1

Basic Concepts

OPERATING UNIT IN A REFINERY

Analysis of ASME Boiler, Pressure Vessel, and Nuclear Components in the Creep Range, Second Edition. Maan H. Jawad and Robert I. Jetter.

1.1 Introduction

Many vessels and equipment components encounter elevated temperatures during their operation. Such exposure to elevated temperature could result in a slow continuous deformation and creep of the equipment material under sustained loads. Examples of such equipment include hydrocrackers at refineries, power boiler components at electric generating plants, turbine blades in engines, and components in nuclear plants. The temperature at which creep becomes significant is a function of material composition and load magnitude and duration.

Components under loading are usually stressed in tension, compression, bending, torsion, or a combination of such modes. Most design codes provide allowable stress values at room temperature or at temperatures well below the creep range; for example, the codes for civil structures such as the American Institute of Steel Construction and International Building Code. Pressure vessel codes such as the ASME Boiler and Pressure Vessel Code, British, and the European Standard BS EN 13445 contain sections that cover temperatures from the cryogenic range to much higher temperatures where effects of creep are the dominant failure mode. For temperatures and loading conditions in the creep regime, the designer must rely on either in-house criteria or use a pressure vessel code that covers the temperature range of interest. Table 1.1 gives a general perspective on when creep becomes a design consideration for various materials. It is broadly based on the temperature at which creep properties begin to govern allowable stress values in the ASME Boiler and Pressure Vessel Code. There may

Table 1.1 Approximate temperatures[1] at which creep becomes a design consideration in various materials.

	Temperature	
Material	**°F**	**°C**
Carbon and low alloy steel	700–900	370–480
Stainless steels	800–1000	425–535
Aluminum alloys	300	150
Copper alloys	300	150
Nickel alloys	900–1100	480–595
Titanium and zirconium alloys	600–650	315–345
Lead	Room temperature	

[1] These temperatures may vary significantly for the specific product chemistry and failure mode under consideration.

be other specific considerations for a particular design situation, e.g., a short duration load at a temperature above the threshold values shown in Table 1.1. These considerations will be discussed later in this chapter in more detail.

It will be assumed in this book that material properties are not degraded due to process conditions. Such degradation can have a significant effect on creep and rupture properties. Items such as exfoliation Thielsch (1977), hydrogen sulfide Dillon (2000), hydrogen embrittlement, nuclear radiation, and other environment impacts may have great influence on the creep rupture of an alloy; engineers have to rely on experience and field data to supplement theoretical analysis.

One of the concerns for design engineers is the recent increase in allowable stress values in both ASME VIII-1 and VIII-2 and their effect on equipment design, such as hydrotreaters. The recent increase in allowable stress reduces the temperature at which creep controls and upgrading older equipment based on the newer allowable stress requires the knowledge of creep design covered in this book.

1.2 Creep in Metals

1.2.1 Description and Measurement

Creep is the continuous, time-dependent deformation of a material at a given temperature and applied load. Although, conceptually, creep will occur at any stress level and temperature if the measurements are taken over very long periods, there are practical measures of when creep becomes significant for engineering considerations in metallic structures.

Metallurgically, creep is associated with the generation and movement of dislocations, cavities, grain boundary sliding, and mass transport by diffusion. There are many studies of these phenomena and there is extensive literature on the subject. Fortunately for the practicing engineer, a detailed mastery of the metallurgical aspects of creep is not required to design reliable structures and components at elevated temperature. What is required is a basic understanding of how creep is characterized and how creep behavior is translated into design rules for components operating at elevated temperatures.

A creep curve at a given temperature is experimentally obtained by loading a specimen at a given stress level and measuring the strain as a function of time until rupture. Figure 1.1 conceptually shows a standard creep testing machine. A constant force is applied to the specimen through a lever and deadweight load. Typically, the test specimen is surrounded by an electrically controlled furnace. Because creep is highly temperature-dependent, considerable care must be taken to ensure that the specimen temperature is maintained at a constant value, both spatially and temporally.

There are various methods for measuring strain. Figure 1.2 shows one such arrangement suitable for higher temperatures and longer times, which uses

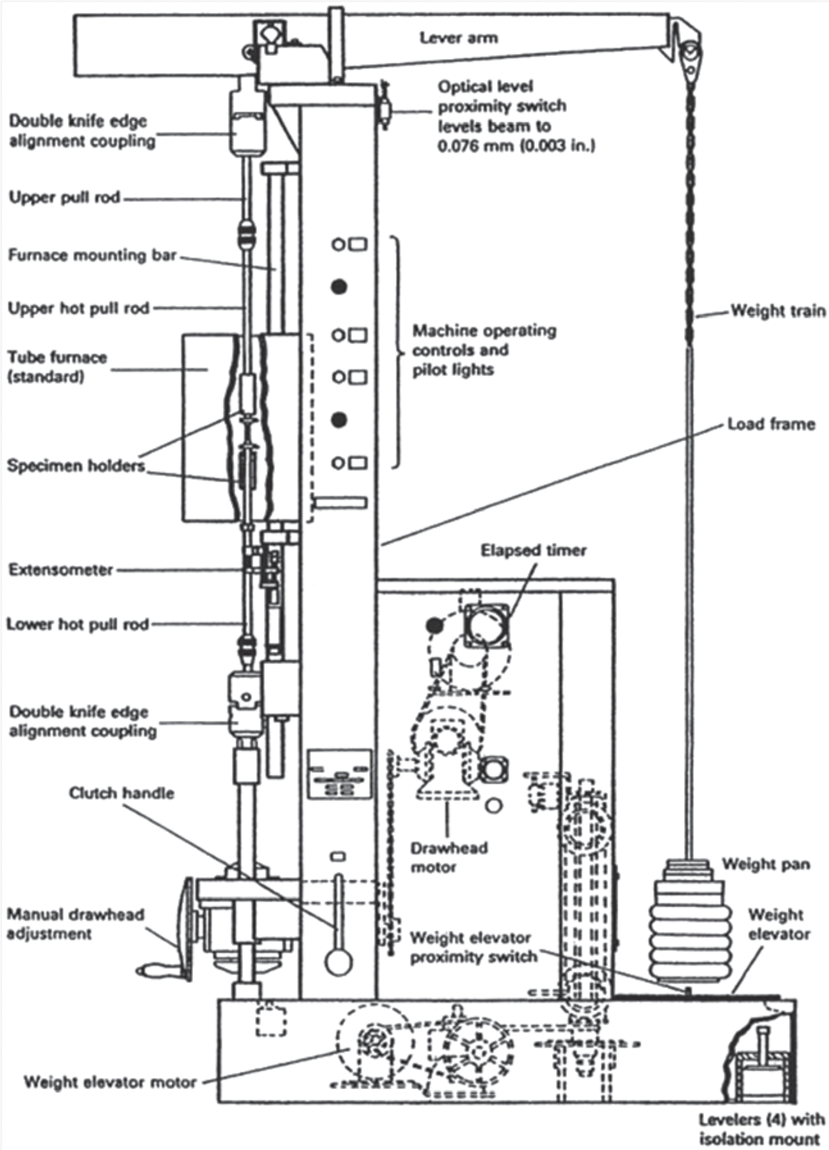

Figure 1.1 Standard creep testing machine [ASM 2000].

two or three extensometers arranged concentrically around the specimen. Penny and Marriott (1995) have summarized the effects of test variables on typical test results. They concluded that faulty measurement of mean stress and temperature are the largest sources of error and that these measurements should be accurate to better than 1% and 1.25%, respectively, to achieve creep

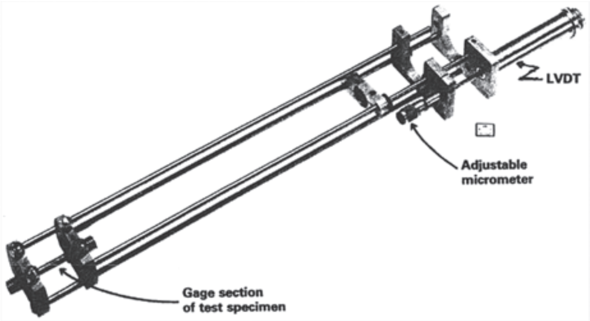

Figure 1.2 Extensometer for elevated temperature creep testing [ASM 2000].

strain measurement accuracy to within 10%. For example, it is recommended that, in order to minimize bending effects, tolerances to within 0.002 in. (0.05 mm) must be achieved in aligning a 1.25-in. (6.4 mm)-diameter specimen.

The above highlights an aspect of material behavior in the creep regime that influences design factors and approximations when establishing allowable design parameters. There can be considerable scatter in measured creep behavior; not only considering the measurement issues addressed above but also the role of alloy composition and the impact of fabrication processes. Basically, it is the consensus evolution of design methods and corresponding margins to account for material variability that leads to component configurations that will robustly withstand the applied loading conditions throughout the intended life of the component. Quoting from the Foreword in each Code Book, "*The objective of the rules is to afford reasonably certain protection of life and property, and to provide a margin for deterioration in service to give a reasonably long, safe period of usefulness. Advancements in design and materials and evidence of experience have been recognized.*"

1.2.2 Elevated Temperature Material Behavior

The distinguishing feature of elevated temperature material behavior is whether significant creep effects are present. Consider a uniaxial tensile specimen with a constant applied load at a given temperature. As shown in Figure 1.3, if the temperature is low enough that there is no significant creep then the stresses and strain achieve their maximum values at time t_0 and remain constant as long as the load is maintained. The stresses and strain are thus *time-independent*. However, as shown in Figure 1.4, if the test temperature is high enough for significant creep effects, the strain will increase with time and eventually, depending on time, temperature, and load, rupture will occur. In the latter case, the strain is time-dependent.

In the previous example, the load was held constant. Now, consider the case with the specimen stretched to a constant displacement. In this case, as shown

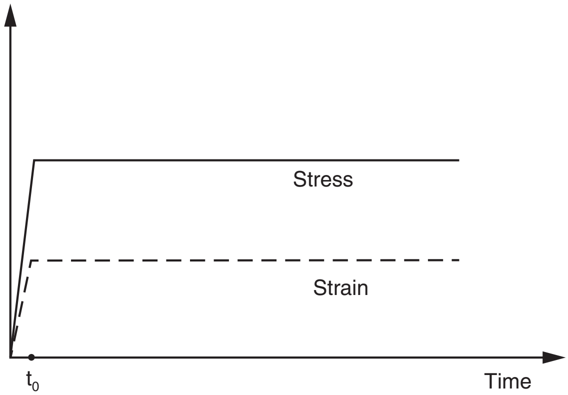

Figure 1.3 Load-controlled loading at low temperature.

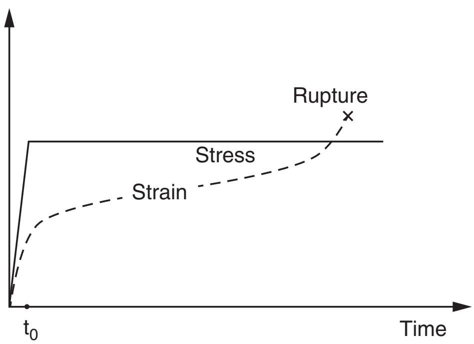

Figure 1.4 Load-controlled loading at elevated temperature.

in Figure 1.5, Line (a), if the temperature is low enough that there is no significant creep, then both the stresses and strain will be constant. However, if the temperature is high enough for significant creep, the stress will relax while the strain is constant (Line b). The behavior illustrated by Line (a) is *time-independent* and by Line (b) *time-dependent*.

