High-Temperature Bolting Life Prediction and Life Assessment

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REPORT SUMMARY

Informed run/replace decisions for high-temperature bolting require nondestructive evaluation (NDE), life assessment, and life prediction. This report gives an improved, analytical life prediction method based on stress relaxation calculation of accumulated creep strain and measured bolt length.

Background

High-temperature turbine cylinders and valve covers are jointed using studs/bolts. Because of the high cost of bolt replacement, it is clear that replacements should be made only when the bolt is near its end of life. Based on utility experience, there is a large population of high-temperature turbine and valve studs/bolts approaching end of life; this situation requires a method to identify bolts susceptible to failure and to perform remaining life calculations.

Objective

To develop and validate an improved, analytical creep life prediction method for high-temperature bolting.

Approach

The project team reviewed bolting life assessment guidelines and life prediction approaches and performed limited testing of ex-service bolts. They concluded that a strain-based approach is appropriate for both life assessment and prediction. Accurate prediction of strain accumulation during stress relaxation requires attention to detail. The creep equation must be able to describe the initial, rapid relaxation strain rates, the long-term relaxed stress, and the complete spectrum of relaxation behaviors—strain hardening, steady state, and strain softening. Because predicted life mirrors input data, improved life prediction requires improved estimates of both operating conditions and current condition.

Results

The life prediction method uses creep behavior, an incremental calculation procedure, and the strain hardening flow rule to predict the stress relaxation behavior. Creep constitutive equations were developed using a two-parameter material model for three bolt materials: 1Cr-1/2Mo-1/4V, 1CrMoVTiB, and 12Cr-1Mo-1W-1/4V. The failure criterion was an accumulated inelastic or creep strain limit of 1%. This failure criterion is consistent with a ductility exhaustion approach for creep failure at the thread root.

For validation, the calculated results were compared to the results of uniaxial stress relaxation testing, bolt model testing, and service experience. For the uniaxial tests, good agreement was found between the observed and calculated relaxation properties for 12Cr-1Mo-1W-1/4V. For bolt model tests, the calculated values for accumulated inelastic strain as a function of cycles

were in good agreement with those measured for 1CrMoVTiB steel. For service-exposed bolts, the current length of HP turbine inner cylinder and stop valve bolts were measured. The initial bolt length was estimated as the nominal specified and was used to calculate the accumulated creep strain. Because of this uncertainty in initial length, the observed creep strain was considered unreliable. The temperature of the stop valve studs were measured. The measured temperature at the mid-point of the valve studs was uniform and approximately 55-60ºF below the nominal steam temperature of 995ºF. There was a temperature gradient of approximately 30ºF along the length of the stud.

EPRI Perspective

This report provides an overview of the complex issues involved in life assessment and prediction of high-temperature bolts. The analytical procedure developed in this study coupled with other industry-wide NDE and measurement procedures is expected to provide broad guidelines to utilities for bolting life assessment. By integrating remaining life prediction and field measurements, this method can produce significant economic O&M benefits for utilities.

TR-113529

Keywords

Bolts
Life assessment
Creep
Stress relaxation

ABSTRACT

A research project was conducted to develop and validate an improved, analytical life prediction method for high temperature turbine and valve studs/bolts. The life prediction method used the two-parameter creep equation, an incremental calculation procedure and a strain hardening flow rule. The failure criterion was an accumulated inelastic or creep strain limit of 1%. The life prediction procedure recommends the use of the service history of operating temperature, number/stress level of tightenings, cycle time, etc., to calculate the stress relaxation behavior. Life assessment uses the measured bolt length to calculate the accumulated creep strain. The link between the current condition, i.e., accumulated creep strain, and the remaining creep life, i.e., time to accumulate 1% strain, is obtained by a prediction of the future creep strain accumulation under the intended loading cycle(s) imposed during future operation. In order to validate the approach, the calculated results were compared to the results of uniaxial stress relaxation testing, bolt model testing and service experience. The analytical procedure coupled with other industry wide NDE and measurement procedures reviewed in the report is expected to provide broad guidelines to utilities for bolting life assessment. The report also includes limited test data on service exposed bolts.

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1 INTRODUCTION

Need for Life Assessment of Bolts

Life assessments are needed to avoid unexpected component failure and premature replacement of components prior to the end of useful life. One important factor affecting the life consumption of high temperature components is the nature of the stresses generated by the applied mechanical and thermal loads. For design and analysis, stresses are classified as either load controlled or displacement controlled. The hoop stress in a pipe produced by internal pressure is an example of a load controlled stress. The axial stress in a bolt produced by an initial applied stretch is an example of displacement controlled stress. Displacement controlled stresses are repeated or cyclic and relax with time. Because of the accumulation of creep damage with continued cycling, displacement controlled stresses can lead to failure. The objective of the current project was to develop and validate an analytical life prediction technique for displacement controlled situations such as that found in high temperature bolting.

To allow for periodic disassembly, inspections and any necessary repairs, high temperature turbine cylinders and valve covers are jointed using studs/bolts. The requirements for high temperature bolts are twofold; to maintain a steam tight joint and not to fracture with possible catastrophic consequences (Branch et al., 1973). In addition, the bolts are expected to have a service life of approximately 30 to 40 years. Bolts are tightened either by thermal or mechanical means to an initial displacement or stretch (Bolton, 1995). The magnitude of the displacement is determined by design to develop the appropriate clamping force for the joint. The initially high stress relaxes with time as the bolt creeps at the operating temperature.

Because of the high cost of bolt replacement, it is clear that replacements should be performed only when the bolt is near the end of life. Based on utility experience, there is a large population of high temperature turbine and valve studs/bolts approaching the end of life and the need for a method to identify bolts susceptible to failure and to perform remaining life calculations (Ellis, Sielski and Viswanathan, 1998). To address these needs, the current project was conducted to develop and validate an analytical creep life prediction method for high temperature bolting.

Need for This Work and Uniqueness

Currently, to the author's knowledge, no domestic utility uses analytical life prediction as a part of their bolting life management strategy. A condition assessment approach is used where the run, repair, or replace decisions are based on experience, engineering judgement, OEM recommendations and the results of the inspection. The inspection yields information regarding only the current condition (primarily, either cracked or uncracked) of the bolting. Clearly, both the degree of accumulated creep damage and the possibility/probability of failure during the next applied stress relaxation cycle are unknown.

The advantages of the marriage of field measurements of critical damage indicators and the analytical calculation of remaining creep life using these values for comprehensive life management are readily acknowledged. The current life prediction approach is unique in that it offers this potential to link field measurements and remaining life prediction. For the case of bolting creep life prediction, the critical field variable identified by this study is bolt length and its use in calculation of accumulated inelastic or creep strain. The link between the current condition, i.e., accumulated creep strain, and the remaining creep life, i.e., time to accumulate 1% strain, is obtained by a prediction of the future creep strain accumulation under the intended loading cycle(s) imposed during future operation. Other important features of the current work that are unique include the following:

- The creep constitutive equation describes the complete creep curve. Thus, it can predict strain softening stress relaxation behavior characteristic of end of life (Mayer and Konig, 1992). The two parameter creep equation is written in terms of rupture time and rupture ductility (Othman and Hayhurst, 1990).
- Based on comparisons of calculated and observed, the calculation provides a good prediction of relaxed stress and accumulated creep strain over a wide range of times - about 0.1 hour to one year, temperatures, and initial stress levels typical of service (Ellis and Tordonato, 1999).
- The failure criterion is an accumulated inelastic or creep strain limit of 1% and is consistent with a ductility exhaustion failure approach for failure at the thread root.
- The life prediction procedure recommends the use of plant specific data for number of tightenings, cycle time, temperature and initial stretch.

With regard to the use of plant data, the applied conditions that lead to increased creep damage and life consumption are well known. For high temperature bolting, these factors are the following; (1) temperature - hotter bolts fail first, (2) stress - overtightening decreases life, (3) cycling - numerous and/or frequent tightening decreases life and (4) ductility - creep brittle bolts fail first. Because the results of the life prediction mirror the input data, improved life prediction requires improved estimates of both operating conditions and current condition.

The current report gives the results of a literature review, limited testing (tensile, Charpy, fracture toughness, creep and creep-rupture) of service exposed bolts, creep constitutive equation development, validation studies (laboratory and field), conclusions and recommendations based on the results of the current study. In addition, an outline of an integrated methodology for bolting life assessment is given in Appendix A.

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2 BACKGROUND

Introduction

A review of current practice for life assessment techniques for studs/bolts accumulating damage under the cyclic, creep-relaxation loading condition was performed. The primary topics of the review were life assessment techniques and service experience/performance of high temperature bolting. Bolt failure mechanism include creep rupture, brittle fracture, low-cycle fatigue and stress corrosion (de Witte and Stubbe, 1985; Viswanathan, 1988; Townsend, 1995). The focus of the current research was life assessment for the creep rupture failure mechanism. However, because the brittle fracture mechanism may be related to the presence of a creep crack, temper embrittlement and methods to identify low toughness materials were also included in the search. The review included EPRI, OEM and other technical publications as well as a search of selected utility records. The following data and information was included in the review:

- Bolting material composition and heat treatment.
- Properties: stress relaxation, thermal expansion, creep, creep rupture, tensile, Charpy, fracture toughness and hardness.
- Service experience: failure history, failure mechanisms, root cause of failure, metallurgical examination and testing results.
- OEM: recommended tightening procedure and life in number of tightening cycles and/or years.
- Stress relaxation and life assessment methods.
- This section gives the results of that review for the following topics; bolt materials and properties, failure mechanisms, manufacturer's recommendations, and other industry guidelines and life assessment approaches.

Bolt Materials and Properties

Selection of the appropriate bolt material for a specific application depends primarily on the design conditions such as required clamping force and expected operating temperature. In addition, the thermal expansion coefficient for the bolt and flange material must be considered (Mayer and Keienburg, 1980; Mayer and Konig, 1992; Mayer and Konig, 1995). Material selection also varies with the original equipment manufacturer (OEM) and the country of origin (Ellis, Sielski, and Viswanathan, 1998). The major factors that determine bolt life are the relaxation strength, creep ductility and fracture toughness (Mayer and Keienburg, 1980; Mayer and Konig, 1992).

Bolting materials can be broadly grouped in the following four types; low alloy steels, 9-12% chromium steels, precipitation hardening austenitic steels and nickel base alloys (Schaff, 1995). For high temperature applications, the low alloy steels are the CrMoV alloys of two nominal compositions:

- 1Cr-1/2Mo-1/4V materials such as the ASTM A193 B16 and ASTM A437 Grade B4D in the USA (designated B5F5B by General Electric and 8337 by Westinghouse), Durehete 950 in the UK and both the 40 CrMoV 47 and 21 CrMoV 57 steels in Germany.
- 1Cr-1Mo-3/4V or Durehette 1055 in the UK.

The 9-12% chromium alloys include Modified 9Cr-1Mo, Type 422 stainless steel - ASTM A 437 Grades B4B and B4C (designated B50A125E by General Electric), General Electric alloys B50A125B with 12%Cr, 3%W, 5%Co, and 1/4%V and B50A125C with 12%Cr, 2.5%W and 1/4%V, Greek Ascoloy, HT9, and both X22 CrMoWV 121 and X19 CrMoVNbN 111 in Germany. Alloy A286 is a commonly used precipitation hardening austenitic bolting steel. The nickel base steels include Nimonic 80A, Nimonic 90, Inconel 718, Refractalloy 26 and Westinghouse 47-12. Detailed chemical and mechanical property specifications for these alloys are found in several sources such as the ASTM standards, ASM Metals handbook, DIN 1740, etc. The chemical composition for several bolting alloys is given in Table 2-1 for several low alloy steels and in Table 2-2 for two 12% Cr steels and two nickel base alloys.

The mechanical property requirements of ASTM A437 - 92) for high temperature bolt materials are given in Table 2-3. Physical and mechanical property data as well as creep rupture properties are also given in the Aerospace Structural Metals Handbook (1977) for 1Cr-1/2Mo-1/4V, 1Cr-1Mo-3/4V, and 12CrMoWV or Type 422 stainless steel. The physical properties, service experience, creep and creep-rupture properties and bolt model testing results for Nimonic 80A and Refractalloy 26 are discussed in the EPRI report on bolt materials for advanced power plants (Mayer and Konig, 1992).

With regard to life assessment, both the thermal coefficient of expansion coefficients and the Young's modulus are important physical properties. Figure 2-1 compares the coefficient of expansion coefficients for typical flange and bolting materials (Mayer and Konig, 1992). The expansion mismatch between the 1CrMoV flange material and the 12Cr bolt materials results in thermal stresses (proportional to thermal expansion differences) for the 12Cr bolt that are in addition to the mechanical stresses generated by the initial bolt stretch. Table 2-4 gives the elastic or Young's modulus values as a function of temperature for two common bolt materials (Tanaka and Ohba, 1984).

Possibly, the most important material property for high temperature bolts is the stress relaxation strength of the bolt material. The relaxation strength is usually taken as the relaxed stress after a fixed time, nominally 10,000 to 30,000 hours as a function of initial strain level, typically between 0.1% to 0.2%. Figure 2-2 compare the relaxed stress for several common bolt alloys after 30,000 hours for an initial strain level of 0.2%. Analysis of the stress relaxation data was beyond the scope of the project but there are numerous sources of relaxation properties including Branch et al. (1973), Mayer and Konig (1995), Konig and Mayer (1995), Townsend (1995), NRIM (1997), etc.

Table 2-1
Chemical Composition In Weight Percent Of Low Alloy Bolt Materials

Element	B4D	21 CrMoV 57	40 CrMoV 47	D1055
C	0.36-0.44	0.17-0.24	0.36-0.47	0.17-0.23
Mn	0.45-0.70	0.30-0.60	0.45-0.70	0.35-0.75
Si	0.20-0.35	0.50 max	0.15-0.35	0.40 max
Ni	---	0.50 max	--	0.20 max
Cr	0.80-1.15	1.10-1.50	0.80-1.15	0.90-1.20
Mo	0.50-0.65	0.50-0.80	0.50-0.65	0.90-1.10
V	0.25-0.35	0.15-0.35	0.25-0.35	0.60-0.80
Ti	--	--	--	0.070-0.15
Al	--	--	--	0.15-0.8
As	--	--		0.20 max
B	--	--	--	0.001-0.10
Cu	--	--	--	0.20 max
Sn	--	--	--	0.020 max

Table 2-2
Chemical Composition Of Selected High Alloy Bolt Materials

Element	B4B & B4C	X19CrMoVNbN111	Nimonic 80A	Refractalloy 26
C	0.20-0.25	0.16-0.22	0.04-0.10	0.08 max
Mn	0.50-1.00	0.30-0.80	1.00 max	0.40-1.00
Si	0.20-0.50	0.10-0.50	1.00 max	0.50-1.50
Ni	0.50-1.00	0.30-0.80	Base	35.0-39.0
Cr	11.00-12.50	10.00-11.50	18.0-21.0	16.0-20.0
Mo	0.90-1.25	0.50-1.00	--	2.5-3.5
V	0.20-0.30	0.10-0.30	--	--
W	0.90-1.25	--	--	trace
Al	0.05 max	0.020 max	1.00-1.80	0.25 max
B	--	0.005 max	0.008 max	0.001-0.01
Nb	0.05 max	0.30-0.60	1.80-2.70	2.5-3.5
N	--	0.50-0.100	--	18.0-22.0
Fe	base	base	1.50 max	balance

Table 2-3
Mechanical Property Requirements Of ASTM A 437- 92 For High Temperature Bolt Materials

Property	Grade B4B	Grade B4C	Grade B4D Versus Diameter - Inches		
			2-1/2"	2-1/2"- 4"	4" - 7"
Yield, min – ksi	105	85	105	95	85
UTS, min – ksi	145	115	125	110	100
Elongation, min - %	13	18	18	17	16
Reduction of Area, min - %	30	50	50	45	45
Charpy - ft*lbf	10	25	--	--	25
Brinell Hardness, max	331	277	302	302	302

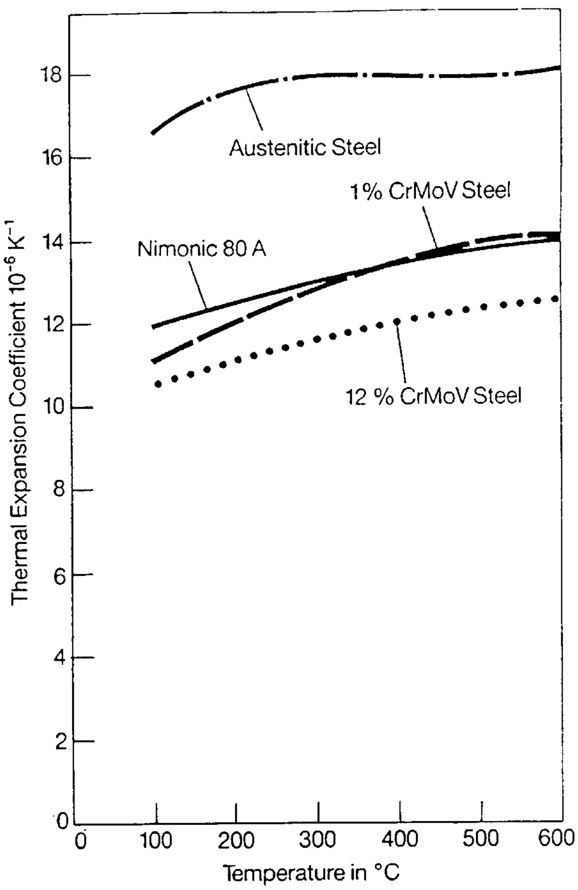

Figure 2-1
Thermal expansion coefficients as a function of temperature for bolt materials. (Mayer and Konig, 1992).

Table 2-4
Young's Modulus As A Function Of Temperature For Two Bolt Materials (Tanaka And Ohba, 1984)

Steel	Young's Modulus – ksi			
	842F	932F	1022F	1112F
1Cr-1/2Mo-1/4V	26,600	24,320	21,050	-
12Cr-1Mo-1W-1/4V	-	23,890	21,480	18,205

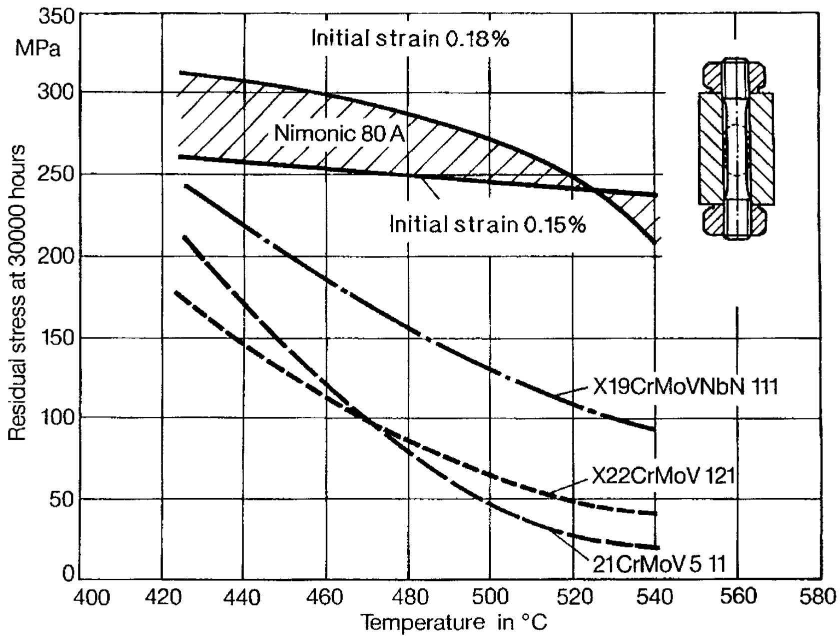

Figure 2-2
Residual stress for bolt joints after 10,000 hours and 30,000 hours for an initial strain of 0.2% according to DIN 17 240. (Mayer and Konig, 1995).

