MATERIALS FOR CENTRIFUGAL COMPRESSORS—A PROGRESS REPORT

by

Joseph A. Cameron

Metallurgical Engineer

Greensburg, Pennsylvania

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.

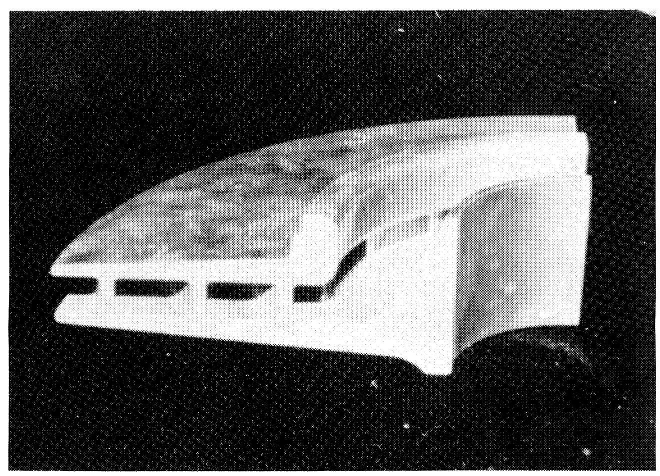

Figure 1. Section of Fabricated Impeller with Disc, Cover, and Blades.

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

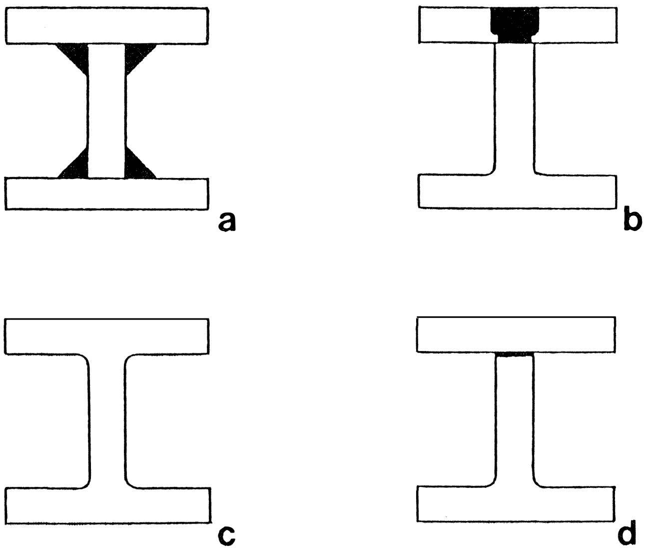

Figure 2. Impeller Constructions. a) fillet welded; b) slot welded; c) integral cast; d) brazed.

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 6AI-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 6A1 4V	130	110	10	20	HRC 36-39
A1 C355	45	33	3		100
A1 2025T6	52	33	12		125
A1 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 temperature is lower than about $-50°F$, there is also an advantage to the nickel containing AISI 43xx group which have higher fracture toughness.

There is a parallel with impellers in the manufacture of alloy steels for shafts. Because the sections are larger in shafts, vacuum degassing has been even more helpful in shafts than in impellers. With proper degassing, flaking due to hydrogen in the steel is eliminated. The improved cleanliness of the basic electric steel has resulted in fewer nonmetallic inclusions. This processing has proven to be adequate. Serious consideration to vacuum arc remelted or electroslag melted steel has not been needed.

Bars vs Forgings

The question of whether shafts should be made from bars or forgings has been debated at length. This question is a classic example of a situation where the desired results should be specified rather than the method by which they are achieved.

At times it has been thought that there existed a clear cut distinction in that forgings were produced on hammers or presses while bars came from rolling mills. Forging, however, is defined [6] as the process of working metal to a desired shape by impact or pressure in hammers, forging machines (upsetters), presses, rolls, and related forming equipment. Rotary forging machines were developed, originally, to manufacture such things as railway car axles. These machines can be and are used to produce what are sometimes called forged bars for turbine and compressor shafts. Some steel suppliers stock and sell what they call bars in diameters of twenty inches and more. These bars are in reality the product of a forging press. The distinction is not sharply defined.

The most recent revision of API 617 (Fifth Edition) [7] permits the use of rolled bars up to a finished diameter of eight inches. It is believed that this figure could be increased to nine inches. Some steel mills are capable of producing bars in sizes up to about ten inch diameter. The machining allowance recommended for bars in this size range subject to magnetic particle inspection is 0.437 in on the radius [8]. The machining allowance is needed to remove oxidation, decarburization, and surface imperfections. The machining allowances of hammer or press forged parts are greater than those for rolled bars. Because of the small quantities and the large sizes, these forgings are made on open dies. Bars can be rolled to closer tolerances than is practical for open die forgings. Obviously, when the size of the shaft is above the range where bars can be obtained, forgings must be employed. This is the case for many compressors. Forgings must also be used when bars are not available due to the relatively small amount of material needed.

The characteristics required to produce a good shaft can be stated very simply as mechanical strength and integrity. These characteristics can be present or absent in either a rolled bar or a press forged shaft. The mechanical properties are easily measured, as they have been for many years, by appropriate tests, principally tensile, but also including impact tests when low temperature toughness is of concern. The integrity of forgings or bars can be assured by means of magnetic particle and, more especially, ultrasonic testing. There are several relevant ASTM procedures, and these are usually supplemented by additional agreements between the material producer and the compressor manufacturer. There are at least two different approaches to ultrasonic testing—standards based on back reflection, and those based on reflection from a flat bottom hole.

One of the characteristics not readily measurable is that of concentricity of the final product with the original center of the ingot. While this is desirable, unless there is gross eccentricity, the importance of concentricity has sometimes been overestimated. Experience indicates that having the actual center of the forging displaced from the center of the ingot by an inch or more in a 12 in diameter makes little difference. For whatever it may be worth, this concentricity is more readily obtained with rolled bars than with forgings.

Summing up, a strong case can be made for considering the method of manufacture of the rotor shaft material to be a minor issue, leaving the decision to be made on grounds of availability and cost. As will be seen, experience on thermal stability testing supports this conclusion.

Processing

In the case of either bars or forgings of the alloy steels used for most shafts, the mechanical properties are obtained by heat treatment. Bars are finished at a uniform temperature after rolling. Forgings have a more variable finishing temperature. For this reason, forgings are usually given a normalizing heat treatment to improve homogeneity. In heat treatment for mechanical properties, the parts are heated into the austenite temperature range and liquid quenched. In most cases, quenching is done in oil, but water or a synthetic polymer may also be used. Especially in large sizes, oil quenching may not be fast enough when the specification requires impact testing at a temperature near the limit of the material. Quenching is followed by tempering at a high enough temperature to get the required ductility and stress relief while retaining the needed strength. Most specifications require that tempering be done at a minimum temperature of 1100°F. Alloy steels such AISI 4140 and its derivatives do not require multiple tempering. There is some difference of opinion about AISI 4340 and its modifications. The general case is that a single temper is sufficient, although there may be evidence for double tempering when sulfide stress cracking may be encountered.

