There are two types of stretch-bending tests: the hemispherical test, in which a hemispherical-tipped punch and a concentric circular lock bead are used, and the angular test, in which a wedge-shaped punch and straight parallel lock beads are used. The hemispherical test involves a range of strain states. The angular test produces the plane-strain state.

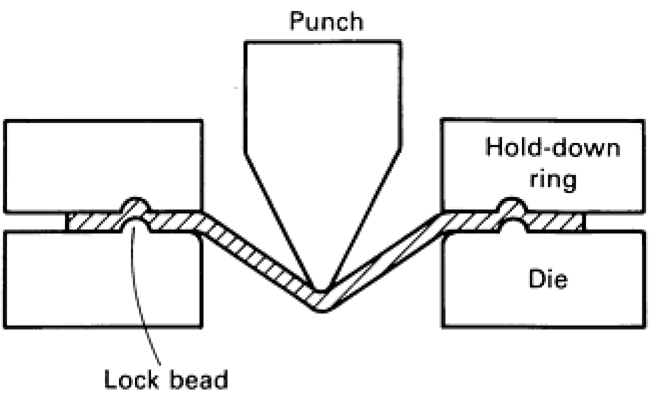

Fig. 20 Schematic of stretch-bending test.

The punch travel between initial contact and specimen fracture is measured. The conditions are chosen so that fracture occurs in the region of punch contact. When fracture occurs in the unsupported region, which tends to happen with narrow thin-gage specimens and large punch radii, the test effectively becomes a tensile test.

The results of several hemispherical and angular stretch-bending tests on three types of steels and an aluminum alloy have been reported (Ref 48). For the hemispherical test, the effects of variations in punch tip radii ranging from 3.2 to 51 mm (0.13 to 2.0 in.), in sheet thicknesses ranging from 0.5 to 3.3 mm (0.02 to 0.13 in.), and in specimen widths ranging from 25 to 203 mm (1.0 to 8.0 in.) were investigated in the dry and lubricated conditions. The tests showed that the height at fracture increased with increasing punch radius and sheet thickness and with the use of lubricants. It decreased with increasing specimen width in the range of 102 to 203 mm (4.0 to 8.0 in.), in which fracture occurred in the region of punch contact. The ranking of two of the steels was found to be dependent on specimen thickness.

Fewer conditions were investigated in the angular test. The results for a 76 mm (3.0 in.) wide specimen and punch radii ranging from 1.6 to 6.4 mm (0.06 to 0.25 in.) showed much greater heights than for the same conditions in the hemispherical test. Increases in height with increasing punch radius were also evident, but in contrast to the hemispherical case, a decrease with increasing thickness was observed. Preliminary correlation between the results of these tests and production experience is reported to be fairly good.

Data from the angular stretch-bending test have been analyzed and indicate that fracture occurs at a constant limit strain that is independent of sheet thickness and punch radius (Ref 49). Stretch-bending tests are useful for material selection and for predicting the effects of material substitution and gage reduction in many forming operations.

Stretching Tests

Historically, ball punch tests, such as the Olsen cup test and Erichsen cup test, have been used to determine the properties of sheet metals in stretching. These tests stretch a specimen over a hardened steel ball and measure the height of the cup produced. More recently, tests that stretch the specimen over a much larger hemispherical dome have been developed, including the limiting dome height test, which uses specimens of different widths to control the strain ratio at fracture.

Many forming operations involve stretching an edge of a part or a cutout (hole) in a part. For example, when a concavely contoured edge is flanged, the metal is stretched. The ability of the material to undergo this type of forming operation can be measured by the hole expansion test. In this test, a cylindrical, hemispherical, or conical punch is pushed through a circular hole of smaller diameter in the specimen. This initially increases the diameter of the hole and then forms a rim of stretched metal. The edge ductility of the material is indicated by the amount of hole expansion that occurs without edge cracking.

Ball Punch Tests. The Olsen and Erichsen cup tests are similar, differing principally in the dimensions of the tooling used. The Olsen test (ASTM E 643) uses a 22.2 mm (0.875 in.) diam hardened steel ball and a die with a 25.4 mm (1.0 in.) internal diameter (28.6 mm, or 1.125 in., for gages over 1.5 mm, or 0.06 in.) and a 0.81 mm (0.032 in.) die profile radius, as shown in Fig. 21. The Erichsen test, which is extensively used in Europe, uses a 20 mm (0.79 in.) diam ball and a die with a 27 mm (1.06 in.) internal diameter and a 0.75 mm (0.03 in.) die profile radius.

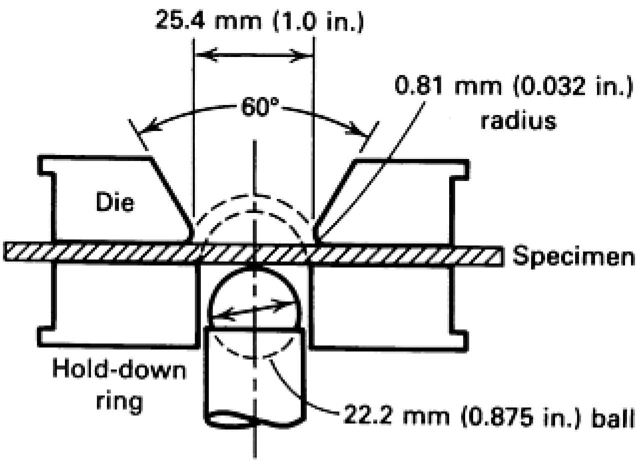

Fig. 21 Schematic of Olsen cup test.

In both tests, the cup height at fracture is used as the measure of stretchability. The preferred criterion for determining this point is the maximum load. When this cannot be determined, the onset of a visible neck or fracture can be used, but this yields a slightly different value. The cup height measured by means of a visible fracture is 0.3 to 0.5 mm (0.012 to 0.020 in.) greater than the height measured at the maximum load.

These tests, as indicators of stretchability, should correlate with the *n* value, but the correlation is not satisfactory. Improved correlations with the total elongation (Ref 50) and reduction in area (Ref 51) have been reported. Some investigators have reported poor reproducibility of results in the Olsen and Erichsen tests and poor correlation with production experience (Ref 52, 53). Satisfactory reproducibility and correlation in specific cases have been reported when experimental conditions were carefully controlled (Ref 50).

The variability in tests has been attributed to the small size of the penetrator, uncontrolled drawing-in of the flange, and inconsistent lubrication (Ref 52, 53). The small size of the penetrator leads to excessive bending, particularly in thicker sheet, and is generally unrepresentative of production conditions. Drawing-in can be controlled somewhat by standardizing the specimen size and by using a high (~71 kN, or ~8 tonf) clamping force. Even greater control can be achieved by using lock beads or serrated dies (dies with concentric circular ridges of triangular cross section that dig into the specimen and prevent slippage).

Consistent lubrication can be achieved by using oiled polyethylene between the specimen and penetrator. The problems with the Olsen and Erichsen tests have led to the development of stretching tests that use a much larger diameter punch and a lock bead to prevent drawing-in.

Hemispherical dome tests using 50.8, 76.2, and 101.6 mm (2.0, 3.0, and 4.0 in.) punches have been reported (Ref 52, 53). A 100 mm (3.94 in.) test is the most widely used. Typical tooling designed for this test is shown in Fig. 22. The lock bead, in combination with a hold-down force of about 222 kN (25 tonf), completely prevents drawing-in of the flanges.

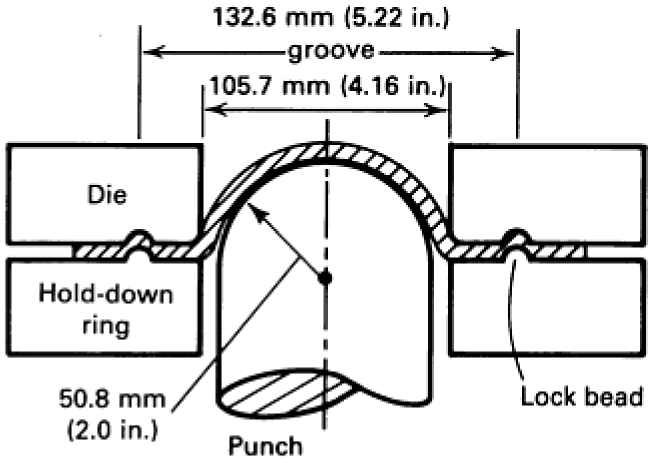

Fig. 22 Tooling for the 101.6 mm (4.0 in.) hemispherical dome test. Source: Ref 52.

The specimens fracture circumferentially at a distance (for lightly lubricated low-carbon steel) of 35 to 40 mm (1.38 to 1.57 in.) from the pole, at which point the radial strain peaks sharply. The circumferential strain varies gradually from a maximum of 10 to 20% at the pole to zero at the lock bead.

The hemispherical dome test yields more reproducible results than the Olsen and Erichsen cup tests. For low-carbon steels, the dome height, which is measured at the point of maximum load, increases linearly with the *n* value. For a wide range of material (including brasses, aluminum alloys, and zinc), optimal correlation is found between the dome height and the total elongation, which incorporates the effects of strain rate hardening and limiting strains.

Overall, the use of lubrication in hemispherical dome tests is beneficial. A thin layer of a standard lubricant, applied in a consistent manner, reduces scatter in test results, simulates production conditions more closely, reduces damage to the tooling, and simplifies specimen preparation. The improved sensitivity obtained in the dry condition is negated by the increased scatter in the results.

The use of lubrication makes the strain ratio at fracture more biaxial. This is undesirable for production simulation, because most production failures occur in the region of plane strain, that is, in a less biaxial manner. To control the strain ratio at fracture, specimens of different widths were used (Ref 54). This technique has been developed further into the limiting dome height test (Ref 55, 56).

Limiting Dome Height (LDH) Test. Specimens of various widths are held in a circular lock bead and stretched over a 100 mm (3.94 in.) dome using tooling of the type shown in Fig. 22. In principle, this test can be used to duplicate a large range of production failure strain states and to select the most suitable material for each particular operation. In practice, most production failures occur close to plane strain, which is generally the strain state at the minimum on a plot of dome height versus specimen width. Consequently, attention has concentrated on this minimum value.

When testing a new material, initial tests should be performed to determine the specimen width that yields the minimum dome height, or LDH value, and the corresponding minor strain. Once this has been established, tests can be conducted at this width only. For low-carbon steels, the minimum dome height occurs at a width of approximately 124 mm (4.9 in.). This can also be used as an approximation for other materials. Increments in test specimen width of ±3 mm (±0.12 in.) are sufficiently close.

It has been found that, for specimens lubricated lightly with a wash oil, the dome height increases with decreasing hold-down force below about 250 kN (28 tonf). This is attributed to the drawing-in of the flange. Therefore, a hold-down force of at least 250 kN (28 tonf) should be used. The limiting dome height is taken as the height at which the maximum load occurs.

Preliminary tests have shown a correlation between the limiting dome height test and production stamping performance (Ref 57). Some problems have been encountered with test reproducibility over a period of time and among different test facilities. Numerous attempts have been made to determine a correlation between the limiting dome height test and mechanical and forming property measurements. The dome height depends on the ability of the material to distribute strain and on the limiting strain level and would therefore be expected to correlate with the total elongation. Correlation for a range of different materials has been reported (Ref 58).

The specimens used in the limiting dome height test can be sheared or blanked from the sheet sample, and the test can be performed rapidly on equipment that automatically measures the dome height at the maximum punch load. The test has considerable potential for production control and research applications.

Hole Expansion Test. A flat sheet specimen with a circular hole in the center is clamped between annular die plates and deformed by a punch, which expands and ultimately cracks the edge of the hole. Flat-bottomed hemispherical and conical punches have been used, and in some cases die plates have been equipped with lock beads to prevent drawing-in of the flange. The punch should be well lubricated and should have a large profile radius. A spacer can be used between the punch and the sample, as in the Marciniak test. Figure 23 illustrates the hole expansion test using a flat-bottomed punch.

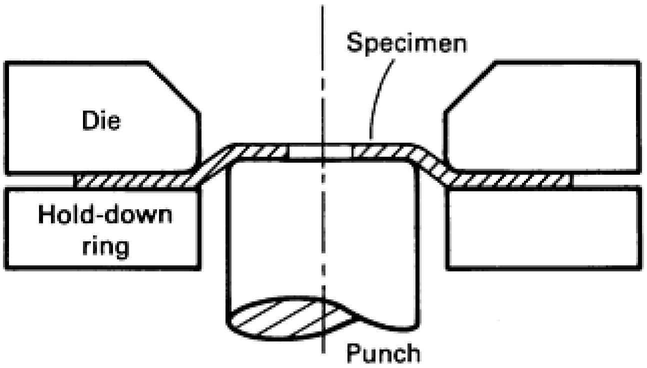

Fig. 23 Schematic of the hole expansion test with a flat-bottomed punch.

The test is terminated when a visible crack is observed, and the hole expansion is expressed as the percentage of increase in hole diameter:

$$\text{Hole expansion (\%)} = \frac{100(D_f - D_o)}{D_o} \quad \textbf{(Eq 25)}$$

where D_o and D_f are the initial and final hole diameters, respectively.

The results of several hole expansion tests on eight different types of steel are reported in Ref 59. Square specimens measuring 203 mm (8.0 in.) on each side with a 25 mm (1.0 in.) diam punched hole, a 101.6 mm (4.0 in.) diam hemispherical punch, and die plates with a 2 mm (0.08 in.) radius lock bead were used. The measured hole expansion ranged from 24 to 82% for steels with yield strengths ranging from 253 to 537 MPa (36.7 to 77.9 ksi).

In most cases, removing the burr and cold-worked metal from the edge of the punched hole increased the hole expansion considerably. The hole expansion also increased with increasing total elongation and r_m value and decreased with increasing tensile strength (which was anticipated, because total elongation decreases with increasing tensile strength). Inclusions were observed in crack locations, and inclusion shape control improved hole expansion performance.

Drawing Test

Swift Cup Test. The most commonly used test for deep drawability is the Swift cup test. Circular blanks of various diameters are clamped in a die ring and deep drawn into cups by a flat-bottomed cylindrical punch. The standard tooling for this test is shown in Fig. 24. Drawability is expressed as either the limiting draw ratio (LDR) or the percentage of reduction. The limiting draw ratio is the ratio of the diameter, *D*, of the largest blank that can be successfully drawn to the diameter, *d*, of the punch:

$$\text{LDR} = \frac{\text{maximum blank diameter}}{\text{punch diameter}} = \frac{D}{d} \quad \textbf{(Eq 26)}$$

Percentage of reduction is defined as:

$$\text{Percentage of reduction} = \frac{100(D - d)}{D} (\%) \quad \textbf{(Eq 27)}$$

Cup height, *h*, is approximately (Ref 60):

$$h = \frac{(D^2 - d^2)}{4d} \quad \textbf{(Eq 28)}$$

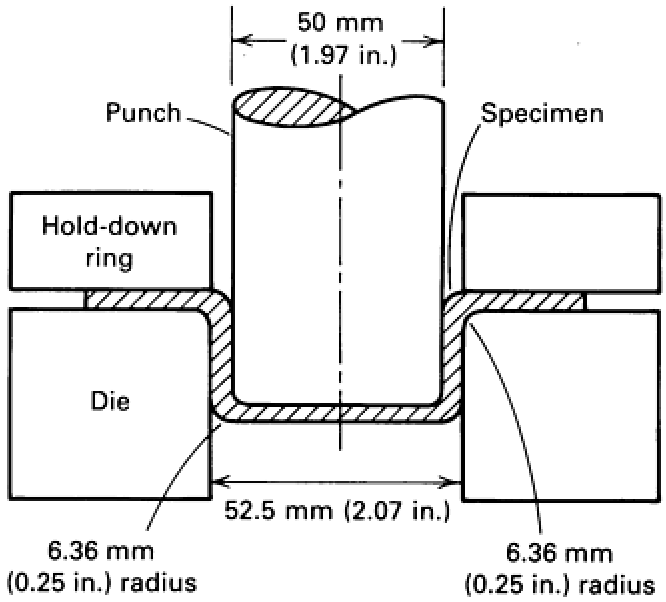

Fig. 24 Standard tooling for the Swift flat-bottomed cup test.

An alternative method for determining the limiting draw ratio uses blanks of a single diameter, which is less than the critical diameter in the standard test (Ref 61). The blanks are drawn to the maximum load, which usually takes place before 50% of the draw has occurred. The clamping force is then increased to prevent further drawing-in of the flange, and the load is increased to the point of fracture. The limiting blank diameter (LBD) is defined by:

$$\text{LBD} = \left[\frac{\text{Fracture load} \cdot (\text{blank diameter} - \text{die diameter})}{\text{Maximum drawing load}}\right] + \text{Die diameter} \quad \textbf{(Eq 29)}$$

The limiting draw ratio is given by:

$$\text{LDR} = \frac{\text{LBD}}{\text{Punch diameter}} \quad \textbf{(Eq 30)}$$

This method has been shown to correlate well with the standard test for a range of materials of widely different drawability (Ref 61).

The limiting draw ratio increases with normal anisotropy (r_m) and thickness, particularly at the low ends of the ranges for these variables, but is not sensitive to the *n* value (Ref 62). The limiting draw ratio also increases as the punch profile radius increases up to about eight times the sheet metal thickness, as the die profile radius increases up to about 12 times the metal thickness, and as the punch speed increases. The height of the ears formed in this test is proportional to the Δr value.

Too low a blankholder force may cause wrinkling, and too high a blankholder force may cause fracture at the punch profile radius. The die rings should be well lubricated, but the punch should not be lubricated. By not lubricating the punch, the amount of stretching that occurs over the punch profile radius and the tendency for splitting to occur at this location are reduced.

Stretch-Drawing Tests

Many forming operations involve stretching and drawing; for example, square cups have drawn corners and stretched sides. The ratio of stretching to drawing in an actual part can be measured by a shape analysis technique (Ref 63). A line is drawn from a reference point (for example, the center of the blank) to the edge of the blank, through the critical forming area. After forming, the ratio of the increases in length of this line inside and outside the initial die contact line is taken as the ratio of stretching to drawing. Two tests are commonly used for stretch-drawing: the Swift round-bottomed cup test and the Fukui conical cup test.

The Swift round-bottomed cup test resembles the Swift flat-bottomed cup test described above. However, the top of the punch is hemispherical, which causes stretching in the center of the specimen in addition to the drawing-in of the flange to produce the wall of the cup.

This test was used to evaluate 50 different steels with a 50 mm (1.97 in.) diam punch and 127 mm (5.0 in.) diam specimens and with a 65 mm (2.56 in.) diam punch and 165 mm (6.5 in.) diam specimens (Ref 64). Hold-down forces of 490 and 981 N (110 and 220 lbf), respectively, were used at a test speed of 1 mm/s (0.04 in./s). Both sides of the specimens were lubricated with thin polyethylene sheet.

The end point of the test is determined by observing fracture visually or by detecting a drop in the punch load. Multiple regression analysis of the test results showed that the cup height at fracture increased linearly with increases in the r_m value, n value, and metal thickness.

To determine the correlation between performance of the steels in the stretch-drawing test and in actual parts production, 4 automotive stampings were made, using 12 different steels for each. The Stampings had stretch-to-draw ratios ranging from approximately 1 to 5 to 2 to 1, and minor-to-major strain ratios in critical areas ranging from -0.3 to +0.45. The correlation coefficients between the test and stamping results had an average value of 0.92 and ranged from 0.89 to 0.94 (a value of 1.00 indicates perfect correlation). In another trial on a stamping with a stretch-to-draw ratio of 4.5 to 1, the test results did not correlate. These tests indicate that for parts that involve both stretching and drawing, without excessive stretching, the Swift flat-bottomed cup test is useful as a quality control tool.

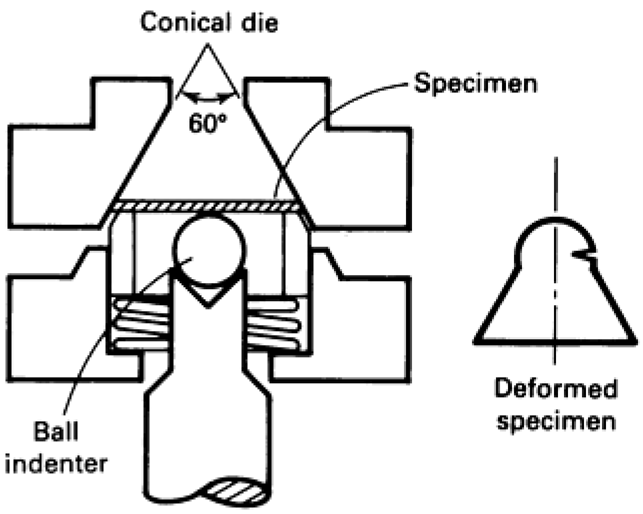

Fig. 25 Schematic of the Fukui conical cup test. Source: Ref 62.

Fukui Conical Cup Test. In the Fukui conical cup test, circular specimens punched from a sample of sheet metal are deformed into conical cups by means of a 12.5 to 27 mm (0.5 to 1.1 in.) diam ball and tooling of the type shown in Fig. 25 (Ref 62, 65, JIS Z 2249). The ball size depends on the sheet thickness. The specimens are lubricated on the die side only. Lubrication on the punch side leads to tilting of the specimens. Specimens are centered and held in place by the hold-down ring and deformed to fracture by the punch.

The diameter of the base of the conical cup formed is measured and divided by the diameter of the original specimen to give the Fukui conical cup value. The end point of the test is not critical, because the diameter of the cone does not change after fracture. A constant punch travel is usually used. When the test material has a high level of planar anisotropy (a high Δr value), the conical cup is asymmetric, and an average diameter must be determined. A high correlation between the Fukui conical cup value and the product of the average n value and the average r value has been reported for low-carbon steels (Ref 62).

An alternative method has been developed for performing this test (Ref 50). The punch travel between the initial contact with the specimen and the onset of a drop in the punch load, which coincides with the formation of a visible neck, is measured and used instead of the ratio of the diameters. This value, known as the formability index, correlates with the uniform elongation and therefore with the *n* value for low-carbon steels.

Wrinkling and Buckling Tests

Two principal types of tests are used for wrinkling and buckling: the conical cup wrinkling test and the Yoshida buckling test. The conical cup wrinkling test is similar to the Swift flat-bottomed cup test, but uses a punch that is much smaller than the die opening. Consequently, the cup wall is conical and is not in contact with the punch. Under some conditions, wrinkles form in the cup wall. In the Yoshida buckling test, a flat, square specimen is stretched slightly in the diagonal direction, and the height of the buckle that is formed is measured (Ref 66).

Conical Cup Wrinkling Test. A circular blank is clamped between annular dies and deformed by a flat-bottomed punch with a diameter that is typically about 75% of the internal diameter of the die. This procedure is illustrated in Fig. 26. At very low levels of hold-down force, wrinkling occurs in the flange. At higher levels, flange wrinkling is suppressed, but wrinkling occurs in the unsupported wall. This is caused by compressive stresses in the circumferential direction (hoop stresses) that are due to the local reduction in diameter as drawing progresses. For example, with a 75 mm (2.96 in.) diam punch and a 100 mm (3.94 in.) diam die, the top of the wall has a diameter of 100 mm (3.94 in.). If the cup depth is doubled, the original top of the wall becomes the new midpoint and must decrease in diameter to 87.5 mm (3.44 in.).

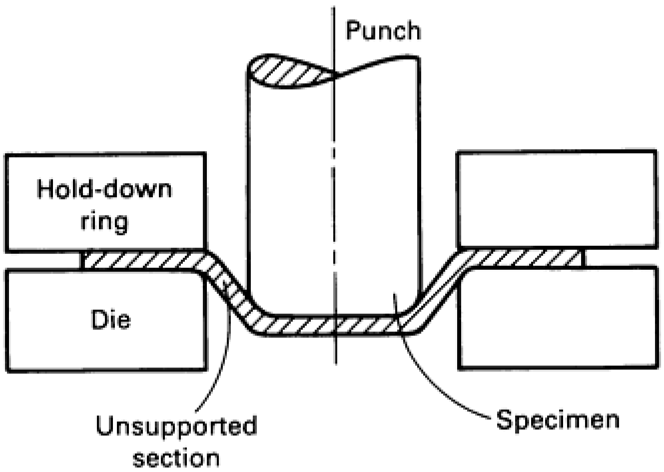

Fig. 26 Schematic of the conical cup wrinkling test.

At high levels of blankholder force, the tensile stresses in the radial direction in the wall prevent the formation of wrinkles, and fracture at the punch or die radius becomes the limiting factor. The maximum cup height occurs at the intersection of the wall wrinkling and fracture limits, as shown in Fig. 27.

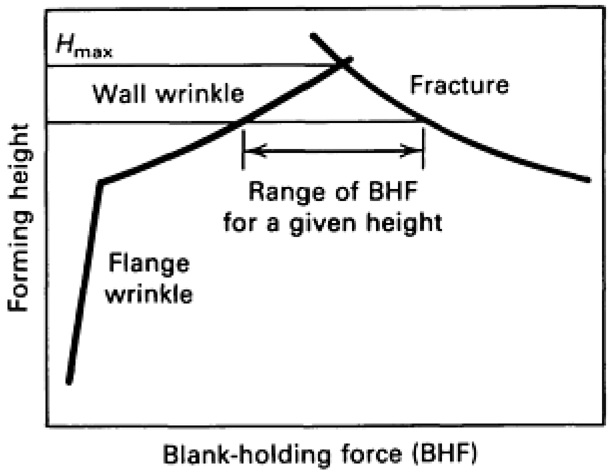

Fig. 27 Wrinkling and fracture limits in conical cup drawing. Source: Ref 67.

The results of experiments on several types of steel with different thicknesses and tooling of various dimensions have been reported in Ref 13 and 68. Wrinkling occurred in the unsupported wall when the true compressive hoop strain exceeded a certain value for each level of the tensile radial strain for all tooling geometries and forming conditions. The critical wrinkling strains were plotted on the forming limit diagram, as shown in Fig. 28.

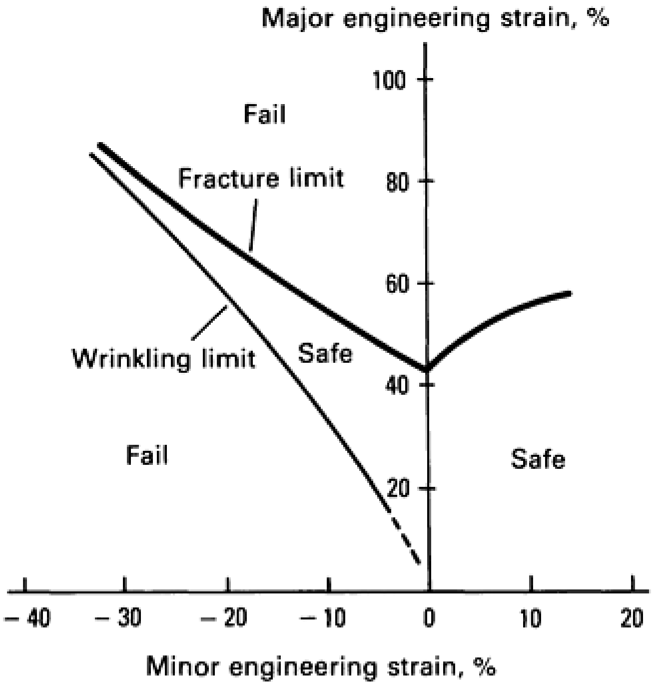

Fig. 28 Combined forming and wrinkling limit diagram. Source: Ref 68.

Attaining the critical wrinkling strain is strongly influenced by the dimensions of the specimen and tooling, lubrication, and the hold-down force. Changes in these variables that reduce the radial stress (that is, an increase in the die radius, improved lubrication, or a reduction in the blank diameter or the hold-down force) increase the tendency toward wrinkle formation.

Material properties that affect wrinkling in the conical cup test are the r_m, Δr, and n values and the ratio of the flow stress to the elastic modulus. A high r_m value and low Δr value reduce wrinkling, which initiates in the directions of lowest r

value. A high *n* value enables the hold-down force to be increased, which increases the radial force and reduces wrinkling. A low flow-stress-to-elastic-modulus ratio also reduces wrinkling.

Yoshida Buckling Test. A flat, square specimen is gripped at opposite corners and pulled in tension in the diagonal direction, as shown in Fig. 29 (Ref 66, 67). The standard specimen is 100 mm (3.94 in.) square with 41 mm (1.6 in.) wide grips and a gage length of 75 mm (2.95 in.). The buckle height is measured over a 25.4 mm (1.0 in.) width at the center of the specimen.

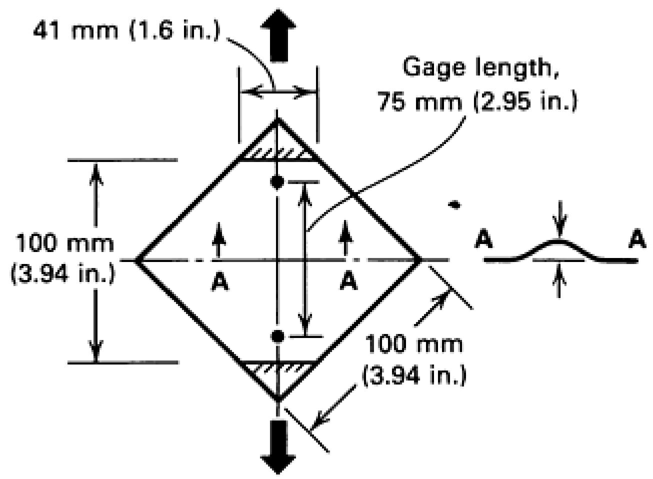

Fig. 29 Schematic of the Yoshida buckling test. Source: Ref 67.

Nonuniform stresses are generated in the specimen, and these stresses cause a buckle to form in the center along the direction of loading. The height of the buckle at a given elongation, for example, 2%, is used as the measure of buckling.

Several investigations have been conducted on the correlation between buckle height and test material properties. The Yoshida buckling test and a conical cone wrinkling test (using a hemispherical punch) were performed on several ferrous and nonferrous materials in different tempers (Ref 69). A direct correlation for both tests between the buckling or wrinkling height and the yield strength, an inverse correlation with the work-hardening exponent, and a lack of correlation with the normal anisotropy were reported. The Yoshida test was not successful for aluminum, because the specimens fractured before buckling.

The Yoshida test was performed on 31 steels of different types and thicknesses, and correlations between the slope of the buckle height versus elongation curve, which is easier to determine than the height at a particular elongation, and the yield strength and the ratio of yield strength to tensile strength were obtained (Ref 14). An inverse correlation with the instantaneous (2%) strain-hardening exponent and a lack of correlation with the uniform elongation and normal anisotropy were also noted.

Springback Tests

Springback tests that bend a specimen about a mandrel and determine the change in the angle of bending upon removal of the bending load have been used as indicators of yield strength. This test was developed with a 12.5 mm (0.5 in.) radius mandrel, as shown in Fig. 30, for use as a quality control tool with sheet materials with thicknesses ranging from 0.15 to 0.38 mm (0.006 to 0.015 in.) (Ref 70). Previously, hardness measurements had been used for this purpose, but they were found to be insufficiently accurate for hard thin-gage steels and aluminum alloys, and they did not provide any information on anisotropy.

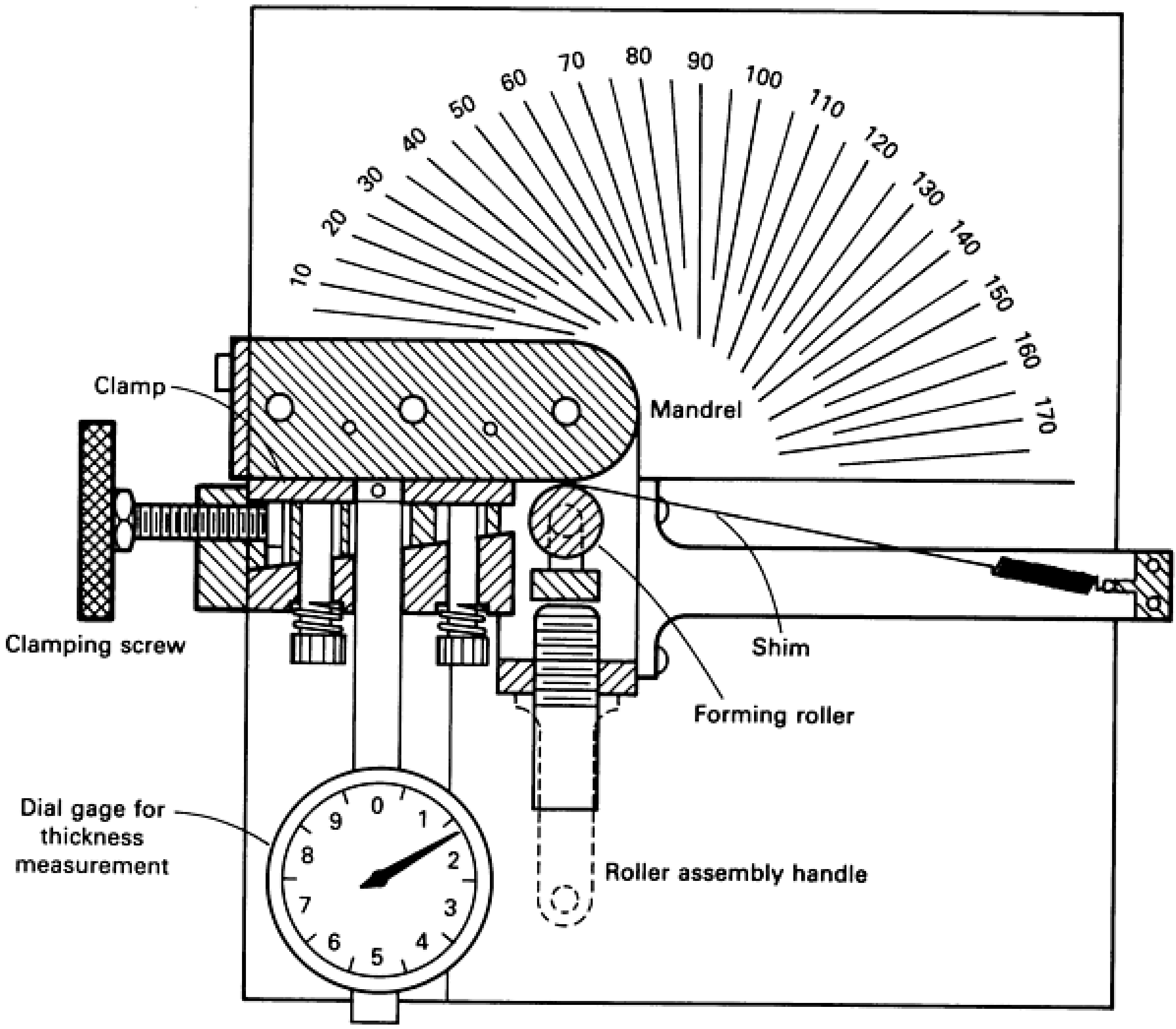

Fig. 30 Springback tester for determining yield strength. Source: Ref 70.

In the test, specimens are bent through 180° and released, and the angle of springback is read on the scale. The yield stress can then be determined from the springback angle and material thickness by means of a previously determined nomograph. Springback depends on the elastic modulus, which necessitates different nomographs for materials with different moduli. The test is most accurate in the range of springback angles of 60 to 120° and should be modified by changing the mandrel radius if the angle is less than 30° or greater than 150°.

The nomograph was calculated assuming an elastic/perfectly plastic stress-strain relationship, which is the same in tension and compression (that is, zero strain hardening and no Bauschinger effect). This calculation has been refined by using an average of the experimentally determined tensile and compressive stress-strain curves, including strain hardening (Ref 71). This improves the average ratio of the yield strength predicted using springback measurements to the tensile test yield strength from 0.80 to 0.91.

More recently, a similar test was developed that uses a larger-radius mandrel (19 mm, or 0.75 in.) (Ref 72). Twenty steels with thicknesses ranging from 0.56 to 2.36 mm (0.022 to 0.093 in.) and with tensile strengths ranging from 293 to 710 MPa (41.6 to 103.5 ksi) were tested. The measured springback correlated better with the forming strength, which is the average of the yield and tensile strengths, than with the individual strength values. For the same forming strength, steels with high (>1.5%) levels of yield point elongation develop less springback than those without. In these cases, the tangent modulus is almost zero in the region of yield point elongation.

Springback after a 90° flanging operation has been measured as a function of material flow stress, thickness, degree of cold work, and die radius and clearance for a low-carbon steel, two high-strength low-alloy steels, and a dual-phase steel

(Ref 73). Springback increased with increases in flow stress as well as die radius and clearance and with a decrease in material thickness.

For minimum springback, the ratio of the thickness to the die radius should be greater than 0.4, beyond which a further increase has little effect. The springback developed by the dual-phase steel increased more rapidly with the level of cold work than that developed by the high-strength steels, as anticipated from its higher strain-hardening exponent.

Springback after the combined effects of bending and stretching has been investigated using a tensile machine and a three-point bending fixture (Ref 19). Tests were performed on four thin-gage (~0.4 mm, or ~0.016 in.) low-carbon steels in various tempers with yield strengths ranging from 155 to 670 MPa (22.5 to 97.0 ksi). For applied tensile stresses that are below the yield strength of the material, the springback decreased linearly with the tensile stress to the same extent whether the stress was applied during or after bending.

For tensile stresses above the yield strength, tension during bending decreased the springback to a level that was independent of the initial yield strength and thickness. Stretching after bending, which deforms the entire cross section plastically in tension, decreases the springback progressively to extremely low levels.

Correlation Between Simulative Tests and Material Properties

Quantitative correlations between the results of simulative tests and select tensile properties have been determined for the Olsen cup, Swift flat-bottomed cup, and Fukui conical cup tests (Ref 50) and for the Swift round-bottomed cup test (Ref 64). In the first correlation, 48 materials were tested, including aluminum-killed, rimmed, and stainless steels and aluminum alloys in various tempers (Ref 50). Tensile properties included the directionally averaged percentage of total elongation, $\bar{e}_T$, to indicate the stretchability, and the normal anisotropy, r_m, to indicate drawability. The following relationships and correlation coefficients were obtained:

Test parameter	Relationship	Correlation coefficient
Olsen cup height/punch diameter	$0.217 + 0.00474\bar{e}_T + 0.00392r_m$	0.925
Limiting draw ratio (Swift)	$1.93 + 0.00216\bar{e}_T + 0.226r_m$	0.835
Formability index (Fukui)	$0.525 + 0.0134\bar{e}_T + 0.207r_m$	0.757

The Olsen test involves a much greater ratio of stretching to drawing than the Fukui test, which in turn involves a much greater ratio than the Swift flat-bottomed cup test.

In the second correlation, 50 different steels were tested (Ref 64), and the results were correlated with the average n and r values and thickness, t, as follows:

$$\frac{\text{Swift round-bottomed cup height}}{\text{Blank diameter}} = 0.0830t + 0.679\bar{n} + 0.0594r_m - 0.036 \quad \textbf{(Eq 31)}$$

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Formability Testing of Sheet Metals

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Determination of Forming Limit Diagrams

Forming limit diagrams indicate the limiting strains that sheet metals can sustain over a range of major-to-minor strain ratios. Two main types of laboratory tests are used to determine these limiting strains. The first type of test involves stretching test specimens over a punch or by means of hydraulic pressure--for example, the hemispherical punch method. This produces some out-of-plane deformation and, when a punch is used, surface friction effects. The second test produces only in-plane deformation and does not involve any contact with the sample within the gage length.

The first type of test has been used much more extensively (Ref 6, 74, 75, 76) than the second and provides slightly different results (Ref 33, 77). Good correlation has been obtained between forming limit diagrams determined in the laboratory and production experience.

The hemispherical punch method for determining forming limit diagrams uses circle-gridded strips of the test material ranging in width from 25.4 to 203 mm (1.0 to 8.0 in.) that are clamped in a die ring and stretched to incipient fracture by a 102 mm (4.0 in.) diam steel punch (Ref 74, 76). The narrowest strip fractures at a minor-to-major strain ratio of about -0.5, which is comparable to that obtained in a tensile test. As the strip width is increased, the strain ratio increases to a slightly positive value for a full-width specimen. Further increases in the ratio to a maximum value of +11.0 (balanced biaxial stretching) are achieved by using progressively improved punch lubrication (oiled polyethylene, oiled neoprene) and by increasing thicknesses of polyurethane rubber.

The strains are measured in and around regions of visible necking and fracture. The forming limit curve is drawn above the strains measured outside the necked regions and below those measured in the necked and fractured regions, as shown in Fig. 31.

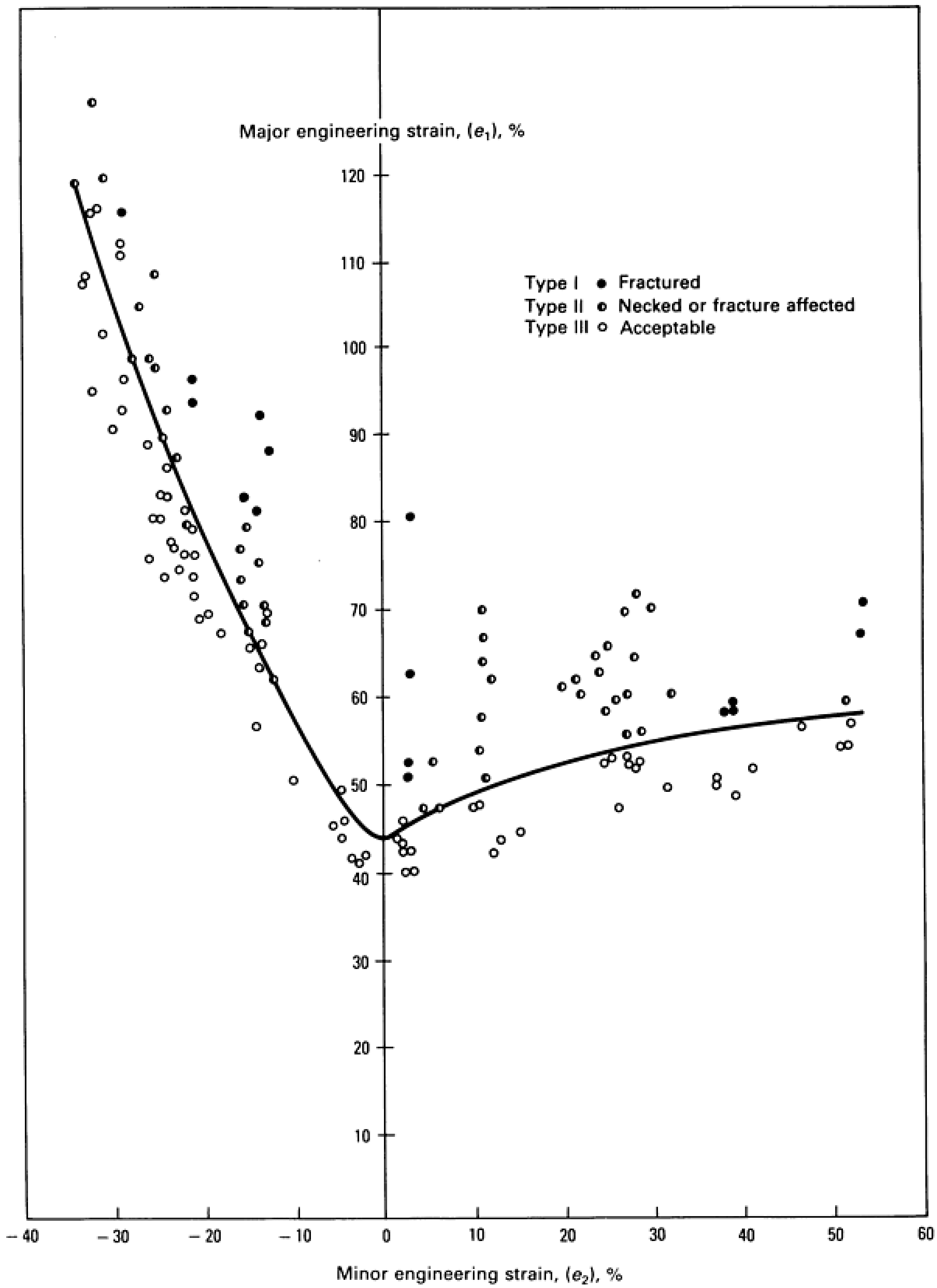

Fig. 31 Strain measurements and forming limit diagram for aluminum-killed steel. Source: Ref 76.

In-plane determination of the forming limit diagram can be achieved by using the uniaxial tensile test, rectangular sheet tension test, or Marciniak biaxial stretching test with elliptical and circular punches, as described earlier in this article. The forming limit curve can be determined over the full range of strain ratios, without introducing any out-of-plane deformation. A comparison of the in-plane and punch methods showed close agreement for negative strain ratios and slightly higher values in the punch test at plane strain and for positive strain ratios (Ref 33).

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Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

Circle Grid Analysis

Circle grid analysis is a useful technique for ensuring that a die is adequately prepared for production and for diagnosing the causes of necking and splitting failures in production (Ref 78). The forming limit diagram for the type and gage of work material selected must first be obtained. Arrays of small diameter (2.5 mm, or 0.1 in.) evenly spaced circles are printed or etched on several blanks in the critical strain regions, preferably in the same location on each blank. Some of the blanks are formed into parts, and the major and minor axes of the deformed circles are measured in the critical locations. The critical strain regions of the part are identified by visual observation of necking or splitting, or by previous experience with similar parts. The local strains are then calculated from the measured dimensions and plotted on the forming limit diagram.

If the maximum strains measured are close to or above the forming limit strains, problems with the tooling, lubrication, blank size or positioning, or press variables are indicated, whether or not necking or splitting actually occurs. Fluctuations that occur in operating conditions and in the properties of the work material over a production run will eventually cause failure if the material is strained to its full capacity. In this case, some of these variables should be changed until the circle grid strain measurements fall below the material limits by a given safety factor, such as 10%.

If the maximum strains measured are significantly below the limit strains and necking or splitting occurs, the batch of work material is substandard. The material used in die tryout must have typical, or slightly lower, forming properties than the production material. The use of superior material may indicate an adequate forming safety margin that will disappear when a more typical or lower formability material is used. It is good practice to form a few gridded blanks of a standard (nonaging) reference material periodically during a production run to determine the trends in the maximum strains. If the strains are approaching the maximum limits, corrective measures can be taken before any actual failures occur.

Circle Grids. Many types of circle grid patterns have been used, such as square arrays of contacting or closely spaced non-contacting circles and arrays of overlapping circles. The contacting and overlapping circles provide improved coverage, but are more difficult to measure manually and, at this time, cannot be measured automatically.

With small, closely spaced circles, it is possible to determine strain gradients accurately, provided the circles are not too small for accurate measurement. Circles with 2.5 mm (0.1 in.) diameters have been found to be a good size. Both open and solid circles have been successfully used, and automatic systems have been developed for measuring both types.

Applying Circle Grids to the Blanks. The circle grids can be applied to the blanks by a printing or photographic technique or by electrochemical etching. Printed and photographically applied circles are easily damaged and tend to rub off in areas contacted by the dies. This has led to general acceptance of etched circles.

In the electrochemical etching process, an electric stencil with the required grid pattern is placed on the blank and covered with a felt pad soaked in an etching solution. An electrode is placed on the pad, and a low-voltage (up to 14 V) current is passed between the electrode and the blank for a short time, usually less than 1 min. This produces a lightly etched and oxidized pattern on the surface of the blank. The stencils, etching solutions, and power supplies for this process are commercially available. Different metals require different solutions, levels and types of voltage, and etching times.

Measuring Strains From Deformed Circles. Deformed circles can be measured manually by means of dividers and a ruler, graduated transparent tapes, or a low-power microscope with a graduated stage. Automatic systems, known as grid circle analyzers, have also been developed for measuring the dimensions of the circles and calculating and displaying the major and minor strains (Ref 79, 80). These systems are now commercially available.

In regions of high curvature, the most accurate method of measurement is use of the transparent tape because it follows the contour of the part and measures the arc length, while the other methods measure the chord length. The tapes have a pair of diverging lines graduated to give direct readings of the strain, as shown in Fig. 32.

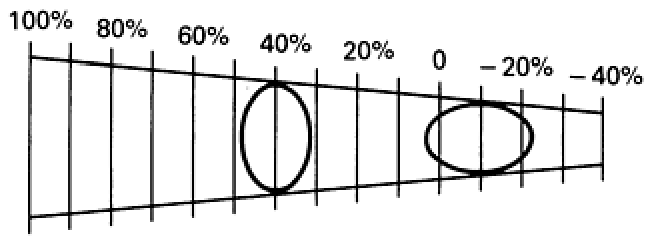

Fig. 32 Transparent tape measurement of deformed circles. Source: Ref 78.

Grid circle analyzers use a solid-state digital array camera with a built-in light source, a minicomputer, keyboard, CRT display, and printer. An image of a given deformed circle is displayed on the CRT, and a least squares curve fitting program selects the most suitable ellipse, which is displayed simultaneously. The major and minor strains, computed from the equation for the ellipse and the diameter of the original circle, are displayed on the screen and printed. A typical layout for the equipment is shown in Fig. 33.