Failure Mechanisms

Bolt failure mechanisms include creep rupture, brittle fracture, stress corrosion cracking and low-cycle fatigue (de Witte and Stubbe, 1985; Viswanathan, 1988; Townsend, 1995). In general, the recent service experience with 1CrMoV and 12CrMoWV bolts has been good (Mayer and Konig, 1992; Townsend, 1995). Townsend (1995) reviewed the early CEGB experience that resulted in the CEGB Code of Practice for bolt replacement. Based on the experience of the CEGB and others, the failure location is at the first engaged thread for bolts that fail by creep rupture (Mayer and Keienburg, 1980; Hickley, 1992; Hickley and Bulloch, 1992; Townsend, 1995). Creep rupture cracks have also been found at the thread roots for bolt model test specimens (Holdsworth and Strang, 1995). The main causes of failure identified by the CEGB and factors that exacerbate the tendency for creep cracking in the first engaged thread were the following:

- Material related - low creep rupture ductility, or notch brittleness.
- Design factor - cracking in the first engaged thread at the hot end of the bolt.
- Tightening procedures - uncontrolled tightening, probable overstraining and frequent tightening.

Currently, there are no nondestructive techniques that can reliably identify low ductility material. The other factors are operational and controlled by utility practice.

The tendency for creep failure at the first engaged thread is related to the stress concentration at the thread root and ductility exhaustion. Holdsworth and Strang (1995) used a relationship for estimating the multi-axial rupture ductility from the uniaxial ductility using values for the effective and hydrostatic stress typical for the multi-axial stress state at the thread root. For the thread root region of their model bolt, the multi-axial rupture ductility was 0.09 times the uniaxial ductility.

Manufacturer's Recommendations

The bolt tightening, inspection and life assessment recommendations of three turbine manufactures - General Electric, Westinghouse and ABB, are discussed in this section. The life prediction method used by a fourth turbine manufacturer, Parsons, is believed to be the similar to the National Power (former CEGB) method discussed below.

ABB recommends that high temperature bolts should be checked periodically for creep elongation (ABB, 1992). The creep elongation is calculated using the current bolt length and the original length "which is usually stamped on the bolt." They recommend replacement of high temperature studs/bolts (service temperature greater than 788°F) based on either of the following two conditions: (1) when the elongation exceeds 1% and (2) when the operating time exceeds 150,000 hours. For inspection, they recommend that the following; (1) measure stud/bolt length at major overhauls, nominal 30,000 to 50,000 hours intervals and (2) perform testing, UT, for cracking at intervals of 100,000 hours.

Westinghouse Electric Corporation has "not established "hard" guidelines as to when bolts should be replaced" (Londergan, 1993). In their experience, the major cause for bolt/stud replacement is thread damage that occurs during the opening/closing operations. The percentage of studs/bolts damaged per cycle depends on the care that is taken but they not that as many as 10-15% of the bolts may be damaged. With regard to bolt life, the major limiting factors cited were bolt stretching, bowing and cracking. For high temperature bolts/studs operating at temperatures greater than 800°F, they estimate that the bolting can endure 8 to 12 retightening cycles before the damage becomes significant.

With regard to bolt tightening, their recommended procedure is relatively standard with the amount of stretch equal to free length times a factor to produce the desired initial strain. The nominal tolerance on stretch is +/- 10%. The bolt free length is calculated as flange thickness from joint face to spot face plus washer thickness plus stud diameter. Westinghouse does note that the only positive means to determine the stretch of a bolt/stud is by measurement, typically using a micrometer.

General Electric has provided recommended guidelines for tightening, inspection and replacement of high temperature valve studs (General Electric, 1979). Their recommendations were based on the results of studies of the variables that affect valve stud life including the following; time in service, number of retightenings, tightening stress level, thermal cycling and differential expansion. They concluded that the major factors were tightening stress level and number of tightenings to that stress level. Based on these results, they implemented a reduced tightening stress level in 1970 as shown in Figure 2-3. The "1970 Level" is approximately 70% of the pre-1970 stress level.

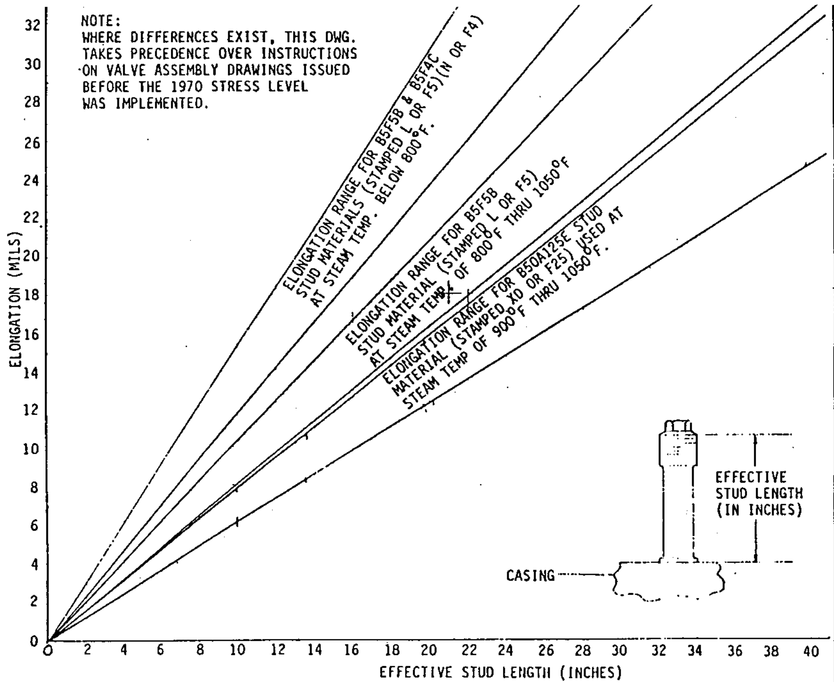

Figure 2-3
Recommended elongation for pre-stressing valve studs. (General Electric, 1979).

Their inspection frequency and replacement recommendations were based on analytical methods that were calibrated against laboratory and field data. The end of life or failure criterion is based on a 50% probability of stud cracking. Because of the change in stress levels, the inspection/replacement recommendations depend on the initial time in service. Only the recommendations for studs in service after 1966 are discussed below.

The inspection interval and replacement recommendations depend on stud material. The recommendations for the B5F5B stud material, 1Cr-1/2Mo-1/4V, are given in Table 2-5. As shown in the table, it was recommended that the studs for the valve upper heads and stands be inspected for cracking after every three (3) tightenings or every six (6) years, whichever comes first. Following 11 tightenings, the studs should be replaced at the next valve inspection.

Table 2-5
B5F5B Stud Inspection And Replacement Recommendations (General Electric, 1979)

Stud Inspection No.	1	2	3	4
Tightening No.	3	6	9	*
(or) Years Service	6	12	18	24

* Replace after 11 tightenings

The recommendations for the B50A125E stud material, 12Cr-1Mo-1W-1/4V, are given in Table 2-6. As shown in the table, it was recommended that the studs for the valve upper heads and stands be inspected for cracking after every three (3) tightenings up to 18 and after every two (2) tightenings thereafter or every six (6) years, whichever comes first. Following 25 tightenings, the studs should be replaced.

Table 2-6
B5OA125E Stud Inspection And Replacement Recommendations (General Electric, 1979)

Stud Insp. No.	1	2	3	4	5	6	7	8	9	-
Tightening No.	3	6	9	12	15	18	20	22	24	*
(or) Years Service	6	12	18	24	30	36	42	48	54	-

*Replace after 25 tightenings

Other Industry Guidelines and Life Assessment Approaches

Condition and life assessment guidelines for threaded fasteners have been developed by the CEGB (1986), VGB (1988), Laborelec (De Witte, 1989) and ESB (Bulloch and Bissell, 1995). The CEGB (currently National Power) uses a calculation based on a formula involving the total operating hours and the number of tightenings to assess the operating life of bolts (CEGB, 1986; Townsend, 1995; Elsender and Carr, 1995). Jones (1995) has reviewed the VGB approach. They recommend dye penetrant and ultrasonic inspection for crack detection and elongation, diameter and hardness measurements for creep strain accumulation and softening. In addition, selective destructive tests are used such as elevated temperature tensile, accelerated creep and creep rupture and toughness. ESB's approach uses conventional NDT to detect cracking and measurement of average strain, grain size, phosphorus content and hardness to insure fastener integrity (Bulloch and Bissel, 1995).

Laborelec's approach for turbine bolt condition and life assessment method follows a one page logic diagram shown in Figure 2-4 (de Witte, 1989). The prior event of interest is the service history of bolt prestress, i.e., as-measured stretch and calculated initial stress level. The primary NDE techniques are magnetic particle testing (MT), ultrasonic inspection (UT) and permanent elongation. Both MT and UT are used for crack detection. If cracks are found, then the bolt is replaced. For the case of no reported indications, the permanent elongation is measured as a function of bolt position and operating hours. Plots of measured strains versus time reveal the influence of the cycle time, number of re-tightenings, level of applied prestress and any differences in the left and right side of the turbine. Bolt replacement is recommended at an accumulated strain of 1%. In the Laborelec method, diameter and hardness readings on the bolt shaft are supplementary techniques used to detect evidence of creep strain as either a local reduction in area or lower hardness values (Mayer and Konig, 1986). Destructive testing is performed only as part of a failure analysis.

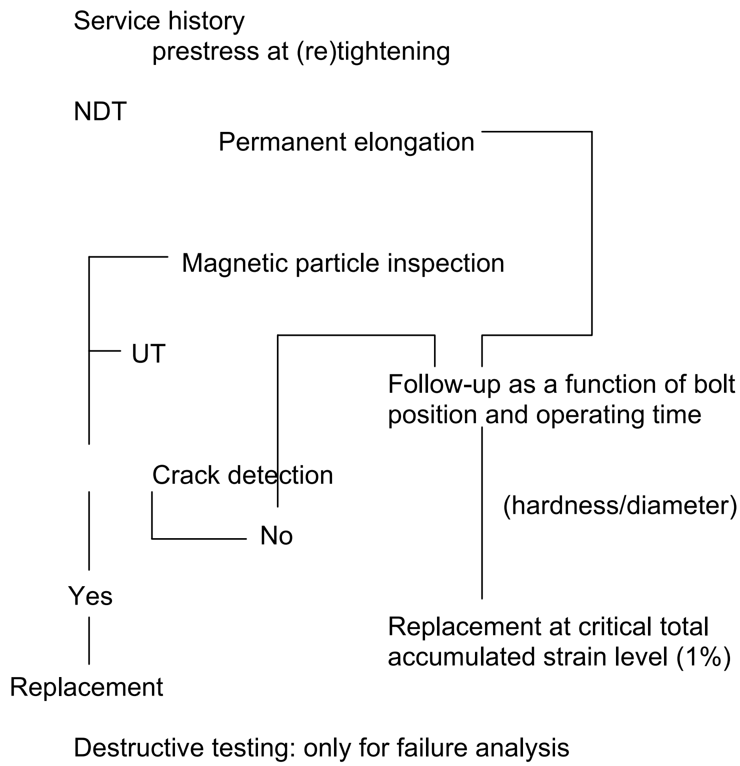

Figure 2-4
Logic diagram for turbine bolt life assessment developed by Laborelec. (De Witte, 1989).

One method currently used to assess the operating life for bolts is a calculation based on a formula involving the total operating hours and the number of tightenings (Parker, 1984; CEGB, 1986; Elsender and Carr, 1995). For example, one tightening to typical design level is equal to 15,000 hours of service. Uncontrolled tightening procedures can result in applied loads that are either higher or lower than design. Both levels of tightening have serious consequences; possible leaks for loads significantly lower than design and premature failure for loads consistently higher than design loads. To account for uncontrolled tightening operations in their procedure, the life consumed per tightening is increased by a factor of from 1.5 to 3 times that for a controlled tightening. The total equivalent service time is compared to the listed values for the specific bolt material and operating temperature. For Durehete 950 comparable to ASTM B4B, the basic material life varies with temperature range as follows; 350,000 hours for 700-750F, 300,000 hours for 751-850F, 200,000 hours for 851-900F and 150,000 hours for 901-960F.

Ellis, Sielski and Viswanathan (1998) recommended an integrated methodology for bolt life assessment with life prediction, condition assessment, failure/fracture analysis and recommended replacement criteria. Condition assessment relies primarily on nondestructive techniques including elongation for creep strain accumulation, UT for crack detection and either the small punch test or composition/microstructural examination for embrittlement susceptibility. Failure analysis uses the root cause method and fracture analysis uses the failure assessment diagram (FAD) approach. Life prediction focuses on creep failure and recommends the use of the service history of operating temperature, number/stress level of tightenings, cycle time, etc., to calculate the stress relaxation behavior. The life prediction method used the two-parameter creep equation, an incremental calculation procedure and a strain hardening flow rule. The failure criterion was an accumulated inelastic or creep strain limit of 1%.

Because of this tendency for failure by cracking in the threads, several approaches have been developed for life prediction by the creep rupture failure mechanism for bolts. The life prediction is usually based only on crack initiation at the thread root rather than both initiation and crack growth schemes. The strain rate damage approach of Schlotner and Seeley (1985) has been shown to be useful for notched bars and valve bolts. Their method requires a finite element stress analysis to obtain the stresses as a function of time.

Ando et al. (1990) use a simplified approach based on an FEM analysis. In this approach, the maximum principal stress and the effective stress at the stress concentration is related to the net section stress by relations involving the stress concentration factor and the effective time related to stress redistribution. They used either the maximum principal stress or the effective stress and either smooth or notched bar rupture properties in order to obtain the rupture time.

Currently, there are several mature technologies available for low alloy CrMoV material to determine embrittlement susceptibility. These methods are based on composition and microstructural examination techniques (Viswanathan and Gehl, 1991; Bulloch and Hickey, 1993) and/or fracture toughness using nondisruptive bolt sampling and mechanical testing, i.e., the small punch test (Foulds and Viswanathan, 1994; Foulds and Viswanathan, 1996). One advantage of the small punch test method is the direct correlation of the small punch measured fracture toughness with conventionally measured value using the ASTM procedure. For CrMoV rotor steels, the fracture appearance transition temperature, FATT, was shown to be a function of phosphorous content and grain size. There were different trends for small grain size typical of Grade D rotor material from those of large grain size typical of Grade C rotor material (Viswanathan and Gehl, 1991). As expected, similar considerations have been shown to apply for CrMoV bolting material.

Bulloch and Hickey (1993; 1994) have developed an assessment procedure to identify reverse temper embrittlement in 1CrMoV bolts. They remove a small section of material from a noncritical location on the bolt. The chemical composition, hardness, and prior austenite grain size are measured using the sample. Based on toughness testing of selected bolts, it was shown that grain size and bulk phosphorous content correlated with embrittlement. On a plot of grain size, d, versus phosphorous content, %P, the embrittled region was separated from the non embrittled region at an interface described by the following:

$$d\,(\%P) = C \qquad \textbf{Eq. 2-1}$$

where C is a constant that depends on accumulated strain (Bulloch and Bissell, 1995).

For embrittled Type 422 bolts, microstructural examination results were not conclusive regarding the cause of embrittlement (Brooks and Zhou, 1989). However, Auger analysis of fracture surfaces indicate that P segregation to the grain boundaries is involved in the weakening of the grain boundary and allows intergranular fracture for Type 422 (Choudhury, Padgett and Brooks, 1990). Hickey and Parker (1995) have studied embittlement in X19 CrMoVNb 111 bolts after approximately 30000 hours at ~490C. Auger analysis of intergranular fracture surfaces revealed high content of P, Mo, C, N, and Cr. Based on the results of the small punch test, they concluded that this test is useful in monitoring for embrittlement.

Fracture analysis has frequently been used to estimating the critical size of cracks at the time of failure. However for bolts, it is also useful in performing a failure analysis. For the case of bolts containing cracks, Holdsworth and Strang (1995) have shown that the failure assessment diagram (FAD) approach developed by the British Standards Institute is applicable (BSI, 1991). The FAD approach requires crack depth, applied load, material properties of flow stress and fracture toughness. Details on application of FAD can be found in the references cited.

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3
MECHANICAL PROPERTIES OF EX-SERVICE BOLTS

Introduction

Limited testing - tensile, Charpy, fracture toughness, creep and creep-rupture - was performed on service exposed bolts for two primary reasons. First, although the long-term operating experience with the conventional bolt materials has been good, it is important to determine if service exposure/stressing has resulted in any unacceptable reductions in the material properties. For example, lowered yield strengths for service exposed bolts implies that plastic deformation could occur during bolt tightening operations. A second motivation for testing was the desire to obtain more comprehensive creep strain-time data than the tabular time to total strain data found in the literature for 1CrMoV and 12CrMoWV (NRIM, 1997). Even limited creep data would be helpful in evaluating the two parameter creep equation selected for life prediction development.

Post exposure tensile, Charpy and stress relaxation tests have been performed on 1CrMoV and 12CrMoV bolts (Mayer and Keienburg, 1980; Mayer and Konig, 1986). Results for 1CrMoV bolts have shown the following; Charpy energy towards bottom of scatterband for virgin, yield strengths slightly below the nominal pre-load (for a 0.18% stretch) at room temperature in a few instances and relaxation strength slightly below virgin at 797F but within scatterband for virgin at 896F and 1005F. Incidents of in-service embrittlement and reduced Charpy impact properties have also been reported for 1CrMoV (Bulloch and Hickey, 1993a; Bulloch and Hickey, 1993b; Bulloch and Bissell, 1995) and for 12CrMoWV bolt materials (Parker, 1984; Hickey and Parker, 1995; Parker and Wilshire, 1995). For the 1CrMoV bolts, the tendency towards embrittlement can be related to chemistry, specifically % phosphorus, grain size and accumulated creep strain.

For 12CrMoWV bolts, metallurgical examination revealed significant differences in microstructure for both virgin and service exposed bolts (Parker, 1984; Parker and Wilshire, 1995). With regard to creep properties, the minimum creep rate for the service exposed bolts with the coarse microstructure was from 10 to 1000 times that for the new material at high stresses. At 1000F, the creep-rupture life of the service exposed bolts compared to mean strength new material was approximately 1/10 at high stresses and 1/3 at ~50 ksi.

Materials

In the current study, three service exposed bolts and one nut were obtained. The first bolt, A, was removed based on an indication detected by UT inspection. The other two bolts, C and D, had fractured. The nut, B, contained the mating fracture surface for one of the fractured bolts. Figure 3-1 documents the as-received condition of the service exposed/failed bolts and nut. All of these threaded fasteners had eight threads per inch.

Bolt A

Nut B, Bolts C and D

Figure 3-1
Documentation of service exposed turbine bolt and nut samples in as-received condition.

Chemical analysis, visual examination and mechanical property testing was performed. The chemical composition of the service exposed materials is given in Table 3-1. The three bolts all contained 12% chromium with bolt A corresponding to the 422 stainless steel or ASTM A437 B4B/B4C composition (GE 50A125E) while the other two bolts had compositions consistent with the General Electric bolt material designated as 50A125B. The nut material composition is similar composition to the bolt specification ASTM A437 B4D, a 1Cr-1/2Mo-1/4V material.