Note also the difference in structural response between the constant applied load and the constant applied displacement. In the first case, referred to as *load-controlled*, the stress did not relax and, at elevated temperature, the strain increased until the specimen ruptured. The membrane stress in a pressurized cylinder is an example of load-controlled stress. In the second case, referred to as *deformation-controlled*, the strain was constant and the stress relaxed without causing rupture. Certain stresses resulting from the temperature distribution in a structure are an example of deformation-controlled stresses. Load-controlled

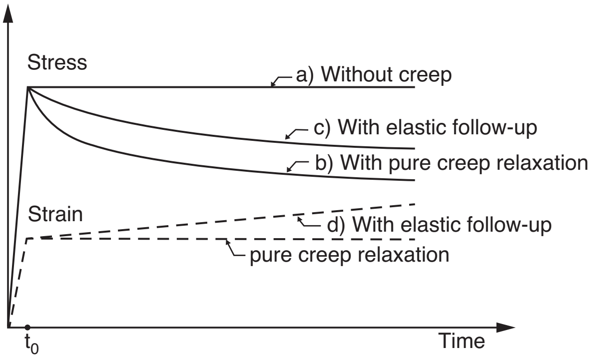

Figure 1.5 Strain-controlled loading at elevated temperature.

stresses can result in failure in one sustained application, whereas failure due to a deformation-controlled stress usually results from repeated load applications. However, due to stress and strain redistribution effects (discussed in more detail in subsequent chapters), the behavior of actual structures is more complex. For example, if there is elastic follow-up, then stress relaxation will slow and there will be an increase in strain, as shown in Figure 1.5, Lines (c) and (d), respectively. Thus, elastic follow-up, depending on the magnitude of the effect, can cause deformation-controlled stresses to approach the characteristics of load-controlled stresses. The distinction between load-controlled and displacement-controlled response and the role of elastic follow-up – or, more generally, time-dependent stress and strain redistribution – is central to the development and implementation of elevated temperature design criteria.

1.2.3 Creep Characteristics

A representative set of creep curves is shown in Figure 1.6 for carbon steel. As shown in Figure 1.7, the curve is usually divided into three zones. The first zone is called primary creep and is characterized by a relatively high initial creep rate that slows to a constant rate. This constant rate characterizes the second zone, called secondary creep. For many materials, the major portion of the test duration is spent in secondary creep. The third zone is called tertiary creep and is characterized by an increasing creep rate that culminates in creep rupture. Although for many materials most of the test is spent in secondary creep, for some materials – for example, certain nickel-based alloys at very high temperatures – primary and secondary creep are virtually negligible, Figure 1.8, and almost the entire test is in the third, or tertiary creep, stage.

As described more fully in Section 1.4.6, it is sometimes assumed that deformations and stresses in the primary creep regime do not significantly contribute

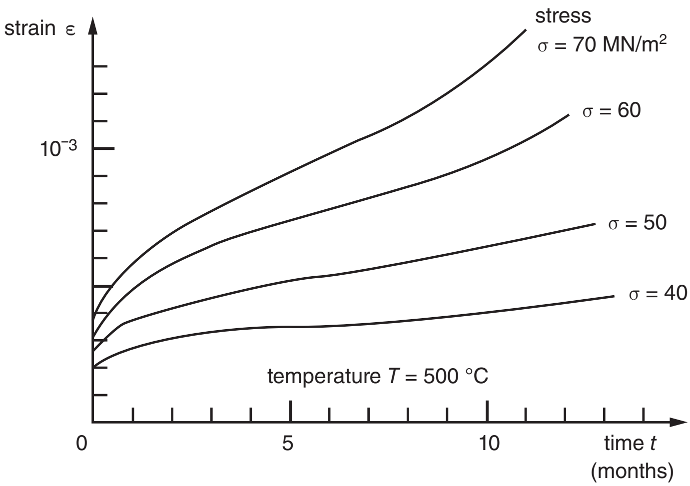

Figure 1.6 Creep curves for carbon steel Hult (1966).

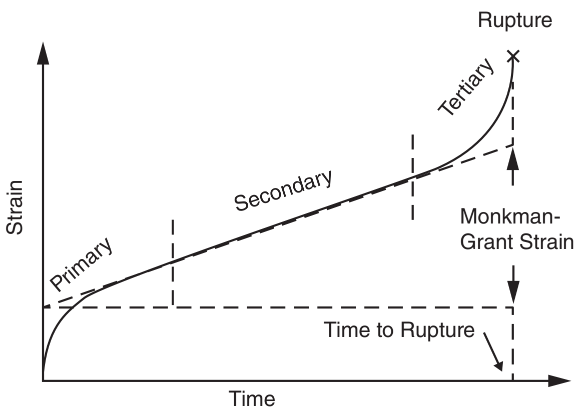

Figure 1.7 Creep regimes – strain vs. time at constant stress.

to accumulated creep rupture damage. An interesting application of the above assumption occurs in the assessment of the impact of heat treatment on structural integrity. For very large components, in particular, the complete time for the whole heat-treating cycle can be quite significant. Thus, if it were possible to ensure that the heat-treating cycle did not exceed the time duration of primary creep, then one could rationalize that the time spent in heat treatment would not significantly compromise the functional structural integrity of the component. Clearly, the key to this approach is to have an estimate of the time

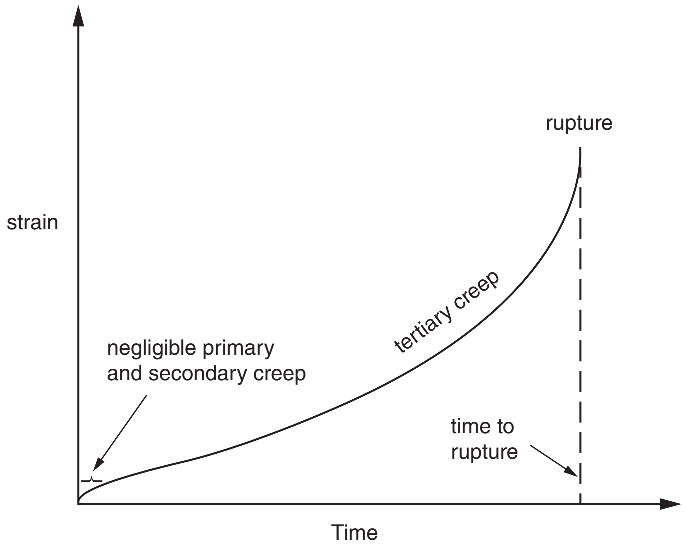

Figure 1.8 Material with negligible primary and secondary creep regimes.

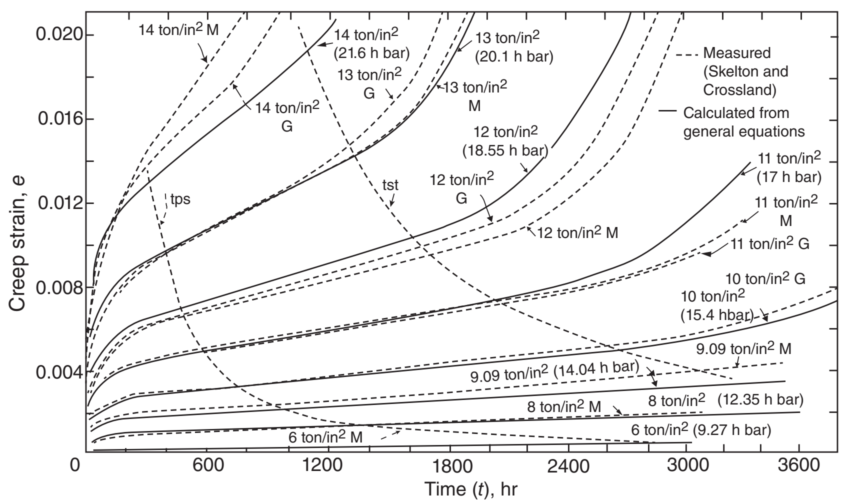

Figure 1.9 Measured and calculated tensile creep curves – primary creep duration Smith and Nicolson (1971).

to the point at which primary creep ends and secondary begins. To obtain a general idea of the relevant time duration of primary creep, there is an evaluation by Larke and Parker in a volume edited by Smith and Nicolson (1971) where they have plotted creep data and analytical correlations for a 0.19% carbon steel at 842°F (450°C). In Figure 1.9, it can be seen that the duration of

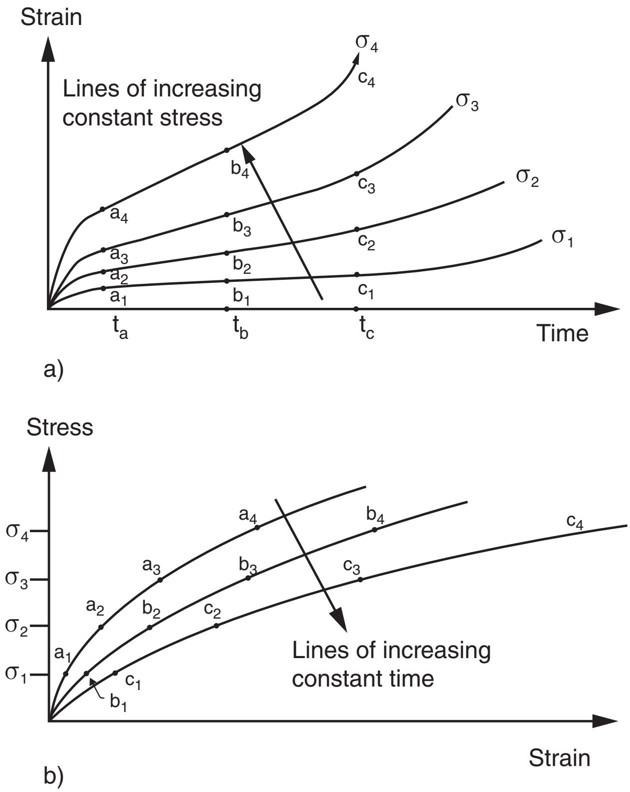

Figure 1.10 (a) A family of creep curves conventionally plotted as strain vs. time at constant stress. (b) The resultant stress-strain curves, plotted as stress vs. strain at constant time.

primary creep depends on the stress level, varying from 300 to about 1200 h, indicating that a total cycle time of 150–200 h should be acceptable.