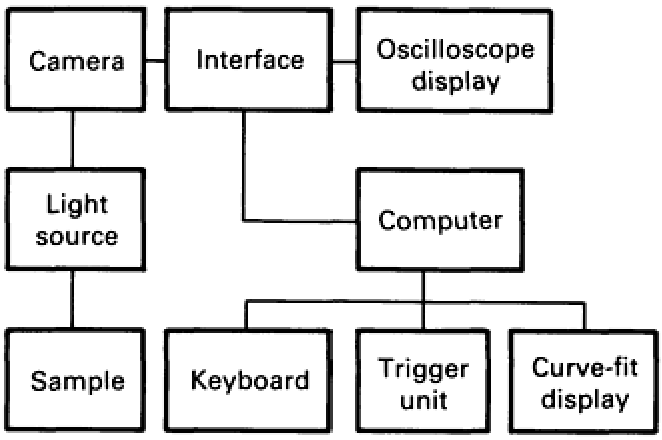

Fig. 33 Layout of a grid circle analyzer. Source: Ref 79.

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Drawbead Forces

It is common practice in production stamping operations to control the movement of the edges of the blank into the die cavity by means of drawbeads placed in the blankholder. These consist of a semicylindrical ridge in the upper part of the blankholder and a corresponding groove with rounded shoulders in the lower part, or a similar but opposite configuration. The drawbeads cause the periphery of the blank to bend and straighten three times as it passes through each bead, as shown in Fig. 34.

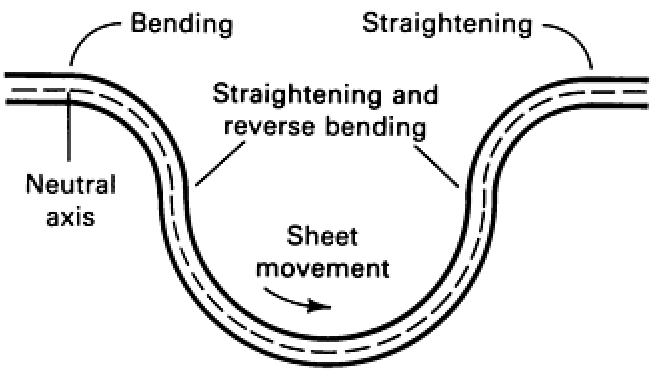

Fig. 34 Repeated bending and straightening of a blank edge in a drawbead.

The repeated bending and straightening produces a restraining force in addition to that caused by surface friction. A method has been devised for measuring the restraining force due to deformation independently of the effects of friction, using a drawbead simulator with low-friction rollers instead of a fixed bead and groove (Ref 81, 82). A second drawbead simulator with nonrotating parts can be used to measure the combined effects of friction and deformation. Figure 35 shows both types of simulators.

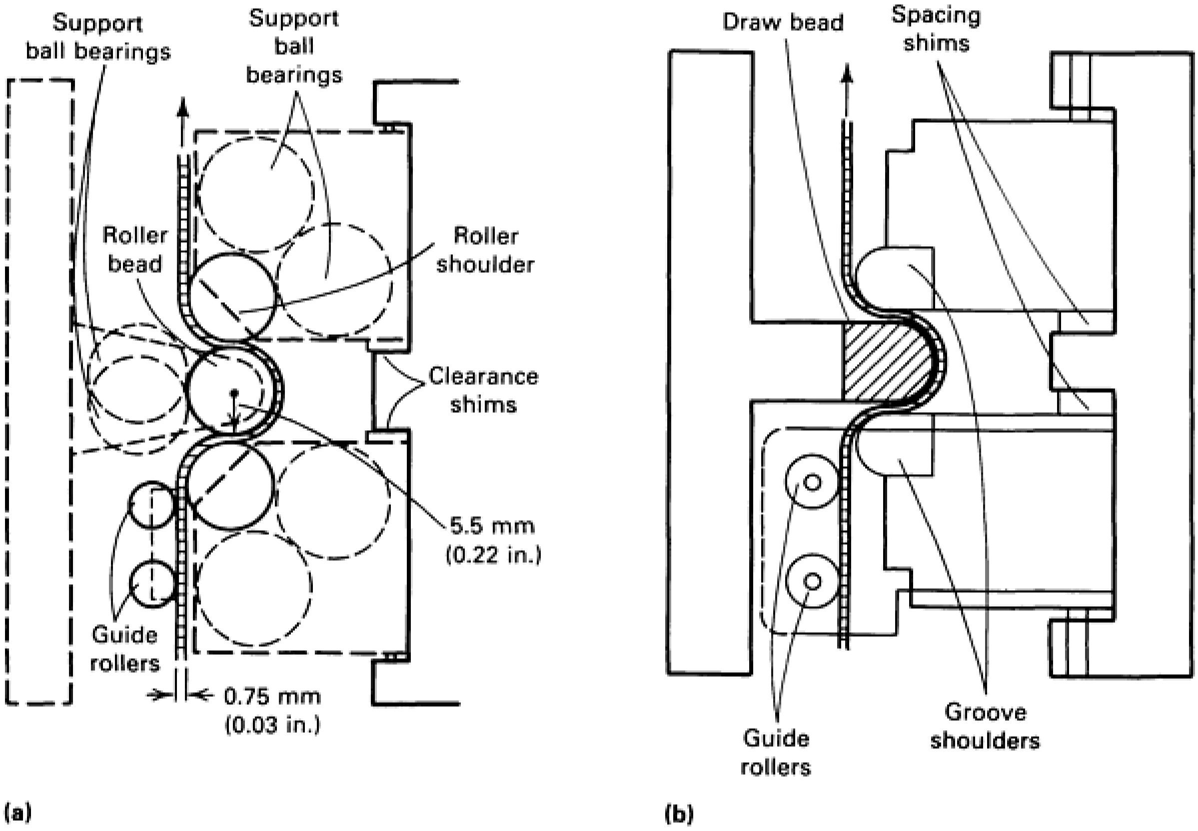

Fig. 35 Drawbead simulators. (a) Frictionless simulator. (b) Standard simulator. Source: Ref 82.

Strips of 0.75 to 1.00 mm (0.03 to 0.04 in.) thick and 50 mm (1.97 in.) wide rimmed and aluminum-killed steels and two aluminum alloys were tested using simulators and a universal testing machine. The contribution from deformation to the total restraining force depended on the lubricant used and ranged from an average of 60% with poor lubrication to 85% with very good lubrication. The required clamping forces, surface strains in the workpiece at various locations in the drawbead simulators, effect of drawbead radius, and effect of rate of testing were also investigated.

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Lubricants

The use of lubricants is essential in most forming operations (see the article "Selection and Use of Lubricants in Forming of Sheet Metal" in this Volume). An effective lubricant provides the following advantages:

- Reduction or elimination of direct sheet metal to die contact and the associated wear and galling

- Control of friction
- More uniform distribution of strain and therefore an increase in the overall level of deformation
- Reduction of heating

Lubricants must meet many requirements to be used in a production operation, such as:

- Suitable viscosity over the ranges of temperatures and pressures encountered
- Chemical and physical compatibility with work and die materials
- Ease of application, removal, and disposal
- Compatibility with welding operations, sealants, and paint systems

No single lubricant is optimal for all types and rates of forming and all combinations of work and die materials. Rankings of lubricants change considerably for different types of operations and material combinations; this necessitates evaluation on an individual basis. Differences in the performance of lubricants are to be anticipated in view of the differences in surface composition, roughness, and texture of work and die materials and the different strain paths and rates of different forming operations. For example, some stretching operations involve local increases in area in excess of 100%, but in many drawing operations, a negligible or negative change in area occurs. In addition, the rate at which the work material slides over the dies varies widely.

A simple test for evaluating lubricants measures the frictional force exerted on a lubricated strip of sheet metal when it is pulled between two rectangular blocks of die material. The force used between the blocks and the test rate can be varied. The number of strips that can be tested before the onset of galling provides an additional measure of the effectiveness of the lubricant. However, this test does not include some of the important aspects of actual forming operations, such as plastic deformation of the work material, the ranges of sliding speeds and rates of straining involved, die geometry (which influences the amount of residual lubricant in various locations as the operation progresses), and the heating that occurs in high-volume production operations.

The drawbead simulator described earlier is more realistic and has been used to measure friction forces with various lubricants (Ref 81, 82). Under most of the conditions tested, friction was described by Coulomb's law, which states that the friction force, F, is directly proportional to the normal force, N, between the contacting surfaces:

$$F = \mu N \quad \textbf{(Eq 32)}$$

where μ is the coefficient of friction. Coulomb's law did not apply at the highest contact pressure tested. The value of the coefficient of friction for mill oil, a poor lubricant, was found to be 0.17; for the best lubricant tested--a soap-based lubricant--it was 0.06.

In addition, simulative tests can be used as lubricant evaluation tests. The production operation should be characterized in terms of the principal types of forming operations involved and their relative severities, and the appropriate simulative test selected. The Swift flat-bottomed cup test has been extensively used for the evaluation of lubricants for deep drawing, and the Swift round-bottomed and Fukui conical cup tests have been used for stretch-drawing operations. The 100 mm (3.94 in.) hemispherical dome test can be used for stretching operations.

The various regimes of lubrication--that is, thick film, thin film, mixed, and boundary lubrication--and their characteristics are reviewed in Ref 83, which also discusses the limited validity of Coulomb's law, methods for lubricant evaluation, and some of the current limitations in this area.

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Formability Testing of Sheet Metals

Brian Taylor, General Motors Corporation

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Introduction

Virginia Mouch, Electronic Data Systems Corporation

COMPUTER-AIDED DESIGN AND MANUFACTURE (CAD/CAM) typically involves the use of graphic displays, which allow the interactive creation and modification of geometric shapes, but CAD/CAM is not limited to this technique. In its broadest definition, CAD/CAM can be the application of any software program--batch or interactive--that facilitates product design and manufacture.

There are two principal advantages of applying CAD to the design of dies for sheet metal stamping. First is the generation of computer surface data for downstream CAM applications to generate numerically controlled (NC) cutter paths and to eliminate the need for die models. Second is the reduction of downstream die tryout time and the reduction of die construction aids by producing more geometrically accurate data. These advantages can result in both time and cost savings. This article will discuss the application of CAD/CAM to the diemaking process, first in general terms and then in a specific case study.

CAD/CAM as Applied to Diemaking

Diemaking, whether manual or computerized, is currently an art, not a science. The methods of approaching the problem may be as numerous as the number of people doing the job. Once these methods are known, it is easier to select the best system package. Most currently available CAD/CAM systems are generic; few systems have specific software tools tailored to the designing of dies. However, with some attention to several key issues, these generic systems can be successfully applied to the design and manufacture of dies. Some of the key issues involved in moving from a manual process to a computerized one are identified below. Flow charts for manual and computer-aided die processing are shown in Fig. 1.*

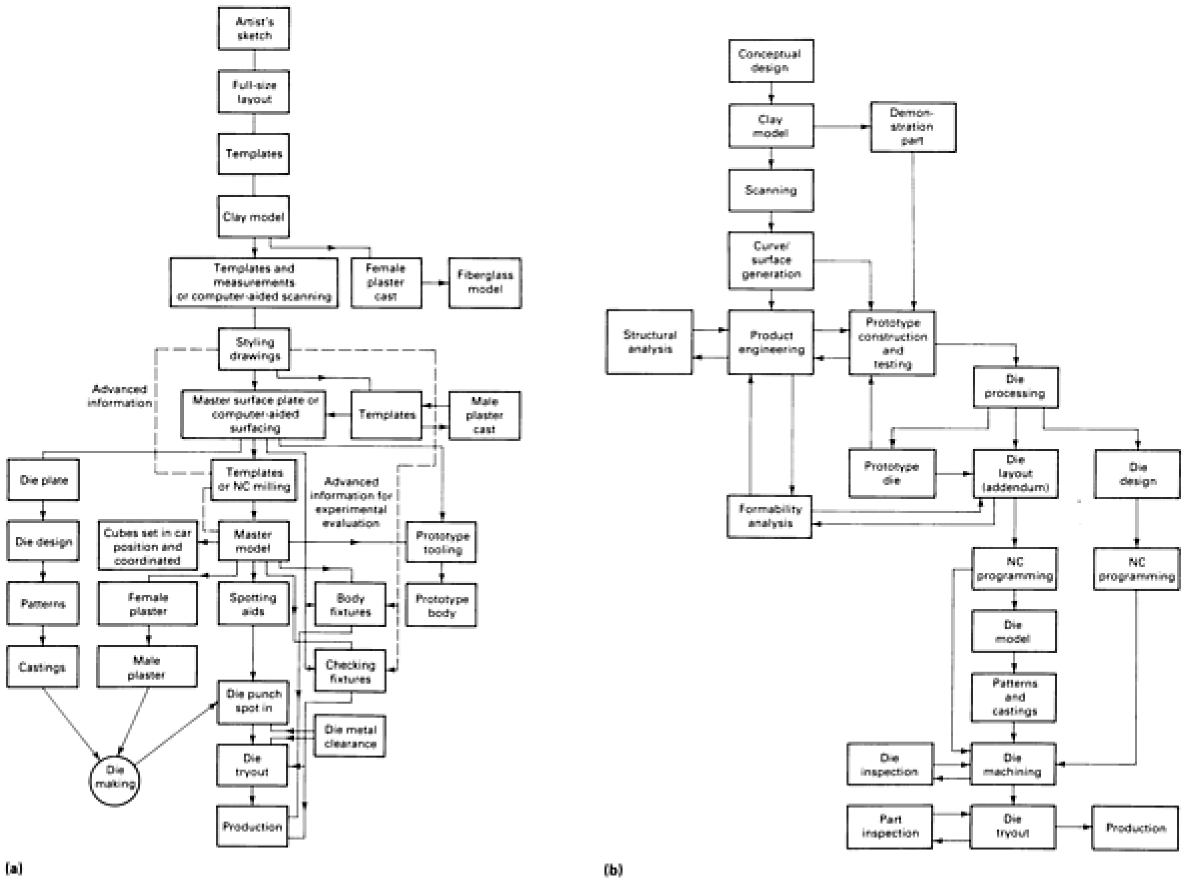

Fig. 1 Comparison of manual and computer-aided diemaking operations illustrating the steps eliminated using CAD/CAM. (a) Traditional manual die processing. Source: Ref 1. (b) Die processing using CAD/CAM.

Establishing the Form of the Master Product Data. It is imperative that the master product data (the computer model, drawings, and physical model) be established and that the CAD system have the capability of accepting this information in the form identified. These master product data represent the starting point for development of the die. All personnel involved in the tooling process must know where to obtain the master product data. Depending on its form, they may need to perform additional operations to make use of it. For example, prior to computerization, a physical model may have been the master. When CAD/CAM systems are used to produce the tooling and the physical model is still the master, it becomes necessary to incorporate scanning devices and data translation software to make use of the master data in the CAD/CAM system.

Determining the Mathematical Representation Required for the Computer Model of the Die. Computer-aided design and manufacture systems can present a number of different mathematical methods of representing or storing data. This can be in the form of traditional two-dimensional drawings, three-dimensional lines, arcs, points, or more complex three-dimensional surface representations.

If NC cutter path generation is to be done by a CAM system, then a surface representation for most of the product and die will make the process more efficient. Surface creation and modification will then be a critical feature of the CAD system selected. Wireframe capabilities will also be needed for designing the mechanical components of the die. Depending on the CAM system selected, the mechanical components may or may not require surfacing.

Determining the Form and Use of the CAD Die Data. In a typical manufacturing operation, several different departments may need these data in order to generate NC cutter paths, to develop castings, to produce prototype models, and to schedule construction. The CAD-developed die data may need to be transmitted through drawings or mathematical models to a CAM system.

In a noncomputerized environment, a drawing is the standard output for those sectors of an organization concerned with die construction. Depending on the extent of computerization, this may be sufficient. However, if NC machines are to be programmed offline using an automatic part-programming system, then the most efficient form of the output is an electronic mathematical representation of the tool. If these needs are many and varied, they must be identified, and the data translation tools included in the CAD/CAM package(s) must be selected.

Determining How CAD and CAM Will be Linked. In the previous paragraphs, an assumption was made that the same system need not be used to design the product, to design the tool, and to manufacture the tool. However, if CAD and CAM systems will be used, the method for establishing the electronic mathematical model between the two systems must be analyzed.

Again, in a noncomputerized environment, this link may consist of passing a drawing from one person to the next. Ideally, it is most efficient for moving data between processes if the systems are the same. However, there may be trade-offs in accomplishing this total integration. It may be difficult to find one CAD/CAM system that meets all of the functional requirements. Before branching off into different systems, the cost of data movement and translation should be analyzed.

Determining the Type of CAM System Required. This issue gets closer to the shop floor. The sophistication required of the NC cutter path generation software will depend on the NC machines to be programmed (that is, three axis or five axis). In addition, postprocessors must be acquired for each NC machine. Numerically controlled cutter paths can be postprocessed directly on the CAM system or downloaded to another system.

Determining the Method of Downloading Posted Machine Data to the NC Machines. A number of methods are available, ranging from paper tape to floppy disk to direct numerical control (DNC) (direct communication link from a host computer to the NC machine controller). In determining which method is appropriate for a given installation, the anticipated volume of NC data to be downloaded versus the cost of the link should be considered.

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* The flow chart shown in Fig. 1(b) represents one point of view of the application of CAD/CAM to the product life cycle. It is intended to be a generic description of the cycle. The diagram may vary from organization to organization based on the amount of time computer assists have been used and the degree of success achieved in phasing out manual methods. The figure attempts to show that computer assists are being added to the process but some manual assists, in reality, have not been phased out. Several processes in the cycle can be phased out. These include wood/plaster master models, checking fixtures and spotting aids. There are also several processes that can be added to the cycle. These include formability analysis, die inspection and part inspection.

CAD / CAM System Selection

The processes and data flow should be analyzed before any system is purchased. In addition, an analysis of future processes and data flow is needed to assess what, if any, organizational or procedural changes will be required. This future analysis also facilitates the selection of the CAD/CAM systems and the peripheral equipment, such as printers and plotters. These analyses can be done by in-house personnel, outside independent consultants, or companies that provide such services along with their systems.

The application of CAD/CAM technology to the diemaking process is ongoing. Key to its success are careful analysis and planning, quality training, and commitment from all levels of the organization. The process must be considered in terms of the application of CAD/CAM as well as the interface with the shop floor. The following sections provide more specific details on some of the issues discussed in this introduction.

Equipment, Advantages, and Applications of CAD / CAM for Diemaking

Lee Spruit, Autodie Corporation

The advent of CAD/CAM has transformed the manufacture of dies for automotive sheet forming from what was once essentially a manual process that involved making expensive models and trial and error for arriving at a die design into a highly accurate and automated process that is augmented by the use of computers. Many of the steps taken in traditional die design can be eliminated (see Fig. 1), resulting in much shorter lead times. Die quality is also improved through the use of computer-aided manufacture, and as a data base of standard die designs is developed, a cost savings can be realized.

Computer-Aided Design

Computer-aided design has been defined as the technique by which geometrical descriptions of three-dimensional objects can be created and stored in the form of mathematical models in a computer system; once created, the models can then be displayed, manipulated, and analyzed in a number of ways on a CRT (Ref 2).

This Section will describe typical CAD equipment and processes used for sheet forming die design. Figure 2 shows a flow chart of the operations described in this Section.

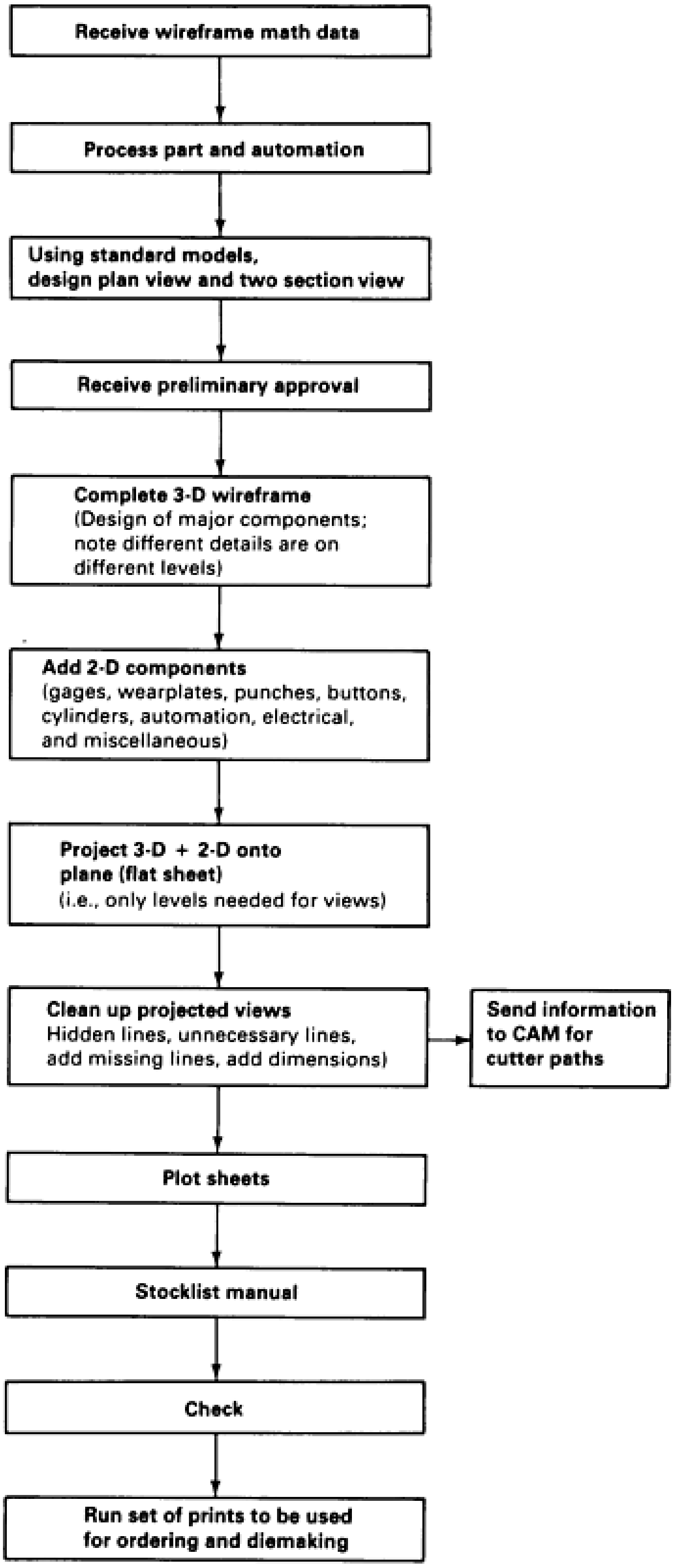

Fig. 2 Flow chart of die design and processing. See also Fig. 1. Courtesy of Autodie Corporation.

Equipment

Hardware. Central to a CAD system is a series of individual workstations, each of which may be equipped with its own processing unit or may operate through a central computer on a time-sharing basis. Even when each workstation has its own processing unit, they are usually linked to all of the other workstations at the facility. This avoids the system slowdown that may be encountered during peak computing periods when using a central computer and prevents the idling of an entire department in the event that the main computer "crashes" (fails or "goes down").

Each workstation must be equipped with memory capacity to handle the size of the data files used. A typical workstation may have 8 megabytes of random-access memory. Additional memory capacity is supplied by a disk drive. Peripheral equipment may include magnetic-tape readers, telephone modems for communication with other computers, and a printing device for output.

Software used for CAD/CAM applications is often available from workstation suppliers. One function of the software is to maintain, update, and store a constantly expanding parts library. This data base will continue to provide the key to the increasing cost-effectiveness of computer-aided design for diemaking. As tooling for new parts is required, it can be patterned after designs for existing tooling rather than created from the beginning, as is usually done in manual operations. However, to realize this economy, standard details for tooling components must be adopted and shared. When this is accomplished, there will ideally be almost no original design work undertaken; all new tooling will come from an assembly of standard die components.

Costs and Advantages Associated With CAD

Costs. Unquestionably, the cost of a tool designed with CAD will be higher than one designed by the traditional method. Some shops have estimated this increase in cost to be approximately 20% or more. In addition, although die design has traditionally represented about 10% of total die cost, with CAD, the current percentage is closer to 15%. However, these figures are transitory and deceptive.

Computer-aided design requires a significant financial commitment to the purchase of computers and associated equipment and software. Modifications to the plant are also often necessary to accommodate all of the sensitive equipment. Even when the commitment has been made to spend hundreds of thousands of dollars to purchase the system, skilled operators will then be needed to handle it. Aside from the training costs for these operators, efficiency will suffer while the designers become familiar with the power of the new techniques. Beyond all of this, there are the financial and time costs associated with building a library of parts and standard die components. In all, any firm deciding to enter this technology should allot approximately 1 year of relative inefficiency to setting up the entire system.

As discussed later in this Section, the primary advantages of CAD are its tie-in to CAM and the smooth, efficient machining of dies that results. Therefore, the cost of machining the die is reduced, and this in turn raises the percentage of total cost represented by the design function, even if absolute design costs were to remain constant. Nevertheless, die design costs may eventually fall below the levels associated with traditional design.

Advantages. Although the costs of implementing computer-aided design are readily apparent, the advantages are far less so and are often unquantifiable. In fact, most of the advantages accrue downstream in the manufacturing process, rather than directly in the design function. These advantages include DNC part programming, better scheduling, reduced number of setups, and faster machining times. These will be covered more fully in the discussion "Computer-Aided Manufacture" in this section.

Within the design phase itself, most advantages derive from enhanced productivity in terms of speed and accuracy. Depending on the specific task, productivity can typically increase from three to ten times for particular functions. As mentioned above, standardized components help tremendously in raising this figure; other factors include the complexity of the part, the degree of part symmetry, and its similarity to previously designed components. Another element of productivity gain is found in the reaction time to any changes in input, which may range from days to literally moments.

As important as productivity increases may be, perhaps the greatest contribution of CAD is in design accuracy. On one hand, there is dimensional control that is often well above that attainable by manual drafting. A designer can precisely scale and view the work from any perspective, rotating the design on the screen at will. Further, with this three-dimensional capability, when an alteration is made to a design in one dimension, the computer automatically makes the equivalent change in the remaining views, thus preserving continuity and minimizing the possibility of errors. These features lead to vastly improved comprehension by the designers, because they are not required to assemble a series of two-dimensional drawings into a mental three-dimensional image. On the other hand, CAD systems provide the opportunity to avoid errors in drafting and documentation because the computer maintains records and continuity. In addition, computer tolerances are much closer than those of a drafter; inconsistencies that may be difficult to detect on paper are immediately obvious to the system.

Advantages also result from the maintenance of a single, unified data base. No longer are engineering changes circulated about haphazardly from a variety of sources, often in ignorance of other changes being proposed. Instead, all changes are made directly into one data base for all users to access. Historical data can also easily be filed for reference.

When to Use CAD

Not all die design work is currently suitable for CAD; even if CAD is capable of handling the design work, it is not always economically feasible to do so. As noted previously, it is often more expensive to design a die with CAD than manually. However, economies can be achieved for large families of similar parts, for which a library can be maintained and accessed often. Poor candidates for CAD are unique items, or those requiring a sizable amount of individual attention, for which library entries do not exist and will not be useful in the future. Ideal candidates are repeat commodities. In the automotive industry, these include hood inner and outer panels, roofs, fenders, doors, and quarter panels.

Even where CAD is used, only 80 to 90% of the designing is done on-screen. Functions still frequently done manually include such tasks as double checking and detailing[**] the designs. Computer detailing requires exorbitant amounts of computer memory, but it is a relatively simple task to perform manually. Thus, there are not enough advantages to justify performing this operation on the computer. This may change, however, as newer and more powerful software and hardware become available.

To use CAD, it is necessary to obtain three-dimensional wireframe diagrams (Fig. 3) of the finished part from the customer. Not all parts are available as wireframes; perhaps 95% of all new outer-skin prints in the automotive industry have such diagrams available. However, virtually 100% of all new automotive parts can be found in the computer. Surface data are not required for CAD, although they are necessary for CAM.

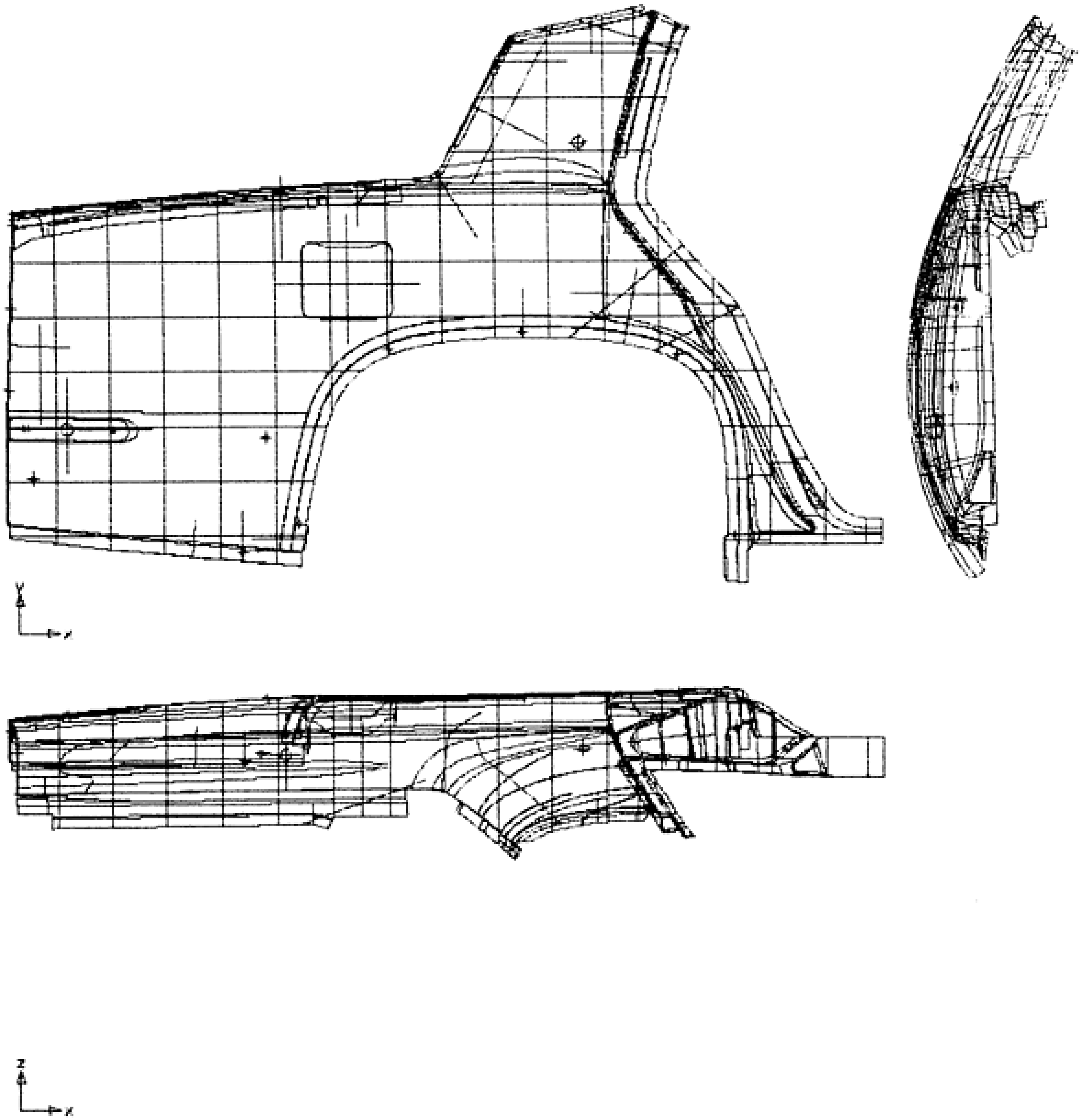

Fig. 3 Wireframe (three-dimensional representation) of an automobile rear quarter panel. Courtesy of Autodie Corporation.

Drawing dies require the development of draw-die binder surfaces, the areas used to restrain and clamp the sheet metal blank during forming (Ref 3). Two methods can be used to generate the binder geometry:

- Creating the geometry on the CRT
- Digitizing the geometry off an actual binder development

In the latter method, a prototype model is NC cut and a binder surface is created. A prototype die is built, tried out, and altered to make an acceptable panel. The proven binder is then digitized and entered into the computer. The design then represents a proven development. The digitized data are passed on to the cutter path programs, where cutter paths are generated. Binder geometries are often complex shapes where the contours cannot be visualized on a CRT, and far less time and money are spent using the digitizing approach. However, digitizing requires some means of obtaining data from the prototype and uploading it to the design computer; a computer-controlled coordinate-measuring machine is often used for this purpose.

Die Design Using CAD

Obtaining and Storing Data. The designing of sheet forming dies for automotive stampings usually begins with a telephone link to the data center of the automaker. Using the direct link, the die designer can obtain the data necessary to generate a wireframe of the panel (Fig. 3). Once obtained from the computer of the automaker, the data can be used immediately or stored on magnetic disk or tape for later reference.

For design work, the wireframe must be in three dimensions. Two-and-one-half dimensions fail to depict sufficient detail in the third dimension to allow proper design of the entire surface. In addition, when changes are made in one dimension, the computer is unable to calculate the resulting changes in the remaining dimensions.

When the design is being manipulated on the workstation, the data for the part may require 25 to 30% of the memory. The operating system occupies about 20%, and the graphics package used may take up another 30%. This leaves about 25 to 30% of available memory capacity. As much as one-half of the data obtained from the automaker are unnecessary for die design and may be purged.

Laying Up the Design. In the design process, models are summoned from the library and compared to the current workpiece. The designer enlarges or reduces the size of the details of the standard model to fit the new part. Guide pins, wear plates, air cylinders, air headers, and other die details are included in the standard model. With the full three dimensions available, the panel and die representation can be rotated in any direction for a better view of the part. Because the model is still in a wireframe format, sections initially appear as a series of unconnected dots. The points are connected by the designer. Currently, there is no automatic routine for connecting the points. Because the computer does not know which points to connect, a skilled designer is needed. In laying up the design, hidden lines also must be indicated by the designer. This is done by altering line fonts on each view or section that contains hidden design features.

Creating Views and Detailing. To create a view, the three-dimensional wireframe is dissected in a manner that allows viewing of specific areas within the die. A three-dimensional image seen on the workstation screen is projected onto a flat plane. At this time, the designer can remove hidden lines and otherwise enhance the projection (for example, add dimensions, finish marks, and so on) to suit his needs. When this process is completed, the depiction is transferred to the plotter. The resulting drawing(s) are then used for manual detailing and checking and filing for future reference. The final drawing is used as a medium to convey the information from the computer to the shop floor.

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** Detailing refers to the process of looking at the overall design and subsequently breaking the design down into individual elements for construction.

Computer-Aided Manufacture

Computer-aided manufacture is defined as the use of computers (or, in this case, stand-alone workstations) for partial or complete control of the manufacturing processes; in practice, the term is usually applied only to computer-based developments or NC technology (Ref 2).

Equipment

Hardware. The equipment used for the computer-aided manufacture of sheet forming dies can be divided into two categories. First are the workstations used to generate cutter paths for NC machines on the shop floor; these were described in the discussion "Computer-Aided Design" in this section. The second category is the machine tools themselves, which are described below.

Software. To establish the cutter paths on outer sheet metal panels, it is necessary to work from a surface model--the full mathematical equivalent of what the part should be. The main reason for this requirement is to ensure compatibility between the male punch and the female shoe of the die. As with CAD, two-and-one-half dimensions are insufficient. The CAM worker must have three dimension to see the details on all surfaces in order to predict cutter behavior, to check for potential gouging, to avoid interference, and to generate accurate cutter paths.

As part of its own processes, each automaker develops its own surface models for all of its new exterior panels. Die manufacturers may not have the capability to develop such models and usually use data supplied by the automaker. However, this can be done only if the software used by the automaker is compatible with that of the die manufacturer. A common graphics software exchange package is Interactive Graphic Exchange Software (IGES), which was developed by the National Bureau of Standards. Although wireframe data do not require this intermediary, surface model data (as well as solid model data) do. Not all of the software used by automakers and CAD/CAM suppliers is IGES compatible, and not all of the characters are recognized by all systems. A direct data translator for coupling two different systems together is a faster and more accurate means of communication. This is desirable when data transfer will be frequent, but the translator must be modified whenever software changes are made.

In cases where surface data are not available, the die manufacturer must generate such models in-house. This is done by interpolating points between the wires in a wireframe model. At times, additional section cuts are required to define the surface fully. However, it is preferable to avoid this method of developing surface data when possible because it often duplicates work already performed by the automaker and represents a potential source for errors and/or misinterpretations.

There are several different methods by which the automobile companies generate their own surface models, and all of them involve best-curve interpolation from wire-frames. Among the techniques used are the Gordon method, the Coons patch method, and the Chebychev method. In some European installations, surfaces are generated directly from original equations, primarily employing the B-splines, or Bezier, method.

Patches

To handle a surface model efficiently, it is usually necessary to break the panel representation into a series of patches. A patch is a region on the surface over which a smooth flow for a cutter path can be generated. If a given panel were to be almost featureless, permitting long, uninterrupted strokes by the machine tool, only one surface patch encompassing the entire part would be necessary. In practice, even the simplest panel requires a number of patches.

The borders of such patches are determined by the surface geometries involved. A border is created wherever there are sharp changes in geometry requiring significant changes in cutter path. Each patch is treated independently; even when the die is actually being machined, the tool will work on one surface patch completely before moving on to another. The boundaries are common, and there is no overlapping of sections.

The number of patches required for a die is a function of its size and geometric complexity. A roof panel, because of its comparative simplicity, may be divided into only about 10 patches; a quarter panel, into around 40; and a floor pan, with all of its convolutions, as many as 100. To handle the entire panel as one model would require data files of enormous proportions that would be virtually unmanageable with a stand-alone computer. This occurs when it occasionally becomes necessary to combine adjacent patches on the computer. This practice usually requires a good deal of integration effort by the CAM programmer and up to seven times the computer space needed by the two former patches separately. In theory, such combining would help reduce the potential for error in matching adjacent patches; however, the amount of additional effort required is seldom justified by the marginal benefit. Newer software packages, however, will soon facilitate the machining of multiple patches.

Cutter Path Generation

Not all CAM work must be run from three-dimensional surface models. Relatively straightforward work such as milling, profiling, and trim and pierce operations, in which only two-dimensional descriptions are required, typically runs off of wireframe product data or computer-aided die design data. Complex surface machining dictates the use of three-dimensional surface models.

In addition, although it is a simple procedure in the design phase to take the mirror image of a right-hand part to create its left-hand complement, and vice versa, the same is not true for cutter path generation. When the machine tool is reversible, surfaces can be mirrored, but a conventional cut on one hand is a climb cut on the other.

Postprocessing

During this phase of CAM, the computer takes the cutter path data created at the workstation and translates it into the appropriate NC machine language. Generally, each type of NC machine tool has its own language; therefore, a routine for making the data readable to one tool would be different from that required for another. After the translation is complete, a computer may generate a paper tape containing the machine instructions, or it may download them directly through an electronic link to the tool. Direct loading of data is preferred; paper tape is very slow to make and to read in and is bulky and confusing to distribute.

Computer Requirements

The raw wireframe data for a part taken from the computers of the automaker typically require 1 to 2 megabytes of space (in the case of a roof panel, the figure may be about 1.1 megabytes). In contrast, the surface model may take 2.5 to 4 megabytes of space (for a roof panel, approximately 2.8 megabytes). As with the CAD process, much of the data found in the files of the automotive company are unnecessary for CAM cutter path generation and are purged.

The size of the NC files sent to the machine tools may range from 1 to 10 megabytes, again depending on part complexity and the number of patches required. Therefore, the file for any one panel may require up to 30% of the operating storage space of the workstation.

NC Machine Tools

Controls. It is not necessary to have a different type of controller for each machine tool. For example, one die manufacturer maintains only three types of controllers for 29 NC machine tools. This is in keeping with a prime rule for the shop--keep it simple. This low number of controls yields flexibility in the choice of tool to be used. In addition, not all jobs must be programmed using numerical control. Some tasks are simpler to program manually; numerical control should be used when the computer can do the programming more accurately and efficiently.

The presence of cutter offsets in the control must be ensured. This is to compensate for cutter size due to grinding, as well as the changing to different cutter sizes other than those programmed.

On-board machine memory may use paper tape or disk drives. In the case of disk drives, downloading is done directly from the computer. The time required to load a program may range from 4 to 30 min, depending on the hardware used. In addition, some machine tool manufacturers are developing routines to allow data downloading while the machine is already operating, thus saving more time. If paper tape were used to load programs, loading would be up to approximately 30 times slower.

Machine Tools. A wide variety of machines can be used with numerical control. Care should be taken in selecting machines to be sure that they are able to meet the demands of the jobs typically undertaken. Features such as accuracy, number of axes, and size of the workpiece that can be handled should be considered to ensure that the tool is suitable for the intended application.

Cutting Tools. Carbide tools are often used in the machining of sheet forming dies. An advantage of NC machines is their rigidity, which permits the use of carbide tools. Older machines, without proper holders, could cause shattering of the carbide tools. The use of carbide tools allows higher speeds and much faster metal removal than other tool materials.

Maintenance. Periodic preventive maintenance is critical to the sustained accuracy and repeatability of the machine tool. Although such a process may appear costly on a short-term basis, it will more than pay for itself over the longer term in the form of greatly reduced downtime and consistent superior-quality products that require minimal subsequent hand work. A rigid maintenance schedule must be established at the outset and followed closely throughout the lifetime of the tool. Air filters in the control cabinets must be changed regularly because of the sensitive nature of the electronics and the magnetic properties of the dust.

Costs and Advantages Associated With CAM

Costs. As with CAD, there are significant initial investment costs that are necessary in order to take advantage of CAM. There is no question that NC machine tools are considerably more expensive to purchase than their conventional counterparts; even retrofitting is an expensive process. In addition, there is the cost of training skilled tool operators capable of programming the machines on the shop floor, as well as the higher wages associated with these increased skills.

Advantages. Again, like CAD, the benefits derived from the use of CAM are difficult, and sometimes impossible, to quantify. Some stem from the reduction in man-hours required for machining; even more come from the greater accuracy and product quality achieved from the use of the system. Among the specific advantages are:

- Time savings from the reduction of multiple setups
- Greater productivity in the cutting process itself (due primarily to the elimination of drag imposed by the tracer on the model surface; improvements of up to 50% may be achieved)
- Elimination of inaccuracies in models due to warpage
- Accurate cutter paths for profiling from computer data instead of from templates
- Less subsequent handwork required
- Reduction in tryout time due to reduced part errors
- More productive use of employees

Computer-aided manufacturing allows the machine to perform the routine functions, thus enabling the diemaker to concentrate on more complex tasks. The main theme of the above list of advantages is the avoidance of errors, and this is what makes the quantification of benefits associated with the use of CAM so difficult. It is difficult to place a value on errors and slowdowns that never occur.

In addition, the use of true CAM is impossible without CAD. Computer-aided design inputs the data that allow the cutter paths to be generated by computer. Without CAD, CAM must create all of its own data, slowing the overall process. Although CAD and CAM operations are described separately in this Section, the system is more appropriately viewed as a whole.

When to Use CAM

Two-And-One-Half Dimensional Work. Some aspects of die manufacture are relatively straightforward, requiring only two-dimensional representations for cutter path generation. In the case of casting millwork (flat surfaces that require simple mill cutting), it remains easier to input much of the data manually, particularly for straight areas. Operators can use their experience and judgment to determine cutter paths quickly; therefore, it is not economical to use CAM for this process. However, for angled millwork, the operator would have to perform excessive calculations and programming. With CAD/CAM, the process is effortless and accurate. The overall is a net reduction in design and manufacturing time.

Computer-aided manufacturing is also useful for the machining of standard die components. These components are used quite frequently; therefore, much duplication of effort can be avoided. Profiles are also done completely on CAM. For trim and flange operations, CAM is a must. On pierce dies, CAM is useful for cutting die buttons, keys, and clearances in die pads. This is examined in more detail below.

Three-Dimensional Work. To program three-dimensional surfaces, data must be available in wireframe; surface data are more desirable, but are not always available. If a surface model does not exist, it must be created. The more complex the part geometry, the greater the number of patches required, and the longer it takes to create the surface. If surfaces are available, a small amount of trimming of the surface patches is required in order to eliminate patch extension and/or overlap. Outer surface parts have relatively simple geometries with critical surface and dimensional requirements, and CAM is a must with or without supplied surfaces. Existing CAD/CAM systems can handle the data and create excellent cutter paths in a timely, efficient manner. On inner parts, where surfaces are complex with multiple patches, the effectiveness of cutting the shape by CAM is questionable because of the current limitations of surface mathematics. The surface is seldom supplied. The task of creating a surface on a panel such as a hood inner is an 8-week program before cutter paths can start to be generated. If the programming for CAM takes too long or creates gouges, the net effect is lost time and money. A directive cannot be set that all machining be programmed and cut using CAM. Because CAD/CAM is an emerging technology that does certain items well, it should be used only for those applicable items, such as outer panels, hoods, and fenders. Applications can be expanded as new software allows.

Machining With CAM

To ensure precise alignment of all machining work and subsequent addition of die components, a reference system should be used to locate the centerlines of a die. An example of such a system would be two reference holes, which are located precisely on centerline or an exact distance from centerline. These holes form a precise centerline down the length of the casting. All points for future work are then referred to in terms of distance traversed along the centerline and distance away from the centerline.

The location of the panel must be called out in relationship to the centerline of the die. Holes are put into the die post shape as an aid for roughing or spotting. Kellering heights are also called out for all duplicating. All tips must be called out in degrees.

Two-And-One-Half Dimensional Work. As described above, much casting millwork is more efficiently performed by using manual, rather than CAM, programming procedures. However, flange and trim die profiles are often suitable for CAM programming, specifically on the male and female dies and the die pad. The position of the trim line is generally the key in deciding whether to use mathematical data or to develop data. If the trim line is in part-print (final) position, there are no difficulties, and the cutter paths are programmed directly from mathematical data; however, if the trim line is in a laid-out position, problems often occur. In such a situation, the radius in the drawn panel cannot be adequately simulated; therefore, the locating of a trim line will be developed incorrectly. Because of this, trim dies often require a trial-and-error manual process, in which a stamping is laser trimmed (with the trim line used recorded) and put through the flanging process. Errors in the resulting part are noted, the trim line is adjusted, and the procedure is repeated until a satisfactory panel results. Once a satisfactory development is determined, the final development is programmed for an NC machine and the male die, female die, and pad are cut, thus eliminating the welding of trim lines.

Three-Dimensional Work. The first step in cutting a surface is to rough cut the finished part shape using tracers to a roughing aid. The roughing aid is used to speed the process; acceptable CAM roughing routines are currently unavailable. On large dies, casting within 13 mm ($\frac{1}{2}$ in.) of the final shape is impractical. A roughing aid is inexpensive and quick. The NC cutter paths are used for the semifinish and finish cuts of the surface.

To maintain part shape integrity through the line of dies, a spotter is made off the draw die. This spotter is fitted against all subsequent line dies to ensure proper feature alignment. When this process of spotting has been completed, the dies are ready for tryout.