For either material, standard manufacturing practice includes a stress relief heat treatment in the machining cycle. In the case of open die forgings, this is usually performed by the forge shop after machining to the customer's rough machining configuration. Stress relief heat treatment at 1100°F has been satisfactory.

Thermal Stability

In the 1973 report, brief mention was made of thermal stability testing. This question has received concentrated attention in the intervening years.

The procedure used for thermal stability testing of compressor shafts is similar to that described in ASTM A472 [9] for steam turbine shafts and integral rotors. Because of the lower operating temperature, compressor shafts are tested at a lower temperature such as 400°F to 500°F. The limits on permissible runout are also adjusted to fit compressor experience. Briefly, the test is performed on a lathe with the shaft between centers. A heater box is mounted on the lathe so that the ends of the shaft protrude through the end walls of the box. Three to five indicators are mounted on the shaft. There is one at each end as close as possible to the centers on which the shaft is mounted, and outside the heater box. The purpose of these two indicators is to determine whether there is any movement of the centers during performance of the test. Depending on the length of the shaft from one to three indicators are mounted on extension rods inserted through the side of the box. All of these indicators are read at intervals during heating, at hourly intervals during a hold time of at least three hours at the test temperature, and after cooling to about 100°F. The readings of most interest are those at the test temperature, usually called the final hot, and those after cooling, usually called the second cold.

Timo and Parent [10] identified four causes of instability:

- Type A due to circumferential differences in emissivity
- Type B due to relief of nonuniform residual stresses

- Type C due to non-uniform heat treatment
- Type D due to variations in thermal diffusivity

Shimoda, et al. [11], concluded that Types A and D are similar, and do not warrant rejection of the shaft. They also introduced a Type C′ which is similar to Types A and D. When the residual stresses which caused the Type B instability have been relieved by heat treatment the shaft is suitable for operation. The stress relief heat treatment may be performed in the stability testing rig.

The most serious type of instability is that due to nonuniform heat treatment—Type C. The nonuniformity may occur in any of three places. It may be in the austenitizing heat treatment prior to quenching if the furnace is overloaded, or if the parts are not supported in such a way as to obtain uniform heating. If too much material is put into the quench tank at one time, the quenching medium may not have sufficient access to all of the shaft resulting in a non- uniform quench. The third possibility is in the tempering heat treatment. Just as in the case of the austenitizing heat treatment, it is important that heating be uniform.

If sufficient precautions are not taken, the result is a difference in the microstructure around the circumference [12]. These different microstructures can have different coefficients of thermal expansion leading to unacceptable deflection on thermal stability testing. This condition can be corrected only by additional heat treatment. If the deficiency occurred in the tempering treatment, it may be possible to correct the difficulty by retempering. This, however, is the least likely of the three possibilities. It is far more probable that the instability arose from shortcomings in the austenitizing heat treatment or in the quenching cycle.

The oxide on bars after hot rolling is usually thinner, tighter, and more uniform than on forgings. For this reason, bars may be heat treated without removing the oxide. In the case of forgings, it is frequently required that forging scale be removed by machining prior to heat treatment for mechanical properties. Sometimes, these forgings have low spots which do not completely clean up in this preliminary machining. If these spots are large, the oxide should be removed by local grinding prior to austenitizing and quenching.

As pointed out, Type C instability can be corrected only by additional heat treatment. It must be recognized, however, that this can be done. In a number of cases, where the deflection on thermal stability testing was several times the maximum allowed, reheat treatment corrected the problem. The treatment required to achieve stability was a complete retreatment including, austenitizing, quenching, and tempering. The shafts in question have been in service for about ten years without difficulty.

Returning to the question of bars *vs* forgings, the record on stability testing has been about the same, with a slight edge in favor of bars.

CASINGS

Most multistage centrifugal compressor casings are fabricated or cast from carbon steel. The higher temperatures requiring alloy steels in steam turbine casings are rarely encountered with horizontally split centrifugal compressor casings. Occasionally, an alloy steel is required for strength reasons, especially, in high pressure vertically split units. Alloy steels are more frequently needed to achieve the desired toughness at subzero temperatures such as are encountered in propane and ethylene compressors, and compressors for boiloff gas in liquified natural gas service. A listing of several typical casing materials along with the usual minimum temperature for each of them is presented in Table 6. This table must be used with some caution. Some of these materials may not be readily available in small quantities.

Table 6. Casing Materials.

ASTM	Trade Name	Minimum Temperature (Fahrenheit)
Wrought		
A516 Gr.60	Carbon Steel	−50
A537 Cl.1	Carbon-Manganese	−75
A203 Gr.A	2.25% Nickel	−100
A203 Gr.E	3.50% Nickel	−150
A203 Gr.E	3.50% Nickel	−160
A353/A553	9% Nickel	−320
Cast		
A216 Gr.WCB	Carbon Steel	−20
A352 Gr.LCB	Carbon Steel	−50
A352 Gr.LC2	2.25% Nickel	−100
A352 Gr.LC3	3.50% Nickel	−150
A352 Gr.LC4	4.50% Nickel	−175
A571	Aust.Nickel Duct.Iron	−320

While the basic materials have remained unchanged, there have been some developments and changes worthy of note. One of these is the advent of calcium-argon blown steel. Plate steels such as those featured in Table 6 can be blown with calcium in an argon stream in the ladle after tapping from the melting furnace. The effect of the treatment is to reduce the sulfur content. Without this treatment the usual specification for sulfur is 0.04 percent maximum, and a typical value would be in the range 0.02 percent to 0.03 percent. With the treatment, sulfur can be specified at 0.010 percent maximum. The calcium treatment also provides inclusion shape control. In total, the steel is cleaner, less likely to contain stringer type inclusions, and, therefore, less likely to show imperfections on plate edges during in-process magnetic particle inspection. It is also less likely to be susceptible to lamellar tearing during fabrication. The advantage is questionable in lighter plate thicknesses, but can be significant in thicknesses above about three or four inches. The cleaner steel is also higher in fracture toughness which means that the low sulfur material can be made to the required standards for impact testing for a lower temperature than would be possible with the standard product. There are still further developments including ladle refining furnaces on the horizon. These developments, however, are not yet much concerned with steels of the types used in the manufacture of compressor casings.

For service at low temperatures, the usual practice in both cast and fabricated casings has been to apply the rules of ASME Boiler and Pressure Vessel Code, Section 8, Division 1, as required by API 617. In a 1987 addendum [13] to the 1986 Code, the rules that are applied to fabricated compressor casings were changed, significantly. The required Charpy V notch energy absorption values have been made a function of the plate thickness. Recognition of the effect of thickness on fracture toughness has now been extended to Section 8, Division 1 of the Code. It had previously been included in some other sections, and in some individual company specifications. Further, the latest requirements are divided on a basis of specified minimum yield strength. Formerly, the division was based on tensile strength. A few examples of the “old” and “new” requirements are listed in Table 7. In most cases, the minimum energy absorption values have been increased. In some instances the increases are nominal, but in others they are substantial. There are a few cases, in lighter plate thicknesses, where the requirements have been reduced.

Table 7. Casing Impact Test Requirements.

