科技资料

Proceedings of the 18th Turbomachinery Symposium

Proceedings of the Eighteenth Turbomachinery Symposium

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Turbomachinery Laboratory

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The Texas A&M University System

Dr. Dara W. Childs, P.E., Director

Dr. Jean C. Bailey, Editor

with assistance from

The Advisory Committee

and

The Staff of the Turbomachinery Laboratory

October 1989

PREFACE

These *Proceedings* contain papers from the lectures for the Eighteenth Turbomachinery Symposium, held in Dallas, Texas, 10-12 October 1989. The Symposium is sponsored by the Turbomachinery Laboratory of the Department of Mechanical Engineering, the Texas A&M University System.

The Turbomachinery Symposia were established as a forum for manufacturers and users of industrial turbomachinery. Because of many overlapping areas in interest, the Symposia are directed primarily to commercial users with the utility and petrochemical industries.

The Advisory Committee for the Eighteenth Symposium and past symposia have had a continuing influence on the content and direction of the symposia. The committee is composed of recognized leaders in the commercial turbomachinery field from users, manufacturers, and universities. Based on their experience and knowledge of the field, papers are solicited and selected to address contemporary problems of interest. Their continued assistance is wholeheartedly appreciated.

Essential elements of the symposia which are not entirely covered by this *Proceedings* include two short courses which preceded the symposium, seven discussion groups, a panel session on "Dismantle Inspection Frequency of Critical Turbomachinery," four tutorials, and a product exhibit show. The short courses are: 1) Gearing, and 2) Fundementals of Compressor Performance and Design Audits.

The discussion groups are led by engineers with a great deal of experience in the subject areas, who facilitate discussion from the floor. Attendees *actively* participate in the discussion groups, and many use the discussion groups to get sound advice from their peers on problems of immediate importance. The discussion groups facilitate a transfer of information across industry boundaries.

The product exhibit show has over 120 representatives and features new products, accessories and analysis tools. This aspect of the symposium has continued to improve over the past several years in the quality and range of products exhibited.

Again, the vigorous support of the Advisory Committee is very much appreciated. My very considerable thanks are also extended to authors, short course speakers, tutorial leaders, panel members and discussion leaders. Finally, the efforts of the Turbomachinery Laboratory staff in seeing through the detailed execution of the Symposium are greatly appreciated, with particular thanks extended to Sherrie Shirley and Gayle Piper. With regard to this *Proceedings*, a special thank you is extended to Dr. Jean Bailey for her work in editing and preparation and to our faculty for their engineering assistance.

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October 1989

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Lectures

HEAVY SHELL CASTINGS DESIGN/ OPERATION AND REPAIR CRITERIA

by

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Mark T. Breslin, Vice President and part owner of Mechanical Dynamics and Analysis, Incorporated, worked for the General Electric Company for more than 23 years—designing rotors and buckets; specifying rotor and bucket manufacturing procedures; and marketing, installing and repairing turbine components. During this time, he wrote and published for internal distribution a complete set of bucket assembly instructions for all types of turbine buckets, which has become the repair standard.

For five years, as Manager of Turbine/Generator Maintenance for GE's Apparatus Service Business Division, he was responsible for organizing national and worldwide service shop repair practices for turbine buckets, rotors, diaphragms, and other steam path components.

Mr. Breslin graduated from Union College with a B.S. degree in Mechanical Engineering. He has written technical manuals and conducted training programs for the development of technical skills in turbine repair techniques.

ABSTRACT

Unless the utility industry is prepared as a whole to commit a very large dollar amount to the purchase of new heavy wall castings for turbine casings and valve chests, the use of the art of repair for existing castings will need to be both understood and used in the near future. Existing equipment, some of it well over 40 years old, will need to be operated and in most cases the older the equipment, the more severe this service duty, i. e., cycling.

OVERVIEW

Turbine casings or chests that contain sections operating above 750°F are subjected to thermally induced stresses that result in distortion and eventually cracking. The materials used are generally cast alloy steels 0.15 to 0.25 C., 1.0 to 2.25 Cr., 0.75 to 1.0 Mo., 0 to 0.25 V. The calculation routine needed to provide mechanical strength to support the assembled parts and to contain steam up to 3600 psi at 1000°F or 1050°F is relatively straight forward. The detail required to control casting geometry, i. e., elimination of abrupt changes in cross-section, right angle transitions, points of shell penetration, etc. can be and is extremely difficult. Once designed and manufactured, if a turbine casing could be very gradually and uniformly heated to operating conditions and then left that way indefinitely, they would probably never fail. Consideration of operating parameters is absolutely critical to prolonging the life of heavy turbine castings. Once a problem occurs, if it is major, the repair effort can be both very costly and very time consuming. Of the factors that will eventually determine the repairability of a turbine casting, two are of paramount importance, the percentage of low cycle fatigue strength and/or creep rupture strength remaining at the point of failure and the chemistry/casting quality as originally made.

PRELIMINARY COST COMPARISON

The as delivered cost of a replacement turbine shell casing for a utility size turbine from a domestic original equipment manufacturer (OEM) has historically run from $12.00 to $18.00 per pound with a one to two year delivery cycle. Quotations within the past several years indicate that replacement turbine shell casings, i. e., high pressure, reheat or high pressure/reheat combined are generally in the $1.5 to $2.5 million range in cost. These prices and delivery cycles may not, however, reflect current costs and times, as both of the major domestic OEMs have disposed of their capability to produce large castings in house and generally now procure such items offshore.

As a general comparison, a major permanent repair to such a casing will cost between $400 thousand and $600 thousand and take approximately six to seven weeks to complete. If there is extensive remachining involved, the cost will go up fractionally i. e., $50 thousand to $100 thousand, however, the time required may increase by several weeks or more.

It would indeed solve many of the problems in making decisions relative to repair or replace if turbine casings could be broken down into a finite number of categories and then well defined parameters developed that described a finite set of possible problems for each category. Unfortunately, experience has shown that as far as the individual details of exactly how to address a cracking problem in a turbine casing, each must be treated on a case by case basis. In general, the preheat, welding parameters and post weld heat treatment are standard, however, as will be described later, many other parameters will be dependent upon the individual casing geometry and the extent of the repair required.

OPTIONS (PREVENTION)

The attack on the problem of cracking of existing casings must, of necessity, be twofold i. e., prevention and then if required, repair. The comment earlier about very gradual heating and cooling cannot be understated and is fairly well understood in the industry, if not always practiced; there is, however, another area of concern in prevention. Historically, since much of the final surface of a turbine casting is used in the ascast state, the assumption has been made that the only surfaces that are critical are those that are a machined fit, i. e., blade ring or diaphragm steam fits, gib and key bearing surfaces and steam sealing flange surfaces. Many castings are filled with very sharp internal radii, obliquely drilled holes that penetrate the casing wall with razor sharp edges, sharp false cuts adjacent to finish machined surfaces, etc., and, in general, a lack of attention to the condition of the surface finish on machined surfaces.