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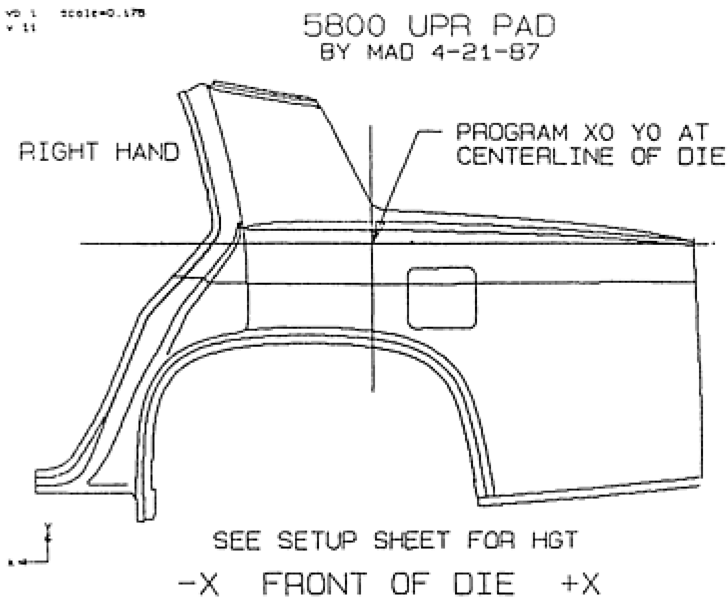
Flow of Information

To maximize the efficiency of die production, a series of decisions must be made and communicated to the shop floor. At the same time, the shop floor operators must have a channel of communication back to the planners to allow their expertise to influence these decisions. The sequence of decisions should follow a distinct pattern:

- Determination of what can be CAM machined and what should be programmed
- Setting the location of coordinating and reference holes to ensure that all personnel are working off the same center line
- Creation and storage of machining data ahead of time, including cutter paths, cutter specifications, and other relevant information
- Establishment of a distribution manuscript to die leaders that shows and describes the workpiece, the tools required, the work to be performed, and the machine file (Fig. 4)
- Downloading of programs to machine tools
- Completion and shipment of die(s)
- Disposal of CAM routines. Because every die is unique, there is no value in maintaining a library of CAM

routines



(a)

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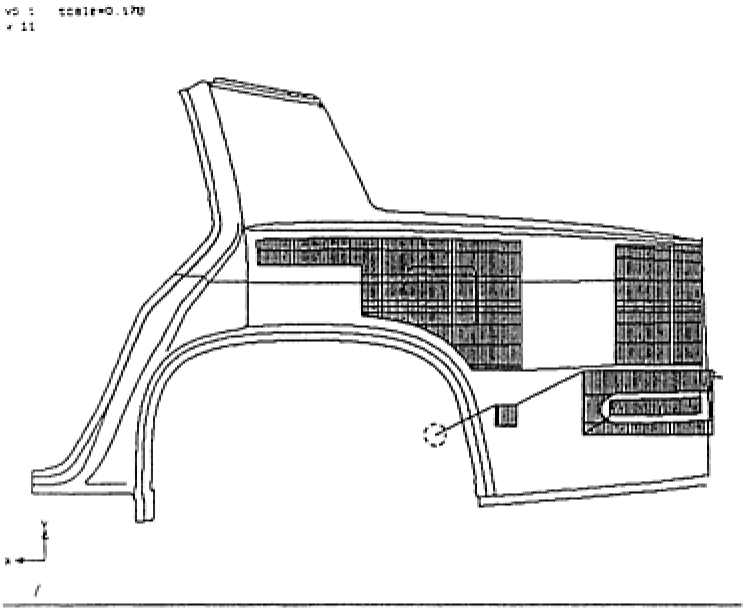
TOOL	DIAMETER	CORNER RADIUS	FLAT RADIUS	TAPER	LENGTH	DESCRIPTOR
1	2.0000	1.0000	..0000	.0000	.0000	2.0 CARB BALL
2	1.0000	.5000	.0000	.0000	.0000	1.0 CARB BALL
3	.7500	.3750	.0000	.0000	.0000	.750 CARB BALL
4	.5000	.2500	.0000	.0000	.0000	.500 CARB BALL
5	.3750	.1875	.0000	.0000	.0000	.375 CARB BALL

(b)



(c)

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X
X
N1G90
G17T100M99
T1M06
S1000M03
G00X60000Y-180000
N2G01X115435Y-150462Z-42179F400
Z-92179
X115444Y-156768Z-90697
X115458Y-163025Z-88929
X115477Y-169224Z-86892
N3X117445Y-169226Z-86703
Y-169211Z-86789
X117425Y-163016Z-88824
X117410Y-156763Z-90590
X117400Y-150461Z-92071
N4X119375Z-91962
X119385Y-156739Z-90486
X119401Y-162969Z-88729
X119422Y-169228Z-86674
X121401Z-86563
N5Y-169072Z-86618
X121379Y-162921Z-88633
X121363Y-156714Z-90382
X121352Y-150460Z-91851
X123050Z-91756
N6X123330Z-91740
X123341Y-156690Z-90276
X123350Y-162874Z-88535
X123381Y-169229Z-86452
X125362Z-86340
N7X125361Y-168931Z-86445
X125338Y-162826Z-88437
X125321Y-156666Z-90170
X125309Y-150460Z-91627
X127209Z-91513
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X127319Y-162778Z-88338
X127344Y-169231Z-86226
X129326Y-169233Z-86112
X129324Y-168788Z-86267
N9X129300Y-162730Z-88238
X129282Y-156618Z-89953
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(d)

Fig. 4 Distribution manuscript showing the workpiece (a), tool requirements (b), the work to be performed (c), and the machining file (d). Courtesy of Autodie Corporation.

The Future of CAD/CAM

The evolution of CAD/CAM has been rapid, and it is accelerating. Within the next 5 years, CAD and CAM will merge to become CAE (computer-aided engineering). This has already been achieved on a small scale. The trend toward more extensive libraries filled with standardized components will also aid this trend. The unfortunate side to this development is that reinvestment in ever-more-advanced equipment and software will be required to keep pace with the competition; however, the benefits of such reinvestment will continue to outweigh the costs.

Although reinvestment in equipment is an obvious fact of life, the reinvestment in personnel must not be overlooked. Shop-floor workers and programmers must be given the ability and knowledge to understand the process as it evolves. At the very minimum, they should participate in training programs on a continual basis to stay current with the latest developments.

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Introduction

ALTHOUGH SHEET METAL FORMING is a widely used processing method representing an industry with large production volumes, an integrated engineering approach was not adopted by the metal forming industry until the 1960s when the concept of the forming limit diagram (FLD) was introduced by S.P. Keeler (Ref 1) and G.M. Goodwin (Ref 2). The forming limits, which represent the relationship between limiting major and minor strains in the plane of the deformed sheet, however, remained a diagnostic tool rather than a predictive method. For example, when a sheet metal part fails by localized necking and fracture during manufacturing, a common practice is to either redesign the part or change the processing method so that the strains in the critical region do not exceed the forming limit.

Predicting such a failure in the formed sheet metal part, however, is a difficult task, largely because of the complex boundary and metal-die interfacial conditions and the difficulty of describing quantitatively the nonuniform deformation behavior of metals. It was not until the 1970s that important advances were made in the development of localized plasticity theory that could be used in the modeling of neck initiation and growth behavior. Some of these advances include constitutive and plasticity relations by J.W. Hutchinson and K.W. Neale (Ref 3), bifurcation and imperfection growth analysis methods by A. Needleman and J.R. Rice (Ref 4), and a variety of numerical schemes for elastic-plastic analysis using finite-element analysis methods (Ref 5). The most frequently used method for predicting the limiting strain, however, is based on the idea that necking develops from local regions of initial non-uniformity, as suggested by Z. Marciniak and K. Kuczynski (Ref 6). A number of authors have extended the idea of initial flaw, and an entire FLD was predicted by J.D. Hutchinson and K.W. Neale (Ref 3, 7), D. Lee and F. Zaverl (Ref 8), and Z. Lu and D. Lee (Ref 9).

The purpose of this article is to review various modeling and simulation methods that are available for the sheet metal forming processes and to outline an integrated process analysis method that may be used to predict the success or failure of the formed sheet metal part at the design stage. After reviewing various analysis methods, three sample cases will be examined to illustrate some of the critical points in modeling. Some of the new approaches are outlined to convey the underlying idea that an interdisciplinary approach can be developed to analyze a manufacturing process at a computer terminal.

Acknowledgements

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Process Modeling and Simulation for Sheet Forming

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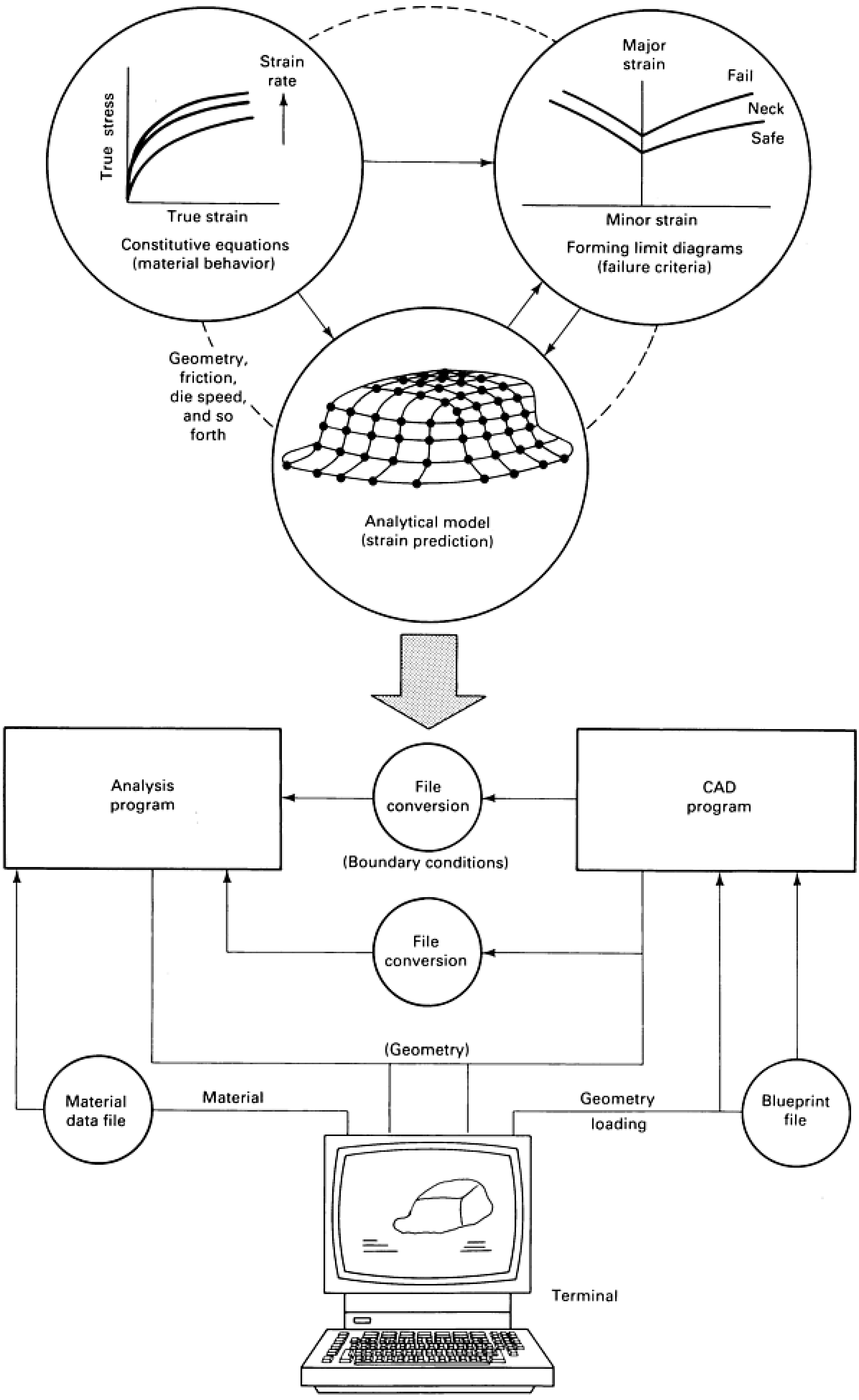
Generalized Approach

One of the important objectives of process modeling is to predict whether or not a particular sheet metal part can be formed successfully. In order to accomplish that objective, several data bases and analysis programs are required. A diagram illustrating the main elements, which may consist of an analysis program, a computer-aided design (CAD) program, and the computer terminal, is shown in Fig. 1. The analysis program consists of constitutive relations, an FLD analysis program, and a finite-element analysis program; some of the details of these elements will be covered in the following sections. The CAD program contains all the information necessary for a full geometric description of the parts that may be obtained from the engineering drawings. In addition, the material data base as well as details of loading condition are specified. Brief descriptions of each of the three elements that make up the analysis program are outlined below.



True stress
True strain
Strain rate
Constitutive equations (material behavior)
Major strain
Fail
Neck
Safe
Minor strain
Forming limit diagrams (failure criteria)
Geometry, friction, die speed, and so forth
Analytical model (strain prediction)
Analysis program
File conversion
(Boundary conditions)
CAD program
File conversion
(Geometry)
Material data file
Material
Geometry
loading
Blueprint file
Terminal

Fig. 1 Major elements of a computer-aided sheet metal formability prediction system. Source: Ref 10.

Plasticity and Constitutive Relations. Assuming an anisotropic, inelastic material dependent on deformation rate under a general state of stress, the inelastic strain rate, $\dot{\epsilon}_i$, may be expressed in terms of the flow potential, or the yield function f and the scalar multiplier λ(Ref 11, 12):

$$\dot{\epsilon}_i = \dot{\lambda} \left[\frac{\delta f}{\delta \sigma_i}\right]_{\dot{\lambda}} \quad \textbf{(Eq 1)}$$

where f is a generalized yield function such as that given in Ref 13, and the scalar multiplier $\dot{\lambda}$ is equal to (3/2) ($\dot{\bar{\epsilon}}/\bar{\sigma}$) where $\dot{\bar{\epsilon}}$ and $\bar{\sigma}$ represent equivalent strain rate and equivalent stress, respectively. For example, the yield function f can be written in terms of the distortion matrix M_{ij}, which describes the variation of the flow stress with orientation, or:

$$f = M_{ij}\sigma_i\sigma_j \quad \textbf{(Eq 2)}$$

where σ_i and σ_j denote the stress vectors corresponding to the appropriate tensor counterparts, σ_{ij}. Therefore, it follows that the equivalent stress, $\bar{\sigma}$, can be expressed as $\bar{\sigma} = (M_{ij}\ \sigma_i\sigma_j)^{1/2}$. For the case of a plane stress loading condition ($\sigma_3 = 0$) and materials with planar isotropy ($M_{11} = M_{22}$), it can be shown that the well-known anisotropy parameter R is related to M_{33} by $R = (2/M_{33}) - 1$.

In order to describe the strain hardening and strain rate hardening behavior of materials, the equivalent strain rate, $\dot{\bar{\epsilon}}$, may be expressed in terms of the equivalent stress, $\bar{\sigma}$, or:

$$\dot{\bar{\epsilon}} = \dot{\epsilon}_0 \left[\frac{\bar{\sigma}}{k}\right]^{1/m} \quad \textbf{(Eq 3)}$$

where m is known as the strain rate sensitivity of flow stress; the effective flow stress k and the reference strain rate $\dot{\epsilon}_0$ are defined below. The effect of temperature can be readily incorporated into Eq 3 by including an exponential function as an Arrhenius-type expression (Ref 12).

The effective flow stress k and its dependence on $\bar{\epsilon}$ can be determined from a uniaxial tension test at a reference strain rate ϵ_0. It has been demonstrated that a number of experimental data could be represented most accurately by the well-known Swift-type equation (Ref 14):

$$k = k_0\ (\epsilon_0 + \bar{\epsilon})^n \quad \textbf{(Eq 4)}$$

where k_0 and ϵ_0 are material constants and n is the well-known strain hardening exponent. Equations 3 and 4 can now be combined to express the equivalent stress $\bar{\sigma}$ in terms of variations in $\bar{\epsilon}$ and $\dot{\bar{\epsilon}}$ (Ref 12, 15, 16).

Prediction of Forming Limit Diagrams (FLDs). The detailed method of computing the limiting and elongation FLDs has been recently outlined (Ref 8, 9). Specifically, there are several methods that can be used to compute the FLDs. One method is to treat the onset of failure as the condition that leads to plastic instability (Ref 14, 17, 18); the second approach incorporates J_2 deformation theory into a classical bifurcation analysis (Ref 4, 7, 19). Finally, the approach based on the idea that necking develops from local regions of initial nonuniformity has been widely used in recent years to establish FLDs (Ref 3, 6, 7, 8, 9).

Extending the idea of initial nonuniformity to start the neck growth process, it is possible to formulate an analytical model that predicts the development of nonuniform flow in sheet metals under various plane-stress loading conditions (Ref 8, 9). Specifically, the specimen is assumed to have an initial geometric nonuniformity in the form of a band of reduced section. When the initial minimum thickness at the reduced section and the thickness at the uniform section are designated by $h)_0^0$ and $h)_1^0$, respectively, the nonuniformity index, η, is given by η= 1 - $h)_0^0/h)_1^0$]. A uniform strain rate state is then imposed on the specimen containing the initial nonuniformity. The growth of initial nonuniformity is described by simultaneously integrating a series of equations at different but prescribed locations in the neck. A rate-dependent flow theory of plasticity was used, and the computation of the detailed neck growth was repeated over a wide range of proportional loading conditions to establish the limiting strain FLDs. In addition to η, the input material parameters that are required for the calculation of FLD are *m, n,* ε_0, and M_{33}.

A computed limiting strain FLD and experimental data (Ref 15, 20, 21, 22) are compared for aluminum-killed steel in Fig. 2. The η value of 0.008 was obtained from the measurement of sheet thickness variations. Because the limiting strain refers to the ultimate level of strain in the uniform section while the neck growth has taken place, it is safe to conclude that the material has failed when the state of limiting strain is reached. An additional lower bound is also shown in Fig. 2 by the dashed lines which separate safe from marginal regions, taken from an appliance business practice (Ref 23). An added advantage of specifying such a lower bound is that it incorporates the empirical relationship that accounts for the effects associated with sheet thickness and material properties at the onset of necking (Ref 24).

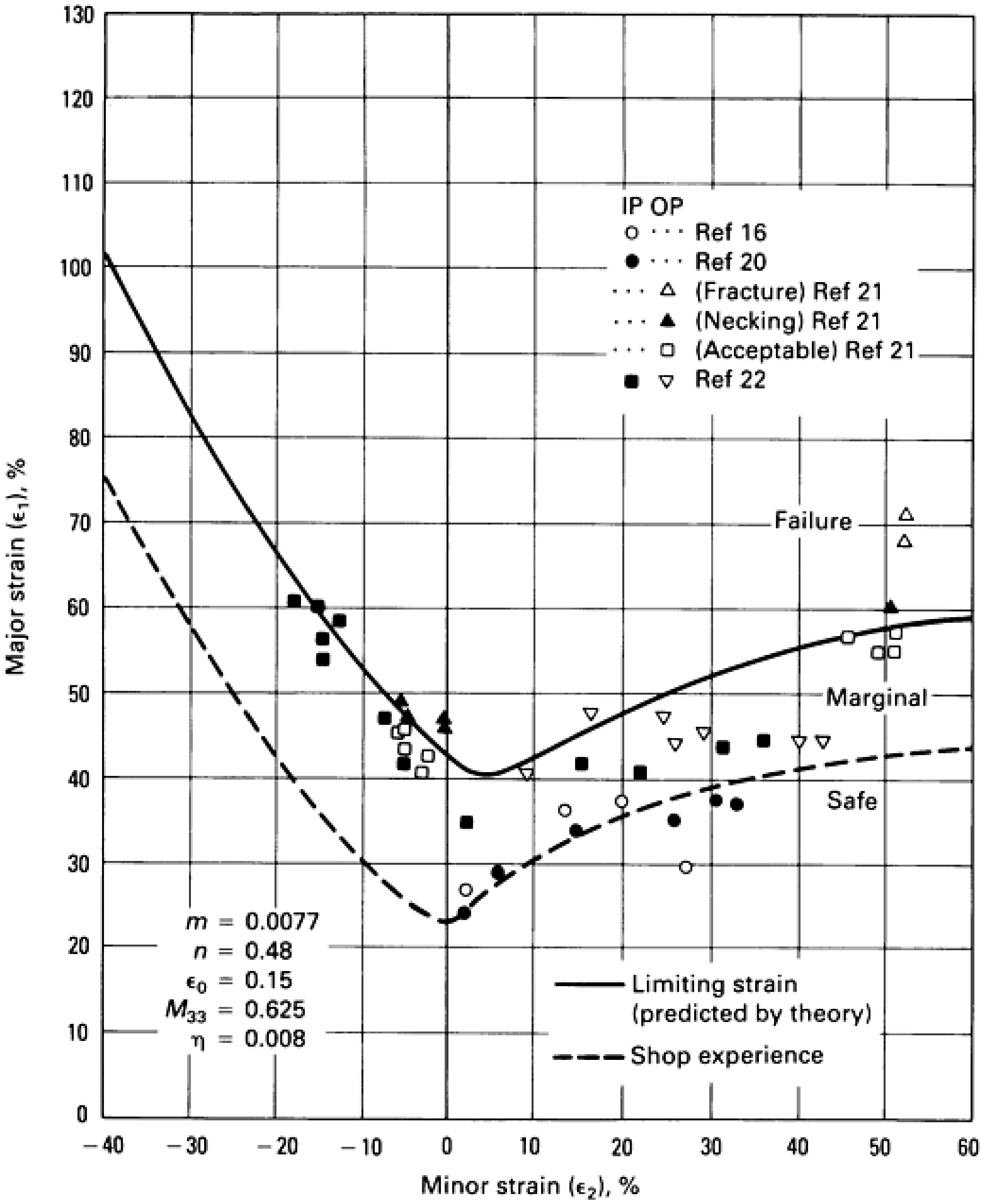


Fig. 2 Comparison of calculated limiting strain forming limit diagrams (FLDs) for aluminum-killed steel with experimental data. Experimental data are distinguished between those tested under the in-plane (IP) and the out-of-plane (OP) loading conditions.

Integrated Systems Approach. A method of processing and controlling the flow of information is outlined in Fig. 3 in the form of a simplified flow diagram. All the critical decision points are also indicated in the diagram. Briefly, the finite element analysis program obtains the necessary input material data, geometry of the part, and boundary conditions of loading, and thereby computes the stress and strain distributions on the formed part. Branching to the right side of the diagram, the necessary material parameters are read again from the material data base, and the theoretical limiting FLD is computed for the particular material. Because necking may occur below the limiting strain region, a boundary depicting the safe and marginal regions obtained from the shop experience is also specified on the computed FLD. Finally, the computed major and minor strains in the formed part are plotted directly on the composite FLD. If all the computed strains in the formed part fall in the safe zone, the program terminates with a sign that indicates that the particular part could be formed without any difficulty. On the other hand, if the computed strain levels are in the failure zone, the program requests that the material, geometry, or the details of loading condition be changed. The entire routine may be repeated until a satisfactory or safe condition is achieved.

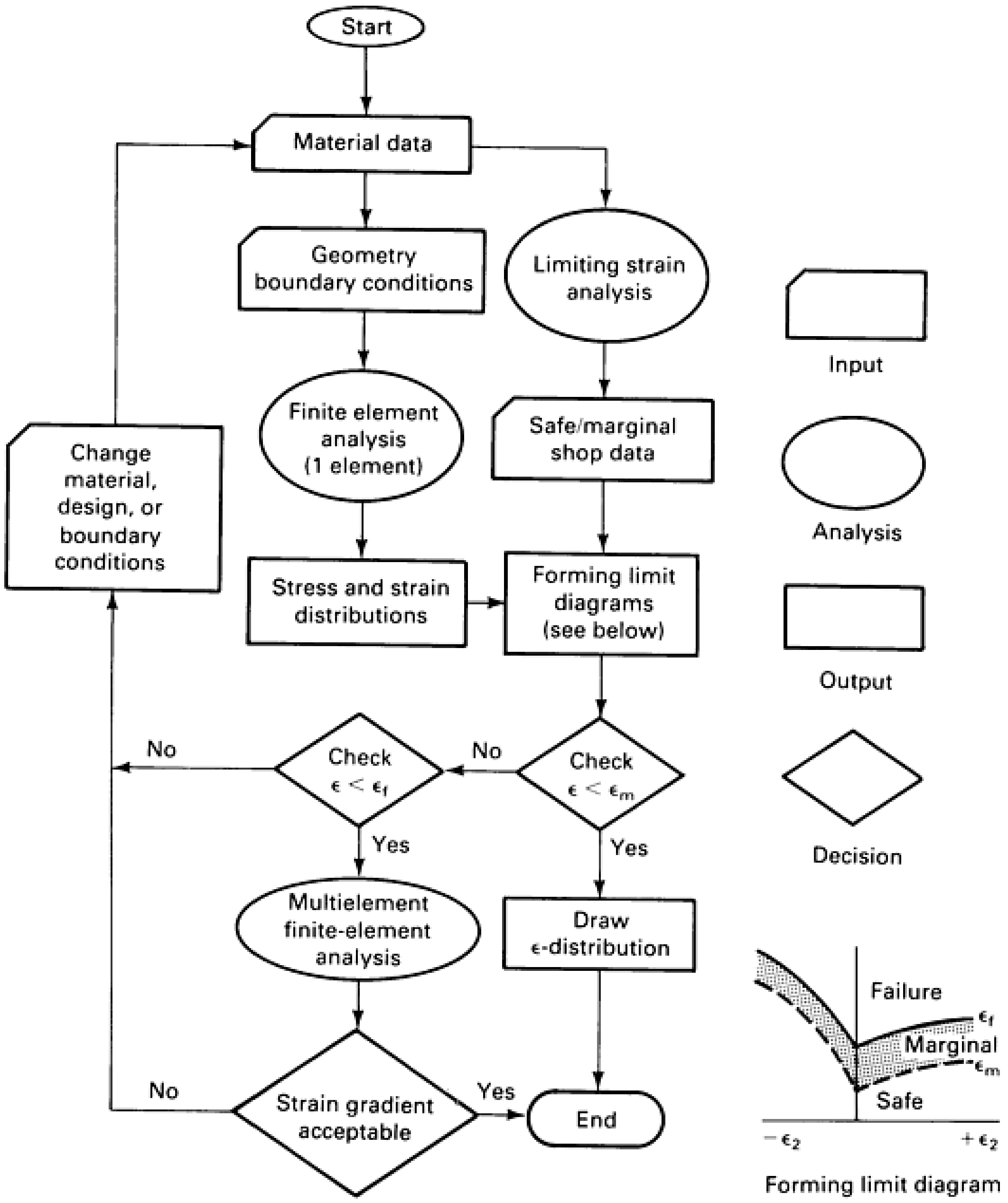


Fig. 3 Flow diagram illustrating various points of input, analysis, output, and decision for the formability analysis.

Because one of the main objectives of computer-aided analysis is to identify critical design and processing parameters while being cost effective, the use of a full-scale finite-element analysis method must be exercised with caution. For example, a typical three-dimensional finite-element mesh generation, analysis, and post-processing of the output for a structural analysis may easily require many days of effort.

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Analytical Methods

Two different types of analytical methods will be examined in this section, one dealing with simple and empirical methods, and the other discussing more elaborate analytical methods for representative sheet metal forming processes. Application of these methods should be based on trade-offs between computation time and accuracy. It should also be pointed out that the following section does not include all the analytical methods that are available for sheet forming processes.

Simple and Empirical Methods

Sheet shearing is a process of separating adjacent parts of a sheet through controlled fracture. This is accomplished by placing the sheet between two edges of the shearing tools--in the case of blanking, a punch and a die. The quality of the cut surface is largely influenced by the clearance between the two shearing edges. Based on experience, the clearance is taken between 2 and 7% of the sheet thickness. The smaller clearance is used with a more ductile material. Because the process is largely localized shear over a very narrow zone where strain hardening takes place, a good approximation of shearing force, $\boldsymbol{P}_s$, is obtained from the following empirical equation (Ref 25):

$$\boldsymbol{P}_s = 0.7\ (\text{UTS}) \cdot h \cdot l \qquad \textbf{(Eq 5)}$$

where UTS stands for the ultimate stress, h is the sheet thickness, and l is the length of cut. When the shearing edges are parallel, the entire length of the contour must be taken into consideration.

Shearing is practiced in a number of other processes. When shearing is accomplished between rotary blades, the process is referred to as slitting; cutting along a straight line is simply shearing. A contoured part, such as a circular or more complex shape, is cut between the punch and die in a press, and the process is called blanking. The economy of the process depends on the proper physical arrangement of the parts to minimize scrap losses. Essentially, the same process is also used to remove unwanted parts of a sheet, such as punching of a hole.

Sheet Bending. One of the main characteristics of the sheet bending process is stretching (tensile elongation) imposed on the outer surface and compression of the inner surface (Fig. 4). There is only one line (the neutral line) that retains its original length. For a given sheet thickness, the tensile strain increases with decreasing bend radius. A structural weakening of the bent part occurs when elongation in the outer fiber exceeds the uniform elongation of the material, ϵ_u, in the tensile test. Referring to Fig. 4, the engineering strain e_t is equal to:

$$e_t = -y/\rho \qquad \textbf{(Eq 6)}$$

where y equals $\frac{1}{2}$ the sheet thickness, t, and ρ is bend radius $R + \frac{1}{2}t$. By substituting and rearranging,

$$e_t = \frac{1}{\left(\frac{2R}{t+1}\right)} \qquad \textbf{(Eq 7)}$$

The minimum permissible radius (or more generally, radius-to-thickness ratio) is related to the outer fiber strain, as shown by Eq 7. For example, as the bend radius to sheet thickness ratio, R/t, increases, the strain in the outer surface decreases. In order to develop a simple relationship between the geometry and material properties to achieve a maximum strain on the outer surface, it is assumed that the true strain at cracking is equal to fracture strain ϵ_f, that the material is isotropic and homogeneous, and that the plane strain condition is valid, that is, that width is relatively large compared to thickness.

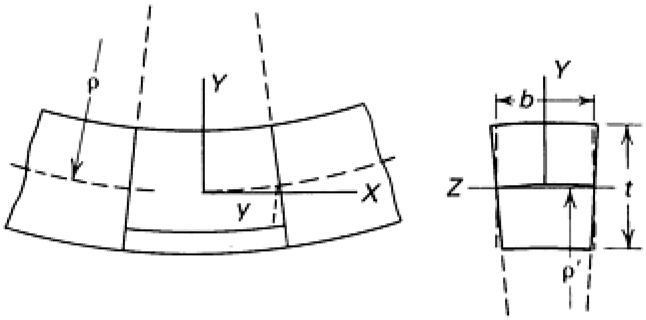

Fig. 4 A simple bending geometry.

The true strain at fracture in tension, ϵ_f, is equal to:

$$\epsilon_f = \ln(A_0/A_f) \qquad \textbf{(Eq 8)}$$

where A_0 and A_f are the initial and final areas. The reduction in area, r, is defined as l - (A_f/A_0). Therefore, $\epsilon_f = \ln(1/(1 - r))$. Because the maximum surface strain, ϵ_s, is equal to ln (e_t + 1), substituting the expression for e_t, gives:

$$\epsilon_s = \ln\left[\frac{R+t}{\frac{R+t}{2}}\right] \qquad \textbf{(Eq 9)}$$

Combining the two equations above and setting $\epsilon_f = \epsilon_s$, the final expression is:

$$\frac{R}{t} = \frac{1}{2r} - 1 \qquad \textbf{(Eq 10)}$$

Failure may take place by either nonuniform deformation (thinning) or by splitting the outside surface layer, which depends on ϵ_f.

Sheet Drawing. Although cup drawing may appear to be a simple operation, a thorough analysis of the forming process is rather difficult (Ref 26). A simple analysis method was, however, outlined by E. Siebel (Ref 27). From the equilibrium condition in the radial direction, the required drawing load and its variations along the punch stroke were determined analytically. The following equation for calculating the drawing load, F_d, has been proposed (Ref 27):

$$F_d = \pi d_m S_0 \left[\exp\left(\frac{\mu\pi}{2}\right) \cdot 1.1\sigma_{f,m} \ln\left(\frac{d}{d_m}\right) + \mu(d - d_m)P_{bh}/S_0 + \sigma_{f,m}\,(S_0/2r_d)\right] \qquad \textbf{(Eq 11)}$$

where d_m is the mean wall diameter, d is the instantaneous outside radius of the flange, S_0 is the blank thickness, r_d is the die radius, $\sigma_{f,m}$ is the mean value of flow stress of the blank material, μ is the coefficient of friction between flange and die or blankholder, and P_{bh} is the blankholder pressure. In Eq 11, the first term represents the ideal deformation load including the load increase due to friction at the die radius, the second term is the component produced by friction between the flange and die or blankholder, and the last term is the load necessary for bending the sheet around the die radius.

A typical load-stroke diagram for deep drawing, shown in Fig. 5, indicates that the flow stress increases continuously with punch displacement as the flange diameter becomes smaller (Ref 26). It has been suggested that the maximum drawing load is nearly independent of the workpiece material and the drawing ratio and occurs when $d = 0.77d_0$ (Ref 27), where d_0 is the initial blank diameter.

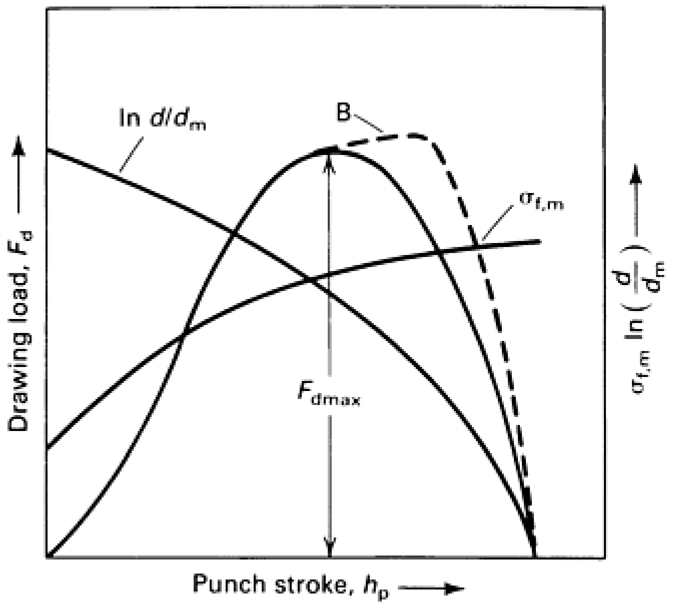

Fig. 5 Typical load-stroke diagram for the drawing process. The portion of the curve labeled B indicates the load distribution for narrow clearances or ironing at the end of the draw. Maximum drawing load F_{dmax} occurs when diameter $d \cong 0.77d_0$, where d_0 is the initial blank diameter.

In general, buckling cannot occur as long as the blank is clamped rigidly between the blankholder and die. During cup formation, the sheet thickness in the flange does not remain constant but may increase toward the outside edge; the center portion near the die radius becomes thinner than the initial thickness, S_0 in the early stages of the draw. Because the distance between the blankholder and die is determined by the largest flange thickness, there will be a gap between the blankholder and sheet near the die radius. This gap may create an opportunity for the initiation of wrinkles. Thin sheets (d_0/S_0 > 25 to 40) are especially sensitive to wrinkle formation because their moment of inertia in buckling is relatively small. The pressure necessary to avoid wrinkling depends on the material properties, the relative sheet thickness, and the drawing ratio. Earlier work by E. Siebel and H. Beisswänger showed that the required blankholder pressure could be estimated from the following empirical equation (Ref 27):

$$P_{bh} = 10^3 c[(\beta - 1)^3 + 0.005\,(d_0/S_0)]S_u \qquad \textbf{(Eq 12)}$$

where the factor c ranges from 2 to 3; β is the drawing ratio, which is defined as the ratio of the initial blank diameter d_0 to the inside diameter of the finished cup d_1; and S_u is the ultimate tensile strength of the material.

The flange-wrinkling behavior of sheet materials in deep-drawing operations was further analyzed by B.W. Senior (Ref 28) and J.M. Alexander (Ref 29). Their expression for the blankholder pressure is given as:

$$P_{bh} = \frac{E_0 t^3 \gamma (b^4 n^4 - 7.464 a^4)}{30.54 a^3 b^3} \qquad \textbf{(Eq 13)}$$

where the buckling modulus E_0 is equal to $4E\rho/(E^{1/2} + \rho^{1/2})^2$, E is the elastic modulus, ρ is the tangent modulus, t is the sheet thickness, γ is the wrinkle amplitude, b is the flange width, n is the number of wrinkles, and a is the mean radius of the flange. Equation 13 shows that blankholder pressure increases with the flange width in a complicated fashion.

Qualitatively, it is conceivable that proper control of the blankholder pressure may reduce fracture and wrinkles in simple drawing operations while reducing the punch load at the same time. In fact, possible ranges of the variation of minimum and maximum allowable blankholder pressure along the drawing depth were estimated by E. Doege and N. Sommer (Ref 30); the blankholder motion was also controlled by a cam mechanism to achieve greater drawing ratios by E.I. Odell (Ref 31).

Analytical Methods

Sheet forming operations may be roughly grouped into two types for the purpose of developing analytical models. One class includes both drawing and stretch forming, where the variations in stress and strain through the thickness of the sheet are usually neglected if any sort of closed-form solution is to be obtained. Within this class, drawing is one extreme, in which the sheet experiences tension in one principal direction and compression in the other; the other extreme is biaxial tension, which occurs in stretch forming. The other class of sheet forming operations is made up of those cases where bending is dominant. In bending, through-the-thickness variations in stress and strain must be taken into account.

Often a given forming operation will involve both bending and stretching/drawing. To include both, it may be necessary to divide the problem into parts, solving the bending and stretching/drawing problems separately and combining the results. A general solution of the combined problem will usually require a numerical procedure such as the finite-element method. In this section, the assumptions and equations used to develop a closed-form solution will be illustrated for both types of operations, using a simple example of each, and some of the techniques developed for extending these solutions to more difficult problems will be outlined.

The general approach to developing a model for a process involves several steps. The first step is to classify the problem according to whether a bending or stretching/drawing type of solution is applicable, and then to express the appropriate strains in terms of the geometry. This involves making simplifying assumptions about either the geometry or the strain distribution; constancy of volume is also usually assumed.

The second step is to choose a constitutive model, relating the stresses in the material to the strains. A constitutive or material model may consist of a definition of effective stress and effective strain and/or strain rate and the relationship between them, plus a flow rule that further relates the individual components of stress to the components of strain.

The next step is to write an equation of equilibrium, usually a force balance obtained by equating the forces applied by the tooling to the stresses in the material required to support these loads. In sheet forming, the effects of inertia and gravity are usually neglected in this force balance. In order to solve the resulting equations and obtain a useful solution, it is often necessary to refine the model, changing some of the assumptions or introducing some additional ones; therefore, the whole process is usually somewhat iterative.

Bending Analysis. As a first example, consider the bending of a flat plate to a prescribed radius. As a bending problem it is clear that the variations in both stress and strain through the thickness of the sheet are important. In order to relate the strains in the sheet to the bending geometry, it is convenient to make several assumptions. First of all, plane strain conditions are assumed to exist throughout the sheet, because the width of the sheet along the axis of the bend will not change significantly. While this assumption is not valid near the edges of the sheet, it is a good assumption as long as the width of the bend is much greater than the thickness of the sheet ($w/t \gg 1$).

Second, the neutral axis is assumed to remain in the center of the sheet, meaning that the midplane of the sheet does not change in length. The error introduced by this approximation is greater with tighter bends, but it is often reasonable for practical bend radii. Another assumption is that planar cross sections of the sheet remain planar and do not warp during the bend.

With the above assumptions in mind, the strains in the sheet may be related to the bend geometry. Referring to Fig. 6, let L_0, be the arc length at the midplane of the sheet, and let r_0, be the radius of curvature of the midplane. Before bending, all planes parallel to the surface of the sheet had the same length as the midplane ($L_0 = r_0\theta$), while the length of a plane in the bent configuration is given by $L = r\theta$. The engineering strain is:

$$e_\theta = \frac{\Delta L}{L_0} = \frac{r\theta - r_0\theta}{r_0\theta} = \frac{z}{r_0} \quad \textbf{(Eq 14)}$$

where $z = r - r_0$. This is not identical to the true strain, but for the strains encountered in most bends the two strains are nearly equal. Because e_w, has been assumed to be zero (that is, plane strain conditions exist throughout the sheet), assuming volume constancy gives $e_t = -e\theta$.

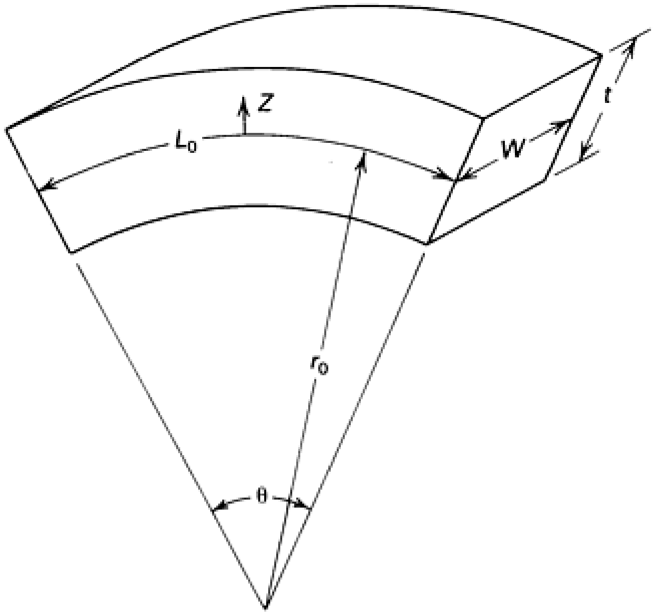

Fig. 6 Geometry definitions for bending analysis.

In order to find the stress distribution, it is necessary to have a material model. For simplicity, assume that the material is elastic-perfectly plastic (no work hardening), and that the stress-strain behavior is the same in tension or compression. While no material is exactly described by this model, in many cases it will give good results if the plastic flow stress chosen for the model is somewhere between the actual yield strength of the material and its ultimate strength.

With this model, the material toward the outside of the bend will be at some uniform value in tension, and the material at the inner part of the bend will be at the same uniform value in compression; the central part of the sheet will be elastic (see Fig. 7). Using both the von Mises yield criterion

$$(\sigma\theta - \sigma_t)^2 + (\sigma_t - \sigma_w)^2 + (\sigma_w - \sigma\theta)^2 = 2Y^2 \quad \textbf{(Eq 15)}$$

where Y is the plastic flow stress, and the plastic flow rule

$$\frac{\delta e_\theta - \delta e_t}{\sigma_\theta - \sigma_t} = \frac{\delta e_t - \delta e_w}{\sigma_t - \sigma_w} = \frac{\delta e_w - \delta e_\theta}{\sigma_w - \sigma_\theta} \quad \textbf{(Eq 16)}$$

the plastic stress level may be determined. In the plastic flow equation, δe refers to an increment of plastic strain, but in bending, these relationships also hold for the total strain because the ratio of the strains does not change during the bend. Using the results obtained for the strains, the stresses are computed to be:

$$\sigma\theta = \sqrt{4/3}\,Y$$
$$\sigma_w = \sqrt{1/3}\,Y \quad \textbf{(Eq 17)}$$
$$\sigma_t = 0$$

While it has not been stated what kind of tooling is being used to make this bend, the bending moment required can be computed. To do this, the contribution of the product of stress, area, and distance from the neutral axis is integrated to give the bending moment, *M*, about the neutral axis of the sheet:

$$M = \int_{-t/2}^{t/2} w\sigma_\theta z dz \qquad \textbf{(Eq 18)}$$

While this integral can be evaluated for the assumptions stated so far, it can be simplified a great deal by neglecting the elastic part of the material near the center of the sheet. For many practical ratios of sheet thickness to bend radius, this does not introduce much error. Making this assumption, and using the values for plastic flow stress just calculated, the following result is obtained:

$$M = \frac{wt^2 Y}{2\sqrt{3}} \qquad \textbf{(Eq 19)}$$

At this point the springback may be computed by noting that the applied bending moment goes to zero upon unloading, and therefore the internal moment must also go to zero. The (elastic) strains can be computed from the new geometry. Letting ***r***' be the new radius of curvature of the midplane after unloading:

$$\Delta e_\theta = \frac{z}{r_0} - \frac{z}{r'} \qquad \textbf{(Eq 20)}$$

Because the unloading is elastic, the appropriate material model for this part of the problem is Hooke's law, and solving for the stress in the θ direction,

$$\Delta\sigma_\theta = \frac{E\Delta e_\theta}{1 - \nu^2} = E' \, \Delta e_\theta \qquad \textbf{(Eq 21)}$$

where *E* is Young's modulus and *E*' = *E*/(1 - ν^2) is called the plane strain or bending modulus.

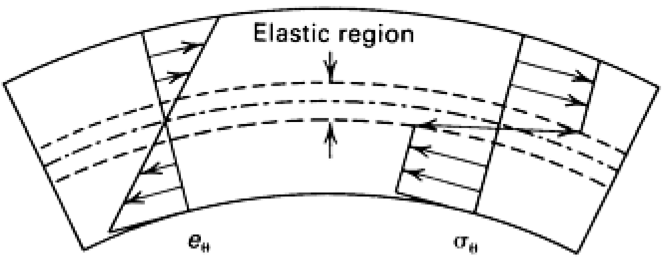

Fig. 7 Stresses and strains in bending.

The equilibrium condition here is that the change in the internal moment during unloading must be equal to the change in the external moment. The change in the internal bending moment is obtained from the following integral, which may be evaluated by using Eq 20 and 21:

$$\int_{-t/2}^{t/2} w\Delta\sigma_\theta z dz = \frac{wt^3 E'}{12}\left[\frac{1}{r_0} - \frac{1}{r'}\right] \qquad \textbf{(Eq 22)}$$

Because the change in the external moment is equal to the original applied moment (Eq 19), the equilibrium equation is:

$$\frac{wt^3E'}{12}\left[\frac{1}{r_0}-\frac{1}{r'}\right]=\frac{wt^2Y}{2\sqrt{3}} \quad \textbf{(Eq 23)}$$

Solving for *r'*, the radius of curvature of the midplane after springback:

$$\frac{1}{r'}=\frac{1}{r_0}-\frac{6Y}{\sqrt{3}E't} \quad \textbf{(Eq 24)}$$

This result is useful in predicting springback in simple bending. However, the assumptions made in deriving this expression must be kept in mind when applying it, principally that the ratio of sheet thickness to bend radius must be neither too small nor too large, and that the only load applied by the tooling is the bending moment. For instance, if the sheet is bent while in tension, or if a significant compressive stress is applied across the thickness of the sheet (due to contact with the die), the springback will be reduced. These conditions must be incorporated into the derivation if a solution for either of these cases is desired.

There are other examples of closed-form analytical solutions of sheet bending processes. For example, R. Hill (Ref 32) formulated the theory of plane strain bending for rigid-perfectly plastic materials. F. Proksa (Ref 33) used Hill's suggested displacement equations in developing a theory for plane strain bending of rigid-linear hardening materials. Extensive work has also been done by B.W. Shaffer and his collaborators (Ref 34, 35, 36).

If the process is more complicated than one of simple bending, or if a more accurate solution is desired, it may be necessary to use a numerical approach to solve the equations. For example, in air bending, considerably more springback will occur upon unloading than is predicted by Eq 24 because of the elastic unbending of the unsupported portion of the sheet. An analysis of this process (Ref 37) considers the springback to be due to both the portion of the sheet in contact with the punch and the free section of the sheet. These contributions are combined with the actual shape of the material response curve to compute the springback, using an iterative-solution procedure.

Other numerical solutions of the bending process have also been developed. For example, K.H. Wolter (Ref 38) and H. Verguts and R. Sowerby (Ref 39) took the deformation history of individual fibers into account. In a later study, further improvements were made by P. Dadras and S.A. Majlessi (Ref 40, 41).

Drawing Analysis. The analysis of sheet drawing or stretching is different in some respects, as described earlier, and an analysis of the cup-drawing process provides a good example. The following analysis is similar to work by R. Whitely (Ref 42) and shows the effect of anisotropy on the limiting drawing ratio.

The geometry of cup drawing is clearly more complex than can be represented by radial drawing alone, but an analysis of the flange, or radial drawing portion of the cup can yield useful results. To simplify the analysis, it is assumed that the thickness change may be neglected, or $\epsilon_t = 0$ (see Fig. 8).

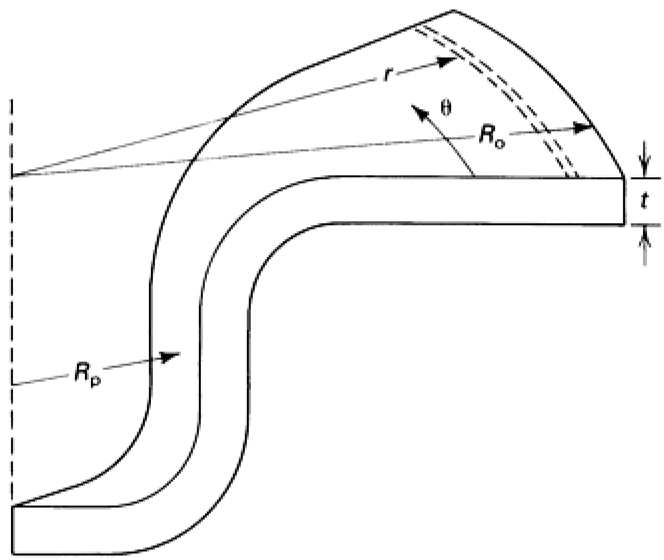

Fig. 8 Geometry definitions for drawing analysis.

The circumferential strain, $\epsilon\theta$, is related to the change in circumference at any radius by:

$$\epsilon_\theta = \ln\left[\frac{2\pi r}{2\pi R}\right] = \ln\left[\frac{r}{R}\right] \qquad \textbf{(Eq 25)}$$

where r is the radius of any element of material and R is the initial radius of that element. Based on the assumption of volume constancy with $\epsilon_t = 0$, the radial strain is given by $\epsilon_r = -\epsilon\theta$.

To relate the stresses to the strains, a rigid-perfectly plastic material model is assumed. To further simplify the analysis, the Tresca yield criterion is chosen, so that the difference between largest and smallest principal stress is assumed to be a constant at any point in the sheet. In the flange, shear stresses induced by friction are neglected, and the coordinate stresses are taken to be the principal stresses. The radial stress, σ_r, is therefore the (algebraically) largest principal stress, and the compressive circumferential stress, $\sigma\theta$, is the smallest. Therefore:

$$\sigma_r - \sigma\theta = Y_f \qquad \textbf{(Eq 26)}$$

where Y_f is the flow strength of the material in the flange under the condition that $\epsilon_t = 0$.