The results of a previous study conducted by TVA on seven service exposed bolts is included for comparison (Sielski, 1992). The chemical composition of their 12%Cr bolts is given in Table 3-1 while the chemical analysis results for the ASTM A437 B4D bolts is given in Table 3-2. For these 12Cr bolts, one bolt, 2740, had a composition consistent with the General Electric bolt material designated as 50A125B while the second bolt, 2738, had a composition consistent with the General Electric bolt material designated as 50A125C.

Table 3-1
Chemical Composition Of Service Exposed Bolts And Nut

Element	Bolt A	Nut B	Bolt C	Bolt D	2740	2738
C	0.22	0.48	0.21	0.21	0.20	0.28
Cr	13.23	0.97	10.82	10.94	12.5	12.5
Co	0.11	0.13	4.56	4.76	5.7	0.02
Cu	0.12	0.18	0.08	0.08	0.16	0.21
Mn	0.51	0.54	1.26	1.31	1.34	1.24
Mo	0.99	0.51	0.06	0.06	0.06	0.04
Ni	0.5	0.18	0.28	0.28	0.13	0.16
P	0.018	0.17	0.018	0.019	0.022	0.026
Si	0.35	0.28	0.33	0.43	0.61	0.27
S	0.26	0.013	0.010	0.009	0.008	0.015
V	0.22	0.27	0.25	0.27	0.25	0.18
W	0.89	-	2.65	2.67	3.0	2.6

Table 3-2
Chemical Requirements For A 437 Grade B4D Bolting Material And Measured Composition For Service Exposed Bolts

Element	A 437 Grade B4D		Service Exposed 1CrMoV Bolts				
	Range (%)	Var. (%)	#36	#67	#70	#75	#76
C	0.36-0.44	0.02	0.51	0.55	0.49	0.46	0.47
Mn	0.45-0.70	0.03	0.65	0.63	0.61	0.61	0.61
P, max	0.04	0.005	0.013	0.011	0.010	0.018	0.019
S, max	0.04	0.005	0.011	0.011	0.009	0.007	0.010
Si	0.20-0.35	0.02	0.30	0.30	0.29	0.26	0.24
Ni	...		0.09	.07	0.07	0.15	0.15
Cr	0.80-1.15	0.05	0.97	1.09	1.07	0.84	0.86
Mo	0.50-0.65	0.03	0.49	0.55	0.54	0.50	0.52
V	0.25-0.35	0.03	0.31	0.25	0.24	0.26	0.27

Tensile Properties

For bolt A and nut B, the tensile specimen was taken from the uniform section of the bolt and near the mid-section of the threaded portion for the nut. The tensile axis was in the axial direction for both. The room temperature tensile properties are given in Table 3-3. Based on the measured tensile properties, the tensile properties of the 12%Cr materials are nominally equal and bolt A would meet the requirements for ASTM A437 B4B material. The minimum tensile property requirements at room temperature for B50125B are as follows; yield strength of 90 ksi, tensile strength of 145 ksi, elongation of 13% and reduction of area of 30%. Based on this requirement, bolt 2740 would meet the strength requirements but would be below the specified minimum elongation. The tensile properties for the low alloy nut and bolts are nominally equal to those for the ASTM A437 B4D bolt material with a diameter of 4-7".

Table 3-3
Room Temperature Tensile Properties Of Service Exposed Bolts

Material	Yield Strength (ksi)	Tensile (ksi)	Elongation (%)	Red. in Area (%)
Bolt A	123.9	158.6	13	38
2738	106.3	158.1	10	-
2740	119.6	163.1	6	-
Nut B	93.2	120.3	21	61
#36	92.2	127.3	16	-
#67	95.5	110.6	22	-
#70	89.7	113.7	22	-
#75	91.3	109.9	20	-
#76	91.7	111.8	24	-

Charpy And Fracture Toughness Properties

Standard size Charpy specimens, 0.394" square, were used. The Charpy specimens were notched in the axial - circumferential plane. The measured Charpy impact energy at room temperature are given in Table 3-4. The measured impact energy for all 12Cr bolts is low and the Charpy energy for service exposed 12CrMoWV bolt A is below the ASTM A437 specified value of 10 ft-lb for unexposed 12CrMoWV bolt material. However, this result is consistent with the results of Parker (1984) and Parker and Wilshire (1995) that showed a lower impact energy for service exposed 12CrMoWV bolts compared to virgin material. The measured impact energy for nut B and all bolts except #36 would meet the ASTM A437 requirement of 25 ft-lb specified for B4D or 1Cr-1/2Mo-1/4V bolt material.

Table 3-4
Charpy Impact Energy At Room Temperature

Material	Impact Energy - Ft-lb		
	1	2	3
Bolt A	6	7	6
2738	2	-	-
2740	2	-	-
Nut B	33	34	25
#36	8	9	11
#67	45	40	39
#70	41	44	40
#75	60	60	36
#76	55	39	55

The fracture toughness tests were conducted at room temperature, 77F (Sielski, 1992). The specimen was a bend bar with a thickness, B, of 0.700" and a depth, W, of 0.800". The specimens were fatigue pre-cracked and the toughness tests were performed in accordance with the ASTM E399-90 specification. The measured toughness values, K_Q, in units of ksi $\sqrt{}$in were the following; 55.6 for #36, 52.5 for #67, 51.5 for #70 and 38.4 for 2738. The test specimen for bolt material 2740 failed in precracking.

Creep And Creep-Rupture Properties

The tensile axis of the creep specimens was oriented in the axial direction of the bolt/nut. The nominal specimen dimensions were a gage diameter of 0.350" and a gage length of 1.25". The measured rupture properties are given in Table 3-5. The rupture properties for bolt A were compared with those for unexposed 12Cr-1Mo-1W-1/4V steel given in the NRIM data compilations (NRIM, 1979; NRIM, 1997) at 1112F and 1202F. Based on this comparison, the rupture strength for the service exposed bolt is significantly above average, i.e., at the top of the scatterband.

Table 3-5
Creep-Rupture Properties Of Service Exposed Bolt And Nut

Temp. - F	Stress - ksi	Time – Hours	Elong. - %	Red in Area (%)
Bolt A (12Cr-1Mo-1W-1/4V)				
1112	42.7	247.8	21	65
1112	31.3	1981.1	17	48
1202	19.9	347.9	19	63
Nut B (1Cr-1.2Mo-1.4V)				
1022	38.4	167.3	21	33
1022	34.1	418.9	12	20
1022	31.3	743.1	14	16

For the service exposed 1Cr-1/2Mo-1/4V nut tested at 1022F, the rupture properties were compared with those given for unexposed bolt material in the NRIM data compilation (NRIM, 1997) and the results of Ewald et al. (1988). In essence, the rupture strength of the service exposed nut was approximately equal to those given by Ewald et al. (1988) and significantly greater than that for the unexposed bolt material in the NRIM compilation. This creep strength comparison is consistent with the lower than expected relaxation strength found for the NRIM bolt compared to values given in the German data compilation and cited by Mayer and Konig (1995).

The creep strain versus time curves are shown in Figure 3-2 for the 422 stainless steel bolt A and in Figure 3-3 for the 1Cr-1/2Mo-1/4V nut B. The primary portion of the creep strain versus time curves was linear least squares fit to a power law written as follows;

$$\varepsilon = K\, t^{m+1} \qquad \textbf{Eq. 3-1}$$

where ε is the creep strain in %, t is time in hours, K and m+1 are constants. The regression coefficients are given in Table 3-6. Overall, the fits were good based on visual comparisons, R^2 value and standard estimate of error in log creep strain. For the 12CrMoWV bolt, the slope coefficient increased as the temperature increased. For the 1CrMoV nut material, the slope coefficient had a greater dependence on stress than expected but it is consistent with the inherent variability in primary creep behavior.

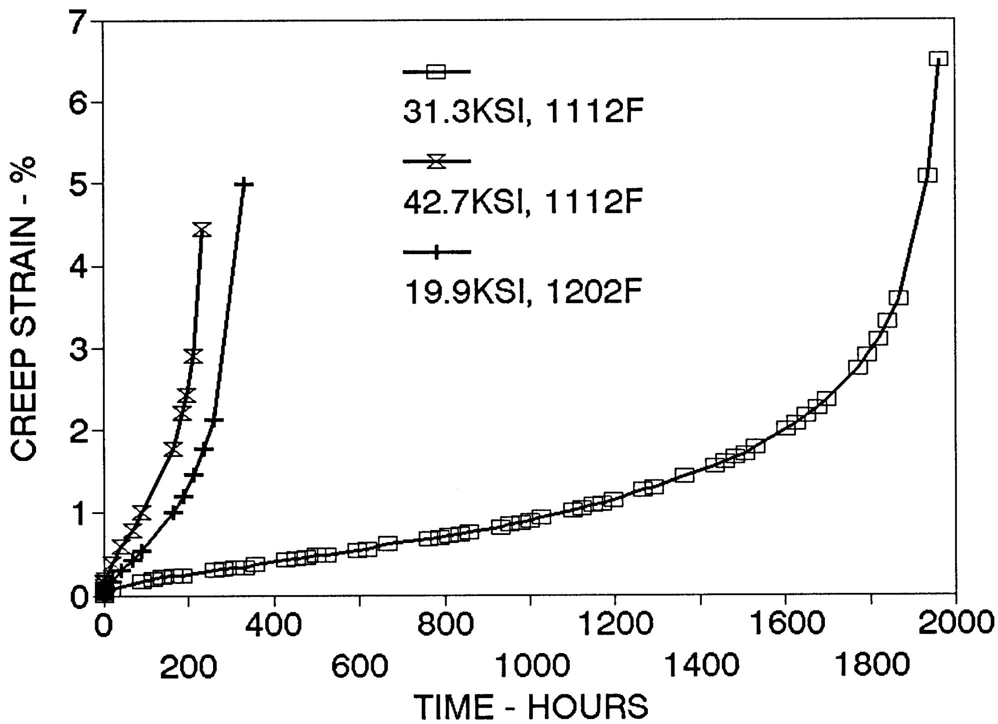

Figure 3-2
Creep strain versus time curves for bolt A, 12Cr-1Mo-1W-1/4V steel, at 1112F and 1202F.

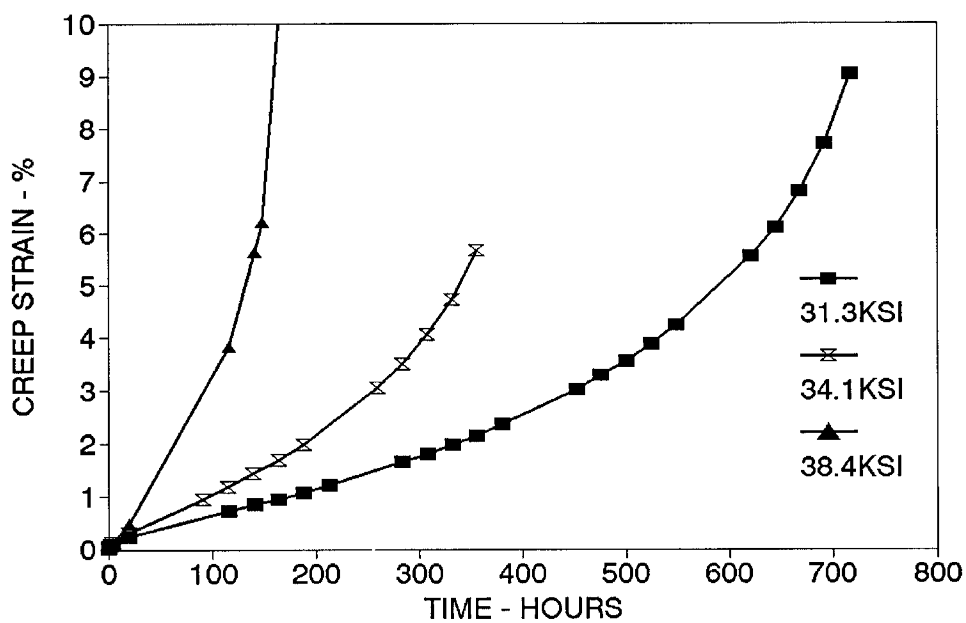

Figure 3-3
Creep strain versus time curves for the service exposed 1Cr-1/2Mo-1/4V nut at 1022F.

Table 3-6
Power Law Creep Constants For Service Exposed Bolt And Nut

Temperature - F	Stress - ksi	m+1	K - %/(Hours)$^{m+1}$
Bolt A (12Cr-1Mo-1W-1/4V)			
1112	42.7	0.438	0.1076
1112	31.3	0.461	0.0216
1202	19.9	0.661	0.0222
Nut B (1Cr-1/2Mo-1/4V)			
1022	38.4	0.614	0.0557
1022	34.1	0.561	0.0560
1022	31.3	0.420	0.0598

The creep strain - time curves were also fit to the two parameter creep model (Othman and Hayhurst, 1990) that was discussed in detail in section four. For these regression analyses, the creep curve is normalized by the rupture time and rupture strain. At low strains, the two parameter model is a power law. Values for the time exponent given in Table 3-5 were used. As discussed in section four, the goodness of the fit depends on the maximum strain used in the regression. Because the life assessment uses a strain limit of 1%, the fits were performed using only the data up to approximately 1%. The observed and calculated creep strain versus time curves are compared in Figure 3-4 for the 12CrMoWV bolt material at 31.3 ksi and in Figure 3-5 for the 1CrMoV nut material at 34.1 ksi.

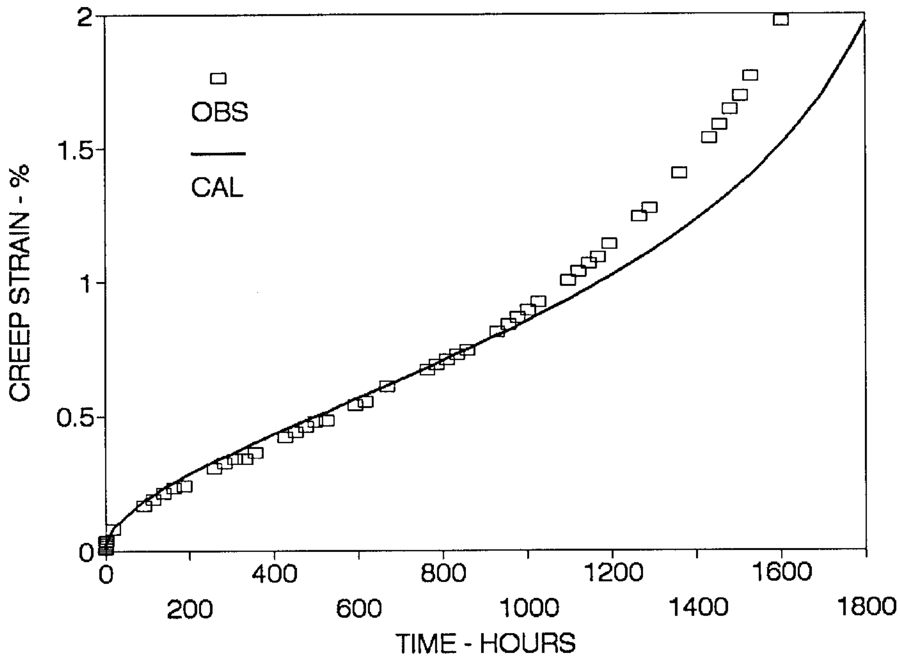

Figure 3-4
Comparison of calculated and observed creep strain versus time curve for bolt A, 12Cr-1Mo-1W-1/4V steel, at 1112F and 31.3 ksi using the two parameter creep model.

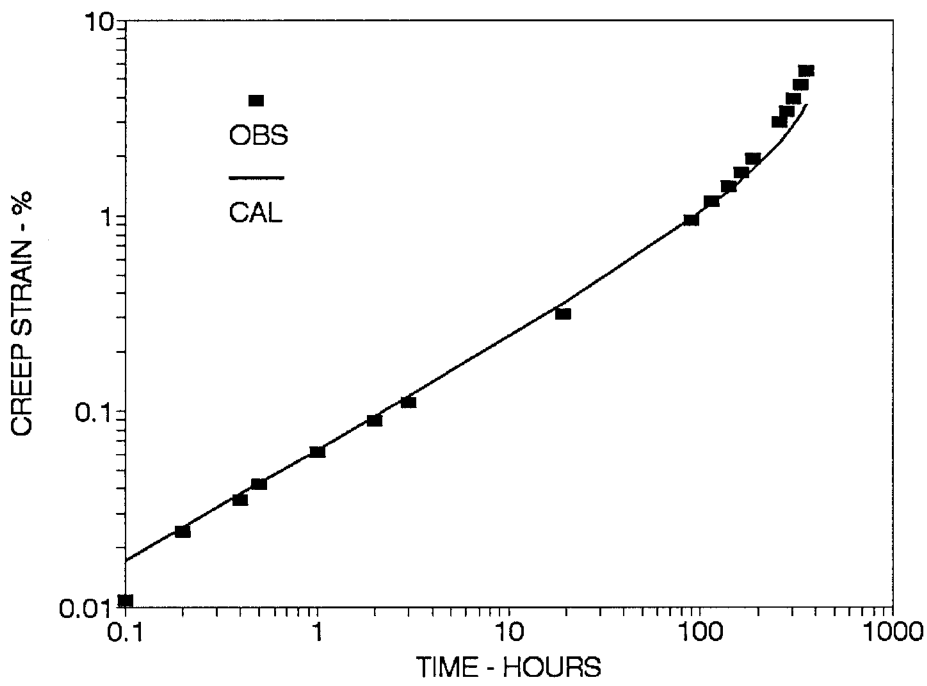

Figure 3-5
Comparison of calculated and observed creep strain versus time curve for nut B, 1Cr-1/2Mo-1/4V steel, at 1022F and 34.1 ksi using the two parameter creep model.

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4
CREEP EQUATIONS

Introduction

Typical applications for creep equations are remaining life assessment, finite element stress analysis and creep crack growth analysis. In the current study, an analytical life prediction method was developed for high temperature turbine and valve studs/bolts. The analytical method uses the creep behavior to calculate the stress relaxation behavior.

Since both stress relaxation and creep involve time-dependent accumulation of inelastic strain, it is reasonable that they can be treated as inverse processes providing the path dependence is properly treated. Indeed, the results of Laflen and Jaske (1979) suggest that the primary creep response can be predicted from stress relaxation data for the case of material response that is approximately path independent.

To predict the stress relaxation behavior using creep data, several flow rules have been used to account for the path dependence. Both time hardening and strain hardening flow rules have been commonly used. The time hardening flow rule states that the current creep rate following a stress change is equal to the creep rate at the new stress and the current time. The strain hardening flow rule states that the current creep rate following a stress change is equal to the creep rate at the new stress and the current creep strain. However, most stress relaxation calculations use a strain hardening flow rule because of simplicity and the better agreement between calculated and observed relaxation behavior than that obtained using a time hardening flow rule. Thus, the ultimate success of the prediction of stress relaxation behavior is strongly related to the creep constitutive equation used in the calculation.

This section discusses the effect of the creep constitutive equation on calculated stress relaxation behavior, the two parameter creep equation and the creep constitutive equations developed for 12Cr-1Mo-1W-1/4V, 1CrMoVTiB and 1Cr-1/2Mo-1/4V bolting materials.