All turbine castings should be examined with the thought in mind that any change from a flat, smooth surface is a potential site for crack initiation as the result of low cycle fatigue from thermal cycling.

A view is shown in Figure 1 of the seat area of a control valve. *Note* the sharp corner left in the casting surface just outboard of the valve seat on the left hand side. Later, NDT identified a crack in this location. In grinding out the crack, the size of the radius in the corner and the general surface finish were improved, however, had the crack been deep it would not have been possible to improve upon the geometry.

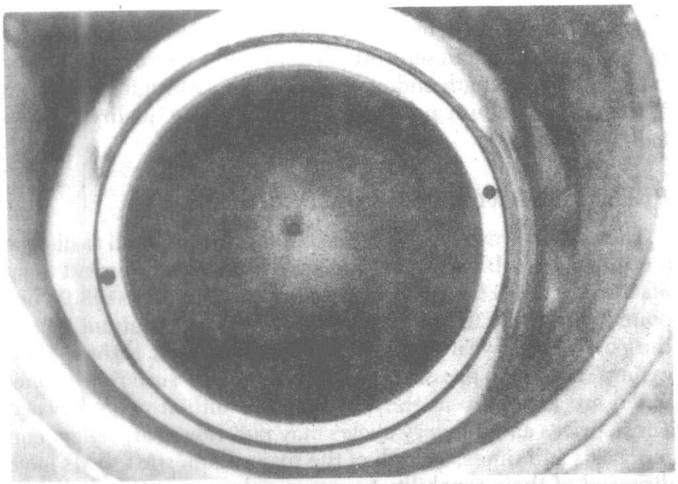

Figure 1. View of Turbine Control Valve Seat with Sharp Internal Corner in Casing Body.

The opening or ledge in a lower half, high pressure shell that contains a nozzle plate is shown in Figure 2. The circled area is an original casting defect that has led to cracking and to the left is a sharp corner that is also cracked. The very same corner in the upper half of the same turbine shell is shown in Figure 3. *Note* the very poor condition of the machined surface in the area of the crack.

A view is shown in Figure 4 of a similar high pressure casing with its nozzle plate assembled to the left in the photograph. *Note* the sharp corners in the recess in the casing for the nozzle and the groove with its small radius at the corner of the opening or ledge for the second stage stationary steampath.

It is not too difficult for an individual experienced in turbine inspection to be able to pinpoint the future location of the first crack in a new heavy turbine casing.

OPTIONS (REPAIRS)

Once a turbine casting has cracked, there are a number of items that should be considered, such as:

- The projected future for the turbine, i. e., under the framework of power requirements for the particular utility—is the turbine expected to run only five more years or 20 or more years? The answer to this question will usually dictate the method and extent of repair.
- The history of both the turbine duty cycle and any problems through the years with the casting in question—a casing that has had numerous minor temporary repairs through its life that have resulted in scattered heavy cracking in a number of locations is a poor candidate for an extensive repair with the expectation of a reasonable extension of life. On the other hand, a casing that may have deep, serious cracking, but only in one general area, is usually a good candidate for a successful repair.

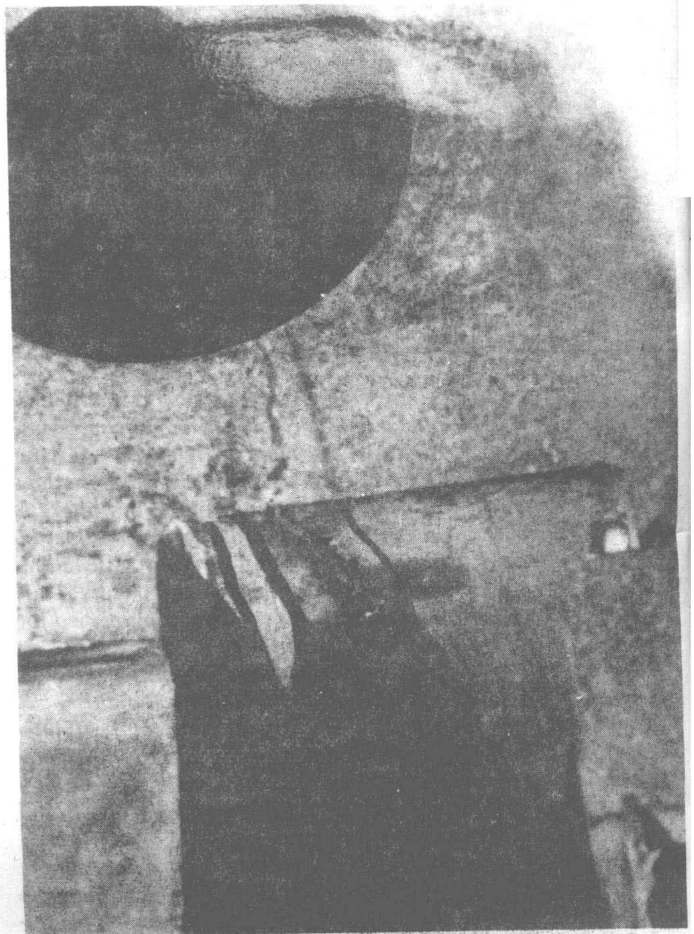

Figure 2. View of Turbine Shell Casting Illustrating Both Old Casing Defects and Sharp Internal Radii.

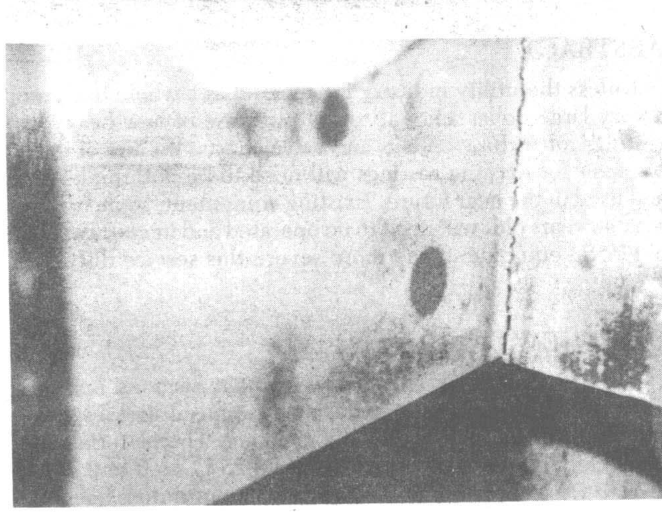

Figure 3. View of Turbine Shell Casting Illustrating Sharp Internal Radius.

