FOREWORD

This Final Technical Documentary Report covers all work performed under Contract AF 33(657)-8784 from 5 June 1962 to 5 January 1964. The manuscript was released by the author on 12 May 1964 for publication as an RTD Technical Documentary Report.

This contract with IIT Research Institute, Chicago, Illinois, was initiated under Manufacturing Methods Project 7-945, "Liner for Extrusion Billet Containers." It was accomplished under the technical direction of T. S. Felker of the Metallurgical Processing Branch, (MATB), Manufacturing Technology Division, AF Materials Laboratory, Wright-Patterson Air Force Base, Ohio.

Dr. Sheldon A. Spachner of IITRI's Metals and Ceramics Division was the metallurgist in charge and performed all the container calculations. Others who cooperated in the research were Michael Hnatusko and Edward H. Zemke, Project Technicians; Roy E. Reinholds, Project Assistant Experimentalist; Jack V. Smith, Tool Designer; R. G. Sturm, Project Consulting Engineer; and Harry Schwartzbart, Assistant Director, Metals and Ceramics Research. This report has been given the Institute's designation IITRI-B244-18.

This project has been accomplished as a part of the Air Force Manufacturing Methods Program, the primary objective of which is to develop, on a timely basis, manufacturing processes, techniques and equipment for use in economical production of USAF materials and components. The program encompasses the following technical areas:

Metallurgy	-Rolling, Forging, Extruding, Casting, Fiber, Powder.
Chemical	-Propellant, Coating, Ceramic, Graphite, Nonmetallics.
Electronic	-Solid State, Materials and Special Techniques, Thermionics.
Fabrication	-Forming, Material Removal, Joining, Components.

Suggestions concerning additional Manufacturing Methods development required on this or other subjects will be appreciated.

ABSTRACT

LINER FOR EXTRUSION BILLET CONTAINERS

A program was carried out to secure an improvement of extrusion container liner performance and life for metal extrusion effected in the 2000°F-3400°F temperature range. Liner-support tooling was developed to permit use of superalloy, ceramic-coated tool steel, and solid ceramics as liner materials capable of withstanding 180,000 psi stem pressure. The tooling utilized liner-sleeve assemblies which were not shrink-fitted in the container and could quickly be interchanged. The liner-sleeve assemblies therefore, were shrink-fitted and semiautomatically assembled by a machine developed for the purpose. Disassembly was accomplished by use of other specially developed tooling. Liner wall thickness was held to 1/4 in. in all cases, effecting a considerable cost reduction in these parts. Prototype extrusion liners of René 41 and Udimet 700 superalloys, and a variety of both Rokide-process and plasma-arc-ceramic coated liners, were produced and evaluated by extrusion of 3-1/2 in. diameter SAE 4340 steel billets at 2000°F and extrusion of TZM alloy billets at 3300°-3600°F, using a 1000-ton capacity, high-speed hydraulic press. Compositions of fiber-metal reinforced ceramic materials specially designed for liner application, were produced and evaluated by extrusion-screening tests.

A Rokide-process stabilized zirconia ceramic-coated liner withstood TZM alloy extrusion at 3450°F. Plasma-arc coatings of stabilized zirconia, and gradated alumina-nickel, and cast Udimet 700 alloy, proved satisfactory for steel extrusion at 2000°F. Preliminary evaluation indicated that glass lubricants were unnecessary when extruding steel billets in a gradated alumina-nickel ceramic coated extrusion liner.

Fiber metal reinforced ceramic mixtures of $72Al_2O_3$-28Mo and $59.5Al_2O_3$-40.5Mo proved capable of withstanding billet temperatures of 3450°F.

* *

PUBLICATION REVIEW

This final technical documentary report has been reviewed and is approved.

FOR THE COMMANDER:

Melvin E. Fields

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LINER FOR EXTRUSION BILLET CONTAINERS

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LINER FOR EXTRUSION BILLET CONTAINERS

I. INTRODUCTION

A. Project Goal and Need for its Achievement

The objective of this effort is the improvement of extrusion container liner performance and life for billet extrusion carried out in the 2000°-3400°F temperature range. Present-day extrusion liners perform quite well at 2000°F, with liner life ranging from 2000 to 6000 extrusions. As billet temperatures increase, however, liner life drops sharply. When 3200°F billet temperatures are employed, liners may have to be removed and replaced after only 20 extrusions have been produced.

Short liner life causes a sharp increase in the cost of the extrusions produced. This is due to the cost of the extrusion liner itself, the difficulties in removing and replacing present liners, and the lost production time.

At the present time, extrusion liners are made of high-grade hot-worked tool steel, a relatively costly material. Liner weight ranges from 250 lb for a 4 in. liner used on a 1000-ton capacity press, to 1000 lb for a 7 1/2 in. liner used on a 2750-ton capacity press. Once the liner surface has been pitted, grossly scored, or distorted, it is necessary to rebore the part to a larger size if it is to be used again. This, of course, makes it unsuitable for the size of billet currently being extruded.

Removal of the liner from the container also can pose a major problem. Current container design requires liners to be shrink-fitted into the containers. This practice develops compressive stresses in the liner which enable it to withstand extrusion-generated hoop stresses of up to 200,000 psi without appreciable distortion. But this practice also requires differential heating procedures to remove the liner from the container. The difficulties inherent in such procedures can be appreciated when the size and weight of the container are considered. A 30 in. diameter container on a 2750-ton press weighs approximately 6000 lb. Removal of the container liner requires that a suitable radial temperature gradient across the liner-container assembly be established. This is achieved by cooling the liner bore of a heated assembly. Often, up to 2000 tons of force are simultaneously applied to effect separation. Should these procedures fail, it may be necessary to anneal the entire assembly, bore out the liner, and then heat-treat and finish machine the container. In spite of the high cost and difficulty of extruding refractory alloys at the temperatures at which they must be worked, extrusion remains the best and in some cases the only method of producing required structural shapes from such materials.

The number of refractory alloys finding use in the aerospace effort increases each year. Consequently, a solution to the problem of short liner life will be expected to pay increasing dividends. Conversely, the lack of a solution may be expected to sharply limit application of promising materials, because of slow and prohibitively expensive extrusion procedures.

B. Material Choices for Improved Extrusion Liners

Clearly, the first requirement of an improved liner material is that it not be adversely affected by contact with a billet at 3400°F. The second requirement is that the liner material possess sufficient strength and elastic strain capability to permit it to withstand the stresses and strains developed in it by extrusion pressure.

These requirements rule out the use of all steels and most metals. All tool steels will lose hardness and strength at temperatures over 1300°F. Those refractory metals which have relatively high strength and hardness at elevated temperatures, e.g., alloys of tungsten or molybdenum, possess poor oxidation resistance. Materials which possess good oxidation resistance at temperatures to 2000°F, and relatively good high-temperature strength, such as nickel-base superalloys, appear to be marginal candidates.

If metal liners do not appear promising, one is led to a consideration of the use of high-strength nonmetals such as ceramics. Some ceramics are capable of withstanding temperatures of 3400°F without apparent injury. Stabilized zirconia and magnesia are examples. However, ceramics have particular properties which may also make them unsuitable for extrusion liner use. Inadequate strength, in both tension and compression, and thermal shock sensitivity are undesirable characteristics which, unfortunately, most ceramics possess.

Ceramics which are suitable in these two respects may find application for liner use if, but only if, they are properly supported. The support required for such items differs from that required by metallic liner materials because ceramics have two properties which differ from those of virtually all metals. First, ceramics have a very low tensile strength, only 6 to 8% of their compressive strength. Secondly, ceramics do not display any appreciable ductility. The material fractures when its elastic limit is exceeded. The higher elastic modulus of ceramics and generally lower thermal conductivity coefficient are important, but secondary, considerations in the design of support tooling.

Adequate support for ceramics may be obtained in one of three ways. First, high-strength steel sleeves may be successively shrink-fitted on a ceramic liner to maintain the liner in a state of compressive stress under all extrusion pressures. Secondly, ceramics may be flame-sprayed on the inside of a steel sleeve. The sleeve, held under some degree of compression by shrink-fitted sleeves, prevents the elastic limit of the coating from being exceeded. In the third case, a composite ceramic-metal structure may be produced, using metal fibers. Due to a difference in thermal expansion coefficient, the metal fibers hold the ceramic in compression. Such material will appear to have a greater tensile elastic strain capability than the parent ceramic. In reality, compressive strain in the material is being relaxed as tensile stress increases.

C. Characteristics Required of Ceramic Liner Support Tooling

1. General Characteristics

Since all ceramics have low tensile strain capability compared with metals, the ceramic liner support tooling must exert a greater compressive stress than tooling supporting a metal liner. This is accomplished by increasing the amount of interference in shrink-fitting operations.

Generating a high compressive stress in the liner by the support tooling develops a high tensile stress in this tooling. Addition of tensile extrusion stresses to the tensile stress already present in the support tooling due to shrink-fitting is likely to overload this tooling and cause failure. High stress levels may be reduced by increasing the number of shrink-fitted support sleeves. Tool steel liners may be adequately supported by one or two shrink-fitted sleeves. Ceramic liners must be supported by at least four shrink-fitted sleeves.

The removal and interchange of a ceramic liner from an extrusion container poses another problem. Normal practice requires heat to be abstracted from a heated container by cooling the liner bore. Such a procedure establishes a suitable temperature gradient across the radius of the container, causing separation at the liner-container interface. Unfortunately, the thermal conductivity of ceramics is almost always much lower than the thermal conductivity of metals. This, in turn, makes it impossible to extract heat rapidly enough from the liner bore to generate the desired radial temperature gradient. Increasing the temperature differential between liner bore and container by heating to maximum container temperature, then pouring liquid nitrogen into the liner bore, will not develop a temperature differential which even closely approaches required values. Consequently, use of ceramic liners requires the design of an assembly which can provide suitable liner support <u>without being shrink-fitted into the container.</u>

Such an assembly would, of course, have to be in close contact with the container if any support were to be obtained from the container. Such contact could be obtained by designing for an initial clearance of less than 0.001 in. Creep resulting from repeated extrusion loading of the assembly would, over a period of time, reduce this clearance to zero. Insertion and ejection of the assembly in the container could then be accomplished by use of light pressing and ejection force. The saving developed in liner interchange time by use of such tooling provides a strong incentive for its use in conventional aluminum or steel extrusion.

The requirement of a non-shrink-fitted assembly does pose design problems, because of the requirement for high compressive stress on the ceramic. High compressive stress is developed by high sleeve interference, rather than by no sleeve interference. Clearly, the sleeves which do interfere must have a sufficiently high interference to provide the required compressive stress. Once a "no-interference" condition is established between an inner and outer sleeve, the container will not be able to develop compressive stress in the liner.

Difference in thermal expansion characteristic of the ceramic and the metal support tooling presents another design problem. Generally, thermal expansion coefficients of ceramics are far lower than those of the metal support tooling. As a consequence, support tooling designed to place a ceramic liner under a given compressive stress at an operating temperature of 600°F, will develop a much higher compressive stress in the liner at room temperature. Allowance for this condition must be made in the liner-sleeve assembly to avoid the possibility of compressive failure of the liner at room temperature.

One other characteristic of ceramic liners which has a direct bearing on the design employed is the cost of such materials. Ceramic liners are much more expensive than tool steel liners on the basis of cost-per-pound of finished liner. Moreover, sound ceramic bodies of the same size as conventional steel liners cannot be produced. The only way that liner size can be reduced is by reducing wall thickness. Therefore, ceramic liners require a support-tooling design which can utilize liner walls of minimum thickness.

2. Specific Characteristics

Solid ceramic liners generally have a tensile strength of less than 8% of the compressive strength. Hence, if such materials are to be used, they must be held in compression under all extrusion conditions. Other restrictions are also present. Axial strain and octahedral shear stress limitations also exist. Compliance with such limitations will be of little avail, however, if liner material should operate in tension during extrusion operations.

Both ceramic coatings and fiber-metal reinforced ceramics exhibit a degree of tensile elastic strain that is greater than that found in the parent ceramic. However, this tensile property is usually obtained at the expense of compressive strength, or compressive strain of the parent ceramic. To make maximum use of such materials, maximum values of both tensile and compressive elastic strains should be minimized. This may be accomplished by suitable design of the support tooling. If the support tooling places a compressive stress on the liner numerically equal to one-half of the maximum tensile stress that the liner will be exposed to under extrusion conditions, then the peak tensile and compressive strain in the liner will be balanced and minimized. For example, if maximum tensile stress under extrusion conditions is +200,000 psi, one should design the support tooling to develop 100,000 psi compressive stress in the liner. The liner will then operate between -100,000 and +100,000 psi.

D. General Procedure Followed in Liner Development Effort

Once liner material requirements and liner support requirements were delineated, two interrelated questions had to be satisfactorily answered before development work could proceed. The first was, "What available materials have suitable properties for liner service?" The second question was, "Can suitable tooling be designed to provide adequate mechanical support under extrusion conditions for extrusion liners made of such materials?"

Satisfactory answers were found to both questions, permitting the conduct of an effort aimed at exploring the potential of ceramics for extrusion liner application, and developing liners made of such materials for industrial application.

The following sections describe material selection, supporting tool design procedures, and the experimental program for development and evaluation of the high-temperature service extrusion liners produced.

II. LINER MATERIAL SELECTION

A. Selection of Material Class

Consideration of general extrusion liner requirements for service at temperatures to 3400°F at stem pressures up to 180,000 psi, indicated that four classes of materials might be useful for this effort.

1. Ceramics and other nonmetals possessing high compressive strength, relatively low tensile strength, and low thermal conductivity.

2. Nonmetal-coated metal liners.

3. High-temperature metal liners possessing suitable mechanical properties to 1400°F.

4. Fiber-metal reinforced ceramics.

Investigation of suitable materials was carried out by a search of the technical literature and contact with producers. The search of the technical literature was greatly aided by the activity of IITRI personnel on ASD Contract AF 33(657)-8339, "Utilization of Refractory Materials in Future Aerospace Vehicles." Over 800 references were searched, and 80 manufacturers of high-strength materials were contacted. In addition, all carbide manufacturers in the U. S. were contacted for interest and/or information concerning products possessing the required mechanical properties and manufactured to specific shapes and tolerances.

B. Solid Ceramic Selection

Selection of solid ceramics was based on the ability of the material, and the material fabricators to meet the following specifications:

Wall thickness	1/4 in.
Tensile and/or compressive strength	Sum of tensile and compressive strengths must be a minimum of 250,000 psi (e. g., a 15,000 psi tensile strength, and a 235,000 psi compressive strength.)
Thermal shock	Liner at 700°-800°F must be able to withstand contact with billet at 3400°F for a maximum of 60 sec.
Thermal cycling	Liner must be able to withstand moderate-rate container heating from room temperature to 600°F and slow cooling from 600°F to room temperature.
Heat conductivity	Relatively low heat conductivity is desirable to reduce cooling rate of the billet.

Elastic modulus and thermal expansion characteristics are important design variables, but do not have critical limits. Materials which have suitable compressive or tensile strengths appear to have adequate elastic moduli values.

Since the project was oriented toward development of a suitable extrusion liner, rather than material development, and since several materials did meet the above specifications, no efforts were made to develop manufacturing processes for ceramics which could not be obtained in the size and shape desired.

The materials finally selected for this effort are listed in Table I. All of the listed manufacturers agreed to attempt to produce liners of required dimensions on a "quote" basis.

TABLE I

SUITABLE SOLID CERAMIC EXTRUSION LINER MATERIALS

Liner Material	Compressive Strength, psi.	Manufacturer
Zirconium carbide	250,000	Norton Company
Titanium diboride	325,000	Norton Company
Titanium carbide	350,000	Norton Company
Alumina (99.3%)	340,000	International Ceramics Company
Lucalox	>250,000	General Electric Co., Lamp Glass, Dept.

C. Ceramic Coating Process and Material Selection

1. Coating Processes

Application of adherent nonmetallic coatings to metal surfaces is effected by use of flame-spraying techniques. The three processes most commonly used are: Rokide-process flame spraying, plasma-arc spraying, and detonation spraying. The principal difference in these processes is the particle velocity of the material being sprayed.

The Rokide process utilizes a relatively slow particle velocity and accordingly, deposits a low-density coating. Relatively thick coatings can be produced by this process.

Plasma-arc spraying generates faster powder particle velocities and deposits a more dense surface coating. Some plasma-arc users believe that thick coatings made by this process are more likely to spall because of this higher density, and accordingly, suggest the use of coatings of 0.010 in. or less thickness.

Detonation spraying, accomplished by igniting acetylene and oxygen in a specially designed gun, develops the highest coating density. Unfortunately, this process is suitable for coating low-curvature surfaces only, since a 6 ft long barrel is employed which develops a flat-trajectory powder stream. Consequently, only the Rokide and plasma-arc processes were considered for ceramic-coated liner use.

2. Rokide-Process Coatings

Two Rokide-process coatings have been successfully used on extrusion dies. These are alumina and stabilized zirconia. Both coatings require the use of a flame-sprayed Nichrome-V alloy undercoat. Since die wear is generally more severe than liner wear during extrusion, it appeared that these two coatings offered reasonable possibilities of successful application.

3. Plasma-Arc Coatings

Based on past performance in high-temperature applications, two different types of plasma-arc coatings appeared to offer promise in liner use. The first type consisted of a ceramic coating sprayed on a metallic undercoat, similar to the Rokide-process spraying. The second type was a gradated coating. In the latter type, both metal and ceramic are sprayed simultaneously. At the start of spraying, the particle stream is almost all metal. Ratio of metal-to-ceramic is continuously adjusted as coating thickness is increased until the particle stream consists of nearly all ceramic.

Two laminar coatings were suggested on the basis of past performance; alumina with a nickel undercoat, and stabilized zirconia with Nichrome undercoat. The suggested gradated coatings were alumina-nickel and a zirconia-nickel-chromium mixture. The latter coating requires a diffusion anneal at 1950°F to develop optimum properties. All other coatings did not require heat treatment.

4. Elevated-Temperature Alloys

Elevated-temperature alloys, in general, have a proof stress of less than 150,000 psi at 1400°F. Measurable distortion is likely to occur at even low stress levels. To minimize stress on the liner, support tooling for such liners was designed in the same manner as for ceramic-coated liners, generating a compressive stress in the liners equal to approximately 50% of the compressive proportional limit. This permitted the liner to operate between a -115,000 psi and a +115,000 psi tangential stress as stem pressure rose from 0 to 200,000 psi.

However, only two superalloys could be found which gave reasonable promise of operating without distortion at 115,000 psi at 1400°F. These were both nickel-base alloys, i.e., René 41 and Udimet 700. The René 41 alloy was procured in the forged condition and machined to liner dimensions. The Udimet 700 was cast in the form of a thin-walled cylinder, heat treated for maximum mechanical property, then machined and ground to size.

5. Fiber-Metal Reinforced Ceramics

Performance of fiber-metal reinforced ceramics in high-temperature applications has indicated that these materials may have attractive properties for liner use; however, such materials are not commercially available. Evaluation of the utility of such materials does require material development and screening test activity, in addition to developing a manufacturing technology for the production of extrusion liners.

Since these materials did appear to have considerable promise for liner application, a modest materials development and testing program was undertaken to provide additional information on their suitability for this use. This was the only materials development effort undertaken by this program.

III. LINER SUPPORT TOOLING DESIGN

A. Mechanical and Thermal Property Requirements of Support Sleeves

1. Yield Strength

A material possessing an extremely high-yield strength is favored for sleeve construction if ceramic liners are to be used. The reason is that ceramics or other nonmetals which have low tensile, but high compressive strengths must be held in compression by the support tooling at all times.

Generation of adequate compressive stress in the liner places a high tensile stress on the support tooling. The tensile stress generated by billet extrusion must be added to the tensile stress developed by shrink-fitting. Multiple shrink-ring construction of the support tooling helps to reduce such tensile stresses. However, the net tensile stress is still high. Hence, a high yield strength material is preferred for support tooling.

2. Temper Resistance

Sleeves should also maintain strength at the highest temperatures possible. This property is important for two reasons. First, the ability to heat sleeves to higher temperatures in shrink-fitting operations permits the use of greater interferences between sleeves, and the generation of higher interfacial pressures.

Secondly, tempering of the first support sleeve during extrusion operations may be avoided. If the thermal conductivity of the liner is relatively high and the liner has a thin wall, the heat transfer rate will be rapid from liner to first sleeve. Tempering of this sleeve would cause a reduction in the compressive stress placed on the liner, and could be a cause of premature failure of the liner.

3. Rapid Liner Replacement Capability

In addition to providing adequate support for the liner, container-sleeve assembly should be designed to permit rapid extraction and replacement of a damaged liner. Reduction of "downtime" during liner change carries two important advantages. First, screening tests of candidate liner materials becomes unnecessary, if extrusion liners can be quickly extrusion-tested and interchanged in the container assembly. Such a procedure increases the accuracy of the test method and reduces the cost of the tests. Secondly, use of liners which possess a relatively short extrusion life becomes economically feasible, increasing the probability of developing satisfactory tooling for high-temperature extrusion service.

Such capability is developed by design of a sleeve assembly which supplied sufficient compressive hoop stress to the liner without the necessity of any contribution from the container. A 0.0001-0.0005 in. clearance may then be permitted between sleeve assembly and container. Such clearance will not substantially reduce compressive stress on the liner during extrusion, but will permit immediate removal of the liner-sleeve assembly when necessary. It is noted that intimate contact between sleeve assembly and container is secured during extrusion due to expansion of the sleeve assembly.

B. Sleeve and Container Material

HTB-2 Steel (Allegheny-Ludlum Steel Co. trade name), was selected for liner-support sleeve use. The 0.2% offset yield stress of this steel, at the container assembly operating temperature of 600°F, is 300,500 psi. The tempering temperature is above 1025°F, allowing shrink-fitting operations to be carried on at temperatures up to 1025°F.

Extrusion stress decreases rapidly with increasing sleeve radius. At a radial distance of 3.25 in. from the liner centerline, a stem pressure of 180,000 psi will generate a hoop stress of less than 50,000 psi. Accordingly, lower-strength, less expensive tool steel may be used for support at radial distances over 3.25 in. Ajax CMV steel proved suitable for this purpose.

C. Tooling Design for Support of a Solid Ceramic Extrusion Liner

1. Design Characteristics

A half-scale assembly drawing of the support tooling used to generate the desired compressive stress in the liner is shown in Figure 1. Five sleeves are used. The first four sleeves are made of HTB-2 steel. They are successively shrink-fitted over the liner and one another. The fifth sleeve, made of H-13 steel, is shrink-fitted into the container, shown in Figure 2. The assembly is designed to have an 0.0001-0.0005 in. clearance between the fourth and fifth sleeve at its operating temperature of 600°F. This permits rapid removal of the liner and four sleeve assembly in the event of liner failure. An extrusion pressure of 5,650 psi will expand the outer radius of the fourth sleeve 0.0005 in., causing it to lock against the container sleeve inner wall. Once this has occurred, the liner support sleeves and the container operate as one in distributing the extrusion stress developed. The flange on the fourth sleeve keeps the assembly in position under no-load conditions. Pertinent mechanical and physical properties of the HTB-2 and H-13 steels used are given in Table II.

It may also be seen that the first sleeves have 1/4 in. thick walls, while the fourth sleeve has a 1/2 in. wall. The advantage of using thin-walled sleeves for generation of compressive stress is decreased as the radial distance from the container axis increases. Use of two shrink-fitted, 1/4 in. wall sleeves in place of the 1/2 in. wall sleeve would do little to increase the compressive stress on the liner. Therefore, a 1/2 in. sleeve is used in this section of the assembly.

Since the assembly shown in Figure 1 places a high tangential compressive stress on the liner, a significant liner axial tensile stress would be expected. Consequently, an attempt was made to incorporate a means for reducing this axial tensile stress in the liner. First efforts involved the use of cantilevered sections on the opposite ends of the ID of the first and second sleeves. The stress magnification and high moment developed near the fulcrum of the cantilevered section by this design made such efforts impractical. Consequently, the sleeve design was modified to make use of straight-walled cylinders. Axial constraint was developed by use of a short liner and spacers

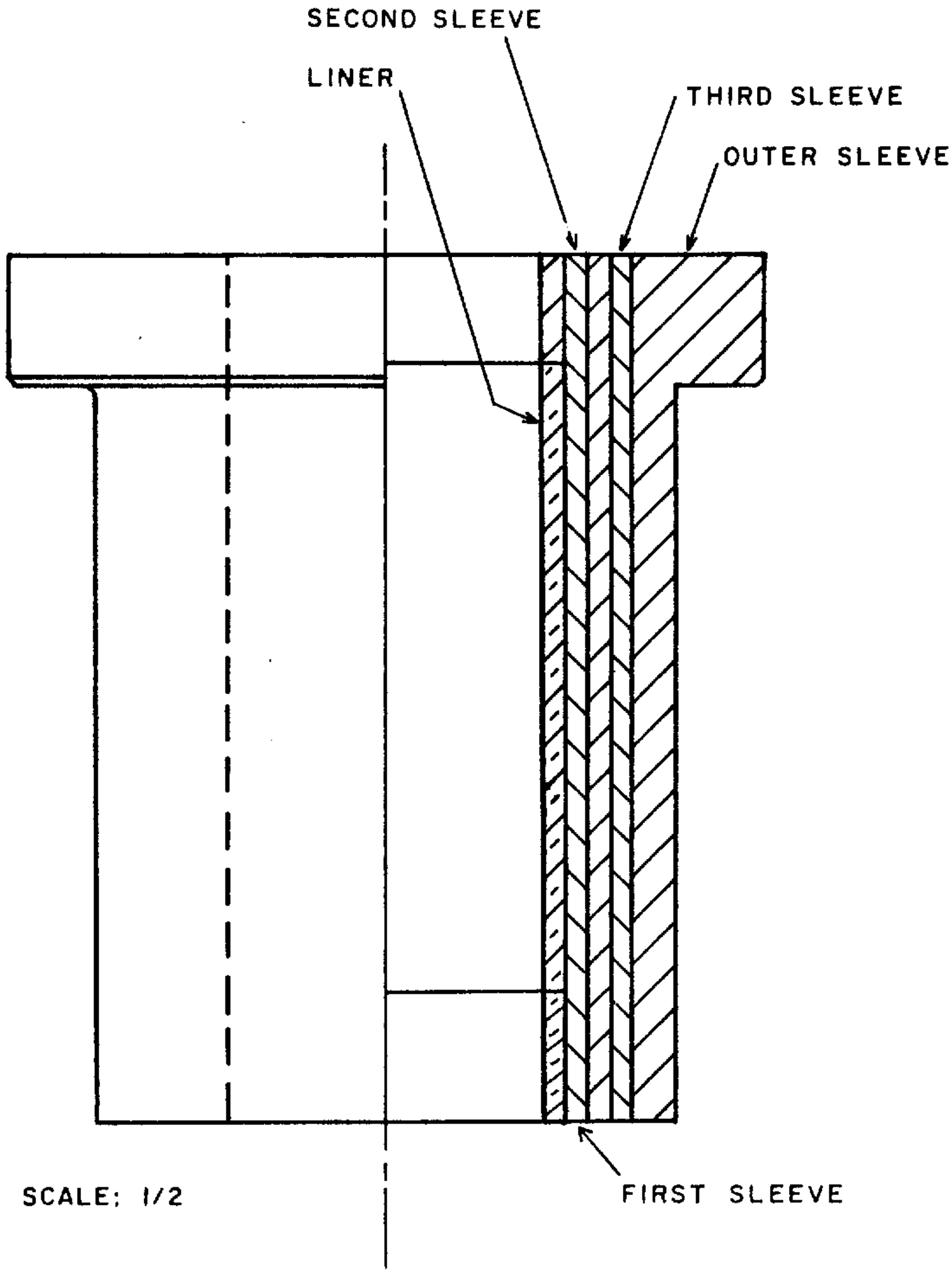
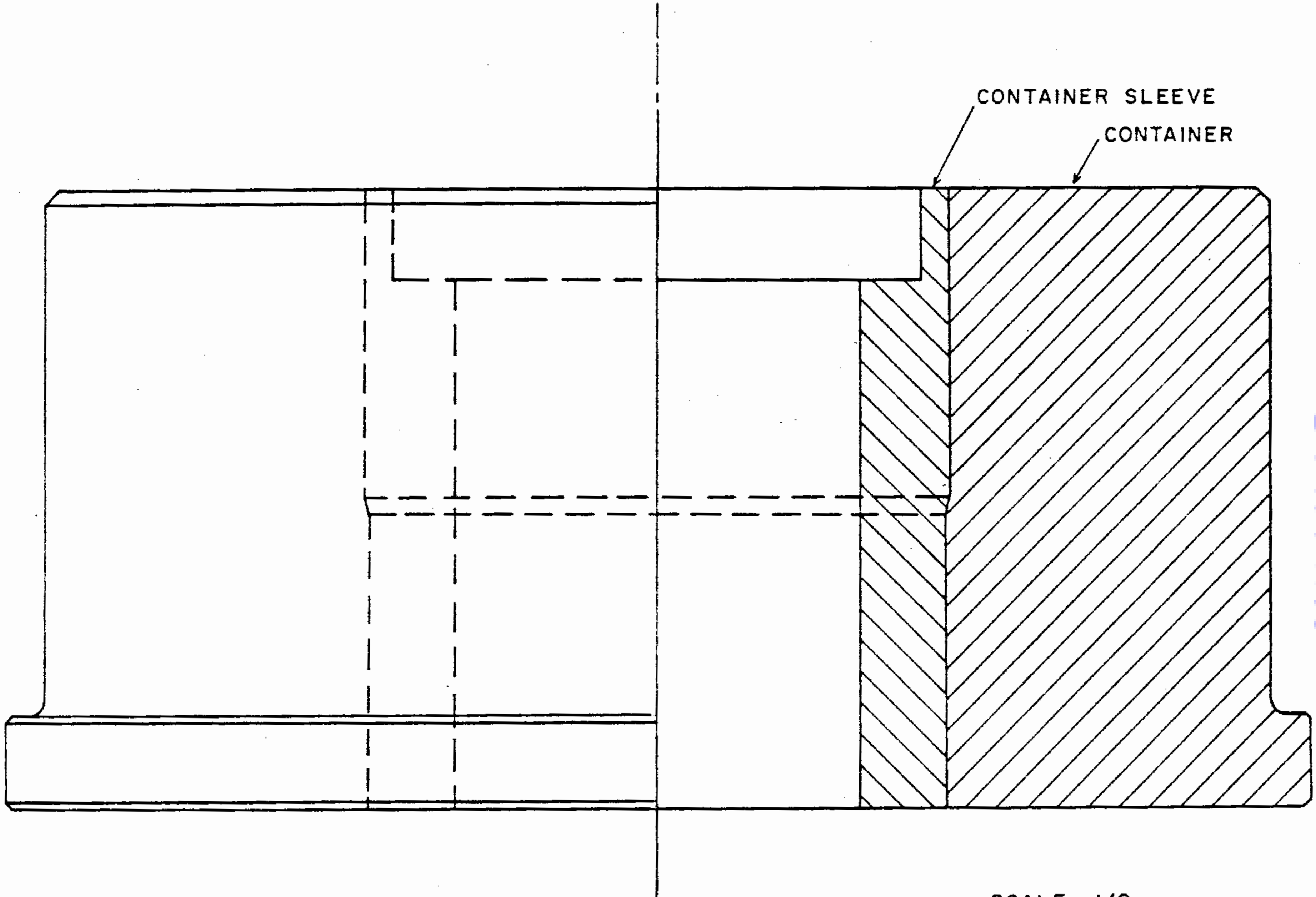


FIG. 1 LINER AND 4-SLEEVE ASSEMBLY



SCALE: 1/2

FIG. 2 CONTAINER SLEEVE AND CONTAINER

TABLE II

SELECTED MECHANICAL AND PHYSICAL PROPERTIES OF HTB-2 AND H-13 STEEL

Property	HTB-2	H-13
Young's modulus: room temperature	29.3×10^6 psi	---*
600°F	25.3×10^6 psi	25.2×10^6 psi
800°F	23.8×10^6 psi	26.2×10^6 psi
Liner thermal expansion coefficient 0-600°F	6.66×10^{-6}/°F	6.8×10^{-6}/°F
0-800°F	7.08×10^{-6}/°F	7.2×10^{-6}/°F
0-1000°F	7.22×10^{-6}/°F	7.4×10^{-6}/°F
0.2% offset tensile yield strength at 600°F when hardened and tempered at 1025°F	300,000 psi	---
0.2% offset tensile yield strength at 600°F at R_c 53-55	---	240,000 psi
0.2% offset tensile yield strength at 800°F at R_c 53-55	---	240,000 psi
0.2% offset tensile yield strength at 600°F at R_c 50-52	---	220,000 psi
0.2% offset tensile yield strength at 800°F at R_c 50-52	---	220,000 psi

* Blanks indicate that the property is not pertinent to design.

which were not set into position until two sleeves had been shrink-fitted over the liner. This procedure caused a slight reduction in the ID of the first sleeve above and below the liner which in turn, prevented the liner from expanding axially. Compressive stresses developed by the third and fourth sleeves held the spacers firmly in position. This procedure was developed through a series of experimental trials described in the following sections of this report.

To assure that all tooling operated below the proof stress, and preferably below the proportional limit, design tensile stresses were kept below 75% of the 0.2% offset yield strength at operating temperature under an extrusion pressure overload of at least 15%. This was achieved by designing the system for operation at 207,000 psi extrusion pressure, 15% above the actual peak working pressure of 180,000 psi.

Peak permissible loading was not determined by the restriction of tensile stresses to 75% of the 0.2% offset yield strength only, since it is possible for tooling to fail in shear under some conditions without exceeding the permissible tensile load. Instead, tooling has been designed to limit octahedral shear stress to:

$$\tau = 0.707\, S_o \left[\frac{1 - S_1 + S_2 + S_3}{3 S_o} \right] = \tau_o \left[\frac{1 - S_1 + S_2 + S_3}{3 S_o} \right]$$

where $\tau_o = 0.707\ S_o$, S_1, S_2, and S_3 are the principal stresses, S_o the peak allowable stress, and τ the octahedral shear stress. This relation developed by R.G. Sturm, has been successfully used in the design of extrusion container assemblies on the Air Force heavy press program. *

Assumptions must also be made in regard to axial stress and strain behavior in order to enable calculations of these quantities. In accordance with the most recent container design practice used on the Air Force heavy press program, axial strain due to shrink stress has been taken as zero for areas where high shrink stresses are present. Axial stress is taken as zero where low shrink stresses are present. In the present design, axial strain has been assumed to be zero for the liner and for the first four sleeves. Axial stress has been assumed zero for the container sleeve and the container.

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* Kaiser Aluminum and Chemical Corp, and Sturm and O'Brien, "Failure Analysis, Modification, and Redesign of Containers for the Loewy 8100-Ton Extrusion Presses," Final Report, March 31, 1958, Book II, Sponsored by U.S.A.F. Contract AF 33(600)-34645.

An operating temperature of 600°F for the liner and the supporting tooling appears to be preferable to 800°F for two reasons--the supporting steel sleeves are 15% stronger at 600°F than at 800°F; and sleeve design for operation at 800°F would place high compressive stresses on the liner and high tensile stresses on the sleeves (at room temperature) if the thermal expansion coefficient of the ceramic liner were appreciably less than that of the supporting steel sleeves. Since most ceramics have a considerably lower thermal expansion coefficient than steel, this is a likely effect. Design for 600°F operation results in a lower stress level in the container and sleeves at room temperature. Alternately, liner materials possessing lower thermal expansion coefficients may be used in a 600°F design which could not be used in an 800°F design.