For the equilibrium equation, consider a small element of material in the flange between the radii r and $r + dr$ (see Fig. 9). Because in practice the flange is slightly thicker at the outer edge than anywhere else, it is assumed that any blankholder load is concentrated there. As a result, $\sigma_t = 0$ throughout the flange. Balancing the forces acting on this element in the r direction:

$$\sigma_r|_r tr\theta + 2\sigma\theta\, tdr \sin(\theta/2) = \sigma_r|_{r+dr} t(r + dr)\theta \qquad \textbf{(Eq 27)}$$

where $\sigma_r|_r$ is equal to σ_r evaluated at radius r. For small angles, sin (θ) ~θ, or:

$$\sigma_r|_r tr\theta + \sigma\theta\, tdr\theta = \sigma_r|_{r+dr} t(r + dr)\theta \qquad \textbf{(Eq 28)}$$

Rearranging and dividing through by r, t, and θ:

$$\frac{\sigma_r|_{r+dr} - \sigma_r|_r}{dr} = \frac{\sigma_\theta - \sigma_r|_{r+dr}}{r} \qquad \textbf{(Eq 29)}$$

Taking the limit as $dr \rightarrow 0$, the equilibrium equation at a point becomes:

$$\frac{\delta\sigma_r}{\delta r} = \frac{\sigma_\theta - \sigma_r}{r} = -\frac{Y_f}{r} \qquad \textbf{(Eq 30)}$$

Because of the assumption of a rigid-perfectly plastic material, the equilibrium equation turns out such that the stresses may be computed without reference to the strain distribution. Integrating Eq 30:

$$\sigma_r(r) = C - Y_f \ln(r) \qquad \textbf{(Eq 31)}$$

where C is an integration constant. Because $\sigma_r(R_0)$ is known, where R_0 is the radius of the outer edge of the flange, $C = Y_f \ln(R_0) + \sigma_r(R_0)$. This gives:

$$\sigma_r(r) = \sigma_r(R_0) + Y_f \ln\left(\frac{R_0}{r}\right) \qquad \textbf{(Eq 32)}$$

For the case of no blankholder, $\sigma_r(R_0) = 0$, or:

$$\sigma_r(r) = Y_f \ln\left(\frac{R_0}{r}\right) \qquad \textbf{(Eq 33)}$$

Now if the effects of bending and friction over the draw ring are neglected, the radial stress that must be supported by the cup wall to draw successfully may be computed from Eq 33:

$$\sigma_r(R_p) = Y_f \ln\left(\frac{R_0}{R_p}\right) \qquad \textbf{(Eq 34)}$$

where R_p is the punch radius. The draw will fail if $\sigma_r - \sigma\theta \geq Y_w$, the flow stress in the wall. The flow in the wall is characterized by $\epsilon\theta = 0$, so that for an anisotropic material, Y_w will be different from Y_f. Taking $\sigma\theta = 0$ at the top of the cup wall, the drawing limit is reached when:

$$Y_w = Y_f \ln\left(\frac{R_0}{R_p}\right) \qquad \textbf{(Eq 35)}$$

This gives the limit drawing ratio as:

$$\frac{R_0}{R_p} = \exp\left[\frac{Y_w}{Y_f}\right] \qquad \textbf{(Eq 36)}$$

This equation predicts a limiting drawing ratio for an isotropic material to be ln(1), or approximately 2.72, but in fact this is quite high. Actual values obtained experimentally are closer to 2.1 or 2.2 (Ref 43). The main reason for the discrepancy is the fact that frictional forces and bending were neglected in the equilibrium equation.

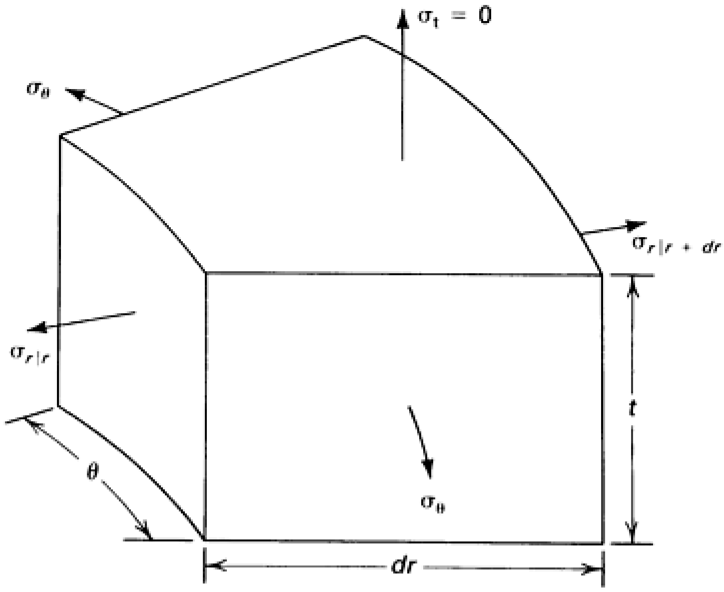

Fig. 9 Flange element for equilibrium equation in drawing.

This analysis may be continued, relating the ratio of flow strengths to the *R* value of the material, and an efficiency factor may be introduced to account for frictional losses and to give a better correlation with experimental results. The resulting theory, while not perfect, does demonstrate the strong dependence of the limiting drawing ratio on material anisotropy.

For a more accurate analysis, a closed-form solution has not yet been obtained. However, a very good solution requiring a computer program and an iterative solution procedure was developed by D.M. Woo (Ref 44, 45) following the same basic steps outlined here, but with much less restrictive assumptions regarding material behavior, friction, and geometry of the sheet. Using the Von Mises yield criterion and a rigidpower law hardening material model, the force equilibrium equations derived above are integrated by approximating the variation of stress and strain (including thickness strain) to be linear across each element. In this manner, the stress and strain distribution for the entire sheet was obtained for each small advance of the punch into the material, with the incremental strains at each stage being added to find the total strain. Many other researchers have since adopted and expanded the resulting finite-difference solution (Ref 46, 47, 48).

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Process Modeling and Simulation for Sheet Forming

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Finite-Element Analysis Methods

With recent advances in high-speed computers, there has been a marked progress in the development and application of numerical methods for mechanics-related problems. Among these techniques, the finite-element analysis method has been widely used in the treatment of diverse classes of problems in this field. In particular, material forming processes, because of their complexity and large number of process variables, are ideally suited for the application of the finite-element analysis technique. A systematic survey of the developments and applications of the technique in the field of material processing has been given by A.S. Wifi (Ref 49). In another review paper, S. Kobayashi (Ref 50) has outlined the important role of the finite-element method in metal forming modeling work.

Although early development of the finite-element method was primarily concerned with linear problems, the method has been extended to nonlinear problems by the initial strain method (Ref 51, 52) and by the tangent modulus method (Ref 53, 54). The actual application of this technique to metal forming problems was made in the 1960s by P.V. Marcal *et al.* (Ref 55), Y. Yamada *et al.* (Ref 56), and O.C. Zienkiewicz *et al.* (Ref 57); additional applications were made in later years by employing the small-strain analysis method (Ref 58, 59, 60, 61, 62, 63, 64, 65).

Metal forming operations, however, inherently involve large strains and therefore different formulations were needed for proper analyses of these processes. During the 1970s two basic approaches for treating large deformation were developed. One of these was the rigid-plastic formulation (Ref 66, 67, 68, 69, 70, 71, 72, 73, 74, 75, 76, 77) where it is assumed that the elastic strains are small compared to plastic strains, and therefore can be ignored. In these analyses, a material model that obeyed the von Mises yield criterion and the associated flow rule were used. The incompressibility constraint was treated by either a Lagrange multiplier (Ref 66) or a penalty function (Ref 78, 79).

The rigid-plastic formulation, however, does not account for elastic unloading; therefore, such phenomena as springback and development of residual stresses cannot be treated by the model. Development of the second approach, elastic-plastic large-deformation finite-element analyses, has been accomplished partially as a result (Ref 80, 81, 82, 83, 84, 85, 86, 87, 88, 89, 90, 91). The classic paper of H.D. Hibbitt *et al.* (Ref 80) introduced one of the first large-strain finite-element formulations, in which a total Lagrangian formulation (TLF) was employed. In this formulation, the initial undeformed configuration of the material is chosen to be the reference state. A.S. Wifi (Ref 81) developed a complete finite-element program and analyzed the axisymmetric stretch-forming and deep-drawing processes, using the incremental TLF. N.M. Wang and B. Budiansky also employed a TLF-based nonlinear membrane theory and simulated the axisymmetric hemispherical punch stretching of sheet metals (Ref 91).

An alternative approach called the updated Lagrangian formulation (ULF), in which the current configuration of the deforming material is considered to be the reference state, was also developed and extensively used (Ref 83, 92, 93). Some of the key papers discussing the development of finite-element analysis using ULF for large elastic-plastic deformation are those of R.M. McMeeking and J.R. Rice (Ref 92) and E.H. Lee (Ref 93). The formulation of initial displacement matrix (the undeformed configuration of the material) is not required in the ULF method.

The basis of the finite-element analysis technique, using the variational approach, is to formulate a functional based on the specific constitutive relations. This functional is usually a statement of the potential energy of the deforming continuum. The finite-element equations are then formulated by the condition that the first variation of the functional should vanish. E.P. Popov *et al.* (Ref 94) employed the shell theory and developed a finite-element program to analyze sheet bending problems. Using this theory gives satisfactory results only when the deformation is moderate and the ratio of thickness to bending radius is small. For analysis of large-strain bending processes, V. Cupka *et al.* (Ref 95) employed the finite-element formulation developed by H.D. Hibbitt *et al.* (Ref 80) and solved a three-point bending problem with counterpressure at the bottom of a sheet. S.-I. Oh and S. Kobayashi (Ref 96) developed two finite-element analysis programs, using formulations suitable for rigid-plastic and elasto-plastic material behavior. It was shown that the results of the two analyses agreed very well with each other. In a recent effort, A. Makinouchi (Ref 97) formulated an elaborate constitutive equation assuming a varying Young's modulus, and developed a finite-element program using a ULF approach. As an example, the essence of that work is outlined in Example 1 of the following section.

Example 1: Finite-Element Analysis of Sheet Bending and Hemming.

An elastic-plastic incremental finite-element analysis program was developed for sheet bending and hemming processes of metals under plane strain conditions (Ref 97). A constitutive equation within the framework of finite deformation was also developed. The derivation of the constitutive equation follows the decomposition of the deformation gradient F as proposed by E.H. Lee (Ref 98):

$$F = V^e F^p \quad \textbf{(Eq 37)}$$

where V^e and F^p are elastic and plastic deformation gradients, respectively. The velocity gradient L in the current configuration, x, and the plastic velocity gradient L^p in the plastically deformed configuration, α, are given by:

$$L = \dot{F}F^{-1}\ 2L^p = \dot{F}^p F^{p-1} \quad \textbf{(Eq 38)}$$

The rate of deformation tensor D, which is the symmetric part of L, is expressed as:

$$D = \frac{1}{2}(L + L^T) = \frac{1}{2}V^{e^{-1}}(V^e \dot{V}^e + \dot{V}^e V^e + V^e V^e L^p + L^{p^T} V^e V^e)V^{e^{-1}} \quad \textbf{(Eq 39)}$$

where T stands for transposed. A measure of elastic strain ^{e} is introduced with respect to the plastically deformed configuration α, and is defined as follows:

$$\epsilon^e = \frac{1}{2}\left\{\frac{\delta u^e}{\delta\alpha} + \left[\frac{\delta u^e}{\delta\alpha}\right]^T\right\} \quad \textbf{(Eq 40)}$$

where u^e is the elastic displacement from the plastically deformed configuration to the current configuration, x. Using the above definitions, it can be shown that:

$$V^e = I + \ ^e \qquad \textbf{(Eq 41)}$$

where I is the unity tensor. Substituting the material time derivative of Eq 41 into Eq 39, we obtain:

$$D = \frac{1}{2}(2\dot{\epsilon}^e - \epsilon^e\dot{\epsilon}^e - \dot{\epsilon}^e\epsilon^e - 2W^p\epsilon^e + 2\epsilon^e W^p + 2D^p) \qquad \textbf{(Eq 42)}$$

where D^p is the plastic rate of deformation tensor, $D^p = (L^p + L^{p^T})/2$, and W^p is the plastic spin tensor $W^p = (L^p = L^{p^T})/2$. Because the analysis is concerned with small elastic strains, the elastic strain rate $\dot{\epsilon}^e$ can be neglected compared to plastic rate of deformation D^p; thus Eq 42 reduces to:

$$D = \dot{\epsilon}^e - W^p\ ^e + \ ^e W^p + D^p \qquad \textbf{(Eq 43)}$$

It is important to note that the first three terms in Eq 43 have the form of a Jaumann rate (Ref 92) of $\ ^e$. Therefore:

D = **MATH OMITTED**$^e + D^p$ **(Eq 44)**

where **MATH OMITTED**^{e} is the Jaumann rate of $\ ^e$. The above relationship is the rate form of the elastic-plastic decomposition for cases with small-elastic but finite-plastic deformation, and replaces the following well-known relationship:

$$D = D^e + D^p \qquad \textbf{(Eq 45)}$$

In one finite-element analysis program, R. Hill's variational principle (Ref 99) and the updated Lagrangian formulation has been employed. Hill's variational principle is expressed in the form:

$$\int_v [(\tau_{ij} - 2\sigma_{ik}D_{kj})\delta D_{ij} + \sigma_{jk}L_{ik}\delta L_{ij}]dV = \int_s t_i \delta v_i ds \qquad \textbf{(Eq 46)}$$

Constant-strain triangular elements were used in the calculations. In order to verify the theory, a number of experiments using mild steel and high-strength steel sheet were carried out. The stress-plastic strain relation obtained from the tensile tests is expressed by a Swift-type equation:

$$\overline{\sigma} = C(\ _0 + \overline{\epsilon}^p)^n \qquad \textbf{(Eq 47)}$$

It has been shown that the evolution of the elastic modulus can also be presented by a similar relation:

$$E = E_0\,(1.0 + 100\overline{\epsilon}^p)^m \qquad \textbf{(Eq 48)}$$

Figure 10 shows a comparison between computed and measured springback angle. Two computed results are shown for each material. One is obtained by using a constant Young's modulus E_0, and the other by using the variable Young's modulus E. As shown in Fig. 10, a closer agreement with the experimental data is achieved when a variable elastic modulus is used. Figure 11(a) illustrates the computed effective strain and the longitudinal stress profile at a 45° bent angle. In Fig. 11(b), the distribution of longitudinal stress σ_{ll}, normal stress σ_{nn}, lateral stress σ_{zz}, and shear stress σ_{ln} are depicted for the bent angle of 45°.

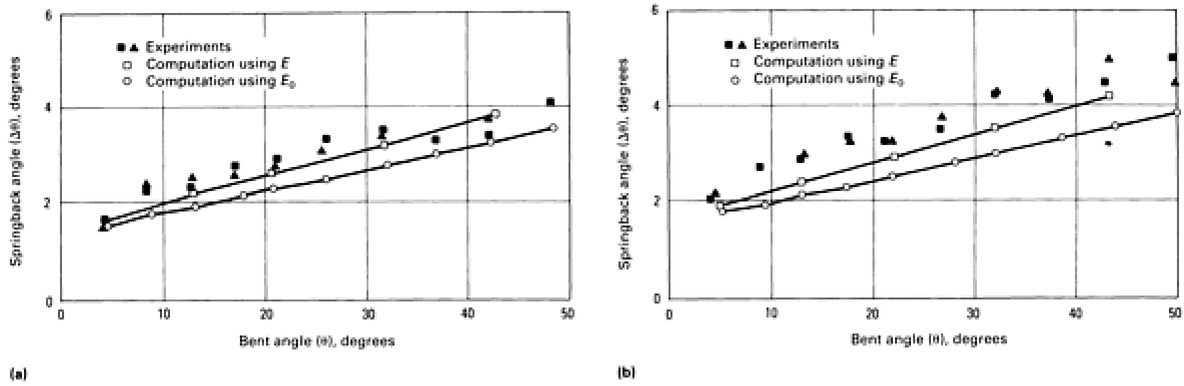

Fig. 10 Comparisons of the springback angle obtained experimentally and using two different computations. (a) Low-carbon steel. (b) High-strength steel. E_0, constant Young's modulus; E, variable Young's modulus. Source: Ref 97.

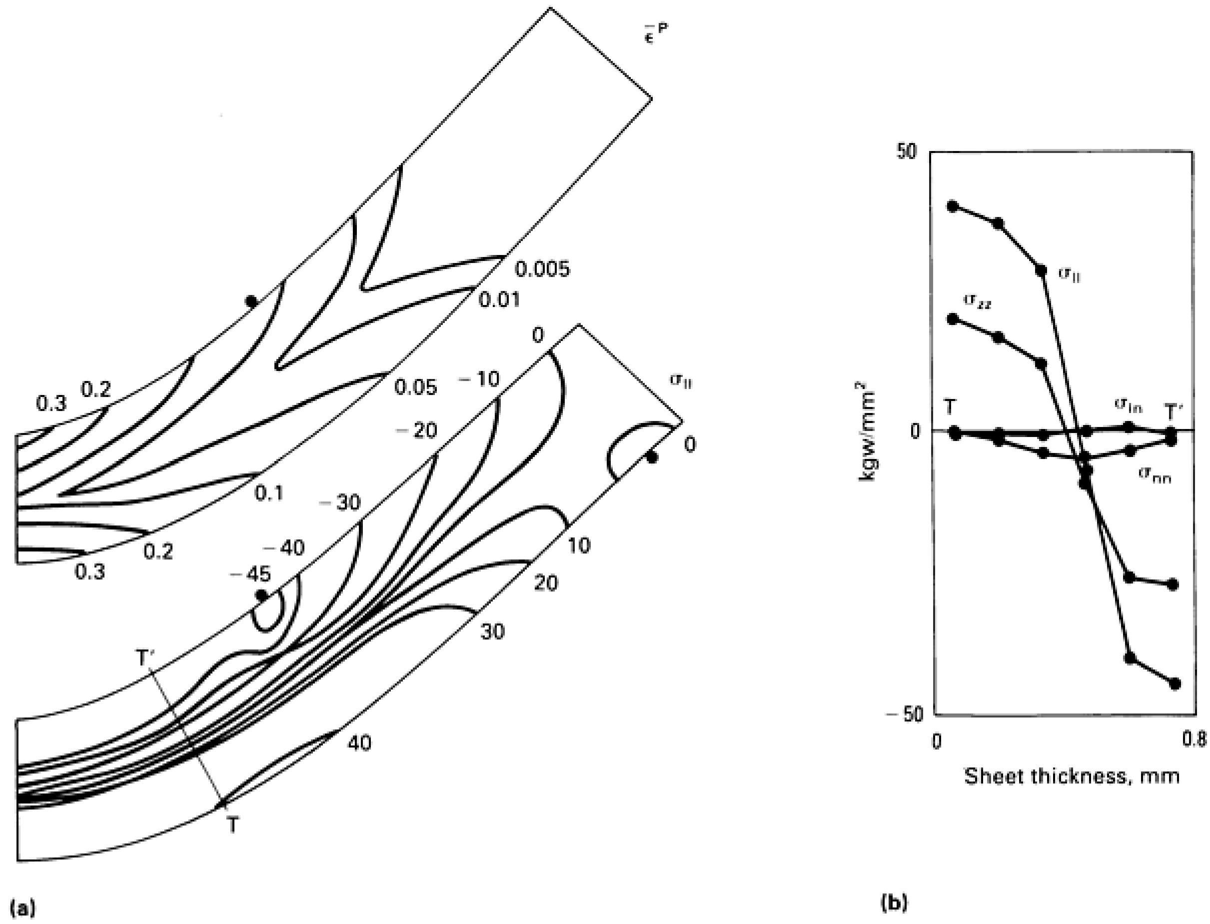

Fig. 11 Distribution of (a) effective strain and longitudinal stress and (b) stress components in the cross section for a high-strength steel material model with bend angle of 45°. Punch diameter, 2.0 mm (0.08 in.); die span, 6.4 mm (0.25 in.); sheet thickness, 0.8 mm (0.03 in.). Source: Ref 97.

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Sheet Stretching

Punch stretching can be characterized as a nonsteady metal-forming process because of the presence of the moving boundary that separates the sheet in contact with the punch head from the unsupported region of the sheet. Because of its complexity, a detailed analysis of the stretching process can only be made by numerical methods such as finite-difference (Ref 100, 101, 102) and finite-element techniques (Ref 91, 103, 104, 105, 106).

N.M. Wang and B. Budiansky (Ref 91) assumed that the sheet metal is rate insensitive, elasto-plastic, and exhibits normal anisotropy. Using a convected coordinate system, the variational principle was formulated and the resulting equations from finite-element discretization were solved. S. Kobayashi and his collaborators have also analyzed the sheet stretching process in rate-sensitive and rate-insensitive materials. The common assumption in those analyses is that the elastic strains are small compared to plastic strains and therefore can be ignored. The following example is presented to illustrate some of the important parts of this type of finite-element formulation.

Example 2: Finite-Element Analysis of Punch Stretching of Rate-Sensitive Materials.

In Ref 105, a finite-element program has been developed to analyze the axisymmetric punch stretching of rate-sensitive materials. A rigid-viscoplastic material model with normal anisotropy has been assumed. The finite-element model was based on the following variational formulation:

$$\delta\Phi = \int_V [\bar{\sigma}\delta(d\bar{E}) + H\, d\bar{E}\, \delta\,(d\bar{E})]dV - \int_{S_F} F \cdot \delta u\, dS = 0 \qquad \text{(Eq 49)}$$

where $\overline{\sigma}$ is the effective stress, $d\overline{E}$ is the incremental effective logarithmic strain, and H is the slope of the stress-strain curve. The second integral represents the variation of the work done by the traction F. In developing Eq 49, it was assumed that the principal axes of true strain rates maintain the same direction in each element and that the ratio of the principal strain rate components remain constant during each time increment. The following constitutive equations for a rate-sensitive material with normal anisotropy have been employed:

$$\bar{\sigma} = Y\left[+\frac{1}{\gamma}\left(\frac{d\bar{E}}{dt}\right)_{\mathrm{m}}\right] \qquad \text{(Eq 50)}$$

where Y is the yield stress and γ and m are material parameters. Following R. Hill's theory of anisotropy (Ref 32), effective stress and increment of effective strain are defined as:

$$\bar{\sigma} = \sqrt{\sigma_\theta^2 - \frac{2R}{1+R}\sigma_\theta\sigma_\phi + \sigma_\phi^2} \qquad \text{(Eq 51)}$$

and

$$d\bar{E} = \frac{1+R}{\sqrt{1+2R}}\sqrt{dE_\phi^2 + \frac{2R}{1+R}dE_\phi dE_\theta + dE_\theta^2} \qquad \text{(Eq 52)}$$

The incremental-strain components are defined as follows:

$$dE_\theta = \frac{d\hat{E}}{\bar{\sigma}}\left[\sigma_\theta - \frac{R}{1 + R^\sigma \phi}\right] \quad \textbf{(Eq 53)}$$

and

$$dE_\phi = \frac{d\hat{E}}{\bar{\sigma}}\left[\sigma_\phi - \frac{R}{1 + R^\sigma \theta}\right] \quad \textbf{(Eq 54)}$$

The sheet geometry is divided into a number of conical frustums, and the functional Φ is expressed within each element in terms of the nodal values associated with the particular element. The condition that the first variation of the functional vanishes provides the stiffness equations. These equations are nonlinear and are solved by the Newton-Raphson iterative scheme.

For verification purposes, a number of aluminum-killed steel specimens were stretched, and the results were compared with the predicted values of the finite-element program. Figure 12 shows the computed radial, $\varepsilon\phi$, and hoop, $\varepsilon\theta$, strain distributions for two coefficients of friction (μ = 0.25 and μ= 0.4) at the punch-sheet interface. The agreement with the measured strains is generally good. In order to examine the sensitivity of the material to strain rate, four separate computations were carried out with different punch speeds. In one case, a rate-insensitive material model was used. The computed load versus punch travel is depicted in Fig. 13.

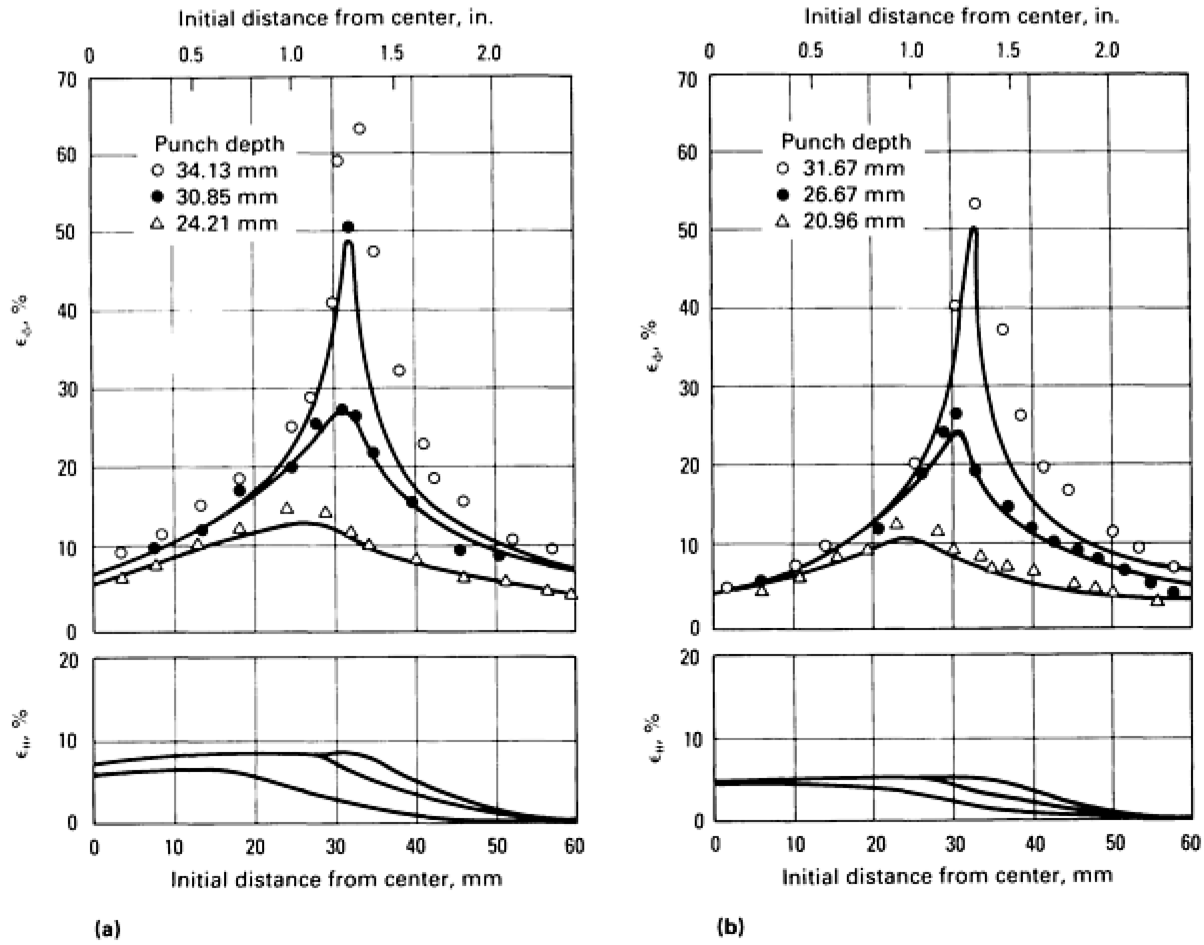

Fig. 12 Comparison of computed and measured strain distribution at several punch depths for two coefficients of friction. (a) μ= 0.25. (b) μ= 0.4 Source: Ref 105.

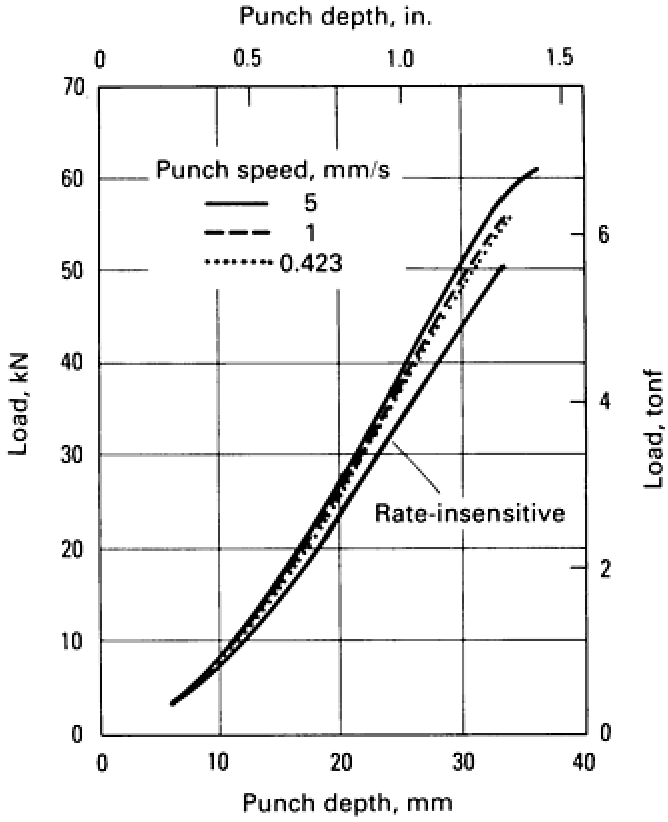

Fig. 13 Punch load-displacement curves for three punch speeds in stretching of rate-sensitive material and one punch speed for rate-insensitive material. Source: Ref 105.

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Deep Drawing

Several finite-element analysis programs using different formulations have been proposed to study the deep-drawing process. The majority of these analyses employed a rigid-plastic material model and the incremental theory of plasticity to analyze axisymmetric geometries. The process of nonaxisymmetric sheet metal forming has also been examined by several authors. C.H. Toh and S. Kobayashi (Ref 107, 108, 109) used a rigid-plastic finite-element analysis method based on the membrane theory to model deep drawing of nonaxisymmetric geometries. Y.J. Kim and D.Y. Yang (Ref 110) also developed an incremental rigid-plastic finite-element analysis method to analyze a hydrostatic-bulging process of rectangular sheet diaphragms.

As an alternative approach, S. Levy *et al.* applied the deformation theory of plasticity and developed a finite-element analysis program to approximate a cup-drawing process of an elastic-plastic material. It was shown that considerable savings in computational time was achieved by using the deformation theory (Ref 111). D. Lee and coworkers (Ref 10, 112, 113) further improved this approach to account for the anisotropic behavior of materials, and demonstrated that a computer software system based on the above approach can be applied to simulate actual sheet metal forming processes. A subsequent refinement of the analysis has also been made, with improved analytical and numerical methods (Ref 114). The basic approach has also been used by R.W. Logan and W.F. Hosford (Ref 115) to analyze necking and wall wrinkling in sheet metal stretching/drawing. They included a scheme for updating the reference state to the current configuration in the analysis, and reported an excellent computational efficiency over the stepwise incremental formulation.

Although a number of general-purpose finite-element codes have been developed lately for analyzing nonlinear problems (Ref 116, 117), they are not suitable for modeling sheet metal forming processes. Moreover, the prohibitive length of computation time has prevented the use of such programs as an interactive tool at the design stage. The main advantage of the membrane-type elements and deformation theory of plasticity is the simplicity. With such a simplified method, solutions may be obtained quickly, and yet they are within an acceptable range of engineering approximations.

Example 3: Using Final Part Configuration to Compute Size of the Undeformed Blank.

In Ref 114, a number of isoparametric elements were specified on the prescribed final configuration of an axisymmetric part, and the shape and size of the undeformed blank sheet were computed. It is shown that considerable savings in computer time can be achieved by using deformation theory.

The computational scheme is composed of first defining the locations of the nodal points in the undeformed configuration (trial nodal points), and then improving these locations by applying a Newton-Raphson iterative scheme. The principle of minimum potential energy is employed in the following manner. The potential energy of deformation ψ is decomposed into two parts:

$$\psi = \psi_x + \psi_u \qquad \textbf{(Eq 55)}$$

The first term, ψ_x, is the energy associated with the deformation of the trial nodes from the initial to the final configurations, and is given by:

$$\Psi_X = \int_V \left(\int_0^{\epsilon_f} \sigma d\epsilon \right) dV - \int_S T_X \, w_X dS \qquad \textbf{(Eq 56)}$$

The volume integral in the above equation refers to the strain energy where ϵ_x, is the strain due to deformation of the trial nodes from their beginning to end positions. The surface integral represents the work of traction forces. For a given final configuration, ψ_x, holds a constant value once the trial nodal positions are assumed.

The second term in Eq 55, ψ_u, is the increment of potential energy due to the small displacements, u_i, of the trial nodal points in the initial configuration, and is given by:

$$\Psi_u = \int_V \left(\int_{\epsilon_X}^{\epsilon_f} \sigma \, d\epsilon \right) dV - \int_S T_u w_u \, dS \qquad \textbf{(Eq 57)}$$

where ε_f, denotes the final strain. It has been shown that the surface integral of Eq 57 represents the frictional work only. Because ψ_u is a function of the small increments, u_i, the correct values of u_i can be determined by minimization of ψ_u:

$$\frac{\partial \Psi_u}{\partial (u)} = 0 \qquad \textbf{(Eq 58)}$$

Because of the nonlinear nature of the problem, the correct values of the initial nodal positions have to be determined through several iterations.

In Fig. 14, the computed strain distributions in radial and hoop directions are shown for a hypothetical cup model. In order to verify the model, several experiments have been conducted for axisymmetric cups made of drawing quality steel sheet. As shown in Fig. 15, the measured strain values agree well with the computed results, although there are some discrepancies in the punch profile region where the description of friction is not well defined.

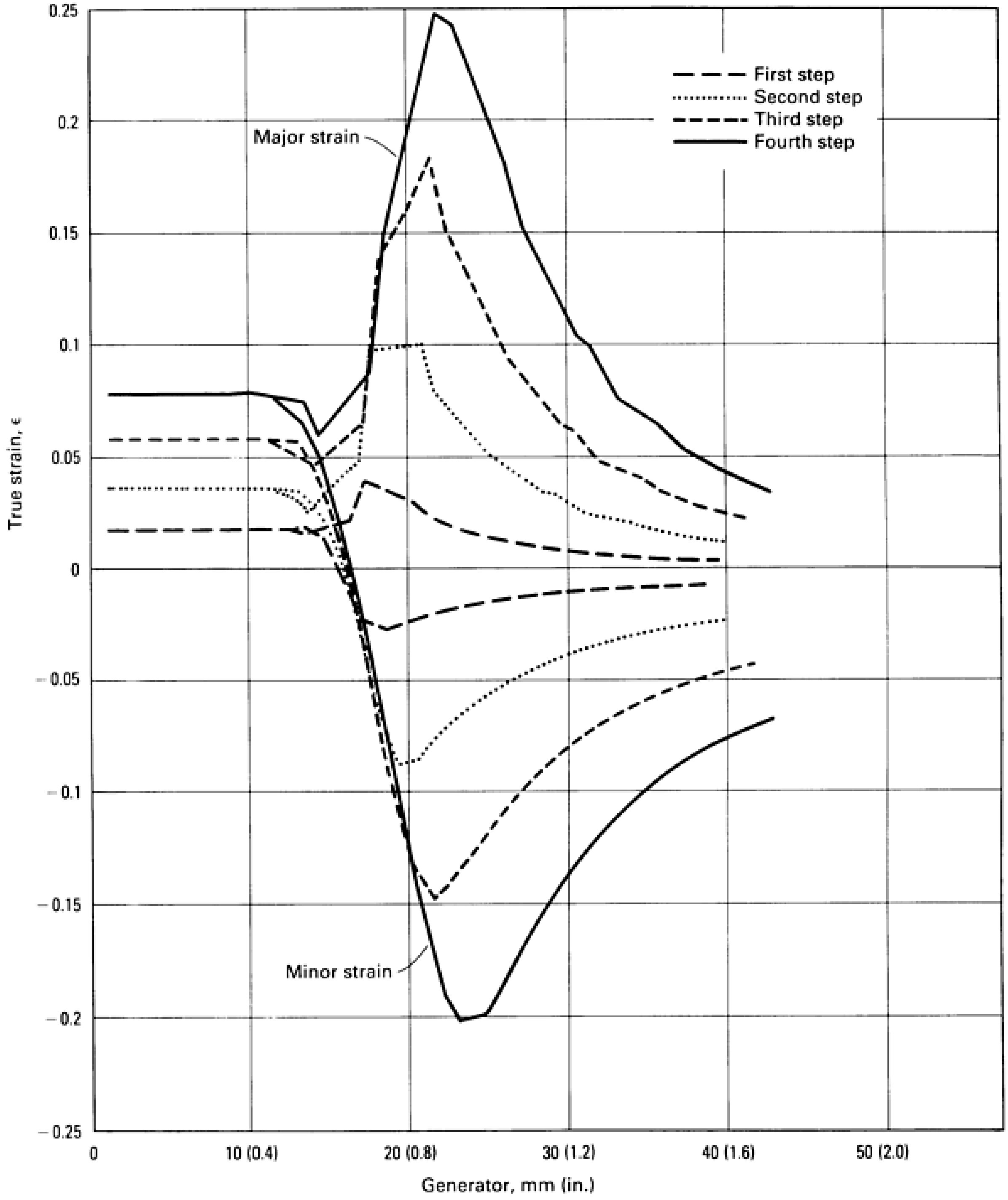

Fig. 14 Strain distributions at the end of each step of a four-step forming process for a steel cup.

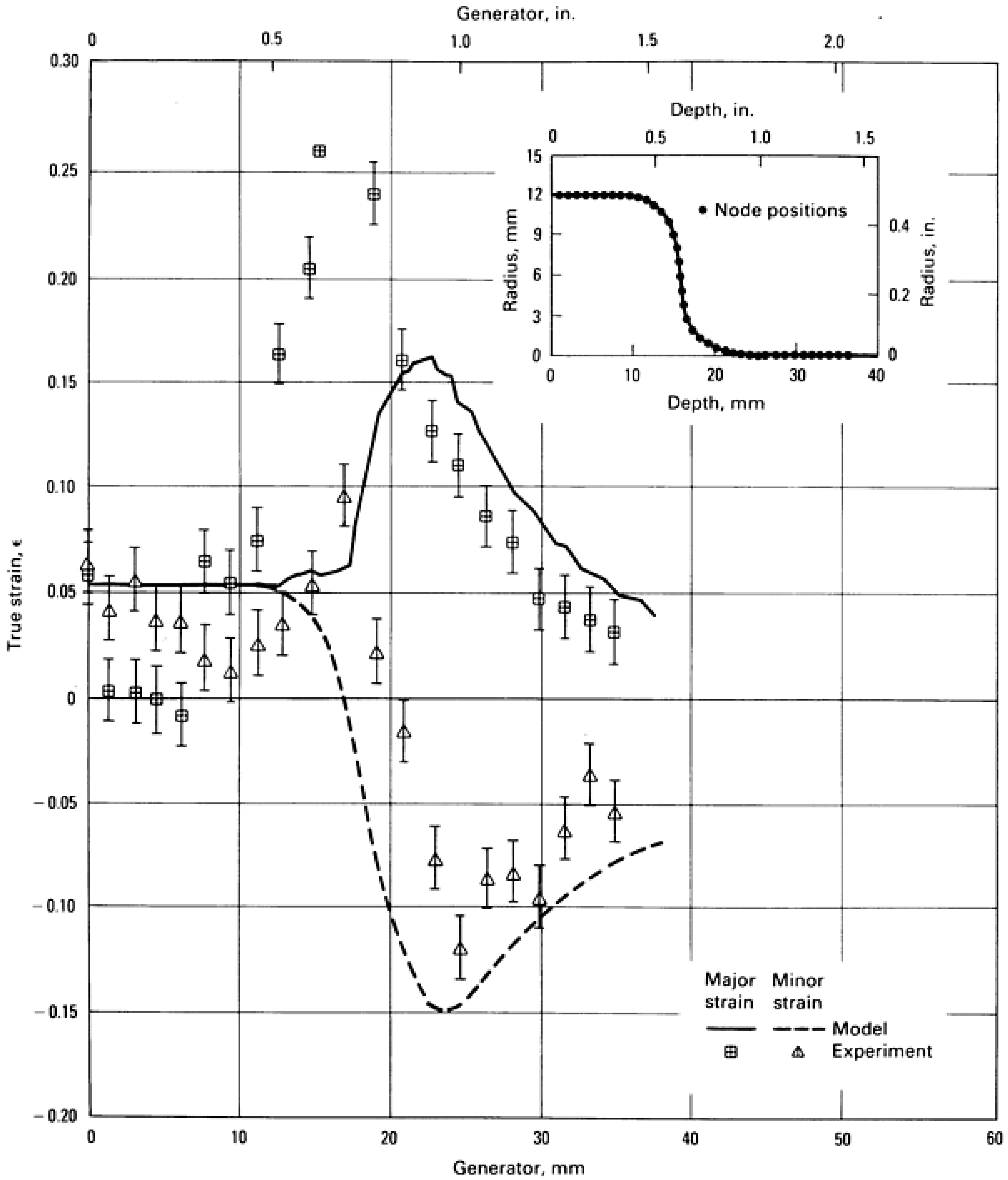

Fig. 15 Comparison of the computed strains along the final generator with experimental data of 11.10 mm (0.437 in.) deep cup made without lubrication. Inset shows the cup geometry. Source: Ref 114.

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Process Modeling and Simulation for Sheet Forming

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Case Studies

This section will examine a total of three cases in order to illustrate different possibilities that occur under these sheet forming conditions. All the cases represent actual forming for which experiments have been conducted to verify the predictions.

In the cup forming, the frictional boundary condition was changed while the geometry was kept the same. In the vent-forming case, a portion of the three-dimensional part was approximated as an axisymmetric geometry. The box forming process was analyzed using three-dimensional sheet forming analysis.

Example 4: Cup Forming.

A photograph of the formed cup and the corresponding finite-element representation of the cup geometry are shown in Fig. 16. Details of the imposed frictional boundary conditions are that the measured coefficient of friction (0.14) was used in cup 1, and an exceedingly low coefficient of friction (0.02) was applied to cup 2 as a hypothetical case. Appropriate normal loads were also specified, and the relative motion of the sheet metal with respect to the die surface was accounted for by the sign of friction coefficient. Selection of specific nodal points for the application of the normal force has been found to be not critical; the normal force may be distributed over the appropriate nodal points.

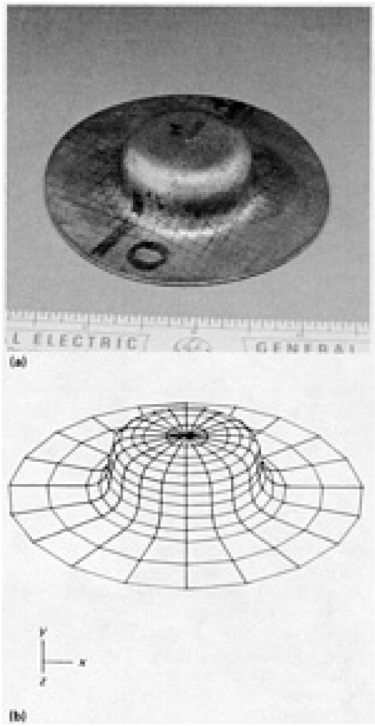


Fig. 16 (a) Photograph of the formed part. (b) A finite-element model for cup.

Given the material properties, the geometry file, and the loading boundary conditions, the next step in the flow diagram (Fig. 3) is to compute the strain distributions when the cups have been formed. A parallel analysis starting from the material data base yields a computed limiting strain FLD. The required inputs were strain rate sensitivity m, strain hardening coefficient n, offset strain ε_0, anisotropy index M_{33}, and the initial inhomogeneity index, η. Superimposing the safe/marginal shop FLD on the computed limiting FLD gives all the information that is necessary to establish a base for FLD criteria.

All the necessary information to make a decision is in place; the strain distributions for the particular formed part will be compared against the failure criteria, that is, the composite FLD, as shown in Fig. 17. The computed radial (major) and hoop (minor) engineering strains at each nodal point are identified in the composite FLD by the corresponding nodal point numbers. The resulting display gives the information necessary to proceed with the next step: Strain levels in some of the nodal points in cup 1 exceed the safe/marginal boundary, while with cup 2 it is entirely safe to proceed.

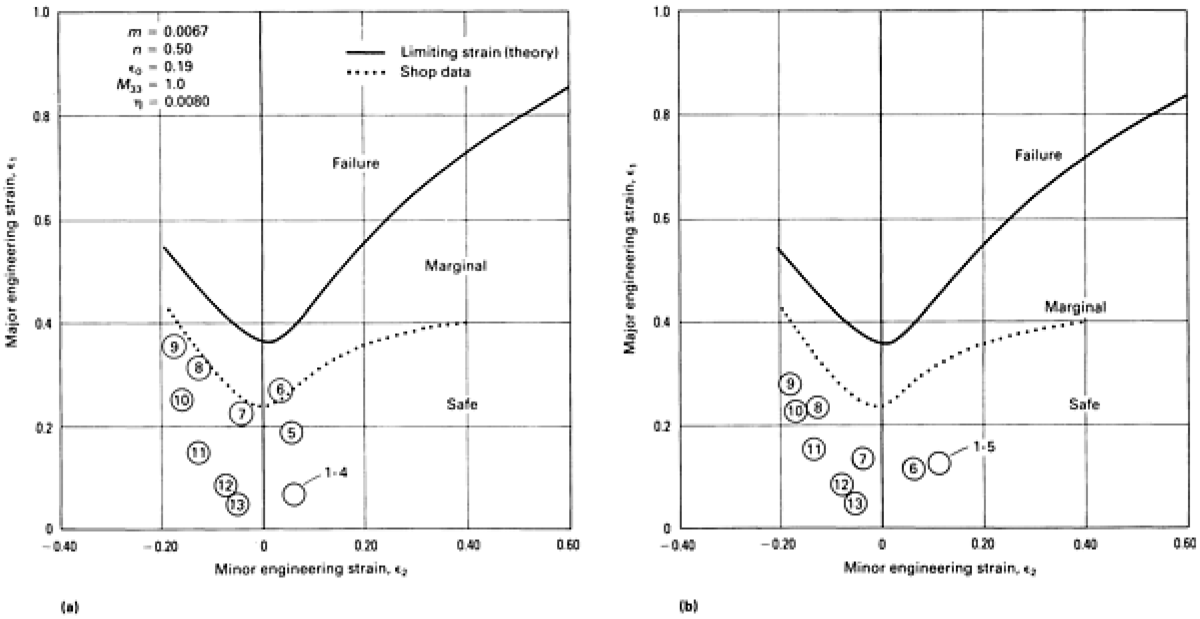


Fig. 17 Comparisons of theoretical and practical limiting strains at each node point for (a) cup 1 and (b) cup 2.

Example 5: Vent Forming.

The same procedure used for cup forming will be followed for the vent forming analysis. Photographs of the formed vent 1 part and the finite-element geometry model corresponding to the round section of the formed part are shown in Fig. 18. An obvious difference between the two is the side flange, which is not axisymmetric in the formed part. The effect of assuming a circular flange section in the analysis will be discussed when the analytical result is compared with the experimental data.

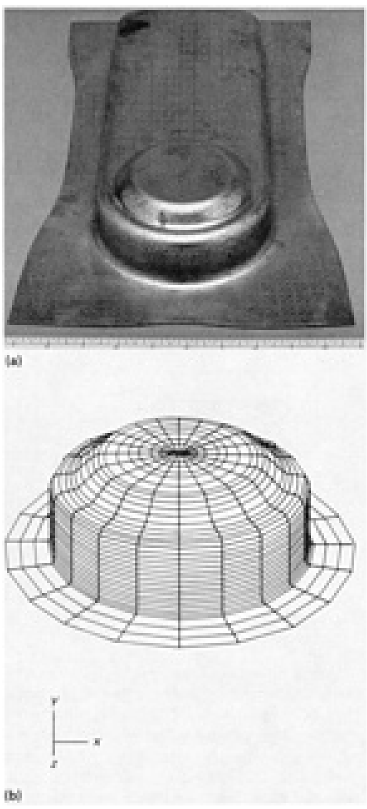


Fig. 18 (a) Photograph of the formed part and (b) finite element model for the circular section in (a) for vent.

Following the same procedure used in the cup analysis, the computed results are compared with the safe/marginal/failure FLD criteria shown in Fig. 19. Vent 1 can be formed without any difficulty. Laboratory forming of vent 1 did not cause any necking, which is consistent with the finite-element analysis results.