Effect of Creep Equation on Calculated Stress Relaxation Behavior

With regard to the calculation of stress relaxation behavior, realistic creep constitutive equations are required for the following reasons; (1) in order to describe the rapid, initial stress relaxation rates, (2) to capture the long term trends, and (3) to describe the full range of cyclic stress relaxation behaviors encountered in service. As discussed below, a power law primary creep equation or the two parameter creep equation (Othman and Hayhurst, 1990) is capable of describing the rapid, initial stress relaxation rates. Long time creep strain versus time data over a wide range of stresses is required in order to capture trends in stress relaxation at the long times and low stresses. Also, a more complex stress dependence for the creep equation than a simple power law is generally needed because of the range of stresses encountered. The cyclic stress relaxation behaviors typically encountered in laboratory testing and service are the following:

- Strain hardening - increasing relaxed stress with increasing cycles.
- Steady state - constant relaxed stress.
- Strain softening - decreasing relaxed stress with increasing cycles.

The strain softening behavior is usually associated with end of life (Mayer and Konig, 1992) and can only be predicted by creep constitutive equations that describe the tertiary stage of creep such as the two parameter creep model. All of these factors should be considered in the development of the creep constitutive equation.

The relation between the creep equation and the predicted, cyclic-relaxation behavior was shown in a recent study (Ellis, Sielski and Viswanathan, 1998). Results were given for low alloy CrMoV materials using several creep equation and material combinations. The creep constitutive equations evaluated include a power law, the rational polynomial primary plus steady state, power law primary plus steady state (Seeley et al., 1993) and the two parameter creep model (Othman and Hayhurst, 1990). The advantage of the power law is simplicity, but only strain hardening behavior can be predicted. The rational polynomial plus steady state equation can be solved for time explicitly so that a strain hardening flow rule can be used and the model coefficients have simple physical interpretation. The power law primary plus steady state creep equation would require an iteration procedure to solve for time in the application of the strain hardening flow rule. However, neither the rational polynomial plus steady state nor the power law plus steady state creep equation can predict the strain softening relaxation behavior associated with end of life. The two parameter creep equation and its advantages for the prediction of stress relaxation behavior are discussed in more detail below.

Several other studies also show the effect of the creep equation on the predicted stress relaxation behavior. Bolton (1995) concluded that comparisons of the calculation and the uniaxial testing depend on the creep constitutive equation used but, in general, the calculation tends to underestimate the magnitude of the stress relaxation at short test times. Supporting this claim are the results found by Yamashita and Wada (1989) for Type 304 stainless steel. They used the Blackburn type creep equation developed by Aoto et al. (1987). The equation has two exponential terms for primary creep and a single steady state term. In predicting the relaxation behavior, they considered the following sources of scatter in the data:

- The scatter in the initial stress level for the same initial strain.
- The scatter in the material creep response, i. e., heat-to-heat variability in creep strain-time behavior.

The stress relaxation behavior was underpredicted at short times but was approximately equal to the observed at the longest test time of approximately 2000 hours.

In agreement with the results of the current study, a good correspondence between observed and calculated stress relaxation behavior was found for Modified 9Cr-1Mo steel at 1112F (Kitade, et al., 1989). They used a power law primary plus steady state creep equation. These apparently contradictory results can be rationalized by consideration of the significantly higher initial creep rates calculated by a power law compared to exponential formulations.

Two Parameter Creep Model

The two parameter creep model was developed by Othman and Hayhurst (1990) and has been applied to many materials including 1CrMoVTiB bolting steel (Aplin and Brear, 1995). The model was developed using continuum damage mechanics (CDM) approach typical of Kachanov-Rabotnov. Usually, only the secondary and tertiary portions of the creep curve are described. By modification of the basic coupled differential equations for strain rate and damage rate, a representation was obtained that included primary creep. The two-parameter creep model can be written as:

$$\varepsilon/\varepsilon_r = 1-(1-(t/t_r)^{m+1})^{\delta} \qquad \textbf{Eq. 4-1}$$

where ε_r is the rupture strain, t_r is the rupture time, m and δ are material constants. Values for the model constants m and δ can be found by the usual non-linear regression techniques. However, reasonable estimates can be found by linearization of the two parameter equation in the low strain region, i.e., $\varepsilon << \varepsilon_r$, as a simple power law:

$$\varepsilon/\varepsilon_r = \delta\ (t/t_r)^{m+1} \qquad \textbf{Eq. 4-2}$$

For the prediction of stress relaxation of bolts, it has several advantages (Ellis, Tordonato and Visvanathan, 1998; Ellis and Tordonato, 1999). First, it can be solved for time explicitly so that a strain hardening flow rule can be used. For repeated loading or cyclic stress relaxation, strain hardening predictions are in better agreement with measured stress - time curves than predictions using time hardening. Second, it can represent the primary-secondary-tertiary creep behavior of the material. With regard to cyclic bolt stress relaxation prediction, the description of the complete creep curve implies that strain hardening, steady state and strain softening behavior can be predicted using one relation. Lastly, the effects of material strength and material ductility can be independently varied by selecting the desired rupture time correlation to position the material within the scatterband and using the appropriate rupture ductility correlation. In theory, any strength/ductility level could be modeled to match the actual material of interest or perform sensitivity studies.

Type 422 Stainless Steel, 12Cr-1Mo-1W-1/4V

Long time creep, creep-rupture and stress relaxation tests were performed by the National Research Institute for Metals of Japan (NRIM) for 12Cr-1Mo-1W-1/4V, Type 422 stainless steel bolting material (NRIM, 1997). The test temperatures were 932F, 1022F and 1112F. This data base has several advantages:

- Only a single heat of material was used for all testing, thus, eliminating concerns regarding heat-to-heat variability in material properties.
- The creep and rupture data has results of long-term testing and is sufficiently detailed to allow the development of the two parameter creep equation.

Based on analysis results for their creep-strain time curves (NRIM, 1997) and the limited tests for a service exposed bolt (see Section 3), the creep behavior of Type 422 stainless steel can be described using a two-parameter material model. The creep constitutive equation requires values for the material constants m+1 and δ as well as correlations for the rupture time and elongation as a function of stress and temperature. The methods used to analyze the creep curves and determine the material constants were discussed by Ellis and Tordonato (1999) and are summarized below.

Although values for the model constants m+1 and δ could be found by the usual non-linear regression techniques, a two-step, linear regression approach was used. In the first step, the constant m+1 was found using the primary creep data for all tests, even those that did not rupture. In the second step, the constant δ was determined using only those tests that did rupture and the value for m+1 determined in the first step.

Figure 4-1 shows the creep strain versus time data at 1022F for 12Cr-1Mo-1W-1/4V given in the NRIM (1997) data sheets. The values given in the figure are the test stresses in MPa (1 ksi = 6.895MPa). Although reasonable estimates of the constant m+1 can be found using Equation 4.2, the tests below 35.5 ksi did not rupture and, therefore, their strain-time data would not be included in the analysis. In order to exploit the low stress data for the determination of m+1, the following primary creep equation was used;

$$\log \varepsilon = A1 + A2 \log \sigma + A3\, \sigma + (m+1) \log t \qquad \textbf{Eq. 4-3}$$

where ε is the creep strain in %, t is time in hours, σ is stress in ksi, A1, A2, A3 and (m+1) are constants. At each temperature, the data was selected for regression manually using a straight edge and engineering judgement. Because the creep equation is used to calculate the relaxation properties, not all of the higher stress tests were used. The largest stress was nominally the maximum initial stress in the relaxation testing program. At 1022F, there were seventeen primary creep strain-time data points and the highest stress was 42.6 ksi.

Results of the primary creep regression analyses using Equation 4.3 yielded the following values for m+1; 0.34 at 932F, 0.376 at 1022F and 0.43 at 1112F. The nominal standard error of the estimate (SEE) in the coefficient m+1 was ~0.02. The R^2 value for all of these regressions was greater than 0.95 and the nominal SEE in log ε was ~0.08.

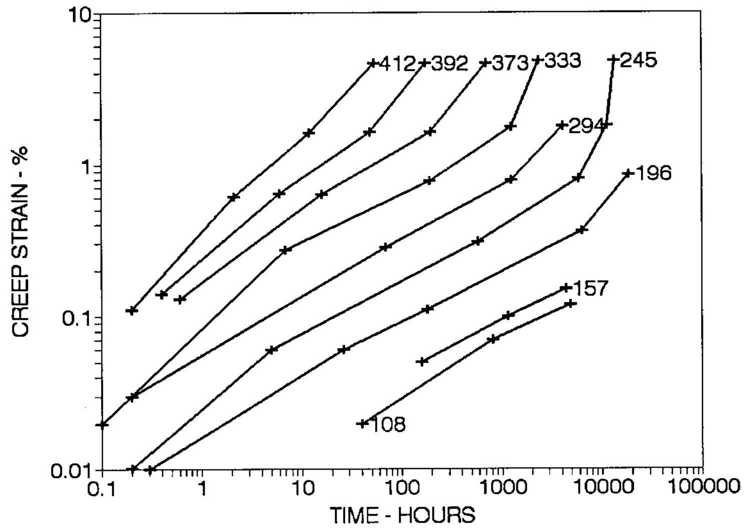

Figure 4-1
Creep strain versus time curves for 12Cr-1Mo-1W-1/4V bolting material at 1022F from the NRIM (1997) data compilation.

Plots of strain fraction, $\varepsilon/\varepsilon_r$, versus life fraction, t/t_r, were made at the three test temperatures of 932F, 1022F and 1112F. Figure 4-2 shows strain fraction - life fraction plot at 1022F. For each isotherm, the creep strain-time data at the various stresses form a single curve as required by the two parameter creep equation. The solid line in Figure 4-2 was drawn using the two parameter creep equation and the appropriate temperature dependent material constants of m+1 and δ.

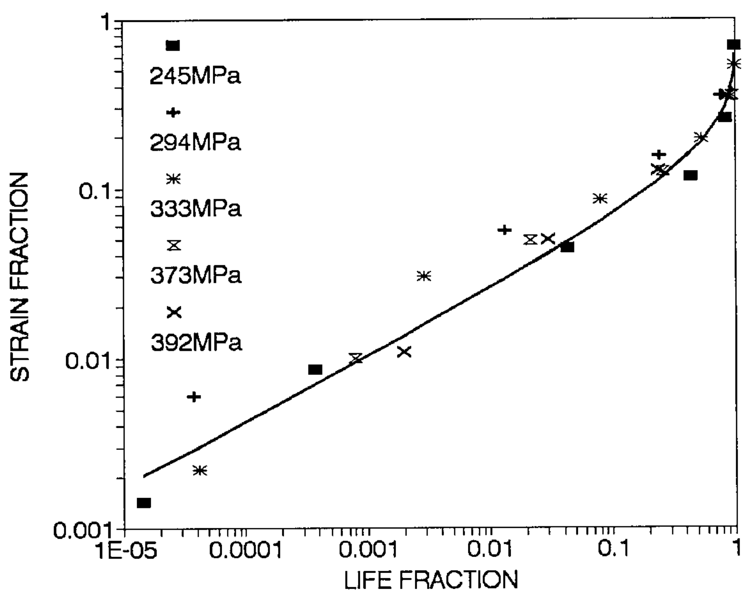

Figure 4-2
Comparison of observed and calculated strain fraction versus life fraction for 12Cr-1Mo-1W-1/4V bolting material using two parameter creep equation at 1022F.

In order to determine the value for δ at each temperature, linear least squares regression was performed on a linearized form of the two parameter creep equation written as:

$$\log (1-\varepsilon/\varepsilon_r) = \delta \log (1-(t/t_r)^{m+1}) \qquad \textbf{Eq. 4-4}$$

where ε is the creep strain in %, t is time in hours, ε_r is the rupture strain in %, t_r is the rupture time in hours, m and δ are material constants.

The value determined for δ depended on strain range used in the analysis. For the case of all data, 5% total strain maximum, the fit was very poor in the primary region but was good in the tertiary region. Limiting the maximum total strain to 1% resulted in good prediction of primary-steady state creep but an underprediction in the tertiary creep regime. Based on the intended use of the two parameter creep equation for bolting stress relaxation prediction, the value of δ for the 1% total strain range was selected. The material constants for the two parameter creep equation are summarized in Table 4-1.

Creep strain versus time curves were calculated using the following:

- The two parameter creep equation.
- The material constants given in Table 4-1.
- The measured rupture time and rupture elongation.

The calculated creep curves were compared to the observed at the three test temperatures. Overall, the correlations appear to be very good. Figure 4-3 shows the comparison of the observed and calculated creep strain - time at 932F on a log - log plot that emphasizes the power law nature of the two parameter creep equation at low strains. Figure 4-4 shows the comparison of the observed and calculated creep strain - time at 1022F on the more familiar linear plot that illustrates the comparison better in the steady state and tertiary creep regimes at higher strains.

Table 4-1
Material Constants For Two Parameter Creep Equation Of 12Cr-1Mo-1W-1/4V

Temperature - F	M+1	δ
932	0.340	0.1050
1022	0.376	0.1339
1112	0.430	0.0917

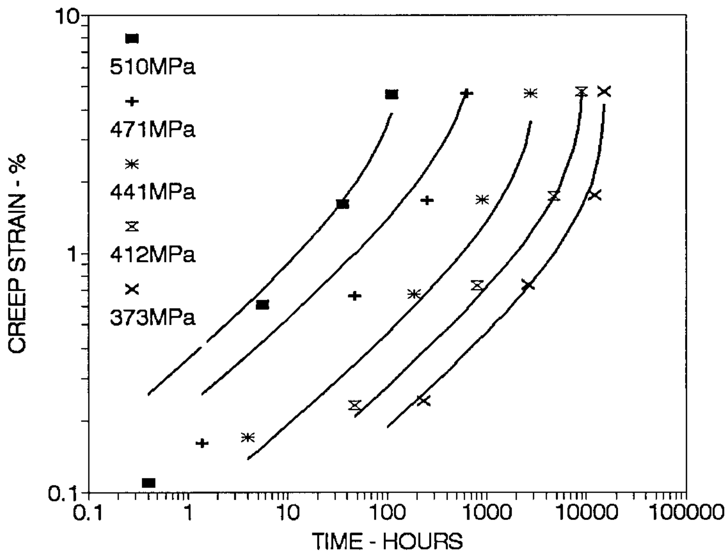

Figure 4-3
Comparison of observed and calculated creep strain versus time curves for 12Cr-1Mo-1W-1/4V bolting material on a log-log plot at 932F.

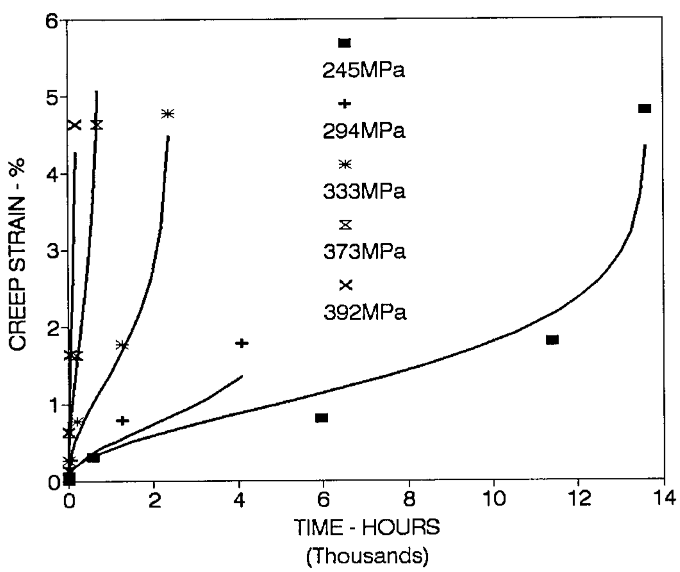

Figure 4-4
Comparison of observed and calculated creep strain versus time curves for 12Cr-1Mo-1W-1/4V bolting material on a linear-linear plot at 1022F.

To complete the creep constitutive equation, correlations for the rupture time and rupture elongation as a function of stress and temperature are required. For the same heat of 12Cr-1Mo-1W-1/4V bolting material used in the material constants development, NRIM (1997) has reported the results of creep-rupture and stress relaxation tests. The creep-rupture tests were conducted at 932F, 1022F and 1112F. Comparisons of the NRIM and the ASTM rupture properties data base for Type 422 stainless steel (Smith, 1980) were performed and, as expected, the rupture properties were similar.

Rupture Time

The rupture properties for the 12Cr-1Mo-1W-1/4V bolting material (NRIM, 1997) is shown in Figure 4-5 at 932F. The rupture properties for 12CrMoWV bar material (NRIM, 1979a) is shown for comparison in the figure and illustrate the expected variability in material properties. The bar data is useful because the ultimate objective of the project was to develop statistically based creep equations. For the 932F data, the rupture time correlation developed by NRIM (1997) is shown by a dashed line designated M-H for the Manson-Haferd time-temperature parameter. Because the correlation greatly overpredicts the rupture properties in the region of interest, a time shift correction factor was used to force the Manson-Haferd results through the lowest stress-longest rupture time. The magnitude of the correction factor was 0.245 in log time. The solid line in Figure 4-5 is the shifted M-H prediction of rupture time. In essence, the shift results in a predicted rupture time for the bolting material that is near the maximum for the bar material in the extrapolated region. Heat-centered regression analyses are based on the assumption that the position of a heat of material within the scatterband is independent of stress and temperature, i.e., a strong heat is always a strong heat.

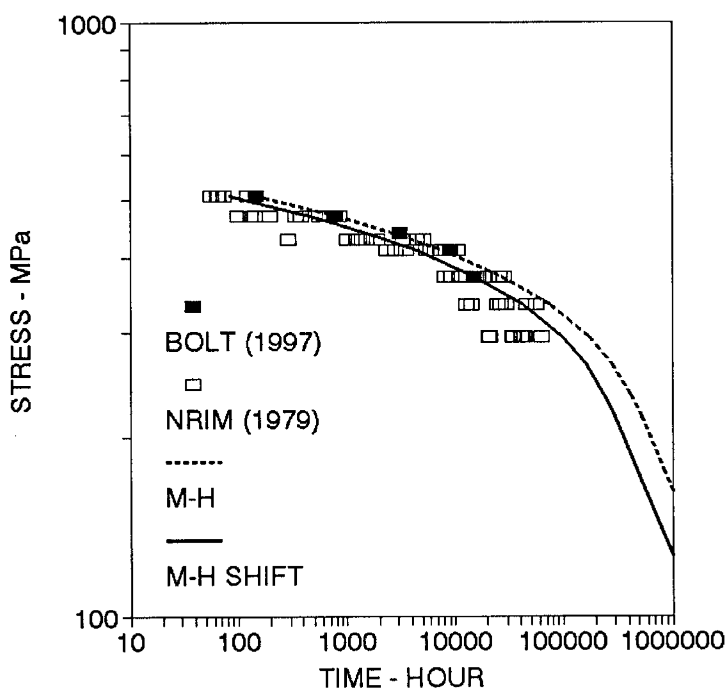

Figure 4-5
Comparison of observed and calculated rupture properties for 12Cr-1Mo-1W-1/4V bolting material at 932F.

Because the Manson-Haferd master curve is double valued for stresses in the range of interest at 1022F and 1112F, a Dorn parameter analysis of the rupture time data was performed for stresses of 42.6 ksi and below. To model the curvature of the log time - log stress curve, the master curve had a log stress plus stress term. The isothermal rupture time equations were:

$$\log t_r = C_T - 1.454 \log \sigma - 0.00663\, \sigma \qquad \textbf{Eq. 4-5}$$

where t_r is the rupture time in hour, σ is the stress in ksi and C_T is the temperature dependent intercept constant equal to 8.0287 at 1022F and 6.435 at 1112F. The R^2 value for this regression was 0.996 and the SEE in log time was 0.067.