- The general condition of the casting in question, i. e., extent of cracking, probable remaining life of whole casting, chemistry, etc. This item is an extension of the second option above and is extremely important. Normally, less than ten percent and in many cases less than five percent of the overall volume of a particular casing is in deep trouble. However, every bit of that five or ten percent may be unusable. The use of surface replication and "boat" samples is a must in determining how

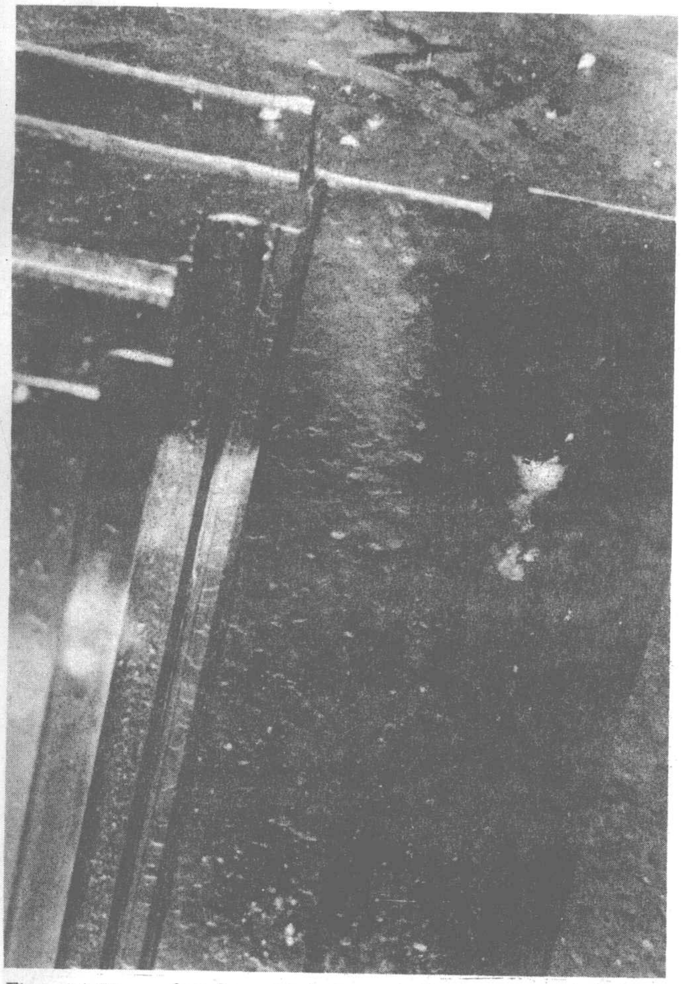

Figure 4. View of Turbine Shell Casting Illustrating Sharp Internal Radii.

Figure 5. View of Internal Surface of Turbine Shell Casting During Process of Surface Replication.

much of an existing casting is no longer usable. These methods of investigation can give an accurate picture of the extent of loss of creep rupture strength in particular areas of a casing. It is not practical to attempt major weld repairs on material that exhibits evidence of creep damage. It is possible, however, to remove fairly large areas of damaged material and replace it with either a stress relieved weld deposit or new welded in sections. A view is shown in Figure 5 of the internal surface of a high pressure shell casing that is in the process of being surface replicated for evidence of creep damage.

Advances over the last several years in the technique of surface replication have provided a readily available, effective and relatively inexpensive method of evaluating the material properties of castings in question. The material properties of a casting in service as a turbine casing or a turbine valve body will normally deteriorate first at the site of a change in cross sectional geometry, i. e., a relatively thin cross section adjacent to a relatively thick cross-section. Older turbine castings can and do suffer from a number of abnormalities that would not usually be found in a modern "state of the art" casting such as:

- Inattention to geometry which often leads to excessive shrinkage.
- Unfused or unmelted chills, nuts, bolts, etc.
- Higher than desirable carbon content.

As one experienced casting engineer explained, if the casing is old enough, do not be surprised to find part of the refractory wall buried in it.

The actual replication process should involve enough of the casing surface in the general area of all cracking that all areas of deteriorated material properties are identified. In areas of deep cracking, an edge of the crack should be removed in the form of a boat sample that can provide both chemistry and material properties. In addition, nearby areas should also have boat samples removed to obtain information for comparison with the information obtained near or at the cracks. These additional boat samples can be removed from the corners of ledges in the casing or from smooth areas of the casing wall where removal will not encroach upon casing wall thickness.

Once these questions have been addressed, there are a number of differing options available:

- Based upon location and geometry, there are some cracks that are better left alone and neither ground nor weld repaired. These will normally occur in an area where there is no mechanical loading, and the only real stress is induced by thermal gradients at the internal surface of the casting. Cracks that are the result of low cycle fatigue tend to initiate quickly but propagate slowly. The cracked material at the surface of the casting can actually protect or shield the subsurface material from the thermal gradients present at the casting surface. The high thermal stresses on the surface of a freshly ground out crack may be higher than the concentrated stresses at the tip of the crack were it not ground out. It should also be recognized that most cracks will occur at a transition from a smooth surface, i. e., an internal radius and when such a radius cracks, it is seldom possible to make the final radius, after grinding the crack out, anywhere near as large as the original radius. Views of the inside surfaces are shown in Figures 6 and 7 of a large high pressure casing in the area of the first stage nozzle box inlet openings and support recesses. Most of the internal radii contain surface cracks that are best left alone. A nozzle box support pad location in the same casing is shown in Figure 8. The crack at the back of the support pad shelf is best left alone. A radius in the corner of a stationary blade ring fit that has been sporadically ground in the past to remove cracking (Figure 9). The present cracking is centered in the middle of the old grinding marks. Because of the obvious poor present surface condition, this crack may benefit from blending a generous radius at the bottom of the fit to remove all evidence of cracking. However, when cracking reoccurs, and it will, it should be left alone.

• Localized cracks at abrupt changes in geometry can usually be ground over a series of outages until they become so deep as to present a substantial stress concentration in and of themselves, or they encroach upon the minimum required wall thickness.

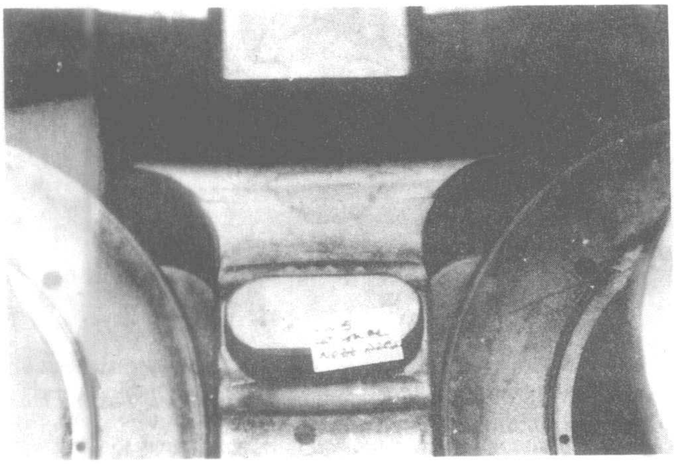

Figure 6. View of Internal Surfaces of Turbine Shell Casting Illustrating Multiple Cracking at Internal Radii.