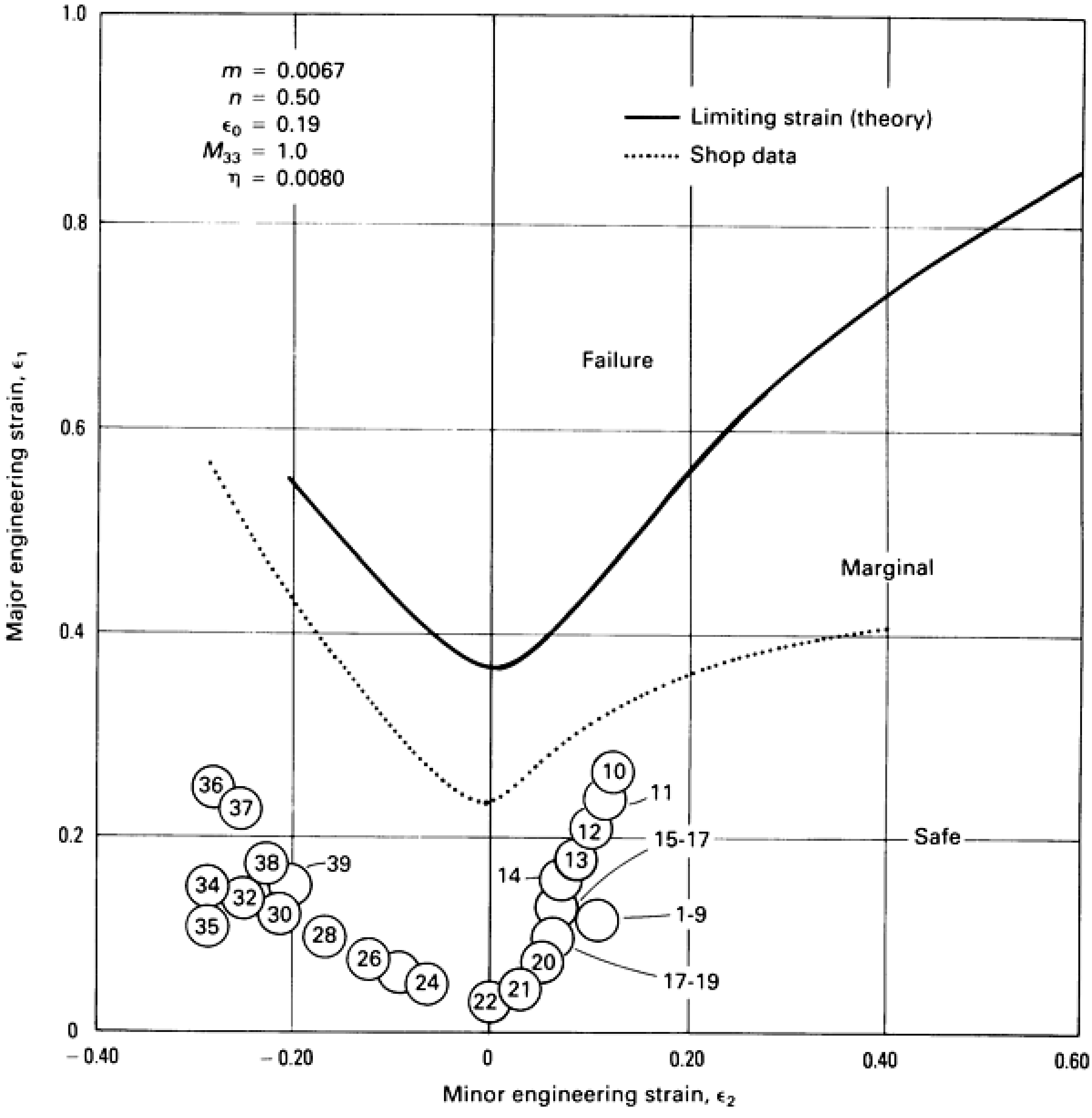


Fig. 19 Comparisons of theoretical and practical major and minor strains at each node point for vent 1.

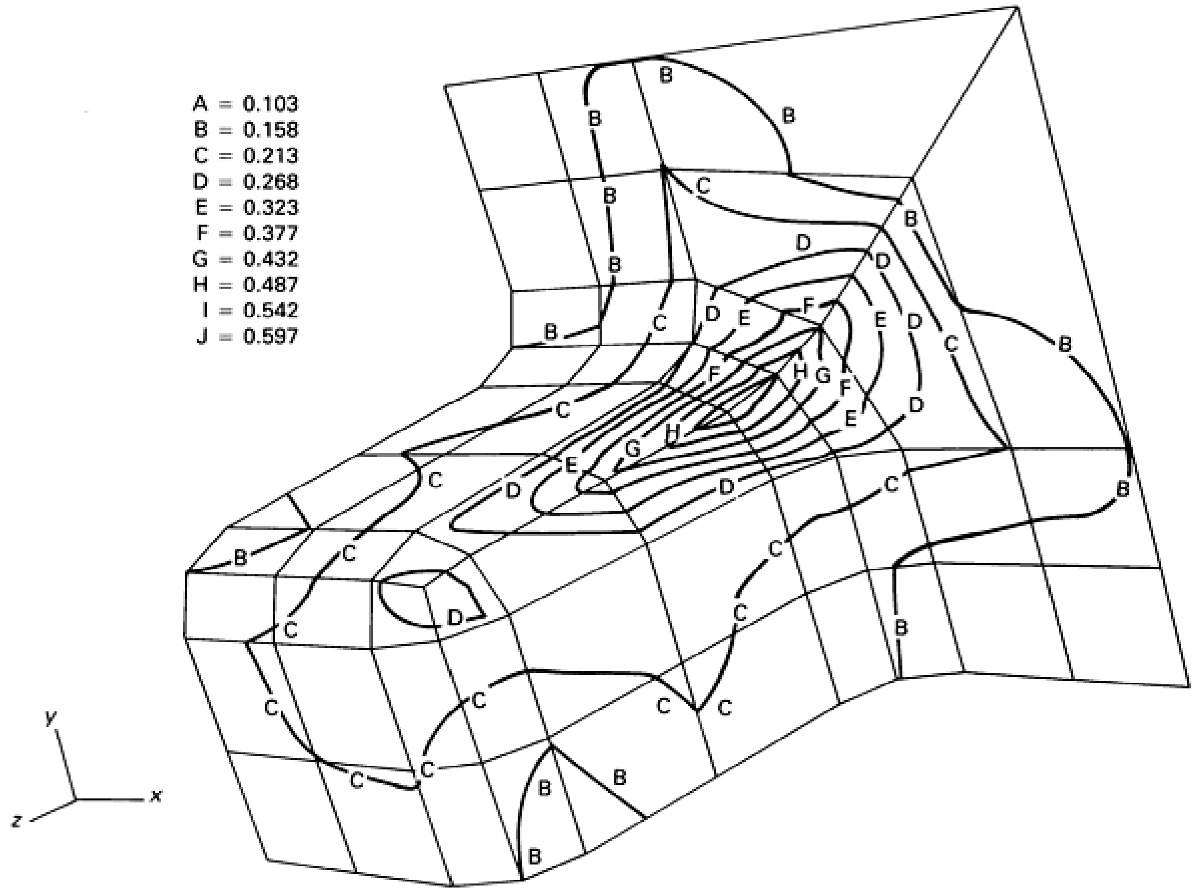
Example 6: Square Box Forming.

The three-dimensional square-box forming process was modeled following an analytical procedure similar to that used in Examples 4 and 5. Here again, two assumptions were made: First, the plane stress condition is valid, and, second, the deformation theory of plasticity is applicable. The unknowns, then, are the initial location of the element nodal coordinates as well as the blank shape. These unknowns are computed by applying the principle of minimum potential energy. The basic mathematical approach is summarized in Ref 118.

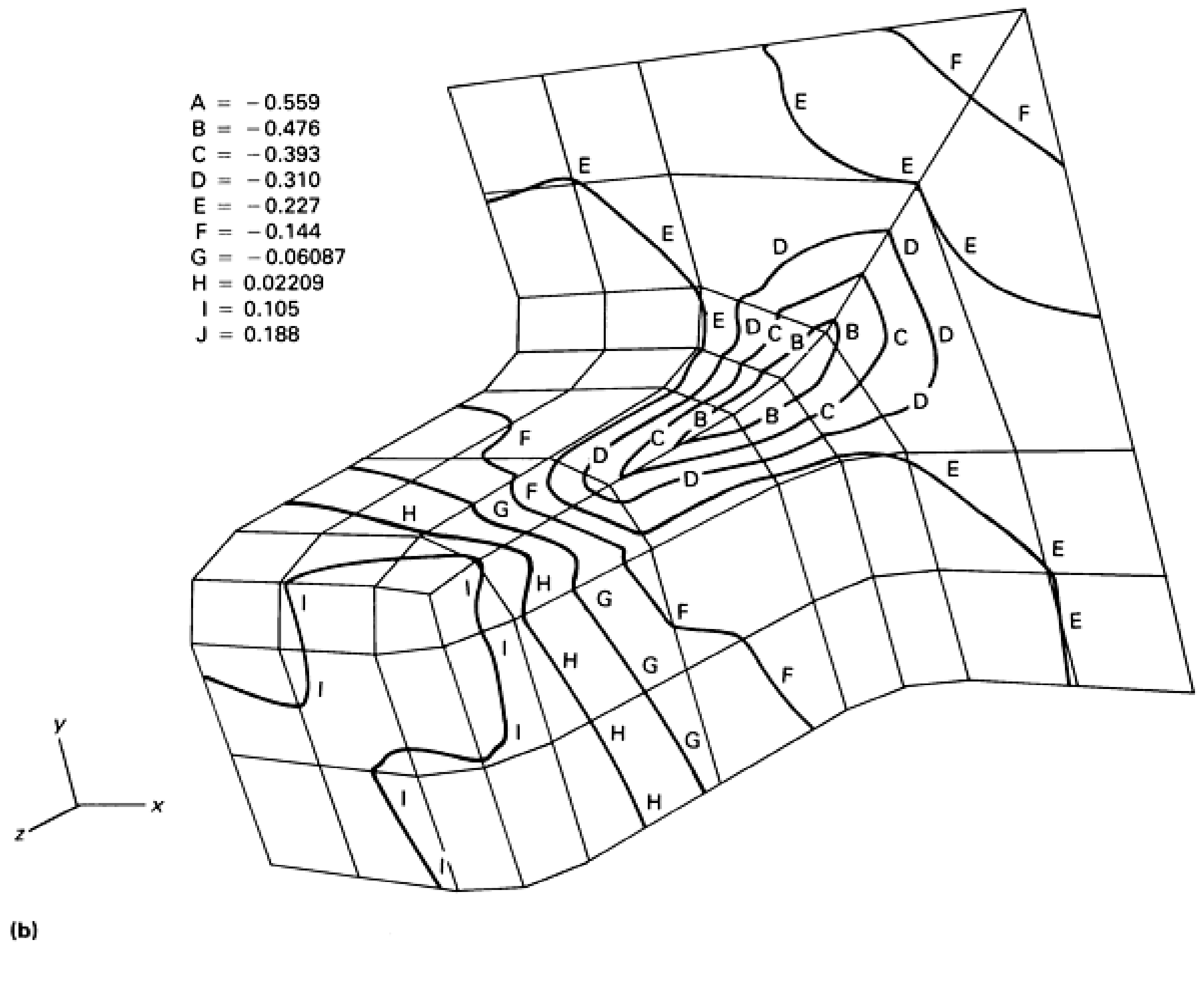
A summary of the details of square-box geometry and loading boundary conditions include:

- Material: aluminum-killed steel
- Stress-stain curve: $\sigma= 739\ \varepsilon^{0.3}$ MPa
- Punch size: 40 × 40 mm (1.6 × 1.6 in.)
- Die size: 42.5 × 42.5 mm (1.7 × 1.7 in.)
- Punch radius: 5 mm (0.2 in.)
- Die radius: 5 mm (0.2 in.)
- Corner radius: 3.2 mm (0.125 in.)
- Blank size (undeformed): 110 × 110 mm (4.3 × 4.3 in.)
- Coefficient of friction: 0.2 at the punch and 0.04 at the die
- Blank holding pressure: 500 kgf (1100 lbf)

The deformed sheet domain used in the square-cup simulation is shown in Fig. 20. Eight noded isoparametric membrane-type elements were used. The solution was converged within 25 iterations to increments of nodal displacements of <25 μm (0.001 in.). Figures 21 and 22 show the major strain distributions across the diagonal and transverse generators of the square cup, respectively. The experimental results obtained by T.R. Thomson (Ref 119) were also included for comparison. Although a considerable discrepancy of the results is seen under the flat punch profile, good correlation between the predicted finite-element model and the experimental results is observed. In particular, the experimental and predicted results match most closely at the critical areas of the punch and die profile radii where the highest strains are observed.


A = 0.103
B = 0.158
C = 0.213
D = 0.268
E = 0.323
F = 0.377
G = 0.432
H = 0.487
I = 0.542
J = 0.597
y
x
z

(a)


A = −0.559
B = −0.476
C = −0.393
D = −0.310
E = −0.227
F = −0.144
G = −0.06087
H = 0.02209
I = 0.105
J = 0.188
y
x
z

(b)

Fig. 20 (a) Major and (b) minor strain distributions superimposed on the finite-element mesh representing one-quarter of the square box.

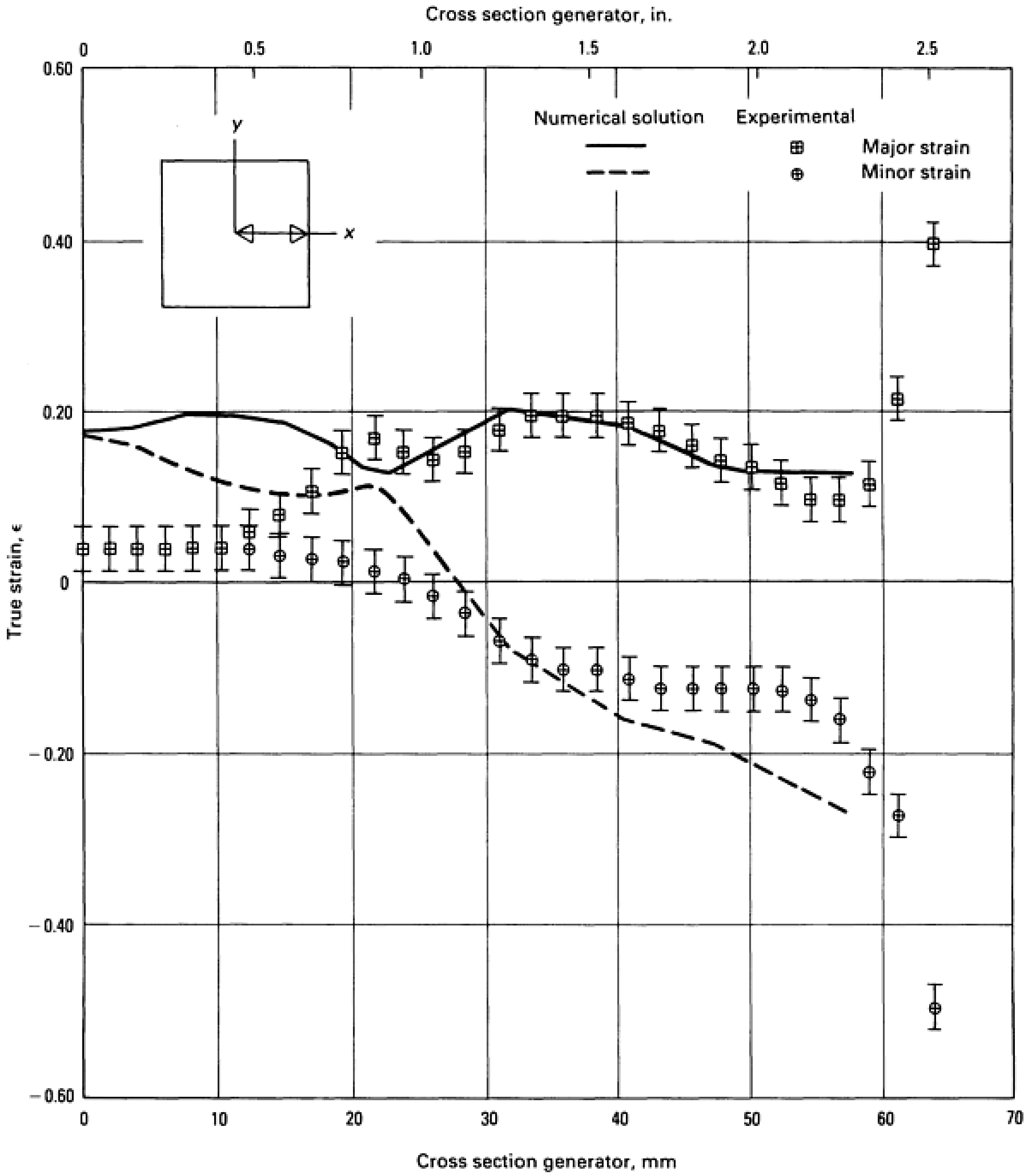


Fig. 21 Comparison of the numerical solutions with experimental results for major and minor strain distributions along the diagonal generator of a square box. Punch depth: 30 mm (1.18 in.).

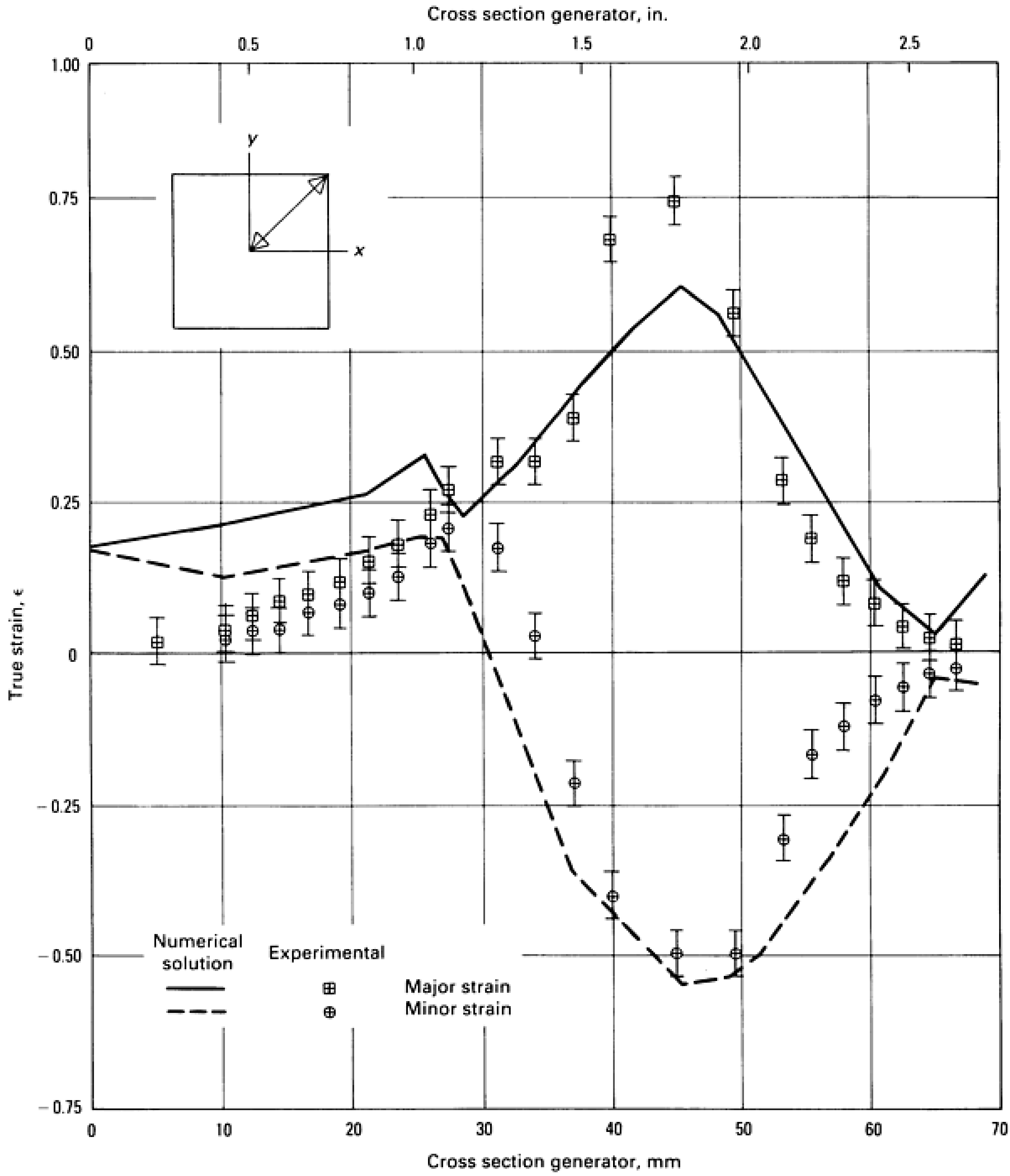


Fig. 22 Comparison of the numerical solutions with experimental results for major and minor strain distributions along the transverse generator of a square box. Punch depth: 30 mm (1.18 in.).

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Statistical Analysis of Forming Processes

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Introduction

STATISTICS are becoming important tools in the operation of press shops, providing numerical process analysis capabilities that far exceed the more traditional recording of simple breakage rates. The most common use of statistics in the press shop is the area of statistical process control (SPC). Though utilized in many formats, SPC is simply the use of statistical techniques such as control charts to analyze a process or its output and thus enable appropriate actions to be taken to achieve and maintain a state of statistical control. The use of statistical process control instead of traditional quality control methods such as inspection/sorting is beneficial in a number of ways. Statistical process control:

- Decreases scrap, rework, and inspection costs by controlling the process
- Decreases operating costs by optimizing the frequency of tool adjustments and tool changes
- Maximizes productivity by identifying and eliminating the causes of out-of-control conditions
- Allows the establishment of a predictable and consistent level of quality
- Eliminates or reduces the need for receiving inspection by the purchaser because it produces a more reliable, trouble-free product, resulting in increased customer satisfaction

The use of statistics in analysis of the forming process, however, goes well beyond SPC. The whole area of design of experiments (DOE) is becoming important as the interactions within the forming process are detailed and studied. This article will discuss the role of statistics in sheet metal forming operations both in terms of different statistical process control techniques and design of experiments. Information on the statistical analysis of mechanical testing is available in the Section "Statistics and Data Analysis" in *Mechanical Testing*, Volume 8 of *ASM Handbook*, formerly 9th Edition *Metals Handbook.*

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The Forming Process

A wide range of forming processes are currently in use. Although the details of these processes differ significantly, most forming processes share certain characteristics. Each forming process, however simple, can be viewed as a system. For sheet metal forming, one common breakdown of the system consists of the following components:

- Material
- Lubricant
- Tooling
- Press

Each of these major components can be further broken down into subcomponents.

The components of forming systems are highly interactive. A change in one component can produce significant changes in the effects of other components. Not only are the changes within a single component difficult to trace and understand, but the interaction of the components makes the task even more difficult. Small changes made in one or more components of the system can cause very large changes in the output of the system (the finished part). These synergistic changes may not be predictable or even possible to anticipate.

Ideally, the forming system should be a continuous process. For many processes, the system appears to be continuous. The tools are inserted into the designated press and remain there for the production life cycle of the part, which often spans several years. Changes made during the production life cycle may in fact create major disruptions in the continuous nature of the process. These changes can include engineering modifications, tooling replacement, routine maintenance, process improvements, and other seemingly minor modifications to the process.

Many forming processes are conducted on a batch basis, typically with large volumes. Tooling is inserted into a press, a specific number of pieces are made, and the tooling is removed from the press. This type of process is often considered to be an interrupted or segmented form of a continuous process. In reality, however, a new forming system is created each time the tooling is inserted into the press. This is evidenced by the extended period of trial-and-error adjustments necessary to the tooling before the production of satisfactory parts can begin.

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The Statistical Approach

Statistical process control programs are becoming commonplace in industry. An entire science has been developed to deal with the problem of defining, analyzing, correcting, and controlling production processes (Ref 1, 2, 3, 4, 5). By providing data on the capabilities and output of a process, statistical methods provide a rational, rather than an emotional, basis for problem solving and decision making. Other benefits of statistical process control have been listed in the introduction to this article.

As a system, the forming process is amenable to system analysis and the techniques of system control. Various statistical techniques, many of which have their origins in quality control practices, can be applied to the forming process. In this article, these statistical techniques are divided into two broad categories: historical tracking and statistical deformation control (process design).

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Historical Tracking

Historical tracking is a long-term process of measuring, recording, and analyzing one or more specific characteristics of a process. This record then becomes the basis upon which the current state of the process can be assessed and the future state of the process predicted. Numerous statistical techniques are applicable to historical tracking (Ref 1, 2, 3, 4, 5); one is the control chart and another is statistical deformation control.

Control Charting

The heart of many SPC systems is the control chart. The control chart is a method of monitoring process output through the measurement of a selected characteristic and the analysis of its performance over time. Because the output from one process is often the input to the next process, the physical location of the measurement can be at either end of the transfer link. For example, the output from a blanking press--the blanks--becomes the input to the next stage, which is the press itself.

A control chart can be a very powerful statistical tool. Information from the control chart can be used to inform the operator when to adjust the process and, perhaps more important, when not to adjust the process. This places the operator in control of the process, based on statistically valid numbers instead of trial and error.

For example, the operator is given a machine capable of producing the required parts and is then given the means to measure the characteristics of output (the finished parts) in real time. Thus, the operator knows the quality of the part coming off the machine on a real time basis. In addition, the operator has control limits for the process. These control limits tell the operator when to adjust the process and when not to adjust the process, which permits the operator to prevent rather than detect defects. Such a system for the control of steel sheet thickness is described in the following example.

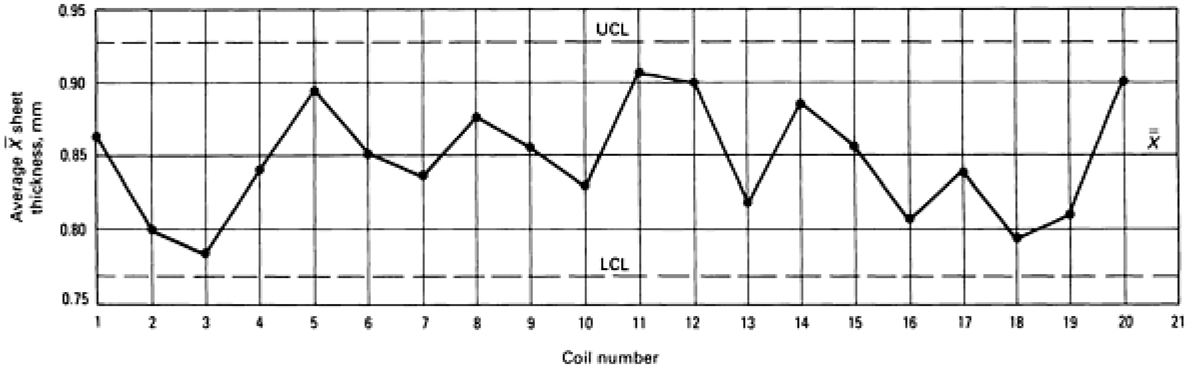
Example 1: Constructing a Control Chart for Steel Thickness Measurements.

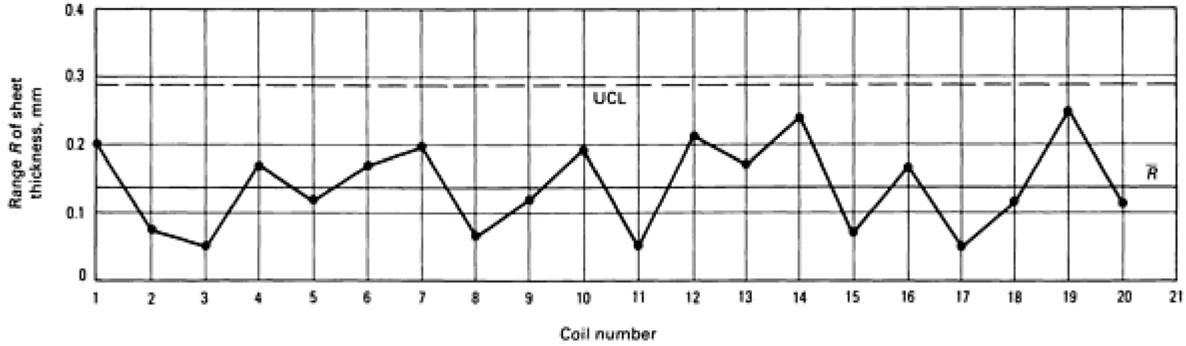
Uniformity of blank thickness is often stated to be important for high productivity in sheet metal forming operations, although published information on this subject is limited (Ref 6, 7). Once the tooling is set for a specific sheet thickness, adjustments to the tooling cannot be readily made. A control chart can be constructed to statistically monitor variations in the incoming steel.

For each coil of steel received, five blanks are sampled at specific locations along the length of the coil, such as the head, quarter, center, quarter, and tail locations. Examples of such measurements are given below.

The values of $\overline{X}$ (the average thickness) and R (the range of thicknesses) are plotted on the respective graphs. After sufficient measurements are made, the $\overline{\overline{X}}$-value and the upper and lower control limits (UCL and LCL) can be calculated and plotted on the same diagram.

The control chart shown in Fig. 1 indicates that the thickness of the steel being processed is stable. A process is said to be in a state of statistical control if it is operating without special causes of variation (Ref 4). It will exhibit random variation within calculated control limits and will have predictability; that is, measurements taken in the future are expected to be within the same control limits. However, no prediction can be made about where any individual measurement will lie within these control limits.





Coil number	**1**	**2**	**3**	**4**	**5**	**6**	**7**	**8**	**9**	**10**	**11**	**12**	**13**	**14**	**15**	**16**	**17**	**18**	**19**	**20**
Test No.																				
1	0.97	0.79	0.76	0.76	0.81	0.84	0.84	0.84	0.84	0.76	0.89	0.91	0.76	0.91	0.81	0.71	0.84	0.76	0.89	0.84
2	0.89	0.79	0.76	0.84	0.86	0.96	0.86	0.84	0.91	0.89	0.94	0.97	0.81	0.89	0.89	0.79	0.81	0.79	0.94	0.89

3	0.86	0.86	0.81	0.84	0.94	0.79	0.97	0.91	0.89	0.94	0.89	0.99	0.94	1.02	0.84	0.89	0.86	0.89	0.71	0.89
4	0.84	0.79	0.76	0.81	0.94	0.84	0.76	0.89	0.86	0.81	0.89	0.86	0.76	0.86	0.86	0.79	0.84	0.76	0.69	0.96
5	0.76	0.79	0.81	0.94	0.89	0.84	0.76	0.91	0.79	0.74	0.94	0.76	0.79	0.76	0.89	0.84	0.84	0.76	0.81	0.94
Total	4.30	4.02	3.90	4.19	4.44	4.27	4.19	4.39	4.29	4.14	4.55	4.49	4.06	4.44	4.29	4.02	4.19	3.96	4.04	4.52
Average, $\overline{X}$	0.86	0.80	0.78	0.84	0.89	0.85	0.84	0.88	0.86	0.83	0.91	0.90	0.81	0.89	0.86	0.80	0.84	0.79	0.81	0.90
Range, R	0.21	0.07	0.05	0.18	0.13	0.17	0.21	0.07	0.12	0.20	0.05	0.23	0.18	0.26	0.05	0.18	0.05	0.13	0.25	0.12

Fig. 1 Raw data (in millimeters; 1 in. = 25.4 mm) obtained from steel thickness measurements and the resulting $\overline{X}$ -*R* control charts. Upper and lower control limits can be calculated and the mean determined when sufficient data have been collected.

Unfortunately, the control limits shown in Fig. 1 are rather wide and would require frequent tooling adjustments. This means that a normal distribution of thicknesses within the control chart would be expected. However, this expected range is too large for the tooling and would necessitate tooling adjustments to accommodate changes in thickness between the extremes of the range. Therefore, it is necessary to work with the steel supplier to reduce the range of the control limits to an engineering specification window acceptable to the tooling.

Control limits are important in statistical process control because they define the amount of variation in a process due solely to chance causes. If the process is operating within these limits (Fig. 2a), it is considered to be stable. If the process is operating outside these limits (Fig. 2b), it is unstable.

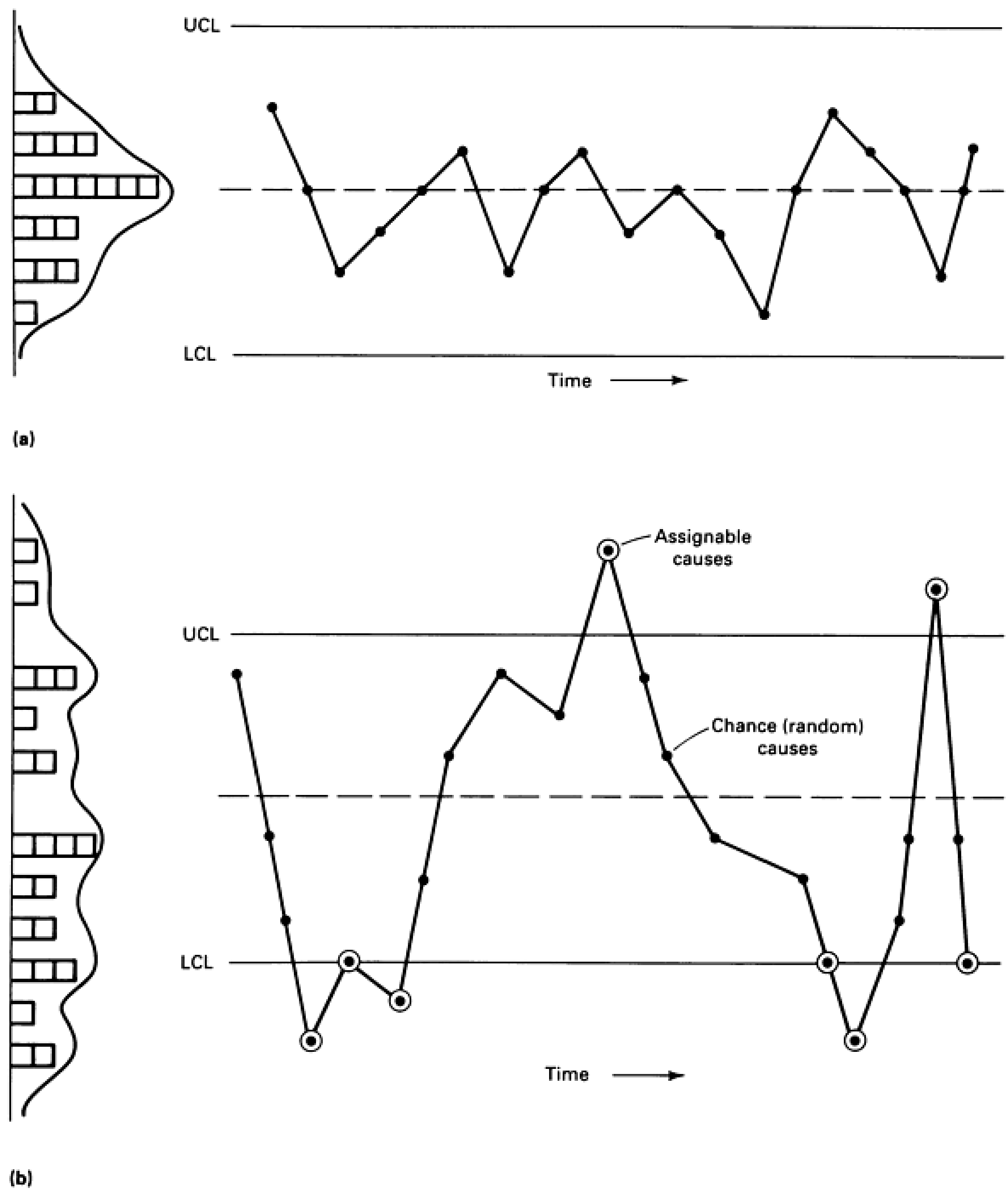


Fig. 2 Distribution curves (histograms; left) and control charts illustrating processes that are in control (within control limits) (a) and out of control (outside of control limits) (b). Variation outside of assigned control limits is always the result of some assignable cause, as shown in (b).

The control chart represents the best that the operator of a given process can do with the process as it exists. If this is unacceptable, then the basic process must be changed. Engineering and management, not the operator, are required to implement these process changes because this may require new equipment, new tools, new processing sequences, additional stages, or even a new part design.

If the process is operating within its control limits (statistical control), then the capability of the system to meet specifications can be assessed. If the system is out of control, then changes to the process must be made to make the process stable before the capabilities can be assessed.

Control charts are most effective when monitored over an extended period of time. Thus, changes in the process can be detected, often before rejects begin to occur. Two general types of changes in the process might be encountered. One is the central sample tendency $\overline{X}$, which is monitored to detect a shift in the location of the distribution (Fig. 3). A location change would imply that the normal distribution remains the same but that the average is displaced. An example in forming might be the angle of a bend. The variation from bend to bend would remain acceptable, but the average angle of all bends would have changed. Some factors that may affect the $\overline{X}$ -value are gradual deterioration of the equipment, worker fatigue, accumulation of waste products (such as excess lubricant or flaked metallic coatings), and environment.

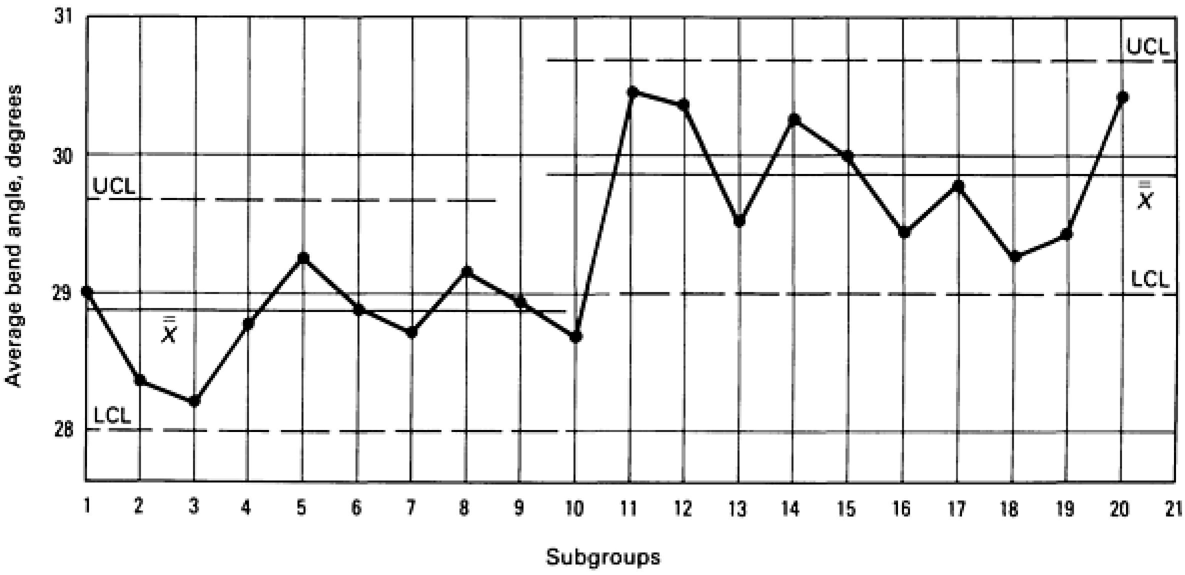


Fig. 3 Shifting of a control chart (upper and lower control limits and mean) due to a change in processing. Values of *R* remain the some. See also Fig. 4.

Another type of process change is a change in the sample range or the variation from one sample to another (Fig. 4). This range change is observed as a change in the width of the normal curve for a given area under the curve. This means increased variability. For the bending example shown in Fig. 3, the average bend angle would remain the same, but large differences would be detected among samples from the same lot or even consecutive samples. Some factors that may affect the *R* chart are changes in operator skill, worker fatigue, change in the mix of components feeding an assembly line, and gradual change in quality of the incoming material.

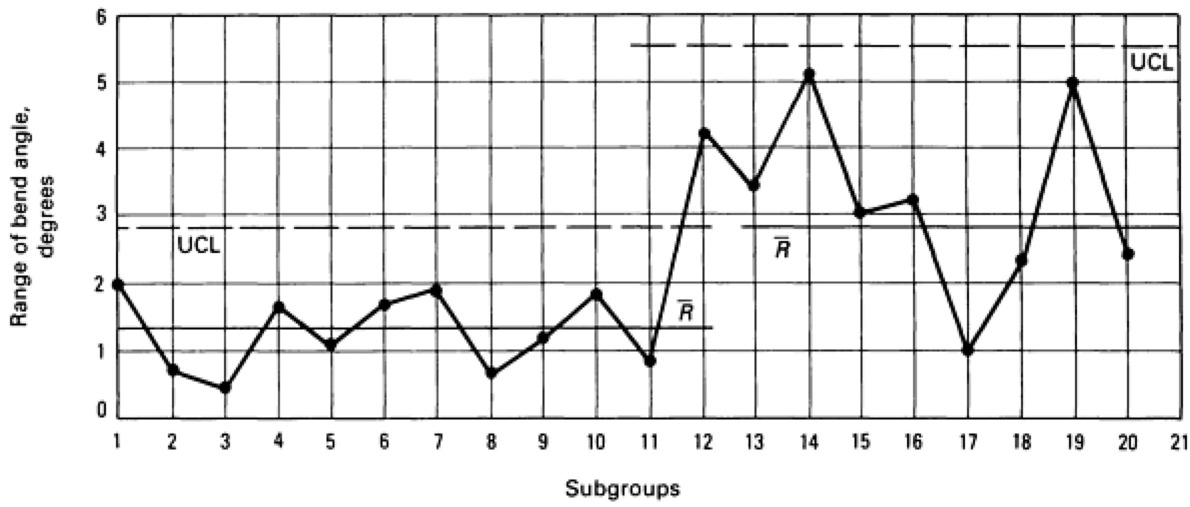
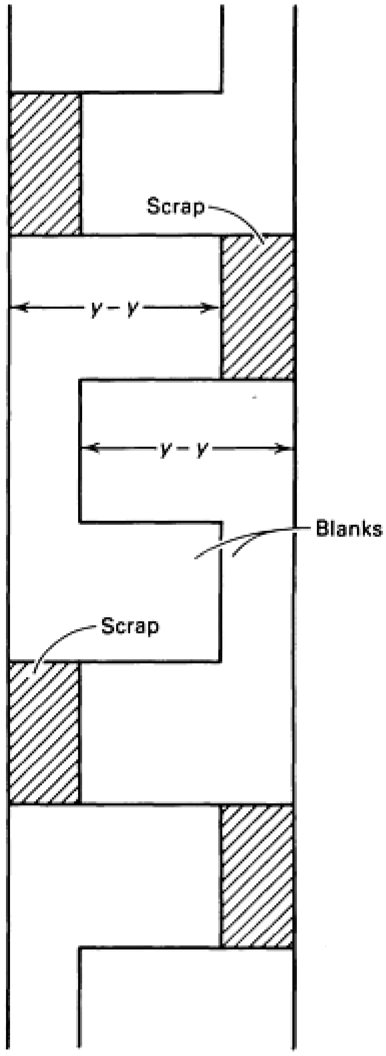


Fig. 4 Increase in the range of values within a subset (increase in *R*). Values of $\overline{X}$ remain the same. See Fig. 3.

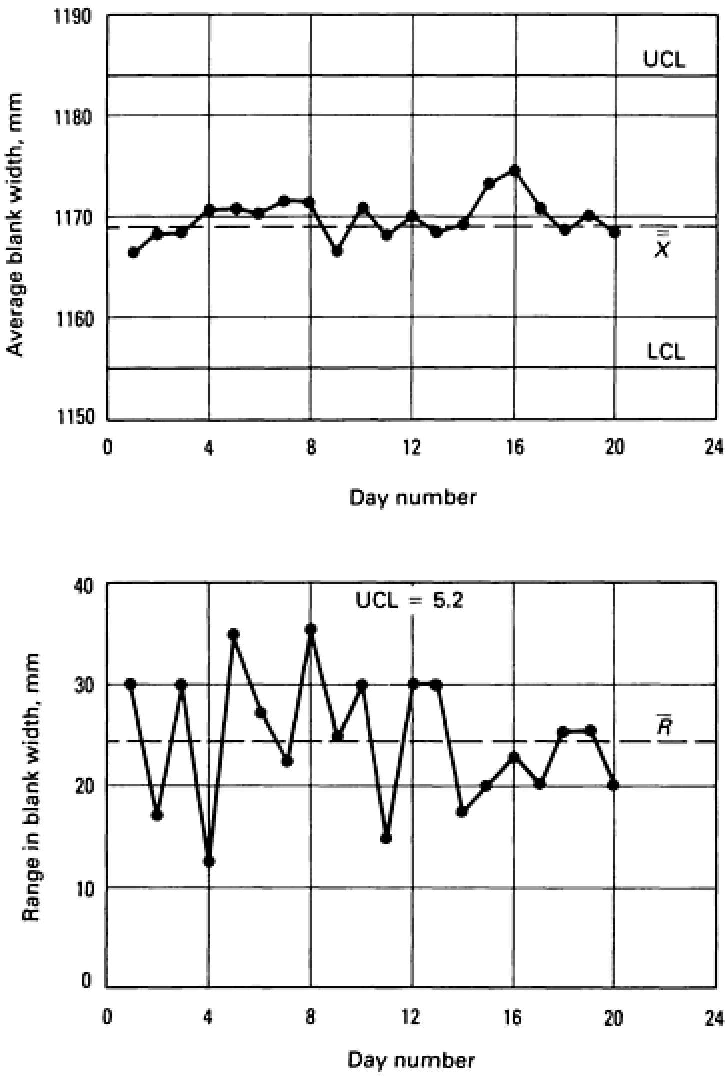
The two key concepts that emerge from control charts are control and capability. The term control defines the stability of the process, which in turn means that the process is predictable. It does not necessarily mean that the process is acceptable with regard to the specification. This is in contrast to the term capable; this term means that the process has the capability to meet specifications.

Example 2: Solving a Problem of Fender Blank Size Variation.

Two fender blanks were generated with each stroke of a blanking punch (Fig. 5a). Measurements of the critical dimension *y*-*y* on each blank showed large variations in *R*-values, but $\overline{X}$ -values were quite constant (Fig. 5b). Further analysis revealed that plots of only the right-side blanks and only the left-side blanks had different characteristics (Fig. 6). These data showed a reduced variation in *R* but a larger variation in the $\overline{X}$ -values. Interestingly, the $\overline{X}$ -values of the right- and left-side blanks changed simultaneously but in opposite directions.



(a)



(b)

Fig. 5 (a) Schematic showing nesting of blanks for an automobile fender. Dimension *y*-*y* was monitored for the SPC study reported in Example 2. (b) $\overline{X}$ -*R* control charts for both right side and left side fender blanks taken as a single population. See also Fig. 6.

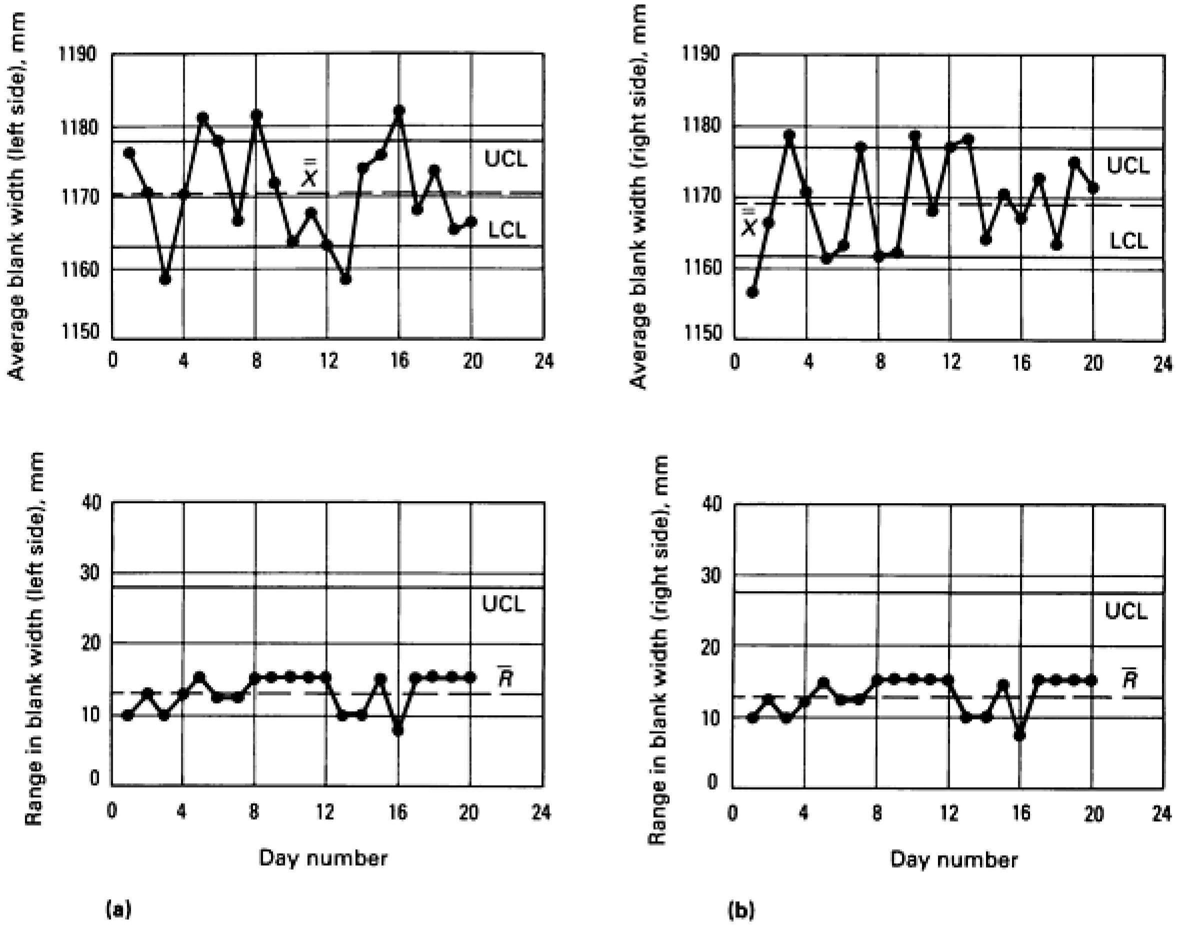


Fig. 6 $\overline{X}$ **-*R* control charts for left-side fender blanks (a) and right-side fender blanks (b) taken as separate populations.**

Further observation of the blanking process revealed frequent tandem adjustment of both the right- and left-side guides in response to changes in the width of the coil of steel. Even if the coil of steel did not change in width, movement of the right and left guides would cause one blank to increase in width while the other blank would decrease in width. This would cause the range to increase equal to twice the amount of the guide shift.