Assembly of shrink-fitted cylinders requires sufficient clearance between hot and cold pieces, and a means of rapidly and accurately inserting the cold cylinder inside the hot cylinder. Otherwise, premature seizure may result when an assembly attempt is made. Three procedures were employed to facilitate this assembly. First, shrink fits were designed to make refrigeration of cold sleeves unnecessary. Cold sleeves were at room temperature; hot sleeves at 1025°F. (This upper temperature limit is fixed by the temperature of the steel used.) Secondly, a minimum clearance of 0.002 in/in of sleeve radius was maintained, up to a maximum of 0.006 in. Finally, a heating and transfer system was designed which first heated the outer sleeve then rapidly and accurately transferred the cold sleeve into the hot sleeve.

2. Liner and Sleeve Machining and Shrink-Fitting Data

In practice, the sleeve ID is precision machined before being shrink-fitted. OD is rough machined, with grinding stock allowed. After shrink-fitting, OD is ground to size. Machining data are given in Table III. The unstressed OD is increased by 0.0120 in. over calculated size to allow grinding stock for finishing. Machining tolerance is ±0.0004 in., except as noted.

Machining dimensions are calculated for a temperature of 75°F. Since liner and sleeves have different thermal expansion coefficients, machining at a different ambient temperature will develop a different shrink stress in sleeves when they are fitted. Hence, machining temperature should be held as close to 75°F as is practicable.

Shrink-fit temperature for all sleeves is 1025°F. A room temperature of 75°F is assumed in calculating shrink fits. Sleeve clearances at assembly temperatures are sufficiently high to permit a ±30°F change in room temperature without significantly increasing difficulty of assembly.

Stresses throughout liner, sleeves, and container due to shrink fitting and extrusion as a function of radial distance from liner axis are shown in Figure 29 in the Appendix.

TABLE III

MACHINING DIAMETERS OF LINER AND SLEEVES

FOR FOUR-SLEEVE ASSEMBLY

Part	ID, in.	OD, in.	Final Size of Stressed OD, in.
Liner	3.6000	4.1250*	---
1st Sleeve	4.0918	4.6218**	4.6228
2nd Sleeve	4.6012	5.1332**	5.1384
3rd Sleeve	5.1132	5.6452**	5.6542
4th Sleeve	5.6274	6.6322**	6.6220

* Grinding tolerance of ± 0.001 in. for ceramics

** Tolerance of ± 0.002 in.

Procedures for calculation of all plotted stresses and tabulated machining dimensions are given in the Appendix, under "Design 1." Procedures are given in a step-by-step manner to permit design calculations to be made routinely, without requiring any knowledge of mechanics on the part of the designer. "Plug-in" formulas are supplied to permit design of any size of extrusion container.

D. Tooling Design for Support of Ceramic-Coated Steel Liners and Elevated Temperature Metal Liners

1. Design Characteristics

In this case, tooling has been designed for an extrusion pressure of 210,000 psi, 16.7% above the peak operating pressure of 180,000 psi. The 210,000 psi extrusion pressure generates a 224,000 psi peak tangential stress in the liner. A -112,000 psi tangential compressive stress is placed on the liner wall, to cause the liner to operate between -112,000 and +112,000 psi. Tangential elastic strain of the liner wall will then range between -0.38% and +0.38%. Accordingly, the peak tensile and compressive operating stress requirement of a liner is +112,000 psi, and the peak elastic strain capability of a coating becomes $\pm$0.38%.

This design requires the use of elevated temperature metal liner materials whose thermal expansion and elastic modulus coefficients are within +10% of that of the supporting steel sleeves. This permits the use of several materials which appear to have requisite mechanical properties at 1200°F. Ceramic coatings must, of course, have thermal expansion coefficients which are compatible with the base material. Considerable information is available on compatible coatings for H-13 steel. Since this tool steel has suitable properties for liner use, it was selected as the base material for coating application.

A half-scale assembly drawing of the support tooling used to generate the desired compressive stress in the liner is shown in Figure 3. It can be seen that this assembly has three shrink-fitted sleeves, in contrast to the four-sleeve assembly shown in Figure 1. Since less compressive stress is required on a metal liner than on a ceramic liner, three sleeves are sufficient for the purpose. Container sleeve and container design are the same as used in the solid ceramic tooling design shown in Figure 2. Clearance between the third sleeve and the container sleeve is maintained at 0.0001-0.0005 in., at operating temperature of 600°F. This permits rapid removal of liner and three-sleeve assembly in the event of liner failure.

Since compressive stress on the liner is moderate, a sleeve material could be used which possessed lower tensile properties than the HTB-2 steel. H-13 tool steel appeared well suited for this application. Pertinent mechanical and physical properties of this steel are given in Table II. As in the case of ceramic support tooling, design tensile stresses were kept below 75% of the 0.2% offset yield strength (at operating temperature), under a 15% extrusion pressure overload, and octahedral shear stress was limited to

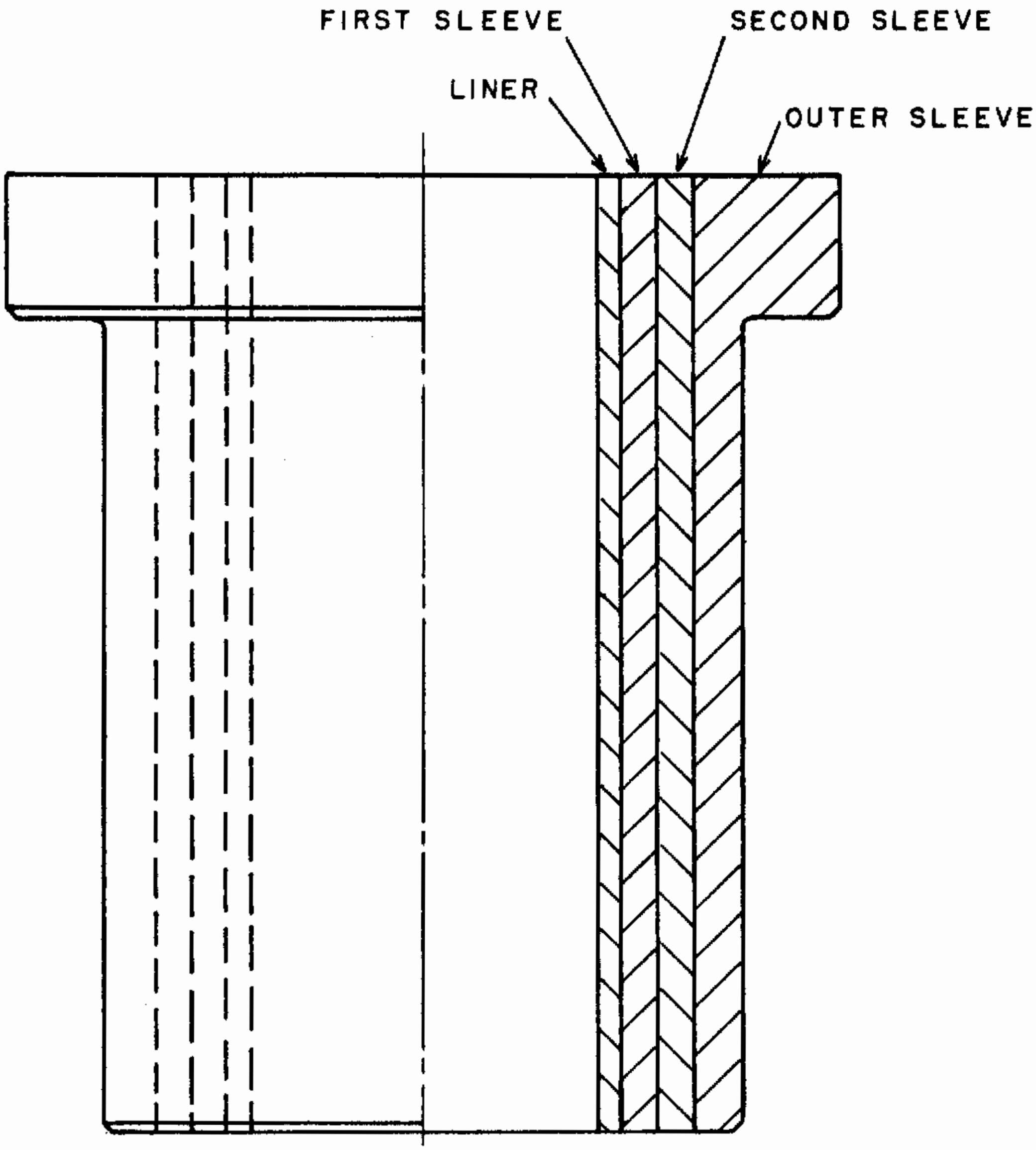

FIG. 3 CERAMIC COATED METAL AND ELEVATED TEMPERATURE METAL LINER-SLEEVE ASSEMBLY.

values calculated by the Sturm criterion. Similar assumptions were also made concerning axial stress and strain behavior of the supporting sleeves.

It should be noted that the calculated liner and support tooling shrink stresses do not depend on the container temperature, in contrast to the ceramic support tool design. This is because liner and sleeves have approximately the same coefficient of thermal expansion. Container temperature was held at 600°F to permit comparative behavior studies with the solid ceramic liners when this action appeared desirable.

Similar clearances are maintained between hot and cold cylinders to be shrink-fitted in both solid ceramic and ceramic-coated liner designs. Since less compressive stress is generated in the latter design by shrink-fitting, sleeves need not be heated to as high a temperature. In this case, cold sleeves were at room temperature; hot sleeves were below 850°F. This relatively low upper temperature limit made it possible to use sleeves which were hardened to Rc 53-55. This hardness had a sufficiently high accompanying tensile strength to permit employment of the same degree of design conservatism used with the solid ceramic tooling, even though only three supporting sleeves were used.

2. Liner and Sleeve Machining and Shrink-Fitting Data

In practice the sleeve ID is precision machined before being shrink-fitted. The OD is rough machined, with grinding stock allowed. After shrink-fitting, the OD is ground to size. Machining data are given in Table IV. The unstressed OD is increased by 0.0120 in. over calculated size to allow grinding stock for finishing. Machining tolerance is -0.0000, +0.0004 in., except as noted.

Ambient temperature value is of no importance in these machining operations, but it must remain reasonably constant. For example, a 10°F change in temperature will cause the OD of the third sleeve to change by 0.00048 in. This is greater than the tolerance allowed. Temperature change effects are not as pronounced on the smaller diameter sleeves.

A room temperature of 75°F is assumed in calculating shrink-fit temperature. Sleeve clearances at assembly temperatures are sufficiently high to permit a ± 30°F change in room temperature without significantly increasing the difficulty of assembly.

Stresses throughout liner, sleeves, and container due to shrink-fitting and extrusion are shown in Figure 33 in the Appendix, as a function of radial distance from the liner axis.

Procedures were calculated for all plotted stresses and tabulated machining dimensions are given in the Appendix, under Design 2. Section 14 in the Appendix shows that liner ID will contract 0.0156 in. due to shrink stress. This contraction, and the thickness of any coating placed on the liner wall, must be considered in establishing die diameter, since dies fit inside the liner.

TABLE IV

MACHINING DIAMETERS AND SHRINK-FIT TEMPERATURE OF LINER AND SLEEVES FOR THREE-SLEEVE ASSEMBLY

Part	ID, in.	OD, in.	Final Size of Stressed OD, in.	Shrink-Fit Temperature, °F
Liner	3.6000	4.1370*	----	----
1st Sleeve	4.1172	4.8898*	4.8800	845
2nd Sleeve	4.8690	5.6294*	5.6200	730
3rd Sleeve	2.6030	6.6278*	6.6194	795

*Machining tolerance is ± 0.002 in.

IV. DESIGN AND DEVELOPMENT OF EQUIPMENT FOR HEATING AND ASSEMBLY OF SHRINK-FITTED COMPOUND CYLINDERS

A. Necessary Equipment

A review of dimensions of the supporting sleeves for both solid ceramic and ceramic-coated liner use shows that all but the outermost cylinder of such assemblies have relatively thin walls in comparison to their inner diameters. Such cylinders will cool rapidly when removed from a heating furnace, because of the relatively large surface-to-volume ratio which they possess. Hot-cold clearances are close. None exceed 0.012 in. in diameter. In some cases, clearances are as low as 0.007 in. on a 4 in. ID. There is no possibility of increasing clearances without either tempering the steel by overheating, or else reducing the compressive stress generated by the shrink-fit.

Since clearances are close and cooling times are rapid, some means must be used to rapidly and accurately transfer a cold cylinder into a heated cylinder. Accordingly, a machine was developed for this purpose, capable of semiautomatically transferring and assembling cylinders of all sizes required by this project.

B. Design and Operation of Equipment

An assembly drawing of the first equipment design is shown in Figure 4. The sleeve to be heated is placed over a Monel metal block containing cartridge heaters rated at 6 kw. The sleeve to be inserted in the heated sleeve is held in a jig well above the Monel block. The heater block heats the surrounding sleeve to the desired temperature. The block is then dropped out of the sleeve. Next, the sleeve to be inserted is dropped into the heated sleeve, occupying the space formerly taken by the heating block.

C. Tests and Modifications

Tests of the shrink-fit assembly device showed that the basic design was sound, but that modification would be required. The heater block and liner sleeve assembly transfer system operated satisfactorily. Use of a proportional temperature controller enabled $\pm 5°F$ control at 1000°F, even though three 2-kw heaters were used to heat the relatively low thermal mass heater block.

However, the heat transfer characteristics from heater block to heating sleeve proved to be both inadequate and undesirable. This system was required to heat a sleeve to a peak temperature of 1025°F with a +0°F, -50°F maximum temperature differential. It was found that operation of the heater block at 1150°F developed a peak temperature of only 800°F in the upper section and generated a 160°F axial temperature gradient.

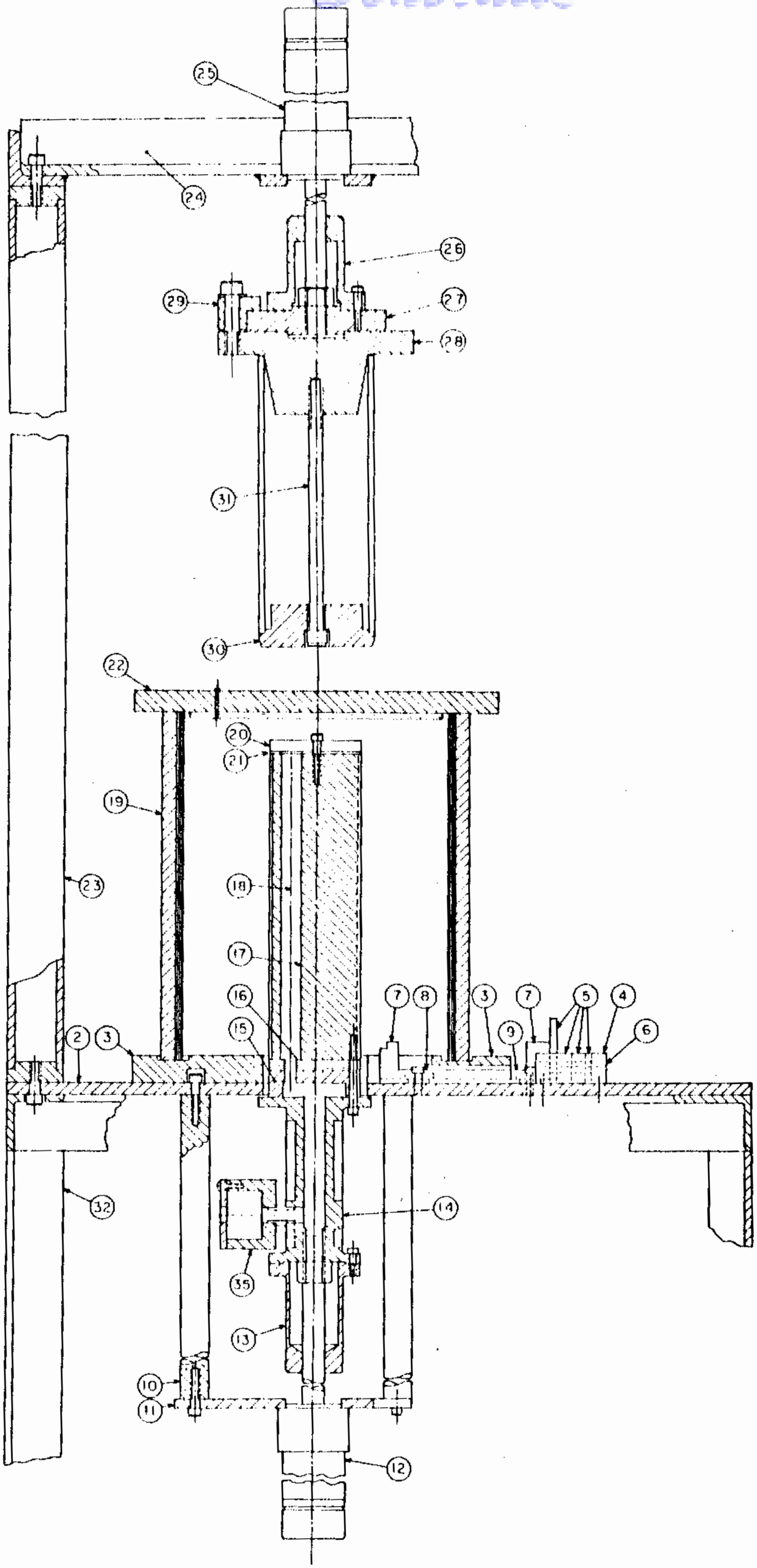

FIG. 4 SHRINK FIT ASSEMBLY DEVICE

The following procedures were carried out in an attempt to reduce this gradient and to increase the peak temperature in the liner, with the indicated result:

Procedure	Result
Replacement of 10 in. long cartridge heaters in heater block with 5 in. long cartridge heater.	Location of peak temperature zone in heating sleeve was shifted and greatly reduced. Temperature gradient reduced to $\pm 60°$F.
Axial displacement of heater block	Location of peak temperature zone in heating sleeve was shifted and slightly reduced. Temperature gradient reduced to $\pm 50°$F.
Construction of propeller fan inside furnace roof	Location of peak temperature zone in heated sleeve was slightly shifted but not reduced. Temperature gradient reduced to $\pm 50°$F.
Construction of centrifugal blower inside furnace roof	Location of peak temperature zone in heated sleeve was slightly shifted but not reduced. Temperature gradient reduced to $\pm 35°$F.

It may be seen that all of the procedures employed involved movement of air, either by convection or agitation, and that all fell short of the desired goal. Consequently, an attempt was made to improve the heat transfer characteristic by providing additional heat to the outer surface of the sleeve which was to be heated.

This activity was accomplished by use of two semicylindrical heavy-wall steel plates in place of the furnace wall insulation. Plates were vertically bored to accommodate ten 1-kw cartridge heaters, and then covered with thermal insulation on the outside. Temperature control was initially obtained by use of two separate controllers. When it was determined that the heating characteristics of the two plates were sufficiently similar, control was obtained by use of two parallel-connected thermocouples and one controller.

Use of these heater plates in conjunction with the center heater block provided a rapid and effective means of sleeve heating. Temperature gradient location and size could be controlled by axial displacement of the center heater block relative to the heater plates. Optimum placement of the center heater block (5 in. from the top of the heating sleeve) permitted sleeves to be heated to 1025°F in as little as 30 min. with a +0°, -28°F temperature gradient. Heating time was increased, but the temperature gradient remained the same when the largest diameter sleeve was heated in place of the smallest diameter sleeve.

Determination of the time at which transfer was to be effected was the next consideration. The first procedures for determination of this time utilized 4 thermocouples, spaced equidistant from top to bottom of the sleeve, and held in place against the sleeve OD by wire bands. Minimum sleeve temperature for the desired expansion was calculated. When all 4 thermocouples had attained this minimum temperature, transfer was effected. This procedure was found to be time-consuming and cumbersome. Consequently, a different means for detecting the desired expansion was developed.

First, the height of the center block heater in the heating sleeve was adjusted so that the coldest spot on the sleeve was at the sleeve base. One of the four sleeve support legs was then connected to a rod which, in turn, actuated a microswitch and signal light. Rod length could be varied by adjusting a fine-pitch screw on its end. Procedure for actuating the signal lamp at a desired expansion was as follows:

1. Sleeve to be heated was centered by adjusting three of the four support legs.

2. A shim half the thickness of the desired expansion was inserted between the fourth support leg and sleeve.

3. Expansion rod screw was adjusted until the signal light was turned on.

4. Shim was removed, and the 3 support legs adjusted for half the value of the required expansion.

This procedure worked very well. Microswitch resolution and repeatability was found to be within 0.001 in. Once the adjustment was made, the operator simply turned on the heaters, then actuated air transfer valves as soon as the signal lamp glowed. Temperature differential in the base plate connecting the fourth support leg and microswitch was equal to that of the switch actuating rod, providing self-compensation for expansion of the base plate during heating.

An assembly drawing and photograph of the modified machine is shown in Figures 5 and 6. Operating characteristics are as follows:

1. <u>Sleeve Heating Time</u>

   Varied between 30 min and 2 hr depending upon diameter and mass of liner.

2. <u>Maximum Sleeve Temperature Gradient</u>

   28°F ($\pm$14°F) in the 800°-1150°F temperature range. This gradient is obtained when the sleeve is within 150°F of the peak temperature desired, and remains constant above this point.

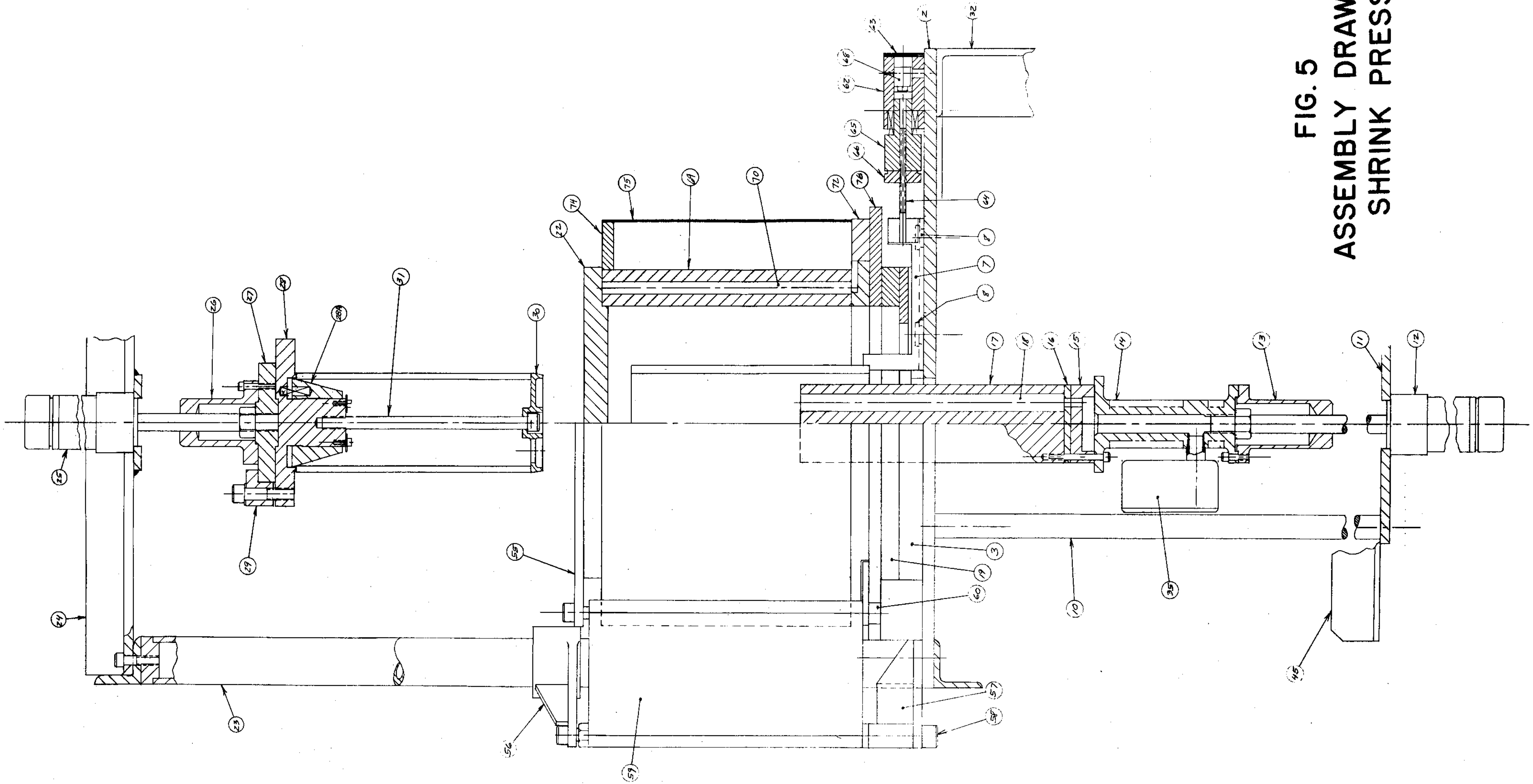

FIG. 5
ASSEMBLY DRAWING
SHRINK PRESS

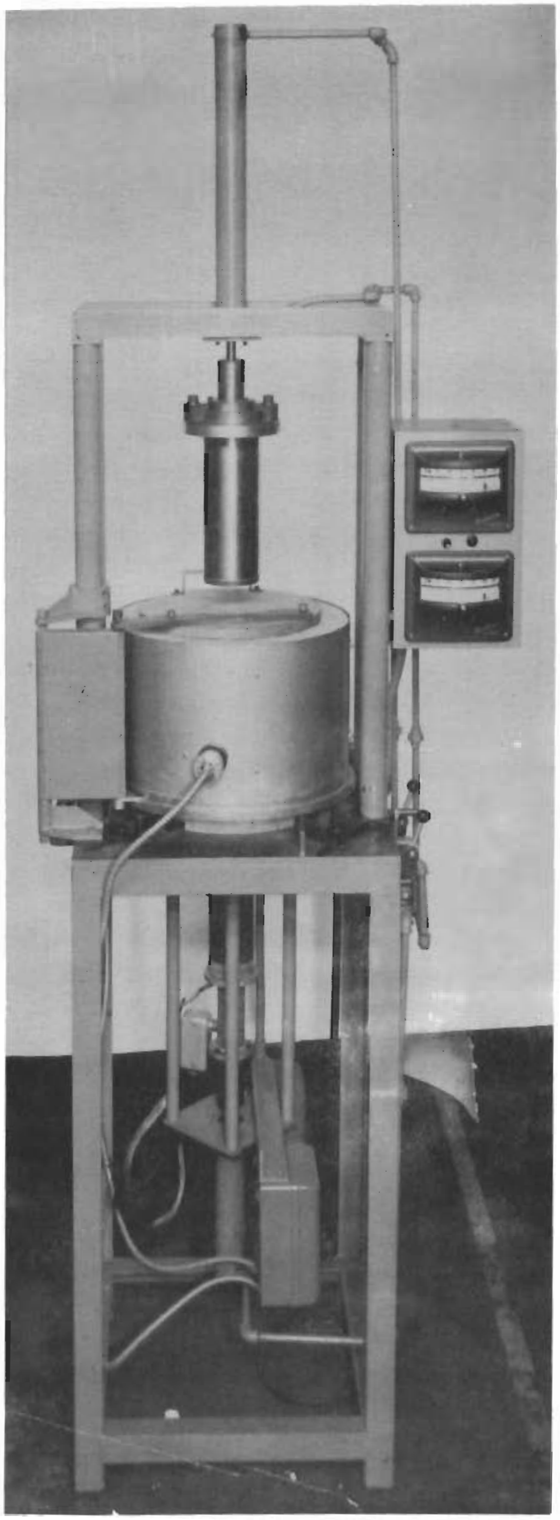

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FIG. 6 - PHOTOGRAPH OF SHRINK-FIT ASSEMBLY DEVICE.

3. Range of Expansion Sensing Adjustment

From 0.001 in. to 0.060 in. Microswitch resolution and repeatability is within 0.001 in. Diameter sensing rod of the microswitch contacts the heating sleeves at the coldest point on the sleeve, assuring that expansion sensing is made at the point of smallest diametral expansion.

The shrink-fitting procedure employed in assembly of all liner sleeve combinations is as follows:

1. Sleeve to be heated is positioned so that its centerline is directly beneath the centerline of the suspended inner sleeve assembly, or liner.

2. Microswitch assembly is adjusted to actuate a neon lamp when sleeve expands the required amount.

3. Sleeve temperature for required expansion is calculated. Both temperature controllers are set 150°F above this value, and heaters are energized.

4. When the signal lamp lights, electric power is turned off. Actuation of the lower air cylinder drops the center block heater out of the heated sleeve. Actuation of the upper air cylinder drops the cold sleeve assembly inside the heated sleeve.

5. Sleeve assembly is allowed to remain inside the furnace shell approximately 60 sec, while sleeves seize, to avoid personnel injury in event of a fracture in the outer sleeve. Liner-and-sleeve assembly is then lifted by upper air cylinder.

6. Next, sleeve to be shrink-fitted onto the assembly is placed in the heating position, and procedures (1) through (5) are repeated.

V. EQUIPMENT AND PROCEDURES FOR DISASSEMBLING SHRINK-FITTED SLEEVES

A. Necessary Equipment

The ability to replace worn liners relatively quickly and inexpensively is, of course, of first importance in a liner development program. The tooling design employed, permits a fast interchange of liner-sleeve assemblies. However, it is impractical to fabricate a set of supporting sleeves for every liner produced. Hence, some means must be employed for disassembly of support sleeves from worn or damaged liners.

Differential heating is a time-honored method of effecting separation of shrink-fitted parts. In this case, this procedure, in addition to being slow because of the number of sleeves which must be removed, is also impractical. The reason is that differential heating technique is most effective in separating shrink-fitted cylinders when the wall thickness of the inner cylinder is relatively small compared to the wall thickness of the outer cylinder. As the wall thickness of the cylinders approach the same size, disassembly by differential heating becomes progressively more difficult, finally becoming impossible. The liner support assemblies designed for this effort are of such dimensions that separation by differential heating is impossible. Consequently, another method of separation was required.

B. Description of Disassembly Equipment

The simplest way to disassemble a shrink-fitted assembly is to successively press-off the outer sleeves from the liner. This can be readily accomplished if:

1. Straight-wall cylinders are used.

2. Surface finish is 20 or better

3. OD surface of cold cylinder is coated with molybdenum disulfide lubricant before shrink-fitting.

Tapered-wall cylinders may be expected to give trouble because of difficulty in exactly mating tapering surfaces, with resultant localized surface distortion.

All support tooling designed for this activity fulfilled the three requirements for press disassembly. Accordingly, two sets of such tooling were produced, one for the three-sleeve assembly, the other for the four-sleeve assembly. A photograph of the four-sleeve disassembly tooling is shown in Figure 7. Operations for separating a liner-sleeve assembly using this tooling, proceed in the following manner:

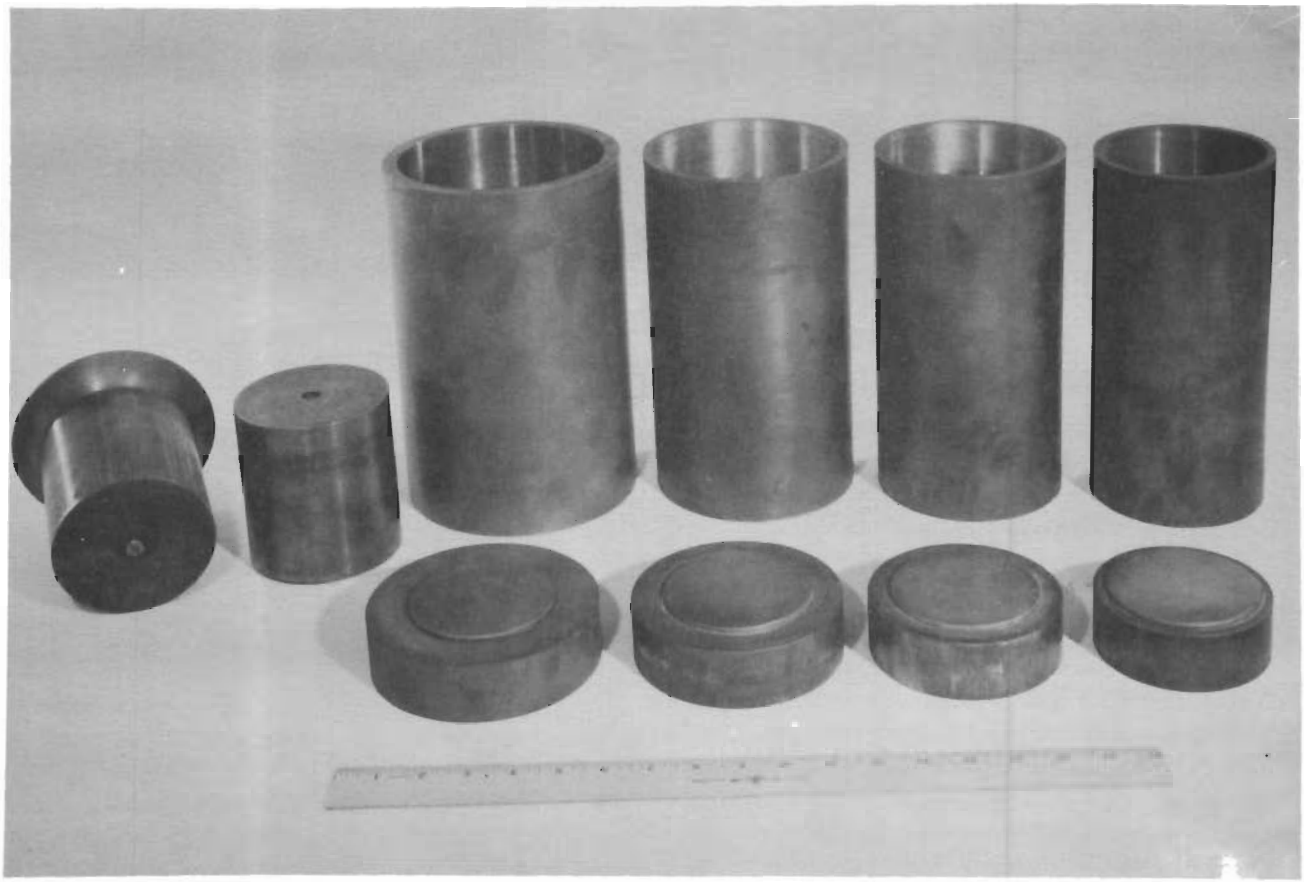

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FIG. 7 - PHOTOGRAPH OF FOUR-SLEEVE DISASSEMBLY TOOLING.

1. Liner-sleeve assembly is ejected from container by use of a jack. This jack is bolted to the front end of the shear slide, and moved into position beneath the container centerline by actuation of the slide.

2. The largest diameter sleeve shown in Figure 7 is placed inside the container sleeve. The OD of this sleeve is within 0.010 in. of the ID of the container sleeve assuring proper positioning.

3. The liner-sleeve assembly is placed on top of the pushout sleeve and centered.

4. The largest stem head shown in Figure 7 is placed on top of the liner-sleeve assembly. This stem head has a pilot-diameter machined on it to assure proper centering. The OD of this part is slightly less than the OD of the third sleeve.