Another means of characterizing creep is to plot "isochronous" stress-strain curves. Outwardly, these curves resemble conventional stress-strain curves except that the strain on the abscissa is the strain that would be developed in a given time by the stress given on the ordinate, as shown in Figure 1.10. These stress-strain values are usually plotted as a family of curves, each for a constant time, as shown in Figure 1.11 for 316 stainless steel at 1200°F (649°C). Although, conceptually, these curves could be directly plotted from data, the curves are usually generated from creep laws which are in turn derived from experimental data and correlate stress, strain, and time at a constant temperature. These curves can be very useful when designing elevated-temperature

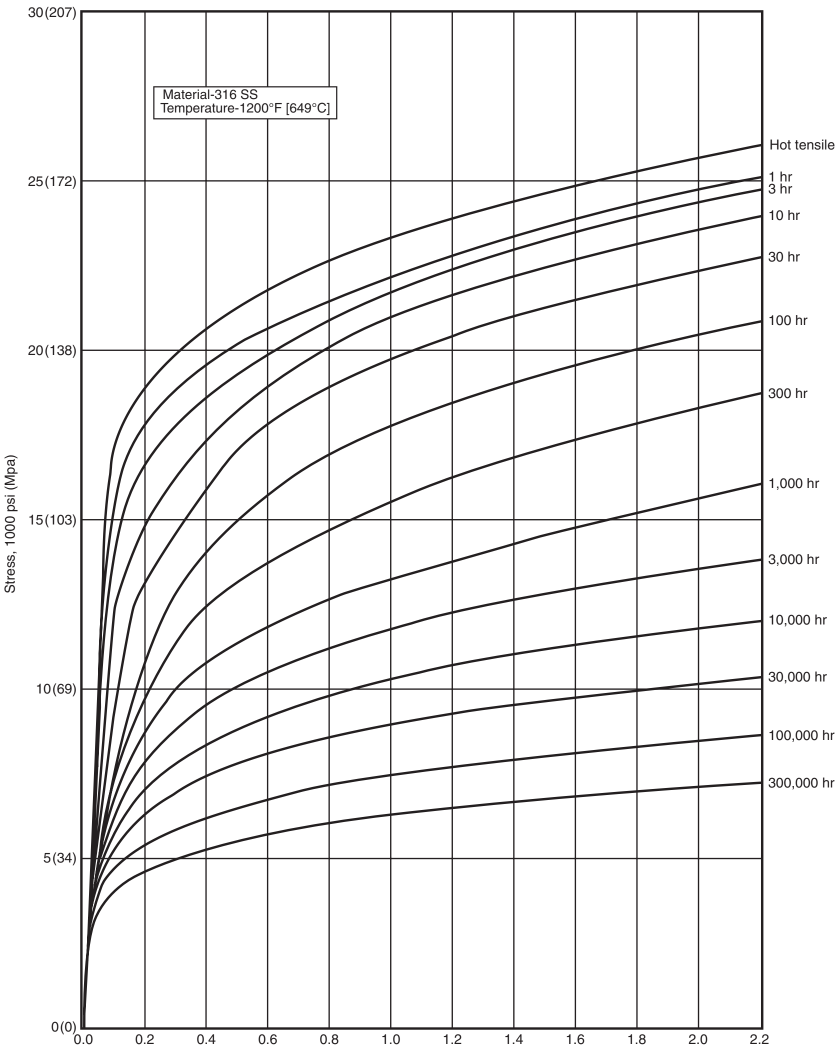

Figure 1.11 Isochronous stress-strain curves [ASME II-D].

structures where they can be used similarly to a conventional stress-strain curve in some situations; i.e., evaluating buckling and instability, and as a means of approximating accumulated strain. The equations for Figure 1.11's isochronous curves are shown in Appendix B.

Example 1.1 The effective stress in a pressure vessel component is 8000 psi. The material and temperature are shown in Figure 1.11. What is the expected design life of the component if:

(a) A strain limit of 0.5% is allowed?
(b) A strain limit of 1.0% is allowed?

Solution
(a) In Figure 1.11, the expected life is 15,000 h.
(b) In Figure 1.11, the expected life is 65,000 h.

1.3 Allowable Stress

1.3.1 ASME Boiler and Pressure Vessel Code

The ASME Boiler and Pressure Vessel Code lists numerous materials that meet the ASTM, as well as other European and Asian specifications. It provides allowable stresses for the various sections of the Code for temperatures below the creep range and at temperatures where creep is significant. For non-nuclear applications, by far the most common, these allowable stress levels are provided as a function of temperature in ASME II-D.

For ASME I and VIII-1 applications, the allowable stress criteria are given in Appendix 1 of ASME II-D. The allowable stress at elevated temperature is the

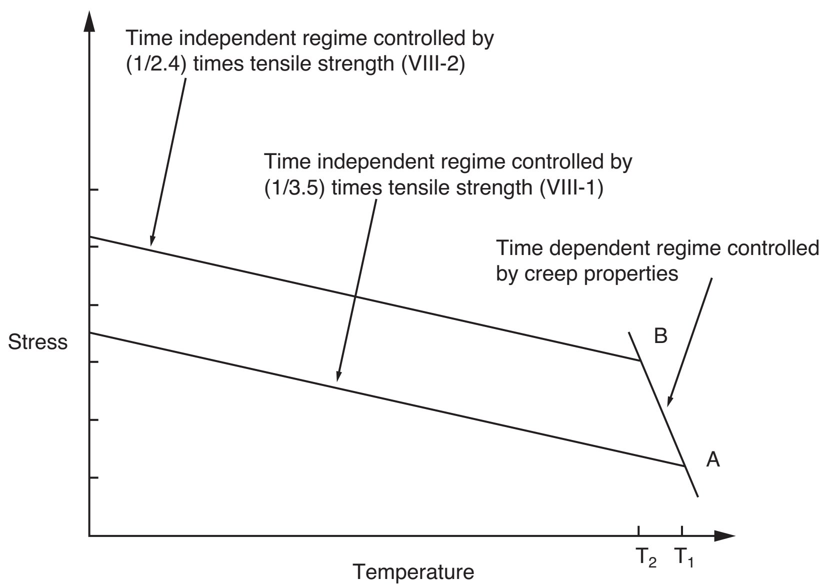

Figure 1.12 Shift of time-dependent stress with factor of safety.

lesser of: (1) the allowable stress given by the criteria based on yield and ultimate strength, (2) 67% of the average stress to cause rupture in 100,000 h, (3) 80% of the minimum stress to cause rupture in 100,000 h, and (4) 100% of the stress to cause a minimum creep rate of 0.01%/1000 h. Above 1500°F, however, the factor on average stress to rupture is adjusted to provide the same time margin on stress to rupture as existed at 1500°F (815°C). Although the allowable stress is a function of the creep rupture strength at 100,000 h, this is not intended to imply that there is a specified design life for these applications. There are additional criteria for welded pipes and tubes that are 85% of the above values. A very large number of materials are covered in these tables.

Unlike previous editions, the 2007 edition of ASME VIII-2, and subsequent editions, cover temperatures in the creep regime. The time-dependent allowable stress criteria for ASME VIII-2 are the same as for ASME VIII-1. However, because the time-independent criteria are less conservative (tensile strength divided by a factor of 2.4 vs. 3.5), the temperature at which the allowable stress is governed by time-dependent properties is lower in ASME VIII-2 than ASME VIII-1, as shown in Figure 1.12.

The allowable stress criteria for components of Class A nuclear systems, covered by ASME III-5, are different than for non-nuclear components. For nuclear components, the allowable stress at operating conditions for a particular material is a function of the load duration and is the lesser of: (1) the allowable stress for Class A nuclear systems based on yield and ultimate strength; (2) 67% of the minimum stress to rupture over time T; (3) 80% of the minimum stress to cause initiation of third-stage creep over time T; and (4) 100% of the average stress to cause a total (elastic, plastic, and creep) strain of 1% over time T. Note that these allowable stress criteria are more conservative than for non-nuclear systems for the same 100,000h reference time. However, these allowable stresses apply to operating loads and temperatures (Service Conditions in ASME III terminology) that are, in general, not defined as conservatively as the Design Conditions for non-nuclear applications. There are additional criteria for allowable stresses at welds and their heat-affected zones. All these allowable stresses are given in ASME III-5 for a quite limited number of materials.

The allowable stresses for Class B elevated-temperature nuclear systems are in general similar to those for non-nuclear systems, and are provided in ASME III-5.

ASME III-NB covers Class 1 nuclear components in the temperature range where creep effects do not need to be considered. Specifically, ASME III-NB is limited to temperatures for which applicable allowable stress values are provided in ASME II-D. These temperature limits are 700°F (370°C) for ferritic steels and 800°F (425°C) for austenitic steels and nickel-based alloys.

Unlike ASME I and VIII-1 components, the design procedures for nuclear components, particularly Class A are significantly different at elevated temperatures compared to the requirements for nuclear components below the creep regime. This is due in part to the time-dependence of allowable stresses, but more significantly to the influence of creep on cyclic life. As compared to ASME I and VIII-1 components, ASME III-5 explicitly considers cyclic failure modes at elevated temperature, whereas the former do not. ASME VIII-3 does address cyclic failure modes below the creep range. The provisions of ASME III-5, particularly with respect to VIII-2 are discussed in greater detail in Chapter 8.

ASME VIII-2 addresses cyclic failure modes and, as previously noted, currently covers temperatures in the creep regime above the previous limits of 700°F (370°C) and 800°F (425°C) for ferritic and austenitic materials, respectively. ASME VIII-2 also stipulates either meeting the requirements for exemption from fatigue analysis, or, if that requirement is not satisfied, meeting the requirements for fatigue analysis. However, above the 700/800°F (370/425°C) limit, the only available option is to satisfy the exemption from fatigue analysis requirements because the fatigue curves required for a full fatigue analysis are limited to 700°F and 800°F (370°C and 425°C).

1.3.2 European Standard EN 13445

EN 13445 applies to unfired pressure vessels. It is analogous to ASME VIII-1 and -2 in that it covers both Design by Formula (DBF), similarly to ASME VIII-1, and Design by Analysis (DBA), similarly to ASME VIII-2. It is unlike ASME VIII in several important respects. First, the EN 13445 allowable stresses are time-dependent, as in ASME III-5. They are also a function of whether there is in-service monitoring of compliance with design conditions. Provisions are also made for weld strength reduction factors, as in ASME III-5. Unlike the DBF rules in ASME VIII-1, those in the EN code are only applicable when the number of full pressure cycles is limited to 500.

The basic allowable stress parameters in EN 13445 in the creep range are: the mean creep rupture strength in time, t, and the mean stress to cause a creep strain of 1% in time, t. For DBF rules, the safety factor applied to the mean creep rupture stress is 1/1.5 if there is no in-service monitoring, and 1/1.25 if there is. There is no safety factor on the 1% strain criteria. If there is in-service monitoring then the strain limit does not apply, but strain monitoring is required. Thus, for a design life of 100,000 h in the EN code, without in-service monitoring, the base metal design allowable stress will be the same as in ASME VIII-1, which is to say, the allowable stresses are governed by creep rupture strength (remembering that ASME VIII-1 allowable stresses are based on 100,000h properties, even though there is no specified design life in ASME VIII-1).