Service Temperature (F)	ASTM Spec	Thickness (inches)	Charpy V 1986	Notch (ft.lbs.) 1987
−10	A516 Gr.60	2.5	not reqd.	16
−25	A516 Gr.60	1	13	not reqd.
−40	A516 Gr.60	2.5	13	16
−75	A537 Cl.1	2.5	15	23
−150	A203 Gr.E	2.5	15	17

Note: Above values provided for discussion only. For specification purposes reference should be made directly to the ASME B&PV Code.

There has not yet been sufficient experience for a full evaluation of the effect of these new requirements. It is clear, however, that increasing the minimum acceptable impact energy absorption specifications will have the effect of raising the minimum temperature at which a given material can be employed.

Welding consumables that will satisfy the impact test requirements are available for all of the grades in Table 6. Actual test results for any given consumable or combination of them depend heavily, not only on their chemical composition, but also on the preheat, interpass temperature, thickness of individual weld beads, heat input rate, and post weld heat treatment. The complex interaction of these variables makes it inadvisable to cite test results, but the manufacturers should have the data for the specific welding consumables and procedures used on their casings. The greatest difficulty in meeting the requirements may occur in the base metal weld heat affected zone, rather than in the weld metal. It is not unusual, with the nickel alloy steels, to find that the welding consumables employed result in a nickel content in the weld metal a little higher than that in the base metal.

The revisions to the ASME Code published to date apply only to wrought products. A revision similar to the above in the standards for castings has not yet been adopted. Castings involve some special problems so that what is done for plate cannot be readily be applied directly to castings. Some modifications will be needed. It is assumed, however, that the same guiding principles will be followed, and that the requirements for castings will be modified along similar lines.

SULFIDE STRESS CRACKING

Background

Sulfide stress cracking involves interaction of several variables:

- Hydrogen sulfide concentration and partial pressure
- pH
- Tensile stress
- Material composition, heat treatment, strength, and microstructure
- Water
- Temperature
- Time

When all of the necessary conditions have been fulfilled, sulfide stress cracking may occur. Onset of the problem can be overcome, in many cases, by making adjustments to one or more of the above variables.

NACE MR0175

For both manufacturers and users of centrifugal compressors, the most important single event of the last fifteen years in the area of sulfide stress cracking was the 1975 National Association of Corrosion Engineers (NACE) publication, Standard MR0175 [14]. Since initial publication, MR0175 has been revised several times, and supplements have been issued between revisions. NACE has recently announced that MR0175 will be reissued annually, and that intermediate supplements will not be issued. This step should help to minimize confusion in a complex situation. Several NACE Task Groups are continuing to work actively on MR0175.

For the alloy steels such as AISI 4140 and the 12 percent chromium steels including Type 410 stainless steel, the general requirement of MR0175 is that they must have a maximum hardness of HRC 22. API 617 contains provisions requiring not only the maximum hardness, but also a maximum yield strength of 90,000 psi. There are differing schools of thought about the relative importance of hardness and yield strength. It is difficult to measure the yield strength of a weld heat affected zone. In practice, manufacturers work to a yield strength for impellers of 80 to 90 ksi. For the 13 percent chromium, four percent nickel modification, there is provision in MR0175 for a maximum hardness of HRC 23. There is also an exception for the precipitation hardening grade 17-4PH to have a maximum hardness of HRC 33 when heat treated in accordance with either of two procedures in Appendix A of MR0175. There are a number of additional exceptions dealing with materials not used for compressor impellers.

Compressor impellers for sour gas service are given a full quench and temper heat treatment after welding. With this treatment, the weld heat affected zone ceases to exist [1]. The base metal then has uniform hardness conforming to NACE MR0175 and API 617. With the stress relief postweld heat treatment used for general application the weld heat affected zone hardness may be about HRC 30.

The list of approved materials in MR0175 was, initially, based on satisfactory service experience. There is provision for adding materials on this basis as well as by successfully completing a test program where the material is tested for susceptibility according to the requirements of NACE Test Method TM0177 [15]. There is a dilemma here. Referring to the materials in Table 2, AISI 4140, for example, when heat treated to the limits required in MR0175, has an outstanding record of successful service in centrifugal compressor impellers. AISI 4140, in the same condition of heat treatment, performs well when tested according to TM0177. The 12 percent chromium steels and the precipitation hardening grades also have an excellent service record. They do not, however, perform as well as AISI 4140 in the TM0177 test. There is now a move underway to put a note concerning this situation in MR0175. There are two possible explanations for this anomaly: 1) the test conditions in TM0177 are more severe than those encountered in operation of the impellers, or 2) the operating stress is lower than that used in the test program. Probably, there is some combination of these two effects at work.

This gives rise to the question about how rigorously the requirements of MR0175 should be applied. A specific question concerns application of the specification to compressor shafts. There are many shafts in service with yield strengths much higher than that permitted for alloy steels in MR0175, but no failures have occurred. This is not just fortuitous. The highest stress in shafts occurs at the coupling fit where there is no exposure to the gas containing hydrogen sulfide. Inside the compressor where the sour gas is present, the operating stress is quite low. The combination of these circumstances gives rise to successful operation. In the large sections encountered with shafts, it is frequently desirable to use a steel of higher hardenability than AISI 4140 for the reasons discussed in the section on shafts. AISI 4340 and modified 4340 are probably the most common shaft materials. API 617 has a note appended to paragraph 2.11.1.7, approving the use of higher yield strengths for shafts for the reason cited here.

Recent Literature

MR0175 has, from the beginning, contained a prohibition against alloy steels containing more than one percent nickel. There have been conflicting reports on the effect of nickel on susceptibility to sulfide stress cracking. Payer, Pednekar, and Boyd [16] have recently published a report showing that the nickel containing AISI 4330 is more susceptible than the non nickel free AISI 4130 by only a small margin, when both are given optimum heat treatment. With a less than optimum treatment, AISI 4330 has less attractive properties. Adequate control of the heat treatment does not present a serious problem. The conclusions reached by Payer et al support the view expressed by Craig [17] that some of the earlier conclusions relative to the harmful effect of nickel did not take into sufficient account the importance of other variables such as heat treatment, microstructure, and other elements.

There has been a series of papers [18, 19, and 20] covering the development of a modification of AISI 4135 containing about 0.75 percent molybdenum and 0.035 percent columbium. Because of its higher hardenability and superior response to heat treatment, as compared with standard 4135, the modified grade requires a higher tempering temperature for the same yield strength. This reduces internal stress, and increases resistance to hydrogen embrittlement. The beneficial effect of molybdenum is at a maximum near 0.75 percent. Both higher and lower molybdenum contents have a lesser effect. Troiano and Heheman [21] have also reported on the benefits of high tempering temperatures. Tumuluru [22] has shown improvement of resistance to sulfide stress cracking with improved steel cleanliness.

Concerning the 12 percent chromium steels mentioned previously, it has been reported By Ishizawa, Shimada, and Tanimura [23], and by Klein [24] that Type 420 with a carbon content of about 0.20 percent is more resistant to sulfide cracking than Type 410 with a carbon content of 0.10 percent. This is interesting, and the material will have some applications for this reason. Compressor impellers, however, are unlikely to be one of them, because of the poor weldability of Type 420.