5. The stem in Figure 7 is placed on top of the stem head and pressing force is exerted. The liner and first three sleeves will be pushed downward through the fourth sleeve. After a 5 in. stroke has been made, the press ram is retracted and the stem extension is inserted between stem and stem head.

6. The liner and first three sleeves drop inside the pushout sleeve in the container. The fourth sleeve, which has been separated. is removed from press. The liner assembly is lifted out of pushout sleeve. The pushout sleeve remains in the container.

7. The second-largest diameter sleeve shown in Figure 7 is placed inside the pushout sleeve remaining in the container. Steps 3 through 6 are repeated using a smaller stem head. This procedure removes the third sleeve from the liner-sleeve assembly.

8. Again, steps 3 through 6 are repeated, using successively smaller pushout sleeves and stem heads until all the sleeves have been separated from the liner.

To assure calculated values would be conservative, a friction coefficient value of 0.2 was assumed. Calculation of pressing force required for separating the sleeves is made in Section 21, Design 1, and Section 15, Design 2. Values are presented in tabular form in Tables X, XI, and XIII in the Appendix. These calculations are based on an assumption that the friction coefficient between the sleeve surfaces would be 0.2. Use of molybdenum disulfide lubricant on these surfaces apparently lowered this friction coefficient to 0.10 in., since measured sleeve separation forces were approximately 1/2 of the calculated values.

VI. HIGH STRENGTH STEM DESIGN AND FABRICATION

If a container assembly is designed to withstand a peak extrusion pressure of 220,000 psi, it would be desirable to have a stem which could withstand a similar peak pressure. This strength, unfortunately, is not readily attained. Tool steels can be hardened to develop yield strengths in excess of 220,000 psi. However, ductility decreases at such hardness levels to a point where stem failure will be brittle rather than ductile. Since serious damage to tooling and personnel may result from such a failure, normal stem loading was restricted to 180,000 psi. However, an improvement in stem design was effected which permitted a 9.6% increase in stem pressure when using a given tool steel tempered to a particular hardness.

The design improvement may be appreciated by a comparison with ordinary fixed dummy block stem design. Fixed, rather than floating, dummy blocks are used in vertical press operation to avoid the necessity of transferring the dummy block to the top of the billet immediately before extrusion. Since the dummy block may occasionally stick in the liner because of back-extruded metal or lubricant, a relatively large connecting stud is used to reduce the probability of a stud failure, and/or thread damage when the stem is retracted. The dummy block connection to a 3-1/2 in. stem would normally be made by using a 1-1/8 in. to 1-1/4 in. stud. Unfortunately, this relatively large stud provides no support to the dummy block or stem as the stem is compressively loaded during the extrusion stroke. If the stud diameter is 1-1/8 in., the load bearing area of a 3-1/2 in. stem is reduced to 89% of that of a solid stem.

Clearly, an improvement in stem load-carrying capacity requires use of a small diameter dummy block stud which will not damage either dummy block or stem threads if it is overloaded, and which may be easily replaced. Figures 8, 9, and 10 show such a stem, dummy block, and stud design. Effective stem area is increased 9.6% by this arrangement. It is noted that a vertical slot is machined in the stud. This serves two purposes, i.e., in event of dummy block sticking, the stud will collapse when the stem is retracted without damaging either stem or dummy block threads; and the slot enables the stud to be removed from either the stem or the dummy block with a screwdriver, in the event that the stud should fracture when the stem is retracted.

This design worked very well. No damage to either stems or dummy blocks was sustained during the course of the project. Peak stem pressures of 204,000 psi were exerted on H-13 steel stems hardened to Rc 50-52 without causing any perceptible stem distortion.

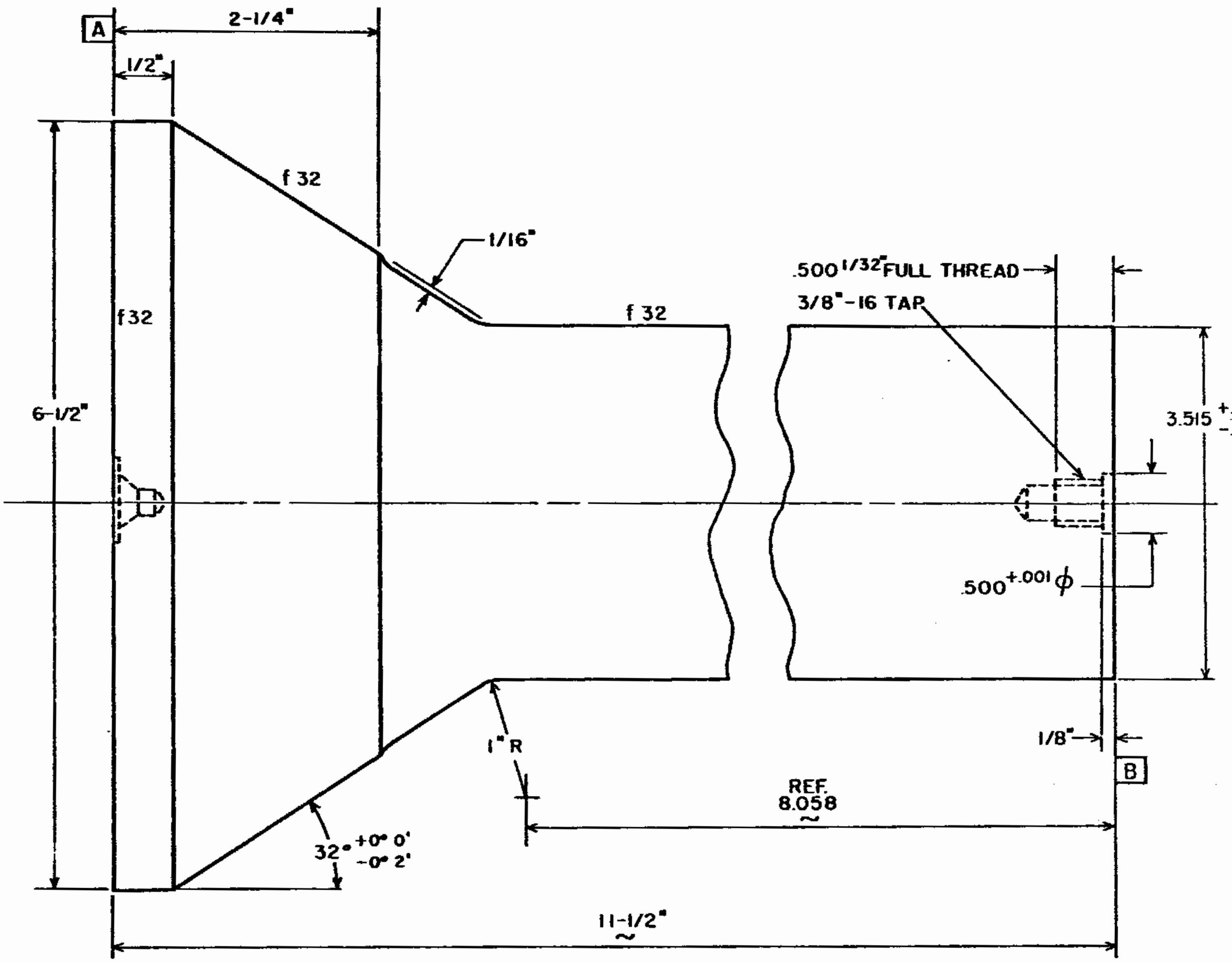

FIG. 8 3-1/2 INCH STEM
DETAIL 250
MATERIAL: H-13 TOOL STEEL Rc 50-52

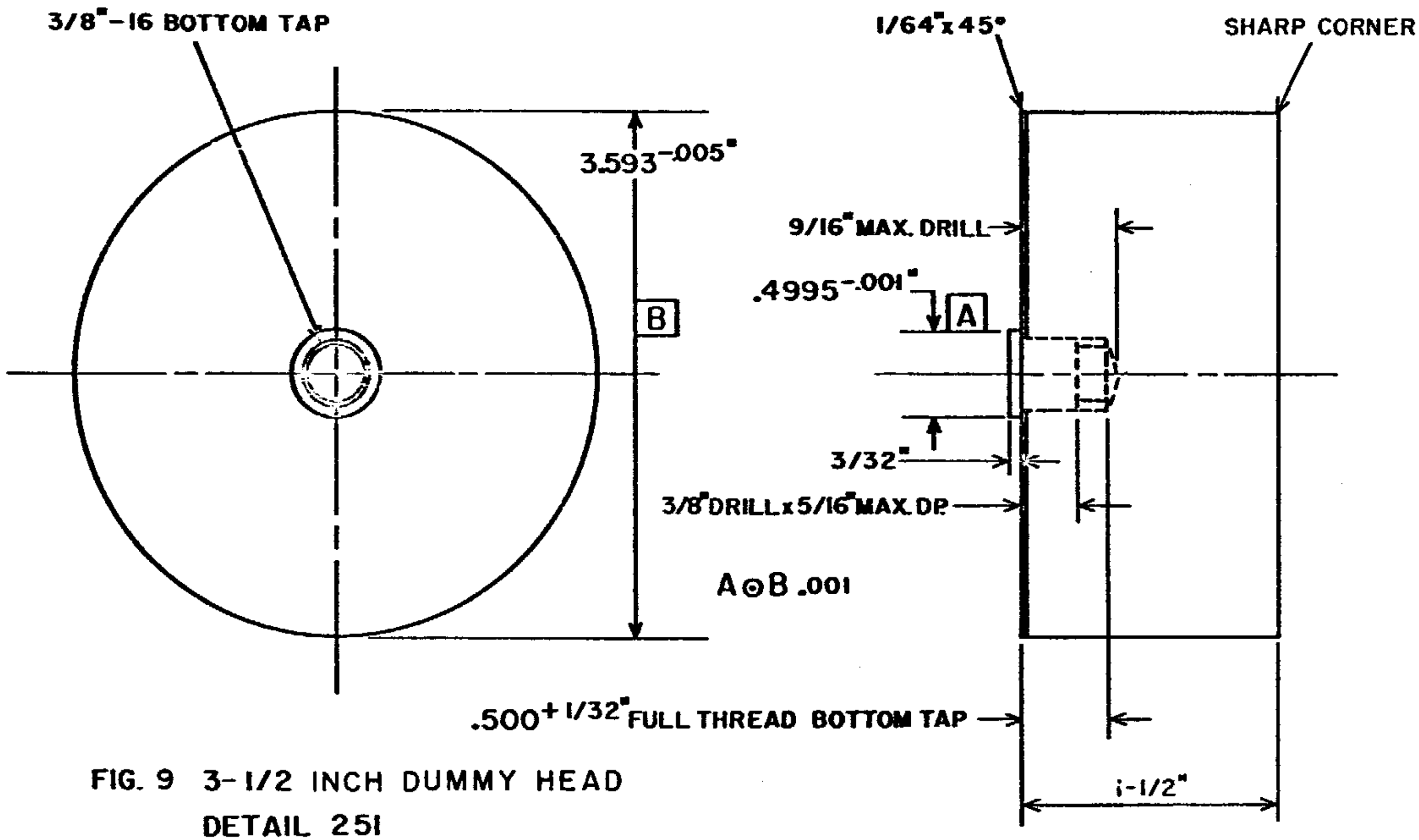

FIG. 9 3-1/2 INCH DUMMY HEAD
DETAIL 251
MATERIAL: CARPENTER T-K
Rc 51-53 (DRAW AT 1100°F)

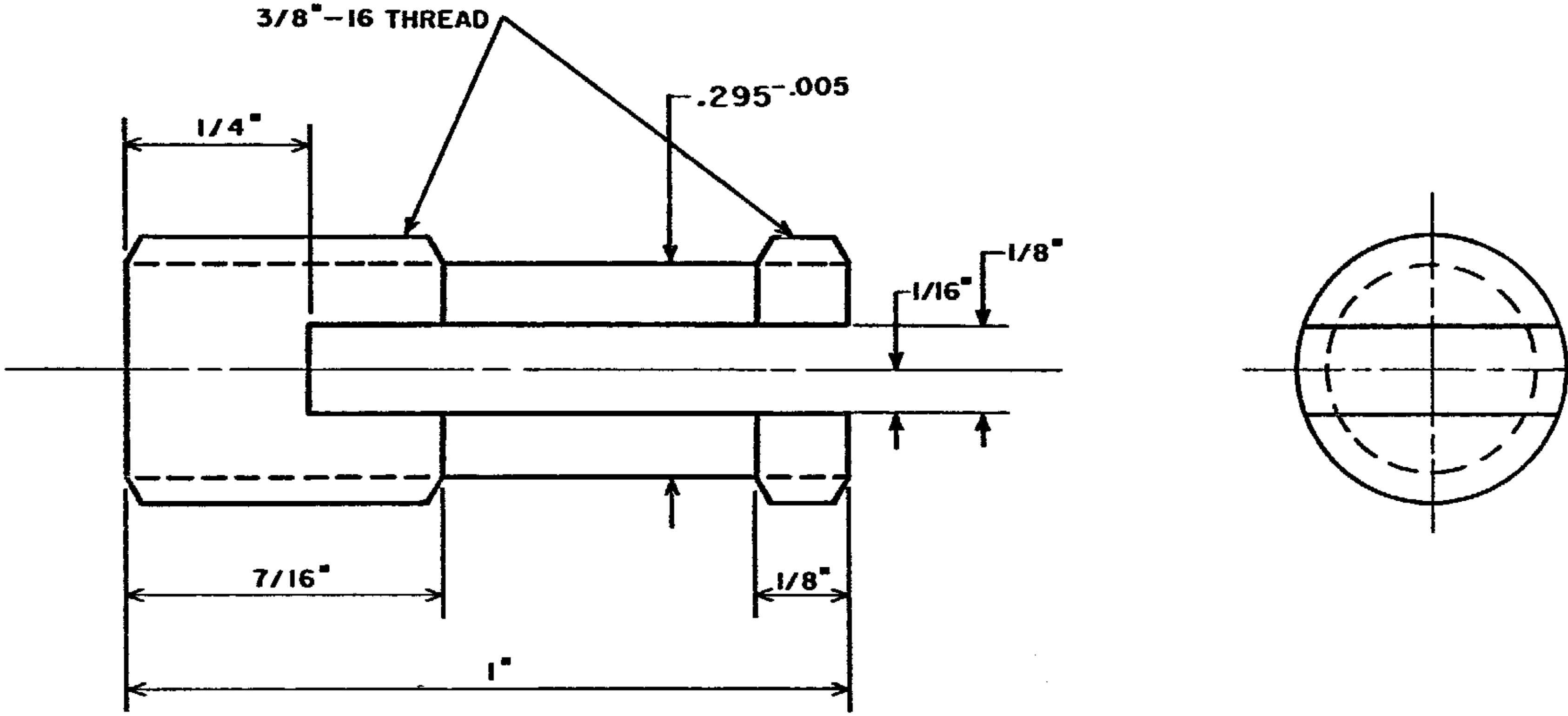

FIG. 10 SAFETY COUPLING
DETAIL 252
MATERIAL: SAE 1030 STEEL

VII. DEVELOPMENT OF MATERIALS AND MANUFACTURING PROCESSES

A. Fabrication of Nine Different Compositions of Fiber Metal Reinforced Oxides and Carbides - by N. M. Parikh and R. L. Hodson

The purpose of this work was to prepare specimens of fiber-metal oxide and fiber-metal-carbide composites for evaluation in hot extrusion tests. Because the specimens were to contact materials at temperatures of at least 3200° F, the components were selected such that their melting points would exceed these temperatures. Another basis for the selection of the specimen components was the relative chemical stability of the metal fibers on contact with the oxide or carbide matrices at fabrication temperatures, as well as at the test temperatures. Using these criteria, the systems selected were:

1. Al_2O_3, MgO, ZrO_2, and SiO_2, all reinforced with molybdenum fibers.

2. TiC, TaC, ZrC, VC, and CbC, all reinforced with tungsten fibers.

The method used to prepare the specimens involved the impregnation of fiber metal felts with fine powders of the oxides and carbides, followed by hot pressing in graphite dies. The fibers used were cut from spooled wires to approximately 1/4 in. lengths. The molybdenum wire used was 0.002 in. in diameter, and the tungsten wire was 0.005 in. in diameter. The molybdenum fibers were hand cut, and consequently had to be "kinked" in a Waring Blender to assure good mechanical interlocking when felted. The tungsten wires were cut out in a hammer mill, and the fibers were naturally kinked as a result of the action of the hammer mill.

Fibers were felted through a No. 20 screen into a glass tube of the desired specimen diameter (1 in.) and were then compacted to about 30% of theoretical density (70% porosity). The tube containing the felt was then placed in the impregnation apparatus shown in Figure 11. The apparatus consists of a vacuum flask, Buchner funnel, water aspirator, glass tube containing the felts, slurry container, and vibrator. The oxide or carbide powders (-325 mesh) were made into thin water-base slurries to which about 1% of a wetting agent (Aerosol OT) was added, and were then placed in the slurry container at the top of the glass tube. The apparatus below the slurry container was evacuated with the aspirator, and the slurries were then allowed to fill the tube to a depth of some 4 to 5 in. above the top of the felts. The vibrato was started to keep the powders suspended while the water was drawn off through the filter in the Buchner funnel. This, in effect, causes layers of powder to form at the bottom of the felts and continuously build up until the felts are completely filled with the powders.

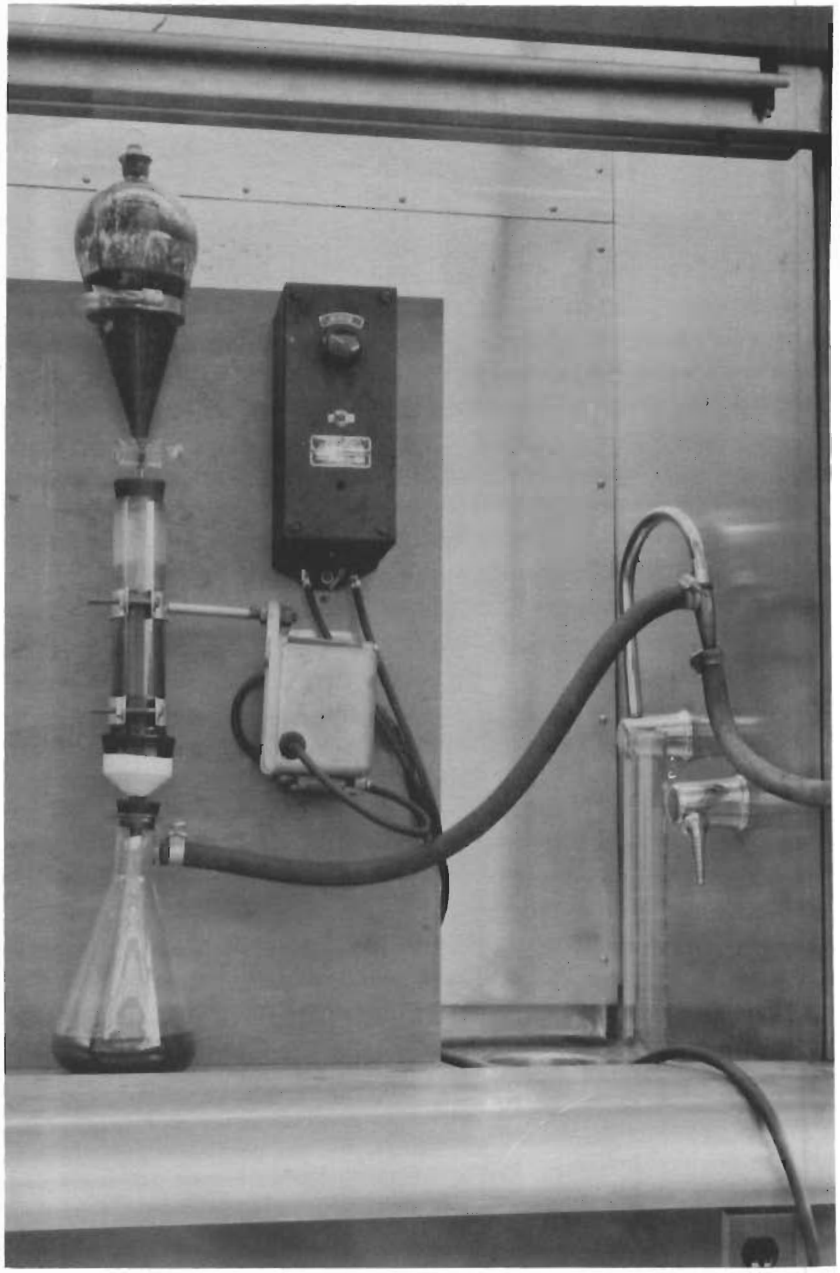

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FIG. 11 - PHOTOGRAPH OF IMPREGNATION APPARATUS

Impregnated felts were then dried and transferred to graphite pressing dies which, in turn, were induction heated. Oxide compacts were heated to 4000°F and carbide compacts to 4250°F. When the hot pressing temperatures were reached, pressing pressures of 3000 psi were applied in all cases.

The specimens which were prepared are listed in Table V. Because each specimen was prepared individually, it was not possible to maintain close compositional control within each set. Large variations are to be found in sets No. 1 and 8. These are due to the somewhat erratic impregnation behavior of the materials Al_2O_3 and VC. The amounts of these materials accepted by the felts were found to vary by 8 to 12 volume per cent for reasons which are not clear at this time.

B. Investigation of the Feasibility of Fusing Short, Hollow, Ceramic Cylinders of High-Strength Carbides and Borides to Form a Ceramic Extrusion Liner of Suitable Length

Although the Norton Company has developed experience in the fabrication of many different parts from the materials of interest, the size and shape of the required extrusion liners did present a fabrication problem. Hollow cylinders of 1/4 in. wall, 3-1/2 in. ID, made of zirconium carbide, titanium diboride, and titanium carbide, could be produced in 4 in. lengths only. This limitation was due to the sintering properties of the materials, not available tooling. It did, however, appear possible to the company's laboratory division that the 4 in. cylinders could be satisfactorily fused to produce an 8 in. cylinder which would be monolithic, as far as compressive strength and thermal shock properties were concerned. Accordingly, arrangements were made with the Norton Company to perform a series of experiments and mechanical evaluations which would determine the practicability of producing extrusion liner cylinders by fusing 4 in. cylindrical lengths. Experiments were carried out on carbides representative of the class selected and on titanium diboride. Solid rounds were used in these tests.

Experimental results were encouraging initially. Techniques were developed for welding all three of the materials under consideration. Bend tests showed that the welded section possessed satisfactory strength. However, when the same techniques were applied to the production of the required hollow cylinders, only zirconium carbide cylinders could be welded.

The effect of an interface on the combined shrink and extrusion stresses is not readily calculable. Since it could not be categorically stated that this interface would substantially weaken either liner or supporting sleeves, it was decided to attempt to use two 4 in. cylindrical lengths, for both titanium diboride and titanium carbide ceramics, rather than abandon efforts using such materials.

TABLE V

LIST OF SPECIMENS PREPARED BY HOT PRESSING OF IMPREGNATED FIBER FELTS

Set No.	Specimen No.	Composition Volume, %	Composition Weight, %	Hot Pressing Temperature, °C	Hot Pressed Density, % of Theoretical
1	1	$72Al_2O_3$-28Mo	$48.5Al_2O_3$-51.5Mo	1600	100
	2	$59.5Al_2O_3$-40.5Mo	$36.5Al_2O_3$-63.5Mo	1600	93
2	3	74MgO-26Mo	50MgO-50Mo	1600	100
	4	74MgO-26Mo	50MgO-50Mo	1600	98
3	5	$84ZrO_2$-16Mo	$74ZrO_2$-26Mo	1600	87
	6	$83ZrO_2$-17Mo	$72ZrO_2$-28Mo	1600	86
4	7	$91SiO_2$-9Mo	$70SiO_2$-30Mo	1600	93
	8	$89SiO_2$-11Mo	$65SiO_2$-35Mo	1600	88
5	9	82TiC-18W	49.5TiC-50.5W	2200	87
	10	78TiC-22W	46.4TiC-53.6W	2200	94
6	11	76TaC-24W	72TaC-28W	2350	89
	12	76TaC-24W	72TaC-28W	2350	88
7	13	75.5ZrC-24.5W	52ZrC-48W	2350	90
	14	79.5ZrC-21.5W	55.5ZrC-44.5W	2350	85
8	15	74.5VC-25.5W	45VC-55W	2200	100
	16	66.5VC-33.5W	35.5VC-64.5W	2200	97
9	17	77CbC-23W	57.8CbC-42.2W	2350	84
	18	74.5CbC-25.5W	54.1CbC-45.9W	2350	83

C. Determination of Elastic Modulus Coefficients of Solid Ceramics at Room Temperatures and at 600°F

Design of suitable support tooling for solid ceramic liners requires a knowledge of values of both linear thermal expansion coefficient, and Young's modulus, at room temperature and 600°F. (See Specific Procedure section in Appendix). Information on linear expansion coefficient was available in the literature and from the ceramics manufacturers. However, no accurate values of Young's modulus coefficient for titanium diboride, zirconium carbide, titanium carbide, or Lucalox could be obtained. Manufacturers did not possess this information on their products. Values reported in the literature varied ±25%. Since the accuracy of liner and supporting sleeve stress calculation is directly proportional to the accuracy of elastic modulus determination, a possible 50% error in elastic modulus was obviously unacceptable.

Accordingly, arrangements were made to experimentally determine the Young's modulus coefficient for the solid ceramics of interest.

Test specimens of suitable dimensions produced from the same material used in manufacturing the ceramic cylinders and by similar production methods, were procured from the ceramic manufacturers. Actual test work was carried out at the College of Ceramics of Alfred University, utilizing special equipment for determining Young's modulus in brittle materials. Information obtained from these measurements is listed in Table VI.

Thermal cycling did not affect Young's modulus values of ZrC, and is known not to affect the Lucalox modulus. Relatively large shifts in the modulus values of titanium diboride and titanium carbide did occur, however. The titanium diboride modulus dropped 15.9% after thermal cycling, while the titanium carbide modulus rose 26.4%. No further change was noted on subsequent cycling. Hence, reported values are believed to be stable. Reproducibility of TiB_2 data was ±5.5%, possibly due to a small porous area in the sample. Reproducibility of zirconium carbide and titanium carbide data was ±1.5%.

D. Manufacture of Solid Ceramic Liners

Unfortunately, both the Norton Company and General Electric Company ran into manufacturing problems in the production of zirconium carbide, titanium diboride, titanium carbide, and Lucalox cylinders of the size required for extrusion use. Delivery of the carbide and diboride items was not made until the project was almost over, preventing evaluation of these materials on the present contract.

The General Electric Company could not produce a satisfactory Lucalox cylinder of the size required. Several attempts were made by G. E. to make this part over a 15-month period, at the company's expense. All efforts were unsuccessful.

TABLE VI

YOUNG'S MODULUS AT DIFFERENT TEMPERATURES FOR VARIOUS CERAMICS

Temperature, °F	Young's Modulus, x 10^6 psi			
	TiB_2	ZrC	TiC	Lucalox
76	68.4	46.4	59.4	
100		46.3		
163	68.0			
179			59.1	
200		46.1		
259	67.9			
273			58.8	
300		45.8		
379	68.0		58.4	
400		45.7		
500		45.5		
545	67.0			
600		45.3		53.0
633			57.6	
687	66.3			

Consequently, the experimental work with solid ceramic liners was limited to solid alumina. High-quality, accurately ground cylinders of this material were produced by the International Pipe and Ceramics Company without apparent difficulty. Improved manufacturing capability would be expected with solid alumina, because the state of the manufacturing art for alumina is considerably advanced over that of the other materials of interest.

E. Procedures Employed in Production of Liner Support Sleeves

1. H-13 Tool Steel Sleeves for Support of Ceramic-Coated and Superalloy Liners

All sleeves were machined from annealed solid round, leaving a 0.030 in. grinding allowance on both OD and ID. Processing was as follows:

a. Heat-treat to Rc 53-55 hardness

b. Grind ID to tolerance

c. Shrink-fit the first sleeve on an H-13 steel liner

d. Grind OD of stressed first sleeve to size

e. Repeat steps (c) and (d) for second and third sleeves.

This procedure required considerably more time than grinding sleeve OD to size before shrink-fitting. However, attainment of desired interferences was assured. Measurements of stress sleeve dimensions and disassembled finished sleeve dimensions did establish that sleeve expansion calculations were accurate within 0.002 in. (See sections 11c, 12c, 13c, in Design 2, of the Appendix). However, the "shrink-and-grind" procedure is still preferred because it avoids cumulative tolerance errors.

2. HTB-2 Tool Steel Sleeves for Support of Solid Ceramic Liners

Processing for the HTB-2 steel sleeves was similar to that of the H-13 steel sleeves, with the exception of the heat treatment required. Since these sleeves were used for support of solid ceramic liners, maximum mechanical strength was required. The heat treatment required for the development of such properties was to austenitize at 2050°F, air cool, and double temper at 1025°F. An Rc 64-64.5 hardness was expected, but not obtained. Hardness values of Rc 59-62 were obtained instead. Austenitizing temperature was increased to 2100°F with the approval of the vendor. Approximately 1.5 points of hardness were gained by this procedure. Tests of a tensile bar which had been heat treated with the sleeves showed that the desired mechanical properties had been obtained. A 0.2% offset yield strength of 330,000 psi and an elongation of 6% at the use temperature of 600°F were recorded. Therefore, it was concluded that the heat treatment had been satisfactory. Succeeding events proved that this was not the case.

Two failures of shrink-fitted sleeves occurred within a 2-month period. The first failure occurred within 2 hr of shrink-fitting a second sleeve onto a solid ceramic liner. The second failure occurred 3 to 5 days after shrink-fitting of a second sleeve onto a ceramic-coated H-13 steel liner, at a relatively low stress level.

Shrink-fitting of a uniformly cylindrical first sleeve on a solid ceramic liner proceeded without incident. After the assembly cooled to room temperature, it was externally ground in preparation for shrink-fitting of the second sleeve on this assembly. The second sleeve was then shrink-fitted on the assembly without apparent difficulty. However, two hours later, when the assembly had cooled to approximately room temperature, the second sleeve burst. Figures 12a and 12b show the crack pattern on the outside and inside of this sleeve. It may be seen that the sleeve had a flange at one end, in contrast to the sleeves used for this effort to date, which were all uniformly cylindrical.

Macroscopic examination of the cracked surfaces, coupled with the observed location of the broken parts, established that fracture had initiated in the flange corner. (Fracture initiation at this point would be most likely in any event, because of the stress-magnification characteristic of the flange corner.)

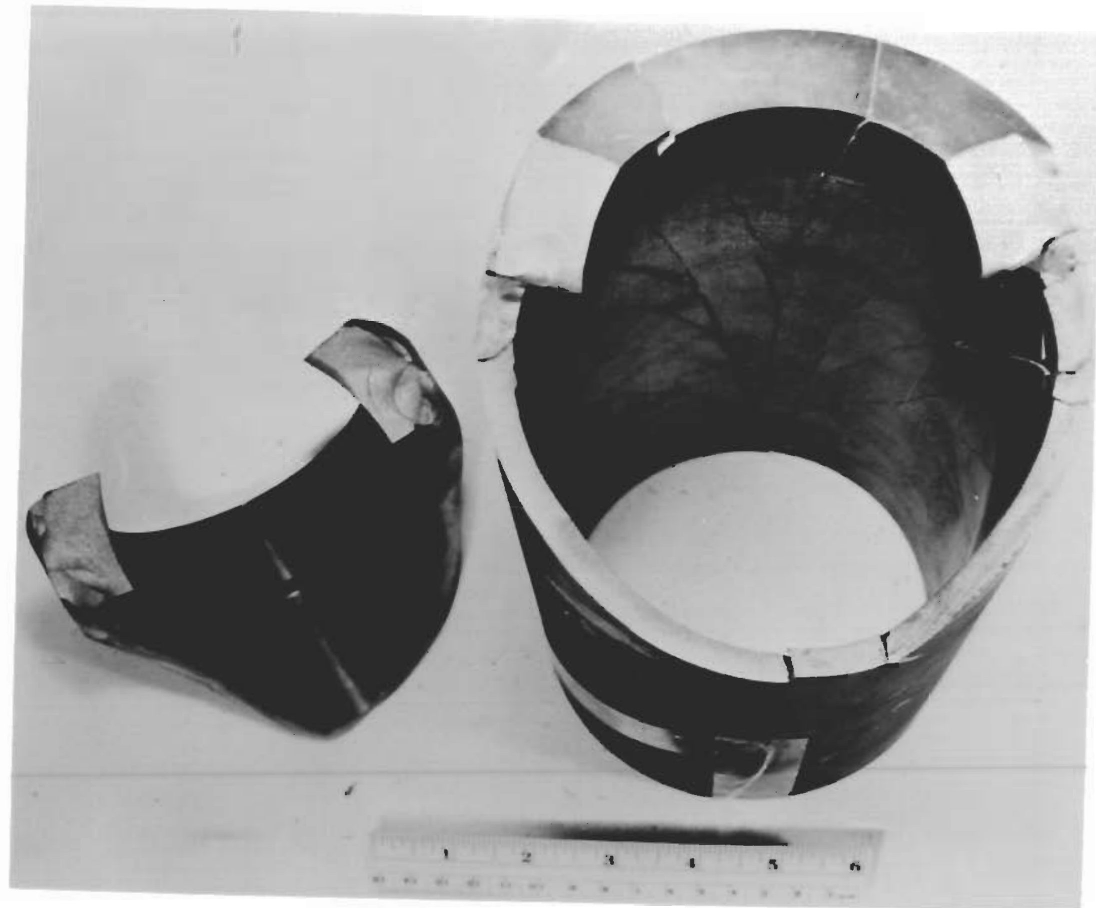
Such an event had been considered possible, but improbable. Expansion of this sleeve by shrink-fitting would be expected to cause a contraction in axial length which, in turn, would generate a couple when the flange pressed against the liner-first sleeve combination. No estimate of the magnitude of this couple could be made because its value would be dependent on the amount of sliding between the first and second sleeve. Increased sliding would reduce the value of the couple at the flange. The first sleeve outer surface was coated with molybdenum disulfide lubricant prior to its transfer into the second, to reduce friction between the two surfaces and promote such sliding.

Since the effort was apparently unsuccessful, the second sleeve was redesigned as a straight cylinder. A separate assembly made from shrink-fitted solid alumina and HTB-2 steel rings, clamped to the top of the liner-first sleeve assembly during second sleeve shrink-fitting operations, served to fill in the area previously occupied by the flange and to support successive sleeves. Fabrication, shrink-fit assembly, and external grinding of the new parts proceeded without incident.

All sleeves for support of ceramic-coated liners and superalloy liners had been initially made of H-13 steel. It was found that the H-13 steel sleeves would hold a hardness level of Rc 51 when used with either Rokide-process or plasma-arc sprayed coatings, but would temper when superalloy

Neg. No. 25451

(a) Outside



Neg. No. 25452

(b) Inside

FIG. 12 - CRACK PATTERNS ON SLEEVES

liners were used for TZM alloy extrusion at temperatures over 3450°F. This effect was due to the higher thermal conductivity of the liner surface and the relatively thin liner wall used. Since HTB-2 steel is far more temper resistant than H-13 steel, it appeared desirable to fabricate the first two support sleeves from HTB-2. Substitution of HTB-2 for H-12 steel in the first sleeve was mandatory if superalloy liners were to be given a fair test. Accordingly, first and second replacement sleeves were fabricated from HTB-2 steel and shrink-fitted on a liner which had a plasma-arc coating.