There are two DBA methodologies defined in the EN 13445, the “Direct Route” and the “Method based on stress categories.” Conceptually, the stress category methodology is similar to the methodology defined in ASME VIII-2 and III-NB for temperatures below the creep range and in ASME III-5 for elevated temperatures; however, there are many differences in the details of their application. The basic allowable stresses for the stress category DBA methodology are the same as for the DBF rules, and are dependent on whether there is in-service monitoring. The "Direct Route" is based on limit analysis and reference stress concepts. It is quite complex. Indeed, there is a warning in the introduction cautioning that, "Due to the advanced methods applied, until sufficient in-house experience can be demonstrated, the involvement of an independent body, appropriately qualified in the field of DBA, in the assessment of the design (calculations)." On that basis, a detailed discussion of the "Direct Route" DBA rules in EN 13445 will be considered beyond the scope of this presentation; however, there is a further discussion of the reference stress concept in Section 1.5.3.2.

Example 1.2 Figure 1.13 shows a representative plot of creep rupture data with extrapolation to 100,000 h. Figure 1.14 shows a plot of creep strength

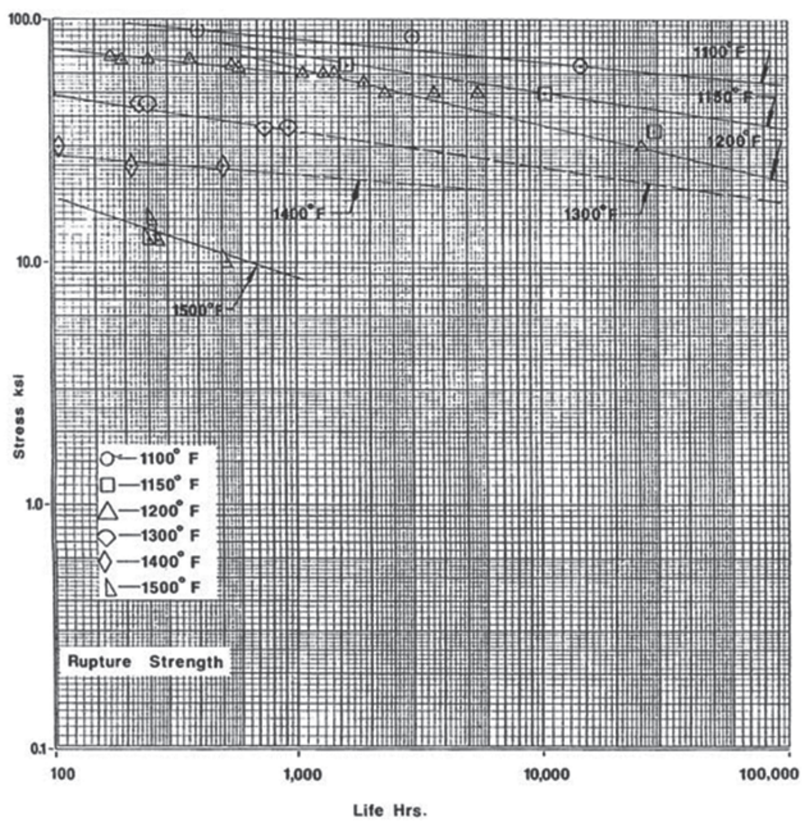

Figure 1.13 Rupture strength Jawad and Farr (2019).

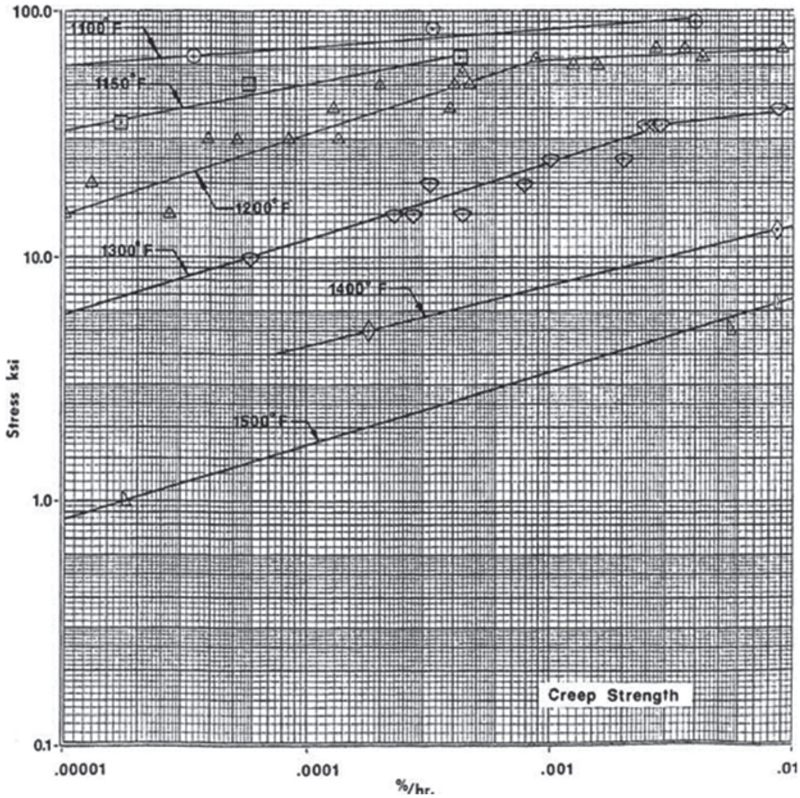

Figure 1.14 Creep strength Jawad and Farr (2019).

(minimum creep rate) for the same material. Curve fitting procedures are usually used for the extrapolation. Based on the creep properties shown in Figures 1.13 and 1.14, calculate the allowable stress at 1200°F for an ASME VIII-1 application. (Note: This is for a non-ASME Code application. The Code has published values for Code applications.) Compare the results to the allowable stress for an EN 13445 application with 100,000 h design life and with in-service monitoring. (Note: Under EN 13445, allowable stress values are established by the user based on published properties as described therein.)

Solution

In Figure 1.13, the average stress to rupture in 100,000 h is 22 ksi and the allowable stress for ASME VIII-1, based on average creep rupture, is $22 \times (0.67) = 14.7$ ksi assuming a minimum value of creep rupture based on a 20% scatter band gives a minimum creep rupture strength of 17.6 ksi, and an allowable stress based on minimum creep rupture of $17.6 \times (0.8) = 14.1$ ksi. In Figure 1.14, the stress for a minimum creep rate of 0.01% in 1000 h is 15 ksi, which gives an allowable stress of 15 ksi. Therefore, the applicable stress for an ASME VIII-1 application is 14.1 ksi governed by the minimum creep rupture strength.

For EN 13445 applications with in-service monitoring, the safety factor is 1.25 on mean creep rupture strength (assumed equal to average strength plotted in Figure 1.13), so the allowable stress for a design life of 100,000 h is 22/(1.25) = 17.6 ksi.

1.4 Creep Properties

1.4.1 ASME Code Methodology

One of the issues facing the designer of elevated temperature components is how to extrapolate limited time duration test data to the service lives representative of most design applications or, in the case of developing allowable stresses for ASME Code applications, 100,000 h. The method for the development of ASME Code elevated temperature allowable stresses for non-nuclear applications is described in Chapter 3 ("Basis for Tensile and Yield Strength Values") of *the Companion Guide to the ASME Boiler & Pressure Vessel Code* (Jetter 2018). Quoting from that source:

> At the elevated temperature range in which the tensile properties become time-dependent, the data is analyzed to determine the stress to cause a secondary creep rate of 0.01% in 1000 h and the stress needed to produce rupture in 100,000 h. This data must be from material that is representative of the product specification, requirements for melting practice, chemical composition, heat treatment, and product form. The data is plotted on log-log coordinates at various temperatures. The 0.01%/1000 h creep stress and the 100,000 h rupture stress are determined from such curves by extrapolation at the various temperatures of interest. The values are then plotted on semi log coordinates to show the variation with temperature. The minimum trend curve defines the lower bound for 95% of the data.

As part of the ASME Code methodology, data for development of allowable stress values is required for long times, usually at least 10,000 h for some data, and at temperatures above the range of interest, usually 100°F (56°C) higher. Considerable judgment is exercised in the development of ASME Code allowable stress values and the use of these values is required for ASME Code-stamped construction. As a corollary, if the material of interest is not listed in the Code for the applicable type of construction, or at the desired temperature, then it is not possible to qualify the component for a Code stamp. The designer may, however, use this method for non-Code applications.

A somewhat different approach is taken in EN 13445. There, the mean creep rupture strength and mean stress for a 1% strain limit are listed in referenced standards for approved materials for various times and temperatures. The requirements for extrapolation or interpolation to other conditions are defined and the safety factors to be applied are defined as a function of the application, as described above.

1.4.2 Larson-Miller Parameter

As might be expected, there are numerous methods Conway (1969) for extrapolation of creep data; the ASME procedure described above is the one used for establishment of allowable stresses shown in ASME II-D. Generally, the use of other extrapolation techniques would only be required for non-coded construction, or for evaluation of failure modes beyond the scope of the applicable code. Penny and Marriott (1995) provide an extensive assessment of various extrapolation techniques, including the widely used Larson-Miller parameter, which they characterize as simple and convenient, but not particularly accurate.

The starting point for development of the Larson-Miller Grant and Mullendore (1965) parameter is to assume that creep is a rate process governed by the Arrhenius equation

$$d\varepsilon_c \,/\, dt \;=\; Ae^{(-Q/RT)} \tag{1.1}$$

where

A = constant
Q = activation energy for the creep process, assumed a function of stress only
R = universal gas constant
T = absolute temperature (460 +°F)
t = time.

Noting that from the Monkman-Grant relationship the time to rupture, t_r, multiplied by the minimum creep rate, can be assumed to be constant, Eq. (1.1) can be rewritten as

$$AT_r e^{(-Q/R\tau)} = \text{constant}.$$

Taking the logarithms for each side, the Larson-Miller parameter, P_{LM}, can be expressed as

$$P_{\mathrm{LM}} = T(\,C + \log_{10} t\,) \tag{1.2}$$

where P_{LM} is a function of time and temperature and independent of stress. It was also assumed that C is independent of both stress and temperature and is a function of material only. Experimental data shows that the range of C for various materials is between 15 and 27. Most steels have a C value of 20. Hence, Eq. (1.2) can be expressed as

$$P_{LM} = (460 + {}^\circ F)(20 + \log_{10} t). \tag{1.3}$$

The Larson-Miller parameter is also used to correlate creep data using specific values of C that are material and temperature range-dependent, thus minimizing some of the uncertainties. Further details of the Larson-Miller parameter are given in Chapters 2 and 6.

Another important use of the Larson-Miller parameter is determination of equivalent time at temperature, as shown by the following examples.

Example 1.3 A pressure vessel component was designed at 1200°F with a life expectancy of 100,000 h. What is the expected life if the design were lowered to 1175°F?

Solution

The Larson-Miller parameter for the original design condition is obtained from Eq. (1.3) as

$$\begin{aligned} P_{LM} &= (460 + 1200)(20 + \log_{10} 100{,}000). \\ &= 41{,}500 \end{aligned}$$

Using this value for the new design condition yields

$$41{,}500 = (460 + 1175)(20 + \log_{10} t)$$

or

$$\begin{aligned} \log_{10} t &= 5.382 \\ t &= 241{,}100 \text{ h} \end{aligned}$$

which indicates a 2.4-fold increase in the life of the component when the temperature drops 25°F.