Rupture Ductility

The rupture ductility versus the rupture time data is shown in Figure 4-6 on a log-log plot at 1022F for 12Cr-1Mo-1W-1/4V bolting material (NRIM, 1997) and bar material (NRIM, 1979a). The rupture elongation for the bolting material is near the bottom of the scatterband for multiple heat data base of the bar material. For both materials, it is evident that there is a reasonable power law correlation between rupture elongation and rupture time but the slope depends on temperature. Similar trends in elongation with rupture time have been displayed by the multiple heats of 2-1/4Cr-1Mo (Ellis et al., 1992) and CrMoV rotor steel (Ellis, Tordonato and Viswanathan, 1998).

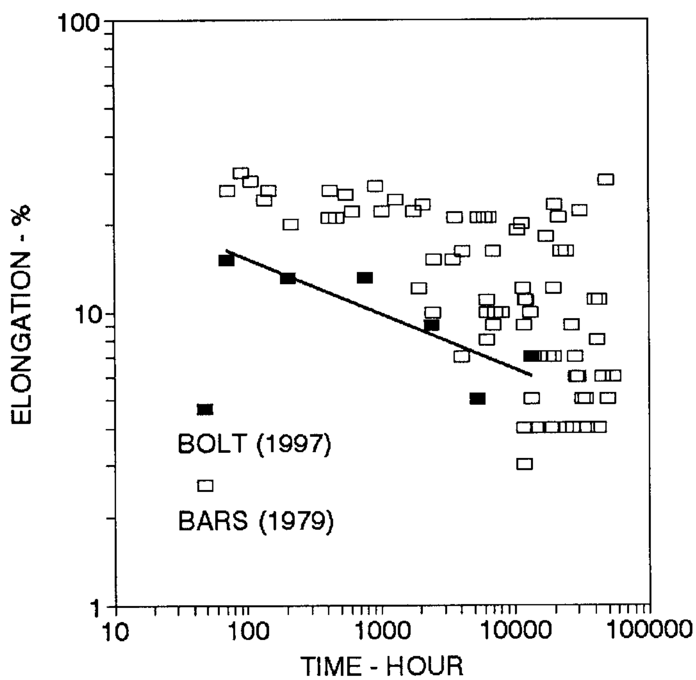

Figure 4-6
Comparison of observed and calculated rupture elongation as a function of rupture time for 12Cr-1Mo-1W-1/4V bolting material at 1022F.

In order to account for the temperature dependence, isothermal regression analyses were performed. The rupture ductility relations are written as:

$$\varepsilon_r = 41.75\ (t_r)^{-0.15}$$ **Eq. 4-6**

at 932F

$$\varepsilon_r = 35.5\ (t_r)^{-0.186}$$ **Eq. 4-7**

at 1022F and

$$\varepsilon_r = 18.0\ (t_r)^{-0.083}$$ **Eq. 4-8**

at 1112F, where ε_r is the rupture strain in % and t_r is the rupture time in hour. Results of the analyses had R^2 values of 0.755 at 932F, 0.761 at 1022F and 0.686 at 1112F. The values of the standard error of the estimate (SEE) in log were 0.081 at 932F, 0.101 at 1022F and 0.052 at 1112F. The calculated rupture elongation versus rupture time values are compared to the observed in Figure 4-6 for the Type 422 stainless steel bolting material.

Durehete 1055, 1CrMoVTiB

This section discusses the bolt model material, Durehete 1055 or 1CrMoVTiB, used in the validation study and the development of the two parameter creep constitutive equation including the creep and creep-rupture properties for 1CrMoVTiB.

Material

The development of low alloy ferritic steels for high temperature bolting applications was discussed by Everson, Orr, and Dulieu (1988) and Orr et al. (1995). Because of the application, these steels must have a combination of good creep strength and high ductility to avoid notch brittleness. Durehete 1055 was initially developed in the 1960s and is a modification Durehete 1050 that was brittle in creep. Additions of titanium and boron resulted in increased creep ductility. Although, the basic composition of 0.2%C, 1%Cr, 1%Mo and 0.7%V for Durehete 1055 has remained unchanged, the alloy has undergone significant changes through the years in both composition ranges and final heat treatment. The optimum fine grained bainitic microstructure for Durehete 1055 is obtained by heat treatment consisting of a pre-hardening treatment at 1220-1292F, austenitising at 1778-1814F, oil quenching and tempering at 1256-1328F. Using this heat treatment, typical room temperature tensile strengths range from 120 to 140 ksi and the minimum 10^4 hour rupture strength at 1022F is 40.6 ksi.

Significant improvements in creep ductility have been shown by decreasing the residual element content (Oakes et al., 1988). The empirically based compositional factor used to summarize the effect of residual tramp elements for Durehete 1055, is the R factor, defined

$$R = P + 2.43\ As + 3.57\ Sn$$ **Eq. 4-9**

This calculation uses element content in weight percent and 0.13 Cu is sometimes included in the formula for R. Current specifications limit the phosphorus, tin and arsenic levels to 0.02% and the copper and nickel levels to 0.2%. Using these values in the formula above, the maximum residual element factor for a steel within specification would be calculated as 0.14. Although the improvement in ductility is related to purity, the mechanism is not fully understood.

Two Parameter Creep Equation

The creep behavior of 1CrMoVTiB (Durehete 1055) bolting material was studied by Aplin and Brear (1995). They showed that the two parameter creep equation provided a good description of the strain-time behavior. The creep data was obtained in a large collaborative test program and was for multiple heats of steel. The stresses range from 16.8 to 76 ksi and the temperatures range from 887F to 1112F. Based on their analysis of the strain-time properties, the optimum value of m+1 for the multiple heat data base was 0.784. This value for m+1 is larger than the 1/3 to 1/2 value more typically found for the primary creep time exponent in many steels. The value for δ was not reported by Aplin and Brear (1995) but was calculated as 1/8.2 = 0.122 based on a plot of strain fraction versus life fraction and the known value for m+1.

Holdsworth and Strang (1995) have reported the results of relaxation tests on full size bolt models for 1CrMoVTiB (Durehete 1055) at 1022F. Two materials were tested in their study. At a rupture time of 20,000 hours, the commercial purity steel had a rupture ductility of 7% while the high purity steel had a rupture ductility of 14%. Because the two parameter model incorporates both rupture strength (through the stress dependence of rupture time) and rupture ductility, it is possible to study the effect of each variable independently on the predicted stress relaxation behavior. Comparisons of the creep strain versus time curves were made for these two materials using the two parameter model with the constants given above. As expected for the case of equal rupture times, the higher ductility steel (high purity) was predicted to accumulate higher creep strains than the lower ductility, commercial purity steel.

To complete the model, correlations for the rupture time and rupture elongation as a function of stress at 1022F are required. Graphical representation of the rupture properties for Durehete 1055 has been given in several sources (Everson, Orr, and Dulieu, 1988; Aplin and Brear, 1995; Orr et al., 1995). Values for rupture time were scaled from the plots in Orr et al. (1995) and were used to construct the 1022F isotherm. Their data included heats with a range of R values typical of commercial practice. Rupture equations were developed and were reported elsewhere (Ellis, Tordonato, and Viswanathan, 1998).

The rupture ductility data for the 1Cr-1Mo-0.25V rotor forging material (NRIM, 1979b) was used to develop the needed creep ductility relation. A log-log plot of rupture ductility versus rupture time was made. Visually, there was a reasonable correlation between rupture elongation and rupture time. Both heat-centered and lot analyses were performed and similar results were found. For the lot analysis, values were a slope of -0.144, R^2 of 0.599 and standard error of the estimate (SEE) of 0.095. For the heat-centered analysis, values were a slope of -0.152, R^2 of 0.749 and SEE of 0.07. Because similar trends in elongation with rupture time are shown by the typical heats of Durehete 1055 steel, it was assumed that their rupture ductility trend can be described using the basic relationship as that for the CrMoV rotor forging material. The rupture ductility versus rupture time relations for the commercial purity and high purity Durehete 1055 steel used the slope from the lot analysis and values for rupture elongation at 20,000 hours cited earlier. They are written as:

$$\varepsilon_r = 29.04\ (t_r)^{-0.144} \qquad \textbf{Eq. 4-10}$$

for the commercial purity steel and

$$\varepsilon_r = 58.08\ (t_r)^{-0.144} \qquad \textbf{Eq. 4-11}$$

for the high purity steel, where ε_r is the rupture strain in % and t_r is the rupture time in hour.

Grade 4BD, 1Cr-1/2Mo-1/4V

Long time creep, creep-rupture and stress relaxation tests were performed by the National Research Institute for Metals of Japan (NRIM) for Grade 4BD or 1Cr-1/2Mo-1/4V bolting material (NRIM, 1997). The test temperatures were 842F, 932F and 1022F. Based on these results and limited tests for a service exposed nut (see Section 3), the primary creep behavior can be described using a power law and the complete creep curve can be described using the two-parameter material model. Because of the funding limitation, only preliminary creep stain data analysis was completed. Therefore, no final creep constitutive equation was developed. Comparisons of the observed and predicted relaxation behavior were made and the results were encouraging. Because at least one more iteration was required before good agreement would have been obtained, no interim results are presented.

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5 LABORATORY VALIDATION

Introduction

Testing methods used to determine the stress relaxation strength of bolted joints include the following (Konig and Mayer, 1995):

- The isothermal stress relaxation test.
- Stress relaxation tests with bolt/nut combination.
- Anisothermal stress relaxation test.
- Bolted joint model test.

The isothermal stress relaxation test has been used primarily for material property characterization studies. Bolt model tests have been performed for many years and are considered by some as a cost-effective means to size bolted joints (Mayer and Keienburg, 1980; Mayer and Konig, 1992; Konig and Mayer, 1995). There are two major differences between the uniaxial stress relaxation test and the bolt model test. First, the initial pre-load in the model tests can be as much as 20% less due to settling effects on the screw threads and repeated tightenings show significantly smaller settling effects. Second, the relaxed stress at long time for the bolt model test are slightly higher than those found in uniaxial tests because of the elastic follow-up of the flange.

Regarding the application of the results towards a procedure validation, it is important to consider the physical factors that affect strain accumulation and bolt life. These factors have been enumerated by Bolton (1995) as follows;

- Material.
- Temperature and temperature gradient.
- Applied strain.
- Bolt form.
- Number of re-tightenings.

In addition to these factors, the frequency of retightening or cycle time is also known to strongly influence creep damage accumulation and bolt life. The uniaxial stress relaxation test results discussed below for 12CrMoWV (NRIM, 1997) are of value in determining the effect of temperature and applied strain level. The bolt model test results for CrMoVTiB (Holdsworth and

Strang, 1995) are of value in determining the effect of material, applied strain and number of tightenings.

The objective of the current study was to develop an analytical life prediction method for high temperature turbine and valve studs/bolts. The analytical prediction method uses the creep behavior to predict the isothermal stress relaxation properties. In order to validate the approach, the calculated results given by this approach were compared to the results of uniaxial stress relaxation testing (Ellis and Tordonato, 1999) and bolt model testing (Ellis, Tordonato and Viswanathan, 1998). This section discusses the calculation procedure, uniaxial test and bolt model validation results.

Calculation Procedure

The ultimate objective is to perform life assessment for operating bolts. The typical bolt geometry has a uniform section, a bore hole in the center and threaded ends. For service applications, the remote loads are determined by the bolt tightening procedure and were treated as an initial stress elastically calculated from the specified nominal shank strain. The relaxation of the stress in the uniform section as a function of time was calculated using the creep strain-time properties and a strain-hardening associated flow rule. This analysis procedure predicts the stress-time and accumulated creep/inelastic strain-time curves for the uniform section portion of the bolt.

The calculation is based on the total strain remaining constant and the conversion of elastic strain by stress reduction into creep strain as follows;

$$-(1/E)\,d\sigma/dt = d\varepsilon_c/dt \qquad \textbf{Eq. 5-1}$$

where E is Young's modulus, σ is the stress, t is time and ε_c is the creep strain. For the case of an infinitely stiff relaxation testing machine, Equation 5.1 is generally regarded as accurate. With regard to service and bolt model predictions, this equation assumes that the flange and nut do not creep appreciably and the elastic follow-up associated with unloading of the flange is negligible.

A standard incremental calculation procedure was used. For the bolt model tests, the starting stress was calculated elastically as the product of Young's modulus and the applied initial strain. For the uniaxial tests, Young's modulus for 12CrMoWV bolt steel used in this study were 23,900 ksi at 932F, 21,480 ksi at 1022F and 18,200 ksi at 1112F (Tanaka and Ohba, 1984). The starting stress was equal to that given in the NRIM (1997) compilation for the tests of interest at each of the three test temperatures. The corresponding initial strain was also given but can be calculated elastically as the initial stress divided by the appropriate Young's modulus (valid for initial stresses below proportional limit, typical of service loading).

In the incremental procedure, the stress is assumed constant for a time increment and the increment of creep strain accumulated is calculated. The creep strain is calculated using the two parameter creep equation and a strain hardening flow rule. The rupture time and rupture strain used in the creep equation are calculated using the current value of stress. The magnitude of the stress reduction is the product of Young's modulus and the creep strain increment. The time steps were chosen to limit the maximum stress changes to ~0.5%.

Uniaxial Test Comparisons

The simplest comparison is expected to be for the case of the uniaxial, isothermal stress relaxation test. The procedure for this test involves heating the specimen to the desired temperature prior to the application of the total strain. Both the temperature and strain are held constant during the test. The residual or relaxed stress is continuously measured as a function of time. One advantage of the calculation procedure is the complete stress-time and strain-time history compared to the single point values at the end of the cycle measured by model tests.

Comparison of the calculated (Ellis and Tordonato, 1999) and observed relaxation behavior for 12Cr-1Mo-1W-1/4V (NRIM, 1997) is given in Figure 5-1 at 932F, in Figure 5-2 at 1022F and in Figure 5-3 at 1112F. At 932F and 1022F, there is good agreement in the relaxed stress values. In addition, the predicted curve shape is also similar to that of the measured. At 1112F, the quality of the agreement is a function of time. The agreement is good at short times and at the end of the tests where almost complete relaxation of the stress was observed and predicted for all three initial stress levels. The shape of the predicted relaxation curve was similar that for the observed, i.e., sigmoidal on a semi-log plot. However, the prediction at intermediate times was poor. Although improvement in the comparison at 1112F could be obtained by using the creep properties typical of a stronger and less ductile heat of steel than that characterized in the creep and creep-rupture testing program - i.e., within heat variation - the magnitude of the shift required to produce agreement was considered beyond that expected.

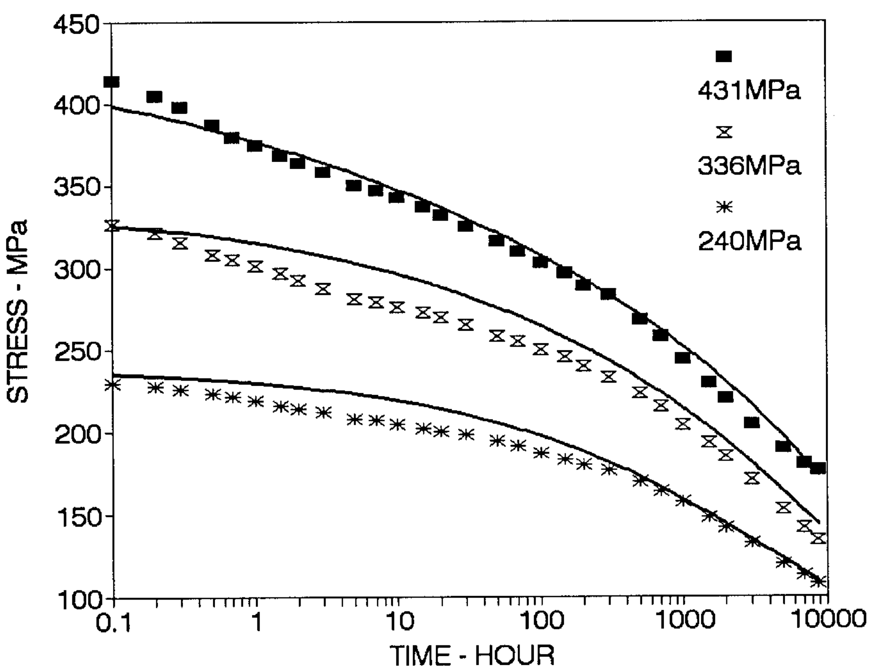

Figure 5-1
Comparison of the calculated and observed stress versus time curves for 12Cr-1Mo-1W-1/4V at 932F (1 ksi = 6.895 MPa).

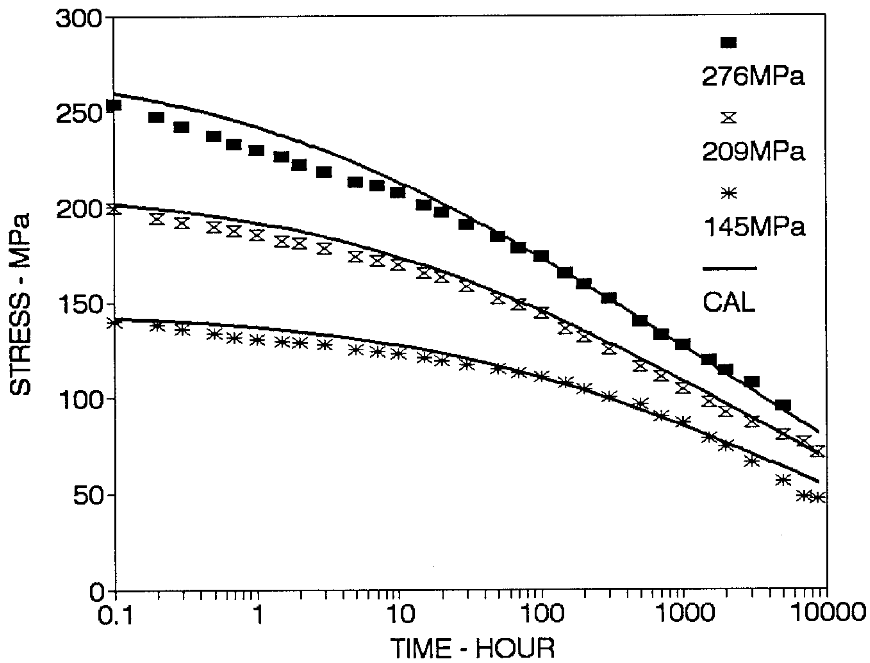

Figure 5-2
Comparison of the calculated and observed stress versus time curves for 12Cr-1Mo-1W-1/4V at 1022F (1ksi = 6.895 MPa).