The effort proceeded without incident, until the second sleeve had been shrink-fitted. Two days after the sleeve was shrink-fitted, grinding operations were started on the OD of the assembly. This work was not completed by the end of the week, and, accordingly, was left in the grinding machine over the weekend. This second sleeve exploded sometime between Friday afternoon and Monday morning. Force of the explosion broke a 1-1/2 in. piece out of the grinding wheel, and tore the coolant guard from the machine. Fractured sections of the sleeve indented the concrete floor 6 ft from the machine. This failure was particularly disturbing because:

(a) Peak tangential stress on the sleeve was only 50,000 psi--much lower than the 150,000 psi stress developed in sleeves shrink-fitted on solid alumina liners.

(b) Failure was apparently time dependent, which could make shrink-fitting and grinding operations dangerous at any time.

(c) Failure during grinding could be fatal to the machinist.

All shrink-fitted assemblies using HTB-2 steel sleeves were immediately disassembled. Each sleeve was checked visually for cracks and was checked with the aid of magnaflux and Zyglo techniques. Samples of the broken second sleeve were sent to the producing mill for metallurgical evaluation. A procedure was developed by IITRI's metallographic group for the polishing and metallographic examination of the ends of the relatively large sleeves.

Results of this failure determination activity indicated that the difficulties experienced were largely, if not totally, due to improper heat-treatment specification and poor control practice on the part of the heat treater. Austenitizing temperature was originally specified at 2050°F. When difficulty in hardening was encountered, the mill changed the austenitizing temperature to 2100°F. The heat treater apparently had a $\pm 50°F$ variation in the austenitizing furnace on successive runs, with the result that austenitizing temperature ranged from 2050° to 2150°F.

The probable cause for hardening difficulty at an austenitizing temperature of 2050°F was the solution of carbides in the austenite which, in turn, resulted in retention of an excessive amount of austenite after quenching. Hence, the 2050°F austenitizing temperature was too high, not too low. An increase in the austenitizing temperature increased the carbide solubility of the austenite, making austenite transformation much more difficult.

Tempering of the quenched structure did temper what martensite had formed, but did not serve to produce fresh martensite until the structure cooled. The resultant structure then consisted of tempered martensite, brittle martensite and retained austenite, a mixture that was both brittle and weak. Wide variations in the amount of retained austenite in different sleeves indicated a wide variation in austenitizing temperature.

Failure probability under load, in this undesirable structure was increased by a number of short, hairline cracks in one end of some of the cylinders. These were apparently caused by the heat treater setting the sleeves on end on a cold surface after removing them from the austenitizing furnace. Examination of fractured surfaces showed that propagation of one of these cracks was the direct cause of fracture of the sleeve.

Since the microstructure of the metal could be improved by a relatively low temperature heat treatment without greatly distorting the sleeves, it appeared worthwhile to attempt to salvage these sleeves. Accordingly, all parts were re-heat treated to develop an acceptable structure possessing maximum hardness. This was accomplished by heating to 1025°F, holding 10 min to 1 hr, then air cooling. Hardness was checked before and after each heat treatment. Holding at 1025°F tempered the brittle martensite that remained in the structure. Cooling to room temperature caused more of the retained austenite to transform to brittle martensite.

In all cases, sleeve hardness first rose, then fell with successive heat treatments, as retained austenite was gradually depleted by transformation. In some cases, two hardness peaks were observed. Tempering times were reduced from 1 hr to 10 min as it appeared that austenite transformation was nearing completion, avoiding the possibility of reducing the hardness of the microstructure more than was necessary. Finally, sleeves were spot checked for proper microstructure by metallographic inspection.

This procedure appeared to be successful. All eleven of the HTB-2 steel sleeves responded to this treatment. Minimum hardness was Rc 60; average hardness was Rc 62. The hardness variation of three readings on each sleeve did not exceed 1.5 points. Since it had been established that HTB-2 steel possesses a 0.2% offset yield strength of 330,000 psi and an elongation of 6% at the use temperature of 600°F, it appeared that the heat-treated sleeves would possess the requisite mechanical properties for extrusion liner support.

Sleeve distortion due to austenite transformation took the form of a maximum 0.0015 in. growth of diameters. Accordingly, it was necessary to plate and regrind those sleeves whose ID has been finish-ground to the high limit of the 0.0004 in. specified tolerance.

F. Procedures Developed for the Shrink-Fitting of Solid Ceramic Liners

All of the shrink-fitting tests carried out with solid ceramic liners utilized solid alumina, since this material was the least expensive and the most readily available. It is believed that the procedures developed are applicable to ceramic liners of any composition, though, because all of these materials possess the same general properties of high compressive strength, low tensile strength, and virtually no ductility.

The OD of the first liner assembled was coated with molybdenum disulfide lubricant prior to shrink-fitting. Within 1 min after shrink-fitting of the first cylinder, cracking was heard. Examination of the liner showed the presence of several hairline cracks (which would not have been visible if the molybdenum disulfide had not penetrated and stained the crack line) but no checking or spalling. No cracking was heard when the second sleeve was shrink-fitted. However, inspection showed additional cracks and two small areas approximately 1/32 in. in diameter where spalling had occurred. Shrink-fitting of the third sleeve caused an implosion of the liner, which resulted in complete destruction of the liner over approximately half of its length.

Thermal shock did not appear to be a likely cause of the liner cracking because of the time delay between shrink-fitting and cracking. Axial extension appeared more likely in view of the high tangential and radial compressive stresses and the absence of axial constraint. Consequently, it appeared desirable to introduce an axial constraint. This could be accomplished by substituting a fine abrasive for the lubricant used between the liner and sleeve. Axial expansion of the liner, under tangential compression, would be counteracted by axial contraction of the sleeve, under tangential tension. It appeared that use of such material would not cause damage when the first sleeve was pushed off the liner, because, in contrast to metal-to-metal surface contact, no galling would occur between ceramic and metal, and the abrasive was sufficiently fine to prevent metal gouging. (This assumption was proven in subsequent separation tests. The damaged liner fragmented, as expected, as it was pushed out of the sleeve. ID surface of the first supporting sleeve was not noticeably affected).

The abrasive used for this purpose was -325 mesh tungsten carbide powder suspended in a methylcellulose solution. This mixture could be readily sprayed. Coating sprayed on liner OD surface was as thin as possible, under 0.002 in., in order to prevent an increase of liner-first sleeve interference and generation of increased stress in the liner and support tooling.

Substitution of powdered tungsten carbide for molybdenum disulfide on the liner OD was partially effective in generating the desired liner axial constraint. No cracking was heard or observed, after the first sleeve was shrink-fitted on the liner. Difficulty was not encountered until the third sleeve was shrink-fitted. At this point, considerable cracking was evident. This might have been expected, since the counteracting tensile stress in the first sleeve is gradually reduced to a compressive stress as additional sleeves are shrink-fitted. (See $S_{T(s)}$, first sleeve, in Figure 29 of Appendix). Hence, additional liner axial constraint appeared necessary. This was accomplished by using undersized top and bottom liner spacers. Interferences were adjusted so that the top spacer was held in with a medium shrink fit, and the bottom spacer with a light shrink fit. Such a procedure caused the first sleeve to contract 0.003 to 0.005 in. above and below the liner.

Axial constraint developed by the undersized liner spacers, in conjunction with abrasive coating of the liner, proved effective in preventing cracking. Only a few very small cracks were observed after the fourth and final support sleeve had been shrink-fitted. These cracks were near the end of the liner, in the area where medium shrink-fitting of the liner spacer had been used. No cracks could be found in the end where the liner spacer had been lightly shrink-fitted.

Use of lightly shrink-fitted liner spacers at both ends of the liner in future liner sleeve assemblies will undoubtedly eliminate liner cracking.

It is noted that the extrusion billet does not come into contact with the liner spacers. The lower spacer is covered by the extrusion die. The upper spacer contacts the graphite follower block. Extrusion stresses will not reduce axial constraint because the entire liner-sleeve assembly axially contracts under extrusion pressures.

Unfortunately, the time required to develop reliable HTB-2 steel support sleeves for the solid ceramic liners, and suitable shrink-fitting procedures for prevention of cracking in liners, was sufficiently great to prevent the testing of the solid ceramic liner assembly produced during the period of the contract.

G. Procedures Employed in Production of Ceramic-Coated Liners

1. Rokide Process

All liners which were to be Rokide-process coated had coarse threads machined on the ID to provide a better gripping surface for the coating. Annealed H-13 steel hollow bar, with 3-1/2 in. ID and 1/4 in. wall, was used for base material. Processing was as follows:

a. Machine coarse thread on liner ID, turn OD 0.030 in. oversize for grinding allowance.

b. Heat-treat liner to Rc 47-49 and grind OD.

c. Flame spray Nichrome coating on OD

d. Flame spray a 0.025-0.030 in. thick coat of either alumina or stabilized zirconia on ID.

e. Shrink-fit supporting tooling on liner.

f. Grind liner coating to required ID size.

Two Rokide-process alumina-coated liners and two stabilized zirconia coated liners were processed in this manner.

2. <u>Plasma-Arc Process</u>

Annealed H-13 steel hollow bar, with 2-1/3 in. ID and 1/4 in. wall was used as base material. Processing for coatings which did not require heat treatment (all coatings but gradated zirconia-nickel-chrome) was as follows:

a. Machine, heat treat, and grind liner to finish size. Hardness was held at Rc 45-47. ID tolerance was +0.001 in. OD tolerance was -0.0004 in.

b. Grit blast ID to prepare surface for coating.

c. Ceramic-coat liner ID. Finish grind ID to size.

Hardness of these liners was reduced from Rc 47-49 to Rc 45-47 to facilitate grit blasting. This hardness level was still sufficiently high to prevent liner distortion during extrusion use. Liner coatings processed in this manner were alumina-nickel undercoat, zirconia nickel undercoat, and gradated alumina-nickel. Two liners were produced with each coating, making a total of 6 liners coated by the process described.

Grinding tolerance was held to 0.002 in. on these coatings, causing a total maximum thickness variation of 0.0015 in. Final coating thickness on the laminar coatings was between 0.002 and 0.0035 in. Final thickness of the gradated alumina-nickel coating was between 0.004 and 0.0055 in. These thicknesses were in conformance with recommendations of the Linde Flame Plating Department of the Union Carbide Company, who coated the liners.

The gradated zirconia-nickel chrome coating required a 2 hr anneal at 1950°F. Hence, a different processing schedule was required for this coating. Annealed H-13 steel hollow bar, with 3-1/2 in. ID and 1/4 in. wall, was used as base material. The procedure was as follows:

a. Machine and heat treat to Rc 42-44. Grind ID to ±0.001 in. tolerance. Allow 0.030 in. grinding stock to remain on OD.

b. Grit blast ID to prepare surface for coating.

c. Ceramic coat liner ID.

d. Heat liner to 1950°F at 100°-150°F per hr in dry argon. Hold at 1950°F for 2 hr. Furnace cool at 100°-150°F per hr.

e. Heat treat liner to Rc 47-49.

f. Finish grind OD to size. (OD tolerance was 0.0004 in.)

g. Finish grind coating to size.

Grinding tolerance was held to 0.002 in. on these coatings, developing a total maximum thickness variation of 0.0015 in. Final coating thickness was between 0.0040 and 0.0055 in., also in conformance with Linde Division recommendations. Two such sleeves were initially produced by this division. Several delays were encountered in producing these parts. When the coated sleeves finally were produced, sleeves were high-temperature annealed in a horizontal instead of a vertical position by mistake, causing the parts to become elliptical. Total out-of-roundness was 0.040 in. Consequently, it was necessary to scrap the sleeves and produce another set. This second pair of coated sleeves were received too late to permit final processing and testing.

H. Procedures Employed in Production of Superalloy Liners

René 41 (R-41) and Udimet 700 alloys (U-700) were selected for this purpose. R-41 alloy was used in the wrought form. U-700 alloy sleeves were cast to determine (1) general suitability of superalloys for liner use, and (2) possibility of using cast liners with resultant savings in material and machining costs.

Two R-41 forgings, heat treated for maximum mechanical strength were obtained from the Allegheny-Ludlum Steel Co. Two U-700 cylinders possessing a 3/8 in. wall were cast by the Austenal Division of the Howe-Sound Co. Casting of flaw-free cylinders presented problems in mold design and required several experiments in which casting practice was varied, because of the relatively thin wall required. However, a satisfactory casting procedure eventually was developed by the Austenal Division, and two sound U-700 cylinders were produced. Castings were radiographically inspected, then heat-treated for maximum mechanical strength.

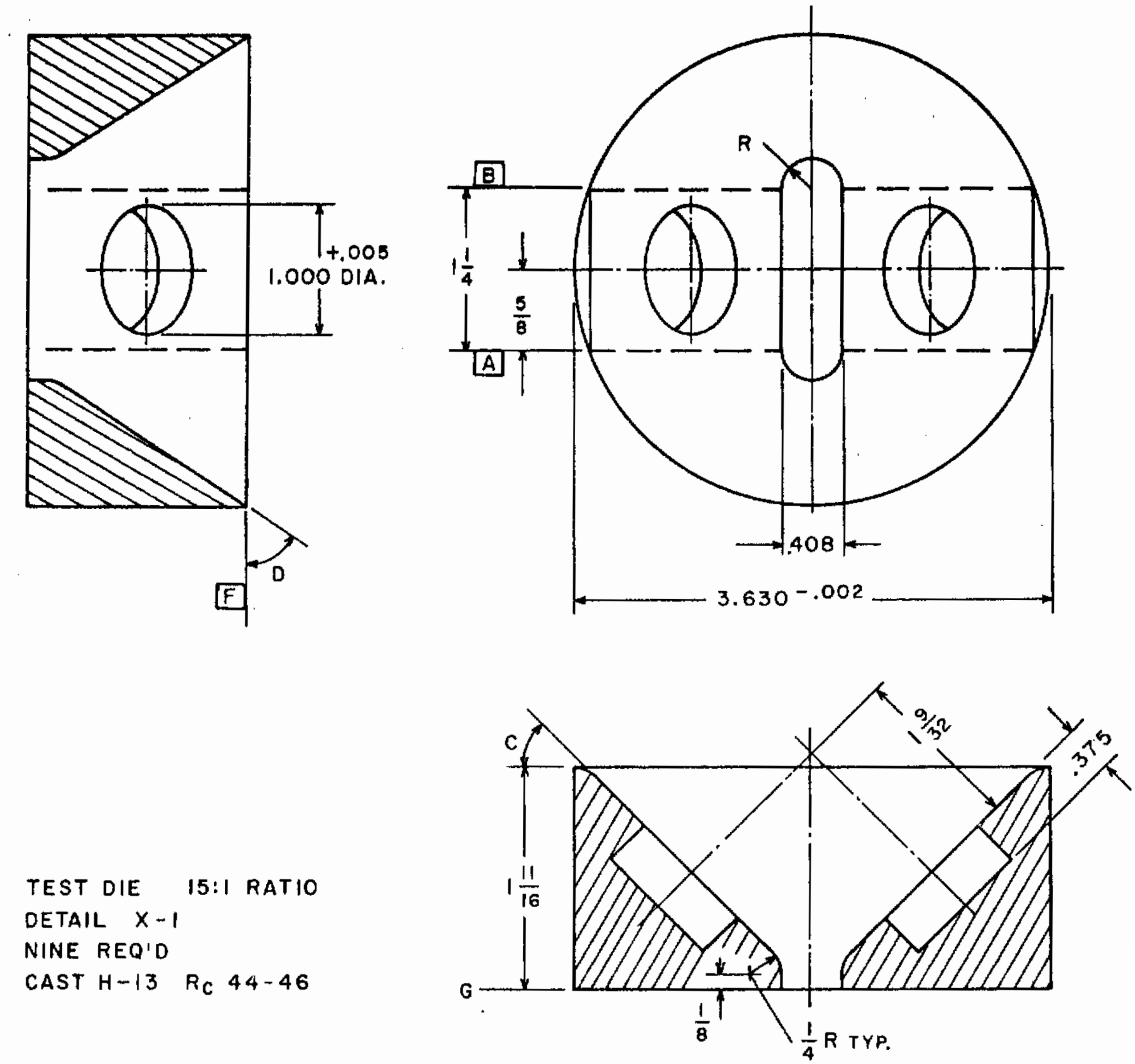
Both R-41 and U-700 alloy cylinders were machined and ground to size. Machining tolerances were held to within 0.0004 in. Wall thickness of finished parts was 1/4 in. Liners were shrink-fitted into the three-sleeve support assembly developed for ceramic-coated liner and superalloy liner use.

I. Procedures Developed for Extrusion Screening Test of Fiber-Metal Reinforced Ceramic Test Specimens

A drawing of the fixture for extrusion evaluation of fiber-metal reinforced ceramic composites is shown in Figure 13. It may be seen that the fixture consists of a bar die modified to accommodate two fiber-metal reinforced ceramic disks in the die entry section. The extrusion ratio employed required a 180,000 psi stem pressure when extruding the TZM alloy which, in turn, placed a 180,000 psi pressure on the test sample surfaces. Since samples were mounted in a "reduced area" section, billets were extruded at a slower-than-normal speed to compensate for increased flow rate of the metal across the test sample surfaces. This type of test simulated all liner environmental conditions except that of internal stress distribution. A test sample was exposed to billet metal at extrusion temperature. Metal moved across the sample face at a similar speed to a billet moving across a liner face. Contact time between billet and sample was the same as maximum contact time between billet and liner wall. However, the sample was exposed to an axial compressive stress due to stem pressure, and a shearing stress due to billet movement across the sample face. The liner is exposed to both tangential and radial tensile and compressive stresses, a varying axial stress, and a shearing stress due to billet movement.

This difference between stress distribution in the test sample and the liner did cause the sample to be subjected to less rigorous test conditions than would be encountered by the liner in extrusion service. Thus, samples which failed the screening test would certainly fail in extrusion liner service. Samples that passed the test might, or might not, be serviceable as liner materials. This, of course, is the function which a properly designed screening test should fulfill.

It was not necessary to subject any of the other materials used in liner evaluation studies to such a test, because sufficient material property information was available to make it possible to predict that they would pass this screening test. Past experience with fiber-metal reinforced-ceramic technology indicated that the particular composites produced represented the best possibilities for successful application of such materials to liner service. However, virtually nothing was known of the mechanical properties of most of them. Hence the need for the screening test described.



ANGLE C = 45° FROM [A] TO [B] FLAT.

ANGLE D – BLEND FROM [F] TO THROAT AT BOTH ENDS OF DIE TO FLAT SECTIONS.

FIG. 13 FIXTURE FOR EXTRUSION EVALUATION OF FIBER-METAL REINFORCED CERAMIC COMPOSITES.

VIII. EXPERIMENTAL PROGRAM

A. General Procedures

All extrusion liners and fiber-metal reinforced ceramics were evaluated at temperatures in the 2000°-3600°F range. SAE 4340 steel, stainless steel, and nickel-base superalloys were extruded at temperatures in the 2000°-2350°F range. TZM alloy billet was used at temperatures between 2800° and 3600°F. Zirconia-coated H-13 tool steel extrusion rod dies of 12:1, 16:1, and 40:1 ratio were used for most of the extrusion trials. On occasion "T" sections of 13.4:1 ratio and 1/4 in. web, circumscribed by a 2 in. diameter circle were produced to determine the effect that die geometry might have on liner performance, and to demonstrate the utility of test liners for "T" section extrusion. Utilization of this relatively large number of materials and die styles made it possible to evaluate liner performance over a number of different extrusion pressures at both relatively low and high temperatures. Ram speeds of 100 to 600 in/min were employed, depending on the alloy being extruded. Direct recordings of ram force vs. ram stroke, and ram velocity vs. ram stroke were made for all definitive extrusion trials. Examination of the liner surface was also made after each extrusion trial.

B. Test Equipment

Extrusion trials were carried out on IITRI's 1000-ton capacity vertical extrusion-forging research press. A photograph of the press is shown in Figure 14. This equipment has several features which were particularly valuable for the test activity required. These were:

1. High-Temperature Induction Heating and Temperature-Measuring Equipment

   Billets are heated in an 80-kw induction furnace, mounted on the side of the press. Peak temperature of this furnace is in excess of 3800°F. Heating time of a 3-1/2 x 6 in. TZM alloy billet to 3400°F is approximately 25 min.

   Furnace is equipped with an argon atmosphere. Argon is injected through the "Ray-O-Tube" temperature-sensing device, assuring that performance of this optical device will not be impaired by any smoke developed during heating operations.

   Furnace and temperature control are maintained by use of a remote furnace control panel, mounted in the press control console, and an AZAR recorder, connected to the "Ray-O-Tube." Temperature control accuracy of $\pm$10°F is obtained with this equipment.

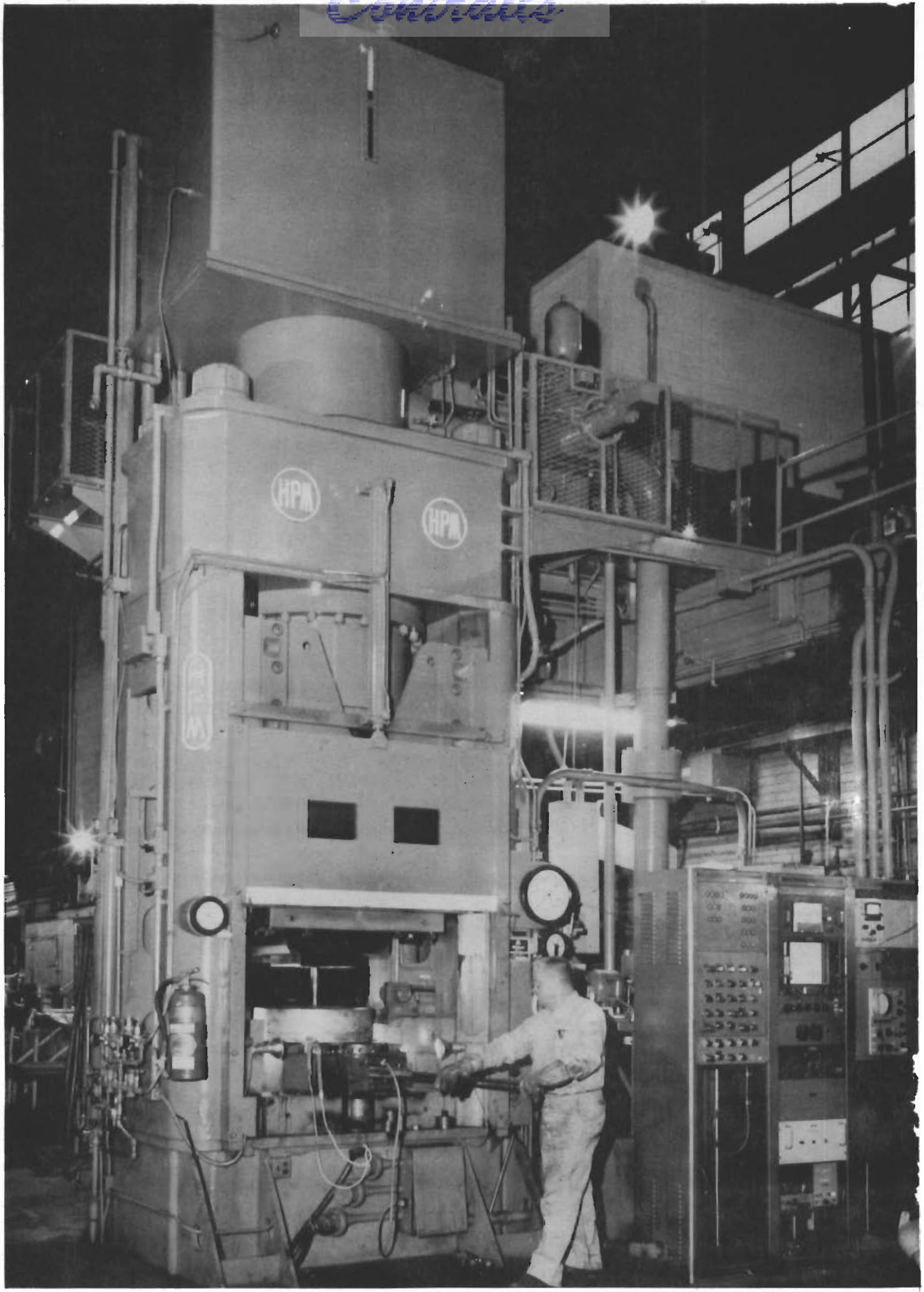


FIG. 14 - PHOTOGRAPH OF 1000-TON CAPACITY VERTICAL EXTRUSION-FORGING PRESS

2. High-Speed Billet-and-Follower Block Transfer Equipment

   Billets are machine-transferred from the induction furnace to the extrusion container in 3 sec. Graphite-follower blocks, when required, are transferred by a second specially designed machine in 2 sec. The follower-block loader is automatically operated by the billet loader. Billet and follower-block transfer operations are controlled by the press operator by actuating two "in-and-out" push buttons.

3. High Press Ram Speed. Press ram speed of 600 in/min can be obtained with a 1000-ton force. This permits high-temperature extrusion to be accomplished without die wash or billet chilling.

4. Direct Recording Instrumentation. Both ram force vs. stroke, and ram velocity vs. stroke recordings are simultaneously produced by the use of appropriate transducers, and a dual-beam Y_1Y_2X oscilloscope. No data replotting is necessary. Use of an oscilloscope instead of a pen recorder permits response time of the system to be governed by response time of the transducers. Maximum transducer response time is 15 milliseconds, assuring accurate measurements of extrusion transients.

C. Lubrication Practice

All billets were glass-coated prior to extrusion, using the spray process developed by the Air Force, Wright Field extrusion facility. It was found that spraying could be accomplished as easily and equipment maintenance reduced, if a simple inexpensive foundry mold-wash sprayer was substituted for a spray gun. Glass was applied in three-to-four coats. Coats dried within a few minutes if billets were heated to 400°F before spraying. Corning glass No. 0010 was used for lubrication for billets heated to temperatures of up to 2600°F. Glass No. 7052 was used for the 2600°-3000°F temperature range, and glass No. 7740 was used for temperatures from 3000°-3600°F.

Fiske-Lube 604* was used as a liner lubricant for billets extruded at temperatures below 3000°F. Past experience has demonstrated that this lubricant can be used for billet temperatures as high as 3600°F. However, it was found that the material generated gas at a sufficiently high rate to cause a mild explosion if billet temperatures exceeded 3000°F. To avoid the possibility of tool damage (and insure safety of the press crew) Molykote "G" was substituted as a liner lubricant for temperatures over 3000°F. This material appeared to possess the same lubricity as Fiske-Lube 604 in this temperature range, but did not generate any significant amount of gas.

*Product of Fiske Bros. Refining Company.

D. Installation and Removal of Test Liners from Extrusion Container

Necessary support sleeves were routinely shrink-fitted on each liner to be evaluated by use of the procedure described in Part IV, Section C. Some difficulty was encountered in transferring cold sleeve assemblies into the heated sleeve, if both hot and cold parts had sharp corners. A slight mismatch between centerlines of the parts would then result in a bouncing of the cold sleeve before it settled into the heater sleeve. Premature sleeve seizure could then result. This difficulty was remedied by the use of a 1/32 in. radius on the OD of the cold sleeve assembly, and a 10°F, 1/4 in. long conical entry on the ID of the heated sleeve. All assemblies could then be smoothly transferred without audible scraping. Transfer time, in all cases, was approximately 1 sec.

Once a complete liner-sleeve assembly had been shrink-fitted the assembly was dropped, or pressed with a few tons of force into the container liners.

Removal of used extrusion liners for inspection, and/or replacement was accomplished by use of the procedure described in Part V, Section B.

E. Liner Evaluation Procedure

Initial liner inspection procedures consisted of visual examination of the liner after each extrusion trial, followed by scraping of any suspicious surfaces. This procedure was found to give misleading results in evaluating the stability of the alumina and zirconia coatings.

The coating surface could spall or crack in some areas during extrusion use, but could not be seen because of the container or billet lubricant. This material would be forced into the cracks, or cover the spalled areas with a durable smooth coating which was indistinguishable from the liner surface and was surprisingly chip resistant. Spalled areas as large as 1/4 x 1/2 in. could be hidden effectively in this manner, with the lubricant apparently securely locked in place by the rough-threaded undersurface of the tool steel liner. Extrusion pressure required for extrusion did not rise when using liners protected in this manner, and the extrusion surface did not appear affected.

In order to determine the true condition of the liner surface, it was necessary to remove the liner-sleeve assembly from the container, and then boil the assembly in an Alconox-water solution for 2 to 4 hr. This procedure did prove effective in removing virtually all of the graphite-base container lubricant used during extrusion operations.

IX. EXTRUSION LINER EVALUATION

A. Test Conditions

Extrusion test conditions are summarized in Table VII. Liner code letter designations are listed at the end of the table.

B. Results

1. Support Tooling for Ceramic Coated Liners and Superalloy Liners

Liner evaluation test interposed with 20 to 30 assembly-disassembly operations demonstrated that the liner support sleeves provided adequate support for the liners and did not suffer dimensional change during use. None of the thin-wall liners tested showed any measurable expansion under stem pressures as high as 200,000 psi. On one occasion, accidental use of a stem of improper length, and press ram overstroking, caused the stem head to be driven into the die at a speed of 600 in/min and at a force of 1000 tons. Although dummy block, die, and ceramic liner coating in the die vicinity were destroyed by the impact, the liner and liner support tooling did not distort. Maximum out-of-roundness of support sleeves after use and disassembly was 0.003 in. When sleeves were restressed by shrink-fitting, out-of-roundness of the stressed sleeve would be reduced to less than 0.001 in. However, use of two procedures was found to be necessary to prevent sleeve damage or distortion. These were:

a. Use of molybdenum disulfide lubricant (Molykote G) between shrink-fitted surfaces.

b. Slight reduction in collar height of outer sleeve to prevent stem slide contact if press were overstroked.

A molybdenum disulfide coating on the OD of the cold sleeve to be shrink-fitted prevented galling during disassembly operations. If a lubricant were not used, grooves as deep as 0.010 in. would be cut into the sleeves when they were separated.

If collar height of the outer sleeve were slightly higher than the extrusion container, this collar would sustain the full force of the press, (1000 tons), if stem slide impacted the container during the extrusion stroke. This would force down the container liner sleeve supporting the liner-sleeve assembly. Figure 2 shows that the OD of the container liner sleeve had an angular step approximately 5 in. from the top of the sleeve. If this sleeve were forced down, the angular step would decrease the ID of the container sleeve at the point of the step. This, in turn, caused a high pressure to be placed on the OD of the outer sleeve of the liner-sleeve assembly. Results of the application of such pressure was a slight hour-glass effect on both

TABLE VII

EXTRUSION LINER EVALUATIONS ON 3 1/2 IN. DIAMETER BILLETS

Liner Material	Billet Material	Temp, °F	Extrusion Ratio	Ram Velocity, in/min.	Peak Force, tons	Stroke, in.
Alumina Coating,	SAE 4340	2200	12	------	----	6.2
Rokide Process,	SAE 4340	2200	16	------	----	5.4
No. 1	SAE 4340	2000	16	------	----	5.1
Alumina Coating,	SAE 4340	2000	16	420-500	630	7.5
Rokide Process	SAE 4340	2200	16	------	200(c)	6.2
No. 2	TZM	2600	16	------	800	---
	TZM	2600	16	------	890	---
	TZM	3150(a)	16	120-180	960	5.1
Alumina Coating,	Inco 713C	2250	12	500	580	7.6
Rokide Process,	TZM	3350(a)	16	500-620	700	5.4
No. 3						
Stabilized Zirconia	PH15-7Mo	1900	16	300-400	710	5.4
Coating, Rokide-	U-700	2050	12	80-100	840	5.4
Process, No. 1	U-700	2050	12	100	800	4.1
Stabilized Zirconia	U-700	2050	12	100-150(d)	700(c)	5.5
Coating, Rokide-	U-700	2050	12	100-150(d)	700(c)	5.4
Process, No. 2	U-700	1900	12	25	710	5.4
	SAE 4340	2000	40	100-300	825	3.3
	SAE 4340	2000	40	------	----	5.5
	SAE 4340	2000	40	------	400	4.7
	SAE 4340	2000	40	200-400	810	4.7
	SAE 4340	2000	40	200-400	700	3.0
	SAE 4340	2000	14"T"	350-400	700	4.7
	SAE 4340	2000	16	200-440	700	4.2
	Inco 713C	2250	12	35-70	800	5.8
	U-700	2050	12	70-100(d)	450(c)	5.9
	TZM	3450	16	300-580	775	4.8
	TZM	3100	16	------	900(e)	2.0

TABLE VII (Continued)

Liner Material	Billet Material	Temp, °F	Extrusion Ratio	Ram Velocity, in/min.	Peak Force, tons	Stroke, in.
Udimet 700, cast	SAE 4340	2000	16	200-400	690	4.9
	SAE 4340	2000	40	400-580	710	5.5
	SAE 4340	2000	40	80	720	4.6
	TZM	3600	16	------	600[(c)]	4.8
Rene 41, Wrought	PH15-7M0	2150	14"T"	900	600	3.8
	PH15-7Mo	2100	14"T"	180-300	690	3.2
	Steel-MgO	2900	8	520-580	440	5.0
	Steel-MgO	3275	8	580-620	300	4.9
Stabilized Zirconia Laminar Coating, Plasma-Arc Process No. 1	PH15-7Mo	2100	14"T"	120-170	700	3.0
	SAE 4340	2000	40	80	770	5.9
	PH15-7Mo	2050	14"T"	100-400	780	4.3
	PH'5-7Mo	2000	14"T"	100-410	790	4.3
	U-700	2050	40	------	220	2.1
	U-700	2100	14"T"	50-80	800	5.5
	TZM	3400	14"T"	320	800	4.3
Stabilized Zirconia Laminar Coating, Plasma-Are Process No. 2	SAE 4340	2000	16(strip)	350-510	750	6.8
	TZM	3475	16(strip)	410-470	810	5.7
Alumina Laminar Coating, Plasma-Arc Process No. 1	SAE 4340	2000	40	311-500	750	6.2
	SAE 4340	2000	40	------	----	6.3
	SAE 4340	2000	40	220-420	680	6.4
	SAE 4340	2000	40	------	100	0.1
	SAE 4340	2000	16(strip)	424-475	750	6.4
Alumina Laminar Coating, Plasma-Arc Process No. 2	SAE 4340	2000	16(strip)	450-500	720	7.2
	SAE 4340	2000	16(strip)	320-340	700	6.2
	SAE 4340	2000	40	275-435	830	6.2
	SAE 4340	2000	40	425	775	6.4
	SAE 4340	2000	40	400-460	820	6.4

TABLE VII (Continued)

Liner Material	Billet Material	Temp, °F	Extrusion Ratio	Ram Velocity, in/min.	Peak Force, tons	Stroke, in.
Alumina Gradated Coating, Plasma-Arc Process, No. 1	SAE 4340	2000	40	220	770	5.9
	SAE 4340	2000	40	400-450	700	6.6
	SAE 4340	2000	40	350-400	800	6.0
	Spec. Cst Tool Steel	2085	12	240-490	325	5.5
	TZM	3400	16	380	800	6.3
	Steel-MgO	2175-2200	8	300-400	225	5.8
	SAE 4340	2000	40	300-480	780	7.3
	TZM	3400	16	280-380	840	5.5
Alumina Gradated Coating, Plasma-Arc Process, No. 2	SAE 4340	2000	40	------	700(c)	6.4

(a) Temperature accuracy is ±50°F. On other runs, temperature accuracy is ±15. F.

(b) If ram velocity at first inch of stroke is different from peak velocity, first number indicates velocity at first inch of stroke, second number indicates peak ram velocity.