Example 1.4 A pressure vessel shell is constructed of 2.25Cr-1Mo steel. The thickness is 4 in. and requires post-weld heat treating at 1300°F for 4 h. The fabricator requires two separate post-weld heat treatments (8 h) and the user needs three more (12 h) for future repair. Hence, a total of 20 h is needed. The material supplier furnishes the steel plates with material properties guaranteed for a minimum of 20 h of post-weld heat treating. During the manufacturer's second post-weld heat treatment, the temperature spiked to 1325°F for 2 h. How many hours are left for the user?

Solution

Calculate P_{LM} from Eq. (1.3) for 2 h at 1325°F.

$$\begin{aligned} P_{LM} &= (460 + 1325)(20 + \log_{10} 2) \\ &= 36{,}237 \end{aligned}.$$

Substitute back into Eq. (1.3) to calculate the equivalent time for 1300°F.

$$\begin{aligned} 36{,}237 &= \left(460 + 1300\right)\left(20 + \log_{10} t\right) \\ t &= 3.9 \text{ h} \end{aligned}.$$

Thus, the fabricator used a total of 4.0 + (2.0 + 3.9) = 9.9 h.

Available hours for the user = 20 − 9.9 = 10.1 h. This corresponds to two full and one partial post-weld heat treatment.

1.4.3 Omega Method

The Omega Method Prager (2000) is based on a different model for creep behavior than that described above for the Larson-Miller parameter. Originally developed to address the issue of determining the accumulated damage, and thus the remaining life of service-exposed equipment, the Omega Method is based on the observation that, at design stress levels, both the primary creep and secondary creep phases are of relatively short duration, with small strain accumulation, and that most of the component life is spent in the third stage, where the strain rate increases with time and accumulated strain. In the Omega Method, the creep strain rate is accelerated in accordance with the following relationship:

$$\ln(\mathrm{d}\varepsilon / \mathrm{d}t) = \ln(\mathrm{d}\varepsilon_o / \mathrm{d}t) + \Omega_p \varepsilon \tag{1.4}$$

where

$(\mathrm{d}\varepsilon/\mathrm{d}t)$ and $(\mathrm{d}\varepsilon_0/\mathrm{d}t)$ = current and initial strain rates, respectively

Ω_p = Omega parameter

ε = current strain level.

From this relationship, various parameters relating to accumulated damage and remaining life may be developed. The Omega Method has been incorporated into ASME FFS-1 for remaining life assessments. Chapter 7 discusses this method in more detail.

1.4.4 Negligible Creep Criteria

Another issue of interest is the temperature at which creep becomes significant. To answer this quantitatively, the key point is – significant compared to what?

There are no single, rigorous criteria for assessing when creep effects are negligible. However, in each of the design codes of interest, the criteria for negligible creep applicable to that particular design code are defined.

For ASME I and VIII-1, the comparison is between the results provided by the allowable stress criteria based on short-time tensile tests without creep as compared to long-term tests with creep. When the allowable stress as a function of temperature is governed by creep properties, the stress value is italicized in ASME II-D, Table 1. However, in this case, even though the allowable stress is governed by creep properties, the design evaluation procedures do not change.

The situation is different with ASME III-5. In ASME III-5, there are two sets of allowable stress for primary (load-controlled) stresses to be used in the evaluation of Service Conditions. One set, S_m, is time-independent and a function of short-time tensile tests. The other set, S_t, is time-dependent and a function of creep. As will be discussed in more detail later, the design rules for time-independent and time-dependent allowable stress levels are different. However, the rules for displacement-controlled stress, such as thermally induced stress, state that the criteria for negligible creep are the most restrictive.

The ASME III-5 criteria for negligible creep for displacement-controlled stresses are based on the idea that, under maximum stress conditions, creep effects should not compromise the design rules for strain limits or creep-fatigue damage. The key consideration from that perspective is that actual stress in a localized area can be much greater due to discontinuities, stress concentrations, and thermal stress than the wall-averaged primary stresses in equilibrium with external loads. Basically, the magnitude of the localized stress will be limited by the material's actual yield stress because it is at this stress level that the material will deform to accommodate higher stresses due to structural discontinuities or thermal gradients. Thus, the objective of the negligible creep criteria for localized stresses is to ensure that the damage due to the effects of creep at the material's yield strength will not significantly impact the design rules for the failure mode of concern. For example, there are two resulting criteria, one based on negligible creep damage and the other on negligible strain. Negligible creep damage can be approximated by:

$$\sum(t_i \,/\, t_{id}) \leq 0.1 \tag{1.5}$$

where

t_i = the time duration at high temperature

t_{id} = time duration at a stress level as defined in III-5 and VIII-2 but nominally 1.5 times the yield stress, S_y, except for 9Cr-1Mo-V steel.

For negligible strain, the criteria are given by

$$\sum \varepsilon_i \leq 0.2\% \tag{1.6}$$

where

ε_i = the creep strain at a stress of 1.25 times yield strength, S_y.

In ASME III-5 Part HCB, which provides elevated temperature design rules for Class B nuclear components, Appendix HCB-III contains a figure that shows time temperature limits below which creep effects need not be considered in evaluating deformation-controlled limits. These curves are lower, smoothed versions of the ASME III-5 criteria for negligible creep for a limited number of materials: cast and wrought 304 and 316 stainless steel, nickel-based Alloy 800H, low alloy steel, and carbon steel. The advantage of these curves is that no computations are required.

The French code for elevated temperature nuclear components, RCC-MR, also provides criteria for negligible creep, which is somewhat different than that in ASME III-5. The procedures are more involved than those in ASME III-5, but the resulting values for long-term service are similar to the temperature limits of ASME III-NB – 700°F (371°C) for ferritic and 800°F (427°C) for austenitic and nickel-based alloys. For 316L(N) stainless steel, whose creep properties are fairly close to 316 stainless steel, the time–temperature limit curve is generally in agreement with the curve shown in ASME III-5 Part HCB for 316 stainless steel.

1.4.5 Environmental Effects

As stated in its Foreword, the ASME Boiler and Pressure Vessel Code does not specifically address environmental effects. However, non-mandatory general guidance is provided in several sections. ASME II-D, Appendix A provides guidance on metallurgical effects, including a number of references on corrosion and stress-corrosion cracking. ASME VIII-1, Appendix E suggests good practice for determining corrosion allowances, which are the responsibility of the user to specify based on the equipment's intended service. It is noted that the corrosion allowance is in addition to the minimum required thickness. ASME III, Appendix W, has a comprehensive discussion of environmental effects. Included for each phenomenon is a discussion of the mechanism, materials, design, mitigating actions, and references.

In the context of elevated temperature applications, the designer should be particularly aware of environments that can reduce a material's creep rupture life and/or ductility. For example, it has been shown that short-term exposure to oxygen at temperatures exceeding 1650°F (900°C) could lead to embrittlement at intermediate temperatures of 1300°F–1500°F (705°C–815°C), which was attributed to intergranular diffusion of oxygen. Hydrogen, chlorine, and sulfur may also cause embrittlement due to penetration. Sulfur is of particular concern because it diffuses more rapidly and embrittles more severely than oxygen.

1.4.6 Monkman-Grant Strain

Another parameter of interest is the strain computed by multiplying the time to rupture by the secondary creep rate. This strain parameter, shown diagrammatically in Figure 1.7, is sometimes known as the Monkman-Grant strain. As discussed by Penny and Marriott (1995), this computed strain has been shown to be useful in correlating rupture under variable loading conditions. A corollary of this approach is that it implies that the primary creep strain may be disregarded in assessing damage accumulation.

It has also been suggested that a relevant measure of creep ductility for the application of reference stress methods (Section 1.5.3.2) in the presence of local stress discontinuities is for the material of interest to show a ratio of total strain at failure to the Monkman-Grant strain of at least 5:1.

1.5 Required Pressure-Retaining Wall Thickness

There are basically two approaches in general use in design for determining the wall thickness required to resist internal pressure and applied external loads. The first is usually referred to as Design by Rule, or Design by Formula (DBF in the European Standard terminology), and the second is Design by Analysis (DBA). As an alternative to DBA, there are other approaches based on experimental methods; however, those methods are generally not applicable in the creep regime. In addition to the above approaches, there are many pressure-retaining components that have standardized allowable pressure ratings as a function of design temperature. Typically, these include flanges, piping components, and valve bodies. In general, these pressure/temperature ratings do not include the effects of loadings other than internal pressure. The following discussion will provide an overview of these methodologies; the specific requirements for their implementation will be discussed in later chapters.

1.5.1 Design by Rule

In this approach, formulas are provided for the required thickness as a function of the design pressure, allowable stress, and applicable parameters defining the geometry of interest. Numerous diagrams are provided to define the requirements for specific configurations; for example, reinforcement of openings, head-to-cylinder joints, and weldments. This is the approach used, for example, in ASME VIII-1 “Unfired Pressure Vessels,” and ASME I “Power Boilers.”

1.5.2 Design by Analysis

In the DBA approach, stress levels are determined at various critical locations in the structure and compared to allowable stress levels, which are a function of the applied loading conditions and failure mode under consideration. The most commonly used methodology, particularly at elevated temperatures, is based on elastically calculated stresses, which are sequentially categorized based on the relevant failure mode. Primary stresses (those that normally determine wall thickness) are first determined by separating the structure into simpler segments (free bodies) in equilibrium with external loads. Next, secondary and peak stresses (which in combination with primary stresses normally determine cyclic life) are determined from stresses at structural discontinuities and induced thermal stresses. Different allowable stresses are assigned to the different stress categories based on the failure mode of concern.

1.5.3 Approximate Methods

There is another category of Design-by-Analysis methodologies that are approximate in the sense that they approximate the "true" time-dependent stress and strain history in a component. In fact, considering the variations in creep behavior and difficulties encountered in defining comprehensive models of material behavior, they can be quite useful under appropriate circumstances. Two main approaches will be described. The first is the elastic analog or stationary creep solution and the second is the reference stress approach, which is somewhat analogous to limit analysis.

1.5.3.1 Stationary Creep – Elastic Analog

Subject to certain restrictions on representation of creep behavior, a structure subjected to a constant load will reach a condition where the stress distribution does not change with time, thus the term "stationary creep." The fundamental restriction on material representation is that the creep strain is the product of independent functions of stress and time. Conceptually, stationary creep is valid when the strains and strain rates due to creep are large compared to elastic strains and strain rates.

If the structure is statically determinate throughout, then the initial stress distribution will not change with time; subject to the applicability of small displacement theory, which applies to the large majority of practical design problems. Examples would be a single bar with a constant tension load and the stresses in the wall of a thin-walled cylinder, remote from discontinuities, subjected to a constant internal pressure.