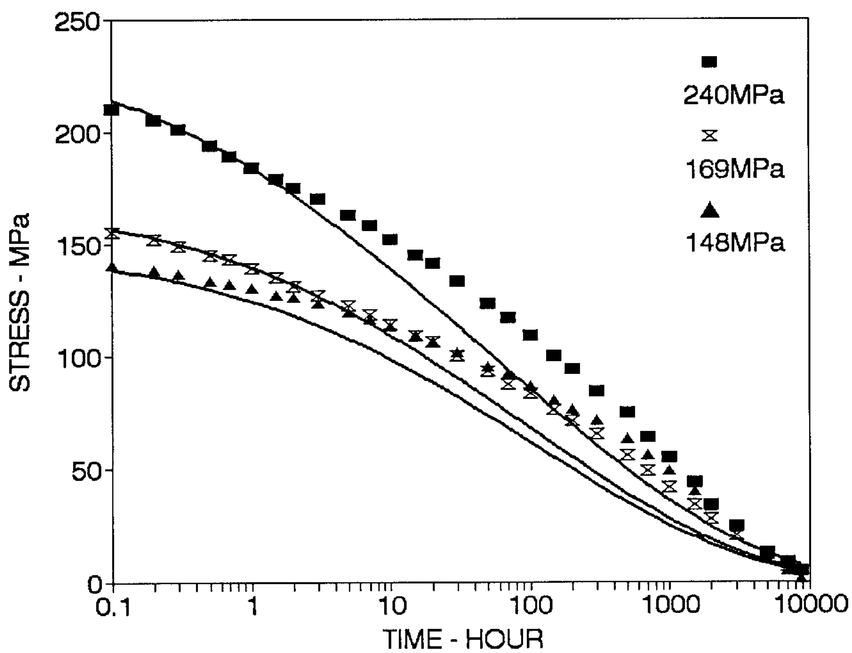

Figure 5-3
Comparison of the calculated and observed stress versus time curves for 12Cr-1Mo-1W-1/4V at 1112F (1ksi = 6.895 MPa).

Of particular importance to a strain based remaining life assessment procedure is the good agreement at all temperatures at the longest test time. The magnitude of the accumulated inelastic strain is directly related to the stress reduction. Thus, a good prediction of long time relaxed stress implies accurate strain estimates.

In addition to the calculation of the stress-time curve, the analysis procedure predicts the accumulated creep or inelastic strain-time curves as shown in Figure 5-4 at 1022F. For bolts in service, the initial stress level is determined by the tightening procedure. As seen in the figure, the accumulated inelastic strain is a strong function of the initial stress/strain level. Uncontrolled tightening procedures can result in applied loads that are either higher or lower than design. Both levels of tightening have serious consequences; possible leaks for loads significantly lower than design and premature failure for loads consistently higher than design loads. The calculation of the strain-time curve is important because one failure criterion for bolts in service is an accumulated strain limit, i.e., the time to 1% creep strain (de Witte and Stubbe, 1985; de Witte, 1989; Ellis, Sielski and Viswanathan, 1998). Strain based life assessment has also been used for elevated temperature steamlines with a creep strain limit of 2% recommended as a failure criterion (Ellis et al., 1992).

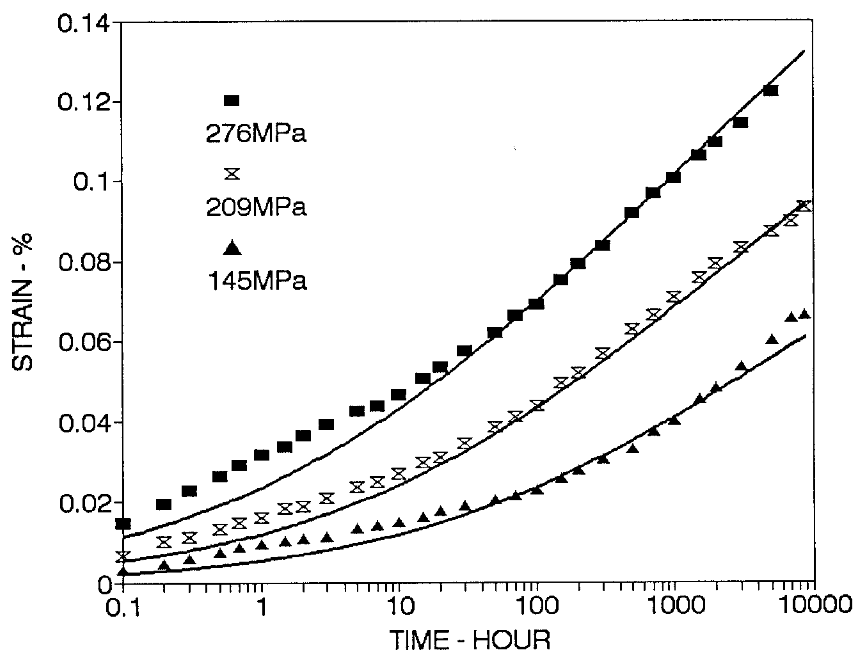

Figure 5-4
Comparison of calculated and observed accumulated inelastic strain versus time curves at 1022F as a function of initial stress (1ksi = 6.895 MPa).

Bolt Model Comparisons

Bolt model testing simulates service by tightening a model bolt assembly consisting of a bolt, two nuts and a sleeve to an initial strain/stress level at room temperature, heating the bolt model to a high temperature and holding for the desired test time (Konig and Mayer, 1995). To estimate the magnitude of the stress relaxation as a function of operating time, analytical calculation techniques, uniaxial stress relaxation testing and experimental bolt model testing have been used. Comparisons of these three approaches have been performed. A detailed discussion of the comparison for uniaxial testing and analytical calculation method was given above. It is important that for the case of similar materials for the bolt, flange and nut, the results of uniaxial relaxation testing and bolt model testing are in good agreement for 1CrMoVTiB (Holdsworth and Strang, 1995), 12CrMoV (Mayer and Konig, 1995) and fair agreement for 1CrMoV (Mayer and Keienburg, 1980; Konig and Mayer, 1995). For 1CrMoV, the uniaxial relaxation specimens tend to have slightly higher residual stresses than the bolt models on average but the scatterbands for the two test methods overlap.

Twelve full scale bolt model tests were performed at 1022F for 1CrMoVTiB steel (Holdsworth and Strang, 1995). The bolt materials were a commercial purity steel with a R of 0.137 and a high purity VIM/VAR steel with a R of 0.027. The shank strains and initial stresses were 0.15% and 34 ksi, 0.2% and 45 ksi. The cycle durations were 144, 1000 and 3000 hours. The tests were continued until failure or significant inelastic strains were accumulated in the bolt shank. Results were plotted for the relaxed stress and accumulated plastic strain as a function of cycles completed. In addition, values were tabulated for the plastic strain at the end of the Nth cycle and the relaxed stress at the end of the first, second, fifth and Nth cycle. These tabulated values were used for comparison purposes with the results of the two parameter creep calculation.

Based on an evaluation of scoping analyses, the material strengths used for comparisons with the bolt model tests were minimum strength for the commercial purity material and maximum strength for the high purity material. Comparisons of the calculated and observed relaxation behavior for CrMoVTiB at 1022F were made (Ellis, Tordonato and Viswanathan, 1998). The results for accumulated inelastic strain are shown in Figure 5-5 for the 144 hour cycle, Figure 5-6 for the 1000 hour cycle and Figure 5-7 for the 3000 hour cycle. Several features are evident.

With regard to the accumulated creep strains versus cycles for both materials, there is a good agreement in values and the predicted curve shape is also similar to the measured. The quality of the strain agreement is a function of cycle time with excellent agreement for the 1000 hour cycle and usually, a slight, ~10 to 20% overprediction for the 144 hour and 3000 hour cycle times. The predicted strain versus cycles curves, when fully developed, have the primary, secondary, tertiary regimes expected from the strain versus time shape of the two parameter creep curve. Strain prediction is important aspect of life assessment for threaded fasteners, especially, if the failure can be related to ductility exhaustion of the material in the first engaged thread of the bolt. Based on an estimate of the multiaxial stresses in the thread root, the strain at failure in thread root is approximately 0.09 times the uniaxial failure strain. If the 20,000 hours rupture ductilities are used, the failure strains are calculated as 0.09 * 7% = 0.63% for the commercial purity steel and 0.09 * 14% = 1.26% for the high purity bolt material. Three of the commercial purity bolts failed at shank strains of 0.52% to 0.56%. The shank strains for the high purity bolt model tests were all less than ~1% except for one with a strain of 1.46% and no failures were reported.

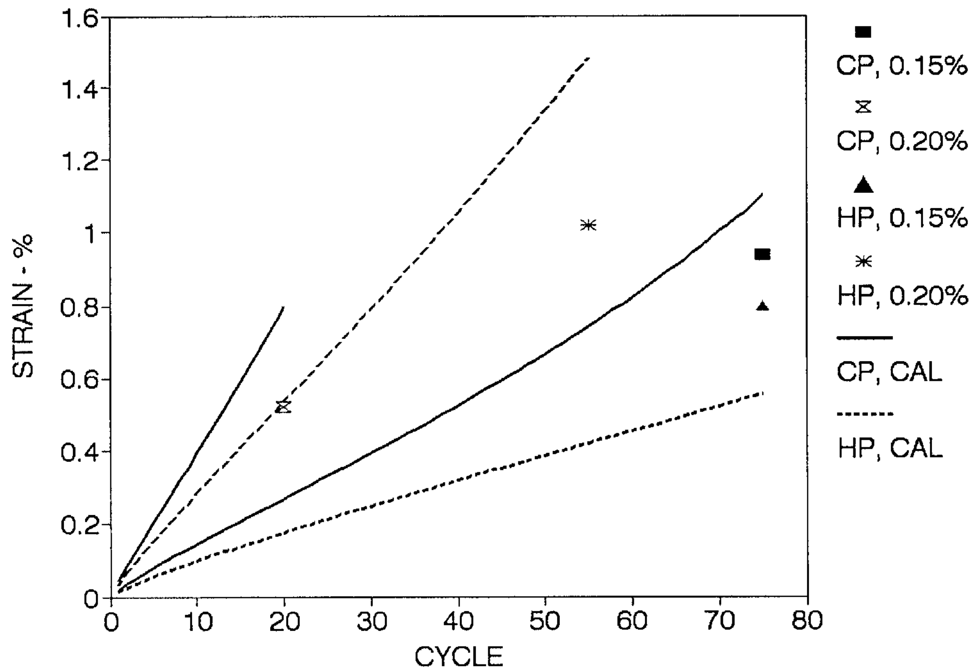

Figure 5-5
Comparison of observed and predicted strain versus cycle data for cycle time of 144 hours at 1022F.

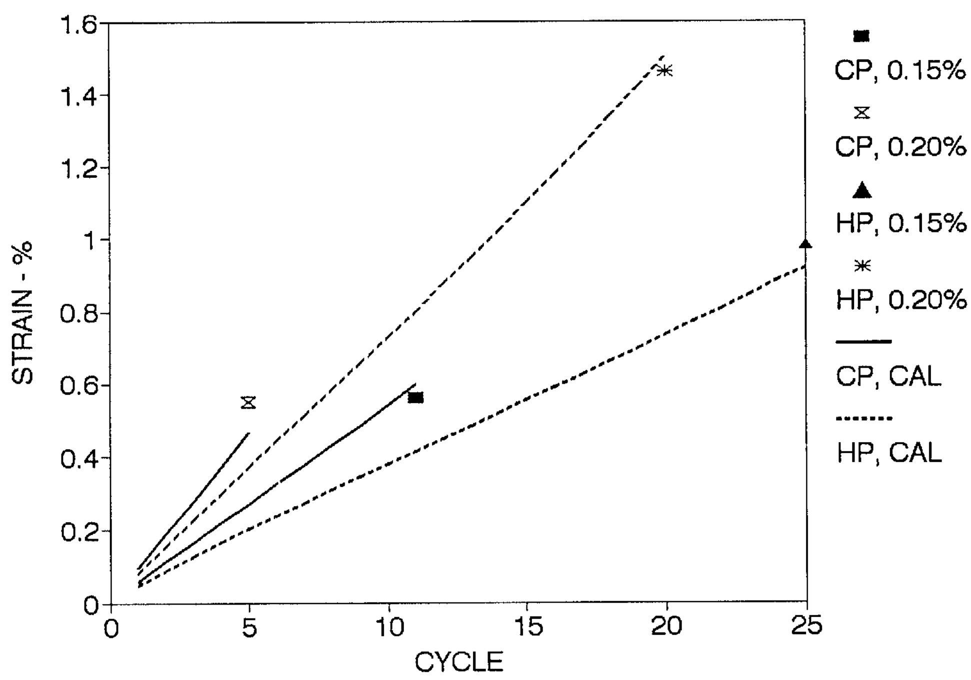

Figure 5-6
Comparison of observed and predicted strain versus cycle data for cycle time of 1,000 hours at 1022F.

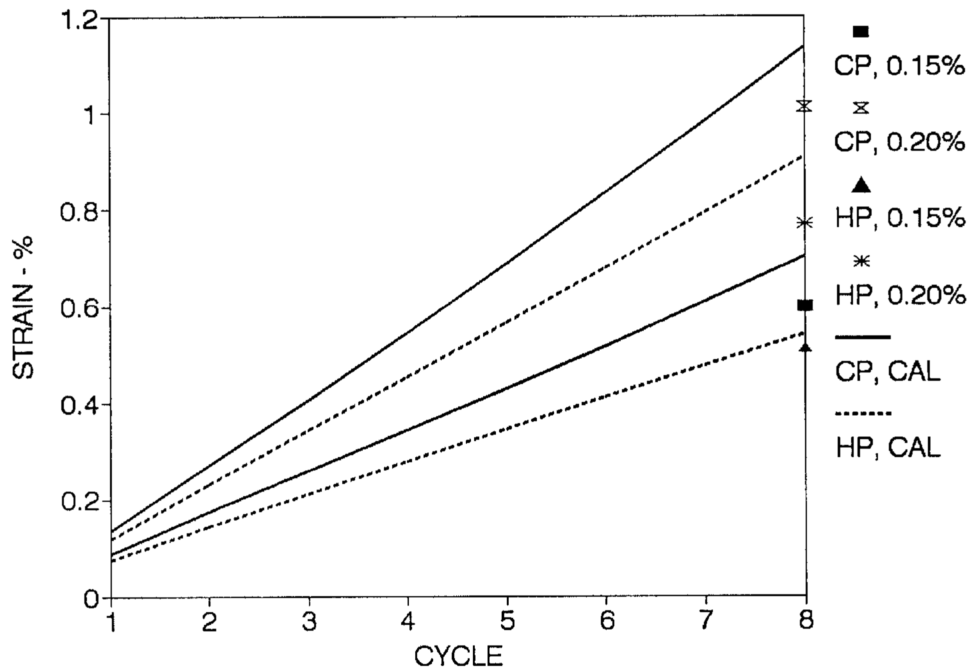

Figure 5-7
Comparison of observed and predicted strain versus cycle data for cycle time of 3,000 hours at 1022F.

With regard to the relaxed stress versus cycle values, the agreement depends on number of cycles (Ellis, Tordonato and Viswanathan, 1998). For the initial cycle, the values are in poor agreement with the predicted significantly higher than the observed. This divergence is believed to be related to the magnitude of the time exponent of 0.78 in the two parameter creep equation being larger than the 1/2 to 1/3 value expected for power law primary creep. In the steady state and strain softened regime, the overall curve shape is in reasonable conformity but the predicted values are only in fair agreement, generally within ~ 4.3 ksi for the 144 hour and 1000 hour cycles. For the 3000 hour cycle, the calculated relaxed stresses are significantly below the observed, but the strain softening nature of the relaxed stress is correctly predicted.

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6 FIELD VALIDATION AND REMAINING LIFE PREDICTION PROCEDURE

Introduction

To validate the remaining life prediction approach for service applications, the following validation procedure was envisioned:

- Measure the current bolt length for selected high temperature bolts after service. Calculate the accumulated inelastic strain using the current and initial bolt lengths.
- Predict accumulated inelastic strain as a function of temperature using operating history values for stretch and cycle times.
- Compare observed and predicted strain values.
- Apply corrections, if necessary; i.e., adjust prediction and measurement procedure (within limits) in order to improve correlation.

Because of funding limitations, no predictions of accumulated inelastic strain and remaining creep life were made. This section presents the results of field measurement for stud/bolt length at one plant and valve stud temperature and temperature gradient measurements at a second plant. In addition, an outline is given for the recommended analytical calculation method used to predict the remaining creep life of operating bolts.

Bolt/Stud Length Measurements

Length measurements were performed at a fossil fuel power plant (Plant A) with a nominal operating steam temperature of 1005F (Sielski and Steakley, 1998). The measurements were made for the HP turbine inner cylinder and stop valve studs. Figure 6-1 shows a photograph of the HP inner cylinder studs. The studs of interest were numbered 47 through 78. There were two stop valves - valve A and valve B - each with 14 studs.

The stud lengths were measured using ultrasonic testing (UT). The UT measurement was calibrated for each of the two bolt materials of 1Cr-1/2Mo-1/4V and 12Cr-1Mo-1W-1/4V bolt material. The stud lengths were measured with a micrometer and these values were used in the calibration of the UT instrument.

Figure 6-1
Photograph of HP turbine inner cylinder studs at Plant A.

The accumulated inelastic strain was calculated using the following equation (de Witte and Stube, 1986);

$$\varepsilon_p = (L_2 - L_0) / L_e \qquad \textbf{Eq. 6-1}$$

where ε_p is the accumulated inelastic or permanent strain, L_2 is the total bolt length after service, L_0 is the original total bolt length, and L_e is the effective bolt length. The effective bolt length, i.e., actively deforming region, was calculated by the following;

$$L_e = l_1 + (l_2 + l_3)/2 \qquad \textbf{Eq. 6-2}$$

where l_1 is the length of the uniform section, l_2 and l_3 are the length of the threaded section. The threaded length was assumed to be equal to the bolt diameter in the calculation results given in Table 6-1. This formulation for effective bolt or gage length is consistent with typical stud tightening procedures. An improved definition for effective gage length is one of those corrections that would be considered. Any change in the formula for the effective gage length would be based on analysis (either simplified or FEM) regarding the actively deforming portion of the stud.

Table 6-1
Measured Hp Inner Cylinder Stud Length And Calculated Accumulated Inelastic Strain After Service

Stud	Material	Diameter (Inches)	Length - Inches		Strain (%)
			Specified	Current	
47	1	3	23.1875	23.00	-0.93
48	1	3	23.1875	23.25	0.31
49	1	3	23.1875	23.25	0.31
50	1	3	23.1875	23.25	0.31
51	12	3.5	25.1875	25.61	1.95
52	12	3.5	25.1875	25.61	1.95
53	12	3.5	25.1875	25.86	3.10
54	12	3.5	25.1875	25.61	1.95
55	12	3.5	25.1875	25.61	1.95
56	12	3.5	25.1875	25.61	1.95
57	12	4	27.3125	27.12	-0.83
58	12	4	27.3125	27.12	-0.83
59	12	4.5	27.3125	31.64	-1.08
60	12	4.5	31.9375	61.64	-1.08
61	12	4.5	31.9375	61.64	-1.08
62	12	4.5	31.9375	61.64	-1.08
63	12	5	33.4375	33.14	-1.05
64	12	5	33.4375	33.14	-1.05
65	12	5	33.4375	33.14	-1.05
66	12	5	33.4375	33.14	-1.05
67	12	5	38.3125	37.91	-1.25
68	12	6	38.3125	37.91	-1.25
69	12	6	38.3125	38.16	-0.47

Table 6-1
Continued

Stud	Material	Diameter (Inches)	Length - Inches		Strain (%)
			Specified	Current	
70	12	6	38.3125	37.91	-1.25
71	12	6	38.3125	37.91	-1.25
72	12	6	38.3125	37.91	-1.25
73	12	6	38.3125	38.16	-0.47
74	12	6	38.3125	38.16	-0.47
75	12	6	38.3125	37.91	-1.25
76	12	6	38.3125	38.16	-0.47
77	12	5	34.9375	34.65	-0.96
78	12	5	34.9375	34.65	-0.96

Table 6-1 gives the results for the HP turbine inner cylinder studs gives including the material, measured UT stud length, manufacturer specified stud length, specified stud diameter, and calculated accumulated strain. Because no measured values were available, the original stud length was assumed to be equal to the specified length. If the studs were machined to a tolerance, say +/- 1/8", then this assumption would not greatly affect the calculated strain. Because the current length was generally less than the original specified length, the accumulated strain is negative for the majority of the studs. The reason for the unexpected strain trend is believed to be the assumption regarding the initial bolt length equal to manufacturer specified and not inaccuracies in the current measured stud length.