(c) Reading obtained from peak-reading hydraulic gage.

(d) Value estimated from the accumulator speed control valve setting and the ram force developed.

(e) Value estimated from accumulator pressure at end of extrusion stroke.

container liner sleeve and outer support sleeve. This made the liner-sleev removal operations difficult. Subsequent grinding, plating, and regrinding operations of the container and outer support sleeves were tedious and expensive. Reduction in collar height sleeve assured that the stem slide would contact the container and not the sleeve collar, in the event of overstroking, and prevented further incidence of distortion in the collar and container liner sleeves.

H-13 steel proved to have sufficient temper resistance to permit its use for all support sleeves when ceramic-coated liners were used. Even a 0.003 in. thickness of ceramic was sufficiently effective in reducing heat transfer rate from the billet to the support sleeves to permit extrusion billet butts to remain in the liner for as much as 5 min without damage to support sleeves when billet temperatures were at 3450°F.

If uncoated metal liners were used for extrusion of billets over 3400°F, H-13 steel support tooling would temper, because of the increased heat transfer rate from billet to the support sleeves. This problem was corrected by substitution of HTB-2 steel for the H-13 steel in the first support sleeve. H-13 steel could be heated to a temperature of only 1075°F, if the Rc 46-48 hardness were to be held. The HTB-2 steel could withstand 1200°F and hold the same room-temperature hardness level.

2. Rokide Process Alumina-Coated Liners

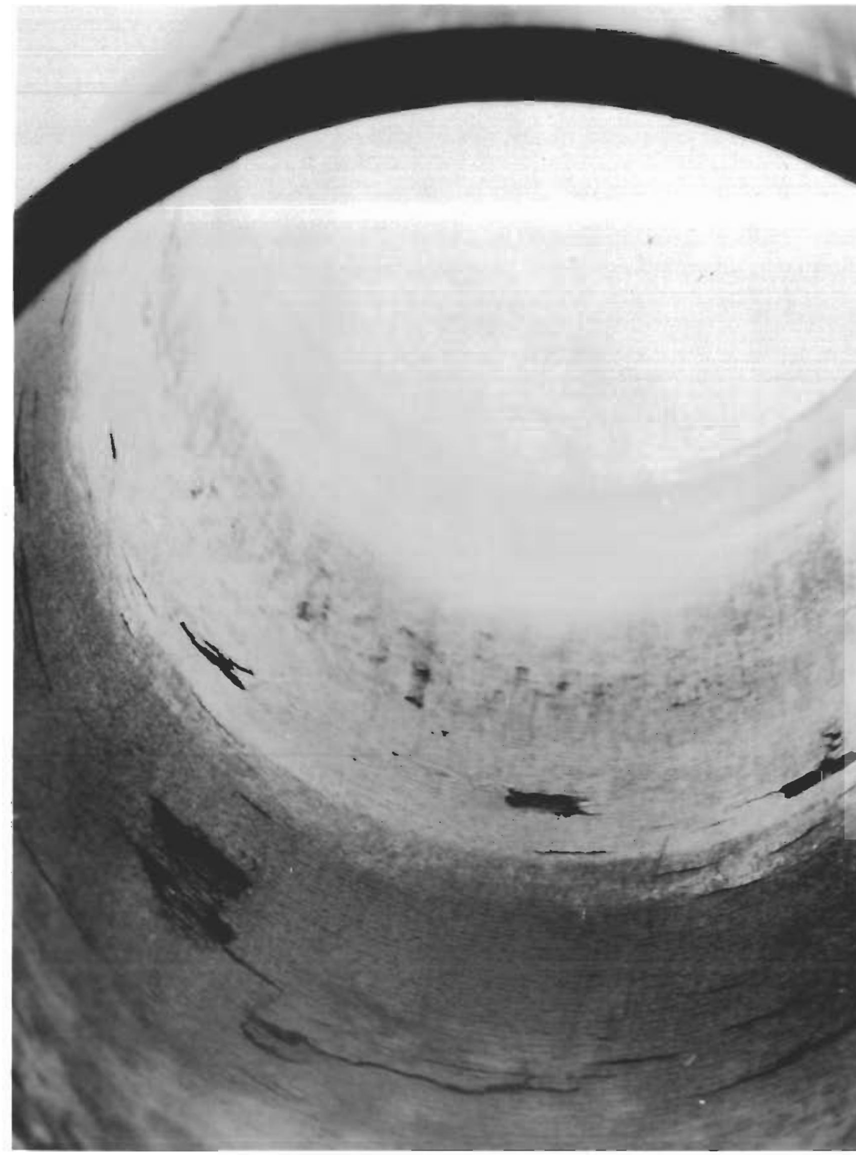
Three alumina-coated liners were tested. These liners showed excellent thermal shock resistivity in the 2000°-2300°F temperature range at extrusion pressures up to 150,000 psi, but were sensitive to mechanical shock. It was noted that cracking regularly occurred in the liner area that was in contact with the extrusion die. Also, cracking was noticed on two occasions when the stem slide accidently impacted the container. Since neither of these conditions should be encountered in normal industrial practice, this does not appear to be a serious limitation of this coating at these temperatures. (The extrusion die is normally retained in a die slide outside the container or industrial presses.)

Performance of these coatings at temperatures between 2900° and 3450°F was not particularly good. The spalling and cracking shown in Figures 15a and 15b were typical of the performance displayed in this temperature range.

However, the coating did provide an effective thermal barrier between the billet, the steel liner, and the supporting tooling, and did protect the liner from wear, even in places where the coating was worn. In one test, a TZM billet was heated to 2900°F and then loaded to 184,000 psi in the liner without extruding. The load was maintained for 1 min. The steel liner did not temper. In another test, a TZM billet was heated to 3100°F and placed in the liner under a "no load" condition. The billet was left in the liner 5 min. Although the alumina coating showed evidence of local melting in this test, liner hardness was not affected.

Neg. No. 25198

(a) Over-all view



Neg. No. 25199

(b) Close-up view of coating

FIG. 15 - PHOTOGRAPHS OF ALUMINA-COATED LINER AFTER TESTING.

3. Stabilized Zirconia-Coated Rokide Process

Two stabilized zirconia-coated liners were extrusion tested. Behavior of the two coatings showed a wide variation. The first coated liner showed severe spalling after extrusion of 5 billets at 2000°F. Figures 16a and 16b show this damage. It can be seen that the coating shows virtually no wear in the places where it has adhered to the liner.

The second zirconia-coated liner tested did not spall. Extrusion testing was carried out at 2000°, 3200°, and 3450°F. Figures 17a and 17b show the condition of this liner after testing at 3450°F. Cracking was observed only in the die contact area. The reason for the great difference in performance between the two liners could not be immediately determined, since both liners were coated by the same source, under conditions thought to be similar. One possible cause for the apparent difference in performance could be the temperature to which the liner was heated during the flame-spraying operation. Establishing an optimum liner temperature, and/or other factors responsible for erratic coating performance could entail a considerable expenditure of time and effort. Since there were many other liners which had not yet been evaluated at the time of these tests, further studies of this particular coating were deferred.

4. Cast Udimet 700 Liner

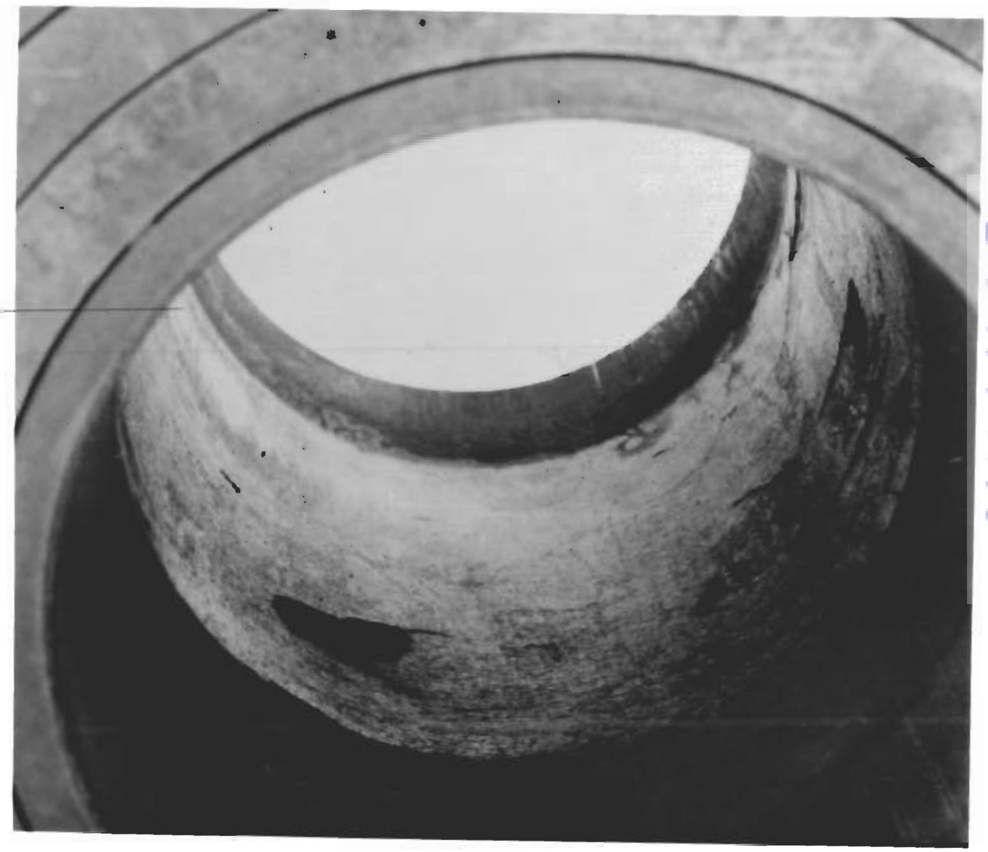
Udimet 700 alloy was chosen as a liner candidate material because of its relatively high proof stress--145,000 psi at 1200°F. Since suitably designed support tooling could limit tangential stress to 110,000 psi under a 180,000 psi stem pressure, the elevated temperature strength of the alloy, coupled with the heat-sink properties of the supporting tooling, could be expected to enable it to withstand very high billet contact temperatures. A question did exist, however, concerning the wear resistance of the material. Udimet 700, in the heat-treated condition for maximum strength, has a Rockwell C hardness of only 41-44. This is considerably below the Rc 50-52 hardness of most extrusion liners in present use for steel extrusion.

Extrusion trials at 2000°F, using steel test billets, did not cause any noticeable wear or scoring on the liner surface, demonstrating some reasonable degree of wear resistance of the Udimet 700 alloy. Consequently, an attempt was made to extrude TZM alloy at high temperature. This particular billet was heated to 3600°F, then extruded to rod at a 16:1 ratio. Combined billet and follower block transfer time was less than 7 sec on this run. Required extrusion force was relatively low, approximately 120,000 psi, in marked contrast to the 150,000 psi required at 3450°F, and the 180,000 psi required at 3150°F.

Examination of the liner after extrusion showed that a layer of TZM alloy, approximately the thickness of the space between the stem head and the liner, had been deposited on the liner wall. Figures 18a and 18b show the liner condition immediately after removal from the container.

Neg. No. 25194

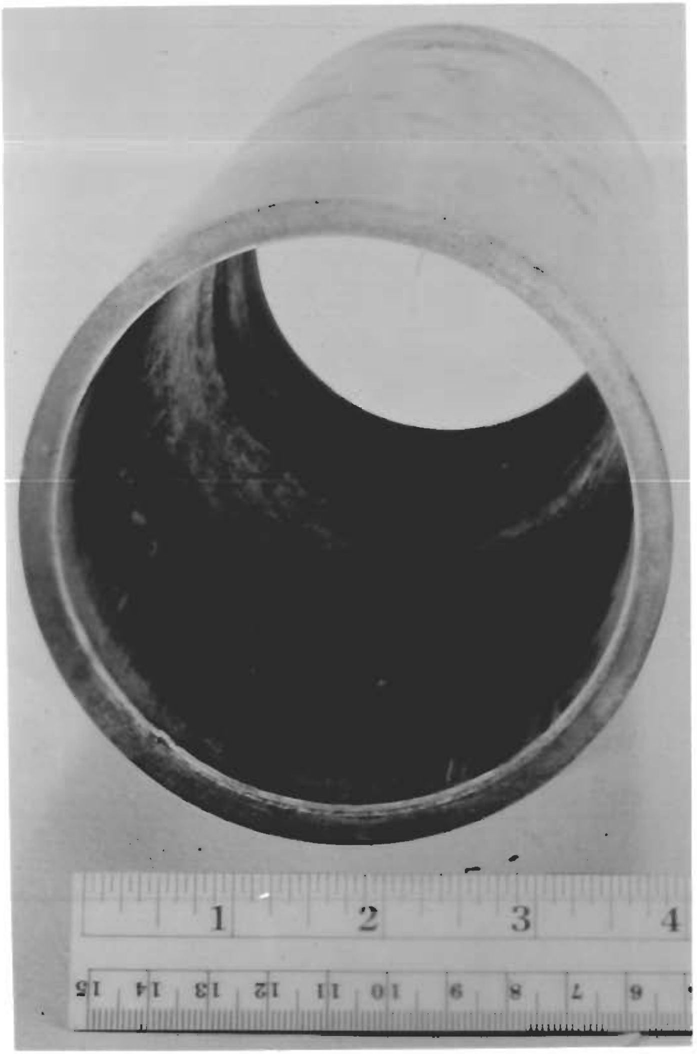
(a) Liner and sleeve assembly



Neg. No. 25195

(b) Close-up view of coating

FIG. 16 - PHOTOGRAPHS OF FIRST ZIRCONIA-COATED LINER AFTER TESTING.



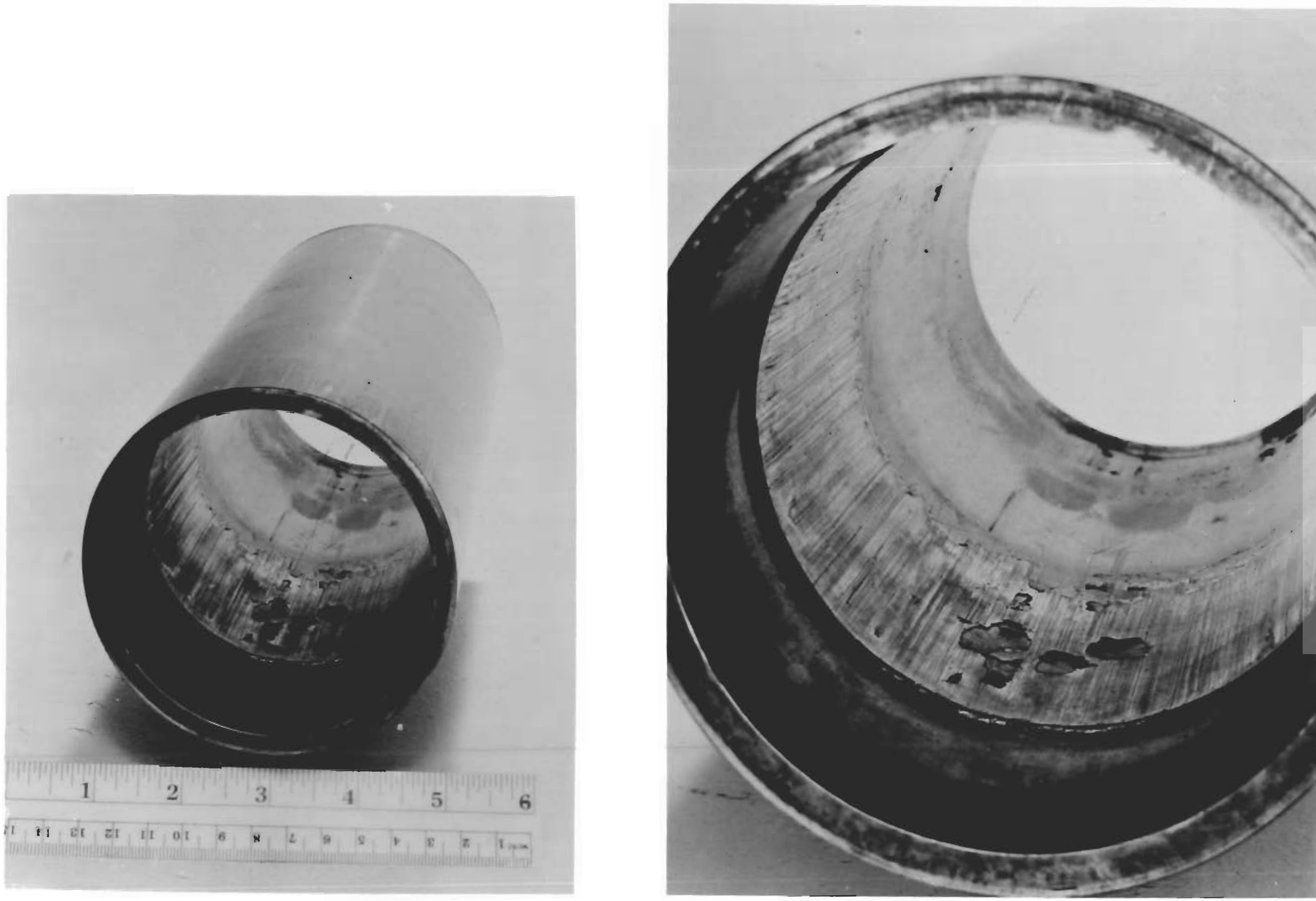
Neg. No. 25249

(a) Over-all view

Neg. No. 25250

(b) Close-up view of coating

FIG. 17 - PHOTOGRAPHS OF SECOND ZIRCONIA-COATED LINER AFTER TESTING.



Neg. No. 25247
(a) Over-all view

Neg. No. 25248
(b) Close-up view of TZM coating

FIG. 18 - PHOTOGRAPHS OF TZM-COATED UDIMET 700 LINER AFTER TESTING.

Removal of this TZM layer proved difficult. The material could not be machined with carbide cutters, and grinding proved to be a slower than usual operation. When the TZM finally was removed, internal and external hardness checks and OD measurement showed that no softening or distortion of the Udimet alloy had occurred. However, the 1-1/2 in. length of liner between stem end and die which had not been galled with TZM appeared scored to a depth of 0.010 to 0.012 in. Figures 19a and 19b show the liner condition after removal of the TZM.

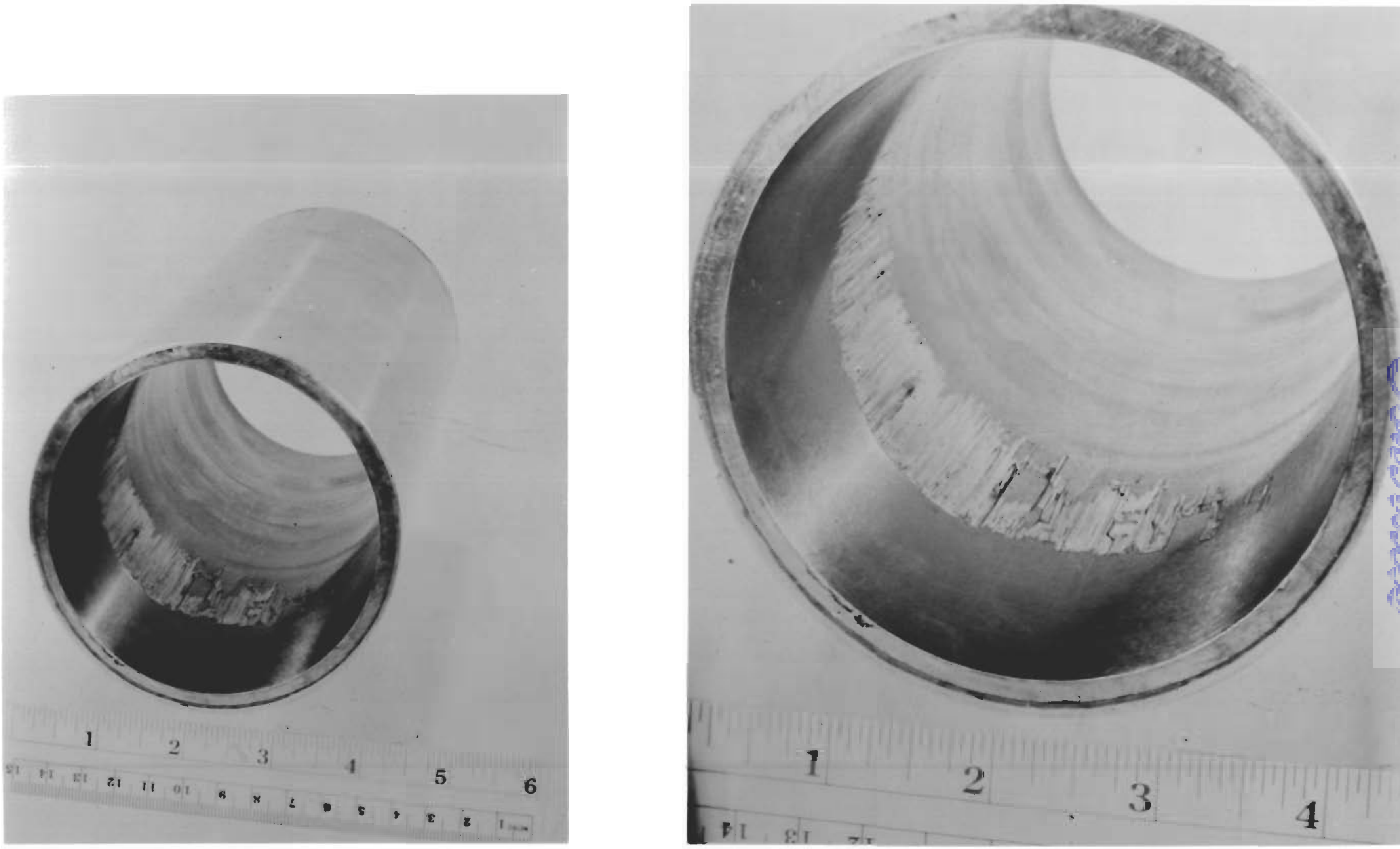
It is possible that a procedure could be devised for maintaining a reasonably smooth layer of galled TZM over the entire length of the Udimet alloy liner as a protective coating for future extremely high-temperature extrusion. For the present, however, it appears that this alloy should not be used as a liner material when extruding TZM billets at 3600°F.

It should be noted that use of a 3600°F billet under extrusion pressure is an extremely severe test for a liner material. Since the radiant energy emitted by a heated body varies directly as the fourth power of the absolute temperature, a billet at 3600°F (2255°K) will emit 16% more radiant energy than it would at 3450°F (2175°K), and 56% more radiant energy than it would at 3150°F (2000°K).

Use of a cast Udimet 700 liner of the type tested may be particularly well suited to extrusion of the red metals, i.e., copper, high brass, and curpo-nickels. Recently, an A-286 superalloy has been substituted for H-12 tool steel in extrusion container liners on presses extruding these metals. Liner wear has been appreciably increased. It is likely that the Udimet 700 alloy would have a considerably longer life than the A-286 alloy in such a liner application. The increased cost of the Udimet 700 would be offset by the relatively small amount used, because of the thin-wall liner design. Casting would also effect material economy. Hence, it is likely that the thin-wall cast Udimet 700 alloy liner would have a lower first cost than conventional A-286 alloy liner, in addition to longer life and better wear resistance, in the extrusion of red metals.

5. Forged René 41 Superalloy Liner

The René 41 alloy performed very well at both 2100° and 2900°F. At both temperatures, no liner damage or significant change of interior dimensions could be noted. Some liner damage was sustained at 3275°F, however. A metallic buildup on the liner wall was observed following extrusion at this temperature. Buildup started at the top of the die, and extended 1 to 3 in. upward. Buildup thickness was approximately 1/32 in. A cleanout block, driven through the liner with a force of 10-20 tons, removed most of the buildup, but did develop score marks on both the cleanout block and the liner wall. (Residual glass lubricant can often cause such an effect when a close-fitting cleanout block is used.)



Neg. No. 25335
(a) Over-all view

Neg. No. 25336
(b) Close-up view

FIG. 19 - PHOTOGRAPHS OF UDIMET 700 LINER SURFACE AFTER TESTING AND REMOVAL OF TZM COATING.

Examination of the liner after its removal from the container showed that the greatest wall buildup had occurred in a length which was approximately equal to the length of the billet stub remaining in the liner after the extrusion had been sheared. This section was not ejected for several minutes following the extrusion of the billet. As a result, the liner wall in this section reached a much higher temperature than the rest of the wall. Apparently, the heat conduction properties of the René 41 alloy and the supporting steel were not sufficiently high to permit a rapid enough rate of heat extraction to prevent chemical interaction between liner and billet materials.

Measurement of the liner ID after disassembly of the supporting sleeves showed that the ID had decreased approximately 0.003 in. over the full length of the liner. This indicated that the 115,000 psi tangential compressive stress placed on the liner by the supporting sleeves was marginally high, or that this material is not suitable for a peak tangential stress of 230,000 psi (approximately 205,000 psi stem pressure). This was the only material tested which demonstrated this weakness. The 0.2% offset tensile yield strength of the René 41 alloy at 600°F was the lowest of any of the liner materials tested, approximately 130,000 psi. The support tooling stressed this material to 90% of its 0.2% offset yield strength. All other materials were stressed to a maximum of 75% of their 0.2% offset yield strength. The dimensional change of the René 41 alloy indicates that the maximum per cent of offset yield stress which can be used safely in the present design lies somewhere between the 75 and 90% figure.

Damage sustained by the René 41 alloy liner during the TZM alloy test appeared superficial, and it is likely that remachining of the liner bore to its original size would have permitted its reuse for additional testing. However, information gained by the tests carried out was sufficient to demonstrate that this material possessed mechanical property shortcomings which would severely restrict its use in high-performance extrusion tooling used for billet extrusion at any temperature.

6. Plasma-Arc Stabilized Laminar Zirconia Coating

Two zirconia-coated liners were tested. Test results obtained by extrusion in the 2000°-2200°F temperature range were excellent. No wear whatsoever could be detected on the 0.003 in. thick liner coating after six T-section steel extrusion trials. Neither the graphite-base container lubricant nor the glass billet lubricant adhered to the liner coating after extrusion, making it possible to quickly and accurately inspect the liner coating after each extrusion trial. This behavior was in marked contrast to that of the Rokide-process coatings. In those cases, the graphite-base lubricant would tenaciously adhere to the ceramic coating. Boiling in a high-detergent solution was required for accurate inspection. The higher density of the plasma-arc coating probably was responsible for this "no stick" phenomenon.

Coating cracks did occur in the contact area between die and liner wall, as it did in the case of Rokide-process coatings. Since commercial extrusion practice operates with the extrusion die outside the liner, the cracking observed in this particular laboratory setup would not occur in the field, and therefore, is not considered significant.

Extrusion of a TZM T-section at 3400°F demonstrated that this coating is not suitable for such activity, but that it is effective as a thermal barrier.

Unfortunately, accumulator pressure was marginally low at the start of this extrusion trial, with the result that a maximum stem pressure of only 160,000 psi was generated, instead of the required 180,000 psi. Although the billet did extrude at this stem pressure, the ram speed was considerably slower than had been intended, 100-200 in/min, instead of the 500-600 in/min normally used in TZM extrusion. Average ram speed over the complete stroke was only 120 in/min.

The billet did extrude satisfactorily under this relatively slow ram speed, and continued to extrude until sufficient heat had been transferred from the billet to the container to stiffen the billet and cause sticking. Total extrusion time in this trial was 7 sec. A sound, smooth-surfaced T-section approximately 40 in. long was produced during this time. The unextruded portion of the billet was 1-1/2 in. in length. Approximately 5 min was required to clear the tooling of the billet stub, because of the difficulty in shearing the relatively cold T-section with the 50-ton shear.

As expected, the combination of slow ram speed and high billet temperature caused considerable thermal damage to extrusion tooling. The entry section of the T-section die was partially melted. The upper and lower T-section shear blades were so badly washed that it was necessary to discard them. The T-section shear bolster was jammed by an 8 in. length of T-section which proved to be unmachinable, and had to be disintegrated.

However, the liner support tooling sustained no damage whatsoever. Even though the billet contact time was very long, the 0.003 in. thick zirconia coating was effective in preventing tempering of the H-13 steel sleeves. Disassembly and inspection of all parts of the supporting assembly showed that no significant change in hardness or sleeve dimension had been caused by the sticking and slow removal of a TZM billet transferred to the tooling at 3400°F.

Examination of the liner coating after this extrusion trial showed that both spalling and wear had developed which was not connected with the slow ram speed, but was due simply to the combination of billet temperature and ram pressure employed.

7. Plasma-Arc Laminar Alumina Coating

Tests of two plasma-arc laminar alumina coated liners showed this coating to be inferior for steel billet extrusion; consequently, it was unusable for TZM alloy extrusion. Both coatings showed pronounced longitudinal score marks after two steel extrusions were made. Coatings were virtually destroyed after four steel extrusions had been made. Tests were stopped at this point to prevent damage to the backup tool steel liner.

8. Plasma-Arc Gradated Alumina Coating

Two plasma-arc gradated alumina coated liners were tested. Unfortunately, it was possible to test only one liner at high temperature. The liner which could not be fully tested had accidently been coated before the ID had been properly ground causing the ID to be 0.003 in. below minimum tolerance. Since it was not known that the liner was undersize at the time of the tests, a standard-size die was inserted in the liner. This procedure fortuitously worked twice, probably because the die was relatively cold when inserted, and also was near the low side of its OD tolerance. This coating appeared to withstand the 2000°F steel extrusion trials very well. However, when a third test was attempted, the die seized in the liner. Attempts to dislodge it damaged the coating, and made it necessary to discard the liner.

The other liner of this type was evaluated by extruding six steel billets at a 40:1 ratio at 2000°F, and extruding two TZM alloy billets at 3400°F. This coating did not appear to be affected by any of the steel billet extrusions. However, the container lubricant strongly adhered to the surface, indicating some degree of coating porosity. Coating performance under TZM extrusion conditions was superior to other plasma-arc coatings tested but was not adequate. The coating showed light scoring after the first TZM billet was extruded, and heavy scoring and cracking after the second TZM billet was extruded. Extruding a steel billet after the second TZM billet did not cause further deterioration of the liner surface, indicating that coating wear was not accelerative.

It was found that this coating possessed an additional unusual property, that other ceramic coatings may possibly also possess. Extrusion of four steel billets under different lubricant conditions demonstrated that it is not necessary to use glass lubricant when extruding steel billets in a ceramic-coated liner of this type.

These experiments were carried out in the following manner: The first billet was lubricated by spraying the normal 0.010-0.015 in. thick glass coating on the billet surface prior to heating. The container surface was lubricated with Fiske-Lube 604. Lubrication conditions for the second billet were the same as those for the first, with the exception of the thickness of the glass coating, which was only 0.002-0.005 in. thick in this case. The third billet was not glass lubricated. The container was lubricated with Fiske-Lube, as before. Finally, neither glass nor Fiske-Lube lubricant was used in the extrusion of the fourth billet.

Pressing force-stroke photograms and ram velocity-stroke photograms were obtained for each of the four tests. The results may be seen in Figures 20-23. No measurable difference exists among the pressure-stroke photograms shown on the first three of these figures. The fourth photogram shows the linearly sloping curve characteristic of extrusion carried out under poor lubricating conditions.

These photograms demonstrate that a suitable container lubricant supplies all the necessary billet lubrication when used on a plasma-arc graded alumina coated liner. Glass lubricant does little to reduce billet liner friction in this case.

X. EXTRUSION EVALUATION OF FIBER-METAL REINFORCED CERAMIC COMPOSITES

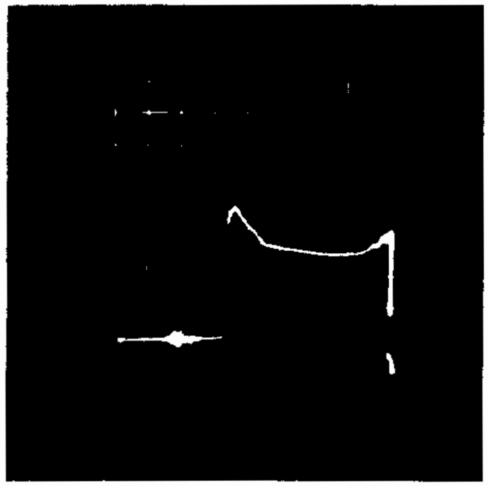
Results of the extrusion evaluation of fiber-metal reinforced ceramic composites are listed in Table VIII. All but the magnesia molybdenum composites appeared to adequately withstand the required extrusion pressure at 2000°F. The use of zirconia-molybdenum and silica-molybdenum composites would not be recommended for liner use, however, because of the difficulty in grinding a smooth surface on such materials. The two alumina-molybdenum composites did not show any wear at the 2000°F extrusion temperature and, accordingly, were selected for extrusion tests at 3475°F using TZM alloy billet.

These two materials performed very well on this test. Again, there was no discernible wear on the test specimens. Unfortunately, the zirconia-coated H-13 steel die did not withstand the test. The relatively high exit velocity of the TZM alloy in the 1/4 in. wide die orifice apparently generated sufficient additional heat to wash out a relatively large section of the die bearing area. The extrusion section then washed out a section of similar size in both the 1-1/2 in. thick tool-steel shear blade and the 8 in. thick SAE 4340 steel bolster supporting the die. Although this eroding of die, shear blade, and bolster considerably increased extrusion resistance, ram force was still sufficiently great to completely extrude the 5 in. long TZM billet used for the test. A photograph of the test samples in the washed die is shown in Figure 24. It can be seen that composite samples are not worn or distorted, despite the lack of support caused by the erosion of the die.

Damage to the supporting tooling required disintegration, boring, heat treatment, and grinding of the bolster, and fabrication of a bolster insert and a new shear blade. This work was not completed before the close of the contract, preventing further testing of fiber-metal reinforced ceramic composite materials. On the basis of the work completed to date, there appears to be at least one composite material which successfully withstands extrusion temperatures in excess of 3475°F, and two materials which are suitable for use at billet temperatures of 2000°F.

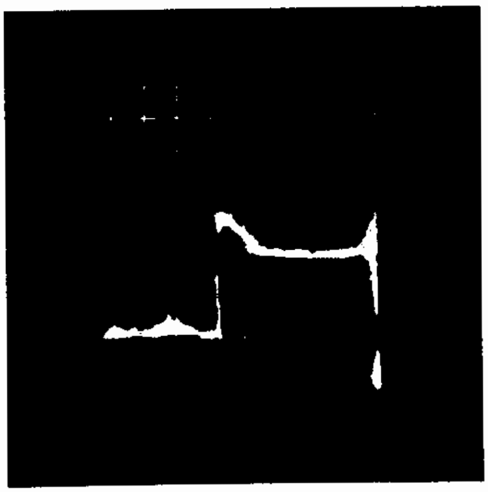
Neg. No. 26387 Fig. 20

Glass and Fiske Lube Lubricant



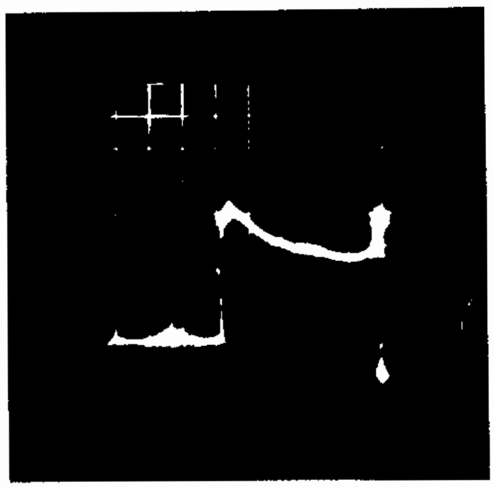
Neg. No. 26388 Fig. 21

Thin Glass Coating and Fiske-Lube Lubricant



Neg. No. 26389 Fig. 22

Fiske-Lube Lubricant Only



Neg. No. 26390 Fig. 23

No Lubricant

FIG. 20-23 - FORCE VS. STROKE PHOTOGRAMS FOR SAE 4340 STEEL EXTRUSIONS PRODUCED UNDER FOUR DIFFERENT LUBRICATION CONDITIONS. EXTRUSION LINER SURFACE HAS A GRADATED ALUMINA-NICKEL COATING.