However, it is with indeterminate structures that the stationary creep concept is of most value. It has been shown that, in a structure with redundant load paths or subject to local redistribution (i.e., a beam in bending), the stress redistribution will take place relatively quickly: on the order of the time it takes for the creep strain to equal twice the initial elastic strain. For a set of variables representative of pressure vessels in current use, Penny and Marriott (1995) calculated an effective redistribution time of about 100 h. Although this would be a long time if the vessel were subject to significant daily cycles, it is short compared to the long times of extended operation.

A number of investigators have shown that, because the stress distribution in stationary creep does not vary with time (and thus corresponding creep rates are constant), the stationary creep stress distribution is analogous to non-linear elastic stress distribution, so solutions to the creep problem can be obtained from solutions to the non-linear elastic stress distribution problem. This is usually referred to as the "elastic analog." Although the elastic analog has been shown as valid in more general terms, a more convenient representation is analogous to a simple power law representation of steady, secondary creep in which primary creep is considered negligible

$$\mathrm{d}\varepsilon / \mathrm{d}t = k'\sigma^n, \tag{1.7}$$

which results in the following expression for accumulated creep strain:

$$\varepsilon = K'\Delta t\sigma^n. \tag{1.8}$$

This is analogous to the equation for non-linear elasticity

$$\varepsilon = K'\sigma^n. \tag{1.9}$$

An example of stationary creep solutions for various values of the power law exponent, n, is shown in Figure 1.15. This is the non-dimensional stationary creep solution for a beam in bending with a constant applied moment. Note that for $n = 1$ the stress distribution is elastic, and for $n \to \infty$ the distribution corresponds to that for the assumption of ideal plasticity. All the distributions pass through a point partway through the wall, which is referred to as the "skeletal point." The reduction in steady creep stress, as compared to the initial elastic distribution, is the basis for the reduction of the elastically calculated bending stress by a section factor when comparing the calculated stresses to allowable stress levels in ASME III-5. Further description of the elastic analog is given in Chapters 2 and 4.

1.5.3.2 Reference Stress

The initial idea of a reference stress is that the creep behavior of a structure could be evaluated against a single creep test at its reference stress. Initially applied to problems of creep deformation, there were a number of analytical solutions

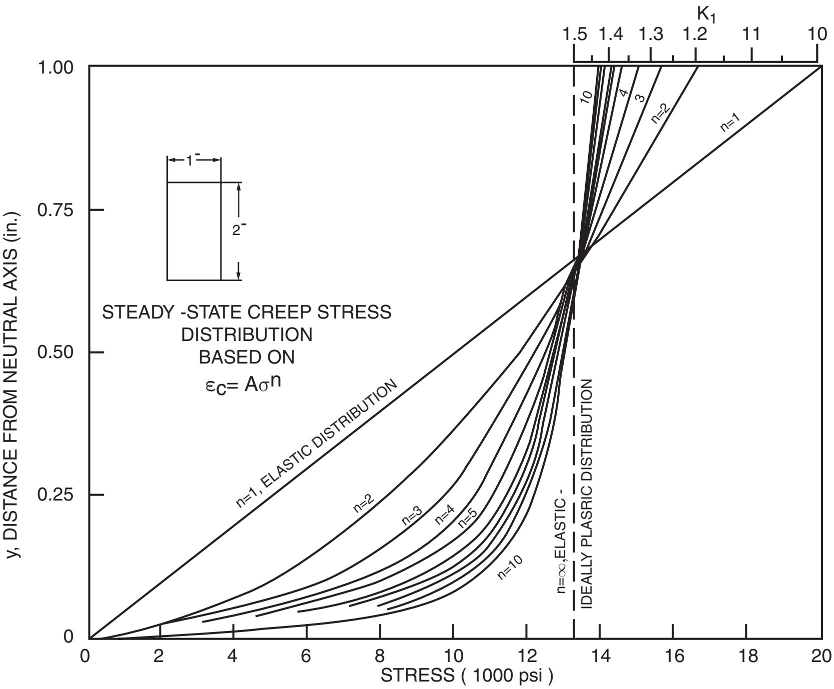

Figure 1.15 Steady-state creep stress distribution across a rectangular beam in pure bending and having a steady-state creep law of the form $\varepsilon_c = A\sigma^n$ [Oak Ridge National Laboratory].

developed for specific geometries. However, Sim (1968), noting that reference stress is independent of the creep exponent, and also that the solution for an infinite creep exponent is analogous to a limit solution which corresponds to ideal plasticity, proposed that the reference stress could be conservatively obtained from

$$\sigma_{R=}(P/P_L)\sigma_y \tag{1.10}$$

where

P = load on the structure

P_L = limit value of the load

σ_R = reference stress

σ_y = the yield stress.

There have been numerous comparisons between the results of this approach and experimental and rigorous analyses of the same component or test article. In general, the results are quite favorable. Although the reference stress approach has not been incorporated into the ASME Boiler and Pressure Vessel Code, it has been used in the British elevated temperature design code for nuclear systems, R5, and in the recent European Standard EN 13445. However, the British standard recommends an adjusted reference stress for design given by a factor of 1.2 times the reference stress, as in Sim's relationship above. Also, as previously noted, the EN standard cautions against the use of the reference stress method by those not familiar with its application.

Part of the reason for this concern is inherent in the basis for both limit loads and reference stress determination. Both are based on structural instability considerations and not local damage. As such, there is an inherent requirement that the material under consideration be sufficiently ductile. This is easier to achieve at temperatures below the creep range. Within the creep range, ductility decreases, particularly at the lower stress levels associated with design conditions. There have been some studies to more specifically identify creep ductility requirements, but current thinking would put it in the range of 5%–10% for balanced structures which don't have extreme strain-concentrating mechanisms.

The following example highlights the differences between an elastically calculated stress distribution, a steady stationary creep stress distribution, and the reference stress distribution.

Example 1.5 Consider the two-bar model shown in Figure 1.16. As explained in Chapter 2, this is actually representative of the way in which cyclone separators are sometimes hung from vessels. For this example, the two, parallel, uniaxial bars are of equal area, A, unequal lengths L_1 and L_2, and attached to a rigid boss constrained to move in the vertical direction only. The assembly is loaded with a constant force F. Compare the (1) initial elastic stress, (2) stationary stress, and (3) reference stress in each bar. Assume that creep is modeled with a power law with exponent $n = 3$. Consider two cases. In the first case, $L_1 = L_2/8$ and in the second case, $L_1 = L_2/2$.

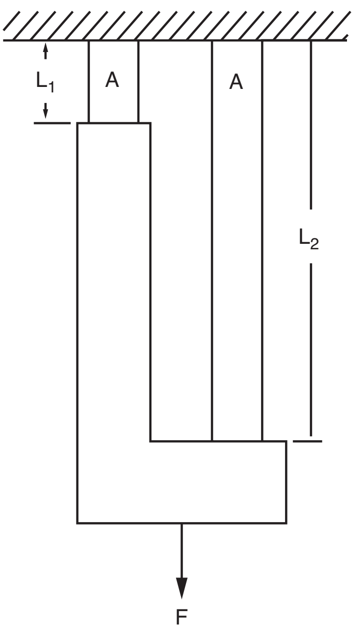

Figure 1.16 Two-bar model with constant load.

Solution

(1) Elastic Analysis

The initial elastic distribution can be expressed as

$$\varepsilon_1(0) = \sigma_1(0)/E \text{ and } \varepsilon_2(0) = \sigma_2(0)/E \quad \text{(a)}$$

where

$\varepsilon_1(0)$, $\sigma_1(0)$ and $\varepsilon_2(0)$, $\sigma_2(0)$ = initial strain and stress in bars 1 and 2, respectively
E = modulus of elasticity.

From equilibrium

$$F = A\left(\sigma_1 + \sigma_2\right). \tag{b}$$

From displacement compatibility

$$L_1\varepsilon_1(0) = L_2\varepsilon_2(0). \tag{c}$$

Substituting Eq. (a) into Eq. (c) gives

$$L_1\sigma_1 = L_2\sigma_2 \text{ or } \sigma_2 = (L_1 / L_2)\sigma_1. \tag{d}$$

Substituting Eq. (d) into Eq. (b) gives

$$F = A\left[\sigma_1 + \left(L_1 / L_2\right)\sigma_1\right]. \tag{e}$$

From which, solving for the initial elastic stress distribution gives

$$\sigma_1\left(0\right) = \left[L_2 / \left(L_1 + L_2\right)\right]F / A \tag{f}$$

$$\sigma_2\left(0\right) = \left[L_1 / \left(L_1 + L_2\right)\right]F / A. \tag{g}$$

(2) Stationary Stress Analysis
The solution for the stationary stress distribution in each bar proceeds in a similar fashion: the equilibrium equation (a) remains the same; the strain rate at any time can be expressed as the sum of elastic and creep strain rates

$$\mathrm{d}\varepsilon_1 / \mathrm{d}t = k'\sigma_1^n + \left(\mathrm{d}\sigma_1 / \mathrm{d}t\right) / E \quad \text{and} \quad \mathrm{d}\varepsilon_2 / \mathrm{d}t = k'\sigma_2^n + \left(\mathrm{d}\sigma_2 / \mathrm{d}t\right) / E. \tag{h}$$

From displacement rate compatibility

$$L_1\mathrm{d}\varepsilon_1 / \mathrm{d}t = L_2\mathrm{d}\varepsilon_2 / \mathrm{d}t. \tag{i}$$

Using the above relationships, an equation can be developed involving functions of σ_1; but, as noted by Kraus (1980), it cannot be solved in closed form. However, we are interested in the stationary creep solution, where $\mathrm{d}\sigma/\mathrm{d}t \to 0$ and $(\mathrm{d}\sigma/\mathrm{d}t)/E < K\sigma^{\mathrm{n}}$. Thus, Eq. (h) in the stationary creep regime becomes

$$\mathrm{d}\varepsilon_1\left(\infty\right) / \mathrm{d}t = k'\sigma_1\left(\infty\right)^n \quad \text{and} \quad \mathrm{d}\varepsilon_2\left(\infty\right) / \mathrm{d}t = k'\sigma_2\left(\infty\right)^n. \tag{j}$$

Substituting Eq. (j) into Eq. (i) provides the following relationship for $\sigma_2(\infty)$

$$\sigma_2\left(\infty\right) = (L_1^{1/n} / L_2^{1/n})\sigma_1(\infty). \tag{k}$$

From the above, and the equilibrium equation (b), the following expressions for stationary stress distribution may be obtained

$$\sigma_1\left(\infty\right) = \left[\left(L_2^{1/n}\right) / \left(L_1^{1/n} + L_2^{1/n}\right)\right] F / A \tag{l}$$

$$\sigma_2\left(\infty\right) = \left[\left(L_1^{1/n}\right) / \left(L_1^{1/n} + L_2^{1/n}\right)\right] F / A. \tag{m}$$

(Note that, from the elastic analog, the initial elastic stress distribution corresponds to the steady creep solution with $n = 1$.)

(3) Reference Stress Analysis

The reference stress is obtained from Eq. (1.10), noting that the limit load in each bar is equal to $A\sigma_y$

$$\sigma_1\left(R\right) = \sigma_2\left(R\right) = F / \left(2A\right). \tag{n}$$

(Note that a similar result is obtained by letting n → ∞ in the stationary creep stress solution, as predicted by the Sim hypothesis.)