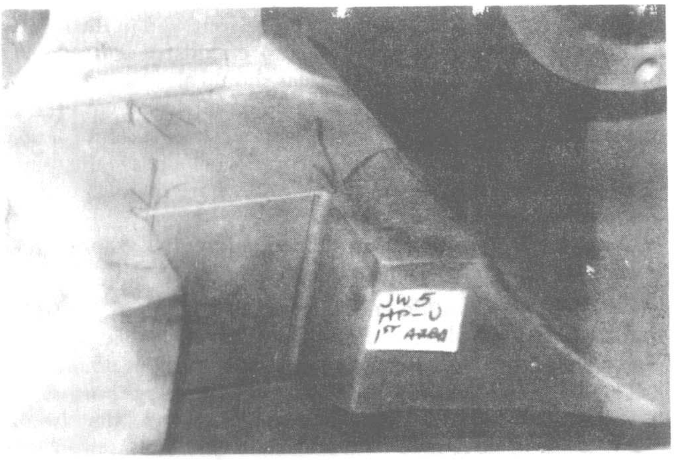

Figure 7. View of Internal Surfaces of Turbine Shell Casting Illustrating Multiple Cracking at Internal Radii.

• Extensive, deep cracking almost always requires weld repair which is normally either an unstress relieved, temporary repair or a stress relieved permanent repair. The unstress relieved temporary repair is usually a locally preheated, high nickel weld deposit of Inconel. There are a number of methods of reducing

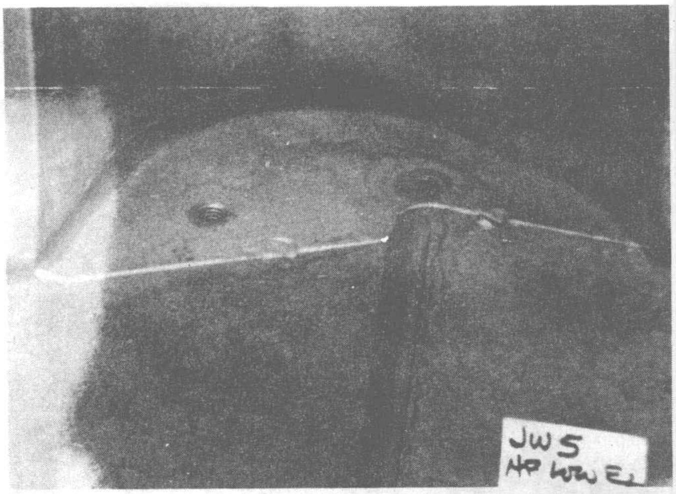

Figure 8. View of Support Pad Recess in Turbine Shell Casting Illustrating Cracking at Internal Radius.

Figure 9. View of Blade Ring Recess in Turbine Shell Casting Illustrating Cracking in Internal Radius.

the final locked in stresses in such a weld, i. e., peening, weld technique, etc. However, there is no procedure/technique to replace the benefits of stress relieving, and as such, these repairs usually result in subsequent cracking at the edges of the weld deposit with time in service. Some of the techniques commonly used to reduce both the likelihood of cracking and the effects of cracks when they occur are spreading the top third of the weld deposit over a wide area or bridging the defect with a welded in plate. The half bead weld technique can be of benefit if properly used. This basically involves laying down all final edge beads at least half bead away from the base metal. To some degree, the heat of the final pass will then hopefully relieve some of the stresses in the edges of the base metal of the casting. A fully stress relieved weld can usually be considered a permanent repair provided the overall quality of the casting is reasonably sound away from the area of repair. When a major repair is warranted on a turbine casing, serious thought should be given to a complete stress relief of the complete casing before attempting the repair and a possible interim stress relief half way through the repair. Both of these stress relief cycles are an attempt to control distortion, and in addition the first stress relief serves to prevent further cracking during the crack removal operation. There are locked up stresses in all turbine shells/casings due to the steady state thermal stresses from turbine operation. Typically there is a 300°F to 500°F thermal gradient over the length of the casing from inlet to exhaust. The final stress relief will both relieve the stresses of welding and temper both the weld and surrounding heat affect-zone. The basic steps of a major weld repair are quite detailed and each one should receive careful attention for a successful repair.

· Removal of the casing from the turbine means, in the case of an outer shell, cutting the casing free from all pipes. There is no foolproof way to accurately assess the stresses or loads imposed on a casing from permanently attached pipes and to attempt a major repair with pipes still attached is counterproductive.

· Based upon either analysis of boat samples and/or surface replication, the areas to be removed should be carefully laid out.

· Based upon the location of the area to be removed and the volume of material to be removed, an assessment should be made by an experienced turbine casing welding specialist as to the probable distortion of critical fits and surfaces as the result of the weld repair, i. e., casing joints, stationary blade ring fits, valve seating areas, etc. Provisions should be incorporated into the overall plan to weld build up and remachine, or just remachine critical areas that will be adversely affected.

· The casing must be instrumented with multiple thermocouples to monitor the preweld stress relief, the preheat for welding and the post weld stress relief.

· The casing in question should receive a full stress relief before attempting a major repair to eliminate the locked up stresses that will be present from operation. The basic stress relief cycle for these materials is a ramp rate up of 75°F to 100°F per hour max to 1275°F ± 25°F and a hold at temperature of eight hours. The ramp rate down is 75°F to 100°F per hour max., however, this ramp rate will probably be much slower below 900°F, because of inability to disipate heat from such a large insulated body.

· The material to be removed can be taken out in a variety of ways, i. e., air arc, chipping hammers (guns), grinding, etc. However, air arcing should always be followed by heavy grinding as the air arced surface may contain a high carbon content that will play havoc with the weld deposit.

· The casing must be solidly braced in every direction in which it would be expected to move as the result of a heavy weld deposit. Once again, there is need of an experienced casing welding engineer to assist in selecting the location of bracing. If possible, main turbine casings should be bolted together for as much of the work as possible. *Note*, this may require up to a complete set of replacement turbine casing bolts.

· The complete casing should be wrapped with insulation and preheated to 450°F minimum.

· Once the casing has been preheated, a complete set of dimensional references should be taken as baseline data, to track distortion as the weld repair progresses. Once again, an experienced casing welding specialist is needed for selecting the points of measurement. As multiple passes of weld are deposited, heavy interpass peening can often be very helpful in controlling distortion.

Of all of the weld deposit techniques/procedures available, sub-arc is by far the most effective and efficient. Sub-arc is:

· Relatively easy to control.

· Does not require that a welder perform critical hand welding in a hostile environment.

· Has a very high deposit rate.