Neg. No. 26381

FIG. 24 - FIBER METAL REINFORCED CERAMIC TEST SPECIMENS AFTER EXTRUSION TRIAL AT 3450°F.

TABLE VIII

FIBER-METAL REINFORCED CERAMIC EXTRUSION EVALUATION

Material Volume Composition	Temp., °F	Ram Velocity, in/min (b)	Peak Force, tons	Stroke, in.	Material surface after extrusion
$72Al_2O_3$-28Mo	2000	425-475	750	6.4	Excellent
$59.5Al_2O_3$-40.5Mo	2000	425-475	750	6.4	Excellent
74MgO-26Mo	2000	320-340	700	6.2	Some erosion
74MgO-26Mo	2000	320-340	700	6.2	Light erosion
$84ZrO_2$-16Mo	2000	350-510	750	6.8	Excellent
$83ZrO_2$-17Mo*	2000	350-510	750	6.8	Good
$91SiO_2$-9Mo*	2000	330-350	710	6.4	Good
$89SiO_2$-11Mo*	2000	330-350	710	6.4	Good
82TiC-18W	2000	450-500	720	7.2	Excellent
78TiC-22W	2000	450-500	720	7.2	Excellent
$72Al_2O_3$-28Mo	3475	410-470	810	5.7	Excellent
$59.5Al_2O_3$-40.5Mo	3475	410-470	810	5.7	Excellent

*Rough specimen surfaces were obtained because of difficulty in grinding.

XI. SUMMARY AND CONCLUSIONS

A. General

Two major project goals were achieved by this effort. The first was the development of support tooling which would make possible the employment of superalloys, ceramic-coated steels, and solid ceramics as extrusion container liner materials. The second was the extrusion evaluation of a number of promising candidate liner materials which had not heretofore been used as liner materials. This activity, by enlarging the scope of the extrusion art, has both delineated fertile areas for future development activity and produced time- and money-saving techniques and designs which are immediately applicable to current extrusion practice. A modest materials development and evaluation program, investigating the utility of fiber-metal reinforced ceramic composites for extrusion liner application, determined that materials of such type can be made which are capable of withstanding the temperatures and pressures encountered in refractory metal extrusion practice. Items of specific accomplishment are listed in the following sections.

B. Achievement in Liner Support Tooling Design

1. Fast Liner Interchange Capability

Extrusion container liners in present use are shrink-fitted into the container and must be removed by differential heating techniques. Such procedures are time-consuming, frequently tedious, and therefore, costly. Occasionally, shrink-fitted liners cannot be removed by differential heating, necessitating liner boring.

A liner-sleeve assembly has been designed and performance tested on this project, which can be used in the extrusion container without the necessity of shrink-fitting. The assembly can be installed or removed in a matter of minutes by light pressing, making it possible to change liner size and replace worn liners at a small fraction of the time and cost presently required for such operations. This procedure also makes possible the use of liner materials of low thermal conductivity, which cannot be removed from the container by differential heating, such as solid ceramics and ceramic coated steel liners.

2. Reduction in Extrusion Liner Coat

Present extrusion liners in commercial service weigh 300 to 1000 lb. Such liners are fabricated from relatively costly H-12 tool steel, which in turn, cause high liner costs. The liner support tooling which was developed on this project enables the use of an H-12 steel liner of relatively thin wall weighing from 20 to 50 lb, and costs considerably less than the heavy-wall liners in present use. Service life of the light-weight liners is identical to that of the heavy liners.

3. Support Tooling Designs for Ceramic-Coated Steel Liners and Superalloy Liners

Ceramic-coated steel liners and superalloy liners cannot be used in present-day extrusion containers at stem pressures of 180,000 psi. If liner bores are ceramic coated, ceramic elastic strain capability will be exceeded at high stem pressure and the coating will spall. Superalloy liners will yield and distort under similar loading conditions.

Support tooling designs have been developed on this project which make it possible to use ceramic coatings on liners and superalloy liners at stem pressures of 180,000 psi.

4. Support Tooling Design and Assembly Technique for Solid Ceramic Liners

Support tooling and assembly techniques were developed which make it possible to generate a sufficiently high compressive stress in the liner to prevent the ceramic from operating in tension on any axis at a 180,000 psi stem pressure and at a container temperature of 600°F. Ceramics can be stressed to a desired compressive level without cracking.

5. Application of Design Procedures for Support Liners of Larger Sizes and Different Materials

A step-by-step design procedure has been developed for support of superalloy, ceramic-coated steel and solid ceramic liners. The calculation of dimensions of required support tooling for a given liner size in a particular material may be made by inserting liner constants in a series of formulae, and by performing the indicated arithmetic operations. Such calculation may thus be carried out by clerical personnel with a minimum of engineering guidance, substantially reducing design costs.

C. Achievement in the Application of New Materials for Extrusion Container Liner Use

1. Cast Udimet 700 Alloy Thin-Wall Liner

A cast Udimet 700 alloy liner with a 1/4 in. thick wall was developed which will not plastically deform under a 180,000 psi stem pressure and does not show any indication of wear when extruding steel billets at 2000°F. It has been demonstrated that superalloys possessing lower mechanical properties thant U-700 have a longer service life in red metal extrusion than in conventional tool steels. Hence, it is likely that the U-700 alloy should be particularly serviceable for red metal extrusion by virtue of its superior mechanical properties at red metal extrusion temperatures and its relatively low cost.

2. Rokide-Process Stabilized Zirconia-Coated Extrusion Liner

A Rokide-process stabilized zirconia coating, applied to a rough-threaded tool steel liner, was evaluated and withstood billet temperatures of 3400°F and protected support tooling from damage due to "stickers" or billet butts at temperatures of 2800°F. Obtaining reproducible results from liner-to-liner is still a problem with this process. Quality control variables during ceramic flame spraying remain to be determined.

3. Plasma-Arc Process Laminar Stabilized Zirconia-Coated Extrusion Liner

A plasma-arc sprayed coating of stabilized zirconia on a Nichrome undercoat was found to be suitable for extrusion of steel billets at 2000°F.

4. Plasma-Arc Process Gradated Alumina-Nickel Coated Extrusion Liner

A plasma-arc sprayed coating in which alumina and nickel powders are mixed and sprayed with increasing alumina ratio was found to be suitable for extrusion of steel billets at 2000°F.

5. Friction Reduction Achieved by Ceramic-Coated Liners

It was determined that it is not necessary to use a glass lubricant when extruding steel billets in a gradated alumina-nickel coated extrusion liner. The use of a suitable low-cost container lubricant alone, provides the same friction reduction that container lubricant and glass together provide when using conventional tool steel extrusion liners.

The capability of friction reduction of ceramic-coated liners was evaluated for the gradated alumina-nickel liner only. However, it is possible that a glass lubricant can be dispensed within steel extrusion using any ceramic-surfaced liner.

D. Achievements in the Development of Fiber-Metal Reinforced Ceramic Materials for Extrusion Liner Use

Four materials were developed which showed promise in extrusion evaluation screening tests. Mixtures of $72Al_2O_3$-28Mo and $59.5Al_2O_3$-40.5Mo proved capable of withstanding billet temperatures of 3475°F and stem pressure of 160,000 psi without any apparent wear. Mixtures of 78TiC-22W and 82TiC-18W proved capable of withstanding billet temperatures of 2000°F.

E. Areas for Further Extrusion Liner Development Efforts

The scope of effort necessarily restricted the number of promising avenues of development which could be pursued on this project. Accordingly, several promising approaches could not be followed up. Also, the time and effort required for the development of suitable support tooling and the difficulty of vendors in supplying the required liner material, prevented some promising liner materials from being extrusion evaluated.

The following technical activities would merit study in furthering extrusion liner development:

a. Determining optimum flame-spraying conditions for the production of Rokide-process stabilized zirconia coatings on extrusion liners.

b. Evaluating the service life of plasma-arc laminar stabilized zirconia coatings in industrial extrusion trials.

c. Determining the extrudability of refractory metals at 3400°F without the use of glass lubricants when using suitable ceramic-coated liners.

d. Extrusion-evaluating solid ceramic liners of titanium diboride, titanium carbide, zirconium carbide, high-purity alumina, and Lucalox.

APPENDIX

DESIGN 1: 1000-TON PRESS CONTAINER CALCULATION FOR A LINER HELD IN COMPRESSION UNDER ALL EXTRUSION CONDITIONS

A. General Procedure for Container Design Calculations

The design calculation is divided into a number of objectives, and steps for achieving the specific objectives. Objectives are designated by numbers, related sequential steps by lower case letters. All equations and their solutions are written in the order used in the calculation. Graphs of liner, sleeve, and container stress as a function of radial position are plotted for three conditions:

(1) Stress due to shrink fitting

(2) Stress due to a 207,000-210,000 psi extrusion pressure

(3) Stress due to combined effect of shrink fitting and 210,000 psi extrusion pressure.

Since any given stress, interfacial pressure, or displacement may be a function of as many as ten variables, and many such functions must be calculated, designation of a particular function by a single symbol becomes impractical. Instead, a notation has been devised to describe the type of the variable, its location, and factors which produce it. The system used may be described as follows:

(1) Stresses

Stresses are identified by the letter S followed by a series of subscripts. The first subscript, or group of subscripts, describes the location of the stress. This is followed by a letter T or R indicating whether the stress is tangential or radial. Additional numerals or letters describe the factors causing the stress. As examples, S_{LR1} indicates a stress throughout the liner which is radial, and generated by the first sleeve. $S_{(1,2,3)T4}$ would be a tangential stress throughout the first three sleeves, generated by the fourth sleeve. $S_{(L,1,2,3,4)\,R(e2)}$ is a radial stress acting through the liner and four sleeves caused by stem pressure P_{e2}.

(2) Pressures

Interfacial pressures (not stem pressures) are identified by the letter P followed by subscripts. The first subscript group before a comma identifies the location of the pressure. Following subscripts denote the cause of the pressure. As examples, $P_{A, e2}$ indicates a pressure acting at a radial distance A from the container centerline, which is generated by stem pressure P_{e2}. $P_{L1, 4}$ indicates a pressure acting at the interface between the liner and the first sleeve, generated by the fourth sleeve.

(3) Radial Displacements

Radial displacements are identified by the letter U followed by subscripts. The first character indicates the liner, or sleeve number involved. The second letter indicates the location of the displacement, and numbers following a comma indicate the cause of the displacement. As examples, $U_{4F, 4}$ indicates a displacement of the fourth sleeve at a radial distance F from the container centerline, generated by the fourth sleeve. $U_{LB, e2}$ indicates a displacement of the liner at distance B, generated by extrusion pressure P_{e2}. $U_{(1, 2, 3, 4)F, e2}$ indicates a displacement of first-second-third-fourth combination sleeve assembly at a distance F due to extrusion pressure P_{e2}.

This notation system is also useful for describing elastic moduli and linear thermal expansion coefficients. In these cases, notation is simplified, because only two different materials are used at three different temperatures. Elastic moduli are identified by the letter Y, thermal expansion coefficient by α. Subscripts indicate operating temperature, and whether material is a ceramic liner type or a steel. Examples of subscript application are Y_{S600}, indicating an elastic modulus of steel at 600° F; Y_{L75}, elastic modulus of a liner at 75° F; or α_{S800}, thermal expansion coefficient of steel at 800° F.

B. Specific Procedure

1. Liner-First Sleeve Interference at 600°F and Dimension of First Sleeve Inner Radius at 75°F

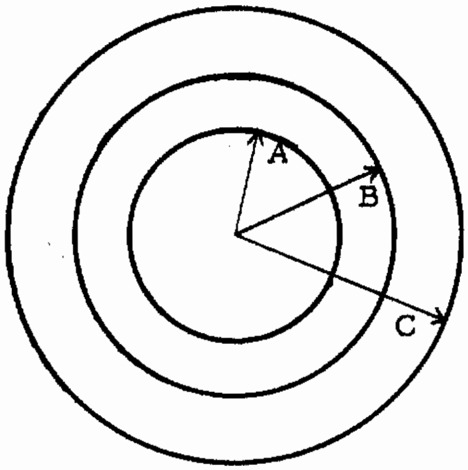


FIG. 25 - LINER AND FIRST SLEEVE ASSEMBLY

t = temperature, °F

A = liner inner radius = 1.804 in., nominal

B = liner outer radius and 1st sleeve inner radius = 2.065 in., nominal

C = 1st sleeve outer radius = 2.315 in., nominal

Y_{L600} = 47.7×10^6 psi

Y_{S600} = 25.3×10^6 psi

α_{L600} = 3.98×10^{-6}/°F

α_{S600} = 6.66×10^{-6}/°F

α_{S1000} = 7.22×10^{-6}/°F

a. Determine 1st sleeve inner radius at 1025°F which satisfies clearance requirement for liner at 75°F.

Liner outer radius at 75°F is 2.065 in. When 1st sleeve is heated to 1025°F, let there be a 0.003 in. clearance between liner and 1st sleeve. Inner radius of 1st sleeve is then 2.068 in. at 1025°F.

$$B_{1025} = 2.068$$

Unstressed inner radius of 1st sleeve at 600°F is:

$$B_{1S600} = \frac{B_{1025}(1 + a_{S600}\,\Delta t_{600-75})}{1 + a_{S1000}\,\Delta t_{1025-75}} = 2.0612 \text{ in.}$$

b. Determine unstressed dimensions of liner outer radius and 1st sleeve inner radius at 600°F, to obtain interference at 600°F

Liner outer radius is

$$B_{L600} = B_{L75}\,(1 + a_{L600}\,\Delta t_{600-75})$$

$$B_{L600} = 2.9693 \text{ in.}$$

First sleeve inner radius is

$$B_{1S600} = B_{75}\,(1 + a_{S600}\,\Delta t_{600-75})$$

$$= \frac{B_{1025}(1 + a_{S600}\,\Delta t_{600-75})}{1 + a_{S1000}\,\Delta t_{1025-75}}$$

$$B_{1S600} = 2.0612 \text{ in.}$$

Difference in unstressed liner outer radius and unstressed 1st sleeve inner radius at 600°F is

$$\beta_{L1} = 2.0693 - 2.0612 = 0.0081 \text{ in.}$$

c. Determine unstressed 1st sleeve inner radius at 75°F, to obtain machining dimension

$$B_{75} = \frac{B_{1025}}{1 + a_{1000}\,\Delta t_{1025-75}} = 2.0549 \text{ in.}$$

Machined size of 1st sleeve inner radius, at 75°F, is 2.0549 in.

2. Stress on Liner and First Sleeve Due to Liner-First Sleeve Shrink Fit, at 600°F

a. Determine deformation of liner outer radius and 1st sleeve inner radius due to 1st sleeve shrink, in terms of liner-1st sleeve interfacial pressure

Deformation of liner outer radius due to 1st sleeve shrink is

$$U_{LB,1} = -\left[\frac{BP_{L1}}{Y_{L600}}\right]\left[\frac{A^2 + B^2}{B^2 - A^2} - \mu\right] = -3.11 \times 10^{-7}\,P_{L1}$$

$$\mu = \text{Poisson's ratio} = 0.3$$

Deformation of 1st sleeve inner radius due to 1st sleeve shrink is

$$U_{1B,\,1} = \left[\frac{BP_{L1}}{Y_{S600}}\right]\left[\frac{B^2 + C^2}{C^2 - B^2} + \mu\right] = 7.13 \times 10^{-7}\, P_{L1}$$

b. Add the two deformations and equate to liner-1st sleeve interference. Solve for interfacial pressure.

$$U_{LB,\,1} + U_{1B,\,1} = \beta_{L1} \quad \text{(absolute value sum)}$$

$$8.1 \times 10^{-3} = 10.3 \times 10^{-7}\, P_{L1}$$

$$P_{L1,\,1} = 7.91 \times 10^{-3} \text{ psi}$$

Interfacial pressure between liner and 1st sleeve is 7,910 psi.

c. Determine tangential and radial stress in liner.

$$S_{LT,\,1} = -\frac{P_{L1}B^2}{B^2 - A^2}\left[1 + \frac{A^2}{r^2}\right] = -33,600 - \frac{112,000}{r^2}$$

At $r = A$, $S_{LT} = -67,200$ psi.

Radial compressive stress on liner is

$$S_{LR,\,1} = -33,600 + \frac{112,000}{r^2}$$

At $r = A$, $S_{LR} = 0$.

d. Determine tangential and radial stress in 1st sleeve.

$$S_{1T,\,1} = \frac{P_{L1}B^2}{C^2 - B^2}\left[1 + \frac{C^2}{r^2}\right] = 29,400 + \frac{158,000}{r^2}$$

$$S_{1R,\,1} = 29,400 - \frac{158,000}{r^2}$$

3. <u>Stressed First Sleeve Outer Radius, at 75°F</u>

a. Determine interference between unstressed liner outer radius and 1st sleeve inner radius.

At 75°F, liner outer radius is 2.0650 in., and 1st sleeve inner radius is 2.0459 in.

$$Y_{L1} = 2.0650 - 2.0549 = 0.0101 \text{ in.}$$

b. Determine deformation of liner outer radius due to 1st sleeve shrink, in terms of interfacial pressure between liner and 1st sleeve.

Using results of 2a, deformation of liner outer radius due to 1st sleeve shrink is

$$U_{LB,1} = -3.11 \times 10^{-7} P_{L1} \left[\frac{Y_{L600}}{Y_{L75}} \right] = -3.08 \times 10^{-7} P_{L1}$$

where $Y_{L75} = 4.80 \times 10^7$ psi.

c. Determine deformation of 1st sleeve inner radius due to 1st sleeve expansion in terms of interfacial pressure between liner and 1st sleeve.

Using results of 2a, deformation of 1st sleeve inner radius, due to 1st sleeve expansion, is

$$U_{1B,1} = 7.13 \times 10^{-7} P_{L1} \left[\frac{Y_{S600}}{Y_{S75}} \right] = 6.15 \times 10^{-7} P_{L1}$$

d. Equate absolute value sum of deformation of liner and 1st sleeve to interference, and solve for interfacial pressure.

$$U_{LB,1} + U_{1B,1} = \beta_{L1,1}$$

$$P_{L1,1} = 11,000 \text{ psi}$$

e. Determine deformation and outer radius of stressed sleeve.

$$U_{1C,1} = \frac{2B^2CP_{L1}}{Y_{S75}(C^2 - B^2)} = 0.0065 \text{ in.}$$

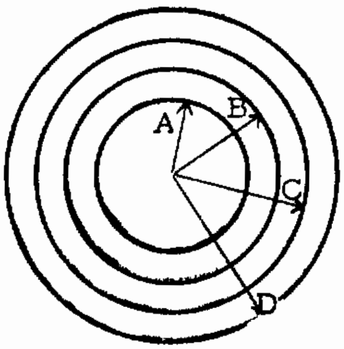
Outer radius of unstressed 1st sleeve is 2.3049 in.

Outer radius of stressed 1st sleeve, at 75°F, is 2.3114 in.

f. Determine tangential stress at liner inner radius, at 75° F

$$S_{LT,1} = \frac{2P_{L1}B^2}{B^2 - A^2} = -93,400 \text{ psi}$$

4. <u>Pressure Generated at Liner-First Sleeve Interface and First Sleeve-Second Sleeve Interface by Second Sleeve Shrink Fit, at 600° F</u>



D = 2.565 in., nominal

FIG. 26 - LINER, FIRST, AND SECOND SLEEVE ASSEMBLY

a. Determine 2nd sleeve inner radius at 1025° F which satisfies clearance requirement for 1st sleeve stressed outer radius at 75° F

First sleeve outer radius, at 75° F, is 2.3114 in. when shrunk over liner. When 2nd sleeve is heated to 1025° F, let there be a 0.0050 in. clearance between 1st and 2nd sleeve. Inner radius of 2nd sleeve is then 2.3164 in. at 1025° F

Unstressed inner radius of 2nd sleeve at 600° F is

$$C_{2S600} = \frac{C_{2S1025}(1 + \alpha_{S600}\,\Delta t_{600-75})}{1 + \alpha_{S1000}\,\Delta t_{1025-75}} = 2.3086 \text{ in.}$$

b. Determine expansion of 1st sleeve outer radius at 600°F from interfacial pressure found in 2b.

$$U_{1C,1} = \frac{2B^2CP_{L1,\ 1}}{Y_{S600}(C^2 - B^2)} = 0.0054 \text{ in.}$$

c. Determine unstressed outer radius of 1st sleeve at 600°F and add to value in 4b to obtain stressed 1st sleeve outer radius

Unstressed 1st sleeve outer radius at 600°F is

$$B_{1S600} = B_{1S75}\,(1 + \alpha_{S600}\,\Delta t_{600-75}) = 2.3130 \text{ in.}$$

Stressed 1st sleeve outer radius, at 600°F, is 2.3130 + 0.0054 = 2.3184 in.

d. Subtract 4c results from 4a result to obtain 1st-2nd sleeve interference at 600°F.

$$\beta_{12} = 2.3184 - 2.3086 = 0.0098 \text{ in.}$$

e. Determine unstressed 2nd sleeve inner radius at 75°F to obtain machining dimension

$$C_{2S75} = \frac{C_{2S1025}}{1 + \alpha_{S1000}\Delta t_{1025-75}} = 2.3006 \text{ in.}$$

Machining dimension of 2nd sleeve inner radius is 2.3006 in.

f. Determine displacement of liner at liner-1st sleeve interface in terms of interfacial pressure developed at this interface by 2nd sleeve, at 600°F

$$U_{LB,2} = -\frac{BP_{L1,\ 2}}{Y_{L600}}\left[\frac{A^2 + B^2}{B^2 - A^2} - \mu\right] = -3.11 \times 10^{-7} P_{L1,2}$$

g. Determine displacement of 1st sleeve at liner-1st sleeve interface in terms of pressure exerted at liner-1st sleeve interface, and pressure exerted at 1st-2nd sleeve interface by 2nd sleeve, at 600°F

$$U_{1B,2} = \left[\frac{1-\mu}{Y_{S600}}\right]\left[\frac{B^2 P_{L1,2} - C^2 P_{12,2}}{C^2 - B^2}\right] B + \left[\frac{1+\mu}{Y_{S600}}\right]\left[\frac{B^2 C^2 (P_{L1,2} - P_{12,2})}{B(C^2 - B^2)}\right]$$

$$U_{1B,2} = 7.12 \times 10^{-7} P_{L1,2} - 7.69 \times 10^{-7} P_{12,2}$$

h. Equate expressions developed in 4f and 4g to obtain liner-1st sleeve interfacial pressure in terms of 1st-2nd sleeve interfacial pressure, at 600°F

$$-3.11 \times 10^{-7} P_{L1,2} = 7.12 \times 10^{-7} P_{L1,2} - 7.69 \times 10^{-7} P_{12,2}$$

$$P_{L1,2} = 0.752 P_{12,2}$$

i. Determine displacement of 1st sleeve outer radius due to $P_{L1,2}$ and $P_{12,2}$

$$U_{1C,2} = \frac{1-\mu}{Y_{S600}}\left[\frac{B^2 P_{L1,2} - C^2 P_{12,2}}{C^2 - B^2}\right] C + \frac{1+\mu}{Y_{S600}}\left[\frac{B^2 C^2 (P_{L1,2} - P_{12,2})}{(C^2 - B^2) C}\right]$$

$$U_{1C,2} = 7.03 \times 10^{-7} P_{L1,2} - 7.67 \times 10^{-7} P_{L2,2}$$

j. Substitute result of 4h in 4i to express displacement of 1st sleeve outer radius as a function of $P_{12,2}$ only

$$U_{1C,2} = -2.39 \times 10^{-7} P_{12,2}$$

k. Determine displacement of inner radius of 2nd sleeve due to $P_{12,2}$

$$U_{2C,2} = \frac{C P_{12,2}}{Y_{S600}}\left[\frac{C^2 + D^2}{D^2 - C^2} + \mu\right] = 9.84 \times 10^{-7} P_{12,2}$$

l. Add absolute value of 4j and 4k and equate to 4d. Solve for $P_{12,2}$

$$(2.39 + 9.84)\,10^{-7}\,P_{12,2} = 0.0098$$

$$P_{12,2} = 8,010 \text{ psi}$$

m. Substitute 4l in 4h to obtain $P_{L1,2}$

$$P_{L1,2} = 6,020 \text{ psi}$$

5. <u>Stress Generated in Liner, First, and Second Sleeve Due to First-Second Sleeve Shrink Fit, at 600°F</u>

a. Determine tangential and radial stress in liner

$$S_{LT2} = -\frac{P_{L1,2}B^2}{B^2 - A^2}\left[1 + \frac{A^2}{r^2}\right] = -25,500 - \frac{82,700}{r^2}$$

$$S_{LR2} = -25,500 + \frac{82,700}{r^2}$$

At $r = A$, $S_{LR2} = 0$

b. Determine tangential and radial stress in 1st sleeve

$$S_{1T2} = \frac{B^2P_{L1,2} - C^2P_{12,2}}{C^2 - B^2} + \frac{(P_{L1,2} - P_{12,2})\,B^2C^2}{r^2\,(C^2 - B^2)}$$

$$S_{1T2} = -15,400 - \frac{39,800}{r^2}$$

$$S_{1R2} = -15,400 + \frac{39,800}{r^2}$$

c. Determine tangential and radial stress in 2nd sleeve

$$S_{2T2} = \frac{C^2P_{12,2}}{D^2 - C^2}\left[1 + \frac{D^2}{r^2}\right] = 37,000 + \frac{242,000}{r^2}$$

$$S_{2R2} = 37,000 - \frac{242,000}{r^2}$$

6. Outer Radius of Stressed Second Sleeve Due to First-Second Sleeve Shrink Fit, at 75°F

a. Determine interference between stressed 1st sleeve outer radius and unstressed 2nd sleeve inner radius at 75°F, by use of values in 3 e and 4e

$$Y_{12} = 2.3114 - 2.3006 = 0.0108 \text{ in.}$$

b. Determine displacement of liner at liner-1st sleeve interface in terms of interfacial pressure developed at this interface by 2nd sleeve, at 75°F, from results of 4f

$$U_{LB,2} = -3.11 \times 10^{-7} P_{L1,2} \left[\frac{Y_{L600}}{Y_{L75}} \right] = -3.09 \times 10^{-7} P_{L1,2}$$

c. Determine displacement of 1st sleeve at liner-1st sleeve interface in terms of pressure exerted at liner-1st sleeve interface, and pressure exerted at 1st-2nd sleeve interface by 2nd sleeve, at 75°F, from 4g

$$U_{1B,2} = (7.12 P_{L1,2} - 7.69 P_{L2,2})\, 10^{-7} \left[\frac{Y_{S600}}{Y_{S75}} \right]$$

$$U_{1B,2} = (6.15 P_{L1,2} - 6.64 P_{L2,2})\, 10^{-7}$$

d. Equate expressions developed in 6b and 6c to obtain liner-1st sleeve interfacial pressure in terms of 1st-2nd sleeve interfacial pressure, at 75°F

$$-3.09 \times 10^{-7} P_{L1,2} = 6.15 \times 10^{-7} P_{L1,2} - 6.64 \times 10^{-7} P_{L2,2}$$

$$P_{L1,2} = 0.718\, P_{L2,2}$$

e. Determine displacement of 1st sleeve outer radius due to $P_{L1,2}$ and $P_{12,2}$ at 75°F, from 4i

$$U_{1C,2} = (7.03\, P_{L1,2} - 7.67 P_{L2,2})\, 10^{-7} \left[\frac{Y_{S600}}{Y_{S75}} \right]$$

$$U_{1C,2} = (6.08\, P_{L1,2} - 6.64 P_{L2,2})\, 10^{-7}$$

f. Use 6d in 6e to express displacement of 1st sleeve outer radius as a function of $P_{12,2}$ only

$$U_{1C,2} = -2.27 \times 10^{-7} P_{12,2}$$

g. Determine displacement of inner radius of 2nd sleeve due to $P_{12,2}$ at 75°F, from 4k.

$$U_{2C,2} = 9.84 \times 10^{-7} P_{12,2} \left[\frac{Y_{S600}}{Y_{S75}}\right]$$

$$U_{2C,2} = 8.48 \times 10^{-7} P_{12,2}$$

h. Add absolute values of 6f and 6g and equate to 6a. Solve for $P_{12,2}$.

$$(2.27 + 8.48)\, 10^{-7} P_{12,2} = 0.0108$$

$$P_{12,2} = 11,000 \text{ psi}$$

i. Substitute 6h in 6d to obtain $P_{L1,1}$

$$P_{L1,2} = 7,900 \text{ psi}$$

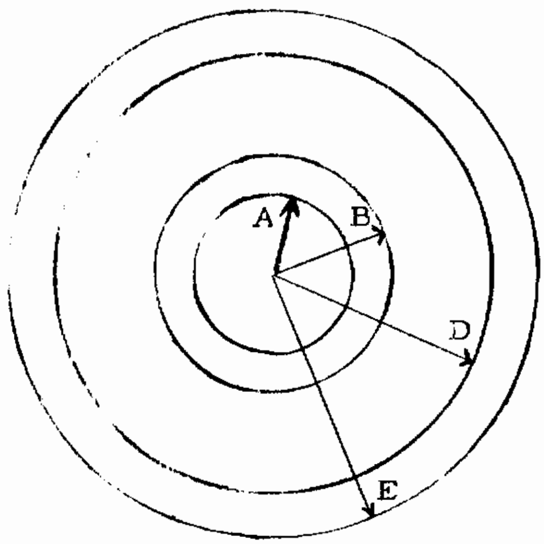
j. Calculate expansion of 2nd sleeve outer radius at 75°F due to $P_{12,2}$.

$$U_{2D,2} = \frac{2C^2DP_{12,2}}{Y_{S600}(D^2 - C^2)} = 0.0086 \text{ in.}$$

Outer radius of unstressed 2nd sleeve, at 75°F, is 2.5606 in.

Outer radius of stressed 2nd sleeve, at 75°F, is 2.5692 in.

7. <u>Pressure Generated at Liner-First Sleeve Interface and Second-Third Sleeve Interface by Third Sleeve Shrink Fit, at 600°F</u>



E = 2.810 in., nominal

FIG. 27 - LINER, FIRST-SECOND SLEEVE COMBINATION, AND THIRD SLEEVE ASSEMBLY

a. Determine 3rd sleeve inner radius at 1025° F which satisfies clearance requirement for 2nd sleeve stressed outer radius, at 75° F

Second sleeve outer radius, at 75° F, is 2.5692 in. when shrunk over first sleeve. When 3rd sleeve is heated to 1025° F, let there be a 0.0050 in. clearance between 2nd and 3rd sleeve. Inner radius of 3rd sleeve is then 2.5742 in., at 1025° F.