The first process change made was to weld the left-side guide into correct position. This meant that the left-side blank had the correct width regardless of the width of the coil. As a result, all blank width variations were associated with the right-side blank, and these variations could be related to the variations in the width of the coils of steel. These control charts could be given to the steel supplier as a performance review of the as-supplied coil width variations.

Problem Solving Using Control Charts. Control charts are only signals to operators and management of the operating conditions of the process. Personnel must respond to these signals and identify the sources of the variations. Control charts are useful in many problem-solving situations (Ref 4); they can:

- Assist in distinguishing between special and common causes of variation
- Help determine whether unacceptable variations can be improved by the personnel immediately involved in the process (such as tightening a loose tool holder) or whether they are due to the system and can be corrected only by management
- Identify trends in the process average
- Highlight increased process variability

The opportunities are limitless for using control charts as a key SPC tool. Data collection is the easiest aspect of the process. Finding the cause of a problem will often require some difficult detective work, and elimination of the problem may be a major task. For example, recording the scheduled times for a shuttle bus is easy. Determining the real cause of deviation from the schedule may be difficult; periodic high traffic density may be due to maintenance being performed on an adjacent street. To make the shuttle bus immune to delays and to improve trip time consistency would require a major study and perhaps drastic modifications to the traffic control system.

Statistical Deformation Control

Each of the four components of a forming system--material, lubricant, tooling, and press--can be tracked using the SPC techniques described above. Typical measurements could include:

- Material thickness, coil/blank dimensions, and properties
- Lubricant composition, viscosity, and application thickness
- Tooling pressures, surface treatment, and dimensional accuracy
- Press speed, stroke, and ram pressures

Many of these measurements are charted in an attempt to reduce process variability and to improve product quality.

However, the components of forming systems are complex, interactive, and synergistic; reliable models are not available for predicting the output of the forming system based solely on the system inputs. Therefore, monitoring of the final output of the system is required. Dimensional checking of the final product can be easily accomplished, but monitoring forming severity is more difficult.

Numbers representing the percentage of scrap or the percentage of breakage are traditionally recorded to represent the status of the forming system. These numbers are inadequate measures of forming severity. For example, many stamping processes result in high levels of strain but not breakage. Therefore, the current behavior--no breakage--gives no indication that breakage may be imminent; some measure of performance must be sought that permits a broader range of conditions. Once breakage begins, the stamping process is out of control. General global straining ceases as the tear develops and opens; other forming modes become active. In addition, the percentage of breakage averages the forming severity over a large number of stampings instead of determining the forming severity at a preselected location in each individual stamping. For these cases, percentage of breakage does not accurately define the various levels of severity.

One means of evaluating forming severity used in many sheet metal press shops is circle grid analysis and forming limit diagrams; these are described in Ref 8, 9, 10, and 11. The actual amount of deformation that the sheet has experienced is determined from the deformed circles (Fig. 7). The forming limit diagram shows the maximum amount of deformation a stamping can undergo before failure. Forming severity can be defined as the maximum allowable deformation minus the actual deformation. This forms the basis for monitoring and controlling stamping performance.

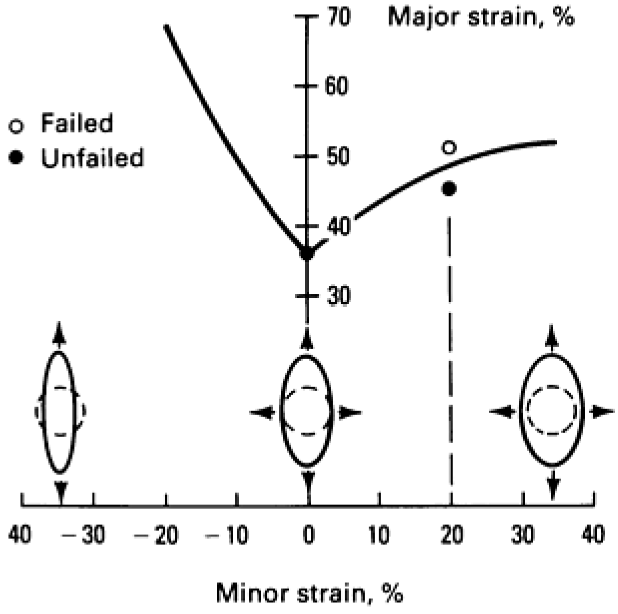


Fig. 7 Forming limit diagram showing strain states where forming is safe (below curve) and where stampings will begin to fail from localized necking during forming (above curve). The strain values are determined from deformed circle grids placed on the blank before forming.

The process of pregridding the blank before deformation and measuring the deformed circles presents logistical problems if it is used for routine analyses of a large number of stampings over an extended period of time. These problems include removing blanks from the material lifts for pregridding, the time required for gridding, careful reapplication of lubrication to duplicate production levels, reinsertion of blanks into the production cycle without stopping production, proper lighting for grid reading, the time required for accurate reading, and disposal of gridded blanks after analysis. Therefore, only one or two stampings are commonly gridded to provide a single analysis (with respect to time) of the severity of the stamping. No information can be deduced about the change of the forming system with time, nor can the single stamping be characterized with respect to the dynamic changes of the system.

Dynamic changes, however, can be analyzed with statistical process control. A critical variable is measured at specified intervals and plotted as a function of time to generate a control chart. From this control chart, using SPC analysis techniques, the current status of the system can be identified relative to its historical performance. The dynamic variability of the system can be defined, and determinations can be made as to whether the system is in control, out of control, or changing.

Statistical deformation control (SDC) combines the best features of circle grid analysis, forming limit diagrams, and statistical process control (Ref 12). The deformation severity of the stamping under investigation becomes the critical variable that is tracked by statistical process control. To simplify the press shop procedures further, the amount of deformation experienced by the stamping is defined by the ratio of final thickness t_f to initial thickness t_0. The thickness ratio is determined from ultrasonic measurements of sheet metal thickness in the most critical zone of the stamping. The ultrasonic measurements are rapid, the blanks do not have to be pregridded, and the stampings are not damaged by a grid and therefore need not be scrapped after the measurement is made.

Like most analytical tools, statistical deformation control does not involve a single, invariant procedure. Several levels of complexity are possible, and each level contributes an increased degree of understanding of the forming system. These levels are:

- *Level 1:* generating a standard control chart
- *Level 2:* assigning a deformation severity value
- *Level 3:* separating material variability from process variability
- *Level 4:* determining sensitivity of the forming system to individual inputs

Again, like most analytical tools, statistical deformation control is flexible and should be applied only at the level necessary to solve the problem. Therefore, routine applications of statistical deformation control to a system that is in control will be at the level of the basic control chart.

All levels of statistical deformation control require a preliminary stamping analysis. Measurement of deformation at the critical location is a key element of statistical deformation control. Therefore, an early circle grid analysis of the stamping in question is required to identify the location, mode, and severity of strain in the most critical zone within the stamping. Repetitive ultrasonic measurement of many stampings over an extended period of time requires a template to be made for the ultrasonic probe; the simplest template is a section of an identical stamping with a hole drilled for the head of the probe.

Template holes are drilled for the most critical location and for an area of the stamping that is undeformed. The latter measurement is required to determine the initial thickness of the stamping in order to generate the final-thickness-to-initial-thickness ratio without the inconvenience of premeasuring the blank thickness. The ability to measure critical and initial metal thicknesses on the formed stamping means that random stampings can be selected for evaluation after production.

SDC Level 1: Control Charting. Standard control-charting procedures are used. A typical application would require the removal of five stampings in sequence at the end of the press line. Measurements of the thickness in the critical zone and an undeformed zone would be made, and the thickness ratio calculated. These five ratio values would then be converted into the traditional $\overline{X}$ -R values and plotted on the control chart. Control chart analysis procedures are used to calculate upper and lower control limits and other SPC data.

The control chart is monitored to determine the viability of the forming system and to initiate corrective actions when such actions are mandated (Ref 4). The importance of these analyses is the predictive capability, which can provide early warning of impending forming process failures. Therefore, statistical deformation control can be used to show an increase in the forming severity for a press line for which breakage is not yet a problem, as in the following example.

Example 3: Tracking a Sheet Metal Forming System by Monitoring Metal Thinning.

An appliance stamping for a range top was selected for study, using sheet metal thinning as a measure of stamping severity. Measurements were conveniently made with an ultrasonic thickness gage, which permitted measured stampings to be returned to the production line without loss of the stampings. Because breakage was not a problem with this stamping, measurements were made only twice a week. The results of the measurements are shown in Fig. 8.

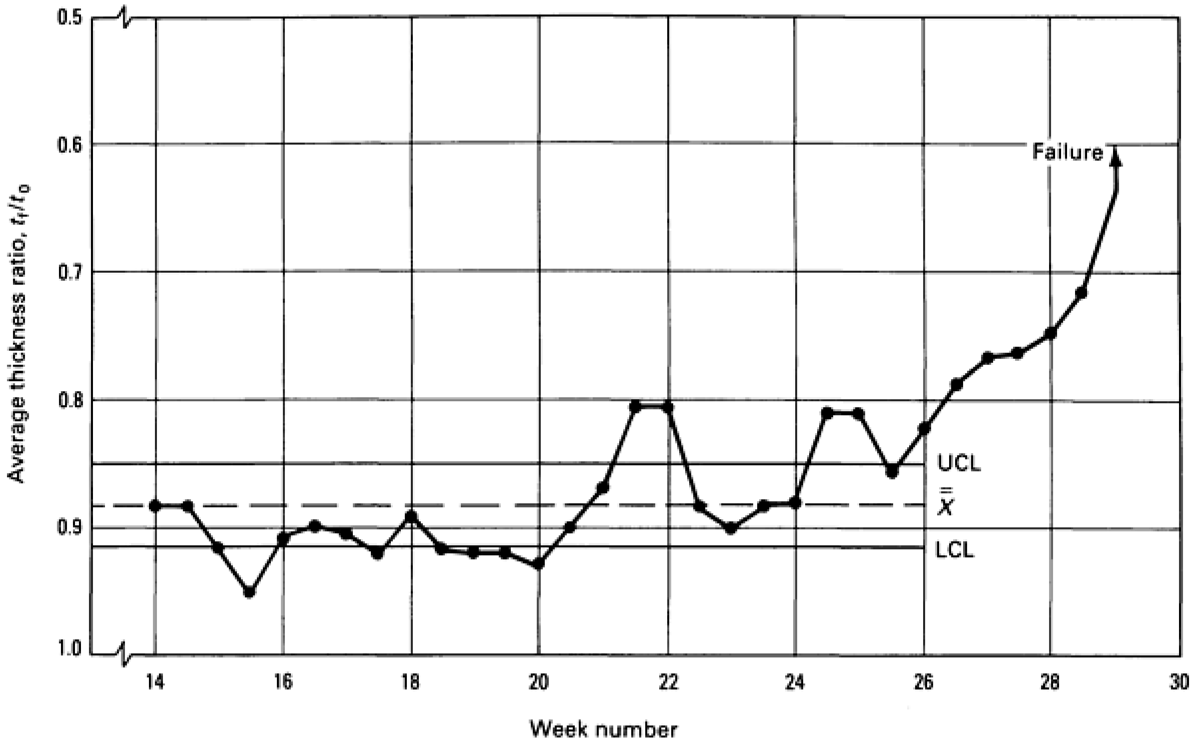


Fig. 8 Control chart of average thickness ratio t_f/t_0 for a stamping. Note the increase in forming severity (reduced thickness ratio) beginning at week 21 and the continuous decrease in thickness ratio beginning at week 25.

The stamping was shown to be out of control. However, because the amount of metal thinning did not cause breakage, the stamping was not on the list of problem stampings. After 6 months of good performance, the amount of metal thinning began to increase. Eventually, excessive production breakage of the stampings began to occur. Because this was a test of the SDC procedure, no attempts were made to determine the cause of the increased thinning or to correct the cause. This would have disrupted the test and would have changed the data base. However, the test did show that the stampings that were not breaking could be statistically tracked over extended periods of time in order to detect changes in severity that would lead to production problems if left uncorrected. The absence of breakage in a forming process does not necessarily indicate that the process is in control.

SDC Level 2: Severity Assignment. A unique feature of statistical deformation control for formability analysis is that each control chart can be subdivided and calibrated according to forming severity for each type of stamping made in the press shop. This severity assignment is independent of whether the process is in or out of control. The severity ranges are assigned based on the thickness forming limit diagram (Ref 12). The following example shows how a process can be in control and producing good stampings and yet have an insufficient safety factor for good long-term reliability.

Example 4: Assigning a Severity Rating for Stampings With Only Sporadic Breakage.

Sporadic breakage was occurring in an automotive stamping. The question was asked whether this breakage was the result of random events not typical of the normal system or whether the system was in fact critical and the breakage was a normal by-product of statistical swings in severity. To determine this, metal-thinning readings were made for several weeks (Fig. 9). The control chart showed that the system was in control.

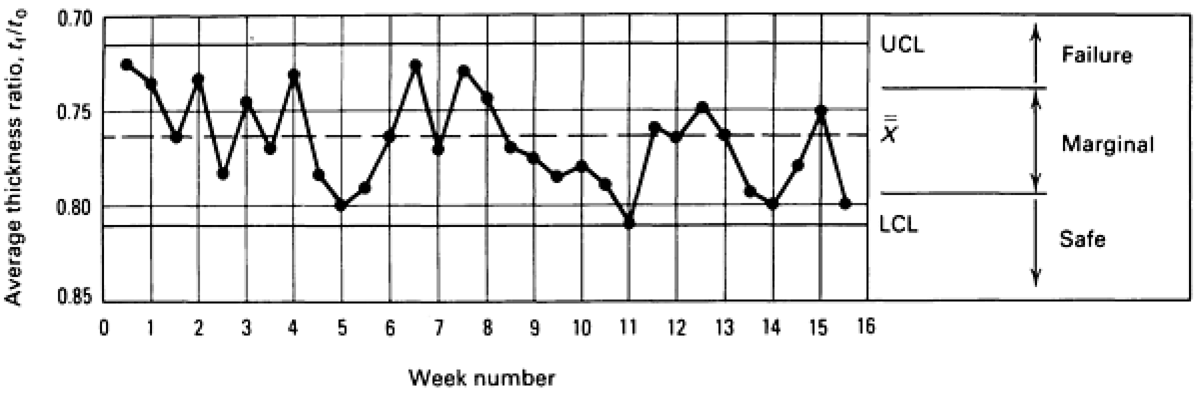


Fig. 9 Severity zones added to average thickness ratio control chart in a Level 2 SDC analysis.

Additional information was gained, however, when deformation severity zones were added to the control chart (Ref 12). The severity zones showed that $\bar{X}$ was in the marginal zone and that some proportion of the control chart fell in the breakage zone. This meant that the sporadic breakage was normal for the system as established and could be expected to continue as long as the process was maintained as currently designed. However, the process was in control and therefore amenable to a process change that would lower the upper control limit from the failure zone into the marginal zone.

Specifications, Control Limits, and Forming Severity. An important point of understanding is the differences among engineering specifications, control limits, and forming severity. For example, a stamping is designed to have a maximum metal thinning of 42% in order to meet in-service performance requirements. The control limits indicate whether the process is in statistical control. The state of being in statistical control means that all special causes of variation have been eliminated and that only common (random) causes remain. Special causes are intermittent sources of variation that are unpredictable or unstable; sometimes called unassignable causes, these variations are signaled by points beyond the control limits. An example of a special cause would be inserting the wrong material into the forming process. On the other hand, common causes of variation are always present and indicate the random variation inherent in the process itself. An example of this would be excess gap in the punch guidance system. Forming severity is the proximity of a given stamping to breakage. This forming limit is independent of future in-service performance requirements or part-by-part variation over time.

Example 5: Comparing Engineering Specifications and Forming Severities to Control Charts.

Engineering specifications, control limits, and forming severity for a control arm are illustrated in Fig. 10. The allowable maximum metal thinning specified by the part print (engineering specification) is rather high. In fact, the engineering specification is in the failure zone of Fig. 10. This means that the stamping would fail before attaining the maximum thinning allowed by the engineering specification. Therefore, the practical thinning limit of the process is established by forming severity rather than engineering specifications.

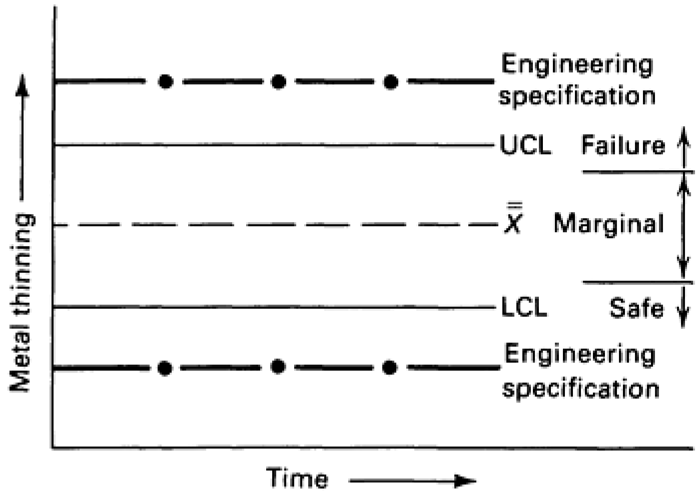


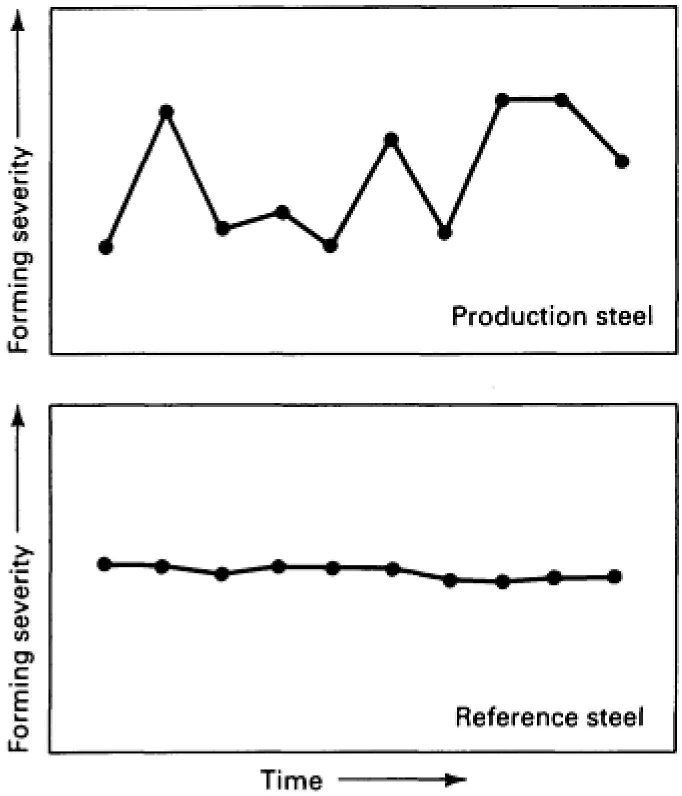
Fig. 10 Schematic control chart showing the differences among engineering specifications, control limits, and severity zones.

The control chart and its attendant control limits reflect the current operating status of the forming process. In Fig. 10, the operating status of the control arm is shown to be in control but with a certain portion of the control chart in the failure zone. This means that some quantity of pieces will break in the forming process. Changes in the forming process, such as modifying the lubricant or reducing the die radii, could narrow the control limits shown in Fig. 10, but would not change the position of the engineering specification or the severity limits. The initial assessment of forming severity and the tracking of the severity through the production life cycle of the stamping--and especially through tooling modifications--are important aspects of statistical deformation control.

SDC Level 3: Material Versus Process Variability. Statistical process control or statistical deformation control can easily identify a forming system that is out of control. Determining the cause of the variability is a more difficult problem. An important first step for most press shops is to separate variability in incoming material from process variability (for example, lubricant, tooling, press, and other in-house variables).

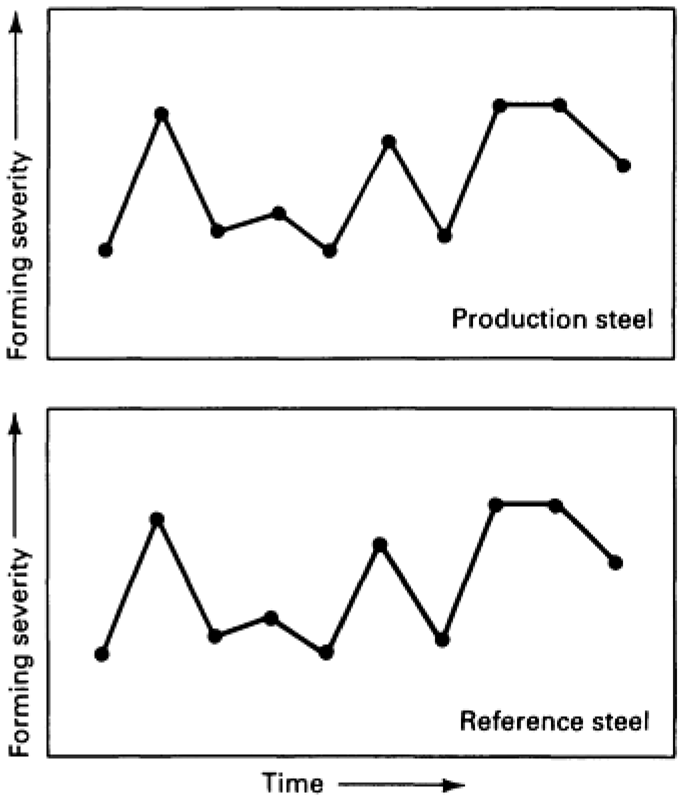
Statistical deformation control provides a method of separating this variability. First, a list of reference material is set aside. This reference material would typically be production material that has average, but consistent, properties. Ideally, this would be verified by evaluating the mechanical properties and surface characteristics of the top and bottom sheets.

Each time a control chart measurement is made with production material, an equal number of identified reference blanks are also formed, and the severity is measured. Two charts of severity versus time are maintained; one for the production material and one for the reference material (Fig. 11). Comparison of Fig. 11(a) and 11(b) indicates whether the system variability is due to the production material or the process.



Forming severity
Production steel
Forming severity
Reference steel
Time

(a)



Forming severity
Production steel
Forming severity
Reference steel
Time

(b)

Fig. 11 Schematic control charts showing the effects of materials variables (a) and process variables (b). The identical variations in forming severity for both production and reference steel in (b) indicate that the fluctuation in severity is caused by process variables.

In Fig. 11(a), the forming severity of the production steel is varying, while the forming severity of the reference steel is relatively constant. This indicates that the process parameters are producing a stamping of constant severity and that system variability is due to incoming material variability. The reverse is true in Fig. 11(b); the variability of the production material is identical to that of the reference material. This indicates that all variation in forming severity is due to process variables.

For most forming processes, the forming severities of both the production material and the reference material will vary, but will not be identical to each other. This indicates the common situation in which both the material and the process affect the output of the forming system. The variability must then be apportioned between material and process.

SDC Level 4: Input Sensitivity. Once the forming system variability has been identified as being either material or process related, a more detailed analysis can be conducted to minimize this variability. A key control factor is identified and changed to determine the improvement to the system variability (or conversely, system stability). Selection of the factor can be based on experience or experimental techniques, such as the Taguchi method (see the section "Taguchi Experiments" in this article).

There are at least two analysis possibilities. One is to process the new material according to the modified specification--for example, a new metal thickness. This new material parameter is then processed with the reference material and the severity data compared. The key is to run the reference material with the new material in order to isolate material variability from process variability.

A second technique would be to substitute the modified material (alternate parameter) for the reference material. Severity data are then collected for the regular production material and the modified material. The two severity curves can be compared to separate the process variability (Fig. 12) and to permit judgments about the relative performance of the two conditions. If sufficient data points (usually > 25) are collected, complete control chart information can be calculated.

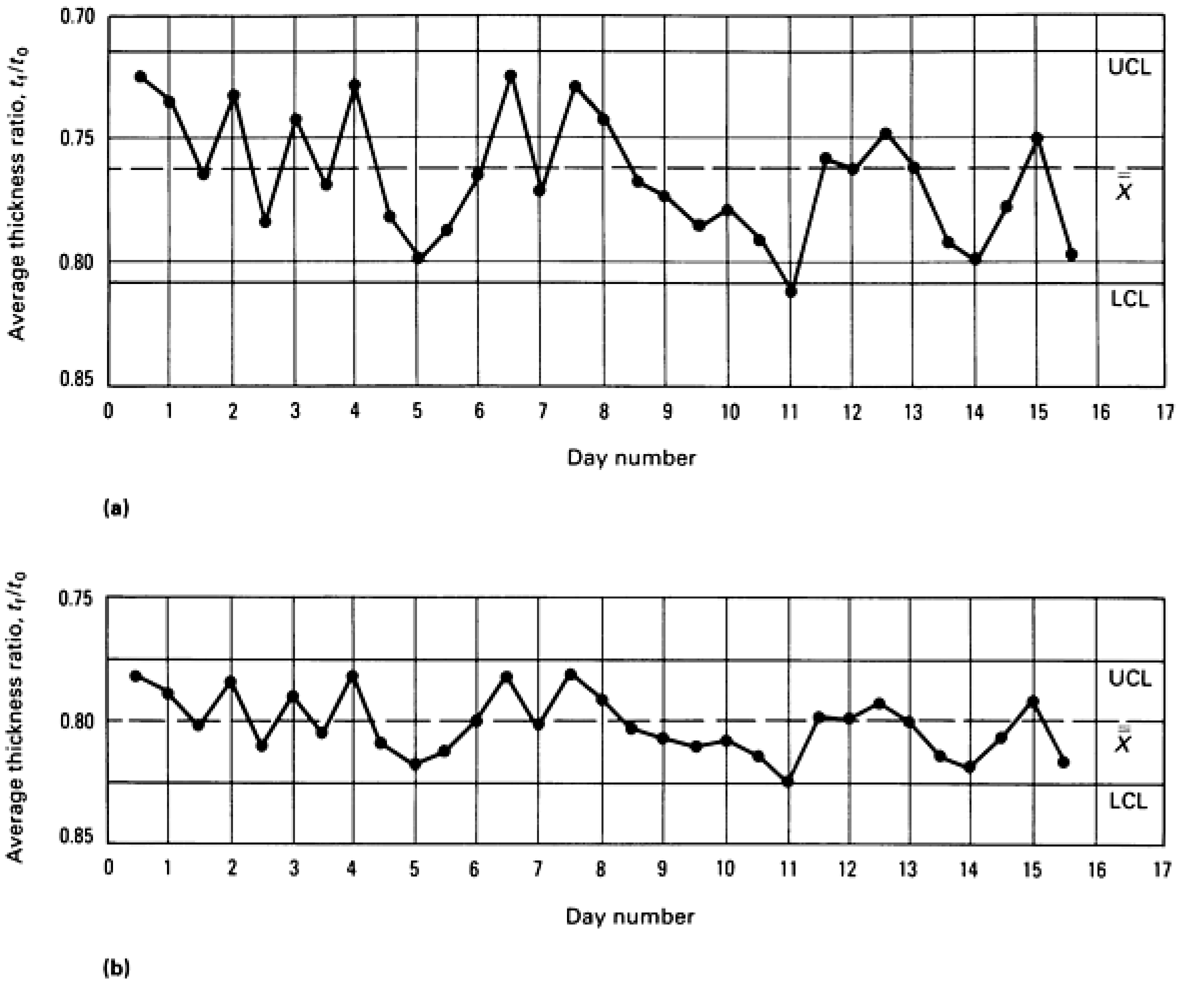


Fig. 12 Control charts showing original spread of process means (a) and second chart showing reduced spread of process means (b) after a change in the process.

The importance of statistical process control and statistical deformation control can be illustrated by describing two alternative methods of process control and analysis, both of which contain major flaws. One common method of process analysis is to produce a large quantity of output (in this case, sheet metal stampings) with condition A (the first level of the parameter under investigation, such as material properties, type of lubricant, press setting, and so on). The next step is to produce a large quantity of output with condition B (the second level of the parameter). The percentage of part breakage is then compared. A major problem exists if breakage is zero for both levels of the parameter under study. Even if the breakage is different for the two conditions, one cannot determine if the difference is due to the change from condition A to condition B, a change in the process itself, or normal statistical variation.

A second method of process analysis is to produce one stamping with condition A and one with condition B. The stampings are compared in some manner, such as degree of tearing or visual quality (Fig. 13). The argument is made that if two sequential stampings are produced, then the process is unchanged. However, statistical information is needed to compare the relative change of the two stampings with the normal expected statistical change between any two consecutive stampings without any intentional system changes.

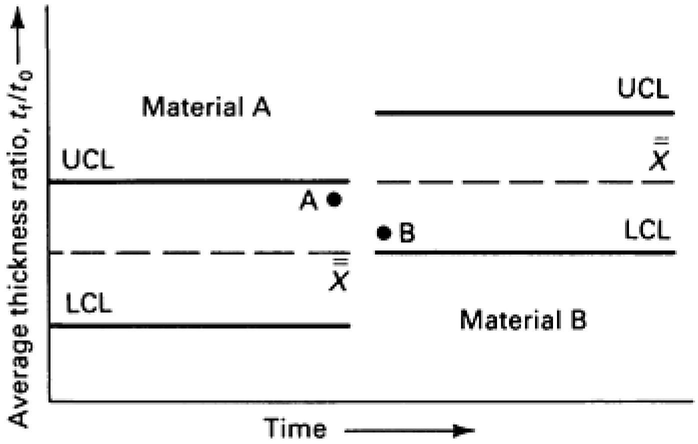


Fig. 13 Measurements from two points in time (values A and B) would suggest that material B has the lower forming severity. However, when full control charts are evaluated, material A has the lower severity.

If the forming system is unstable, then the system must first be stabilized before any changes are made and documented. The following example illustrates how statistical deformation control can be used to identify the sources of processing difficulties.

Example 6: SDC Analysis of an Outer Side Panel of a Truck Body.

The production of a truck outer side panel was marked with sporadic breakage problems. First, high breakage rates were recorded in area A of Fig. 14 for limited but specific lots of steel brought to the press. Mechanical-property evaluation of these high-breakage lots showed certain formability parameters to be equivalent to those measured in other lots of the same steel type that did not break. Second, certain extended runs of the part experienced more incidents of breakage than other runs.

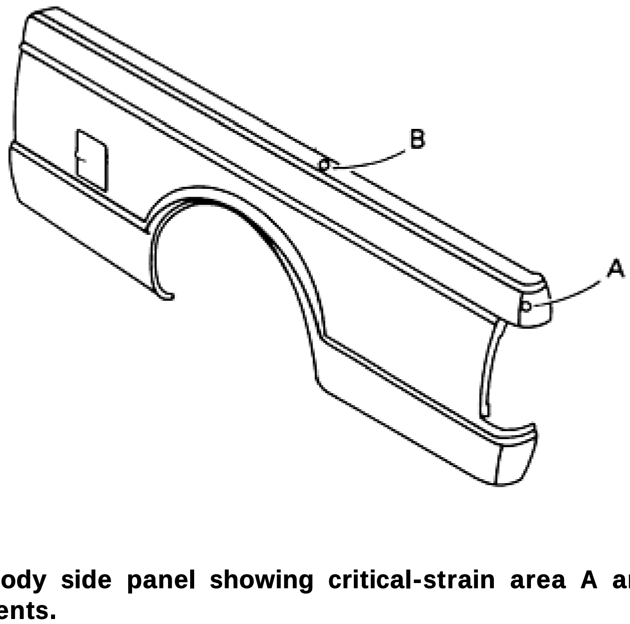


Fig. 14 Illustration of truck body side panel showing critical-strain area A and zero-strain area B used for ultrasonic thickness measurements.

Process Analysis. This problem was selected as a trial for statistical deformation control. The following six steps were utilized.

First, a circle grid analysis of the stamping was performed using a sheet of the current production material that was satisfactorily making the stamping (Ref 8, 9, 12). The most severe strain was located in area A of Fig. 14. Plotting this strain state on the forming limit diagram showed a safety margin of 8 strain%. This means that the stamping had marginal severity but would not be expected to fracture if the current conditions of the forming system were maintained.

Second, the circle grid analysis showed no strain in area B of Fig. 14. This location was designated as the as-received sheet thickness for purposes of the ultrasonic thickness measurements. This location was ideally suited to thickness measurement because it was at the same relative edge-center-edge location in the coil as the most severe strain location.

Third, a template was prepared for the stamping by cutting a section of another stamping to encompass the two locations. A hole was drilled at each location to accommodate the head of the ultrasonic probe.

Fourth, one lot of the production steel that was successfully making the stamping was set alongside the press as a reference lot. Blank edges were marked with yellow paint to identify at the end of the line all stampings made from this reference lot.

Fifth, at the halfway point of each lot of production steel brought to the press, five of the reference blanks were formed. Stampings from these blanks, plus the next five production stampings, were removed from the end of the production line for evaluation. Thickness ratio t_f/t_0 was determined for each stamping. The $\overline{X}$ and R values were calculated for both the production steel and the reference steel.

Sixth, concurrent plots of the $\overline{X}$ and R values for the production and the reference steel lots were maintained. Sections of the $\overline{X}$ plot are shown in Fig. 15. The mean, the upper control limit, and the lower control limit are not shown, because the solution to the problem was achieved before sufficient data points were collected for these statistical calculations.

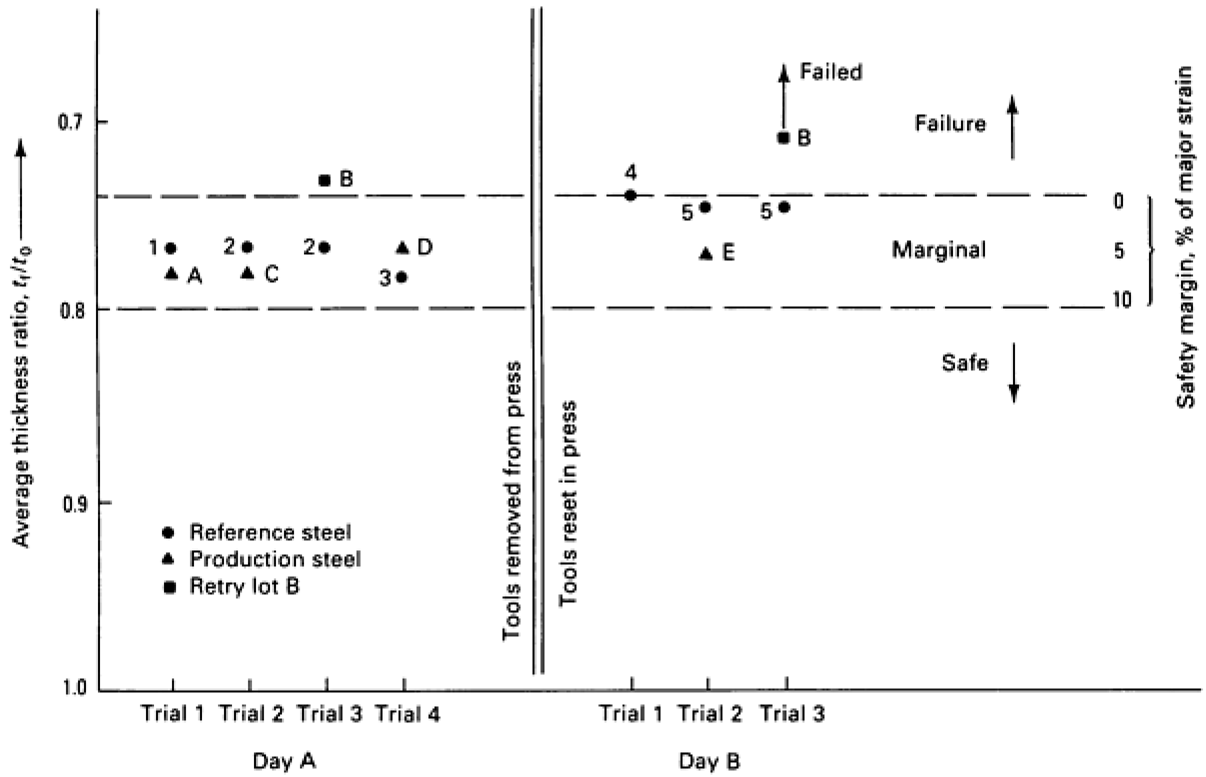


Fig. 15 Data for two days of measurements made on the truck body side panel shown in Fig. 14. See text for details.

Solution. The left-hand side of Fig. 15 verifies the initial circle grid evaluation that the stamping has some safety margin but that the safety margin is insufficient; the value of the thickness ratio is located in the bottom half of the marginal zone. In this case, it is possible to evaluate the severity of each reading as it is made; the quantity of data points needed to create a control chart is not required in order to determine the forming severity of each stamping.

Both the production steel and the reference steel appear to be maintaining not only a constant but also an equal forming severity. Neither the production steel nor the forming process provides indications of impending breakage.

One lot of steel that was previously rejected for high breakage rates (retry lot B, Fig. 15) was retried; a marked increase in forming severity to the failure zone was noted. The formability of sheet steel is an interaction of substrate properties and the lubricity of the steel/lubricant combination. However, this lot of steel had forming properties equal to or better than the production and reference steels noted in Fig. 15. Therefore, the lubricity of the steel surface was suspected; the steel was coated on the punch side of the sheet with a corrosion-resistant paint. To test the influence of the painted surface, several blanks were turned over so that the bare side of the sheet contacted the punch. These sheets were successfully formed into stampings without breakage. Although unacceptable for production in this reversed condition, these stampings verified that the substrate had sufficient formability to produce the stamping without breakage when the interface friction was correct.

It was recommended that the painted (punch) side of the blanks be sprayed with lubricant if breakage was encountered with a specific lot of steel. If breakage persisted with the additional lubricant, the lot was to be rejected for laboratory analysis of the substrate properties.

The right-hand side of Fig. 15 illustrates another problem. When the tooling was reset in the press for another run, extensive breakage was encountered with many of the lots of steel brought to the press. An SDC analysis of the stamping showed that the severity of the stamping for the reference steel had increased to zero safety margin, which meant that the tooling was not performing identically to the previous run. This indicated a different setting in the press or modifications made to the tooling during the period the tool was out of the press. Discussion with the tool and die staff revealed that the draw beads and binder surfaces of the tooling were reworked in an attempt to reduce low spots in the stamping. This reduced the flow of metal from the binder areas and increased the stretch component required over the punch, thus increasing the stamping severity. The increased severity was verified when samples of the previous problem lot B were inserted in the tooling. Long splits were encountered compared to the previously encountered slight necking condition. The recommended solution was to encourage additional metal flow from the binder area through reduced binder pressure until the reworked draw beads became reduced in effectiveness through normal wear.

Production steel E had a slightly increased severity as compared to production steels A, C, and D. However, production steel E had a lower severity than the reference steel; production steels A, B, and D had a severity comparable to that of the reference steel. Using the reference steel as a common base for comparing various production runs, steel E actually had better formability than steels A, B, D, and the reference steel for the specific stamping under investigation.

Continued collection and monitoring of data during the entire production run was recommended. This would require a number of reference lots. However, the SDC procedure allows for this transition by the interleaf forming (alternate blanks) of both reference steels to calibrate the new reference lot relative to the previous reference lot.

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Statistical Analysis of Forming Processes

Stuart Keeler, The Budd Company Technical Center

Experimental Design

Forming operations span an extensive production life cycle, including design, prototyping, engineering changes, tryout, and production. During this entire cycle, a pervasive question is whether the process can be modified. Process optimization is the usual motivation for this modification. This optimization may mean lower cost, faster production rates, fewer rejects, better quality, and so on.

Some process simulation techniques may be available for determining the steps needed to optimize the process and the effectiveness of the various options. However, most of these simulation techniques are mathematically based and only approximate the system under evaluation (Ref 13, 14, 15). Therefore, most optimization studies are conducted on the system itself by selectively changing the system variables and measuring the effects of the changes.

These system studies are most effectively conducted with experimental design. Experimental designs are used to increase the efficiency of information acquisition.

Several forms of experimental design are available, and each has advantages and disadvantages. The types of experiments discussed in this article are single-variable studies, multi-variable studies, and Taguchi experiments. More information on the design and execution of such experiments is available in the article "Process Modeling and Simulation for Sheet Forming" in this Volume.

Single-Variable Studies

The most common experiment conducted with forming operations is the single-variable study. Although many variables are identified as affecting the process, only one of these variables is changed at a time.

Single-variable studies are widely used because they are easy to conduct and require a minimum number of experiments. First, one variable is changed, and then another, and so on, until all of the variables are at the second level. One such experimental design table is shown in Fig. 16. Here the (+) indicates the high level of the variable, and (-) indicates the low level of the variable. The following example outlines the procedure used in a typical single-variable experiment.

	Process variables						
Experiment number	Steel	Blank size	Lubricant	Die temperature	Blank position	Blankholder pressure	Ram speed
1	+A	+B	+C	+D	+E	+F	+G
2	−A	+B	+C	+D	+E	+F	+G
3	+A	−B	+C	+D	+E	+F	+G
4	+A	+B	−C	+D	+E	+F	+G
5	+A	+B	+C	−D	+E	+F	+G
6	+A	+B	+C	+D	−E	+F	+G
7	+A	+B	+C	+D	+E	−F	+G
8	+A	+B	+C	+D	+E	+F	−G
Level of variables (− / +)	A versus B	Large versus small	Yes versus no	Hot versus cold	Normal versus offset	High versus low	High versus low

Fig. 16 Layout of an experimental design for a single-variable investigation.

Example 7: Finding Improved Process Settings for the Production of an Automobile Inner Door.

High breakage rates were observed during the die tryout of an automotive inner door. A quality group met and identified seven key factors (controlling variables) that affect this particular forming process.

Two steels and a lubricant were obtained. First, the stamping was made with steel A; all other variables were kept at their existing conditions. The severity of the process was determined using the thickness ratio analysis described above. The stamping was then made using steel B.

Next followed the additional six changes indicated in Fig. 16. The thickness ratio was determined for each change. After all eight experiments were conducted, the level of each variable showing the greatest thickness ratio (least amount of metal thinning) was selected as the new standard operating procedure.

Problems With Single-Variable Studies. In this study, one variable was changed while all other variables were kept constant. The problem is that the conclusions obtained from this experiment are valid only if all other variables are kept identical to the experimental value. This type of experiment ignores synergistic effects among process variables. In the experiment described in Example 7, the effects of different materials (steels A and B) can be measured only if variable C (lubricant) is maintained at its high level (with lubricant). If variable C is at its low level (no lubricant), misleading results may be obtained. In this case, one cannot deduce what variable A does for all levels of the other variables. Therefore, some type of factorial experiment (orthogonal array) is required.

Multi-Variable Studies

Multi-variable studies are usually conducted using some form of an orthogonal array. The experimental layouts using these orthogonal arrays yield experimental results such that the effects of varying a given parameter can be separated from other effects. Figure 17 shows a complete orthogonal array, sometimes called a full factorial experiment, for seven variables. These orthogonal arrays have also been termed square games (Ref 16).

				A_1				A_2			
				B_1		B_2		B_1		B_2	
				C_1	C_2	C_1	C_2	C_1	C_2	C_1	C_2
D_1	E_1	F_1	G_1								
			G_2								
		F_2	G_1								
			G_2								
	E_2	F_1	G_1								
			G_2								
		F_2	G_1								
			G_2								
D_2	E_1	F_1	G_1								
			G_2								
		F_2	G_1								
			G_2								
	E_2	F_1	G_1								
			G_2								
		F_2	G_1								
			G_2								

Fig. 17 Layout of a full factorial (multi-variable) experimental design for a group of seven variables.

The term orthogonal means balanced, separable, or not mixed, and this indicates that each variable is evaluated equally for all other conditions. Therefore, for each of the two levels of any variable in the array in Fig. 17, there are 64 combinations of the other variables. For example, for level A_1 in Fig. 17, there are 64 possible combinations of variables B, C, D, E, F, and G. This also holds for level A_2. Therefore, any effect of level A_1 versus level A_2 is determined in the presence of both the high and low levels of all the other variables.

When the quantity of parameters is increased in these factorial experiments, the number of experiments required increases so rapidly that it may not be feasible to implement the experimental design. The full factorial experiment detailed above involves 128 experiments. Increasing the number of variables by 1 doubles the number of experiments required to 256.

The full factorial experiment provides the effect of all the single-variable effects plus all the interactions. If some of the interactions can be ignored or deemed unimportant, then the experiment can be redesigned as a partial factorial. A partial factorial has a reduced number of required experiments. The procedure used in one full factorial analysis is outlined in the following example.

Example 8: A Full Factorial Experiment for Finding Improved Process Settings for Stamping an Automobile Inner Door.

The same quality group that conducted the experiment described in Example 7 realized that the synergistic effects were not being evaluated with a single-variable experiment. The group decided to design a full factorial experiment in which all interactions would be active in the study and all interactions could be calculated from the data obtained.

The full orthogonal array shown in Fig. 17 was used. The same variable assignments used in Example 7 (see Fig. 16) were repeated in this study. Again, ultrasonic measurements of sheet thickness were used to determine the thinnest area (maximum metal thinning) for calculation of stamping severity. Analysis of the results showed that some of the interactions were very important and had to be considered in selecting the best operating levels of each parameter.

Taguchi Experiments

Conventional experimental design techniques were primarily developed for use in scientific research in order to determine cause-effect relationships (Ref 16). In science, there is generally only one law to explain a natural phenomenon; therefore, the primary experimental efforts are aimed at finding the law that explains the relationships being studied.

In technological fields, however, there are numerous ways to obtain a given product design objective. In forming a sheet metal stamping, there are various steel characteristics, die designs, die steels and surface treatments, lubricants, forming sequences, and other variables to be considered. Different combinations can be used to produce the same end design of stamping. A desirable goal is to find the combination that provides the most stable and reliable performance at the lowest manufacturing cost. This is the goal of the Taguchi experiments--to develop the most robust process possible (that is, the process that is least sensitive to the causes of variation).

In many cases, knowing the cause of a problem is insufficient for solving the problem. Removal of the cause of the problem may be too costly or even impossible. The Taguchi strategy then becomes one of finding countermeasures--not to eliminate the cause but to reduce the influence of the cause on the end product. Efforts are aimed at making the final product insensitive to all process variables. Taguchi techniques are oriented more toward cost effectiveness and marketing than are conventional experimental design techniques (Ref 16). This difference affects the nature of the parameters to be cited, the way in which the experiments are laid out, and the way in which the data are analyzed. In a sense, the Taguchi method of conducting experiments is formalized to such an extent that only minimal exchange of information is required among experimenters trained in this technique in order to achieve complete understanding of the experimental parameters, the experiments, the analysis of the data, and the results/conclusions.

Differences between the Taguchi approach to experimental design and the conventional approach are philosophical and methodological (Ref 16). Philosophically, the Taguchi approach is technological rather than theoretical, inductive rather than deductive, an engineering tool rather than scientific analysis. Taguchi emphasizes productivity enhancement and cost effectiveness with a rapid, formalized procedure--not statistical rigor. With regard to its methodology, the Taguchi approach emphasizes the application of orthogonal arrays, signal-to-noise ratios (in Taguchi analysis, the variables that affect a process are classified as signal, control, or noise factors), and newly developed analytical techniques (Ref 16).

One primary advantage of the special orthogonal arrays used by Taguchi is the capacity and flexibility of assigning numerous variables with a small number of experiments. An even more important advantage of the array is the reproducibility of the conclusions across many different process conditions. One criticism is that the Taguchi array is nothing more than a fractional factorial, which has long existed in conventional experimental design techniques. However, the Taguchi technique is one of philosophy and content, as explained above. The application is more versatile and sophisticated.