Flux core and/or metal inert gas (MIG) welding are very operator dependent in what can only be described as a hostile environment and both processes are often subject to lack of fussion/penetration. Straight shielded metal arc welding (SMAW) welding is extremely time consuming, and once again requires that the welder be in a hostile environment.

· At the mid point of the weld deposit, based both upon the total volume of weld required and the distortion up to that point, an interim full stress relief must be considered. At this point, welding would stop with preheat maintained and the casing would be allowed to sit at reheat temperature for 2 to 3 hours to allow the recent weld deposit to drop to preheat temperature and then the complete casing would be stress relieved.

· When the casing has cooled to preheat temperature, the welding should continue, keeping close track of the distortion by comparing mechanical measurements taken between passes with the original measurements taken on the preheated casing.

· When all welding has been completed, including any surfaces that require welding to compensate for distortion, the casing should be allowed to soak at preheat temperature for two to three hours, to allow the most recent weld deposit to drop to preheat temperature and then a full stress relief cycle should be begun and completed on the whole casing.

· When the repaired casing has cooled after stress relief, all welds and adjacent areas should be ground and polished smooth and any critical fit surfaces that have distorted should be machined.

· The complete casing should be either shot or grit blasted and NDT tested for defects.

Cross sections of the high pressure/reheat sections of turbines are shown in Figures 10 and 11. As shown in Figure 10, the weld deposit in the center of the control valve body often exhibits deep cracking from the inside as do the stationary blade ring fits in the inner casings for the first several stages of both the high pressure and reheat sections. As shown in Figure 11, the corners in both the inner and outer casing containing the ring sealed inlet pipe to the first stage high pressure, will often develop deep cracking as will the inner casing fits for the first several stages in both the high pressure and reheat sections. Properly approached, these casings are repairable.

FINAL COST CONSIDERATION

Normally, the deterioration of a turbine casing to the point where a major decision is required takes place over a reasonably long cycle of operation, i. e., several inspection cycles. The relative repair cost and cycle times can usually be reasonably determined well ahead of the point of no return for the casing. The

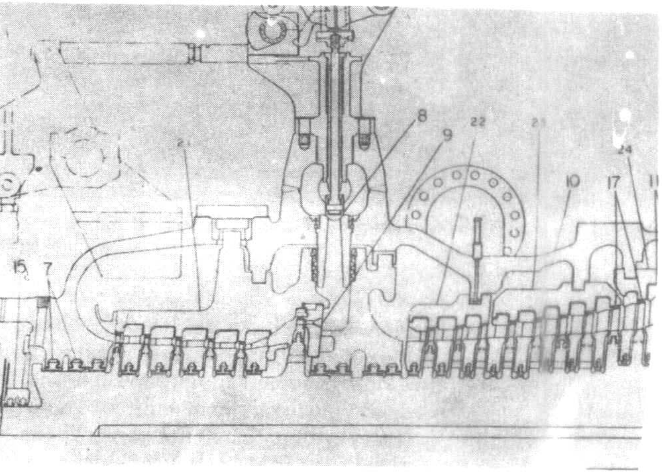

Figure 10. Cross Section Through High Pressure Section of Turbine Illustrating Control Valve Chest Welds and General Internal Geometry of Turbine Casings.

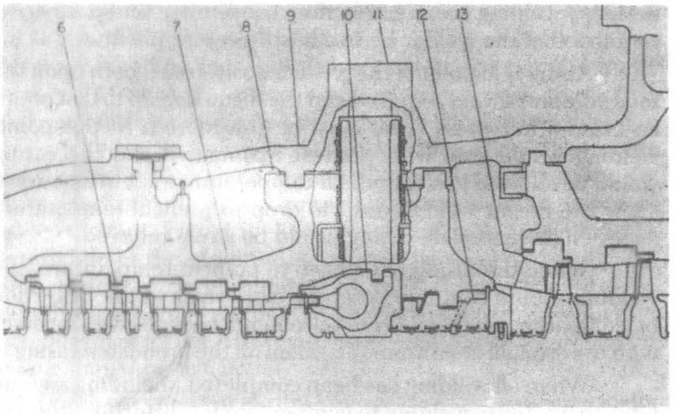

Figure 11. Cross Section Through High Pressure Section of Turbine Illustrating General Internal Geometry of Turbine Casings.

one cost that is separate from actual cost of repair, but in some cases overshadows this cost, is the value of the turbine in terms of lost generation. The normal turbine outage is usually shorter than the time required to perform a major shell repair, especially if a casing with permanently welded pipes is involved. Once a casing has been properly repaired, the problems in integrating it back into the turbine in terms of alignment should not be any greater than those encountered in installing a new casing, i. e., they both require a complete alignment.

The need to run older units in cycling duty will surely result in an increasing number of casings with problems. From a cost standpoint, the utility industry cannot afford not to explore the concept of casing repair to its ultimate.

MATERIALS FOR CENTRIFUGAL COMPRESSORS— A PROGRESS REPORT

by

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Joseph A. Cameron graduated from Carnegie Mellon University in 1941, with a B.S. degree in Metallurgical Engineering. He was employed by General Electric Company from 1941 to 1945. From 1945 until 1986, he was employed by Elliott Company in Jeannette, Pennsylvania, where he served as Manager of Materials Engineering for more than 30 years. He is now a Metallurgical Engineering Consultant for turbomachinery.

Mr. Cameron is a registered Professional Engineer in the State of Pennsylvania. He is a Fellow of ASM and past Chairman of the Pittsburgh Chapter. He is a member of NACE where he is registered as a Corrosion Specialist. He is a member of ASME and ASTM. He is a member of several technical committees in ASTM, ASME, and NACE.

ABSTRACT

Progress in materials for centrifugal compressors over the last twenty years is reviewed. Specific areas discussed include:

- Impeller materials and fabricating procedures.
- Shaft manufacture, processing, and testing.
- Casings with reference to recent changes in the ASME Boiler and Pressure Vessel (B&PV) Code.
- Sulfide stress cracking and the impact of NACE MR0175.
- Wire wool failures and their prevention.
- Repair procedures including plating, metal spray, and welding.
- Technological advances in electron microscopy and fracture mechanics and their relevance to compressor materials engineering.

INTRODUCTION

The selection of materials for rotating and stationary components of centrifugal compressors as well as other turbomachines such as axial compressors and steam and gas turbines requires consideration of a number of factors. In a review by Cameron and Danowski [1] presented at the Second Turbomachinery Symposium, it was pointed out that these considerations included some or all of the following characteristics:

- tensile properties
- modulus of elasticity
- thermal expansion
- fracture toughness
- damping
- fatigue strength
- thermal conductivity
- specific heat
- hardenability
- weldability
- corrosion resistance
- thermal stability

All of the above are mechanical, physical, or chemical properties which are amenable to some form of reasonably satisfactory quantitative measurement. For most, if not all, of these properties, it is necessary to evaluate the effect of temperature if it is significantly different from room temperature. Further, for some components it is necessary to consider properties which are not readily quantifiable. A specific example of this would be susceptibility to wire wool failure of some materials when used as bearing journals.