Unstressed inner radius of 3rd sleeve, at 600° F, is

$$D_{3S600} = \frac{D_{3S1025}(1 + \alpha_{S600}\Delta t_{600-75})}{1 + \alpha_{S1000}\Delta t_{1025-75}}$$

b. Determine expansion of 2nd sleeve outer radius at 600° F from interfacial pressure found in 4L

$$U_{2D2} = \frac{2C^2 DP_{12,2}}{Y_{S600}(D^2 - C^2)} = 0.0075 \text{ in.}$$

c. Determine unstressed outer radius of 2nd sleeve at 600° F and add to value in 7b to obtain stressed 1st sleeve outer radius

$$C_{2S600} = C_{2S75}\,(1 + \alpha_{S600}\,\Delta t_{600-75}) = 2.5695 \text{ in.}$$

Stressed 2nd sleeve outer radius, at 600° F, is 2.5696 + 0.0075 = 2.5771 in.

d. Subtract 7c from 7a to obtain 2nd-3rd sleeve interference at 600° F

$$\beta_{23} = 2.5771 - 2.5657 = 0.0114 \text{ in.}$$

e. Determine unstressed 3rd sleeve inner radius at 75° F to obtain machining dimension

$$D_{3S75} = \frac{D_{3S1025}}{1 + \alpha_{S1000}\Delta t_{1025-75}} = 2.5566 \text{ in.}$$

Machining dimension of 3rd sleeve inner radius is 2.5566 in.

f. Determine displacement of liner at liner-1st sleeve interface in terms of interfacial pressure developed at this interface by 3rd sleeve, at 600°F

Displacement of liner at liner-1st sleeve interface due to pressure $P_{L1,3}$ at 600°F is

$$U_{LB,3} = \frac{BP_{L1,3}}{Y_{L600}}\left[\frac{A^2 + B^2}{B^2 - A^2} - \mu\right] = -3.11 \times 10^{-7} P_{L1,3}$$

g. Determine displacement of 1st-2nd sleeve combination at liner-1st sleeve interface in terms of pressure exerted at liner-1st sleeve interface and pressure exerted at 2nd-3rd sleeve interface by 3rd sleeve, at 600°F

$$U_{(1,2)B,3} = \left[\frac{1-\mu}{Y_{S600}}\right]\left[\frac{B^2P_{L1,3} - D^2P_{23,3}}{D^2 - B^2}\right] B + \left[\frac{1+\mu}{Y_{S600}}\right]\left[\frac{B^2D^2(P_{L1,3} - P_{23,3})}{B(D^2 - B^2)}\right]$$

$$U_{(1,2)B,3} = 3.99 \times 10^{-7} P_{L1,3} - 4.56 \times 10^{-7} P_{23,3}$$

h. Equate expressions developed in 7f and 7g to obtain liner-1st sleeve interfacial pressure in terms of 2nd-3rd sleeve interfacial pressure, at 600°F

$$-3.11 \times 10^{-7} P_{L1,3} = 3.99 \times 10^{-7} P_{L1,3} - 4.56 \times 10^{-7} P_{23,3}$$

$$P_{L1,3} = 0.643\, P_{23,3}$$

i. Determine displacement of 2nd sleeve outer radius due to $P_{L1,3}$ and $P_{23,3}$

$$U_{(1,2)D,3} = \frac{1-\mu}{Y_{S600}}\left[\frac{B^2P_{L1,3} - D^2P_{23,3}}{D^2 - B^2}\right] D + \frac{1+\mu}{Y_{S600}}\left[\frac{B^2D^2(P_{L1,3} - P_{23,3})}{(D^2 - B^2)D}\right]$$

$$U_{(1,2)D,3} = (2.86\, P_{L1,3} - 3.14\, P_{23,3})\, 10^{-7}$$

j. Substitute 7h in 7i to express displacement of 2nd sleeve outer radius as a function of $P_{23,3}$ only

$$U_{(1,2)D,3} = -1.30 \times 10^{-7} P_{23,3}$$

k. Determine displacement of inner radius of 3rd sleeve due to $P_{23,3}$

$$U_{3D,3} = \frac{DP_{23,3}}{Y_{S600}} \left[\frac{D^2 + E^2}{E^2 - D^2} + \mu \right] = 12.0 \times 10^{-7} P_{23,3}$$

l. Add absolute value of 7j and 7k and equate to 7d. Solve for $P_{23,3}$

$$(1.30 + 12.0)\, 10^{-7} P_{23,3} = 0.0114$$

$$P_{23,3} = 8,570 \text{ psi}$$

m. Substitute 7l in 7h to obtain $P_{L1,3}$

$$P_{L1,3} = 5,510 \text{ psi}$$

8. <u>Stress Generated in Liner, First-Second Sleeve Combination, and Third Sleeve, due to Second-Third Sleeve Shrink Fit, at 600°F</u>

a. Determine tangential and radial stress in liner, from 7m

$$S_{LT3} = -\frac{P_{L1,3} B^2}{B^2 - A^2} \left[1 + \frac{A^2}{r^2} \right] = -23,400 - \frac{75,700}{r^2}$$

At $r = A$, $S_{LT3} = -46,800$ psi

$$S_{LR3} = -23,400 + \frac{75,700}{r^2}$$

At $r = A$, $S_{LR3} = 0$

b. Determine tangential and radial stress in 1st-2nd sleeve combination

$$S_{(1,2)T3} = \frac{B^2 P_{L1,3} - D^2 P_{23,3}}{D^2 - B^2} + \frac{(P_{L1,3} - P_{23,3}) B^2 D^2}{r^2 (D^2 - B^2)}$$

$$S_{(1,2)T3} = -14,300 - \frac{36,800}{r^2}$$

$$S_{(1,2)R3} = -14,300 + \frac{36,800}{r^2}$$

c. Determine tangential and radial stress in 3rd sleeve

$$S_{3T3} = \frac{D^2 P_{23,3}}{E^2 - D^2}\left[1 + \frac{D^2}{r^2}\right] = 41,600 + \frac{329,000}{r^2}$$

$$S_{3R3} = 41,600 - \frac{329,000}{r^2}$$

9. <u>Outer Radius of Stressed Third Sleeve, due to Shrink Fit, at 75°F</u>

a. Determine interference between stressed 2nd sleeve outer radius and unstressed 3rd sleeve inner radius at 75°F by use of values in 6j and 7e

$$\gamma_{23} = 2.5692 - 2.5566 = 0.0126 \text{ in.}$$

b. Determine displacement of liner at liner-1st sleeve interface in terms of interfacial pressure developed at this interface by 3rd sleeve, from 7f, at 75°F

$$U_{LB,3} = -3.11 \times 10^{-7} P_{L1,3}\left[\frac{Y_{L600}}{Y_{L75}}\right] = -3.09 \times 10^{-7} P_{L1,3}$$

c. Determine displacement of 1st-2nd sleeve combination at liner-1st sleeve interface in terms of pressure exerted at liner-1st sleeve interface, and pressure exerted by 2nd-3rd sleeve interface by 3rd sleeve, at 75°F, from results of 7g

$$U_{(1,2)B,3} = (3.99\ P_{L1,3} - 4.56\ P_{23,3})\ 10^{-7}\left[\frac{Y_{S600}}{Y_{S75}}\right]$$

$$U_{(1,2)B,3} = (2.68\ P_{L1,3} - 3.94\ P_{23,3})\ 10^{-7}$$

d. Equate expressions developed in 9b and 9c to obtain liner-1st sleeve interfacial pressure in terms of 2nd-3rd sleeve interfacial pressure, at 75°F

$$-3.09 \times 10^{-7}\ P_{L1,3} = 2.68 \times 10^{-7}\ P_{L1,3} - 3.94 \times 10^{-7} P_{23,3}$$

$$P_{L1,3} = 0.684\ P_{23,3}$$

e. Determine displacement of 2nd sleeve outer radius due to $P_{L1,3}$ and $P_{23,3}$ at 75°F, from 7i

$$U_{(1,2)D,3} = (2.86\ P_{L1,3} - 3.14\ P_{23,3})\ 10^{-7}\left[\frac{Y_{S600}}{Y_{S75}}\right]$$

$$U_{(1,2)D,3} = (2.46\ P_{L1,3} - 2.71\ P_{23,3})\ 10^{-7}$$

f. Use results of 9d in 9e to express displacement of 2nd sleeve outer radius as a function of $P_{23,3}$ only

$$U_{(1,2)D,3} = -1.03 \times 10^{-7} P_{23,3}$$

g. Determine displacement of inner radius of 3rd sleeve due to $P_{23,3}$ at 75°F from 7k

$$U_{3D,3} = 12.0 \times 10^{-7} P_{23,3}\left[\frac{Y_{S600}}{Y_{S75}}\right]$$

$$U_{3D,3} = 10.4 \times 10^{-7} P_{23,3}$$

h. Add absolute value of results of 9f and 9g and equate to 9a. Solve for $P_{23,3}$

$$(10.3 + 10.4)\ 10^{-7}\ P_{23,3} = 0.0126$$

$$P_{23,3} = 11,100 \text{ psi}$$

i. Substitute 9h in 9d to obtain $P_{L1,3}$

$$P_{L1,3} = 7,950 \text{ psi}$$

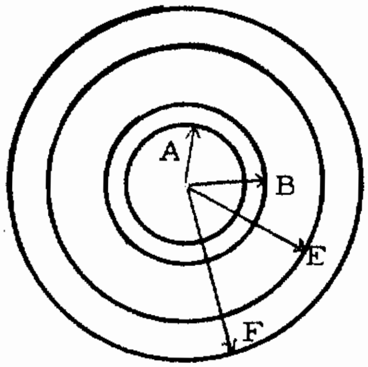
j. Calculate expansion of 3rd sleeve outer radius at 75°F due to $P_{23,3}$

$$U_{3E,3} = \frac{2D^2EP_{23,3}}{Y_{S600}(E^2 - D^2)} = 0.0105 \text{ in.}$$

Outer radius of unstressed 3rd sleeve, at 75°F, is 2.8166 in.

Outer radius of stressed 3rd sleeve, at 75°F, is 2.8271 in.

10. Pressure Generated at Liner-First Sleeve Interface and Third-Fourth Sleeve Interface by Fourth Sleeve Shrink Fit, at 600°F



F = 3.310 in., nominal

FIG. 28 - LINER, FIRST-SECOND-THIRD SLEEVE COMBINATION, AND FOURTH SLEEVE ASSEMBLY

a. Determine 4th sleeve inner radius at 1025°F which satisfies clearance requirement for 3rd sleeve stressed outer radius at 75°F

Third sleeve outer radius, at 75°F, is 2.8271 in. when shrunk over 2nd sleeve. When 4th sleeve is heated to 1025°F, let there be a 0.0060 in. clearance between 3rd and 4th sleeve. Inner radius of 4th sleeve is then 2.8331 in., at 1025°F.

Unstressed inner radius of 4th sleeve at 600°F is

$$E_{4S600} = \frac{E_{4S1025}(1 + \alpha_{S600} \Delta t_{600-75})}{1 + \alpha_{S1000} \Delta t_{1025-75}} = 2.8237 \text{ in.}$$

b. Determine expansion of 3rd sleeve outer radius at 600°F from interfacial pressure found in 7 l

$$U_{3E3} = \frac{2D^2 E P_{23,3}}{Y_{S600}(E^2 - D^2)} = 0.0092 \text{ in.}$$

c. Determine unstressed outer radius of 3rd sleeve at 600°F and add to value in 10b to obtain stressed 1st sleeve outer radius

Unstressed 3d sleeve outer radius at 600°F is,

$$C_{3S600} = C_{3S75} (1 + \alpha_{S600} \Delta t_{600-75}) = 2.8265 \text{ in.}$$

Stressed 3rd sleeve outer radius at 600°F is 2.8265 + 0.0092 = 2.8357 in.

d. Subtract 10a result from 10c result to obtain 3rd-4th sleeve interference at 600°F

$$\beta_{34} = 2.8357 - 2.8237 = 0.0120 \text{ in.}$$

e. Determine unstressed 4th sleeve inner radius at 75°F to obtain machining dimension

Unstressed 4th sleeve inner radius at 75°F is

$$E_{4S75} = \frac{E_{4S1025}}{1 + \alpha_{S1000}\Delta t_{1025-75}} = 2.8137 \text{ in.}$$

Machining dimension of 4th sleeve inner radius is 2.8137 in.

f. Determine displacement of liner at liner-1st sleeve interface in terms of interfacial pressure developed at this interface by 4th sleeve, at 600°F by use of 7f

$$U_{LB,4} = -3.11 \times 10^{-7} P_{L1,4}$$

g. Determine displacement of 1st-2nd-3rd sleeve combination at liner-1st sleeve interface in terms of pressure exerted at liner-1st sleeve interface and pressure exerted at 3rd-4th sleeve interface by 4th sleeve, at 600°F

$$U_{(1,2,3)B4} = \left[\frac{1-\mu}{Y_{S600}}\right]\left[\frac{B^2 P_{L1,4} - E^2 P_{34,4}}{E^2 - B^2}\right] B + \left[\frac{1+\mu}{Y_{S600}}\right]\left[\frac{B^2 E^2 (P_{L1,4} - P_{34,4})}{B(E^2 - B^2)}\right]$$

$$U_{(1,2,3)B,4} = 2.94 \times 10^{-7} P_{L1,4} - 3.51 \times 10^{-7} P_{34,4}$$

h. Equate expressions developed in 10f and 10g to obtain liner-1st sleeve interfacial pressure in terms of 3rd-4th sleeve interfacial pressure, at 600°F

$$-3.11 \times 10^{-7} P_{L1,4} = (2.94 P_{L1,4} - 3.51 P_{34,4})\, 10^{-7}$$

$$P_{L1,4} = 0.580\, P_{34,4}$$

i. Determine displacement of 3rd sleeve outer radius due to $P_{L1,4}$ and $P_{34,4}$

$$U_{(1,2,3)E,4} = \frac{1-\mu}{Y_{S600}}\left[\frac{B^2P_{L1,4} - E^2P_{34,4}}{E^2 - B^2}\right] E + \frac{1+\mu}{Y_{S600}}\left[\frac{B^2E^2(P_{L1,4} - P_{34,4})}{(E^2 - B^2)E}\right]$$

$$U_{(1,2,3)E,4} = 2.57 \times 10^{-7} P_{L1,4} - 3.34 \times 10^{-7} P_{34,4}$$

j. Substitute result of 10h in 10i to express displacement of 3rd sleeve outer radius as a function of $P_{34,4}$ only

$$U_{(1,2,3)E,3} = -1.85 \times 10^{-7} P_{34,4}$$

k. Determine displacement of inner radius of 4th sleeve due to $P_{34,4}$

$$U_{4E,4} = \frac{EP_{34,4}}{Y_{S600}}\left[\frac{E^2 + F^2}{F^2 - E^2} + \mu\right] = 7.09 \times 10^{-7} P_{34,4}$$

l. Add absolute value of 10j and 10k and equate to 10d. Solve for $P_{34,4}$

$$(1.85 + 7.09)\, 10^{-7} P_{34,4} = 0.0120$$

$$P_{34,4} = 13{,}400 \text{ psi}$$

m. Substitute 10 l in 10h to obtain $P_{L1,4}$

$$P_{L1,4} = 7{,}770 \text{ psi}$$

n. Calculate expansion of 4th sleeve outer radius at 600°F due to $P_{34,4}$

$$U_{4F,4} = \frac{2E^2FP_{34,4}}{Y_{S600}(F^2 - E^2)} = 0.0090 \text{ in.}$$

o. Calculate 4th sleeve unstressed outer radius at 600°F

Inner radius of container sleeve is 3.3100 in. at 75°F. Inner radius at 600°F will be

$$F_{600} = F_{75}(1 + \alpha\Delta t_{600-75}) = 3.220 \text{ in.}$$

Set clearance between container sleeve inner radius and 4th sleeve outer radius at 0.0003 in., at 600°F. Stressed 4th sleeve outer radius is then 3.3217 in., at 600°F.

From 10n, unstressed 4th sleeve outer radius is then 3.3217 - 0.0090 = 3.3127 in., at 600°F

p. Calculate 4th sleeve unstressed outer radius at 75°F

$$F_{75} = \frac{F_{600}}{1 + \alpha\Delta t_{600-75}} = 3.3101 \text{ in.}$$

Unstressed 4th sleeve outer radius, at 75°F, is 3.3101 in.

11. <u>Stress Generated in Liner, First-Second-Third Sleeve Combination, and Fourth Sleeve Due to Third-Fourth Sleeve Shrink Fit, at 600°F</u>

a. Determine tangential and radial stress in liner

$$S_{LT4} = -\frac{P_{L1,4}B^2}{B^2 - A^2}\left[1 + \frac{A^2}{r^2}\right] = -32,000 - \frac{106,000}{r^2}$$

At $r = A$, $S_{LT4} = -65,200$ psi

$$S_{LR4} = -32,600 + \frac{106,000}{r^2}$$

At $r = A$, $S_{LR4} = 0$

b. Determine tangential and radial stress in 1st-2nd-3rd sleeve combination

$$S_{(1,2,3)T4} = -\frac{B^2P_{L1,4} - E^2P_{34,4}}{E^2 - B^2} + \frac{(P_{L1,4} - P_{34,4})B^2E^2}{r^2(E^2 - B^2)}$$

$$S_{(1,2,3)T4} = -19,900 - \frac{51,500}{r^2}$$

$$S_{(1,2,3)R4} = -19,900 + \frac{51,500}{r^2}$$

c. Determine tangential and radial stress in 4th sleeve

$$S_{4T4} = \frac{E^2P_{34,4}}{F^2 - E^2}\left[1 + \frac{F^2}{r^2}\right] = 34,200 + \frac{376,000}{r^2}$$

$$S_{4R4} = 34,200 - \frac{376,000}{r^2}$$

12. Outer Radius of Stressed Fourth Sleeve Due to Shrink Fit, at 75°F

a. Determine interference between stressed 3rd sleeve outer radius and unstressed 4th sleeve inner radius at 75°F by use of 9j and 10e

$$Y_{34} = 2.8271 - 2.8137 = 0.0134 \text{ in.}$$

b. Determine displacement of liner at liner-1st sleeve interface in terms of interfacial pressure developed at this interface by 4th sleeve, from results of 10f, at 75°F,

$$U_{LB,4} = -3.11 \times 10^{-7} P_{L1,4} \left[\frac{Y_{L600}}{Y_{L75}}\right] = -3.09 \times 10^{-7} P_{L1,4}$$

c. Determine displacement of 1st-2nd-3rd sleeve combination at liner-1st sleeve interface in terms of pressure exerted at liner-1st sleeve interface, and pressure exerted by 3rd-4th sleeve interface by 4th sleeve, at 75°F, from 10g

$$U_{(1,2,3)B,4} = (2.94 P_{L1,4} - 3.51 P_{34,4})\, 10^{-7} \left[\frac{Y_{S600}}{Y_{S75}}\right]$$

$$U_{(1,2,3)B,4} = (2.54 P_{L1,4} - 3.03 P_{34,4})\, 10^{-7}$$

d. Equate expressions developed in 12b and 12c to obtain liner-1st sleeve interfacial pressure in terms of 3rd-4th sleeve interfacial pressure, at 75°F

$$-3.09 \times 10^{-7} P_{L1,4} = (2.54 P_{L1,4} - 3.03 P_{34,4})\, 10^{-7}$$

$$P_{L1,4} = 0.540 P_{34,4}$$

e. Determine displacement of 3rd sleeve outer radius due to $P_{L1,4}$ and $P_{34,4}$ at 75°F from results of 10i

$$U_{(1,2,3)E,4} = (2.57 P_{L1,4} - 3.34 P_{34,4})\, 10^{-7} \left[\frac{Y_{S600}}{Y_{S75}}\right]$$

$$U_{(1,2,3)E,4} = (2.22 P_{L1,4} - 2.88 P_{34,4})\, 10^{-7}$$

f. Use result of 12d in 12e to express displacement of 3rd sleeve outer radius as a function of $P_{34,4}$ only

$$U_{(1,2,3)E,4} = -1.68 \times 10^{-7} P_{34,4}$$

g. Determine displacement of inner radius of 4th sleeve due to $P_{34,4}$ from 10k, at 75°F

$$U_{4E,4} = 7.09 \times 10^{-7}\ P_{34,4}\left[\frac{Y_{S600}}{Y_{S75}}\right]$$

$$U_{4E,4} = 6.11 \times 10^{-7}\ P_{34,4}$$

h. Add absolute value of results of 12f and 12g and equate to 12a. Solve for $P_{34,4}$

$$(1.68 + 6.11) \times 10^{-7}\ P_{34,4} = 0.0134$$

$$P_{34,4} = 17,200 \text{ psi}$$

i. Substitute 12h in 12d to obtain $P_{L1,4}$

$$P_{L1,4} = 9,300 \text{ psi}$$

j. Calculate expansion of 4th sleeve outer radius at 75°F due to $P_{34,4}$

$$U_{4F,4} = \frac{2E^2FP_{34,4}}{Y_{S75}(F^2 - E^2)} = 0.0099 \text{ in.}$$

k. Calculate stressed 4th sleeve outer radius at 75°F by adding 10p and 12j

$$F_{4E} = 3.3101 + 0.0099 = 3.3200 \text{ in.}$$

Since container sleeve inner radius is 3.3100 in. at 75°F, a 0.0100 in. interference will be developed between 4th sleeve outer radius and container sleeve inner radius at 75°F.

13. Stress Developed in Liner-Sleeve Assembly Due to Combined Action of Shrink Fits, at 600°F

a. Determine tangential and radial stress in liner by adding appropriate equations in 2c, 5a, 8a, 11a

$$S_{LT1} + S_{LT2} + S_{LT3} + S_{LT4} = -116,000 - \frac{376,000}{r^2}; \quad A \leq r \leq B$$

$$S_{LR1} + S_{LR2} + S_{LR3} + S_{LR4} = -116,000 + \frac{376,000}{r^2}$$

b. Determine tangential and radial stress in 1st sleeve by adding appropriate equations in 2d, 5b, 8b, 11b

$$S_{1T1} + S_{1T2} + S_{(1,2)T3} + S_{(1,2,3)T4} = -20,200 + \frac{29,900}{r^2}; \ B \leq r \leq C$$

$$S_{1R1} + S_{1R2} + S_{(1,2)R3} + S_{(1,2,3)R4} = -20,200 - \frac{29,900}{r^2}$$

c. Determine tangential and radial stress in 2nd sleeve by adding appropriate equations in 5c, 8b, 11b

$$S_{2T2} + S_{(1,2)T3} + S_{(1,2,3)T4} = 2,800 + \frac{154,000}{r^2} \qquad C \leq r \leq D$$

$$S_{2R2} + S_{(1,2)R3} + S_{(1,2,3)R4} = 2,800 - \frac{154,000}{r^2}$$

d. Determine tangential and radial stress in 3rd sleeve by adding appropriate equations in 8c, 11b

$$S_{3T3} + S_{(1,2,3)T4} = 21,700 + \frac{277,500}{r^2} \qquad D \leq r \leq E$$

$$S_{3R3} + S_{(1,2,3)R4} = 21,700 - \frac{277,500}{r^2}$$

e. Determine tangential and radial stress in 4th sleeve from 11c

$$S_{4T4} = 34,200 + \frac{376,000}{r^2} \qquad D \leq r \leq E$$

$$S_{4R4} = 34,200 - \frac{376,000}{r^2}$$

Equations developed in section 13 are plotted in Fig. 29 to show stress due to shrink fitting as a function of radial distance in liner and each sleeve.

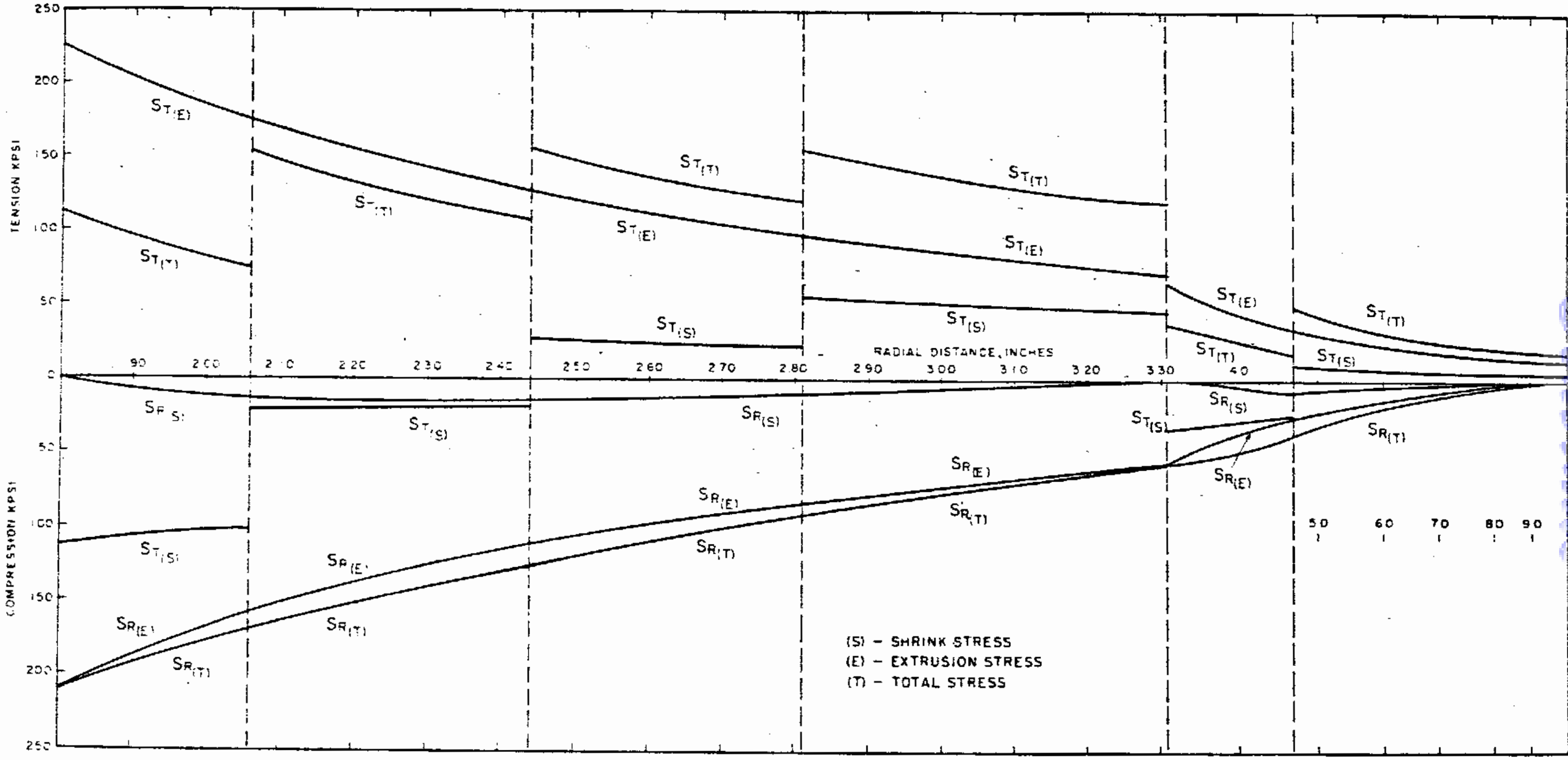


FIG. 29 LINER SLEEVE AND CONTAINER STRESS FOR FOUR SLEEVE ASSEMBLY AS A FUNCTION OF RADIAL DISTANCE FROM CONTAINER CENTERLINE

14. Calculation of Peak Tangential Stress in Liner Due to Combined Shrink Fits, at 75°F

a. Add liner-1st sleeve interfacial pressures developed by 1st, 2nd, 3rd, and 4th sleeves at 75°F. Values are found in 3d, 6i, 9i, 12i. Calculate stress in inner radius of liner

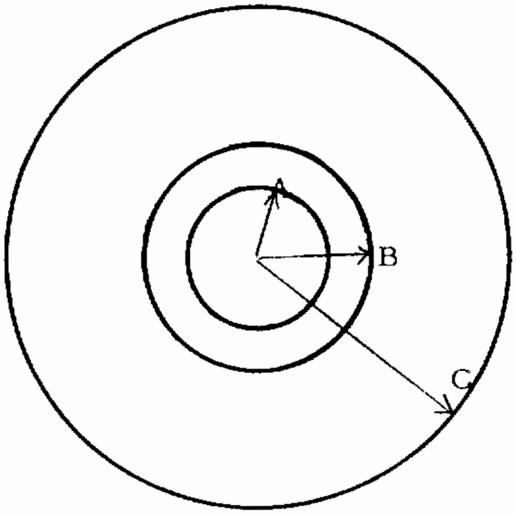
$$S_{LT(1,2,3,4)} = -\frac{2B^2}{B^2 - A^2}(P_{L1,1} + P_{L1,2} + P_{L1,3} + P_{L1,4})$$

$$S_{LT(1,2,3,4)} = -304{,}000 \text{ psi, at } 75°F$$

Ultimate compressive strength of the alumina liner is 340,000 psi. Hence, liner and sleeve assembly may be stored at room temperature, when not in service.

Section 12k shows that a 0.010 interference fit will develop between 4th sleeve outer radius and container sleeve inner radius, at 75°F. The additional compressive stress placed on the liner by this condition is likely to cause liner failure, since this stress adds to the 304,000 psi compressive stress already existing. Therefore, it will be necessary to remove liner-sleeve assembly from the container before container is allowed to cool. The 0.0001-0.0005 in clearance between 4th sleeve outer radius and container sleeve inner radius should enable removal of this assembly at 600°F without difficulty.

15. Extrusion Stress on Liner and Sleeves when Fourth Sleeve Outer Radius is Expanded 0.0005 in., at 600°F



$P_{A,el}$ = stem pressure required to expand 4th sleeve outer radius 0.0005 in.

FIG. 30 - LINER AND FIRST-SECOND-THIRD-FOURTH COMBINATION SLEEVE ASSEMBLY

a. Determine displacement of liner outer radius in terms of extrusion pressure on liner inner radius and liner-1st sleeve interfacial pressure

$$U_{LB, el} = \frac{1 - \mu}{Y_{L600}} \left[\frac{A^2 P_{A, el} - B^2 P_{B, el}}{B^2 - A^2} \right] B + \frac{1 + \mu}{Y_{L600}} \left[\frac{A^2 B^2 (P_{A, el} - P_{B, el})}{(B^2 - A^2) B} \right]$$

$$U_{LB, el} = 2.80 \times 10^{-7} P_{A, el} - 3.10 \times 10^{-7} P_{B, el}$$

b. Determine displacement of 1st-2nd-3rd-4th sleeve combination at 1st sleeve inner radius, in terms of liner-1st sleeve interfacial pressure

$$U_{(1, 2, 3, 4)B, el} = \frac{B P_{B, el}}{Y_{S600}} \left[\frac{B^2 + F^2}{F^2 - B^2} + \mu \right]$$

$$U_{(1, 2, 3, 4)B, el} = 2.09 \times 10^{-7} P_{B, el}$$

c. Equate 15a and 15b to obtain liner-1st sleeve interfacial pressure as a function of extrusion pressure at liner inner radius

$$2.80\, P_{A, el} - 3.10\, P_{B, el} = 2.09\, P_{B, el}$$

$$P_{A, el} = 1.85\, P_{B, el}$$

d. Express 0.0005 in. expansion of 4th sleeve outer radius in terms of liner-1st sleeve interfacial pressure. Determine pressure.

Displacement of 1st-2nd-3rd-4th sleeve combination at 4th sleeve outer radius due to $P_{B, el}$ is

$$U_{(1, 2, 3, 4)F, el} = \frac{2 B^2 F P_{B, el}}{Y_{S600}(F^2 - B^2)} = 0.0005 \text{ in.}$$

$$P_{B, el} = 3,050 \text{ psi}$$

e. Substitute 15d in 15c to obtain stem pressure

$$P_{A, el} = 5,650 \text{ psi}$$

f. Determine tangential and radial stress generated by extrusion pressure at inner radii of liner and all sleeves

$$S_{(L,1,2,3,4)Tel} = \frac{A^2 P_{A,\,el}}{F^2 - A^2}\left[1 + \frac{F^2}{r^2}\right] \qquad A \leq r \leq F$$

$$S_{(L,1,2,3,4)Tel} = 2,360 + \frac{26,000}{r^2}$$

$$S_{(L,1,2,3,4)Rel} = 2,360 - \frac{26,000}{r^2}$$

16. Stress Developed in Liner-Sleeve Assembly by 207,000 psi Stem Pressure, at 600°F

a. Subtract extrusion pressure in 15e from total pressure, and determine tangential and radial stress for this pressure

$$P_{A,\,e2} = 207,000 - 5,650 \cong 201,000 \text{ psi}$$

$$S_{(L,1,2,3,4)Te2} = \frac{A^2 P_{A,\,e2}}{M^2 - A^2}\left[1 + \frac{M^2}{r^2}\right] \qquad A \leq r \leq M$$

$$S_{(L,1,2,3,4)Te2} = 6,730 + \frac{673,000}{r^2}$$

$$S_{(L,1,2,3,4)Re2} = 6,730 - \frac{673,000}{r^2}$$

b. Determine total stress by adding results of 15f and 16a

$$S_{(L,1,2,3,4)T(el,\,e2)} = 9,090 + \frac{699,000}{r^2}$$

$$S_{(L,1,2,3,4)R(el,\,e2)} = 9,090 - \frac{699,000}{r^2}$$

Equations developed in 16b are plotted in Figure 29 to show stress due to extrusion pressure as a function of radial distance in liner and each sleeve.

17. <u>Stress Developed in Liner-Sleeve Assembly Due to Combined Shrink Stresses and 207,000 psi Stem Pressure</u>

a. Add appropriate equations in 13a to those in 16b for liner stress

$$S_T = -106,000 + \frac{323,000}{r^2} \qquad A \leq r \leq B$$

$$S_R = -106,000 - \frac{323,000}{r^2}$$

b. Add appropriate equations in 13b to those in 16b for 1st sleeve stress

$$S_T = -11,100 + \frac{729,000}{r^2} \qquad B \leq r \leq C$$

$$S_R = -11,100 - \frac{729,000}{r^2}$$

c. Add appropriate equations in 13c to those in 16b for 2nd sleeve stress

$$S_T = 11,900 + \frac{853,000}{r^2} \qquad C \leq r \leq D$$

$$S_R = 11,900 - \frac{853,000}{r^2}$$

d. Add appropriate equations in 13d to those in 16b for 3rd sleeve stress

$$S_T = 30,800 + \frac{976,000}{r^2} \qquad D \leq r \leq E$$

$$S_R = 30,800 - \frac{976,000}{r^2}$$

e. Add appropriate equations in 13e to those in 16b for 4th sleeve stress

$$S_T = 43,300 + \frac{1,075,000}{r^2} \qquad E \leq r \leq F$$

$$S_R = 43,300 - \frac{1,075,000}{r^2}$$

Equations developed in 17 are plotted in Figure 29to show total stress as a function of radial distance in liner and each sleeve.

18. Maximum Permissible τ_o Value for Liner and Sleeves

a. Calculate maximum τ_o value for liner, using Sturm criterion

$\tau_{o,max}$ is 0.707 of the tensile strength of material, if material is brittle. Alumina liner is brittle, and has a tensile strength of 21,000 psi at 600°F.

$\tau_{o,max}$ is (0.707)(21,000) = 14,800 psi

b. Calculate maximum τ_o value for sleeves, using Sturm criterion

$\tau_{o,max}$ is 0.707 of the maximum working tensile stress of the material, if material is ductile. The maximum working tensile stress of the HTB-2 steel is taken as 75% of the 0.2% offset yield strength. At 600°F, this value is 225,000 psi.

$\tau_{o,max}$ is (0.707)(225,000) = 159,000 psi

$\tau_{o,max}$ is 159,000 psi

19. Calculation of Peak τ_o Value in Liner and Each Sleeve Under Combined Shrink Stresses and 210,000 psi Stem Pressure

a. Calculate peak τ_o value in liner and each sleeve by the equation

$$\tau_o = \frac{(0.1755\, S_T^2 + 0.1755\, S_R^2 - 0.3155\, S_T S_R)^{1/2}}{1 - \dfrac{0.433\,(S_T + S_R)}{S_o}}$$

where S_o = 21,000 for alumina

S_o = 225,000 for HTB-2

Peak τ_o value is at inner radius of each cylinder.

Calculated values of τ_o for inner radius of liner and each sleeve are listed in Table IX. It may be seen that all τ_o values are below maximum allowable under a 210,000 psi stem pressure, corresponding to a 16.7% extrusion pressure overload.

TABLE IX

PEAK τ_o VALUES FOR LINER AND EACH SLEEVE, UNDER COMBINED SHRINK STRESS AND 210,000 PSI STEM PRESSURE, FOR FOUR-SLEEVE ASSEMBLY

Part	Radius (r), in.	Tangential Stress (S_T), kpsi	Radial Stress (S_R), kpsi	Equivalent Shear Stress in Tension (τ_o), kpsi	Maximum Allowable τ_o, kpsi
Liner	1.80	- 5.0	-207	11.8	14.8
1st Sleeve	2.06	161	-183	134	159
2nd Sleeve	2.32	170	-147	135	159
3rd Sleeve	2.56	181	-118	139	159
4th Sleeve	2.81	179	- 94.9	134	159

b. Calculate peak τ_o value for liner at 75°F under zero extrusion pressure

$$\tau_o = 17,600 \text{ psi, at } 75°F$$

This value is above the maximum allowable. Consequently liner-sleeve assembly should be stored above room temperature. It is noted that both peak tangential stress and the τ_o value are higher when the liner is at 75°F under a zero extrusion load, than at 600°F under a 15% extrusion pressure overload.

20. Calculation of Required Force and Pressure for Separation of Liner-Sleeve Assembly with Liner in Place

a. General Approach

Liner-sleeve assembly cannot be separated by differential heating due to relatively low thermal conductivity coefficient of ceramic liner, geometry of assembly, and relatively high interference values. Instead, sleeves must be pressed off one another. This may be accomplished by either chipping out liner first, then pressing off outer sleeves successively, or pressing off outer sleeves successively without first removing liner.

If ceramic liner can be removed before sleeves are separated, sleeve interfacial pressure is reduced, and assembly may be separated at room temperature as readily as at 600°F, since all sleeve thermal expansion coefficients are the same.

If ceramic liner cannot be removed, sleeve separation should proceed at as high a temperature as possible, because sleeve interfacial pressures increase as temperature decreases. The fourth sleeve may readily be removed when assembly is at 600°F, by simply pressing liner and inner sleeves when assembly is in container sleeve. (Fourth sleeve should be supported at base to prevent an excessive load on sleeve flange when this operation is carried out.) First, second, and third sleeves may be expected to be at a lower temperature during separation, since they would not ordinarily be in contact with a heated surface during this operation.

To assure calculated separation forces are conservative, it is assumed that third, second, and first sleeve separation will be effected at 75°F. It will be seen that all sleeve removal pressure will still be below 50 tsi.

b. Calculate separation force and pressure with liner in place. Use 3d, 6h, 9h, 10m for interfacial pressures

$$F = 2\pi\nu Lr_iP_i = 12.7\, r_iP_i$$

$$P = \frac{2\nu\, Lr_iP_i}{r_o^2 - r_i^2} = \frac{4.04\, r_iP_i}{r_o^2 - r_i^2}$$

P_i = interfacial pressure
F = removal force
P = removal pressure
r_i = cylinder inner radius
r_o = cylinder outer radius
L = cylinder length, 10.1 in.
ν = coefficient of friction, 0.2

Values of required removal force and pressure for each sleeve are given in Table X.