The following results are obtained for Case #1, where $L_1 = L_2/8$:

Initial elastic stress: $\sigma_1(0) = (8/9)F/A$, $\sigma_2(0) = (1/9)F/A$
Stationary creep stress: $\sigma_1(\infty) = (2/3)F/A$, $\sigma_2(\infty) = (1/3)F/A$
Reference stress: $\sigma_1(R) = (1/2)F/A$, $\sigma_2(R) = (1/2)F/A$.

In a similar fashion, for Case #2, where $L_1 = L_2/2$:

Initial elastic stress: $\sigma_1(0) = (2/3)F/A$, $\sigma_2(0) = (1/3)F/A$
Stationary creep stress: $\sigma_1(\infty) = (0.56)F/A$, $\sigma_2(\infty) = (0.44)F/A$
Reference stress: $\sigma_1(R) = (1/2)F/A$, $\sigma_2(R) = (1/2)F/A$.

Discussion

Case #1 is representative of a highly unbalanced system with an extreme stress concentration. The initial elastically calculated stresses differ by a factor of eight. The stationary creep stresses differ by a factor of two and the reference stress is equal in both bars. This phenomenon is sometimes referred to as "load shedding." However, for this highly unbalanced system, the strain in the shorter, stiffer bar is also a factor of eight higher than the lower stressed bar. As a result, the question arises as to whether there is sufficient creep ductility in the shorter bar to eventually realize the lower reference stress level. If the shorter bar fails prematurely, the entire load will shift to the longer, more lightly loaded bar, causing the stress in that bar to increase to twice the reference stress level. Depending on the creep ductility and how far into the loading cycle the shorter bar fails, the result could be a premature failure of the two-bar system.

Case #2 represents a more balanced system without an extreme stress concentration. The stationary creep solution is within 6% of the reference stresses and the strain ratio is only a factor of two. In this case, one would not expect a premature failure.

Although it is difficult to develop quantified guidance from these two cases, the clear lesson is that considerable caution should be taken in applying reference stress methods to highly unbalanced systems, particularly if the creep ductility of the construction material is suspect.

Further descriptions of this method are given in Chapters 2, 3, and 4.

1.6 Effects of Structural Discontinuities and Cyclic Loading

This is a general discussion to acquaint the designer with the structural phenomena which are significant when evaluating elevated temperature failure modes associated with structural discontinuities and cyclic loading. Specific rules and procedures will be presented in subsequent chapters.

1.6.1 Elastic Follow-Up

Elastic follow-up can cause larger strains in a structure with applied displacement-controlled loading than would be calculated using elastic analysis. These strain concentrations may result when structural parts of different flexibility are connected in series while loaded with an applied displacement, and the flexible portions are highly stressed. In order for follow-up to occur, in a two-bar model (as shown in Figure 1.17) a less stressed, flexible element can generate inelastic deformation in a more highly stressed adjacent element. The other requirement is that the lower stressed remainder of the structure should be capable of transmitting a significant deformation to the more highly stressed portion of the structure undergoing inelastic deformation.

In the two-bar example, a displacement applied to the end of the smaller diameter bar, B, will initially cause an elastic deformation in both A and B, with B being the more highly stressed bar. Although under creep conditions the stress in A and B will both relax, the higher stress in B will cause further creep deformation in B and some of the initial elastic deformation in A will be absorbed in B – hence the term “elastic follow-up.” This process is shown

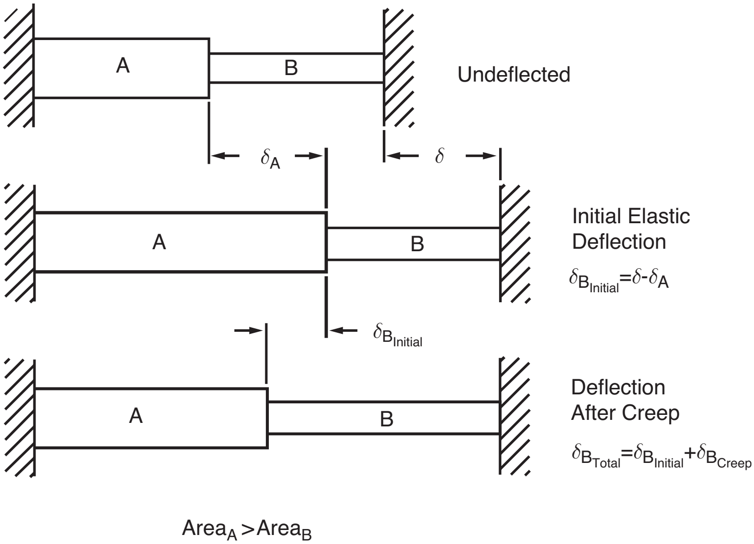

Figure 1.17 Two-bar model with elastic follow-up.

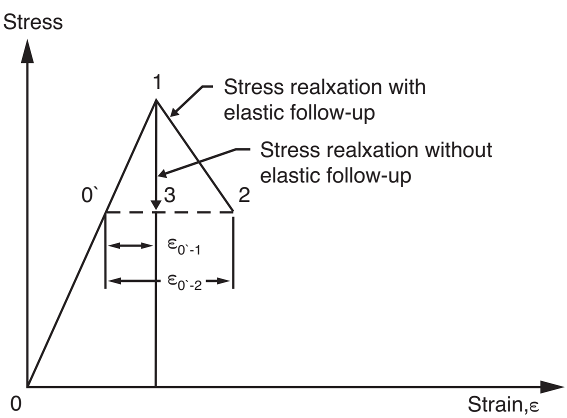

Figure 1.18 Definition of elastic follow-up.

schematically in Figure 1.18. The path 0–1 is the initial elastic loading and the path 1–2 shows the stress relaxation in time, *t*. If there were no elastic follow-up – i.e., the stresses in A and B were equal – there would be "pure" relaxation without follow-up along Path 1–3.

Also, if A is very much stiffer than B, such that the force in B will cause a negligible deformation in A, then there will also be pure relaxation in B. This is why the stress due to a local hot spot in a vessel wall is classified as a peak thermal stress.

One method for defining elastic follow-up is to compute the ratio of 0′-2, the creep strain in B at time, *t*, to the creep strain that would have occurred under pure relaxation, 0′-1. Thus, the elastic follow-up, *q*, is given by

$$q = \varepsilon_{0'-2} / \varepsilon_{0'-1}. \tag{1.11}$$

Note that, for $q = 1$ there is no follow-up, just pure relaxation, and if $q \to \infty$ the stress in B behaves as though loaded by a sustained load – i.e., load-controlled rather than displacement-controlled. For most geometries loaded in the displacement-controlled mode, $q = 2$ or less with a reasonable upper bound of $q \leq 3$. A more representative case of elastic follow-up is illustrated by Figure 1.19, a tube sheet connected to a shell at a different temperature. Elastic follow-up effects can increase the strain and stress levels at the tube-sheet-to-shell junction as shown in Figure 1.20, which shows a representative hysteresis loop with and without elastic follow-up.

The main consequence of elastic follow-up is to reduce the predicted cyclic life as compared to the life that would be predicted from an elastic analysis

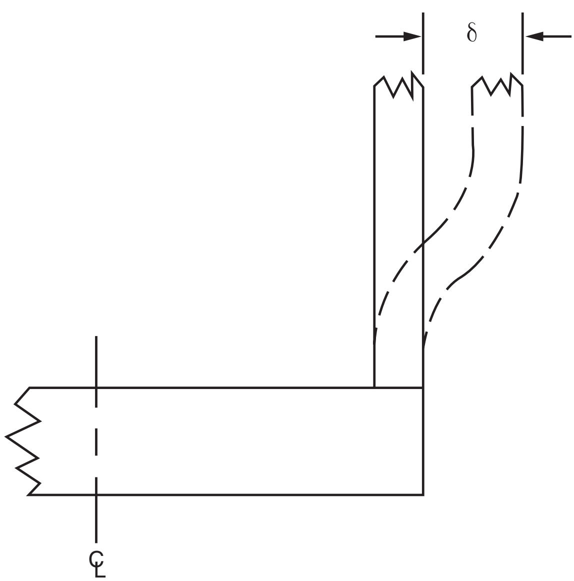

Figure 1.19 Tube-sheet-to-shell junction with relative deflection, δ, due to temperature.

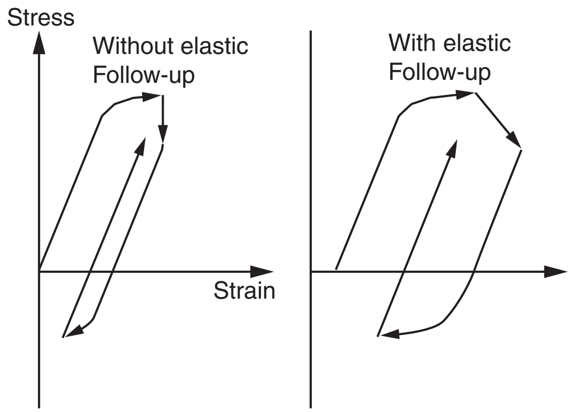

Figure 1.20 Hysteresis loop with and without elastic follow-up.

without consideration of elastic follow-up. This is due to two effects as shown in Figure 1.20. The actual strain range will be greater than that predicted by an unadjusted elastic analysis and the stress level will be higher due to the slowed rate of stress relaxation.

In general, there are two approaches to dealing with this problem. The first and most rigorous approach is to do a full inelastic analysis, which predicts the stresses and strain at critical points in the structure as a function of time. The disadvantage of this approach is that it requires complex models of material behavior, which may only have been established for a quite limited number of materials. These models require substantial judgment in their selection and use, and the actual computation times and effort involved in interpreting the results can be significant. However, in addition to general guidance on inelastic analysis, ASME III-5, Appendix Z, provides explicit guidance for development of constitutive equations and specific equations for 316H stainless steel, 9Cr-1Mo-V steel, and Alloy 617.

The second approach is based on elastic analysis without directly considering the effects of inelastic behavior. In this approach, adjustments are made to the elastic analysis results to compensate for the effects of inelastic behavior. The disadvantage of this approach is that the simpler methods tend to be overly conservative and the more complex methods can, themselves, be difficult to interpret and implement.

1.6.2 Pressure-Induced Discontinuity Stresses

In Section 1.5.2, the first step was to separate the structure into "free bodies" and compute the primary stresses in equilibrium with primary loads. The second step is to establish structural continuity by applying self-equilibrating loads to the boundaries of the "free body" segments. The stresses resulting from these self-equilibrating loads are called discontinuity stresses. This procedure for calculating discontinuity stresses is described in detail in Article A-6000 of the ASME III-1 Appendices.