IMPELLER FABRICATION

Most impellers are fabricated by welding blades to forged discs and covers. A photograph of a section of a typical fillet welded impeller is offered as Figure 1. Other manufacturing techniques, however, have come into increasing use as reported by Boddenberg [2]. Several of the possible constructions are depicted in Figure 2.

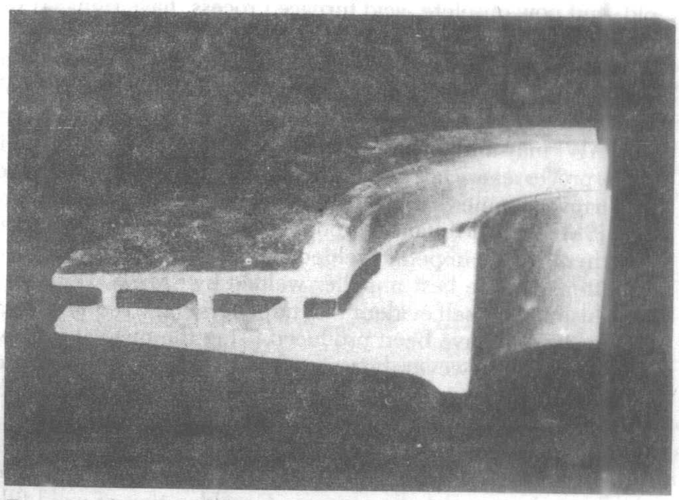

Figure 1. Section of Fabricated Impeller with Disc, Cover, and Blades.

Shielded metal arc welding (SMAW) is still used, as it has been for fifty years, and it is likely to continue to be used. While the original equipment manufacturers will probably move increasingly to other manufacturing processes, the service shops are not likely to follow as rapidly. For some years, one of the biggest problems with SMAW was delayed cracking due to hydrogen. This problem was largely overcome by the development of low hydrogen welding consumables, and by work on overcoming hydrogen problems, perhaps most notably at the Welding

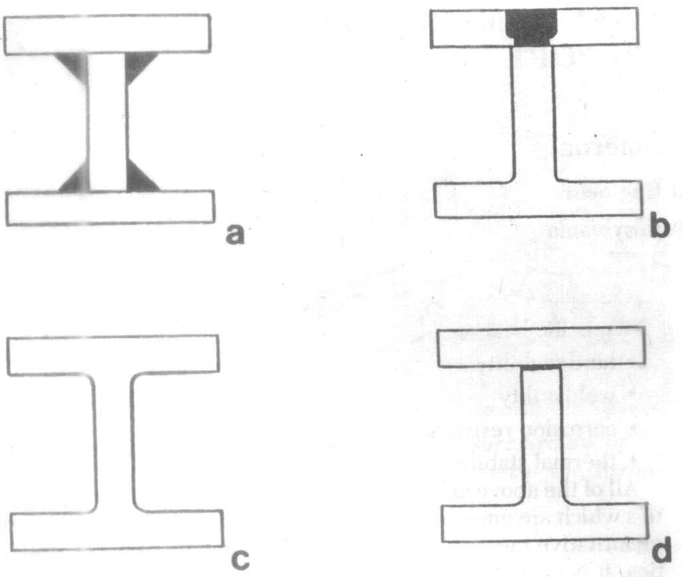

Figure 2. Impeller Constructions. a) fillet welded; b) slot welded; c) integral cast; d) brazed.

Institute in Cambridge, England [3]. The work done in Cambridge and a number of other places served to put proper emphasis on storage of welding consumables in such a manner as to prevent absorption of moisture from the atmosphere. This work also highlighted the importance of preheat and postweld heat treatment. With these parameters under control, the incidence of delayed or cold cracking decreased dramatically. Such cracking is not a major concern now when vigilance is exercised to ensure that all of the necessary precautions are taken. Reduced levels of impurities, such as phosphorus and sulfur, in steel made by the more modern processes as compared with the old, and now obsolete, acid furnace process, have reduced susceptibility to cracking during fabrication.

In the 1980s, several manufacturers automated the welding process with the use of robots. Submerged arc welding (SAW), gas tungsten arc welding (GTAW), and gas metal arc welding (GMAW) may all be used. Usually, the welding heads used in these processes are not capable of entering impeller gas passages as narrow as some that have been welded with the manual process (SMAW). The quality is good. There is some question about whether the best impeller welded with the automated processes is better than the best impeller welded by SMAW. With their long history, it is self evident that impellers of the required quality can be and have been produced using the manual process. There should, however, be a significant advance in consistency with automated processing. With automation, the results of the process are less dependent on the skill and experience factors of individual operators.

Impellers fillet welded as illustrated (Figure 2a) have welds that intrude into the gas passage. Consideration of weld fillet size is more important on small impellers than large ones, because the weld occupies a greater percentage of the gas passage cross sectional area. While the welds can be ground to present a smooth aerodynamic surface if necessary, it is desirable to keep this grinding to a minimum. It is time consuming and expensive. Moreover, it is difficult to control. It is hard to determine exactly how much material has been removed, and how much weld remains in place. If too much material has been removed, the weld thickness may be less than desired. This may be indicated if the root of the fillet weld shows on magnetic particle inspection. Such indications of a thin weld will not be found on nonmagnetic materials inspected by fluorescent or dye penetrant procedures.

Slot welding (Figure 2b) is one method that has been used to improve the aerodynamics. This process was developed, originally, to make it possible to fabricate by welding impellers having a gas passage too small to permit entry of welding apparatus. For this construction, the blades are milled or cast integral with the cover. The slots are machined in the disc. The number of impellers that have been slot welded is small in comparison with the number that have been fillet welded. Still the number of impellers that have been slot welded over the last 25 years is in the thousands.

One piece cast open impellers, similar to the construction in Figure 2c, except open on one side, are universally used in the relatively small sizes employed on shop air compressors. One piece cast closed impellers have been used increasingly in larger sizes for both single and multistage air and gas compressors. Most are produced by some variation of the investment casting process which yields close tolerances and smooth surfaces. The alloy steels used most commonly for welded impellers frequently are replaced by stainless steels for improved casting characteristics.

The first brazed impellers (Figure 2d) were produced in the 1950s. The process then was not used for a number of years. Brazing has, however, been used increasingly in the last few years. Brazed impellers are usually fabricated by machining or casting the blades integral with the disc or cover and attaching the other member by brazing. The earlier brazed impellers had acceptable quality, and some are still in service. The costs were high, chiefly because reruns through the brazing furnace were almost always required. At that time, brazing was done in a dry hydrogen atmosphere. More recently, brazing has been carried out in vacuum furnaces with much improved results and consistency. There have also been substantial advances in ultrasonic inspection techniques and equipment, making evaluation of the braze quality more reliable.