Gaertner and Imgram [25] have confirmed reports concerning the sulfide stress cracking problem being at a maximum somewhere below 150°F, and less serious at higher temperatures. This effect may, in part, be due to the fact that the gas would contain less moisture at the higher temperatures. Although the first stages of many compressors operate in the region of a potential problem, the later stages frequently operate at temperatures above the range of maximum susceptibility.

Keller and Cameron [26], concluded that AISI 4140 is suitable for impellers at yield strengths up to 110,000 psi. This conclusion was based on successful field experience and on a laboratory test program. In part this success may be attributed to the relationship between the stress in the impeller on overspeed testing and in operation. As shown in Table 8, the stress in operation at rated speed is 69 percent of the stress at overspeed. At the maximum continuous speed, this factor is still only 75 percent. Thus, an impeller heat treated to a yield strength of 110,000 psi. might have a stress at overspeed of about 105,000 psi, and in operation of 75,000 psi.

Table 8. Impeller Stresses at Various Speeds of Rotation.

Speed	Speed (pct)	Stress (pct)	Stress as % of Overspeed Stress
Design	100	100	69
Maximum Continuous	105	110	75
Trip	115	130	90
Overspeed	121	145	100

WIRE WOOL FAILURES

Wire wool failures have been encountered in bearings and seals of turbines, generators, and compressors [27]. Such failures do not occur frequently, but when they do, they are usually major problems. The result is not simply scoring, but deep grooving of the rotating member requiring either replacement or a major repair. Dawson and Fidler [28] reported that in one instance it took only ten minutes to wear a groove one eighth inch deep. The debris that is generated looks like steel wool, hence the name wire wooling. This damage has also been called black scab and, less frequently, machining damage (Figure 3). The term machining may be applicable to the early stages, but it is not believed to be the action that causes most of the damage. Most wire wool failures occur soon after a startup, not necessarily the initial startup of the machine. The interval of down time has sometimes been brief. The risk is increased if, during the shutdown, work was performed on the lubricating system or required opening the casing.

Typical chemical analyses for shaft and rotor materials are shown in Table 4. The susceptibility to wire wool type failure increases with the chromium content. Carbon and alloy steels such as AISI 4140 and 4340 are not immune, but are much less likely to give trouble than even the three percent chromium, 0.5 percent molybdenum steel. This latter composition was widely used in Europe for turbine rotors some 30 or 40 years ago, but is not currently in use for new construction. Fidler [29] has suggested that chromium contents be kept below 1.8 percent. The five percent chromium Type 501, martensitic stainless Type 410, austenitic Type 304, and the precipitation hardening grades such as 17-4PH have all been known to present serious risks of wire wool problems. Although they are not good candidates for shafts in turbomachinery for many reasons, it has been suggested that high carbon, high chromium tool steels which contain about one percent carbon and 18 percent chromium are not particularly susceptible to wire wooling. These materials are usually used at a much higher hardness than is the case for shafts, and it has been reported that increasing the hardness is helpful [30]. Manganese has been thought to be detrimental. The evidence for this, however, is based on poor test results on an austenitic manganese steel containing 14 percent manganese and another austenitic grade containing eight percent nickel and eight percent manganese. The effect of varying the manganese content in the range normally present in alloy steels, less than one percent, does not appear to have been studied.

The fundamental reason for the variation in susceptibility to wire wool failure among different materials is not yet understood. Such failures are, however, known to be caused by a small particle of foreign material getting into the bearing or seal. With thinner oil films the likelihood of a problem increases. Wire wool failures are more likely on thrust bearings than journal bearings, due to the higher unit loads and lesser oil film thicknesses. Obviously, however, if a foreign particle falls into a journal bearing while the machine is open, that is where the failure will occur. It is not necessary that the particle be hard. Wire wool failures have been initiated by particles of mild steel becoming embedded in the babbitt bearing material. Such particles develop a hard surface (black scab) when they rub against the rotor and

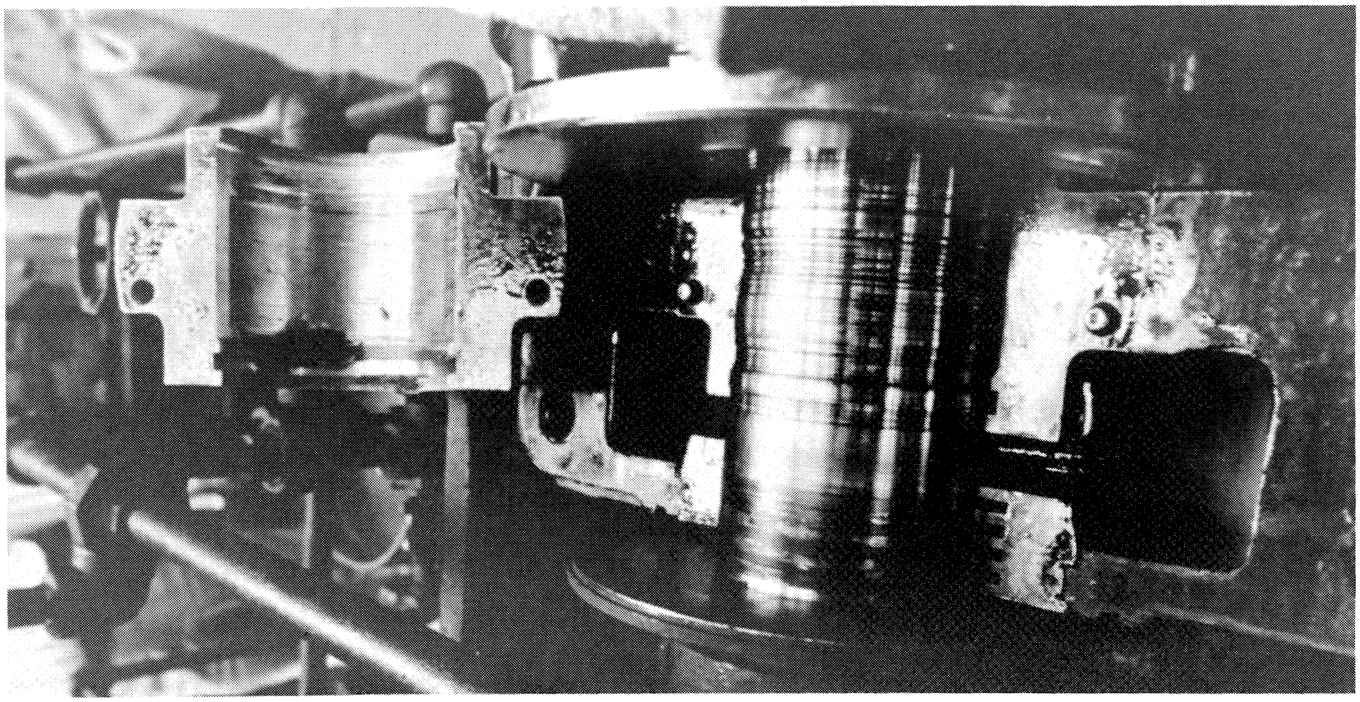

Figure 3. Wire Wool Failure Grooves in Shaft and Black Scab in Bearing.

transfer a thin layer of hardenable material from the rotor to the particle.