Further explanation of the Taguchi method or the experimentation/analysis procedures is beyond the scope of this article. Additional information is available in Ref 16 or from the American Supplier Institute (Dearborn, MI). The following example outlines the approach used in one Taguchi analysis.

Example 9: A Taguchi Design of Experiments for Finding Improved Process Settings for Stamping an Automobile Inner Door.

The same seven variables used in Examples 7 and 8 are used in this experiment. Selection of the proper experimental variables is extremely important in Taguchi experiments. The variables should be primary variables with little interaction. In studying stamping severity, for example, blankholder pressure (variable F) is not set by the tons of force shown on a load indicator, because the measured tons are highly interactive with all of the other variables of the system, such as blank thickness, metal strength, die temperature, and shims used. Instead, the blankholder pressure variable is controlled by the position of the nut (or turns of the screw) on the outer ram relative to the zero load reference position. The extra time spent identifying and selecting the important process variables is the key to the success of the Taguchi experiment.

Figure 18(a) shows the layout of the Taguchi experiment for the seven variables detailed in Example 7 (Fig. 16). The array is a standard array for up to seven controlling variables or factors. The eight experiments required for a full factorial analysis are shown in Fig. 18(b).

Factor	A	B	C	D	E	F	G	Results
No.	1	2	3	4	5	6	7	
1	1	1	1	1	1	1	1	y_1
2	1	1	1	2	2	2	2	y_2
3	1	2	2	1	1	2	2	y_3
4	1	2	2	2	2	1	1	y_4
5	2	1	2	1	2	1	2	y_5
6	2	1	2	2	1	2	1	y_6
7	2	2	1	1	2	2	1	y_7
8	2	2	1	2	1	1	2	y_8

(a)

				A_1				A_2			
				B_1		B_2		B_1		B_2	
				C_1	C_2	C_1	C_2	C_1	C_2	C_1	C_2
D_1	E_1	F_1	G_1	y_1							
			G_2								
		F_2	G_1								
			G_2				y_3				
	E_2	F_1	G_1								
			G_2						y_5		
		F_2	G_1							y_7	
			G_2								
D_2	E_1	F_1	G_1								
			G_2							y_8	
		F_2	G_1						y_6		
			G_2								
	E_2	F_1	G_1				y_4				
			G_2								
		F_2	G_1								
			G_2	y_2							

(b)

Fig. 18 Layout of the Taguchi experimental design for seven variables.

There are eight rows representing eight experiments, numbered 1 through 8. The elements of the seven columns consist of 1s and 2s. There are four 1s and four 2s in every column. In any pair of columns, there are four possible 1, 2 combinations; namely 11, 12, 21, and 22. Because each of these four combinations occurs at an equal number of times in a given pair of columns, the two columns are balanced or orthogonal. Figure 18 shows that all column combinations have an equal number of 11, 12, 21, and 22 combinations; therefore, all interactions are within the experimental design. After the eight experiments are completed, the data are analyzed according to a prescribed procedure in order to determine the level of each variable that contributes to the most robust (most insensitive process) combination of processing variables.

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Statistical Analysis of Forming Processes

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- A
- **air bend die**
  - Angle-forming dies in which the metal is formed without striking the bottom of the die. Metal contact is made at only three points in the cross section: the nose of the male die and the two edges of a V-shaped die opening.
- **air-lift hammer**
  - A type of gravity-drop hammer in which the ram is raised for each stroke by an air cylinder. Because length of stroke can be controlled, ram velocity and therefore the energy delivered to the workpiece can be varied. See also drop hammer and gravity hammer .
- **angle of bite**
  - In the rolling of metals, the location where all of the force is transmitted through the rolls; the maximum attainable angle between the roll radius at the first contact and the line of roll centers. Operating angles less than the angle of bite are termed contact angles or rolling angles.
- **angularity**
  - The conformity to, or deviation from, specified angular dimensions in the cross section of a shape or bar.
- **anvil**
  - A large, heavy metal block that supports the frame structure and holds the stationary die of a forging hammer. Also, the metal block on which blacksmith forgings are made.
- **anvil cap**
  - Same as sow block .
- **automatic press**
  - A press with built-in electrical and pneumatic control in which the work is fed mechanically through the press in synchronism with the press action.
- **automatic press stop**
  - A machine-generated signal for stopping the action of a press, usually after a complete cycle, by disengaging the clutch mechanism and engaging the brake mechanism.
- **axial rolls**
  - In ring rolling , vertically displaceable, tapered rolls mounted in a horizontally displaceable frame opposite to, but on the same centerline as, the main roll and rolling mandrel. The axial rolls control ring height during rolling.
- B
- **backward extrusion**
  - Same as indirect extrusion . See extrusion .
- **bar**
  - (1) A section hot rolled from a billet to a form, such as round, hexagonal, octagonal, square, or rectangular, with sharp or rounded corners or edges and a cross-sectional area of less than 105 cm^2 (16 $in.^2$). (2) A solid section that is long in relationship to its cross-sectional dimensions, having a completely symmetrical cross section and a width or greatest distance between parallel faces of 9.5 mm ($\frac{3}{8}$ in.) or more.
- **barreling**
  - Convexity of the surfaces of cylindrical or conical bodies, often produced unintentionally during upsetting or as a natural consequence during compression testing. See also compression test .
- **bead**
  - A narrow ridge in a sheet metal workpiece or part, commonly formed for reinforcement.
- **beaded flange**
  - A flange reinforced by a low ridge, used mostly around a hole.
- **bed**
  - (1) Stationary platen of a press to which the lower die assembly is attached. (2) Stationary part of the shear frame that supports the material being sheared and the fixed blade.
- **bend or twist (defect)**
  - Distortion similar to warpage generally caused during forging or trimming operations. When the distortion is along the length of the part, it is termed bend; when across the width, it is termed twist. When bend or twist exceeds tolerances, it is considered a defect. Corrective action consists of hand straightening, machine straightening, or cold restriking.

- **bend angle**
  - The angle through which a bending operation is performed, that is, the supplementary angle to that formed by the two bend tangent lines or planes.
- **bending**
  - The straining of material, usually flat sheet or strip metal, by moving it around a straight axis lying in the neutral plane. Metal flow takes place within the plastic range of the metal, so that the bent part retains a permanent set after removal of the applied stress. The cross section of the bend inward from the neutral plane is in compression; the rest of the bend is in tension. See also bending stress .
- **bending brake or press brake**
  - A form of open-frame single-action press that is comparatively wide between the housings, with a bed designed for holding long, narrow forming edges or dies. Used for bending and forming strip, plate, and sheet (into boxes, panels, roof decks, and so on).
- **bending dies**
  - Dies used in presses for bending sheet metal or wire parts into various shapes. The work is done by the punch pushing the stock into cavities or depressions of similar shape in the die or by auxiliary attachments operated by the descending punch.
- **bending rolls**
  - Various types machinery equipped with two or more rolls to form curved sheet and sections.
- **bending stress**
  - A stress involving tensile and compressive forces, which are not uniformly distributed. Its maximum value depends on the amount of flexure that a given application can accommodate. Resistance to bending can be termed stiffness.
- **bend radius**
  - The inside radius of a bent section.
- **billet**
  - (1) A semifinished section that is hot rolled from a metal ingot , with a rectangular cross section usually ranging from 105 to 230 cm^2 (16 to 36 $in.^2$), the width being less than twice the thickness. Where the cross section exceeds 230 cm^2 (36 $in.^2$), the term bloom is properly but not universally used. Sizes smaller than 105 cm^2 (16 $in.^2$) are usually termed bars. (2) A solid semifinished round or square product that has been hot worked by forging, rolling, or extrusion. See also bar .
- **blank**
  - (1) In forming, a piece of sheet material, produced in cutting dies, that is usually subjected to further press operations. (2) A piece of stock from which a forging is made; often called a slug or multiple .
- **blankholder**
  - The part of a drawing or forming die that restrains the movement of the workpiece to avoid wrinkling or tearing of the metal.
- **blanking**
  - The operation of punching, cutting, or shearing a piece out of stock to a predetermined shape.
- **block**
  - A preliminary forging operation that roughly distributes metal preparatory for finish .
- **block and finish**
  - The forging operation in which a part to be forged is blocked and finished in one heat through the use of tooling having both a block impression and a finish impression in the same die block.
- **block, first, second, and finish**
  - The forging operation in which a part to be forged is passed in progressive order through three tools mounted in one forging machine; only one heat is involved for all three operations.
- **blocker dies**
  - Dies having generous contours, large radii, draft angles of 7° or more, and liberal finish allowances. See also finish allowance .
- **blocker-type forging**
  - A forging that approximates the general shape of the final part with relatively generous finish allowance and radii. Such forgings are sometimes specified to reduce die costs where only a small number of forgings are desired and the cost of machining each part to its final shape is not excessive.
- **blocking**

    - A forging operation often used to impart an intermediate shape to a forging, preparatory to forging of the final shape in the finishing impression of the dies. Blocking can ensure proper working of the material and can increase die life.
- **blocking impression**
    - The impression that gives a forging its approximate shape.
- **bloom**
    - A semifinished hot-rolled product, rectangular in cross section, produced on a blooming mill. See also billet . For steel, the width of a bloom is not more than twice the thickness, and the cross-sectional area is usually not less than about 230 cm^2 (36 $in.^2$). Steel blooms are sometimes made by forging.
- **blooming mill**
    - A primary rolling mill used to make blooms.
- **board hammer**
    - A type of forging hammer in which the upper die and ram are attached to "boards" that are raised to the striking position by power-driven rollers and let fall by gravity. See also drop hammer .
- **bolster plate**
    - A plate to which dies can be fastened; the assembly is secured to the top surface of a press bed. In press forging, such a plate may also be attached to the ram.
- **boss**
    - A relatively short, often cylindrical protrusion or projection on the surface of a forging.
- **bottom draft**
    - Slope or taper in the bottom of a forge depression that tends to assist metal flow toward the sides of depressed areas.
- **bottoming bending**
    - Press-brake bending process in which the upper die (punch) enters the lower die and coins or sets the material to eliminate springback .
- **bow**
    - The tendency of material to curl downward during shearing, particularly when shearing long narrow strips.
- **breakdown**
    - (1) An initial rolling or drawing operation, or a series of such operations, for reducing an ingot or extruded shape to desired size before the finish reduction. (2) A preliminary press-forging operation.
- **buckling**
    - A bulge, bend, kink, or other wavy condition of the workpiece caused by compressive stresses. See also compressive stress .
- **bulging**
    - The process of increasing the diameter of a cylindrical shell (usually to a spherical shape) or of expanding the outer walls of any shell or box shape whose walls were previously straight.
- **bulk forming**
    - Forming processes, such as extrusion, forging, rolling, and drawing, in which the input material is in billet, rod, or slab form and a considerable increase in surface-to-volume ratio in the formed part occurs under the action of largely compressive loading. Compare with sheet forming .
- **bull block**
    - A machine with a power-driven revolving drum for cold drawing wire through a drawing die as the wire winds around the drum.
- **bulldozer**
    - Slow-acting horizontal mechanical press with a large bed used for bending and straightening. The work is done between dies and can be performed hot or cold. The machine is closely allied to a forging machine.
- **burr**
    - A thin ridge or roughness left on forgings or sheet metal blanks by cutting operations such as slitting, shearing, trimming, blanking, or sawing.
- **buster**
    - A pair of shaped dies used to combine preliminary forging operations, such as edging and blocking, or to loosen scale.
- **C**
- **camber**

- The tendency of material being sheared from sheet to bend away from the sheet in the same plane.
- **cam press**
  - A mechanical press in which one or more of the slides are operated by cams; usually a double-action press in which the blankholder slide is operated by cams through which the dwell is obtained.
- **canning**
  - (1) A dished distortion in a flat or nearly flat sheet metal surface, sometimes referred to as oil canning. (2) Enclosing a highly reactive metal within a relatively inert material for the purpose of hot working without undue oxidation of the active metal.
- **chamfer**
  - (1) A beveled surface to eliminate an otherwise sharp corner. (2) A relieved angular cutting edge at a tooth corner.
- **check**
  - (1) A crack in a die impression corner, generally due to forging strains or pressure, localized at some relatively sharp corner. Die blocks too hard for the depth of the die impression have a tendency to check or develop cracks in impression corners. (2) One of a series of small cracks resulting from thermal fatigue of hot forging dies.
- **chord modulus**
  - The slope of the chord drawn between any two specific points on a stress-strain curve. See also modulus of elasticity .
- **circle grid**
  - A regular pattern of circles, often 2.5 mm (0.1 in.) in diameter, marked on a sheet metal blank.
- **circle-grid analysis**
  - The analysis of deformed circles to determine the severity with which a sheet metal blank has been deformed.
- **close-tolerance forging**
  - A forging held to unusually close dimensional tolerances so that little or no machining is required after forging. See also precision forging .
- **closed-die forging**
  - The shaping of hot metal completely within the walls or cavities of two dies that come together to enclose the workpiece on all sides. The impression for the forging can be entirely in either die or divided between the top and bottom dies. Impression-die forging, often used interchangeably with the term closed-die forging, refers to a closed-die operation in which the dies contain a provision for controlling the flow of excess material, or flash , that is generated. By contrast, in flashless forging, the material is deformed in a cavity that allows little or no escape of excess material.
- **closed dies**
  - Forging or forming impression dies designed to restrict the flow of metal to the cavity within the die set, as opposed to open dies, in which there is little or no restriction to lateral flow.
- **closed pass**
  - A pass of metal through rolls where the bottom roll has a groove deeper than the bar being rolled and the top roll has a collar fitting into the groove, thus producing the desired shape free from flash or fin .
- **cluster mill**
  - A rolling mill in which each of two small-diameter work rolls is supported by two or more backup rolls.
- **cogging**
  - The reducing operation in working an ingot into a billet with a forging hammer or a forging press.
- **coin straightening**
  - A combination coining and straightening operation performed in special cavity dies designed to impart a specific amount of working in specified areas of a forging to relieve the stresses developed during heat treatment.
- **coining**
  - (1) A closed-die squeezing operation in which all surfaces of a workpiece are confined or restrained, resulting in a well-defined imprint of the die on the work. (2) a restriking operation used to sharpen or change an existing radius or profile. Coining can be done while forgings are hot or cold and is usually performed on surfaces parallel to the parting line of the forging.
- **coining dies**

- Dies in which the coining or sizing operation is performed.

- **cold coined forging**
  - A forging that has been restruck cold in order to hold closer face distance tolerances, sharpen corners or outlines, reduce section thickness, flatten some particular surface, or, in nonheattreatable alloys, increase hardness.
- **cold forming**
  - See cold working
- **cold heading**
  - Working metal at room temperature such that the cross-sectional area of a portion or all of the stock is increased. See also heading and upsetting .
- **cold lap**
  - A flaw that results when a workpiece fails to fill the die cavity during the first forging. A seam is formed as subsequent dies force metal over this gap to leave a seam on the workpiece surface. See also cold shut .
- **cold-rolled sheet**
  - A mill product produced from a hot-rolled pickled coil that has been given substantial cold reduction at room temperature. The usual end product is characterized by improved surface, greater uniformity in thickness, and improved mechanical properties as compared with hot-rolled sheet.
- **cold shut**
  - (1) A fissure or lap on a forging surface that has been closed without fusion during the forging operation. (2) A folding back of metal onto its own surface during flow in the die cavity; a forging defect.
- **cold trimming**
  - The removal of flash or excess metal from a forging at room temperature in a trimming press.
- **cold working**
  - The plastic deformation of metal under conditions of temperature and strain rate that induce strain hardening . Usually, but not necessarily, conducted at room temperature. Also referred to as cold forming or cold forging. Contrast with hot working .
- **combination die**
  - See compound die .
- **compact (noun)**
  - The object produced by the compression of metal powder, generally while confined in a die.
- **compact (verb)**
  - The operation or process of producing a compact; sometimes called pressing.
- **compound die**
  - Any die designed to perform more than one operation on a part with one stroke of the press, such as blanking and piercing, in which all functions are performed simultaneously within the confines of the blank size being worked.
- **compressive strength**
  - The maximum compressive stress a material is capable of developing. With a brittle material that fails in compression by fracturing, the compressive strength has a definite value. In the case of ductile, malleable, or semi-viscous materials (which do not fail in compression by a shattering fracture), the value obtained for compressive strength is an arbitrary value dependent on the degree of distortion that is regarded as effective failure of the material.
- **compressive stress**
  - A stress that causes an elastic body to deform (shorten) in the direction of the applied load. Contrast with tensile stress .
- **compression test**
  - A method for assessing the ability of a material to withstand compressive loads.
- **contour forming**
  - See roll forming , stretch forming , tangent bending , and wiper forming .
- **counterblow forging equipment**
  - A category of forging equipment in which two opposed rams are activated simultaneously, striking repeated blows on the workpiece at a midway point. Action is vertical or horizontal.
- **coring**
  - (1) A central cavity at the butt end of a rod extrusion; sometimes called extrusion pipe . (2) A condition of variable composition between the center and surface of a unit of microstructure

(such as a dendrite, grain, or carbide particle); results from nonequilibrium solidification, which occurs over a range of temperature.

- **corrugating**
  - The forming of sheet metal into a series of straight, parallel alternate ridges and grooves with a rolling mill equipped with matched roller dies or a press brake equipped with specially shaped punch and die.
- **corrugations**
  - Transverse ripples caused by a variation in strip shape during hot or cold reduction.
- **counterblow equipment**
  - Equipment with two opposed rams that are activated simultaneously to strike repeated blows on the workpiece placed midway between them.
- **counterblow hammer**
  - A forging hammer in which both the ram and the anvil are driven simultaneously toward each other by air or steam pistons.
- **counterlock**
  - A jog in the mating surfaces of dies to prevent lateral die shift caused by side thrust during the forging of irregularly shaped pieces.
- **crank**
  - Forging shape generally in the form of a "U" with projections at more or less right angles to the upper terminals. Cranks shapes are designated by the number of throws (for example, two-throw crank).
- **crank press**
  - A mechanical press whose slides are actuated by a crankshaft.
- **crimping**
  - The forming of relatively small corrugations in order to set down and lock a seam, to create an arc in a strip of metal, or to reduce an existing arc or diameter. See also corrugating .
- **crown**
  - (1) The upper part (head) of a press frame. On hydraulic presses, the crown usually contains the cylinder; on mechanical presses, the crown contains the drive mechanism. See also hydraulic press and mechanical press . (2) A shape (crown) ground into a flat roll to ensure flatness of cold (and hot) rolled sheet and strip.
- **cup**
  - (1) A sheet metal part; the product of the first drawing operation. (2) Any cylindrical part or shell closed at one end.
- **cup fracture (cup-and-cone fracture)**
  - A mixed-mode fracture, often seen in tensile test specimens of a ductile material, in which the central portion undergoes plane-strain fracture and the surrounding region undergoes plane-stress fracture. One of the mating fracture surfaces looks like a miniature cup; it has a central depressed flat-face region surrounded by a shear lip. The other fracture surface looks like a miniature truncated cone.
- **cupping**
  - (1) The first step in deep drawing . (2) Fracture of severely worked rods or wire in which one end looks like a cup and the other a cone.
- **cupping test**
  - A mechanical test used to determine the ductility and stretching properties of sheet metal. It consists of measuring the maximum part depth that can be formed before fracture. The test is typically carried out by stretching the test piece clamped at its edges into a circular die using a punch with a hemispherical end. See also cup fracture , Erichsen test , and Olsen ductility test .
- **cutoff**
  - A pair of blades positioned in dies or equipment (or a section of the die milled to produce the same effect as inserted blades) used to separate the forging from the bar after forging operations are completed. Used only when forgings are produced from relatively long bars instead of from individual, precut multiples or blanks. See also blank and multiple .
- **D**
- **daylight**
  - The maximum clear distance between the pressing surfaces of a press when the surfaces are in the usable open position. Where a bolster plate is supplied, it is considered the pressing surface. See also shut height .
- **deep drawing**

- Characterized by the production of a parallel-wall cup from a flat blank of sheet metal. The blank may be circular, rectangular, or a more complex shape. The blank is drawn into the die cavity by the action of a punch. Deformation is restricted to the flange areas of the blank. No deformation occurs under the bottom of the punch--the area of the blank that was originally within the die opening. As the punch forms the cup, the amount of material in the flange decreases. Also called cup drawing or radial drawing.

- **deflection**
  - The amount of deviation from a straight line or plane when a force is applied to a press member. Generally used to specify the allowable bending of the bed, slide, or frame at rated capacity with a load of predetermined distribution.
- **deformation limit**
  - In drawing , the limit of deformation is reached when the load required to deform the flange becomes greater than the load-carrying capacity of the cup wall. The deformation limit (limiting drawing ratio, LDR) is defined as the ratio of the maximum blank diameter that can be drawn into a cup without failure, to the diameter of the punch.
- **Demarest process**
  - A fluid forming process in which cylindrical and conical sheet metal parts are formed by a modified rubber bulging punch. The punch, equipped with a hydraulic cell, is placed inside the workpiece, which in turn is placed inside the die. Hydraulic pressure expands the punch.
- **developed blank**
  - A sheet metal blank that yields a finished part without trimming or with the least amount of trimming.
- **die**
  - A tool, usually containing a cavity, that imparts shape to solid, molten, or powdered metal primarily because of the shape of the tool itself. Used in many press operations (including blanking, drawing, forging, and forming), in die casting, and in forming green powder metallurgy compacts. Die-casting and powder metallurgy dies are sometimes referred to as molds. See also forging dies .
- **die assembly**
  - The parts of a die stamp or press that hold the die and locate it for the punches.
- **die block**
  - A block, often made of heat-treated steel, into which desired impressions are machined or sunk and from which closed-die forgings or sheet metal stampings are produced using hammers or presses. In forging, die blocks are usually used in pairs, with part of the impression in one of the blocks and the rest of the impression in the other. In sheet metal forming, the female die is used in conjunction with a male punch. See also closed-die forging .
- **die cavity**
  - The machined recess that gives a forging or stamping its shape.
- **die check**
  - A crack in a die impression due to forging and thermal strains at relatively sharp corners. Upon forging, these cracks become filled with metal, producing sharp ragged edges on the part. Usual die wear is the gradual enlarging of the die impression due to erosion of the die material, generally occurring in areas subject to repeated high pressures during forging.
- **die clearance**
  - Clearance between a mated punch and die; commonly expressed as clearance per side. Also called clearance or punch-to-die clearance.
- **die closure**
  - A term frequently used to mean variations in the thickness of a forging.
- **die cushion**
  - A press accessory placed beneath or within a bolster plate or die block to provide an additional motion or pressure for stamping or forging operations; actuated by air, oil, rubber, springs, or a combination of these.
- **die forging**
  - A forging that is formed to the required shape and size through working in machined impressions in specially prepared dies.
- **die forming**
  - The shaping of solid or powdered metal by forcing it into or through the die cavity .
- **die height**
  - The distance between the fixed and the moving platen when the dies are closed.

- **die holder**
  - A plate or block, on which the die block is mounted, having holes or slots for fastening to the bolster plate or the bed of the press.
- **die impression**
  - The portion of the die surface that shapes a forging or sheet metal part.
- **die insert**
  - A relatively small die that contains part or all of the impression of a forging or sheet metal part and is fastened to the master die block .
- **die life**
  - The productive life of a die impression , usually expressed as the number of units produced before the impression has worn beyond permitted tolerances.
- **die line**
  - A line or scratch resulting from the use of a roughened tool or the drag of a foreign particle between tool and product.
- **die lubricant**
  - In forging or forming, a compound that is sprayed, swabbed, or otherwise applied on die surfaces or the workpiece during the forging or forming process to reduce friction. Lubricants also facilitate release of the part from the dies and provide thermal insulation. See also lubricant .
- **die match**
  - The alignment of the upper (moving) and lower (stationary) dies in a hammer or press. An allowance for misalignment (or mismatch) is included in forging tolerances.
- **die pad**
  - A movable plate or pad in a female die; usually used for part ejection by mechanical means, springs, or fluid cushions.
- **die proof (cast)**
  - a casting of a die impression made to confirm the accuracy of the impression.
- **die radius**
  - The radius on the exposed edge of a deep-drawing die, over which the sheet flows in forming drawn shells.
- **die set**
  - (1) The assembly of the upper and lower die shoes (punch and die holders), usually including the guide pins , guide pin bushings , and heel blocks . This assembly takes many forms, shapes, and sizes and is frequently purchased as a commercially available unit. (2) Two (or, for a mechanical upsetter, three) machined dies used together during the production of a die forging .
- **die shift**
  - The condition that occurs after the dies have been set up in a forging unit in which a portion of the impression of one die is not in perfect alignment with the corresponding portion of the other die. This results in a mismatch in the forging, a condition that must be held within the specified tolerance.
- **die shoes**
  - The upper and lower plates or castings that constitute a die set (punch and die holder). Also a plate or block upon which a die holder is mounted, functioning primarily as a base for the complete die assembly . This plate or block is bolted or clamped to the bolster plate or the face of the press slide .
- **die sinking**
  - The machining of the die impressions to produce forgings of required shapes and dimensions.
- **die space**
  - The maximum space (volume), or any part of the maximum space, within a press for mounting a die.
- **die stamping**
  - The general term for a sheet metal part that is formed, shaped, or cut by a die in a press in one or more operations.
- **direct (forward) extrusion**
  - See extrusion .
- **double-action mechanical press**
  - A press having two independent parallel movements by means of two slides, one moving within the other. The inner slide or plunger is usually operated by a crankshaft; the outer or blankholder slide, which dwells during the drawing operation, is usually operated by a toggle mechanism or by cams. See also slide .

- **dimpling**
  - (1) The stretching of a relatively small, shallow indentation into sheet metal. (2) In aircraft, the stretching of metal into a conical flange for a countersunk head rivet.
- **draft**
  - The amount of taper on the sides of the forging and on projections to facilitate removal from the dies; also, the corresponding taper on the sidewalls of the die impressions. In open-die forging , draft is the amount of relative movement of the dies toward each other through the metal in one application of power. See also draft angle .
- **draft angle**
  - The angle of taper, usually 5 to 7°, given to the sides of a forging and the sidewalls of the die impression. See also draft .
- **drawability**
  - A measure of the formability of a sheet metal subject to a drawing process. The term usually used to indicate the ability of a metal to be deep drawn. See also drawing and deep drawing .
- **draw bead**
  - An insert or riblike projection on the draw ring or hold-down surfaces that aids in controlling the rate of metal flow during deep draw operations. Draw beads are especially useful in controlling the rate of metal flow in irregularly-shaped stampings.
- **draw forming**
  - A method of curving bars, tubes, or rolled or extruded sections in which the stocks is bent around a rotating form block . Stock is bent by clamping it to the form block, then rotating the form block while the stock is pressed between the form block and a pressure die held against the periphery of the form block.
- **draw marks**
  - See scoring , galling , pickup , and die line .
- **draw plate**
  - A circular plate with a hole in the center contoured to fit a forming punch; used to support the blank during the forming cycle.
- **draw radius**
  - The radius at the edge of a die or punch over which sheet metal is drawn.
- **draw ring**
  - A ring-shaped die part (either the die ring itself or a separate ring) over which the inner edge of sheet metal is drawn by the punch.
- **draw stock**
  - The forging operation in which the length of a metal mass (stock) is increased at the expense of its cross section; no upset is involved. The operation includes converting ingot to pressed bar using "V," round, or flat dies.
- **drawing**
  - A term used for a variety of forming operations, such as deep drawing a sheet metal blank; redrawing a tubular part; and drawing rod, wire, and tube. The usual drawing process with regard to sheet metal working in a press is a method for producing a cuplike form from a sheet metal disk by holding it firmly between blankholding surfaces to prevent the formation of wrinkles while the punch travel produces the required shape.
- **drawing compound**
  - A substance applied to prevent pickup and scoring during deep drawing or pressing operations by preventing metal-to-metal contact of the workpiece and die. Also known as die lubricant .
- **drop forging**
  - The forging obtained by hammering metal in a pair of closed dies to produce the form in the finishing impression under a drop hammer ; forging method requiring special dies for each shape.
- **drop hammer**
  - A term generally applied to forging hammers in which energy for forging is provided by gravity, steam, or compressed air. See also air-lift hammer , board hammer , and steam hammer .
- **drop hammer forming**
  - A process for producing shapes by the progressive deformation of sheet metal in matched dies under the repetitive blows of a gravity-drop or power-drop hammer. The process is restricted to relatively shallow parts and thin sheet from approximately 0.6 to 1.6 mm (0.024 to 0.064 in.).
- **dummy block**
  - In extrusion , a thick unattached disk placed between the ram and the billet to prevent overheating of the ram.

- **dwell**
    - Portion of a press cycle during which the movement of a member is zero or at least insignificant. Usually refers to (1) the interval when the blankholder in a drawing operation is holding the blank while the punch is making the draw or (2) the interval between the completion of the forging stroke and the retraction of the ram.
- **E**
- **earing**
    - The formation of ears or scalloped edges around the top of a drawn shell, resulting from directional differences in the plastic-working properties of rolled metal with, across, and at angles to the direction of rolling.
- **eccentric**
    - The offset portion of the driveshaft that governs the stroke or distance the crosshead moves on a mechanical or manual shear.
- **eccentric gear**
    - A main press-drive gear with an eccentric(s) as an integral part. The unit rotates about a common shaft, with the eccentric transmitting the rotary motion of the gear into the vertical motion of the slide through a connection.
- **eccentric press**
    - A mechanical press in which an eccentric, instead of a crankshaft, is used to move the slide .
- **edger (edging impression)**
    - The portion of a die impression that distributes metal during forging into areas where it is most needed in order to facilitate filling the cavities of subsequent impressions to be used in the forging sequence. See also fuller (fullering impression) .
- **edging**
    - (1) In sheet metal forming, reducing the flange radius by retracting the forming punch a small amount after the stroke but before release of the pressure. (2) In rolling, the working of metal in which the axis of the roll is parallel to the thickness dimension. Also called edge rolling. (3) The forging operation of working a bar between contoured dies while turning it 90° between blows to produce a varying rectangular cross section.
- **effective draw**
    - The maximum limits of forming depth that can be achieved with a multiple-action press; sometimes called maximum draw or maximum depth of draw.
- **elastic limit**
    - The maximum stress a material can sustain without any permanent strain (deformation) remaining upon complete release of the stress. See also proportional limit .
- **elastic deformation**
    - A change in dimensions that is directly proportional to and in phase with an increase or decrease in applied force; deformation which is recoverable when the applied force is removed.
- **elasticity**
    - The property of a material by which the deformation caused by stress disappears upon removal of the stress. A perfectly elastic body completely recovers its original shape and dimensions after the release of stress.
- **electromagnetic forming**
    - A process for forming metal by the direct application of an intense, transient magnetic field. The workpiece is formed without mechanical contact by the passage of a pulse of electric current through a forming coil. Also known as magnetic pulse forming.
- **elongation**
    - A term used in mechanical testing to describe the amount of extension of a testpiece when stressed. See also elongation, percent .
- **elongation, percent**
    - The extension of a uniform section of a specimen expressed as a percentage of the original gage length:

$$\text{Elongation, \%} = \frac{(L_x - L_o)}{L_o} \cdot 100$$

    - where L_o is the original gage length and L_x is the final gage length.
- **embossing**

- A process for producing raised or sunken designs in sheet material by means of male and female dies, theoretically with no change in metal thickness. Examples are letters, ornamental pictures, and ribs for stiffening. Heavy embossing and coining are similar operations.
- **embossing die**
  - A die used for producing embossed designs.
- **ejector**
  - A mechanism for removing work or material from between the dies.
- **ejector rod**
  - A rod used to push out a formed piece.
- **Erichsen test**
  - A cupping test used to assess the ductility of sheet metal. The method consists of forcing a conical or hemispherical-ended plunger into the specimen and measuring the depth of the impression at fracture.
- **explosive forming**
  - The shaping of metal parts in which the forming pressure is generated by an explosive charge. See also high-energy-rate forming .
- **extrusion**
  - The conversion of an ingot or billet into lengths of uniform cross section by forcing metal to flow plastically through a die orifice. In forward (direct) extrusion, the die and ram are at opposite ends of the extrusion stock, and the product and ram travel in the same direction. Also, there is relative motion between the extrusion stock and the die. In backward (indirect) extrusion, the die is at the ram end of the stock and the product travels in the direction opposite that of the ram, either around the ram (as in the impact extrusion of cylinders such as cases for dry cell batteries) or up through the center of a hallow ram. See also hydrostatic extrusion and impact extrusion .
- **extrusion defect**
  - See extrusion pipe .
- **extrusion forging**
  - (1) Forcing metal into or through a die opening by restricting flow in other directions. (2) A part made by the operation.
- **extrusion billet**
  - A metal slug used as extrusion stock .
- **extrusion pipe**
  - A central oxide-lined discontinuity that occasionally occurs in the last 10 to 20% of an extruded bar. It is caused by the oxidized outer surface of the billet flowing around the end of the billet and into the center of the bar during the final stages of extrusion. Also called coring .
- **extrusion stock**
  - A rod, bar, or other section used to make extrusions.
- **eyeleting**
  - The displacing of material about an opening in sheet or plate so that a lip protruding above the surface is formed.
- **F**
- **fillet**
  - The concave intersection of two surfaces. In forging, the desired radius at the concave intersection of two surfaces is usually specified.
- **fin**
  - The thin projection formed on a forging by trimming or when metal is forced under pressure into hairline cracks or die interfaces.
- **finish**
  - (1) The surface appearance of a product. (2) The forging operation in which the part is forged into its final shape in the finish die. If only one finish operation is scheduled to be performed in the finish die, this operation will be identified simply as finish; first, second, or third finish designations are so termed when one or more finish operations are to be performed in the same finish die.
- **finish allowance**
  - The amount of excess metal surrounding the intended final shape; sometimes called clean-up allowance, forging envelope, or machining allowance.
- **finish trim**
  - Flash removal from a forging; usually performed by trimming, but sometimes by band sawing or similar techniques.

- **finishing dies**
  - The die set used in the last forging step.
- **finishing temperature**
  - The temperature at which hot working is completed.
- **finisher (finishing impression)**
  - The die impression that imparts the final shape to a forged part.
- **first block, second block, and finish**
  - The forging operation in which the part to be forged is passed in progressive order through three tools mounted in one forging machine; only one heat is involved for all three operations.
- **fishtail**
  - (1) In roll forging , the excess trailing end of a forging. Before being trimmed off, it is often used as a tong hold for a subsequent forging operation. (2) In hot rolling or extrusion, the imperfectly shaped trailing end of a bar or special section that must be cut off and discarded as mill scrap.
- **flame straightening**
  - The correction of distortion in metal structures by localized heating with a gas flame.
- **flange**
  - A projecting rim or edge of a part; usually narrow and of approximately constant width for stiffening or fastening.
- **flaring**
  - The forming of an outward acute-angle flange on a tubular part.
- **flash**
  - Metal in excess of that required to fill the blocking or finishing forging impression of a set of dies completely. Flash extends out from the body of the forging as a thin plate at the line where the dies meet and is subsequently removed by trimming. Because it cools faster than the body of the component during forging, flash can serve to restrict metal flow at the line where dies meet, thus ensuring complete filling of the impression. See also closed-die forging .
- **flash extension**
  - That portion of flash remaining on a forged part after trimming; usually included in the normal forging tolerances.
- **flash land**
  - Configuration in the blocking or finishing impression of forging dies designed to restrict or to encourage the growth of flash at the parting line, whichever may be required in a particular case to ensure complete filling of the impression.
- **flash line**
  - The line left on a forging after the flash has been trimmed off.
- **flash pan**
  - The machined-out portion of a forging die that permits the flow through of excess metal.
- **flat die forging**
  - See open-die forging .
- **flattening**
  - (1) A preliminary operation performed on forging stock to position the metal for a subsequent forging operation. (2) The removal of irregularities or distortion in sheets or plates by a method such as roller leveling or stretcher leveling .
- **flattening dies**
  - Dies used to flatten sheet metal hems; that is, dies that can flatten a bend by closing it. These dies consist of a top and bottom die with a flat surface that can close one section (flange) to another (hem, seam).
- **flex roll**
  - A movable roll designed to push up against a sheet as it passes through a roller leveler. The flex roll can be adjusted to deflect the sheet any amount up to the roll diameter.
- **flex rolling**
  - Passing sheets through a flex roll unit to minimize yield-point elongation in order to reduce the tendency for stretcher strains to appear during forming.
- **floating die**
  - (1) A die mounted in a die holder or a punch mounted in its holder such that a slight amount of motion compensates for tolerance in the die parts, the work, or the press. (2) A die mounted on heavy springs to allow vertical motion in some trimming, shearing, and forming operations.
- **floating plug**

    - In tube drawing, an unsupported mandrel that locates itself at the die inside the tube, causing a reduction in wall thickness while the die is reducing the outside diameter of the tube.
- **flop forging**
    - A forging in which the top and bottom die impressions are identical, permitting the forging to be turned upside down during the forging operation.
- **flow lines**
    - (1) Texture showing the direction of metal flow during hot or cold working. Flow lines can often be revealed by etching the surface or a section of a metal part. (2) In mechanical metallurgy, paths followed by minute volumes of metal during deformation.
- **flow through**
    - A forging defect caused by metal flow past the base of a rib with resulting rupture of the grain structure.
- **fluid-cell process**
    - A modification of the Guerin process for forming sheet metal, the fluid-cell process uses higher pressure and is primarily designed for forming slightly deeper parts, using a rubber pad as either the die or punch. A flexible hydraulic fluid cell forces an auxiliary rubber pad to follow the contour of the form block and exert a nearly uniform pressure at all points on the workpiece. See also fluid forming and rubber-pad forming .
- **fluid forming**
    - A modification of the Guerin process, fluid forming differs from the fluid-cell process in that the die cavity, called a pressure dome, is not completely filled with rubber, but with hydraulic fluid retained by a cup-shaped rubber diaphragm. See also rubber-pad forming .
- **flying shear**
    - A machine for cutting continuous rolled products to length that does not require a halt in rolling, but rather moves along the runout table at the same speed as the product while performing the cutting, and then returns to the starting point in time to cut the next piece.
- **foil**
    - Metal in sheet form less than 0.15 mm (0.006 in.) thick.
- **fold**
    - A forging defect caused by folding metal back onto its own surface during its flow in the die cavity.
- **follow die**
    - A progressive die consisting of two or more parts in a single holder; used with a separate lower die to perform more than one operation (such as piercing and blanking) on a part in two or more stations.
- **forgeability**
    - Term used to describe the relative ability of material to deform without fracture. Also describes the resistance to flow from deformation. See also formability .
- **forging**
    - The process of working metal to a desired shape by impact or pressure in hammers, forging machines (upsetters), presses, rolls, and related forming equipment. Forging hammers, counterblow equipment, and high-energy-rate forging machines apply impact to the workpiece, while most other types of forging equipment apply squeeze pressure in shaping the stock. Some metals can be forged at room temperature, but most are made more plastic for forging by heating. Specific forging processes defined in this Glossary include closed-die forging , high-energy-rate forging , hot upset forging , isothermal forging , open-die forging , powder forging , precision forging , radial forging , ring rolling , roll forging , rotary forging , and rotary swaging .
- **forging billet**
    - A wrong metal slug used as forging stock .
- **forging dies**
    - Forms for making forgings; they generally consist of a top and bottom die. The simplest will form a completed forging in a single impression; the most complex, consisting of several die inserts, may have a number of impressions for the progressive working of complicated shapes. Forging dies are usually in pairs, with part of the impression in one of the blocks and the rest of the impression in the other block.
- **forging envelope**
    - See finish allowance .
- **forging machine (upsetter or header)**

- A type of forging equipment, related to the mechanical press , in which the principal forming energy is applied horizontally to the workpiece, which is gripped and held by prior action of the dies.

- **forging plane**
  - In forging, the plane that includes the principal die face and is perpendicular to the direction of ram travel. When the parting surfaces of the dies are flat, the forging plane coincides with the parting line. Contrast with parting plane .
- **forging quality**
  - Term used to describe stock of sufficient quality to make it suitable for commercially satisfactory forgings.
- **forging rolls**
  - Power-driven rolls used in preforming bar or billet stock that have shaped contours and notches for introduction of the work.
- **forging stock**
  - A wrought rod, bar, or other section suitable for subsequent change in cross section by forging.
- **form block**
  - Tooling, usually the male part, used for forming sheet metal contours; generally used in rubber-pad forming .
- **form die**
  - A die used to change the shape of a sheet metal blank with minimal plastic flow.
- **form rolling**
  - Hot rolling to produce bars having contoured cross sections; not to be confused with the roll forming of sheet metal or with roll forging .
- **formability**
  - The ease with which a metal can be shaped through plastic deformation. Evaluation of the formability of a metal involves measurement of strength, ductility, and the amount of deformation required to cause fracture. The term workability is used interchangeably with formability; however, formability refers to the shaping of sheet metal, while workability refers to shaping materials by bulk forming . See also forgeability .
- **forming**
  - The plastic deformation of a billet or a blanked sheet between tools (dies) to obtain the final configuration. Metalforming processes are typically classified as bulk forming and sheet forming . Also referred to as metalworking.
- **forming limit diagram (FLD)**
  - A diagram in which the major strains at the onset of necking in sheet metal are plotted vertically and the corresponding minor strains are plotted horizontally. The onset-of-failure line divides all possible strain combinations into two zones; the safe zone (in which failure during forming is not expected) and the failure zone (in which failure during forming is expected).
- **forward extrusion**
  - Same as direct extrusion. See extrusion .
- **four-high mill**
  - A type of rolling mill, commonly used for flat-rolled mill products, in which two large-diameter backup rolls are employed to reinforce two smaller work rolls, which are in contact with the product. Either the work rolls or the backup rolls may be driven. Compare with two-high mill and cluster mill .
- **frame**
  - The main structure of a press.
- **Fuller (fullering impression)**
  - Portion of the die used in hammer forging primarily to reduce the cross section and lengthen a portion of the forging stock. The fullering impression is often used in conjunction with an edger (edging impression) .
- **G**
- **gage**
  - (1) The thickness of sheet or the diameter of wire. The various standards are arbitrary and differ with regard to ferrous and nonferrous products as well as sheet and wire. (2) An aid for visual inspection that enables an inspector to determine more reliably whether the size or contour of a formed part meets dimensional requirements.
- **galling**

- A condition whereby excessive friction between high spots results in localized welding with subsequent spalling and further roughening of the rubbing surface(s) of one or both of two mating parts.
- **gap-frame press**
  - A general classification of press in which the uprights or housings are made in the form of a letter C, thus making three sides of the die space accessible.
- **gibs**
  - Guides or shoes that ensure the proper parallelism, squareness, and sliding fit between press components such as the slide and the frame. They are usually adjustable to compensate for wear and to establish operating clearance.
- **gravity hammer**
  - A class of forging hammer in which energy for forging is obtained by the mass and velocity of a freely falling ram and the attached upper die. Examples are the board hammer and air-lift hammer .
- **green**
  - Unsintered (not sintered).
- **green compact**
  - An unsintered compact .
- **green strength**
  - (1) The ability of a green compact to maintain its size and shape during handling and storage prior to sintering . (2) The tensile or compressive strength of a green compact.
- **gripper dies**
  - The lateral or clamping dies used in a forging machine or mechanical upsetter.
- **Guerin process**
  - A rubber-pad forming process for forming sheet metal.
- **guide**
  - The parts of a drop hammer or press that guide the up-and-down motion of the ram in a true vertical direction.
- **guide pin bushings**
  - Bushings, pressed into a die shoe, that allow the guide pins to enter in order to maintain punch-to-die alignment.
- **guide pins**
  - Hardened, ground pins or posts that maintain alignment between punch and die during die fabrication, setup, operation, and storage. If the press slide in out of alignment, the guide pins cannot make the necessary correction unless heel plates are engaged before the pins enter the bushings. See also heel block .
- **gutter**
  - A depression around the periphery of a forging die impression outside the flash pan that allows space for the excess metal; surrounds the finishing impression and provides room for the excess metal used to ensure a sound forging. A shallow impression outside the parting line.
- **H**
- **hammer forging**
  - Forging in which the work is deformed by repeated blows. Compare with press forging .
- **hammering**
  - The working of metal sheet into a desired shape over a form or on a high-speed hammer and a similar anvil to produce the required dishing or thinning.
- **hammer**
  - A machine that applies a sharp blow to the work area through the fall of a ram onto an anvil. The ram can be driven by gravity or power. See also gravity hammer and power-driven hammer .
- **hand forge (smith forge)**
  - A forging operation in which forming is accomplished on dies that are generally flat. The piece is shaped roughly to the required contour with little or no lateral confinement; operations involving mandrels are included. The term hand forge refers to the operation performed, while hand forging applies to the part produced.
- **hand straightening**
  - A straightening operation performed on a surface plate to bring a forging within straightness tolerance. A bottom die from a set of finish dies is often used instead of a surface plate. Hand tools used include mallets, sledges, blocks, jacks, and oil gear presses in addition to regular inspection tools.