Two types of brazing alloys have been used (Table 1). Some manufacturers prefer a nickel base alloy such as American Welding Society (AWS) BNi-1a or BNi-2 while others use the gold-nickel alloy, BAu-4. In either case, the brazing temperature is in the neighborhood of 1850°F to 2000°F. For the alloy steels such as the American Iron and Steel Institute (AISI) 41xx 43xx series, this high temperature results in a grain size in the base material larger than is generally considered acceptable. The large grain size is accompanied by a low level of fracture toughness. This, however, may be corrected by cooling the brazement to room temperature, and subsequently applying the usual quench and temper heat treatment for mechanical properties. For the 12 percent chromium and 13 percent chromium-4 percent nickel grades of stainless steel, the brazing temperature is at or only a little above the usual austenitizing temperature. Thus, for these grades, the brazing and austenitizing treatments may be combined. A postbraze tempering treatment is then the

Table 1. Brazing Alloys Composition and Brazing Temperature.

	BNi-1a	BNi-2	BAu-4
Chromium	14	7	
Boron	3	3	
Silicon	4.5	4.5	
Iron	4.5	3	
Carbon	0.06	0.06	
Nickel	74	82	18
Gold			82
Brazing Temperature °F	2050	1925	1800

only additional heat treatment needed for these martensitic grades of stainless steel.

Fabricated impellers all require careful attention to the fitup of the parts prior to welding or brazing. The fillet welded construction is more tolerant of imperfect fitup than either slot welding or brazing. In addition to a good fit between the contours of the mating members, slot welding requires accuracy of the registry between the slots in one member and the blades on the other. In brazing, the strength of the resulting braze is heavily dependent on the joint thickness. Optimum strength of the joint is obtained when the thickness of the braze metal in the joint is not greater than 0.002 in to 0.004 in [2, 4].

Machining of one piece impellers by electrodischarge machining (EDM) is still practiced by some manufacturers. There is enough service experience to demonstrate that the process is acceptable even though it is necessary to remove the recast layer. Advances in EDM techniques and apparatus can minimize the thickness of the recast layer, but do not eliminate it. A recast layer of minimum thickness still has a serious adverse effect on fatigue strength [1].

At one time, the principal method of fabrication was riveting. This construction continues to be used by some compressor manufacturers in some of their new apparatus. It is also used in the manufacture of service parts for replacement of damaged impellers where riveting was employed in the original parts.

Other manufacturing procedures including electron beam welding, diffusion bonding, and electrochemical machining have been considered for the manufacture of compressor impellers. For a variety of reasons, none have been widely adopted. In some cases, the reasons have been technical problems. In others, the costs were not acceptable. In the case of electrochemical machining, there is a severe problem with waste disposal.

IMPELLER MATERIALS

Most Commonly Used Materials

Representative data on chemical analyses and mechanical properties of the various impeller materials are shown in Tables 2 and 3. The chemical analyses are typical values. The strengths shown illustrate the range of minimum requirements that may be specified. In the AISI 41xx group, for example, one might find a specification requiring a minimum yield strength of 80 ksi, and another specification requiring a minimum of 95 ksi. The same is true for the AISI 43xx series, but at a somewhat higher level of strength. These differing specifications are readily accommodated with some variation in heat treatment. Ductility requirements must be reduced modestly when strength requirements are increased.

There have been few changes in the most commonly used impeller materials in a number of years. Chromium - molybdenum alloy steels in the AISI 41xx series continue to be used in the smaller sizes and the nickel-chromium - molybdenum AISI 43xx series in the larger sizes. The exact carbon contents in these grades vary a little among different manufacturers, but the principles remain the same. As may be seen from the chemical compositions in Table 2, the 43xx series is more highly alloyed than the 41xx series. The significance of this is that the higher alloy content imparts more hardenability to the 43xx compositions. In the sizes where the 41xx series has sufficient hardenability, there is no advantage to using the more highly alloyed material. In the larger sizes, 43xx is a better choice. The term *hardenability* is not a measure of the maximum hardness that can be developed on quenching. Rather, it is a measure of the maximum section size of the material that will develop the required properties.

It is well known that the various AISI alloy steels and their modifications will yield the same mechanical properties when heat treated similarly and to the same hardness. There are some exceptions to this generalization that are important in some applications. For example, the resistance to brittle fracture at low temperatures is better for the nickel containing AISI 43xx series than for the AISI 41xx series when both have the same carbon content and are heat treated to the same yield strength.

Many of the problems that were serious a number of years ago have been virtually eliminated with the use of vacuum degassing to remove hydrogen, and basic electric steel making which reduced phosphorus and sulfur contents. Advances in what has come to be called ladle refining promise still further improvement. Some of these processes are capable of producing alloy steel plates in thicknesses of several inches having good strength and ductility in all directions, including the through thickness direction. Cross rolled steel plates have been employed for tur-

Table 2. Chemical Analyses of Impeller Materials (Typical).

Alloy Steels						
AISI	41xx	43xx				
Tradename			9% Nickel			
UNS	G41xx0	G43xx0	K81340			
Carbon	0.30-0.40	0.20-0.40	0.10			
Manganese	0.85	0.40	0.60			
Nickel		1.75	9.00			
Chromium	1.00	0.80				
Molybdenum	0.20	0.25				
Stainless Steels						
AISI	410		630	XM-12	XM-25	304
Tradename	12% Cr	13Cr-4Ni	17-4PH†	15-5PH†	Custom 450**	
UNS	S41000	S41500	S17400	S15500	S45000	S30400
Carbon	0.12	0.05	0.05	0.05	0.05	0.04
Manganese	0.75	0.75	0.75	0.75	0.75	1.00
Chromium	12.50	13.00	17.00	15.00	15.00	0.60
Nickel		4.00	4.00	5.00	6.00	19.00
Molybdenum		0.70			0.75	9.00
Copper			4.00	3.50	1.50	
Columbium			0.30	0.30	0.30	
Other Materials						
Tradename	Monel* K500	Titanium Unalloyed	Titanium 6Al-4V	Aluminum CS55	Aluminum 2025	Aluminum 7050
UNS	N05500	R50250	R56400	A035500	A92025	A97050
Carbon	0.1					
Manganese	0.6				0.8	
Nickel	66.5					
Copper	29.5			1.2	4.4	2.3
Aluminum	2.7		6.0			
Titanium	0.6	99.5				
Vanadium			4.0			
Magnesium				0.5		2.2
Silicon				5.0	0.8	
Zinc						6.2
Zirconium						0.1

*Monel K500 is a trademark of International Nickel Company.

†17-4PH and 15-5PH are trademarks of Armco Steel Corporation.

**Custom 450 is a trademark of Carpenter Technology Corporation.

Table 3. Representative Mechanical Properties of Impeller Materials.