Summarizing his own work and that of others, Fidler [29] outlined a sequence of failure events:

- Due to the high coefficient of friction between the particle and the rotor, a high interface temperature develops.
- Transfer and bonding of shaft steel to the foreign particle.
- Hardening, partial oxidation, and perhaps thermal cracking of the transferred layer.
- Breakup and roughening of transferred layer to produce a surface configuration capable of cutting and spinning material from the shaft surface.
- Acquisition of shaft material by foreign particle to form a growing scab.
- Development of excessive heat, local oil breakdown, rapid carburization of the scab.
- Self propagation of the failure.

Even though the fundamental reason for the difference in susceptibilty to wire wool failure remains elusive, understanding the mechanism provides insight into measures that may be taken to prevent wire wooling.

It is desirable to make rotor shafts from 4140 and 4340 type alloy steels to the maximum degree possible. These materials have chromium contents well below the maximum of 1.8 percent proposed by Fidler. There are, however, situations in process gas compressors where it is necessary to use a material of higher corrosion resistance such as Type 410 or Type 304 stainless steel or one of the precipitation hardening grades such as 17-4PH. In these cases, measures can and should be taken to protect the shaft. Karpe [26] has suggested nitriding, but this may pose serious problems in getting it done on large shafts. Further, Fidler [25] has indicated that nitriding is not always successful. Other possibilities include coating the bearing journal with hard chromium plating or coating with tungsten carbide (or a mixture of carbides) or ceramic applied by detonation gun or plasma processes. Both of these latter possibilities involve some care in making certain that the work is well done. A good bond of the coating to the substrate is, obviously, essential. These coatings have been used successfully. Steel sleeves on the shaft may also be used with the sleeve being made from a material resistant to wire wooling. This is not always a viable solution, due to engineering reasons. For example, with reduction of the shaft size, the critical speed of the rotor may fall into an unacceptable range.

One possible way to reduce the risk of wire wool failures in bearings and seals is to increase the clearance. This would simply increase the tolerance for foreign particles to a slightly larger size. It would not eliminate the risk, and would probably be unacceptable for other reasons.

Restoration to service of a part damaged by wire wooling may present a difficult problem, due to the depth of the groove. Nevertheless, it is one that requires serious consideration, given the lead time and cost usually associated with making a new shaft. In most cases, the damage is too severe to permit repair by coatings of the relatively small thicknesses associated with chromium plating and detonation gun or plasma spraying. The use of a sleeve can be satisfactory if the reduction of the shaft size does not unacceptably affect shaft stress or critical speed, and if there is not an integral thrust collar that precludes installation of a sleeve. In the relatively recent past, there has been activity in weld repairing by either filling the groove or welding on a new shaft end. Such repairs have been performed successfully.

REPAIR PROCEDURES

General Comments

There has been increased attention given to the underlying technology of repair procedures in recent years. In part, this has been due to the increased sophistication of new procedures. It has also been due to recognition of the well known principle that prior preparation prevents poor performance. Although the economic pressures for quick repairs can be intense, the time spent in adequate preparation is always rewarded.

Chromium Plating

Chromium electroplating is a prime example of a process where proper preparation is an absolute essential. If the work is not clean when it goes into the plating bath, poor adhesion will surely be the result. For industrial purposes, the plating should always be hard chromium [31] and not decorative chromium. Hard chromium is deposited directly on the substrate. Decorative chromium has a very thin layer of chromium on top of intermediate layers of nickel or nickel plus copper. As the name implies, the hardness of hard chromium is very high, about 1000 on the Vickers diamond pyramid scale.

Chromium plating is one of the oldest repair procedures, having a history that goes back at least fifty years. The thickness of plated chromium is probably most frequently in the range of one or two to perhaps 25 mils. Thicker layers have been used on occasion, but infrequently, and with special precautions. The most common use of chromium for repairs probably is to restore the dimension of a diameter or bore in parts, such as bearings and seals, that have become worn or damaged in service. Chromium plating has been used in new construction for wear resistance. It has also been used for prevention of wire wool failure by application to the journal area on shafts made from a susceptible material.

Federal Specification QQ-C-320 [32] has requirements, which are well founded, with respect to treatment before and after plating:

- For parts having hardnesses below HRC 40, and not subject to dynamic stresses, prior shot peening and baking after plating are not required.
- When the hardness is under HRC 40 but the part is subject to dynamic stress, shot peening prior to plating is required.
- For hardnesses over HRC 40 and static stresses, shot peening is not required, but the parts must be baked at 375°F for a minimum of 3 hours immediately after plating.
- Parts having hardnesses in excess of HRC 40 and cyclically loaded should be shot peened before plating and baked as above after plating.

While shafts and rotors are subject to cyclic or dynamic loading, the cyclic stresses are not usually significant in the bearing or seal areas restored by hard chromium plating. Further, in most cases the hardness is well under HRC 40. There is, however, one situation where additional care is needed. If a bearing journal or seal area has been damaged by rubbing, it is possible that sufficient heat was generated to cause a local hard spot. In at least one case exactly this occurred. Cracks developed, due to hydrogen embrittlement, while the part which had been repaired by chromium plating was in storage.

Hard chromium has been reported to have lost market share to thermal spray coatings because they can be done more quickly, with less specialized equipment, and without waste disposal problems [31].

Thermal Spraying

Thermal spray coatings have been defined [33] as a family of coating systems whereby a properly prepared substrate is coated with any of a variety of materials which have been heated to the molten or semimolten state and propelled at a sufficient velocity against the substrate to produce bond strength sufficient for the application. It is thought [34] that with some combinations of coating and substrate there may be some diffusion alloying, and that some bonding may occur due to Van der Waals forces. The temperature of the substrate is increased very little during application of the coating, not sufficient to have an adverse effect on the properties of the substrate.

Many coatings applied by thermal spraying are in regular use not only as repairs, but in new construction. Aircraft engines are probably the most outstanding example where such coatings are regularly used to prevent oxidation, corrosion, wear, etc. Flame sprayed 12 percent chromium steel has a history of more than 40 years in the seal surface of steam turbine shafts under carbon seal rings. Without this coating, there would be a serious problem with galvanic corrosion of the alloy steel shaft caused by the difference in potential between steel and carbon. Some of the plasma and detonation gun coatings have been used to protect bearing and seal surfaces from wire wooling. In the above example, there has been no difficulty with wire wooling. The 12 percent chromium steel which is used has a carbon content higher than that of Type 410 stainless steel. Due to the rapid cooling of the flame sprayed metal, and the presence of oxide in the deposit, the hardness is about HRC 40.

In many cases, the thickness of flame sprayed coatings is less than 0.030 in, but thicker coatings are used when necessary. Coatings having a thickness well in excess of 0.100 in have been used successfully in some applications. Thick coatings must be applied in a series of light passes rather than a few heavy passes.

There are a number of variations in thermal spraying equipment. The oldest of these (and perhaps still the most used) flame spraying, operates with a fuel gas as the heat source. The coating material may be in either wire or powder form. This process has high deposition rates, low equipment cost, and ease of operation in its favor. The chief disadvantage is that the deposits are less dense than those applied by the more recently developed methods. Several new guns using hydrogen or propylene for the fuel gas are now being marketed. These also have a modified nozzle to increase particle velocity and bond strength. Coatings deposited with some of these guns are said to approach those deposited with a plasma or detonation gun.