- **Hartmann lines**
  - See Lüders lines .
- **header**
  - See forging machine .
- **heading**
  - The upsetting of wire, rod, or bar stock in dies to form parts that usually contain portions that are greater in cross-sectional area than the original wire, rod, or bar.
- **heel block**
  - A block or plate usually mounted on or attached to a lower die that serves to prevent or minimize the deflection of punches or cams.
- **hemming**
  - A bend of 180° made in two steps. First, a sharp-angle bend is made; next, the bend is closed using a flat punch and a die.
- **HERF**
  - A common abbreviation for high-energy-rate forging or high-energy-rate forming .
- **high-energy-rate forging**
  - The production of forgings at extremely high ram velocities resulting from the sudden release of a compressed gas against a free piston. Forging is usually completed in one blow. Also known as HERF processing, high-velocity forging, and high-speed forging.
- **high-energy-rate forming**
  - A group of forming processes that applies a high rate of strain to the material being formed through the application of high rates of energy transfer. See also explosive forming , high-energy-rate forging , and electromagnetic forming .
- **hold-down plate (pressure pad)**
  - A pressurized plate designed to hold the workpiece down during a press operation. In practice, this plate often serves as a stripper and is also called a stripper plate.
- **hole flanging**
  - The forming of an integral collar around the periphery of a previously formed hole in a sheet metal part.
- **Hooke's law**
  - A material in which the stress is linearly proportional to strain is said to obey Hooke's law. See also modulus of elasticity .
- **hot forming**
  - See hot working .
- **hot isostatic pressing (HIP)**
  - A process for simultaneously heating and forming a power metallurgy compact in which metal powder, contained in a sealed flexible mold, is subjected to equal pressure from all directions at a temperature high enough for sintering to take place.
- **hot trimming**
  - The removal of flash or excess metal from a hot part (such as a forging) in a trimming press.
- **hot upset forging**
  - A bulk forming process for enlarging and reshaping some of the cross-sectional area of a bar, tube, or other product form of uniform (usually round) section. It is accomplished by holding the heated forging stock between grooved dies and applying pressure to the end of the stock, in the direction of its axis, by the use of a heading tool, which spreads (upsets) the end by metal displacement. Also called hot heading or hot upsetting. See also heading and upsetting .
- **hot working**
  - The plastic deformation of metal at such a temperature and strain rate that recrystallization takes place simultaneously with the deformation, thus avoiding any strain hardening . Also referred to as hot forging and hot forming. Contrast with cold working .
- **hub**
  - A boss that is in the center of a forging and forms a part of the body of the forging.
- **hubbing**
  - The production of die cavities by pressing a male master plug, known as a hub , into a block of metal.
- **hydraulic hammer**
  - A gravity-drop forging hammer that uses hydraulic pressure to lift the hammer between strokes.
- **hydraulic-mechanical press brake**

    - A mechanical press brake that uses hydraulic cylinders attached to mechanical linkages to power the ram through its working stroke.
- **hydraulic press**
    - A press in which fluid pressure is used to actuate and control the ram.
- **hydraulic press brake**
    - A press brake in which the ram is actuated directly by hydraulic cylinders.
- **hydraulic shear**
    - A shear in which the crosshead is actuated by hydraulic cylinders.
- **hydrostatic extrusion**
    - A method of extruding a billet through a die by pressurized fluid instead of the ram used in conventional extrusion .
- **I**
- **impact extrusion**
    - The process (or resultant product) in which a punch strikes a slug (usually unheated) in a confining die. The metal flow may be either between punch and die or through another opening. The impact extrusion of unheated slugs is often called cold extrusion.
- **impact line**
    - A blemish on a drawn sheet metal part caused by a slight change in metal thickness. The mark is called an impact line when it results from the impact of the punch on the blank; it is called a recoil line when it results from transfer of the blank from the die to the punch during forming, or from a reaction to the blank being pulled sharply through the draw ring .
- **impression**
    - A cavity machined into a forging die to produce a desired configuration in the workpiece during forging.
- **impression-die forging**
    - See closed-die forging .
- **indirect (backward) extrusion**
    - See extrusion .
- **ingot**
    - A casting intended for subsequent rolling, forging, or extrusion.
- **ironing**
    - An operation used to increase the length of a tube or cup through reduction of wall thickness and outside diameter, the inner diameter remaining unchanged.
- **isostatic pressing**
    - A process for forming a powder metallurgy compact by applying pressure equally from all directions to metal powder contained in a sealed flexible mold. See also hot isostatic pressing .
- **isothermal forging**
    - A hot-forging process in which a constant and uniform temperature is maintained in the workpiece during forging by heating the dies to the same temperature as the workpiece.
- **K**
- **knockout**
    - A mechanism for releasing workpieces from a die.
- **knockout mark**
    - A small protrusion, such as a button or ring of flash, resulting from depression of the knockout pin from the forging pressure or the entrance of metal between the knockout pin and the die.
- **knockout pin**
    - A power-operated plunger installed in a die to aid removal of the finished forging.
- **L**
- **laser beam cutting**
    - A cutting process that severs material with the heat obtained by directing a laser beam against a metal surface. The process can be used with or without an externally supplied shielding gas.
- **lateral extrusion**
    - An operation in which the product is extruded sideways through an orifice in the container wall.
- **leveler lines**
    - Lines on sheet or strip running transverse to the direction of roller leveling . These lines may be seen upon stoning or light sanding after leveling (but before drawing) and can usually be removed by moderate stretching.
- **leveling**

    - The flattening of rolled sheet, strip, or plate by reducing or eliminating distortions. See stretcher leveling and roller leveling .
- **liftout**
    - The mechanism also known as knockout .
- **limiting drawing ratio (LDR)**
    - See deformation limit .
- **liners**
    - Thin strips of metal inserted between the dies and the units into which the dies are fastened.
- **lock**
    - In forging, a condition in which the flash line is not entirely in one plane. Where two or more plane changes occur, it is called compound lock. Where a lock is placed in the die to compensate for die shift caused by a steep lock, it is called a counterlock.
- **locked dies**
    - Dies with mating faces that lie in more than one plane.
- **lower punch**
    - The lower part of a die, which forms the bottom of the die cavity and which may or may not move in relation to the die body; usually movable in a forging die.
- **lubricant**
    - A material applied to dies, molds, plungers, or workpieces that promotes the flow or metal, reduces friction and wear, and aids in the release of the finished part.
- **lubricant residue**
    - the carbonaceous residue resulting from lubricant that is burned onto the surface of a hot forged part.
- **Lüders lines**
    - Elongated surface markings or depressions, often visible with the unaided eye, that form along the length of a round or sheet metal tension specimen at an angel of approximately 55° to the loading axis. Caused by localized plastic deformation, they result from discontinuous (inhomogeneous) yielding. Also known as Lüders bands, Hartmann lines, Piobert lines, or stretcher strains.
- **M**
- **mandrel**
    - (1) A blunt-ended tool or rod used to retain the cavity in a hollow metal product during working. (2) A metal bar around which other metal can be cast, bent, formed, or shaped. (3) A shaft or bar for holding work to be machined.
- **mandrel forging**
    - The process of rolling or forging a hollow blank over a mandrel to produce a weldless, seamless ring or tube.
- **manipulator**
    - A mechanical device for handling an ingot or billet during forging.
- **Mannesmann process**
    - A process for piercing tube billets in making seamless tubing. The billet is rotated between two heavy rolls mounted at an angle and is forced over a fixed mandrel.
- **Marforming process**
    - A rubber-pad forming process developed to form wrinkle-free shrink flanges and deep-drawn shells. It differs from the Guerin process in that the sheet metal blank is clamped between the rubber pad and the blankholder before forming begins.
- **master block**
    - A forging die block used primarily to hold insert dies. See also die insert .
- **match**
    - A condition in which a point in one die half is aligned properly with the corresponding point in the opposite die half within specified tolerance.
- **matched edges (match lines)**
    - Two edges of the die face that are machined exactly at 90° to each other, and from which all dimensions are taken in laying out the die impression and aligning the dies in the forging equipment.
- **matching draft**
    - The adjustment of draft angles (usually involving an increase) on parts with asymmetrical ribs and sidewalls to make the surfaces of a forging meet at the parting line.
- **mechanical press**

- A forging press with an inertia flywheel, a crank and clutch, or other mechanical device to operate the ram.
- **mechanical press brake**
  - A press brake using a mechanical drive consisting of a motor, flywheel, crankshaft, clutch, and eccentric to generate vertical motion.
- **mechanical upsetter**
  - A three-element forging press, with two gripper dies and a forming tool, for flanging or forming relatively deep recesses.
- **mechanical working**
  - The subjecting of material to pressure exerted by rolls, hammers, or presses in order to change the shape or physical properties of the material.
- **metalworking**
  - See forming .
- **mill**
  - (1) A factory in which metals are hot worked, cold worked, or melted and cast into standard shapes suitable for secondary fabrication into commercial products. (2) A production line, usually of four or more stands , for hot or cold rolling metal into standard shapes such as bar, rod, plate, sheet, or strip. (3) A single machine for hot rolling, cold rolling, or extruding metal; examples include blooming mill , cluster mill , four-high mill , and Sendzimir mill . (4) A shop term for a milling cutter. (5) A machine or group of machines for grinding or crushing ores and other minerals.
- **mill edge**
  - The normal edge produced in rolling. Can be contrasted with a blanked or sheared edge which has a burr .
- **mill finish**
  - A nonstandard (and typically nonuniform) surface finish on mill products that are delivered without being subjected to a special surface treatment (other than a corrosion-preventive treatment) after the final working or heat-treating step.
- **mill product**
  - Any commercial product of a mill .
- **mill scale**
  - The heavy oxide layer that forms during the hot fabrication or heat treatment of metals.
- **mismatch**
  - The misalignment or error in register of a pair of forging dies; also applied to the condition of the resulting forging. The acceptable amount of this displacement is governed by blueprint or specification tolerances. Within tolerances, mismatch is a condition; in excess of tolerance, it is a serious defect. Defective forgings can be salvaged by hot-reforging operations.
- **modulus of elasticity, *E***
  - The measure of rigidity or stiffness of a metal; the ratio of stress, below the proportional limit, to the corresponding strain. In terms of the stress-strain diagram , the modulus of elasticity is the slope of the stress-strain curve in the range of linear proportionality of stress to strain. Also known as Young's modulus . For materials that do not conform to Hooke's law throughout the elastic range, the slope of either the tangent to the stress-strain curve at the origin or at low stress, the secant drawn from the origin to any specified point on the stress-strain curve, or the chord connecting any two specific points on the stress-strain curve is usually taken to be the modulus of elasticity. In these cases, the modulus is referred to as the tangent modulus , secant modulus , or chord modulus , respectively.
- **multiple**
  - A piece of stock for forging that is cut from bar or billet lengths to provide the exact amount of material needed for a single workpiece.
- **multiple-slide press**
  - A press with individual slides, built into the main slide or connected to individual eccentrics on the main shaft, that can be adjusted to vary the length of stroke and the timing. See also slide .
- ***m*-value**
  - See strain-rate sensitivity .
- **N**
- **natural draft**
  - Taper on the sides of a forging, due to its shape or position in the die, that makes added draft unnecessary.

- **necking**
  - (1) The reduction of the cross-sectional area of metal in a localized area by uniaxial tension or by stretching. (2) The reduction of the diameter of a portion of the length of a cylindrical shell or tube.
- **no-draft (draftless) forging**
  - A forging with extremely close tolerances and little or no draft that requires minimal machining to produce the final part. Mechanical properties can be enhanced by closer control of grain flow and by retention of surface material in the final component.
- **nonfill (underfill)**
  - A forging condition that occurs when the die impression is not completely filled with metal.
- ***n*-value**
  - See strain-hardening exponent .
- **O**
- **offset**
  - The distance along the strain coordinate between the initial portion of a stress-strain curve and a parallel line that intersects the stress-strain curve at a value of stress (commonly 0.2%) that is used as a measure of the yield strength . Used for materials that have no obvious yield point .
- **offset yield strength**
  - The stress at which the strain exceeds by a specified amount (the offset) an extension of the initial proportional portion of the stress-strain curve; expressed in force per unit area.
- **oil canning**
  - Same as canning .
- **Olsen ductility test**
  - A cupping test in which a piece of sheet metal, restrained except at the center, is deformed by a standard steel ball until fracture occurs. The height of the cup at the time of fracture is a measure of the ductility.
- **open dies**
  - Dies with flat surfaces that are used for performing stock or producing hand forgings.
- **open-die forging**
  - The hot mechanical forming of metals between flat or shaped dies in which metal flow is not completely restricted. Also known as hand or smith forging. See also hand forge (smith forge) .
- **orbital forging**
  - See rotary forging .
- **P**
- **pad**
  - The general term used for that part of a die which delivers holding pressure to the metal being worked.
- **pancake forging**
  - A rough forged shape, usually flat, that can be obtained quickly with minimal tooling. Considerable machining is usually required to attain the finish size.
- **parting line**
  - The line among the surface of a forging where the dies meet, usually at the largest cross section of the part. Flash is formed at the parting line.
- **parting plane**
  - The plane that includes the principal die face and is perpendicular to the direction of ram travel. When parting surfaces of the dies are flat, the parting plane coincides with the parting line. Also referred to as the forging plane.
- **pass**
  - (1) A single transfer of metal through a stand of rolls. (2) The open space between two grooved rolls through which metal is processed.
- **perforating**
  - The punching of many holes, usually identical and arranged in a regular pattern, in a sheet, workpiece blank, or previously formed part. The holes are usually round, but may be any shape. The operation is also called multiple punching. See also piercing .
- **permanent set**
  - The deformation or strain remaining in a previously stressed body after release of the load.
- **pickup**
  - Small particles of oxidized metal adhering to the surface of a mill product .
- **piercing**

- The general term for cutting (shearing or punching) openings, such as holes and slots, in sheet material, plate, or parts. This operation is similar to blanking ; the difference is that the slug or piece produced by piercing is scrap, while the blank produced by blanking is the useful part.
- **pinch trimming**
  - The trimming of the edge of a tubular part or shell by pushing or pinching the flange or lip over the cutting edge of a stationary punch or over the cutting edge of a draw punch.
- **Piobert lines**
  - See Lüders lines .
- **plastic deformation**
  - The permanent (inelastic) distortion of metals under applied stresses that strain the material beyond its elastic limit . The ability of metals to flow in a plastic manner without fracture is the fundamental basis for all metal-forming processes.
- **plastic flow**
  - The phenomenon that takes place when metals or other substances are stretched or compressed permanently without rupture.
- **plastic-strain ratio (*r*-value)**
  - The ratio of the true width strain to the true thickness strain in a sheet tensile test, $r = \epsilon_w/\epsilon_t$. A formability parameter that relates to drawing, it is also known as the anisotropy factor. A high *r*-value indicates a material with good drawing properties.
- **plasticity**
  - The ability of a metal to undergo permanent deformation without rupture.
- **platen**
  - The sliding member, slide, or ram of a press.
- **plug**
  - (1) A rod or mandrel over which a pierced tube is forced. (2) A rod or mandrel that fills a tube as it is drawn through a die. (3) A punch or mandrel over which a cup is drawn. (4) A protruding portion of a die impression for forming a corresponding recess in the forging. (5) A false bottom in a die.
- **Poisson's ratio, ν**
  - The absolute value of the ratio of transverse (lateral) strain to the corresponding axial strain resulting from uniformly distributed axial stress below the proportional limit of the material in a tensile test.
- **powder forging**
  - The plastic deformation of a powder metallurgy compact or preform into a fully dense finished shape by using compressive force; usually done hot and within closed dies.
- **power-driven hammer**
  - A forging hammer with a steam or air cylinder for raising the ram and augmenting its downward blow.
- **precision forging**
  - A forging produced to closer tolerances than normally considered standard by the industry.
- **preform**
  - (1) The forging operation in which stock is preformed or shaped to a predetermined size and contour prior to subsequent die forging operations. When a preform operation is required, it will precede a forging operation and will be performed in conjunction with the forging operation and in the same heat. (2) The initially pressed powder metallurgy compact to be subjected to repressing .
- **press**
  - A machine tool with a stationary bed and a slide or ram that has reciprocating motion at right angles to the bed surface; the slide is guided in the frame of the machine.
- **press brake**
  - An open-frame single-action press used to bend, blank, corrugate, curl, notch, perforate, pierce, or punch sheet metal or plate.
- **press capacity**
  - The rated force a press is designed to exert at a predetermined distance above the bottom of the stroke of the slide.
- **press forging**
  - The forging of metal between dies by mechanical or hydraulic pressure; usually accomplished with a single work stroke of the press for each die station.
- **press forming**

- Any sheet metal forming operation performed with tooling by means of a mechanical or hydraulic press.
- **press load**
  - The amount of force exerted in a given forging or forming operation.
- **press slide**
  - See slide .
- **pressure plate**
  - A plate located beneath the bolster that acts against the resistance of a group of cylinders mounted to the pressure plate to provide uniform pressure throughout the press stroke when the press is symmetrically loaded.
- **profile (contour) rolling**
  - In ring rolling , a process used to produce seamless rolled rings with a predesigned shape on the outside or the inside diameter, requiring less volume of material and less machining to produce finished parts.
- **progression**
  - The constant dimension between adjacent stations in a progressive die.
- **progressive die**
  - A die with two or move stations arranged in line for performing two or more operations on a part; one operation is usually performed at each station.
- **progressive forming**
  - Sequential forming at consecutive stations with a single die or separate dies.
- **proof**
  - Any reproduction of a die impression in any material; often a lead or plaster cast. See also die proof .
- **proof load**
  - A predetermined load, generally some multiple of the service load, to which a specimen or structure is submitted before acceptance for use.
- **proof stress**
  - (1) The stress that will cause a specified small permanent set in a material. (2) A specified stress to be applied to a member or structure to indicate its ability to withstand service loads.
- **proportional limit**
  - The greatest stress a material is capable of developing without a deviation from straight-line proportionality between stress and strain. See also elastic limit and Hooke's law .
- **punch**
  - (1) The male part of a die--as distinguished from the female part, which is called the die. The punch is usually the upper member of the complete die assembly and is mounted on the slide or in a die set for alignment (except in the inverted die). (2) In double-action draw dies, the punch is the inner portion of the upper die, which is mounted on the plunger (inner slide) and does the drawing. (3) The act of piercing or punching a hole. Also referred to as punching .
- **punching**
  - The die shearing of a closed contour in which the sheared out sheet metal part is scrap.
- **R**
- **radial draw forming**
  - The forming of sheet metals by the simultaneous application of tangential stretch and radial compression forces. The operation is done gradually by tangential contact with the die member. This type of forming is characterized by very close dimensional control.
- **radial forging**
  - A process using two or move moving anvils or dies for producing shafts with constant or varying diameters along their length or tubes with internal or external variations in diameter. Often incorrectly referred to as rotary forging .
- **radial roll (main roll, king roll)**
  - The primary driven roll of the rolling mill for rolling rings in the radial pass. The roll is supported at both ends.
- **radial rolling force**
  - The action produced by the horizontal pressing force of the rolling mandrel acting against the ring and the main roll.
- **radius**
  - To remove the sharp edge or corner of forging stock by means of a radius or form tool.
- **ram**

- The moving or falling part of a drop hammer or press to which one of the dies is attached; sometimes applied to the upper flat die of a steam hammer. Also referred to as the slide .
- **recoil line**
  - See impact line .
- **redrawing**
  - The second and successive deep-drawing operations in which cuplike shells are deepened and reduced in cross-sectional dimensions.
- **reduction**
  - In cupping and deep drawing, a measure of the percentage of decrease from blank diameter to cup diameter, or of the diameter reduction in redrawing. (2) In forging, extrusion, rolling, and drawing, either the ratio of the original to the final cross-sectional area or the percentage of decreased in cross-sectional area.
- **reduction in area**
  - The difference between the original cross-sectional area and the smallest area at the point of rupture in a tensile test; usually stated as a percentage of the original area.
- **repressing**
  - The application of pressure to a sintered compact; usually done to improve a physical or mechanical property or for dimensional accuracy.
- **rerolling quality**
  - Rolled billets from which the surface defects have not been removed or completely removed.
- **reset**
  - The realigning or adjusting of dies or tools during a production run; not to be confused with the operation setup that occurs before a production run.
- **residual stress**
  - Stresses that remain within a body as the result of nonuniform plastic deformation or heating and cooling.
- **restriking**
  - (1) The striking of a trimmed but slightly misaligned or otherwise faulty forging with one or more blows to improve alignment, improve surface condition, maintain close tolerances, increase hardness, or effect other improvements. (2) A sizing operation in which coining or stretching is used to correct or alter profiles and to counteract distortion. (3) A salvage operation following a primary forging operation in which the parts involved are rehit in the same forging die in which the pieces were last forged.
- **reverse drawing**
  - Redrawing of a sheet metal part in a direction opposite to that of the original drawing.
- **reverse flange**
  - A sheet metal flange made by shrinking, as opposed to one formed by stretching.
- **rib**
  - (1) A long V-shaped or radiused indentation used to strengthen large sheet metal panels. (1) A long, usually thin protuberance used to provide flexural strength to a forging (as in a rib-web forging).
- **ring rolling**
  - The process of shaping weldless rings from pierced disks or shaping thick-wall ring-shaped blanks between rolls that control wall thickness, ring diameter, height, and contour.
- **rod**
  - A solid round section 9.5 mm ($\frac{3}{8}$ in.) or greater in diameter, whose length is great in relation to its diameter.
- **roll bending**
  - The curving of sheets, bars, and sections by means of rolls.
- **roll flattening**
  - The flattening of sheets that have been rolled in packs by passing them separately through a two-high cold mill with virtually no deformation. Not to be confused with roller leveling .
- **roll forging**
  - A process of shaping stock between two driven rolls that rotate in opposite directions and have one or more matching sets of grooves in the rolls; used to produce finished parts of preforms for subsequent forging operations.
- **roll forming**
  - Metal forming through the use of power-driven rolls whose contour determines the shape of the product; sometimes used to denote power spinning .

- **roll threading**
  - The production of threads by rolling the piece between two grooved die plates, one of which is in motion, or between rotating grooved circular rolls.
- **roller leveler breaks**
  - Obvious transverse breaks on sheet metal usually about 3 to 6 mm ($\frac{1}{8}$ to $\frac{1}{4}$ in.) apart that the caused by the sheet fluting during roller leveling . These will not be removed by stretching.
- **roller leveling**
  - Leveling by passing flat sheet metal stock through a machine having a series of small-diameter staggered rolls that are adjusted to produce repeated reverse bending.
- **rolling**
  - The reduction of the cross-sectional area of metal stock, or the general shaping of metal products, through the use of rotating rolls.
- **rolling mandrel**
  - In ring rolling, a vertical roll of sufficient diameter to accept various sizes of ring blanks and to exert rolling force on an axis parallel to the main roll.
- **rolling mills**
  - Machines used to decrease the cross-sectional area of metal stock and to produce certain desired shapes as the metal passes between rotating rolls mounted in a framework comprising a basic unit called a stand . Cylindrical rolls produce flat shapes; grooved rolls produce rounds, squares, and structural shapes. See also four-high mill , Sendzimir mill , and two-high mill .
- **roll straightening**
  - The straightening of metal stock of various shapes by passing it through a series of staggered rolls (the rolls usually being in horizontal and vertical planes) or by reeling in two-roll straightening machines.
- **rotary forging**
  - A process in which the workpiece is pressed between a flat anvil and a swiveling (rocking) die with a conical working face; the platens move toward each other during forging. Also called orbital forging. Compare with radial forging .
- **rotary shear**
  - A sheet metal cutting machine with two rotating-disk cutters mounted on parallel shafts driven in unison.
- **rotary swager**
  - A swaging machine consisting of a power-driven ring that revolves at high speed, causing rollers to engage cam surfaces and force the dies to deliver hammerlike blows on the work at high frequency. Both straight and tapered sections can be produced.
- **rotary swaging**
  - A bulk forming process for reducing the cross-sectional area or otherwise changing the shape of bars, tubes, or wires by repeated radial blows with one or more pairs of opposed dies.
- **rough blank**
  - A blank for a forming or drawing operation, usually of irregular outline, with necessary stock allowance for process metal, which is trimmed after forming or drawing to the desired size.
- **roughing stand**
  - The first stand (or several stands) of rolls through which a reheated billet passes in front of the finishing stands. See also rolling mills and stand .
- **rubber forming**
  - A sheet metal forming process in which rubber is used as a functional die part.
- **rubber-pad forming**
  - A sheet metal forming operation for shallow parts in which a confined, pliable rubber pad attached to the press slide (ram) is forced by hydraulic pressure to become a mating die for a punch or group of punches placed on the press bed or baseplate. Developed in the aircraft industry for the limited production of a large number of diversified parts, the process is limited to the forming of relatively shallow parts, normally not exceeding 40 mm(1.5 in.) deep. Also known as the Guerin process. Variations of the Guerin process include the Marforming process , the fluid-cell process , and fluid forming .
- **S**
- **scoring**
  - (1) The marring or scratching of any formed part by metal pickup on the punch or die. (2) The reduction in thickness of a material along a line to weaken it intentionally along that line.
- **screw press**

- A high-speed press in which the ram is activated by a large screw assembly powered by a drive mechanism.
- **secant modulus**
  - The slope of the secant drawn from the origin to any specified point on the stress-strain curve. See also modulus of elasticity .
- **segment die**
  - Same as split die .
- **semifinisher**
  - An impression in a series of forging dies that only approximates the finish dimensions of the forging. Semifinishers are often used to extend die life or the finishing impression, to ensure proper control of grain flow during forging, and to assist in obtaining desired tolerances.
- **Sendzimir mill**
  - A type of cluster mill with small-diameter work rolls and larger-diameter backup rolls, backed up by bearings on a shaft mounted eccentrically so that it can be rotated to increase the pressure between the bearing and the backup rolls. Used to roll precision and very thin sheet and strip.
- **shank**
  - The portion of a die or tool by which it is held in position in a forging unit or press.
- **shear**
  - (1) A machine or tool or cutting metal and other material by the closing motion of two sharp, closely adjoining edges; for example, squaring shear and circular shear. (2) An inclination between two cutting edges, such as between two straight knife blades or between the punch cutting edge and the die cutting edge, so that a reduced area will be cut each time. This lessens the necessary force, but increases the required length of the working stroke. This method is referred to as angular shear. (3) The act of cutting by shearing dies or blades, as in a squaring shear. (4) The type of force that causes or tends to cause two contiguous parts of the same body to slide relative to each other in a direction parallel to their plane of contact.
- **shear strength**
  - The maximum shear stress a material can sustain. Shear strength is calculated from the maximum load during a shear or torsion test and is based on the original dimensions of the cross section of the specimen.
- **shear stress**
  - (1) A stress that exists when parallel planes in metal crystals slide across each other. (2) The stress component tangential to the plane on which the forces act.
- **shearing**
  - The parting of material that results when one blade forces the material past an opposing blade.
- **sheet**
  - Any material or piece of uniform thickness and of considerable length and width as compared to its thickness. With regard to metal, such pieces under 6.5 mm ($\frac{1}{4}$ in.) thick are called sheets, and those 6.5 mm ($\frac{1}{4}$ in.) thick and over are called plates. Occasionally, the limiting thickness for steel to be designated as sheet steel is No. 10 Manufacturer's Standard Gage for sheet steel, which is 3.42 mm (0.1345 in.) thick.
- **sheet forming**
  - The plastic deformation of a piece of sheet metal by tensile loads into a three-dimensional shape, often without significant changes in sheet thickness or surface characteristics. Compare with bulk forming .
- **shim**
  - A thin piece of material used between two surfaces to obtain a proper fit, adjustment, or alignment.
- **shrinkage**
  - The contraction of metal during cooling after hot forging. Die impressions are made oversize according to precise shrinkage scales to allow the forgings to shrink to design dimensions and tolerances.
- **shut height**
  - For a press, the distance from the top of the bed to the bottom of the slide with the stroke down and adjustment up. In general, it is the maximum die height that can be accommodated for normal operation, taking the bolster plate into consideration.
- **side thrust**
  - The lateral force exerted between the dies by reaction of a forged piece on the die impressions.

- **single-stand mill**
    - A rolling mill designed such that the product contacts only two rolls at a given moment. Contrast with tandem mill .
- **sinking**
    - The operation of machining the impression of a desired forging into die blocks.
- **sintering**
    - The densification and bonding of adjacent particles in a powder mass or compact by heating to a temperature below the melting point of the main constituent.
- **sizing**
    - (1) Secondary forming or squeezing operations needed to square up, set down, flatten, or otherwise correct surfaces to produce specified dimensions and tolerances. See restriking . (2) Some burnishing, broaching, drawing, and shaving operations are also called sizing. (3) A finishing operation for correcting ovality in tubing. (4) Final pressing of a sintered powder metallurgy part.
- **slab**
    - A flat-shaped semifinished rolled metal ingot with a width not less than 250 mm (10 in.) and a cross-sectional area not less than 105 cm^2 (16 $in.^2$).
- **slabbing**
    - The hot working of an ingot to a flat rectangular shape.
- **slide**
    - The main reciprocating member of a press, guided in the press frame, to which the punch or upper die is fastened; sometimes called the ram . The inner slide of a double-action press is called the plunger or punch-holder slide; the outer slide is called the blankholder slide. The third slide of a triple-action press is called the lower slide, and the slide of a hydraulic press is often called the platen.
- **slide adjustment**
    - The distance that a press slide position can be altered to change the shut height of the die space. The adjustment can be made by hand or by power mechanism.
- **slitting**
    - Cutting or shearing along single lines to cut strips from a sheet or to cut along lines of a given length or contour in a sheet or workpiece.
- **slug**
    - (1) The metal removed when punching a hole in a forging; also termed punchout. (2) The forging stock for one workpiece cut to length. See also blank .
- **smith forging**
    - See hand forge (smith forge) .
- **sow block**
    - A block of heat-treated steel placed between the anvil of the hammer and the forging die to prevent undue wear to the anvil. Sow blocks are occasionally used to hold insert dies. Also called anvil cap.
- **spinning**
    - The forming of a seamless hollow metal part by forcing a rotating blank to conform to a shaped mandrel that rotates concentrically with the blank. In the typical application, a flat-rolled metal blank is forced against the mandrel by a blunt, rounded tool; however, other stock (notably, welded or seamless tubing) can be formed. A roller is sometimes used as the working end of the tool.
- **split die**
    - A die made of parts that can be separated for ready removal of the workpiece. Also known as segment die.
- **springback**
    - (1) The elastic recovery of metal after stressing. (2) The extent to which metal tends to return to its original shape or contour after undergoing a forming operation. This is compensated for by overbending or by a secondary operation of restriking .
- **stamping**
    - The general term used to denote all sheet metal pressworking.
- **stand**
    - A piece of rolling mill equipment containing one set of work rolls. In the usual sense, any pass of a cold- or hot-rolling mill. See also rolling mills .
- **steam hammer**

- A type of drop hammer in which the ram is raised for each stroke by a double-action steam cylinder and the energy delivered to the workpiece is supplied by the velocity and weight of the ram and attached upper die driven downward by steam pressure. The energy delivered during each stroke can be varied.

- **stock**
  - A general term used to refer to a supply of metal in any form or shape and also to an individual piece of metal that is formed, forged, or machined to make parts.
- **stop**
  - A device for positioning stock or parts in a die.
- **straightening**
  - A finishing operation for correcting misalignment in a forging or between various sections of a forging.
- **strain**
  - The unit of change in the size or shape of a body due to force, in reference to its original size or shape.
- **strain aging**
  - The changes in ductility, hardness, yield point, and tensile strength that occur when a metal or alloy that has been cold worked is stored for some time. In steel, strain aging is characterized by a loss of ductility and a corresponding increase in hardness, yield point, and tensile strength.
- **strain hardening**
  - An increase in hardness and strength caused by plastic deformation at temperatures below the recrystallization range. Also known as work hardening.
- **strain-hardening coefficient**
  - See strain-hardening exponent .
- **strain-hardening exponent**
  - The value *n* in the relationship $\sigma = K\epsilon^n$, where σ is the true stress; ϵ is the true strain; and *K*, which is called the strength coefficient, is equal to the true stress at a true strain of 1.0. The strain-hardening exponent, also called *n*-value, is equal to the slope of the true stress/true strain curve up to maximum load, when plotted on log-log coordinates. The *n*-value relates to the ability of a sheet material to be stretched in metalworking operations. The higher the *n*-value, the better the formability (stretchability).
- **strain-rate sensitivity (*m*-value)**
  - The increase in stress (σ) needed to cause a certain increase in plastic strain rate ($\dot{\epsilon}$) at a given level of plastic strain (ϵ) and a given temperature (*T*).

$$\text{Strain-rate sensitivity} = m = \left(\frac{\Delta \log \sigma}{\Delta \log \dot{\epsilon}}\right)_{\epsilon T}$$

- **stress**
  - The intensity of the internally distributed forces or components of forces that resist a change in the volume or shape of a material that is or has been subjected to external forces. Stress is expressed in force per unit area. Stress can be normal (tension or compression) or shear.
- **stress raisers**
  - Design features (such as sharp corners) or mechanical defects (such as notches) that act to intensify the stress at these locations.
- **stress-strain curve**
  - See stress-strain diagram .
- **stress-strain diagram**
  - A graph in which corresponding values of stress and strain from a tension, compression, or torsion test are plotted against each other. Values of stress are usually plotted vertically (ordinates or *y*-axis) and values of strain horizontally (abscissas or *x*-axis). Also known as deformation curve and stress-strain curve.
- **stretcher leveling**
  - The leveling of a piece of sheet metal (that is, removing warp and distortion) by gripping it at both ends and subjecting it to a stress higher than its yield strength.
- **stretcher straightening**
  - A process for straightening rod, tubing, and shapes by the application of tension at the ends of the stock. The products are elongated a definite amount to remove warpage.
- **stretcher strains**

    - Elongated markings that appear on the surface of some sheet materials when deformed just past the yield point. These markings lie approximately parallel to the direction of maximum shear stress and are the result of localized yielding. See also Lüders lines .
- **stretch former**
    - (1) A machine used to perform stretch forming operations. (2) A device adaptable to a conventional press for accomplishing stretch forming.
- **stretch forming**
    - The shaping of a sheet or part, usually of uniform cross section, by first applying suitable tension or stretch and then wrapping it around a die of the desired shape.
- **stretching**
    - The extension of the surface of a sheet in all directions. In stretching, the flange of the flat blank is securely clamped. Deformation is restricted to the area initially within the die. The stretching limit is the onset of metal failure.
- **striking surface**
    - Those areas on the faces of a set of dies that are designed to meet when the upper die and lower die are brought together. The striking surface helps protect impressions from impact shock and aids in maintaining longer die life.
- **strip**
    - A flat-rolled metal product of some maximum thickness and width arbitrarily dependent on the type of metal; narrower than sheet .
- **stripper**
    - A plate designed to remove, or strip, sheet metal stock from the punching members during the withdrawal cycle. Strippers are also used to guide small precision punches in close-tolerance dies, to guide scrap away from dies, and to assist in the cutting action. Strippers are made in two types: fixed and movable.
- **stripper punch**
    - A punch that serves as the top or bottom of the die cavity and later moves farther into the die to eject the part or compact. See also ejector rod and knockout .
- **stroke (up or down)**
    - The vertical movement of a ram during half of the cycle, from the full open to the full closed position or vice versa.
- **sub-sow block (die holder)**
    - A block used as an adapter in order to permit the use of forging dies that otherwise would not have sufficient height to be used in the particular unit or to permit the use of dies in a unit with different shank sizes.
- **superplasticity**
    - The ability of certain metals to develop extremely high tensile elongations at elevated temperatures and under controlled rates of deformation.
- **support plate**
    - A plate that supports a draw ring or draw plate. it also serves as a spacer.
- **swage**
    - (1) The operation of reducing or changing the cross-section area of stock by the fast impact of revolving dies. (2) The tapering of bar, rod, wire, or tubing by forging, hammering, or squeezing; reducing a section by progressively tapering lengthwise until the entire section attains the smaller dimension of the taper.
- **Swift cup test**
    - A simulative test in which circular blanks of various diameter are clamped in a die ring and deep drawn into a cup by a flat-bottomed cylindrical punch. The ratio of the largest blank diameter that can be drawn successfully to the cup diameter is known as the limiting drawing ratio (LDR) or deformation limit .
- **T**
- **tandem die**
    - Same as follow die .
- **tandem mill**
    - A rolling mill consisting of two or more stands arranged so that the metal being processed travels in a straight line from stand to stand. In continuous rolling, the various stands are synchronized so that the strip can be rolled in all stands simultaneously. Contrast with single-stand mill .
- **tangent bending**

    - The forming of one or more identical bends having parallel axes by wiping sheet metal around one or more radius dies in a single operation. The sheet, which may have side flanges, is clamped against the radius die and then made to conform to the radius die by pressure from a rocker-plate die that moves along the periphery of the radius die. See also wiper forming (wiping) .
- **tangent modulus**
    - The slope of the stress-strain curve at any specified stress or strain. See also modulus of elasticity .
- **template (templet)**
    - A gage or pattern made in a die department, usually from sheet steel; used to check dimensions on forgings and as an aid in sinking die impressions in order to correct dimensions.
- **tensile strength**
    - In tensile testing, the ratio of maximum load to original cross-sectional area. Also known as ultimate strength. Compare with yield strength .
- **tensile stress**
    - A stress that causes two parts of an elastic body, on either side of a typical stress plane, to pull apart. Contrast with compressive stress .
- **tension**
    - The force or load that produces elongation.
- **thermal-mechanical treatment**
    - See thermomechanical working .
- **thermomechanical working**
    - A general term covering a variety of processes combining controlled thermal and deformation treatments to obtain synergistic effects, such as improvement in strength without loss of toughness. Same as thermal-mechanical treatment.
- **three-point bending**
    - The bending of a piece of metal or a structural member in which the object is placed across two supports and force is applied between and in opposition to them. See V-bend die .
- **throw**
    - The distance from the centerline of the crankshaft or main shaft to the centerline of the crankpin or eccentric in crack or eccentric presses. Equal to one-half of the stroke. See also crank press and eccentric press .
- **toggle press**
    - **A** mechanical press in which the slide is actuated by one or more toggle links or mechanisms.
- **tong hold**
    - The portion of a forging billet, usually on one end, that is gripped by the operator's tongs. It is removed from the part at the end of the forging operation. Common to drop hammer and press-type forging.
- **tooling marks**
    - Indications imparted to the surface of the forged part from dies containing surface imperfections or dies on which some repair work has been done. These marks are usually slight rises or depressions in the metal.
- **torsion**
    - A twisting deformation of a solid or tubular body about an axis in which lines that were initially parallel to the axis become helices.
- **torsional stress**
    - The shear stress on a transverse cross section resulting from a twisting action.
- **total elongation**
    - The total amount of permanent extension of a test piece broken in a tensile test usually expressed as a percentage over a fixed gage length. See also elongation, percent .
- **trimmer**
    - The dies used to remove the flash or excess stock from a forging.
- **trimmer blade**
    - The portion of the trimmers through which a forging is pushed to shear off the flash.
- **trimmer die**
    - The punch press die used for trimming flash from a forging.
- **trimmer punch**
    - The upper portion of the trimmer that contacts the forging and pushes it through the trimmer blades; the lower end of the trimmer punch is generally shaped to fit the surface of the forging against which it pushes.

- **trimmers**
    - The combination of trimmer punch, trimmer blades, and perhaps, trimmer shoe used to remove flash from a forging.
- **trimming**
    - The mechanical shearing of flash or excess material from a forging with a trimmer in a trim press; can be done hot or cold.
- **trimming press**
    - A power press suitable for trimming flash from forgings.
- **triple-action press**
    - A mechanical or hydraulic press having three slides with three motions properly synchronized for triple-action drawing, redrawing, and forming. Usually, two slides--the blankholder slide and the plunger--are located above and a lower slide is located within the bed of the press. See also hydraulic press , mechanical press , and slide .
- **tryout**
    - Preparatory run to check or test equipment, lubricant, stock, tools, or methods prior to a production run. Production tryout is run with tools previously approved; new die tryout is run with new tools not previously approved.
- **tube stock**
    - A semifinished tube suitable for subsequent reduction and finishing.
- **two-high mill**
    - A type of rolling mill in which only two rolls, the working rolls, are contained in a single housing. Compare with four-high mill and cluster mill .
- **U**
- **U-bend die**
    - A die, commonly used in press-brake forming, that is machined horizontally with a square or rectangular cross-sectional opening that provides two edges over which metal is drawn into a channel shape.
- **ultimate strength**
    - The maximum stress (tensile, compressive, or shear) a material can sustain without fracture; determined by dividing maximum load by the original cross-sectional area of the specimen. Also known as nominal strength or maximum strength.
- **underfill**
    - A portion of a forging that has insufficient metal to give it the true shape of the impression.
- **upset**
    - The localized increase in cross-sectional area of a workpiece or weldment resulting from the application of pressure during mechanical fabrication or welding.
- **upset forging**
    - A forging obtained by upset of a suitable length of bar, billet or bloom.
- **upsetter**
    - A horizontal mechanical press used to make parts from bar stock or tubing by upset forging , piercing, bending, or otherwise forming in dies. Also known as a header.
- **upsetting**
    - The working of metal so that the cross-sectional area of a portion or all of the stock is increased. See also heading .
- **V**
- **V-bend die**
    - A die commonly used in press-brake forming, usually machined with a triangular cross-sectional opening to provide two edges as fulcrums for accomplishing three-point bending .
- **vent**
    - A small hole in a punch or die for admitting air to avoid suction holding or for relieving pockets of trapped air that would prevent die closure or action.
- **vent mark**
    - A small protrusion resulting from the entrance of metal into die vent holes.
- **W**
- **warm working**
    - Deformation at elevated temperatures below the recrystallization temperature. The flow stress and rate of strain hardening are reduced with increasing temperature; therefore, lower forces are required than in cold working. See also cold working and hot working .
- **web**

- A relatively flat, thin portion of a forging that effects an interconnection between ribs and bosses; a panel or wall that is generally parallel to the forging plane. See also rib .
- **wiper forming (wiping)**
  - Method of curving sheet metal sections or tubing over a form block or die in which this form block is rotated relative to a wiper block or slide block.
- **wire**
  - A thin, flexible, continuous length of metal, usually of circular cross section and usually produced by drawing through a die.
- **wire drawing**
  - Reducing the cross section of wire by pulling it through a die.
- **wire rod**
  - Hot-rolled coiled stock that is to be cold drawn into wire.
- **work hardening**
  - See strain hardening .
- **workability**
  - See formability .
- **wrap forming**
  - See stretch forming .
- **wrinkling**
  - A wavy condition obtained in deep drawing of sheet metal, in the area of the metal between the edge of the flange and the draw radius. Wrinkling may also occur in other forming operations when unbalanced compressive forces are set up.
- **Y**
- **yield**
  - Evidence of plastic deformation in structural materials. Also known as plastic flow or creep.
- **yield point**
  - The first stress in a material, usually less than the maximum attainable stress, at which an increase in strain occurs without an increase in stress. Only certain metals--those which exhibit a localized, heterogeneous type of transition from elastic to plastic deformation--produce a yield point. If there is a decrease in stress after yielding, a distinction can be made between upper and lower yield points. The load at which a sudden drop in the flow curve occurs is called the upper yield point. The constant load shown on the flow curve is the lower yield point.
- **yield strength**
  - The stress at which a material exhibits a specified deviation from proportionality of stress and strain. An offset of 0.2% is used for many metals. Compare with tensile strength .
- **yield stress**
  - The stress level of highly ductile materials, such as structural steels, at which large strains take place without further increase in stress.
- **Young's modulus**
  - A term used synonymously with modulus of elasticity . The ratio of tensile or compressive stresses to the resulting strain.

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Abbreviations and Symbols

- **Abbreviations and Symbols**
- ***a***
  - crystal lattice length along the *a* axis
- ***A***
  - area; heat retention factor
- **A**
  - ampere
- **Å**
  - angstrom
- **A_1**
  - subcritical annealing temperature
- **AA**
  - Aluminum Association
- **ac**
  - alternating current
- **ACI**
  - Alloy Casting Institute
- **A/D**
  - analog to digital
- **AFD**
  - automated forging design
- **AI**
  - artificial intelligence
- **AISI**
  - American Iron and Steel Institute
- **ALPID**
  - Analysis of Large Plastic Incremental Deformation (bulk deformation modeling software)
- **AM**
  - analytical modeling
- **AMS**
  - Aerospace Material Specification (of SAE)
- **ANSI**
  - American National Standards Institute
- **API**
  - American Petroleum Institute
- **ASME**
  - American Society of Mechanical Engineers
- **ASTM**
  - American Society for Testing and Materials
- **at.%**
  - atomic percent
- **atm**

- atmospheres (pressure)
- **AWS**
  - American Welding Society
- ***b***
  - crystal lattice length along the *b* axis
- **bal**
  - balance or remainder
- **bcc**
  - body-centered cubic
- **bct**
  - body-centered tetragonal
- **BDC**
  - bottom dead center
- **BID**
  - blocker initial design
- ***c***
  - crystal lattice length along the *c* axis
- ***C***
  - heat capacity
- **C**
  - coulomb
- **CAD/CAM**
  - computer-aided design/computer-aided manufacturing
- **CAE**
  - computer-aided engineering
- **CDA**
  - Copper Development Association
- **cm**
  - centimeter
- **CNC**
  - computer numerical control
- **cpm**
  - cycles per minute
- **cps**
  - cycles per second
- ***d***
  - density; used in mathematical expressions involving a derivative (denotes rate of change)
- **d**
  - day
- D_c
  - critical cylinder diameter (indicator of hardenability)
- D_{eff}
  - effective diffusion coefficient
- **DBMS**
  - data base management system
- **dc**
  - direct current
- **diam**
  - diameter
- **DIN**
  - Deutscher Normenausschuss (German standards organization)
- ***E***
  - Young's modulus
- ***e***
  - natural log base, 2.71828 . . .
- e_1
  - major engineering strain

- ***e*$_2$**
  - minor engineering strain
- **EDM**
  - electrical discharge machining
- **EP**
  - extreme pressure
- **EPA**
  - Environmental Protection Agency
- **Eq**
  - equation
- **ESR**
  - electroslag remelting
- ***f***
  - local geometric inhomogeneity
- **fcc**
  - face-centered cubic
- **FEM**
  - finite element modeling
- **Fig.**
  - figure
- **FLD**
  - forming limit diagram
- **FM**
  - free from Mannesmann effect (dies or open-die forging process)
- **FML**
  - free from Mannesmann effect, low load (dies or open-die forging process)
- **FRP**
  - fiber-reinforced plastic
- **ft**
  - foot
- **g**
  - gram
- **gal**
  - gallon
- **h**
  - hour
- ***h***
  - deformation heat factor
- **HB**
  - Brinell hardness
- **hcp**
  - hexagonal close-packed
- **HERF**
  - high-energy-rate forging
- **HIP**
  - hot isostatic pressing
- **HK**
  - Knoop hardness
- **hp**
  - horsepower
- **HR**
  - Rockwell hardness; requires scale designation such as HRC for Rockwell C hardness
- **HSLA**
  - high-strength, low-alloy (steel)
- **HV**
  - Vickers (diamond pyramid) hardness
- **Hz**

    - hertz
- **ID**
    - inside diameter
- **IGES**
    - initial graphics exchange specification
- **I/M**
    - ingot metallurgy
- **in.**
    - inch
- **ipm**
    - inches per minute
- **ips**
    - inches per second
- **ISO**
    - International Organization for Standardization
- **J**
    - joule
- **JIC**
    - Joint Industry Conference
- **K**
    - Kelvin
- ***k***
    - Boltzmann's constant
- **KBES**
    - knowledge-based expert system (computer software)
- **kg**
    - kilogram
- **KI**
    - knowledge-based integration
- **kN**
    - kilonewton
- **kPa**
    - kilopascal
- **ksi**
    - kips per square inch (1000 pounds per square inch)
- **kW**
    - kilowatt
- ***L***
    - length; liter
- **lb**
    - pound
- **lbf**
    - pound (force)
- **LDR**
    - limiting draw ratio
- **ln**
    - natural logarithm (base *e*)
- **log**
    - common logarithm (base 10)
- ***m***
    - strain rate sensitivity factor
- **mA**
    - milliampere
- **max**
    - maximum
- **M.F.**
    - multiplying factor

- **mg**
  - milligram
- **Mg**
  - megagram
- **MIL**
  - military
- **MIL-STD**
  - military standard
- **min**
  - minimum; minute
- **mL**
  - milliliter
- **mm**
  - millimeter
- **MME**
  - material modeling environment
- **MN**
  - meganewton
- **MPa**
  - megapascal
- ***n***
  - strain-hardening exponent
- **N**
  - newton
- **NASA**
  - National Aeronautics and Space Administration
- **NC**
  - numerical control
- **No.**
  - number
- **OBI**
  - open-back inclinable (press)
- **OD**
  - outside diameter
- **OS**
  - operating system
- **OSHA**
  - Occupational Safety and Health Administration
- **oz**
  - ounce
- **p**
  - page
- **Pa**
  - pascal
- **PH**
  - precipitation-hardenable
- **P/M**
  - powder metallurgy
- **ppb**
  - parts per billion
- **ppm**
  - parts per million
- **psi**
  - pounds per square inch
- **psia**
  - pounds per square inch (absolute)
- **psig**

- pounds per square inch (gage)
- **PVA**
  - plan view area
- ***Q***
  - activation energy for diffusion
- ***r***
  - radius; thermal resistance
- ***R***
  - radius; range (in statistical process control); area reduction; extrusion ratio
- **R**
  - Rankine
- **Ref**
  - reference
- **rem**
  - remainder or balance
- **RFQ**
  - request for quotation
- **RHR**
  - root height reading
- **rms**
  - root mean square
- **rpm**
  - revolutions per minute
- **SAE**
  - Society of Automotive Engineers
- **SE**
  - software engineering
- **sfm**
  - surface feet per minute
- **SI**
  - Systeme International d'Unites
- **SPC**
  - statistical process control
- **SPF**
  - superplastic forming
- **SPF/DB**
  - superplastic forming/diffusion bonding
- **SUS**
  - Saybolt universal second (measure of viscosity)
- ***t***
  - thickness; time
- **T**
  - absolute temperature
- **TDC**
  - top dead center
- **TMP**
  - thermomechanical processing
- **tonf**
  - tons of force
- **tsi**
  - tons per square inch
- **TYS**
  - tensile yield strength
- **UHC**
  - ultrahigh carbon
- **UNS**
  - Unified Numbering System

- **UTS**
  - ultimate tensile strength
- ***V***
  - volume
- **V**
  - volt
- **VAR**
  - vacuum arc remelted
- **vol%**
  - volume percent
- ***w***
  - weight or mass
- **W**
  - watt
- **WRC**
  - Welding Research Council
- **wt%**
  - weight percent
- $\overline{X}$
  - average (in statistical process control)
- ***x***
  - distance in the direction of heat flow
- **yr**
  - year
- °
  - degree (angular measure)
- **°C**
  - temperature, degrees Celsius (centigrade)
- **°F**
  - temperature, degrees Fahrenheit
- ÷
  - divided by
- =
  - equals
- $\approx$
  - approximately equals
- $\neq$
  - not equal to
- >
  - greater than
- $\gg$
  - much greater than
- $\geq$
  - greater than or equal to
- $\int$
  - integral of
- <
  - less than
- $\ll$
  - much less than
- $\leq$
  - less than or equal to
- ±
  - maximum deviation
- **-**
  - minus; negative ion charge
- ×

- multiplied by; diameters (magnification)
- $\cdot$
  - multiplied by
- **/**
  - per
- **%**
  - percent
- +
  - plus; in addition to
- $\sqrt{}$
  - square root of
- $\sim$
  - similar to; approximately
- $\propto$
  - varies as; is proportional to
- Δ
  - change in quantity, an increment, a range
- ϵ
  - strain
- μ
  - coefficient of friction
- μ**in.**
  - micro-inch
- μ**m**
  - micron (micrometer)
- μ**s**
  - microsecond
- π
  - pi (3.14159 . . .)
- ρ
  - density
- σ
  - stress
- Σ
  - summation of
- Ω
  - ohm; atomic volume

Greek Alphabet

- **A,** α
  - alpha
- **B,** β
  - beta
- Γ**,** γ
  - gamma
- Δ**,** δ
  - delta
- **E,** ϵ
  - epsilon
- **Z,** ζ
  - zeta
- **H,** η
  - eta
- Θ**,** θ
  - theta
- **I,** ι
  - iota
- **K,** κ

    - kappa
- Λ, λ
    - lambda
- **M,** μ
    - mu
- **N,** ν
    - nu
- Ξ, ξ
    - xi
- **O, o**
    - omicron
- Π, π
    - pi
- **P,** ρ
    - rho
- Σ, σ
    - sigma
- **T,** τ
    - tau
- Υ, υ
    - upsilon
- Φ, ϕ
    - phi
- **X,** χ
    - chi
- Ψ, ψ
    - psi
- Ω, ω
    - omega