21. Calculation of Required Force and Pressure for Separation of Liner-Sleeve Assembly with Liner Removed, at 75°F

a. Calculate interference between 1st sleeve outer radius and 2nd sleeve inner radius from dimensions given in 3e and 4e

$$\gamma_{12} = 2.3049 - 2.3006 = 0.0043 \text{ in.}$$

b. Determine interfacial pressure between 1st sleeve outer radius and 2nd sleeve inner radius

$$P_{12,2} = \frac{Y_{S75}\gamma_{12}(C^2 - B^2)(D^2 - C^2)}{2C^3(D^2 - B^2)} = 2,910 \text{ psi}$$

c. Determine expansion of 2nd sleeve outer radius due to 1st-2nd sleeve interfacial pressure

$$U_{2D,2} = \frac{2C^2DP_{12,2}}{Y_{S75}(D^2 - C^2)} = 0.0023 \text{ in.}$$

d. Determine interference between stressed 2nd sleeve outer radius and unstressed 3rd sleeve inner radius from 7e and 21c

$$\gamma_{23} = 2.5606 + 0.0023 - 2.5566 = 0.0063 \text{ in.}$$

e. Determine interfacial pressure between 2nd sleeve outer radius and 3rd sleeve inner radius

$$P_{23,3} = \frac{Y_{S75}\gamma_{23}(D^2 - B^2)(E^2 - D^2)}{2D^3(E^2 - B^2)} = 4,650 \text{ psi}$$

TABLE X

REQUIRED REMOVAL FORCE AND PRESSURE FOR EACH SLEEVE WITH LINER IN PLACE, FOR FOUR-SLEEVE ASSEMBLY

Sleeve No.	Interfacial Pressure, psi	Removal Force, tons	Removal Pressure, psi
4	13,400	240	49,000
3	11,100	180	85,000
2	11,000	162	88,200
1	11,000	144	80,300

TABLE XI

REQUIRED REMOVAL FORCE AND PRESSURE FOR EACH SLEEVE WITH LINER REMOVED, FOR FOUR-SLEEVE ASSEMBLY

Sleeve No.	Interfacial Pressure, psi	Removal Force, tons	Removal Pressure, psi
4	8,080	144	29,600
3	4,650	76	35,600
2	2,910	43	23,300

f. Determine expansion of 3rd sleeve outer radius due to 2nd-3rd sleeve interfacial pressure

$$U_{3E,3} = \frac{2D^2EP_{23,3}}{Y_{S75}(E^2 - D^2)} = 0.0044 \text{ in.}$$

g. Determine interference between stressed 3rd sleeve outer radius and unstressed 4th sleeve inner radius, using 21f

Unstressed 3rd sleeve outer radius, at 75°F, is 2.8166 in.
Unstressed 4th sleeve inner radius, at 75°F, is 2.8137 in.

$$\gamma_{34} = 2.8166 + 0.0044 - 2.8137 = 0.0073 \text{ in.}$$

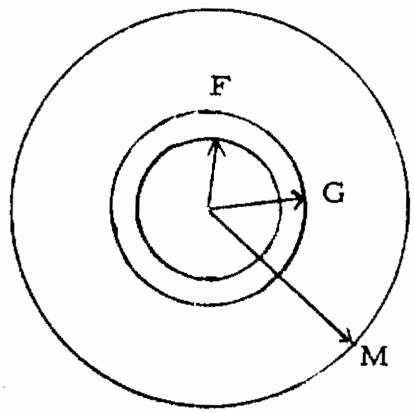
h. Determine interfacial pressure between 3rd sleeve outer radius and 4th sleeve inner radius

$$P_{34,4} = \frac{Y_{S75}\,\gamma_{34}(E^2 - B^2)(F^2 - E^2)}{2E^3(F^2 - B^2)} = 8,080 \text{ psi}$$

i. Substitute interfacial pressures in 21b, 21e, 21h, in equations in 20b to calculate separation force and pressure for successive removal of outer sleeves.

Results are listed in Table XI. Tables X and XI show that liner removal will considerably reduce removal pressures, but is not necessary to effect sleeve separation.

22. Calculation of Container Sleeve Stresses



$F = 3.310$ in., nominal

$G_L = 4.682$ in.

$M = 10.0$ in.

FIG. 31 - CONTAINER AND SLEEVE ASSEMBLY

a. General Approach

Sleeve outer radius is stepped (see Fig. 2). Shrink and extrusion stresses will be calculated for the smaller outer radius, because stresses will be largest at this point.

Both container sleeve and container have approximately the same elastic modulus and linear thermal expansion coefficient, permitting considerable simplification of calculation procedure.

Container sleeve is designed to be removed from container by maintaining sleeve inner radius at the steam point as container is heated. Sleeve outer radius is silver plated with 0.001-0.0015 in. thickness of silver to facilitate heat transfer.

b. Determine interfacial pressure between container sleeve (fifth sleeve) and container due to a radial interference of 0.0060 in.

$$P_{5C,C} = \frac{Y_{S600}\beta_{5C}(G^2 - F^2)(M^2 - G^2)}{2G^3(M^2 - F^2)} = 7,000 \text{ psi}$$

c. Determine stress on container sleeve due to shrink fit

$$S_{5TC} = -\frac{P_{5C,C}G^2}{G^2 - F^2}\left[1 + \frac{F^2}{r^2}\right] \qquad F \leq r \leq G$$

$$S_{5TC} = -16,000 - \frac{176,000}{r^2}$$

$$S_{5RC} = -16,000 + \frac{176,000}{r^2}$$

d. Determine stress on container sleeve due to 210,000 psi stem pressure

Liner-sleeve assembly expansion will absorb effect of 5,650 psi stem pressure. Accordingly, container sleeve will react to a liner pressure of approximately 204,000 psi.

$$S_{5Te2} = \frac{A^2P_{e2}}{M^2 - A^2}\left[1 + \frac{M^2}{r^2}\right] \qquad F \leq r \leq G$$

$$S_{5Te2} = 6,830 + \frac{683,000}{r^2}$$

$$S_{5Re2} = 6,830 - \frac{683,000}{r^2}$$

e. Determine effect of combined shrink and extrusion stress on container sleeve by adding appropriate equations in 22a and 22d

$$S_{5TC,\,e2} = -9,170 + \frac{507,000}{r^2} \qquad F \leq r \leq G$$

$$S_{5Rc,\,e2} = -9,170 - \frac{507,000}{r^2}$$

Equations developed in section 22 are plotted in Figure 29 show stress due to shrink fitting and extrusion pressure as a function of radial distance in container sleeve.

f. Calculate Peak τ_o value in Container Sleeve

Peak octahedral shear stress, and peak τ_o, will be at container sleeve inner radius. There is no axial stress at this point, due to slip fit of 4th sleeve. Octahedral shear stress then becomes.

$$\tau = 0.47(S_1^2 + S_2^2 - S_1 S_2)^{1/2}$$

The 0.2% offset yield strength of H-13 steel at R_c 50-52 is approximately 220,000 psi at 600°F. Let the maximum working tensile stress S_o be 75% of this value, or S_o = 165,000 psi. The τ_o value in container sleeve is given by

$$\tau_{o,\,peak} = \frac{\tau}{1 - \dfrac{S_1 + S_2 + S_3}{3S_o}} \qquad \frac{0.47(S_1{}^2 + S_2{}^2 - S_1 S_2)^{1/2}}{1 - 0.202(S_1 + S_2)10^{-5}} = 36,500 \text{ psi}$$

g. Compare 22f result with maximum permissible τ_o value calculated by Sturm criterion

$$\tau_{o,\,max} \leq 0.707\ S_o = 106,500 \text{ psi}$$

Peak τ_o value in container sleeve is considerably below this value.

23. Temperature Differential and Electric Heating Power Required to Remove Container Sleeve from Container

a. General Approach

Calculation will be carried out for lower section of sleeve, where interfacial pressures are the highest.

Temperature will be assumed zero at container sleeve-container interface for ease of calculation. The t_1 temperature at r = F, and

t_3 temperatures at r = M will then be relative to zero interfacial temperature. As such, only the difference between t_1 and t_3 temperatures has meaning.

b. Calculation of container sleeve-container separation temperature differential

$$t_3 = \frac{-\beta_{5C}}{a_S G\left[\frac{M^2}{M^2 - G^2} - \frac{F^2}{G^2 - F^2}\left(\frac{\ln(G/F)}{\ln(M/G)}\right)\right]} = 220°$$

$$t_1 = -\frac{t_3 \ln(G/F)}{\ln(M/G)} = -100°$$

$$t_3 - t_1 = 320°F$$

If container sleeve inner radius is held at 212°F, container outer diameter must be held at 532°F to separate sleeve from container.

c. Calculation of electric heating power required to maintain separation temperature differential

$$W = \frac{12.8 \times 10^{-6} LK\Delta t}{\ln(M/E)} \qquad L = 10.1 \text{ in.} \qquad K = 197 \text{ Btu/ft}^2\text{/in/hr/°F}$$

W = 7.36 kilowatts

Container heaters are rated at 12.0 kilowatts.

24. Calculation of Container Stresses

a. General Approach

Inner radius of container is stepped as shown in Figure 2. Stresses due to container sleeve shrink and extrusion pressure will be greater in lower section of container. Therefore, tangential and radial stress will be calculated for this section.

b. Determine stress due to shrink of container sleeve

$$S_{CTC} = \frac{G^2 P_{5C}}{M^2 - G^2}\left[1 + \frac{M^2}{r^2}\right] \qquad G \leq r \leq M$$

$$S_{CTC} = 1,950 + \frac{195,000}{r^2}$$

$$S_{CRC} = 1,950 - \frac{195,000}{r^2}$$

c. Determine stress due to 210,000 psi stem pressure.

Liner-sleeve assembly expansion will absorb effect of 5,650 psi stem pressure. Accordingly, container will react to a liner pressure of approximately 204,000 psi.

$$S_{CTe2} = \frac{A^2 P_{e2}}{M^2 - A^2}\left[1 + \frac{M^2}{r^2}\right] \qquad G \leq r \leq M$$

$$S_{CTe2} = 6,850 + \frac{685,000}{r^2}$$

$$S_{CRe2} = 6,850 - \frac{685,000}{r^2}$$

d. Determine effect of combined shrink and extrusion stress on container by adding appropriate equations in 23b and 23c.

$$S_{CTC,\ e2} = 8,800 + \frac{880,000}{r^2}$$

$$S_{CRC,\ e2} = 8,800 - \frac{880,000}{r^2}$$

Peak tangential stress is 50,500 psi.

Equations developed in Section 24 are plotted in Figure 29 to show stress due to shrink fitting and extrusion pressure as a function of radial distance in container.

DESIGN 2: 1000-TON PRESS CONTAINER CALCULATION FOR A LINER WITH BALANCED TENSION AND COMPRESSION STRESSES AT 0.5 OF MAXIMUM EXTRUSION STRESS

A. Design Procedure

1. Calculation of Shrink Stress at Liner Inner Radius, Numerically Equal to 0.5 of Maximum Extrusion Stress

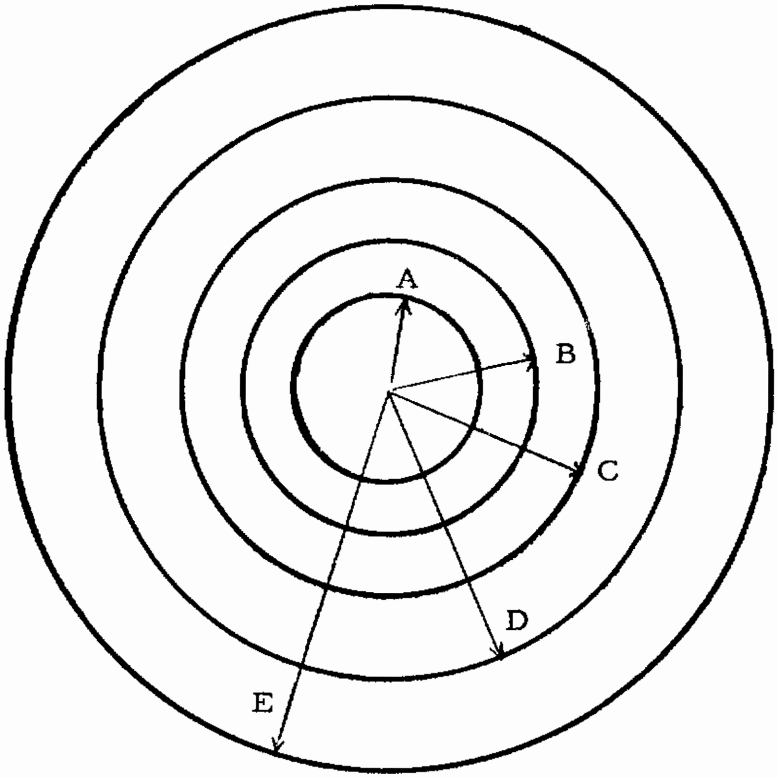


FIG. 32 - LINER AND THREE-SLEEVE ASSEMBLY

A = 1.800 in., nominal	$U_{4D, 1e}$	= 0.0005 in.
B = 2.060 in., nominal	Y_{75}	= 30.2×10^6 psi
C = 2.440 in., nominal	Y_{800}	= 26.2×10^6 psi
D = 2.810 in., nominal	α_{75}	= $6.7 \times 10^{-6}/°F$
E = 3.310 in., nominal	α_{800}	= $7.2 \times 10^{-6}/°F$
	α_{1000}	= $7.4 \times 10^{-6}/°F$

a. Calculate maximum tangential stress developed at liner inner radius by extrusion pressure. Set shrink stress equal to 0.5 of absolute value.

Let maximum extrusion pressure be 210,000 psi. The tangent stress developed at liner inner radius will be

$$S_{L\,T_{el,\ e2}} = \frac{(P_{el} + P_{e2})(A^2 + M^2)}{M^2 - A^2} = 224{,}000 \text{ psi}$$

If tensile and compressive tangential stresses are to balance at 0.5 maximum extrusion pressure, compressive tangential stress at liner inner radius at zero extrusion pressure must be -112,000 psi, when all sleeves are in contact.

2. <u>Extrusion Stress on Liner and Sleeves when Third Sleeve Outer Radius is Expanded 0.0005 in.</u>

a. Determine extrusion pressure as a function of 4th sleeve displacement, and calculate value of this pressure for 0.0005 in. displacement

$$P_{1e} = \frac{U_{4D,\ 1e}Y_{800}(E^2 - A^2)}{2A^2E} = 4{,}750 \text{ psi}$$

b. Calculate tangential and radial stress as a function of radial distance throughout liner-sleeve assembly

$$S_{(L,\,1,\,2,\,3)\,Tel} = \frac{A^2P_{el}}{E^2 - A^2}\left[1 + \frac{E^2}{r^2}\right]$$

$$S_{(L,\,1,\,2,\,3)Tel} = 1{,}980 + \frac{21{,}800}{r^2}$$

$$S_{(L,\,1,\,2,\,3)Rel} = 1{,}980 - \frac{21{,}800}{r^2}$$

3. <u>Stress Developed in Liner-Sleeve Assembly by 210,000 psi Stem Pressure, at 800°F</u>

a. Substract extrusion pressure in 2a from total pressure, and determine tangential and radial stress for this pressure

$$P_{A,\ e2} = 210{,}000 - 4.750 = 205{,}000 \text{ psi}$$

$$S_{(L,\,1,\,2,\,3)Te2} = \frac{A^2P_{e2}}{M^2 - A^2}\left[1 + \frac{M^2}{r^2}\right]$$

$$S_{(L,\,1,\,2,\,3)Te2} = 6{,}860 + \frac{686{,}000}{r^2}$$

$$S_{(L,\,1,\,2,\,3)Re2} = 6{,}860 - \frac{686{,}000}{r^2}$$

4. Total Tangential and Radial Stress in Liner and Sleeve Assembly Due to Extrusion Pressure

a. Add appropriate equations in 2b and 3a

$$S_{(L,1,2,3)T(e_1 e_2)} = 8,840 + \frac{708,000}{r^2} \qquad A \leq r \leq E$$

$$S_{(L,1,2,3)R(e_1 e_2)} = 8,840 - \frac{708,000}{r^2}$$

Equations developed in 4a are plotted in Figure 33 to show stress due to extrusion pressure as a function of radial distance in liner and each sleeve.

5. Sleeve Interfacial Pressures Due to Shrink Stresses

a. Express tangential stress at liner inner radius and each sleeve inner radius as a function of interfacial pressures

Liner stress, at r = A, is

$$S_{(AT,1,2,3)} = -\frac{2P_{L1}B^2}{B^2 - A^2} - \frac{2P_{12}C^2}{C^2 - A^2} - \frac{2P_{23}D^2}{D^2 - A^2}$$

$$S_{(AT,1,2,3)} = 8.48\,P_{L1} + 4.37\,P_{12} + 3.39\,P_{23}$$

First sleeve stress, at r = B, is

$$S_{(BT,1,2,3)} = \frac{B^2 + C^2}{C^2 - B^2}\,P_{L1} - \left[1 + \frac{A^2}{B^2}\right]\frac{C^2 P_{12}}{C^2 - A^2} - \left[1 + \frac{A^2}{B^2}\right]\frac{D^2 P_{23}}{D^2 - A^2}$$

$$S_{(BT,1,2,3)} = 5.89\,P_{L1} - 3.85\,P_{12} - 2.99\,P_{23}$$

Second sleeve stress, at r = C, is

$$S_{(CT,1,2,3)} = \frac{C^2 + D^2}{D^2 - C^2}\,P_{12} - \left[1 + \frac{A^2}{C^2}\right]\frac{D^2 P_{23}}{D^2 - A^2}$$

$$S_{(CT,2,3)} = 7.23\,P_{12} - 2.62\,P_{23}$$

Third sleeve stress, at r = D, is

$$S_{(DT,3)} = \frac{D^2 + E^2}{E^2 - D^2} \; P_{23}$$

$$S_{(DT,3)} = 6.10\, P_{23}$$

b. Calculate tangential stress due to extrusion pressure at liner inner radius and each sleeve inner radius, from 4a

At r = A, $S_{AT(e_1 e_2)} = 114,000$ psi

At r = B, $S_{BT(e_1 e_2)} = 176,000$ psi

At r = C, $S_{CT(e_1 e_2)} = 128,000$ psi

At r = D, $S_{DT(e_1 e_2)} = 98,500$ psi

c. Set sum of shrink stress and extrusion stress in each sleeve equal to the same constant, W. (Sum of shrink and extrusion stress in liner is 114,000 psi)

$$S_{(BT,1,2,3)} + S_{BT(e_1 e_2)} = W$$

$$S_{(CT,2,3)} + S_{CT(e_1 e_2)} = W$$

$$S_{(DT,3)} + S_{DT(e_1 e_2)} = W$$

d. Substitute equations in 5a and values in 5b in 5c, and rearrange terms

$$0 + 8.48\, P_{L1} + 4.37\, P_{12} + 3.39\, P_{23} = 114,000$$

$$W - 5.98\, P_{L1} + 3.85\, P_{12} + 2.99\, P_{23} = 176,000$$

$$W + 0.00 \quad - 7.23\, P_{12} + 2.62\, P_{23} = 128,000$$

$$W + 0.00 \quad + 0.00 \quad - 6.10\, P_{23} = 98,500$$

e. Effect a determinant solution of 5d equations

Results are as follows:

$P_{L1} = 6,050$ psi

$P_{12} = 7,240$ psi

$P_{23} = 9,380$ psi

$W = 156,000$ psi

Maximum tangential stress at 1st, 2nd, and 3rd sleeve inner radii due to combined extrusion stresses is 156,000 psi

6. Tangential and Radial Stresses in Liner and Sleeve Assembly Due to Shrink Fitting

a. Determine liner stress distribution by use of interfacial pressures in 5e

$$S_{(LT,1,2,3)} = -\left[1 + \frac{A^2}{r^2}\right]\left[\frac{B^2 P_{L1}}{B^2 - A^2} + \frac{C^2 P_{12}}{C^2 - A^2} + \frac{D^2 P_{23}}{D^2 - A^2}\right]$$

$$S_{(LT,1,2,3)} = -57,000 - \frac{185,000}{r^2} \qquad A \leq r \leq B$$

$$S_{(RT,1,2,3)} = -57,000 + \frac{185,000}{r^2}$$

b. Determine 1st sleeve stress distribution

$$S_{(LT,1,2,3)} = \frac{B^2 P_{L1}}{C^2 - B^2}\left[1 + \frac{C^2}{r^2}\right] - \left[1 + \frac{A^2}{r^2}\right]\left[\frac{C^2 P_{12}}{C^2 - A^2} - \frac{D^2 P_{23}}{D^2 - A^2}\right]$$

$$S_{(1T,1,2,3)} = -17,000 - \frac{14,600}{r^2} \qquad B \leq r \leq C$$

$$S_{(1T,1,2,3)} = -17,000 + \frac{14,600}{r^2}$$

c. Determine 2nd sleeve stress distribution

$$S_{(2T,2,3)} = \frac{C^2 P_{12}}{D^2 - C^2}\left[1 + \frac{D^2}{r^2}\right] - \left[1 + \frac{A^2}{r^2}\right]\left[\frac{D^2 P_{23}}{D^2 - A^2}\right]$$

$$S_{(2T,2,3)} = 6,600 + \frac{126,000}{r^2} \qquad C \leq r \leq D$$

$$S_{(2R,2,3)} = 6,600 + \frac{126,000}{r^2}$$

d. Determine 3rd sleeve stress distribution

$$S_{(3T,3)} = \frac{D^2 P_{23}}{E^2 - D^2}\left[1 + \frac{E^2}{r^2}\right]$$

$$S_{(3T,3)} = 24,000 + \frac{264,000}{r^2} \qquad D \leq r \leq E$$

$$S_{(3R,3)} = 24,000 - \frac{264,000}{r^2}$$

Equations developed in section 6 are plotted in Figure 33 to show shrink stress as a function of radial distance in liner and each sleeve.

7. Tangential and Radial Stress in Liner and Each Sleeve due to Extrusion Pressure and Shrink Stress

a. Determine liner stress by adding appropriate equations in 4a to those in 6a

$$S_{(LT,1,2,3)ele2} = -48,000 + \frac{523,000}{r^2} \qquad A \leq r \leq B$$

$$S_{(LR,1,2,3)ele2} = -48,000 - \frac{523,000}{r^2}$$

b. Determine 1st sleeve stress by adding appropriate equations in 4a to those in 6b

$$S_{(1T,1,2,3)ele2} = -8,160 + \frac{693,000}{r^2} \qquad B \leq r \leq C$$

$$S_{(1R,1,2,3)ele2} = -8,160 - \frac{693,000}{r^2}$$

c. Determine 2nd sleeve stress by adding appropriate equations in 4a to those in 6c

$$S_{(2T,\,2,\,3)ele2} = 15,400 + \frac{834,000}{r^2} \qquad C \leq r \leq D$$

$$S_{(2R,\,2,\,3)ele2} = 15,400 - \frac{834,000}{r^2}$$

d. Determine 3rd sleeve stress by adding appropriate equations in 4a to those in 6d

$$S_{(3T,\,3)ele2} = 32,800 + \frac{972,000}{r^2} \qquad D \leq r \leq E$$

$$S_{(3R,\,3)ele2} = 32,800 - \frac{972,000}{r^2}$$

Equations developed in section 7 are plotted in Figure 33 to show peak stress due to extrusion pressure and shrink fitting as a function of radial distance, in liner and each sleeve

8. Maximum Permissible τ_o Value for Liner and Sleeves

a. τ_o must be less than or equal to the maximum working tensile stress of the material, S_o. The maximum working tensile stress of the H-13 steel is taken as 75% of the 0.2% offset yield strength. At 800°F, S_o = 180,000 psi

$$\tau_o \leq (0.707)(180,000) = 127,000 \text{ psi}$$

$$\tau_o \leq 127,000 \text{ psi}$$

9. Calculation of Peak τ_o Value in Liner and Each Sleeve under Combined Shrink Stresses and 210,000 psi Stem Pressure

a. Calculate peak τ_o value in liner and each sleeve by procedure in 19a

Peak τ_o value is at inner radius of each cylinder. Calculated values of τ_o for inner radius of liner and each sleeve are listed in Table XII.

Comparison of maximum permissible τ_o value calculated in section 8 with tabulated values shows that peak τ_o value on 1st sleeve is marginally high at a 16.7% extrusion pressure overload of 210,000 psi

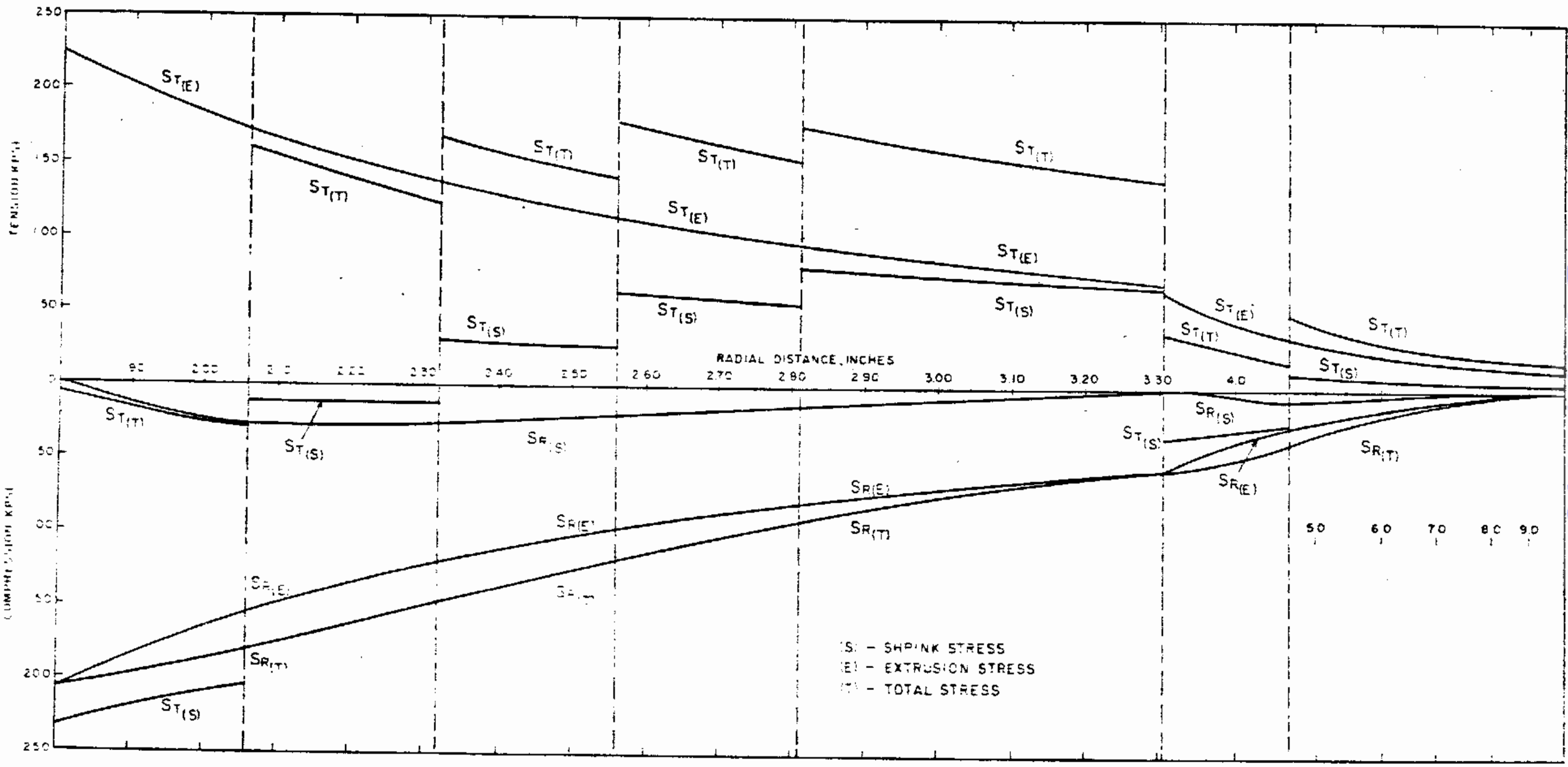


FIG. 33 LINER SLEEVE AND CONTAINER STRESS FOR THREE SLEEVE ASSEMBLY AS A FUNCTION OF RADIAL DISTANCE FROM CONTAINER CENTERLINE

TABLE XII

PEAK τ_o VALUES FOR LINER AND EACH SLEEVE, UNDER COMBINED SHRINK STRESS AND 210,000 PSI STEM PRESSURE, FOR THREE-SLEEVE ASSEMBLY

Part	Radius (r), in.	Tangential Stress (S_T), kpsi	Radial Stress (S_R), kpsi	Equivalent Shear Stress in Tension (τ_o), kpsi
Liner	1.80	114	-210	105
1st Sleeve	2.06	156	-171	134
2nd Sleeve	2.44	156	-125	119
3rd Sleeve	2.81	156	- 90.2	124

10. Container Sleeve and Container Stresses

Container sleeve and container stresses will be identical to those calculated in section 21,for Design 1, since pressure on inner radius of container sleeve will be the same for both first and second liner-and-sleeve assembly designs. Container sleeve and container stress as a function of radial displacement, plotted in Figure 29have been replotted in Figure 33 to provide a complete picture of the stresses present in the liner-three sleeve system.

11. Liner-First Sleeve Interference and Machining Dimensions

a. Calculate interference between liner outer radius and 1st sleeve inner radius

$$\beta_{L1} = \frac{2B^3(C^2 - A^2)P_{L1}}{Y_{800}(B^2 - A^2)(C^2 - B^2)} = 0.0064 \text{ in.}$$

Interference at both 75° F and 800° F will be the same, as liner and 1st sleeve have the same thermal expansion coefficient.

At 75° F, liner outer radius is 2.0650 in.

At 75° F, 1st sleeve inner radius will be 2.0650 - 0.0064 = 2.0586 in.

b. Calculate 1st sleeve shrink-fitting temperature for liner at 75° F, allowing 0.0050 in. radial clearance when 1st sleeve is heated to fitting temperature

Liner-1st sleeve interference is 0.0064 in. Allowing 0.0050 in. clearance between sleeves when 1st sleeve is heated requires a total 0.0114 in. expansion of 1st sleeve inner radius

$$\Delta_{t-75} \simeq \frac{B_t - B_{75}}{\alpha B_{75}} = 770° \text{F}$$

$$t = 770° \text{F} + 75° \text{F} = 845° \text{F}$$

c. Calculate expansion of 1st sleeve outer radius, to determine its stressed dimension at 75° F

$$U_{1C,1} = \frac{C(B^2 - A^2)\beta_{L1}}{B(C^2 - A^2)} = 0.0028 \text{ in.}$$

Outer radius of 1st sleeve, at 75° F, is 2.4372 in.

Expanded outer radius of 1st sleeve, at 75° F, is 2.4400 in.

12. First-Second Sleeve Interference and Machining Dimensions

a. Calculate interference between 1st sleeve stressed outer radius and 2nd sleeve inner radius

$$\beta_{12} = \frac{2C^3(D^2 - A^2)P_{12}}{Y_{800}(C^2 - A^2)(D^2 - C^2)} = 0.0070 \text{ in.}$$

At 75° F, stressed 1st sleeve outer radius is 2.4417 in.

At 75° F, 2nd sleeve inner radius will be 2.4362 in.

b. Calculate 2nd sleeve shrink fitting temperature for 1st sleeve at 75° F, allowing 0.0060 in. radial clearance when 2nd sleeve is heated to fitting temperature

First sleeve-second sleeve interference is 0.0070 in. Allowing 0.0060 in. clearance between sleeves when 2nd sleeve is heated requires a total 0.0130 in. expansion of 2nd sleeve inner radius.

$$\Delta t_{t-75} \cong \frac{C_t - C_{75}}{\alpha C_{75}} \cong 740°F$$

$$t = 740°F + 75°F = 815°F$$

c. Calculate expansion of 2nd sleeve outer radius to determine its stressed dimension at 75° F

$$U_{2D,2} = \frac{D(C^2 - A^2)\beta_{12}}{C(D^2 - A^2)} = 0.0047 \text{ in.}$$

Outer radius of 2nd sleeve, at 75° F, is 2.8053 in.

Expanded outer radius of 2nd sleeve, at 75° F, is **2.8100** in.

13. Second-Third Sleeve Interference and Machining Dimensions

a. Calculate interference between 2nd sleeve stressed outer radius and 3rd sleeve inner radius

$$\beta_{23} = \frac{2D^3(E^2 - A^2)P_{23}}{Y_{800}(D^2 - A^2)(E^2 - D^2)} = 0.0085 \text{ in.}$$

At 75° F, stressed 2nd sleeve outer radius is 2.8100 in.

At 75° F, 3rd sleeve inner radius will be 2.8015 in.

b. Calculate 3rd sleeve shrink fitting temperature for 2nd sleeve at 75° F, allowing 0.0060 in. clearance when 3rd sleeve is heated to fitting temperature

Second sleeve-third sleeve interference is 0.0085 in. Allowing 0.0060 in. clearance between sleeves when 3rd sleeve is heated requires a total 0.0145 in. expansion of 3rd sleeve inner radius.

$$\Delta t_{t-75} = \frac{D_t - D_{75}}{\alpha D_{75}} = 720°F$$

$$t = 720°F + 75°F = 795°F$$

c. Calculate expansion of 3rd sleeve outer radius to determine its unstressed dimension at 75°F

$$U_{3E,3} = \frac{E(D^2 - A^2)\beta_{23}}{D(E^2 - A^2)} = 0.0061 \text{ in.}$$

Machined outer radius of 3rd sleeve, at 75°F, should be 0.0061 + 0.0003 = 0.0064 in. less than container sleeve inner radius to permit a 0.0001 - 0.0005 in. clearance between 3rd sleeve outer radius and container sleeve inner radius. Container sleeve inner radius will be 3.3100 in. at 75°F. Stressed outer radius of 3rd sleeve will be 3.3036 in.

Unstressed outer radius of 3rd sleeve will be 3.2974 in.

14. Contraction of Liner Inner Radius due to Shrink Fit of First, Second, and Third Sleeves

a. Determine liner inner radius contraction in terms of tangential stress generated at liner inner radius by shrink fit of 1st, 2nd, and 3rd sleeves

$$U_{LA,1,2,3} = \frac{(114,000)A}{Y_{800}} = 0.0078 \text{ in.}$$

15. Calculation of Required Force and Pressure for Separation of Liner-Sleeve Assembly

a. General Approach

Sleeves cannot be separated by differential heating for the same reasons discussed in section 20a, for Design 1, and, therefore, must be pressed off one another. As in 20a, outer sleeve will be removed first.

The calculation of sleeve removal force and pressure is considerably simplified, in this case, because liner and sleeves have the same thermal expansion and elastic modulus coefficients. This makes it possible to disregard sleeve temperature in the calculation, since interference of sleeves, and hence removal pressure, does not appreciably change with a change in temperature.

b. Calculate sleeve removal force and removal pressure by use of interfacial pressure calculated in 5d and equations in 20b.

Values of required removal force and pressure for each sleeve are given in Table XIII.

TABLE XIII

REQUIRED REMOVAL FORCE AND PRESSURE FOR EACH SLEEVE IN THREE-SLEEVE ASSEMBLY

Sleeve No.	Interfacial Pressure, psi	Removal Force, tons	Removal Pressure, psi
3	9, 380	167	40, 400
2	7, 240	112	43, 000
1	6, 050	79	29, 200

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