Plasma processes produce higher temperatures and higher particle velocities than are possible with flame spraying. The coatings deposited by the plasma process are also more dense. The coating material is in the form of powder. There are several variations and modifications in the plasma process. Standard plasma guns may be rated at up to 40 kW, while the newer high energy guns are rated as high as 80 kW. The particle velocity is higher with the high energy gun, and both types of plasma guns produce a higher particle velocity than flame spraying (Table 9). There is a considerable spread in the published information on particle velocities. There are obvious difficulties associated with the measurement of these velocities. A converging, diverging nozzle attachment for a standard plasma gun has been used to increase particle velocity.

Low pressure plasma spray equipment has been developed to apply coatings inside a vacuum chamber. Low pressures in the vacuum chamber prevent contamination of the coating particles by oxidation of some of the highly reactive elements in coatings used for aircraft applications. Because of the lesser oxidation, low pressure vacuum chamber plasma coatings are also thought to have a higher bond strength.

Table 9. Thermal Spray Processes.

Process	Particle Velocity (fps)	Density (pct)
Wire flame spray	500-800	85
Plasma	800-1800	90-95
Detonation gun	2500	99

Coatings applied by the detonation gun process have been used for many of the same purposes as coatings applied by the plasma process. Because of the high particle velocity, the bond obtained with detonation gun coatings is probably in excess of that from other processes. Detonation gun coatings, many of which are tungsten or chromium carbides in nickel or cobalt binders, are very hard and wear resistant. They have been used successfully in restoration of bearing and seal surfaces and in a variety of applications involving resistance to severe wear or erosion.

Preparation of the surface of parts to be coated by flame, plasma, or detonation gun processes is critical. For flame spraying, the most successful procedure requires rough machining, frequently consisting of rough threading followed by knurling. When this is not possible, bonding materials that require only grit blasting may be used, but may not be as successful in application. The plasma spray and detonation gun processes require grit blasting. The grit must be carefully monitored, for breakdown of the particles and contamination with foreign material.

The thickness of plasma and detonation gun coatings is often only a few mils, but plasma coatings of 0.050 in to 0.100 in or more have been used for restoration of dimensions damaged in machining or service, and for the deposition of materials used as abradable seals.

Welding

Repair by welding for stationary parts and for impellers that were originally manufactured by welding has long been considered to be a standard practice. Repairs on the shaft and on impellers integral with the shaft have been looked at with reservation. In these cases, repairs by chromium plating, thermal spraying, or sleeving have been regarded more favorably. Repair welding has been used occasionally on steam turbine disc rims. In the last few years, there has been more attention given to repair of rotors by welding. The impetus came mainly from steam turbines where construction having the discs or bucket wheels integral with the shaft is more common than in centrifugal compressors.

There have been several recent publications outlining the general procedures, [35, 36, 37, 38]. The incentives have been time and cost. It has been reported that turbine rotors have been repaired for as little as five percent of the cost of a new rotor [35], although figures of 15 or 20 percent seem to be more common. The time saved could, in extreme cases, be as much as a year for a large turbine. These figures provided a powerful incentive for the development of acceptable procedures for repair by welding.

Welded steam turbine rotors, for new construction, have been produced in Europe for many years. There have been changes and improvements in the welding procedures over the years. The operating record has been good. This background has, no doubt, been a contributing factor in increased acceptance of welding repair. As yet, published data on the repairs are limited. Several manufacturers and repair shops have their own proprietary information on welding consumables, welding procedures, and resulting mechanical properties.

Welding repairs for such locations as bearing journals and seal surfaces would not be expected to be in areas of high stress (Figure 4). This might or might not be the case for a repair on a rotor with integral impellers. In either case, information should be studied covering the properties of the weld metal in relation to those of the original base metal. This should include a review of the properties of the heat affected zone in the base metal adjacent to the weld. Considering the alloy steels customarily used for rotors, a post weld tempering heat treatment should be part of the procedure. This might be a local treatment, as seems usually to be the practice with turbine rotors. It could be done vertically [38], or horizontally with the shaft rotating. If available, thermal stability testing apparatus could be used.

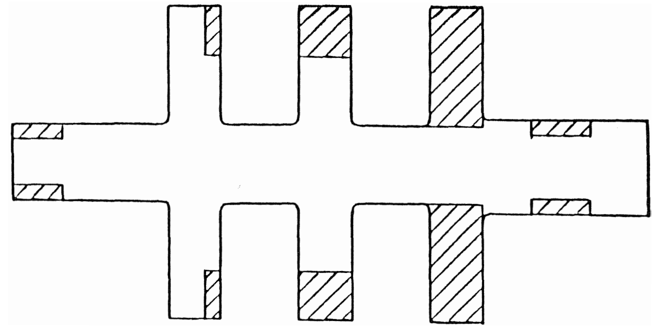

Figure 4. Possible Locations for Repair Welding of Shafts and Rotors.

Most of the repair welding of turbine rotors appears to have been performed using submerged arc welding. There does not appear to be a technical reason that other processes such as gas tungsten arc, gas metal arc, or flux core arc welding could not be employed if satisfactory properties can be obtained. All of these processes lend themselves to mechanization. There are probably a large number of combinations of filler metal and flux which are capable of yielding satisfactory mechanical properties. A full evaluation, however, requires more than identification of the consumables. It is necessary to develop a procedure on simulated parts including the welding parameters of current, voltage, and travel speed in order to know the heat input rate per unit of weld length. Full details of preheat and postweld heat treatment are needed. Variations in these parameters can have a significant effect on the resulting properties, especially in the heat affected zone of the base metal. It follows that actual welding conditions on a shaft or rotor should closely follow those on the simulated parts used for evaluation.

FAILURE ANALYSIS

In the last decade, there has been substantial technological progress with regard to failure analysis in two areas of interest to manufacturers and users of all types of turbomachinery. Specifically, these are electron microscopy and fracture mechanics used in the analysis and prevention of service problems. The two are synergistic.

Scanning electron microscopy (SEM) has been widely used in the examination of fracture surfaces. While electron microscopes are capable of magnifications higher than those usually associated with optical microscopes, much of the work done with SEM is well within the range of magnification available with optical instruments. The most important advantages to electron microscopy are increased resolution and depth of field. Resolution has been defined [39] as capability to separate closely spaced forms or entities, and is specified as the minimum distance by which two lines or points must be separated before they can be distinguished as separate entities. Depth of field is a measure of the degree to which a surface may depart from a plane and still be a sharp image. For optical microscopes, a high degree of flatness is required, while for scanning electron microscopes substantial departure from a plane surface is permissible. This makes it possible to examine the peaks and valleys of fracture surfaces directly at magnifications above the range for optical microscopes. Formerly, such direct examination was possible only with wide field low magnification (up to perhaps $30\times$) instruments.

In Table 10, McCall [40] lists comparative values of three characteristics of light, scanning electron, and transmission electron microscopes. In a scanning electron microscope (SEM), the specimen is examined directly. In a transmission electron microscope (TEM), a replica of the fracture surface is examined with the use of a transmitted electron beam.