The measured stud length distribution was bi-variant with value equal to either 18.25" or 18.5". Because the specified length of the valve studs was unavailable at the present time, no strain calculations were performed for the valve studs.

Bolt/Stud Temperature and Temperature Gradient Measurements

Measurements of stud temperature were performed for valve studs at one unit, Plant B. Figure 6-2 shows the thermocouple arrangement. The bulk of the thermocouples were at the mid-length of the stud (thermocouples TC 2, TC 3, TC 5 and TC 6) but one stud had thermocouples at the ends in order to detect temperature gradients (TC 4 towards nut end of stud and TC 1 within valve body). In addition, the valve body had one thermocouple, TC 7. The temperatures were recorded for about a two week period and include one start-up. Figure 6-3 shows the measured temperature for the valve stud thermocouples over about a two hour period that was considered typical of steady state operation. During this time, the nominal main steam temperature was 995F. In essence, the temperatures for the valve studs at the mid-point are relatively uniform, between ~930F to ~ 935F, and approximately 55-60°F below the nominal steam temperature of 995F. The highest temperature was ~960F for TC 1 and the lowest temperature was ~930F for TC 4. Using these values, there is a temperature gradient of approximately 30°F along the length of the studs.

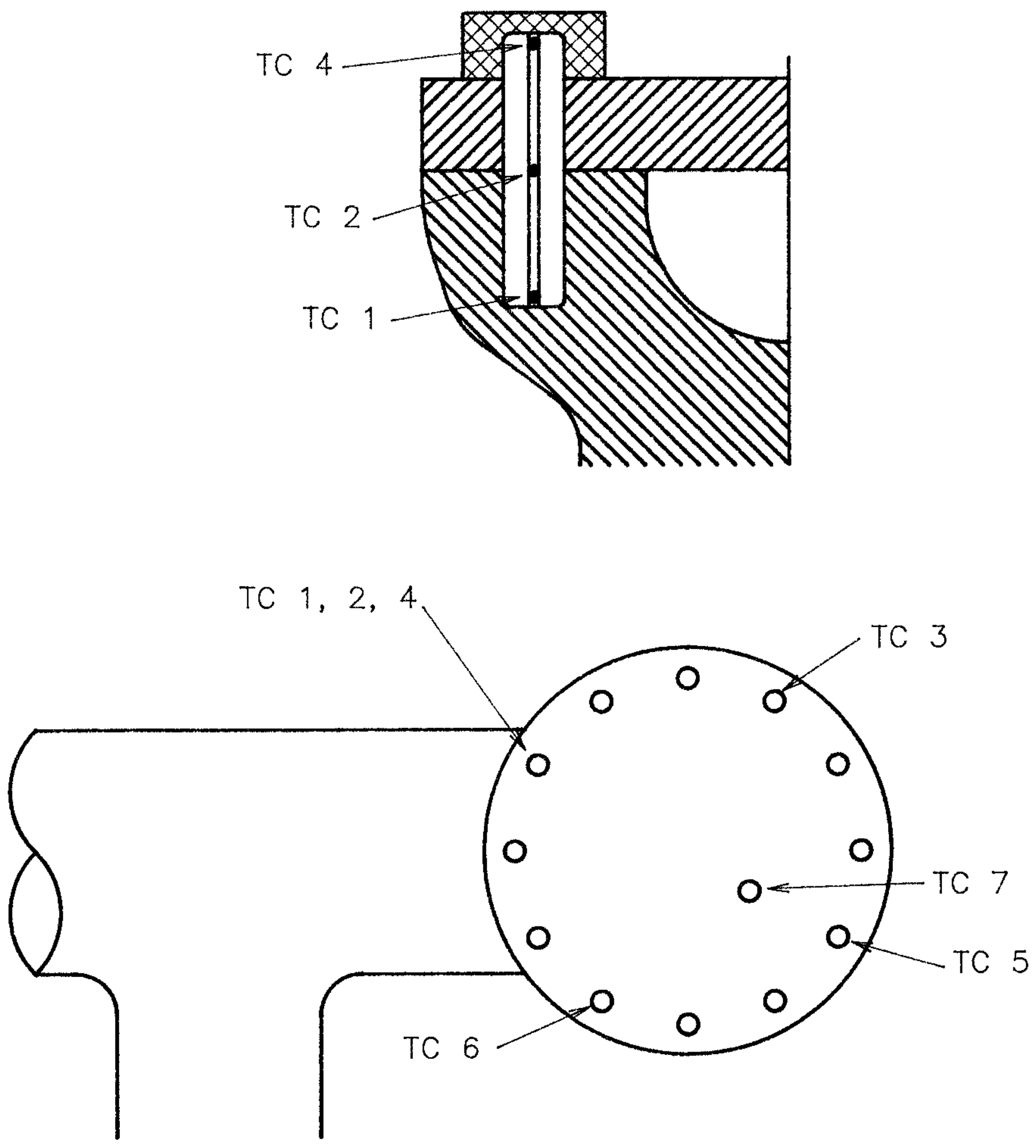

Figure 6-2
Schematic drawing showing the thermocouple arrangement for stop valve studs at Plant B.

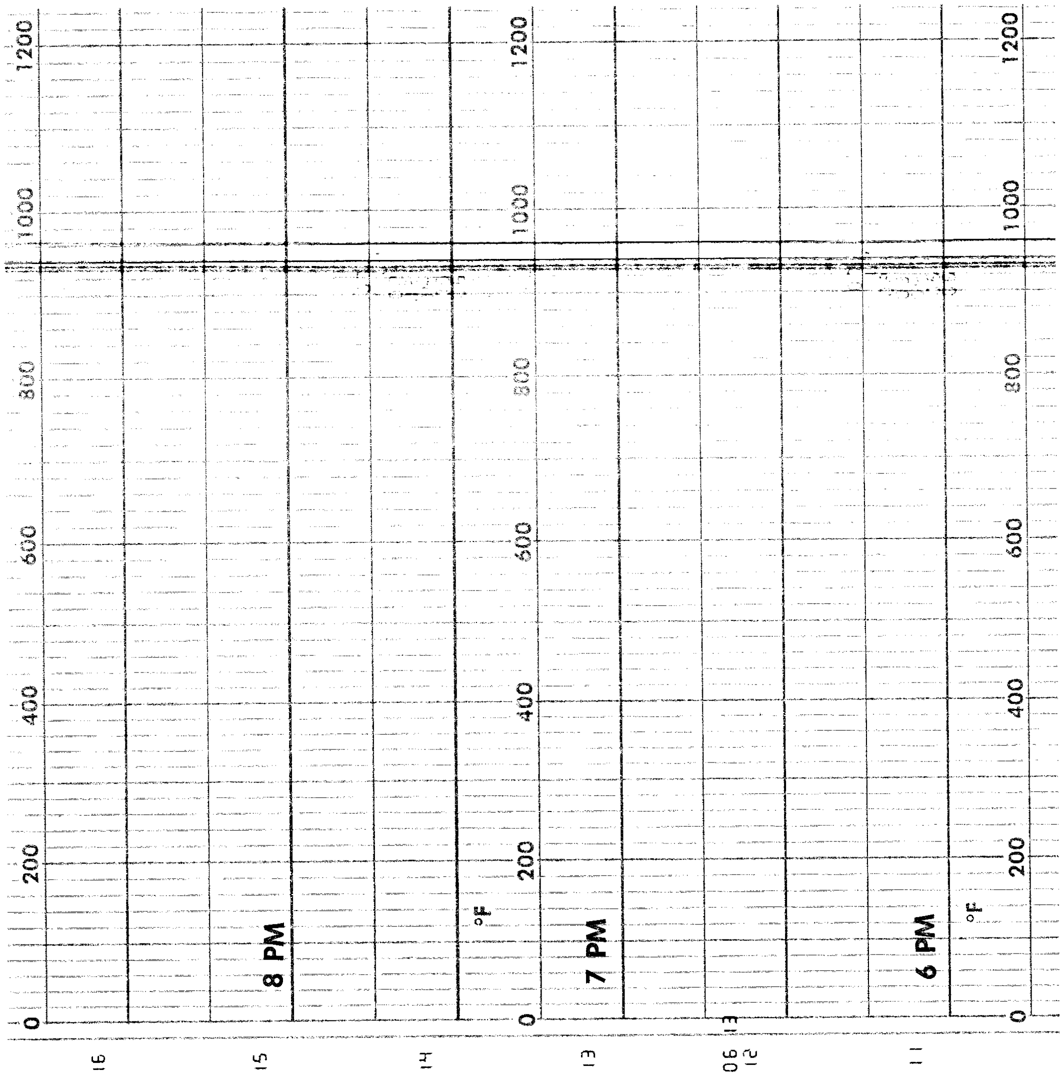

Figure 6-3
Measured stud temperatures for stop valve at Plant B.

Remaining Life Prediction Procedure

The primary objective of the current study was to develop a life prediction calculation for high temperature turbine and valve studs/bolts that was sufficiently accurate to establish the need for critical examination and the need for replacement (Ellis, Sielski and Viswanathan, 1998). The method that was developed used the creep behavior to predict the cyclic stress relaxation properties of stress versus time and accumulated inelastic strain versus time. For life assessment, the end of life was defined as the time to accumulate 1% strain.

To achieve the desired accuracy in life prediction, improvements were sought using the following:

- More realistic creep constitutive equation.
- The service history of operating temperature, number/stress level of tightenings, cycle time, etc. to calculate the cyclic relaxation behavior.
- The use of the current condition of the stud/bolt, i.e., accumulated creep strain, to calculate the remaining creep life.

The creep constitutive equation was discussed in Section 4. It describes the entire creep curve and was written in terms of rupture time and ductility in order to allow for heat-to-heat variability to be included in the bolt material properties description.

The remaining life prediction requires information regarding the current condition of the component and the future operating conditions. The current condition of the stud/bolt is given by the current accumulated inelastic or creep strain. There are two options for the value of the accumulated creep strain to be used in the analysis:

- The measured accumulated creep strain value at the time of the inspection based on the field inspection results.
- The accumulated creep strain that was calculated using the past history of operation.

The current life prediction approach is unique in that it offers the potential to link field measurements of creep damage and the predicted remaining creep life. The appropriate choice depends on the relative confidence in the accuracy of accumulated creep strain. Based on the uncertainty associated with the initial effective gage length used in the analysis of the plant A data given above, option one using the plant history would be recommended in this case.

Additional information required for the remaining life prediction includes the following:

- Bolt geometry.
- Calculation procedure.
- Damage accumulation algorithm.
- Failure criterion.
- Past and future operational variables: applied stretch, cycle hold time, temperature, temperature gradients and number of cycles.
- Material variables of creep strain-time and rupture strength/ductility properties.

Results of the analysis depend strongly of the values used for each of the above variables. The selection of appropriate values depends on the type of analysis, intended use for the analysis and availability of data. For example, steam cooled tubing life prediction uses measured steamside scale thickness and tube wall thickness but the material creep strength is usually assumed to be lower bound. Results of the analysis would be expected to be conservative and correspond to time to first failure. Potential data sources and choices for the life prediction are discussed for these important variables below.

Bolt Geometry

The geometry of interest is a threaded end bolt with a uniform section and a bore hole in the center. The remote loads are determined by the bolt tightening procedure. They were treated as an initial stress that is calculated elastically from the specified nominal shank strain or stretch, the operating temperature and the thermal expansion mis-match between the flange and bolt.

Calculation Procedure

The calculation used the same approach discussed in the validation procedure of Section 5. The calculation is based on the total strain remaining constant and the conversion of elastic strain by stress reduction into creep strain as follows;

$$- (1/E)\, d\sigma/dt = d\varepsilon_c/dt \qquad \textbf{Eq. 6-3}$$

where E is Young's modulus, σ is the stress, t is time and ε_c is the creep strain. For service applications, this equation assumes that the flange and nut do not creep appreciably and the elastic follow-up associated with unloading of the flange is negligible.

As before, a standard incremental calculation procedure was used. The starting stress was calculated elastically as the product of Young's modulus and the applied initial strain (a combination of thermal and mechanical loading). For a time increment, the stress is held constant and the increment of creep strain accumulated is calculated using the two parameter creep equation and a strain hardening flow rule. The rupture time and rupture strain used in the creep equation are calculated using the current value of stress. The magnitude of the stress reduction is the product of Young's modulus and the creep strain increment. The time steps were chosen to limit the maximum stress changes to ~0.5%.

Damage Accumulation Algorithm

The relaxation of the stress in the uniform section as a function of time was calculated using the creep strain-time properties and a strain-hardening associated flow rule as discussed above. This analysis procedure predicts the stress-time and accumulated creep/inelastic strain-time curves for the uniform section portion of the bolt. The damage accumulation algorithm is the summation of the inelastic strain as a function of number of cycles and service time. The calculations use either creep strain equal zero at time equal zero or creep strain equal to the measured accumulated creep strain at the cumulative time of the inspection. The calculations are carried forward in time until an accumulated creep strain of 1% is obtained.

Failure Criterion

Two failure criteria were considered:

- An accumulated strain limit.
- Creep crack initiation at the thread root.

Because of the tendency for failure by cracking in the threads, several approaches have been developed for life prediction by the creep rupture failure mechanism for bolts. The life prediction is usually based only on crack initiation at the thread root rather than both initiation and crack growth schemes. The strain rate damage approach of Schlotner and Seeley (1985) has been shown to be useful for notched bars and valve bolts. Their method requires a finite element stress analysis to obtain the stresses as a function of time.

Ando et al. (1990) use a simplified approach based on an FEM analysis. In this approach, the maximum principal stress and the effective stress at the stress concentration is related to the net section stress by relations involving the stress concentration factor and the effective time related to stress redistribution. They used either the maximum principal stress or the effective stress and either smooth or notched bar rupture properties in order to obtain the rupture time. They used time fractions for the cumulative creep damage summation.

These two failure criterion are interrelated through ductility exhaustion, strain concentration factor for the notch geometry of the thread and the uniform creep strain in the bolt gage section as follows. Holdsworth and Strang (1995) used a relationship for estimating the multiaxial rupture ductility from the uniaxial ductility using values for the effective and hydrostatic stress typical for the multiaxial stress state at the thread root. For the thread root region of their model bolt, the multiaxial rupture ductility was 0.09 times the uniaxial ductility. For the commercial purity Durehette 1055 bolt model tests, they concluded that ductility exhaustion explained the observed creep crack initiation at the thread root. For 1CrMoV bolts, Bulloch and Hickey (1994) found strain concentration where the local thread root strain was approximately 2.7 times the average bolt strain by length measurements. Thus, the initiation of creep cracks in the thread roots corresponds to creep failure by ductility exhaustion at a limiting strain for the material beneath the threads. The magnitude of the limiting strain for crack initiation beneath the threads is equal to the product of the creep strain in the uniform or effective gage section of the bolt and the strain concentration factor.

Both failure criteria would use the time history of the remote stresses as the basis of the damage accumulation summation. The creep crack initiation criterion would then use the remote load history to calculate the local, multiaxial stresses at the root of the thread. Because of the marked effect of operating variables on calculated remote loading and its relative simplicity, the accumulated strain limit was selected as most appropriate. As noted previously, there are several advantages for the strain limit:

- The change in length for the bolt is a measurable quantity.
- The time to 1% creep strain has been used as a failure criterion (ABB, 1992; de Witte and Stubbe, 1985; de Witte, 1989).
- This failure criterion is consistent with a ductility exhaustion failure approach for creep crack initiation at the thread root and the replacement recommendation used in the integrated methodology (Ellis, Sielski and Viswanathan, 1998).

The optimal magnitude of the inelastic strain limit is also one of the adjustable parameters that would be used to forge better agreement between service experience and calculated. For example, Ellis et al. (1992) recommended a creep strain limit of 2% for steamlines.

Number of Tightenings and Cycle Hold Time

The original concept was to obtain values for the number of tightenings and cycle hold time for the bolts/studs at each plant. If available, this approach would be expected to yield the most realistic results because the analysis loading history would correspond most closely to the actual operating history. The future loading history would either be based on the past history or the best estimate of future operating requirements related to the intended duty cycle for the unit.

Table 6-2 gives the tightening number, accumulated service time and cycle hold time for the HP turbine at Plant A. The results for the HP turbine were considered typical. They are consistent with some early break-in problems and nominal routine maintenance on a four to six year schedule yielding relatively uniform cycle hold times in line with the expected 30,000 hour design basis.

Table 6-2
Number Of Tightenings And Cycle Hold Time For Hp Turbine Studs

Number Of Tightening	Service Time - Hours	Cycle Hold Time (Hours)
1	6,303	6,303
2	18,219	11,916
3	54,204	35,985
4	79,798	25,594
5	104,390	24,592
6	127,609	23,219
7	158,952	31,343
8	181,969	23,017

The search of the valve maintenance records revealed inconsistent results for the cycle hold time. The inconsistent cycle hold times were believed to be associated with either missing or incomplete records and/or search procedures. Discussions with plant personnel indicated that nominal valve maintenance was on an approximate two year calendar interval. Based on calendar-operating time correlations for Plant A, the nominal cycle hold time for the valves was ~10,000 hours with an expected variance of ~2,000 hours.

Temperature and Temperature Gradient

For the analysis, the temperature is treated as a variable with the calculation performed at fixed temperatures corresponding to the creep test temperatures. Thus, the calculations are performed at 842F and 932F for 1CrMoV and at 932F and 1022F for 12CrMoWV. Because the relaxation is virtually complete at 1022F for 1CrMoV and 1112F for 12CrMoWV, no calculations are recommended at these temperatures. Interpolation and extrapolation of the calculated time to 1% strain to the actual operating temperature is required in order to determine the bolt life. Based on engineering judgement and an analysis of the NRIM bolt relaxation data (NRIM, 1997), log time to an accumulated creep or inelastic strain of 1% versus inverse temperature would be expected to give good extrapolation/interpolation results.

For comparison with the analysis results, an estimate of the operating temperature for the studs/bolts is needed. The operating temperature is expected to depend strongly on the type of stud/bolt being evaluated. The measured temperature for the valve studs indicates that the stud temperature at the mid-length of the stud was significantly less, ~55 to 60°F, than the nominal steam temperature. No temperature measurements were made for the HP inner cylinder bolts. However, it is possible that the nominal rotor temperatures based on design analysis would be useful in estimating the appropriate temperature for life assessment.

Stretch and Initial Stress

Generally, a three level approach is used for life assessment. A level one life assessment is similar to a design calculation and is not expected to be highly accurate. In a level two assessment, actual operating conditions and measured component geometry are used in the analysis. In a level three analysis, operating conditions, measured geometry and material properties are used. In essence, the current approach was intended to be a level two assessment in which more realistic service conditions were used for the life prediction (Ellis, Sielski and Viswanathan, 1998).