Material	Tensile Strength (ksi)	Yield Strength (ksi)	Elongation (pct)	Reduction Of Area (pct)	Brinell Hardness
AISI 4140	100-120	80-95	16	45	212-321
AISI 4340	125-140	110-125	15	40	269-341
12% Cr Steels	95-110	75-90	14	40	212-255
Ppt'n.Hardening	130-150	100-120	15	45	269-341
AISI 304	75	30	40	50	200
Monel K500	130	85	20		255
Ti unalloyed	65	40	17	30	200
Ti 6Al 4V	130	110	10	20	HRC 36-39
Al C355	45	33	3		100
Al 2025T6	52	33	12		125
Al 7050T73	74	65	5		142

bine discs for half a century. The different shape of impeller forgings has been a deterrent to the use of plate for these applications, but this may change with the improved through thickness properties now becoming available. The viability of such a possibility is yet to be evaluated. The material would be less costly, but more material and machining would be involved. Availability of the material in heavy plate and in the small quantities needed for a few impellers could be a problem.

When more corrosion resistance is needed than can be obtained from the alloy steels, one of the grades of stainless steel is used. The austenitic grades such as Type 304 have been used in single stage machines, but seldom in multistage units because of the low yield strength. The 12 percent chromium steels, the 13 percent chromium - 4 percent nickel, and the precipitation hardening compositions, of which 17-4PH, 15-5PH, And Custom 450 are examples, have been widely used. More will be said about them in the remarks addressed to sulfide stress cracking.

In recent years, the modified 13 percent chromium steel containing about four percent nickel and slightly under one percent molybdenum has received increased attention in the United States. It had earlier been more popular in Europe where it was developed. Due to the low carbon content, it is more readily weldable than the standard AISI 410 containing 12 percent chromium. For castings, many foundries report that not only are castings of 13 percent Chromium, four percent nickel easier to repair weld, they are also less prone to defects in the first place. Castings in this alloy have been more readily available than forgings, but this is changing. There is at least one ASTM specification, A182 [5], covering the 13 percent chromium—four percent nickel steel in wrought form.

Processing of impellers made from the AISI 41xx, 43xx, alloy steels and the 12 or 13 percent chromium steels-Type 410 and the modified grade containing 13 percent chromium and four percent nickel are all similar. They derive their properties from a conventional austenitize, quench, and temper heat treatment. The alloy steels require liquid quenching, but the stainless grades do not require cooling more rapidly than in air after the austenitizing treatment. It is usual to specify that the tempering temperature be at a minimum of 1100°F in order to get a well tempered structure and a low level of internal stress. Unless sulfide stress cracking is a problem, post weld heat treatment consists of a tempering or stress relief heat treatment at 1100°F. Higher temperatures cannot be used without risk of exceeding the tempering temperature. In such an event, the yield strength of the material might be reduced to an unacceptable level.

The precipitation hardening grades, Armco 17-4PH, Armco 15-5PH, and Carpenter Custom 450, are heat treated by solution treating and precipitation hardening. The precipitation treatment should be at the maximum temperature that can be used without forming austenite. A higher level of strength could be obtained at a lower precipitation temperature, but at a sacrifice in ductility, toughness, and resistance to stress corrosion cracking. With the higher precipitation treatment temperature, the strength is still comparable to or higher than that of the alloy steels. Postweld heat treatment of the precipitation hardening grades is a stress relief treatment at, or slightly below, the temperature of the final precipitation treatment. The precipitation treatment is usually in the range of 1100°F to 1150°F, and the postweld treatment at 1100°F.

Special Materials

A variety of other materials have been used for special purposes, but not in large quantities on process gas. For example, Monel K500 is used for dry chlorine. Titanium and titanium alloys have been used for wet chlorine and in special cases where the lower density is attractive. Aluminum alloys are used in large quantities for impellers in air service, for example in diesel engine turbochargers. Aluminum alloys are seldom applicable in process gas machines. The reasons are high coefficient of thermal expansion and loss of strength at temperatures above 200°F. Nine percent nickel steel has been used for impellers in compressors for boiloff gas from liquid methane due its high fracture toughness at temperatures down to −320°F.

These special materials are processed much like the more conventional compositions with the exception of the titanium grades and Type 304 stainless steel which are used in the annealed condition. The low temperature material, nine percent nickel steel is quenched and tempered. Monel K500 and the aluminum alloys are precipitation hardened. The temperatures are different, much lower for the aluminum alloys, but the principles are exactly the same.

ROTOR SHAFTS

General Comments

In recent years, there has been much concern and discussion concerning shafts. Little of it, however, has had to do with the basic materials. Most shafts continue to be made from alloy steels such as AISI 4130, 4140, 4330, 4340, and related modifications. A comprehensive list of shaft materials in Table 4 includes some which are seldom, if ever, used for compressor shafts. In addition to the alloy steels already mentioned, Type 410, Type 304, and 17-4PH have been used occasionally. The other grades are or have been used for turbine rotors, and will be of interest in the discussion of wire wool failures. The mechanical properties of the most frequently used shaft materials are listed in Table 5. These tables are not intended to include all possible shaft materials, but, to give an understanding of the types of alloy steels, and the accompanying properties of shafts that have a history of satisfactory service.

Table 4. Shaft and Rotor Materials—Typical Analyses.

Material	Carbon	Manganese	Chromium	Nickel	Molybdenum	Vanadium
AISI 4140	0.40	0.85	0.95		0.20	
AISI 4340	0.40	0.75	0.75	1.80	0.25	
Mod. 4340	0.40	0.75	0.80	1.80	0.50	0.04
3Cr-0.5Mo	0.20	0.60	5.00		0.50	
Ni-Mo-V	0.25	0.40	0.40	2.80	0.50	0.06
Ni-Cr-Mo-V	0.25	0.35	1.50	3.50	0.40	0.10
Cr-Mo-V	0.30	0.70	1.00		1.25	0.25
Type 501	0.25	0.40	5.00		0.50	
Type 410	0.10		12.00			
Type 304	0.05	18.00	9.00			
17-4PH	0.05	0.75	17.00	4.00	Copper 4	

Table 5. Minimum Mechanical Properties Most Frequently Used Shaft Materials.

Material	Tensile Strength (ksi)	Yield Strength (ksi)	Elongation (pct)	Reduction of Area (pct)	Brinell Hardness
AISI 4140	100	75	16	45	207-321
AISI 4340	115	90	16	45	235-321
Mod. 4340	125	115	15	40	285-341

The governing factor in selection of the alloy steels for shafts is hardenability, which was discussed with reference to impellers. There is no advantage to be gained by the use of the more highly alloyed, more costly AISI 43xx series where AISI 41xx series has the mechanical properties required for the intended service. Conversely, when the sections become too large for the required mechanical properties to be met with 41xx, there is no viable alternative to the use of the 43xx group or a modification designed to have higher hardenability. When the operating tem-