Table 10. Comparison of Resolution and Depth of Field.

	Light Optical	Transmission Electron	Scanning Electron
Resolution, A[1]	~2000	~5	~100
Depth of Field[2]	250@15x 0.08@1200x	500@4000x 0.2@500,000x	1000@100x 10@10,000x
Magnification Range	15 to 2000x	200 to 300,000x	20 to 50,000x

[1]Angstroms
[2]Microns

In the study of fatigue fracture surfaces, the SEM may be satisfactory for measuring the striation spacing for low cycle fatigue. In high cycle fatigue, however, the striation spacing is more often smaller than the resolution capability of an SEM. In such cases, the superior resolution of the TEM makes it possible to measure the spacing. When the striation spacing has been determined and the size of the fatigue crack measured, using fracture mechanics, it is possible to derive a great deal of information concerning the cyclic stress and the number of cycles to failure. Quantitative information of this sort provides a basis for determining corrective action.

There are some difficulties. The crack growth rate is seldom constant throughout the growth of the crack. In most cases, the growth rate increases with the size of the crack requiring several measurements of striation spacing. It is advisable to make several measurements of striation spacing at different points along the path of the crack. It is not unusual for portions of fatigue crack surfaces to have been damaged during crack propagation. In the damaged areas, the striations may have been obliterated. Still, this analysis permits a better evaluation of the problem than would have been possible without it. A better understanding of the problem leads to increased probability of solution.

Examination of the fracture surface will also reveal whether the fracture was ductile or brittle, and whether it was intergranular, transgranular, or mixed mode. Representative electron micrographs showing the marked difference appear in Figures 5 and 6. Intergranular failures may be indicative of stress

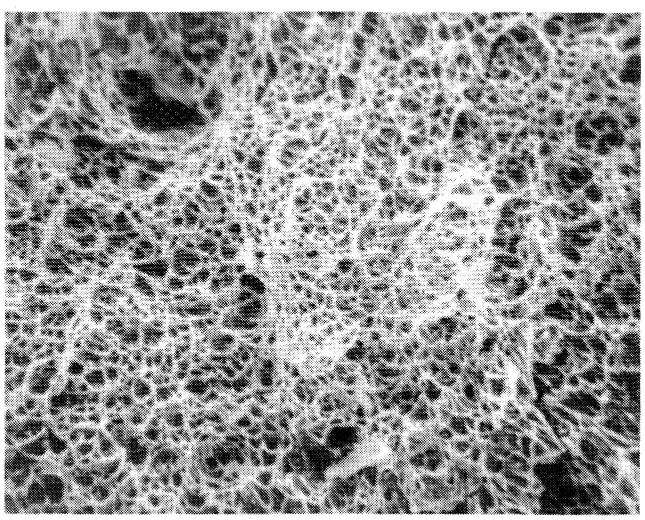

Figure 5. Magnification 575×. Electron Micrograph of Transgranular Ductile Fracture.

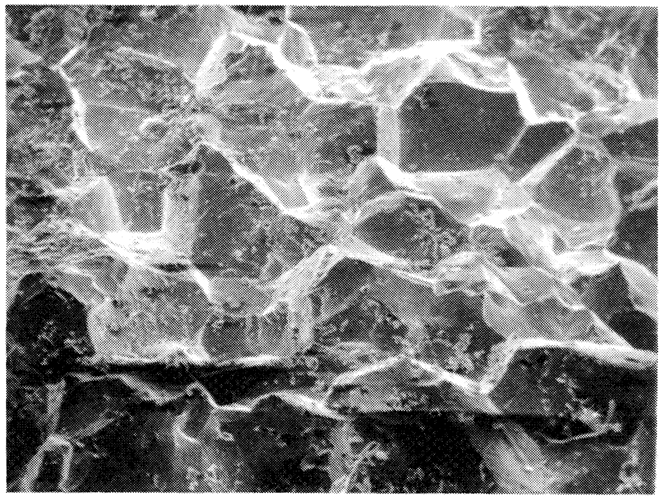

Figure 6. Magnification 60×. Electron Micrograph of Intergranular Fracture.

corrosion or corrosion fatigue. At usual compressor operating temperatures, straight mechanical failures due to overstressing or fatigue are transgranular. At elevated temperatures in the hot end of steam and gas turbines, intergranular failure may occur.

The disadvantage to electron microscopy is that the specimen being examined must be in a vacuum chamber. This imposes a limit on the size that can be accommodated. In most cases, this is not really a serious problem. Electron microscopes with large vacuum chambers are available—that is chambers capable of handling specimens of several inches in all dimensions. A great deal of useful work has been done on microscopes capable of handling specimens not larger than about a one inch cube.

Although not in the realm of failure analysis, but associated with failure prevention, fracture mechanics has made it possible to evaluate quantitatively the effect of imperfections found in ultrasonic and radiographic examination of various compressor components. When the size and location of an imperfection have been established by these inspection procedures, it is possible to assess the effect that it would have on serviceability. It is also necessary to know the state of stress during operation at the location of interest. With this information, the stress intensity may be calculated. (Note: The term stress intensity has one meaning in fracture mechanics and a different meaning in ASME Boiler and Pressure Vessel Code, Section 8, Division 2.) Critical stress intensities below which brittle fracture will not propagate have been published for most materials. Thus, if the actual stress intensity is below the critical value, the part may be accepted. Uncertainties concerning the stress must be factored into the decision making process on the conservative side.

If the stress is steady, the calculated imperfection size is valid. If the stress is alternating, however, it is necessary to evaluate the possibility of crack growth due to fatigue from an original subcritical size to a critical size. This can be done when the magnitude of the cyclic stress is known or can be estimated within reasonable limits. With this information, it is possible to make a determination concerning the likelihood of a crack propagating from the imperfection.

With specific application to welds, British Standards Institution PD 6493 [43] has provided a method for Engineering Critical Assessment of defects in welded structures. Noting that quality control limits are necessarily arbitrary and conservative, PD 6493 contains procedures for determining "fitness for purpose" of fabricated structures with various types of imperfections.

From this quick, introductory look, it may be seen that fracture mechanics and electron microscopy have provided compressor manufacturers and users with powerful tools.

SUMMARY

Progress in the last fifteen or twenty years in materials for centrifugal compressors has been reviewed. Specific subjects that were examined included impeller manufacture and materials, shaft manufacture and stability, sulfide stress cracking. casing requirements, repair technology, and failure analysis. In each case, there were significant advances during this interval. The service record of compressors has been good. With implementation of the newer and improved technology this record may be expected to be still better in the future.