Indeed, Laborelec's approach for turbine bolt life assessment recommends that the service history of prestress at tightenings be used (de Witte, 1989). A historical search of one utilities' records revealed that the high temperature bolting stretch values are almost never recorded. In several instances, stretch records were found. The type of data required for a level two analysis is shown in Figure 6-4. These measured stretch values were for the bolts in the HP turbine outer cylinder. The measured stretch in this case was within approximately +/-10% of the required stretch recommended by the OEM. Statistical analysis of this and other stretch values was performed. Although no definitive conclusion regarding the distribution function was made, it is believed that a Gaussian distribution function would be adequate for the purpose of life prediction based on the analyses performed and engineering judgement. Monte Carlo simulation techniques could then be used to generate the future stretch loading history (Ellis and Roberts, 1988).

In the case of inadequate stretch data, a design based approach can be used. The applied stretch is a function of the bolt material and the service time. General Electric has developed recommended stretch values for 1CrMoV (B5F5B) and 12CrMoWV (B50A125E) bolt materials. Their recommendation was shown on a plot of elongation in mils versus effective stud length in inches. Values were scaled from the plot and converted to strain in per cent. Table 6-3 gives the upper and lower bounds for applied stretch recommended by General Electric for 1CrMoV (B5F5B) and 12CrMoWV (B50A125E) after 1970 (General Electric, 1979). The pre 1970 applied stretch levels were calculated as 1/0.7 times the 1970 applied stretch levels given in the table.

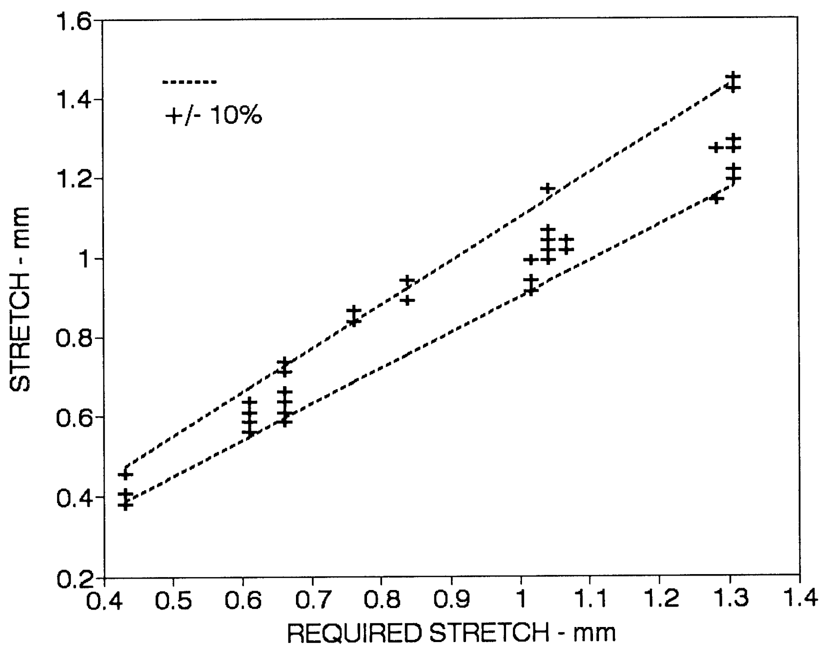

Figure 6-4
Measured stretch versus required stretch for HP turbine outer cylinder bolts (1 inch = 25.4mm).

Table 6-3
Recommended Applied Stretch For High Temperature Studs/Bolts (General Electric, 1979)

Material	Pre 1970 Stretch - %		1970 Stretch - %	
	Upper	Lower	Upper	Lower
B5F5B (1CrMoV)	0.150	0.120	0.105	0.082
B50A125E (12CrMoWV)	0.115	0.090	0.080	0.062

The initial stress level due to mechanical loading is calculated as the product of the applied stretch given in Table 6-3 and the Young's modulus as a function of temperature. For the 1CrMoV steel and a low allow ferritic flange/valve body, either 1CrMoV or 2-1/4Cr-1Mo, the initial stress can be calculated as follows:

$\sigma = E(T)\ \varepsilon_0$ **Eq. 6-4**

where σ is the initial stress in ksi, E(T) is the Young's modulus as a function of temperature (Tanaka and Ohba, 1984) and ε_0 is the applied stretch in inch/inch.

The National Power life assessment approach uses a life reduction factor associated with uncontrolled tightenings (Townsend, 1995). Thus, the appropriate stretch level in the analysis depends on the purpose of the analysis. For a mean life assessment, the mean stretch value would be used and is considered a reasonable first step. For a conservative minimum life assessment, the upper bound stretch would be used. Table 6-4 gives the initial stress level for 1CrMoV bolts/studs as a function of temperature at stretch values of 0.15%, 0.105% and 0.082%.

Table 6-4
Calculated Initial Stress For 1CrMoV Studs/Bolts

Quantity	Temperature - F			
	842	932	1005	1022
E* - ksi	26,597	24,321	21,700	21,050
σ_0 – ksi (ε_0 =0.15%)	39.9	36.5	32.6	31.6
σ_0 – ksi (ε_0 =0.105%)	27.9	25.5	22.8	22.1
σ_0 – ksi (ε_0 =0.082%)	22.0	20.1	18.0	17.4

* Young's modulus values taken from Tanaka and Ohba (1984).

For 12CrMoWV bolts, the applied initial stress level is due to both mechanical and thermal loading. The mechanical portion is calculated in the same fashion that was used for the 1CrMoV bolts, i.e., as the product of the applied stretch given in Table 6-3 and the Young's modulus as a function of temperature. For the 12CrMoWV steel and a low allow ferritic flange/valve body, the initial stress can be calculated as follows:

$\sigma = E(T)\ \varepsilon_0 + E(T)\ (D\alpha)\ (DT)$ **Eq. 6-5**

where σ is the initial stress in ksi, E(T) is the Young's modulus as a function of temperature (Tanaka and Ohba, 1984), ε_0 is the applied stretch in inch/inch, $D\alpha$ is the difference in the coefficient of expansion between the 12CrMoWV bolt material and the 1CrMoV flange material and DT is the temperature difference between the operating temperature of the bolt and the stress free temperature, assumed to be room temperature of ~77F or 25C. Table 6-5 gives the initial stress level for 12CrMoWV bolts/studs as a function of temperature at stretch values of 0.115%, 0.080% and 0.062%. These stress equations are elastic and simplified versions of those given by Mayer and Konig (1992) for the bolt model test. Any improvements in the stress formulation would require additional detailed information regarding valve/turbine geometry.

Table 6-5
Calculated Initial Stress For 12CrMoWV Studs/Bolts

Quantity	Temperature - F		
	932	1022	1050
E^* - ksi	23,894	21,476	20,495
$D\alpha$ - 1/R x 10^{-6}	1.161	1.194	1.194
DT - °F	855	945	973
$D\alpha$ DT - %	.0993	.1128	.1160
σ_0 – ksi (ε_0 =0.115%)	51.2	48.9	47.4
σ_0 – ksi (ε_0 =0.080%)	42.8	41.4	40.2
σ_0 – ksi (ε_0 =0.062%)	38.5	37.6	36.5

* Young's modulus values taken from Tanaka and Ohba (1984).

Material Creep Strength and Ductility

Although the two parameter creep equation allows for the heat-to-heat variation in material creep rupture strength and ductility to be modeled, the creep data used in the creep constitutive development was only for one heat of material. Thus, application of the two parameter equation for other strength/ductility heats cannot be verified. For the 12CrMoWV bolt material, there is sufficient rupture properties data that a multi-heat analysis could be performed. In essence, the single heat of 12CrMoWV material characterized was toward the upper half of the scatterband in rupture strength and towards the lower half of the scatterband in rupture ductility. Based on this strength/ductility condition and the characteristics of the two parameter creep equation, it is expected that the predicted strain would be lower than that for a mean strength/ductility heat of material and significantly less than that for a minimum strength heat of material. Conversely, the heat of 1CrMoV bolt material would appear to be a "weak heat" and the predicted accumulated strain using the properties for this heat would be expected to be significantly greater than that for a mean heat.

Time/Cycles for Life Prediction

To complete the life prediction method, a recommendation is needed for when the calculation should be performed - i.e., the number of tightening cycles and service time. Traditionally, the recommendation for the calculation of creep life expended and remaining life has been based on numerous criteria such as the need for the plant to operate reliably during the next operating cycle, concerns regarding the end of life associated with recent creep failures and so forth. In essence, life prediction is believed to be useful under the following three conditions:

- At approximately 60% of the expected life. The initial life prediction should be performed prior to any expected failures in order to identify those bolt/studs subject to premature failure.
- As part of a comprehensive failure analysis.
- When there is a significant difference between the predicted and observed accumulated creep strain.

For high temperature bolts, the recommendation for the initial calculation of remaining creep life is based on the product of a factor of safety, 0.6 (based on experience and typical creep life guidelines), times the expected life. The expected service life is based on the experience of the turbine manufacturer. The need for subsequent life predictions depends on the results of the initial assessment, the observed accumulated creep strain and any significant change in operation between the initial assessment and current/future. Therefore, the time for future life predictions is revealed by the differences between the predicted and observed accumulated creep strain.

Recommendations are needed for both the inner cylinder studs/bolts in the high pressure turbine and the valve studs/bolts. Because of differences in operating conditions, primarily cycle time, the recommendations are different for each. For the inner cylinder studs/bolts in the high pressure turbine, ABB has recommended replacement at a strain limit of 1% or after 150,000 hours of service (ABB, 1992). Based on this life estimate, the life prediction calculation should be performed after 0.6 x 150,000 = 90,000 hours of service. Using a nominal cycle time of 30,000 hours, the bolts would have experienced approximately three cycles at the recommended initial life prediction time.

For the high temperature valve studs, General Electric has developed a detailed set of recommended inspection intervals and expected cyclic life (General Electric, 1979). The lives are stated in absolute terms for the number of tightenings with a corresponding approximate value for service time given only as a reference. The expected lives depend on the material with shorter lives for the B5F5B material (1Cr-1/2Mo-1/4V) than for the B50A125E material (12Cr-1Mo-1W-1/4V). For B5F5B material, the recommendation is to replace the bolt after 11 tightenings or approximately 24 years of service. Using the factor of 0.6, the nearest scheduled stud inspection was at the end of six tightenings or approximately 12 years service. For the B50A125E material, the recommendation is to replace the bolt after 25 tightenings or approximately 57 years of service. Using the factor of 0.6, the scheduled stud inspection was at the end of fifteen (15) tightenings or approximately 30 years service.

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7 CONCLUSIONS AND RECOMMENDATIONS

The objective of the current study was to develop and validate an analytical creep life prediction method for high temperature bolting. The conclusions and recommendations given below are based on the results of this study that included the following; a literature survey, testing of service exposed bolts, creep constitutive equation development, remaining life prediction and validation studies involving comparisons of calculated and observed stress relaxation behavior.

Conclusions

Constitutive equations were developed for three bolt materials - 1Cr-1/2Mo-1/4V, 1CrMoVTiB and 12Cr-1Mo-1W-1/4V. Based on the results of the creep constitutive equation development studies, the following conclusions were made:

- Primary creep can be described using a power law. The time power increases as temperature increases.
- The two-parameter material model is a good description of the creep strain - time behavior for total strains up to approximately 1%. Values for the material constants m+1 and δ in the two-parameter model are a function of temperature.
- The rupture time as a function of stress and temperature can be correlated using time-temperature parametric methods.
- The rupture elongation at each isotherm is related to the rupture time by a power law.

For the uniaxial testing, comparisons of calculated and observed stress relaxation behavior of 12Cr-1Mo-1W-1/4V bolting steel were made at temperatures of 932F, 1022F and 1112F and at initial strain levels of 0.10%, 0.15%, 0.20% and 0.25%. Based on the results of these comparisons, the following conclusions were made:

- The calculated stress versus time curves were in good agreement with the measured at 932F and 1022F. At 1112F, the calculated stress versus time curves were in good agreement with the measured at both short times and long times but in poor agreement at intermediate times.
- A life prediction methodology using analytical calculation of inelastic strain accumulated during stress relaxation has been validated for 12Cr-1Mo-1W-1/4V bolting steel.

Based on comparisons of the analytical calculation and bolt model test results for two heats of 1CrMoVTiB bolt material at 1022F, the following conclusions were made:

- The calculated values for relaxed stress as a function of cycles were shown to be in fair agreement with those measured in bolt model tests.

- The calculated values for accumulated inelastic strain as a function of cycles were shown to be in good agreement with those measured in bolt model tests.

Because the life assessment for high temperature bolting is based on accumulated inelastic strain and based on the agreement between the following; (1) the analytical calculation procedure and the bolt model results and (2) the bolt model results and service experience, the following conclusion was made:

- The analytical calculation procedure technique for bolting life prediction is validated for application to bolts in service within the same limits/constraints as those pertaining to bolt model predictions.

For service validation, the current length of HP turbine inner cylinder and stop valve bolts were measured. In addition, the temperature of the stop valve studs were measured. Based on the field results, the following conclusions were made:

- The initial bolt length was estimated as the nominal specified and was used to calculate the accumulated inelastic or creep strain. Because of this uncertainty in initial length, the observed creep strain was considered unreliable.
- The measured temperature at the mid-point of the valve studs was uniform at the four positions around the valve body and approximately 55-60°F below the nominal steam temperature of 995F.
- There is a temperature gradient of approximately 30°F along the length of the stud.

Recommendations

Based on the results of the current study, the following recommendations were made:

- The creep constitutive equation development for 1Cr-1/2Mo-1/4V bolt material should be completed.
- The analytical calculation of accumulated inelastic strain should be performed for the HP turbine inner cylinder bolts at Plant A.
- A technique to incorporate the heat-to-heat variability in the creep behavior of 12Cr-1Mo-1W-1/4V bolt material in the two parameter creep constitutive equation should be developed.
- For cases where the initial bolt/stud length is unknown, a method to use the change in measured stud length as a function of service time (strain rate approach) should be developed for life assessment.
- Stud length measurements should be performed at a plant where the initial stud length is known. The measured strains should be compared to the results of the analytical calculation procedure to develop a definitive basis for an evaluation of the utility of the life prediction approach.
- The calculation procedure should be modified to handle temperature gradients along the length of the stud.

A
INTEGRATED METHODOLOGY

Introduction

The need for an integrated methodology for bolting life assessment is illustrated by the results of a typical failure analysis scenario (Ellis, Sielski, and Viswanathan, 1998). Because of stress concentration, cracks in bolts usually initiate at the root of the threads. These cracks generally develop at high temperatures by creep or low-cycle fatigue with subsequent failure frequently occurring by brittle fracture at low temperature during shut downs or cold retightening of the bolt. The brittle fracture tendencies are exacerbated by embrittlement phenomena such as temper embrittlement, coupled with the triaxial state of stress at the thread root. This situation generally leads to rapid crack propagation, once a crack has grown to the critical size. Because of uncertainties regarding applied loads and material toughness, nondestructive techniques are generally employed for "crack detection" and not for "crack sizing".

Based on this failure scenario, life prediction techniques employed to estimate the time to crack initiation and NDE techniques to detect and locate cracks are important elements of the integrated methodology. Equally important are techniques employed to estimate the fracture toughness governing the ultimate failure (see Section 2). Thus, comprehensive life assessment requires an integrated methodology consisting of the following:

- Condition assessment.
- Life prediction.
- Recommended replacement criteria.
- Failure/fracture analysis.

Detailed guidelines on the performance of the life prediction calculation for high temperature studs/bolts was given in Section 6 of the report. This appendix gives an outline of the recommended guidelines for condition assessment techniques, recommended replacement criteria and failure/fracture analysis.

Condition Assessment

The condition assessment techniques and guidelines used for life assessment of threaded fasteners were discussed in Section 2 (ABB, 1992; General Electric, 1979; CEGB, 1986; VGB, 1988; de Witte, 1989; Bulloch and Bissel, 1995). The recommended guidelines for condition assessment given below were based on results of the current project and an evaluation of these currently available guidelines. They were intended to represent a comprehensive practice, i.e., best effort assessment. Selection from the recommended list for actual applications by utilities to their operating studs/bolts depends on many factors including safety, engineering judgement, management philosophy, costs, outage duration, perceived need for the assessment, etc.. Recommended condition assessment techniques are the following:

- Ultrasonic inspection should be performed for crack detection. The detailed inspection procedure and inspection frequency should be in accordance with utility internal standards, insurance requirements and manufacturer recommendations. If cracks are found, then the bolt is replaced.
- For the case of no reported indications, ultrasonic inspection for elongation measurements should be performed. The inspection results should be fully documented and stored in a format compatible with archival retrieval and analysis. The observed creep strain should be calculated using, preferably, the measured initial bolt length or the best estimate such as specified. Plots of accumulated inelastic strain as a function of bolt position and operating hours should be made. The observed strain should be compared to the life prediction results where available. Bolt replacement is recommended at an accumulated inelastic strain of 1%.
- At each tightening, the bolt prestress, i.e., stretch should be measured and documented for all high temperature bolts. The total service time and expected operating temperature should also be recorded for use in life prediction.
- Failure analysis should be performed for all failed high temperature bolts to determine the failure mechanism, as a minimum, and the root cause of failure in best case scenarios. Fracture analysis is recommended for cases where a comprehensive investigation is needed.
- The need for and type of destructive testing (elevated temperature tensile, accelerated creep and creep rupture and toughness) should be evaluated on a case by case basis. Destructive testing should be performed only as part of a failure analysis. Destructive testing is not recommended on a routine, sampling basis for the purpose of damage detection and degradation in material properties due to service.

Supplementary techniques recommended for consideration in specific instances such as cases of known problems that result in an unacceptable failure rate are the following:

- For cases of creep failure, diameter and hardness readings on the bolt shaft should be performed in addition to the recommended bolt length measurements (Mayer and Keienburg, 1980; Mayer and Konig, 1986). These tests detect evidence of creep strain as either a local reduction in area or lower hardness values.
- For cases of known temper embrittlement or reverse temper embrittlement failure, nondisruptive bolt sampling should performed. A small section of material should be removed from a noncritical location on the bolt. Two techniques are recommended for fracture toughness estimation. The first is the small punch test measured fracture toughness that can be correlated with the conventionally measured value (Foulds and Viswanathan, 1994; Foulds and Viswanathan, 1996). The second technique is the measurement of grain size and bulk phosphorous content that can be correlated with embrittlement (Bulloch and Bissel, 1995).

Recommended Replacement Criteria

Based on the results of the current project, the replacement recommendation is based on either crack detection or an accumulated strain limit of 1%. However, hardness and toughness could also be used as a basis for replacement in certain situations (Jones, 1995).

Failure/Fracture Analysis

For failure analysis of high temperature bolts, the root cause methodology is recommended. The root cause approach has been extensively documented for application to boiler tube failures and the application to bolt failures is expected to be straightforward. In order to implement the methodology, the results of numerous, detailed case studies are required. Gathering, evaluating and classifying high temperature bolting failure records was beyond the scope of the current project. However, the failure mechanisms for high temperature bolts of creep rupture, brittle fracture, stress corrosion cracking and low-cycle fatigue were discussed in Section 2 (Hickley and Bulloch, 1992; Townsend, 1995; Ellis, Sielski and Viswanathan, 1998). Typical failure locations, fracture appearance and metallurgical characteristics have been discussed in the open literature.

For fracture analysis of high temperature bolts, the failure assessment diagram (FAD) approach is recommended. The failure assessment diagram (FAD) approach was developed by the British Standards Institute (BSI, 1991) and has been shown to be applicable for the case of bolts containing cracks (Holdsworth and Strang, 1995). The FAD approach requires crack depth, applied load, material properties of flow stress and fracture toughness. Details on application of FAD can be found in the references cited.

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