REFERENCES

1. Cameron, J. A., and Danowski, F. M., Jr., "Some Metallurgical Considerations in Centrifugal Compressors," *Proceedings 2nd Turbomachinery Symposium*, Gas Turbine Laboratories, Department of Mechanical Engineering, Texas A&M University, College Station, Texas, pp. 116-128 (1973).
2. Boddenberg, K., "On the Manufacture of Impellers for Turbocompressors," *Proceedings 15th Turbomachinery Symposium*, Turbomachinery Laboratory, Department of Mechanical Engineering, Texas A&M University, College Station, Texas, (1986).
3. Coe, F. R., "Welding Steels Without Hydrogen Cracking," The Welding Institute (1973).
4. *Metals Handbook*, Ninth Edition, *6*, ASM, pp. 1002-1004, (1983).
5. ASTM A182, "Standard Specification for Forged or Rolled Alloy Steel Pipe Flanges, Forged Fittings, Valves and Parts for High Temperature Service," ASTM Book of Standards, *01.01*, pp. 68-77 (1989).
6. *Metals Handbook*, Ninth Edition, *14*, ASM, p. 6 (1988).
7. API Standard 617, Fifth Edition, "Centrifugal Compressors for General Refinery Service," American Petroleum Institute (1988).
8. *Metals Handbook*, Desk Edition, "Steel Bar, Rod and Wire," ASM, pp. 4-30 (1985).
9. ASTM A472 "Standard Method For Heat Stability Testing of Steam Turbine Shafts and Rotor Forgings," ASTM Book Of Standards, *01.05*, pp. 314-315, (1988).
10. Timo, D. P., and Parent, D. F., "Thermal Distortion of Turbine Rotors," ASME Paper 58-A-70 (1958).
11. Shimoda, H., Onodera, S., and Tokuda, A., "Study on the Heat Indication of Turbine Rotor Forgings," Bulletin of JSME, *5*, (20), pp. 595-619 (1962).
12. Barker, A., and Jones, F. W., "The Reversible Bending of Turbine Shafts with Temperature," Institution of Mechanical Engineers, London (1958).
13. Boiler and Pressure Vessel Code, Section 8, Div. 1, ASME, pp. 64-66.1 and 167-168.4, Addendum (1987).
14. Standard MR0175 "Material Requirements—Sulfide Stress Cracking Resistant Metallic Materials for Oil Field Equipment," NACE (1988).
15. Standard TM0177 "Test Method-Testing Of Metals for Resistance to Sulfide Stress Cracking at Ambient Temperatures," NACE (1986).
16. Payer, J. H., Pednekar, S. P., and Boyd, W. K., "Sulfide Stress Cracking Susceptibility of Nickel Containing Steels," Metallurgical Transactions, *17A*, pp. 1601-1610, (September 1986).
17. Craig, B. D., Letter in support of NACE Ballott Item 88-1 on revision of MR0175-88 (1988).
18. Grobner, P. J., Sponseller, D. L., and Diesburg, D. E., "Effect of Molybdenum Content on the Sulfide Stress Cracking Resistance of AISI 4130 Steel with 0.035 percent Cb," Corrosion/78 Paper No. 40, NACE (1978).
19. Straatmann, J. A., Grobner, P. J., and Sponseller, D. L., "Results of Sulfide Stress Cracking Tests in Different Laboratories on SAE 4135 Steel Modified with 0.75 percent Mo and 0.035 percent Cb," Paper 77-Pet-48, ASME (1977).
20. Garber, R., "Higher Hardenability Low Alloy Steels for Hydrogen Sulfide Resistant Oil Country Tubulars," Corrosion/82 Paper No. 122, NACE (1982).
21. Troiano, A. R., and Heheman, R. F., "Hydrogen Sulfide Stress Corrosion Cracking in Materials for Geothermal Power," Corrosion/78 Paper No. 59, NACE (1978).
22. Tumuluru, M. D., "A Study of the Sulfide Stress Cracking Behavior of Two Low Alloy Steels," Materials Performance, pp. 9-15, NACE (February 1987).
23. Ishizawa, Y., Shimada, T., and Tanimura, M., "Effect of Microstructure on the Sulfide Stress Cracking of AISI 410 and 420 Steels," Corrosion/82 Paper No. 124, NACE (1982).
24. Klein, L. J., "Hydrogen Sulfide Cracking Resistance of Type 420 Stainless Steel Tubulars," Corrosion/84 Paper No. 211, NACE (1984).
25. Gaertner, D. J., and Imgram, A. G., "The Temperature Effect on the Sulfide Stress Cracking of High Strength Casing Material," Corrosion/79 Paper No. 181, NACE (1979).
26. Keller, H. F., and Cameron, J. A., "Laboratory Evaluation of Susceptibility to Sulfide Cracking—A Study of Compressor Impeller Materials," Corrosion/74 Paper No. 99, NACE (1974).
27. Wulpi, D. J., "Failures of Shafts," *Metals Handbook*, Ninth Edition, *11*, ASM, p. 466 (1986).
28. Dawson, P. H., and Fidler, F., "The Behavior of Chromium Steel in Large High Speed Bearings," AEI Engineering, 2, 2, (April 1962).
29. Fidler, F., "Metallurgical Considerations in Wire Wool Type Wear Bearing Phenomena," Wear, *17*, pp. 1-20 (1971).
30. Karpe, S. A., "Turbine System Bearing Failures Generally Classified as the Machining Type," ASTM Paper No. 90 (1967).
31. Chessin, H., "Hard Chromium Plating," *Metals Handbook*, Ninth Edition, 5, ASM, pp. 170-187 (1982).
32. QQ-C-320, Federal Specification—Chromium Plating (Electrodeposited), Amendment 2, U. S. Government Printing Office (December 1963).
33. Clare, J. H., and Crawmer, D. E., "Thermal Spray Coatings," *Metals Handbook*, Ninth Edition, 5, ASM, pp. 361-374 (1982).
34. Kutner, G. L., "Thermal Spray by Design," Advanced Materials and Processes, ASM, pp. 63-68 (October 1988).
35. Russell, N. D., "Welded Repair of Steam Turbine Rotors," The Locomotive, *66*, (4), Hartford Steam Boiler Inspection and Insurance Company, pp. 83-87 (1988).

36. Munson, R. E., "Engineering a Welded Rotor Repair," The Locomotive, *66*, Hartford Steam Boiler Inspection and Insurance Company, pp. 88-93, (1988).

37. Kim, G. S., Indacochea, J. E., and Spry, T. D., "Weldability Studies in Cr-Mo-V Turbine Rotor Steel," J. Materials Engineering, *10*, (2), pp. 117-132 (1988).

38. Bertilsson, J. E., Scarlin, R. B., and Faber, G., "Philosophy of Weld Repair of Steam Turbine Rotors," Advances in Materials Technology for Power Plants, ASM, pp. 573-581 (1987).

39. *Metals Handbook*, Ninth Edition, ASM, *9*, p. 15 (1985).

40. McCall, J. L., "Fracture Analysis by Scanning Electron Microscopy," MCIC Report 72-12 (1972).

41. Logsdon, W. A., "An Evaluation of the Crack Growth and Fracture Properties of AISI Modified 12 Cr Stainless Steel," Engineering Fracture Mechanics, *7*, pp. 23-40 (1975).

42. Barsom, J. T., "Fatigue Crack Propagation in Steels of Various Yield Strengths," Journal of Engineering for Industry, Trans ASME, pp. 1190-1196 (November 1971).

43. PD 6493:1980, "Guidance On Some Methods for the Derivation of Acceptance Levels for Defects in Fusion Welded Joints," British Standards Institution (1980).