At elevated temperature where creep is significant, it has been shown by analysis and experiment that the discontinuity stresses resulting from applied pressure do not relax as might be expected from self-equilibrating loads. Figure 1.21 Becht et al. (1989) is a comparison of the analytically predicted stress history for several structural configurations and loading conditions. A key comparison is Case #5 for a built-in cylinder (radial and rotational constraints at the edge) versus Case #1 for pure strain-controlled stress relaxation. After an initial redistribution of stress across the thickness, the discontinuity stress at the built-in edge is essentially constant, analogous to a primary stress. The explanation for this is that under creep conditions the "free body" segments of the structure undergo continuous deformation due to creep, with the resultant relative displacement continually increasing at the interfaces. This increasing relative displacement prevents relaxation of the interface loads and the consequent discontinuity stresses. Although this phenomenon does not exactly fit the elastic follow-up model, the resulting non-relaxing discontinuity

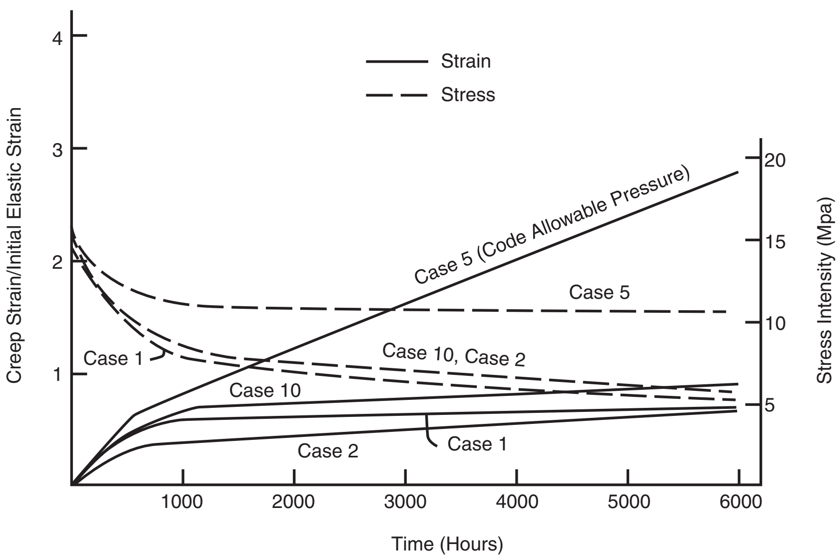

Figure 1.21 Stress relaxation and strain accumulation for pressure discontinuity (Case 5), thermal discontinuity (Case 10), and secondary stress (Cases 1 and 2). Rao (2018).

stress is analogous to the case where $q \to \infty$ and indicative of a sustained, non-relaxing load – e.g., a primary stress.

Pressure (and mechanical load)-induced discontinuity stresses do not affect the required wall thickness, but they can affect the strain and accumulated creep damage at structural discontinuities. Thus, in the rules in ASME III-5, and most other elevated temperature nuclear code criteria, these stresses are classified as primary when evaluating strain limits and creep-fatigue damage using the results of elastic analyses. If strain limits and creep-fatigue damage are evaluated using inelastic analysis while accounting for creep, this effect is automatically included.

For criteria based on Design-by-Rule, there are some restrictions that qualitatively address non-relaxing pressure-induced discontinuity stresses. For example, in the design rules of ASME VIII-1, this issue is addressed for a number of configurations by the requirement for a 3:1 taper when joining plates of unequal thickness (UW-9(c)) and head to shell joints (UW-13).

1.6.3 Shakedown and Ratcheting

Ratcheting can be described as progressive incremental deformation, and shakedown as the absence of ratcheting. A similar definition of shakedown is used in the criteria for ASME III, Class 1 and ASME VIII-2. In this case, shakedown is considered to occur when there is negligible plasticity after a few loading cycles. This later approach is illustrated by Figures 1.22a and b for elastic-plastic materials with no strain hardening or creep. Consider a tensile

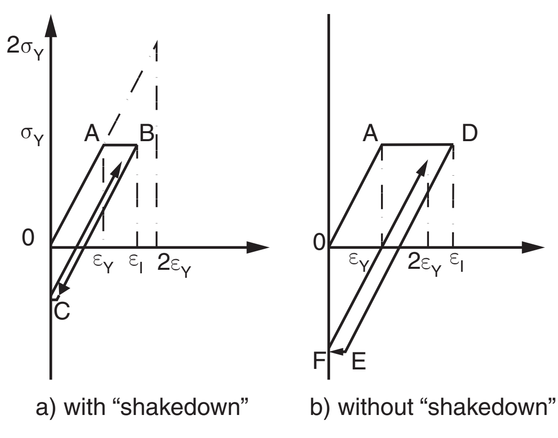

Figure 1.22 Stress and strain for cyclic strain-controlled loading without creep.

specimen that is strained in tension to a value ε_1, as shown in Figure 1.22a, which is somewhat greater than the strain at yield, ε_y, and less than twice ε_y. The initial loading will follow path OAB, initially yielding at A and continuing to plastically deform until the maximum tensile strain is reached at B. The unloading portion of the cycle consists of reversing the applied strain to the original staring point, O, following path BC without yielding. Subsequent loading for the same strain range, $0 \rightarrow \varepsilon_t \rightarrow 0$, will cycle along the path BC without yielding, hence the term “shakedown.” If, on the other hand, the applied strain range is greater than twice the strain at yield, as shown in Figure 1.22b, the loading will follow the path OADEF and there will be yielding from E to F on the unloading cycle. Subsequent cycles of the same strain range will trace out a hysteresis loop with plasticity at each end of the cycle. What enables shakedown when the strain range, ε_t, is less than twice the strain at the yield strength is the establishment of a residual stress extending the strain range that can be achieved without yielding in cyclic strain-controlled loading. However, because the residual stress is limited to the yield strength, if the applied strain range exceeds twice the strain at yield there will be straining in tension and compression at either end of the cycle and shakedown will not occur.

In the creep regime, the residual stresses will relax and the strain range that can be achieved without yielding on each cycle will be reduced. There is, however, a quite useful elevated temperature analogy to the low-temperature shakedown concept. This is illustrated in Figures 1.23 and 1.25, which are plots of stress versus time, and Figures 1.24 and 1.26, which are the corresponding plots of stress versus strain for strain-controlled loading.

As in the preceding example without creep, in the case shown in Figures 1.23 and 1.24 the initial loading follows the path OAB but now the strain is held constant for a certain period and the stress relaxes to B′. The strain is then reversed to point 0, the initial starting point, reaching a compressive stress at

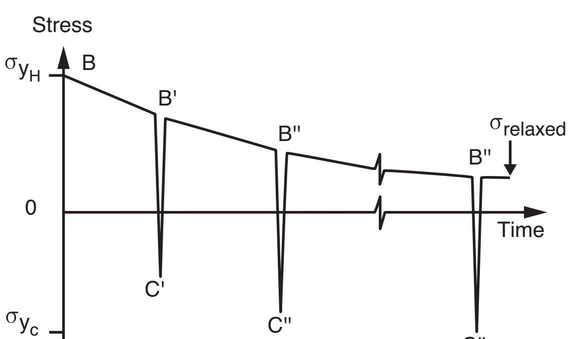

Figure 1.23 Stress history for cyclic strain control with creep and shakedown.

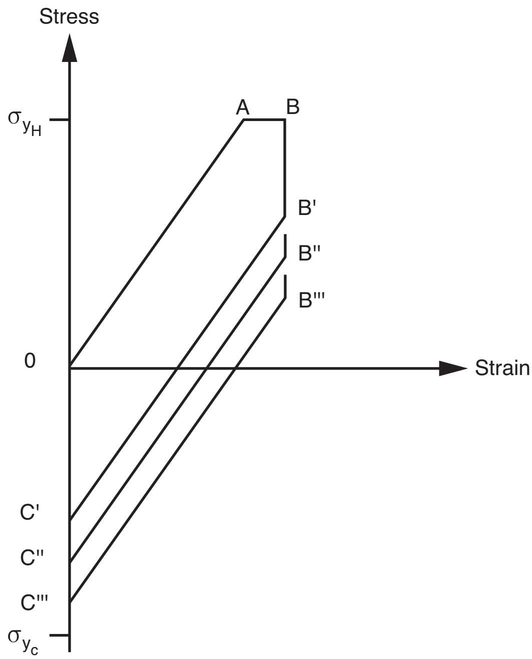

Figure 1.24 Stress and strain cyclic strain control with creep and shakedown.

C′. If the initial strain beyond yield is sufficiently limited, there will be no yielding when the applied strain is reversed and, assuming that there is no creep at the reversed end of the cycle (either the temperature is below the creep range or the duration is short), there will be no subsequent yielding upon reloading to point B′. During the next tensile hold portion of the cycle, the stress will relax to B″ and will subsequently reach C″ when the strain cycle is reversed. Under these conditions, the cyclic history will be as shown in Figures 1.23 and 1.24, and the criteria for shakedown will be that the stress range associated with the applied strain range does not exceed the yield stress at the cold (or short duration) end of the cycle plus the stress remaining after full relaxation of the yield stress over the life of the component. Thus, a criterion for "shakedown" in the creep regime becomes

$$\varepsilon_t \leq \sigma_{yc} / E_{\text{cold}} + \sigma_r / E_{\text{hot}} \tag{1.12}$$

where

E_{cold} and E_{hot} = the modulus of elasticity at the cold and hot ends of the cycle, respectively
ε_t = applied strain range

σ_{yc} = yield strength at the cold (or short time) end of the cycle, approximately equal to 1.5 S_{cold}

σ_r = relaxation strength, the stress remaining after the strain at yield strength is held constant for the total life under consideration. It can be conservatively approximated by 0.5 S_{hot}.

Equation (1.12) can also be expressed in terms of calculated stress as

$$(P_L + P_b + Q) \leq \sigma_{yc} + \sigma_r \tag{1.13}$$

where

P_b = primary bending stress intensity
P_L = primary local membrane stress intensity
Q = secondary stress.

and P_L, P_b, and Q are computed using the values of E which correspond to the operating condition being evaluated. Note that, generally, the use of E_{cold} will be conservative.

The above is a very useful concept for the design of elevated temperature components, as will be discussed in more detail in subsequent chapters, particularly Chapters 4 and 5.

Figures 1.25 and 1.26 illustrate the case where the strain range is large and there is yielding at the end of the strain unloading cycle. The load path goes from 0 to A with subsequent yielding until D. During the hold time at D the stress relaxes to E. This is followed by the unloading, or reversed strain portion, of the cycle that results in yielding at F until the cycle is completed at G. For the cycle shown there will be subsequent yielding on the tensile portion of the cycle and the stress at the start of the hold period, D, will equal the hot yield

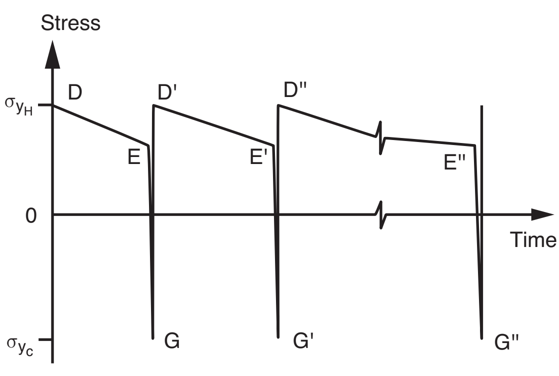

Figure 1.25 Stress history for cyclic strain control with creep and without shakedown.

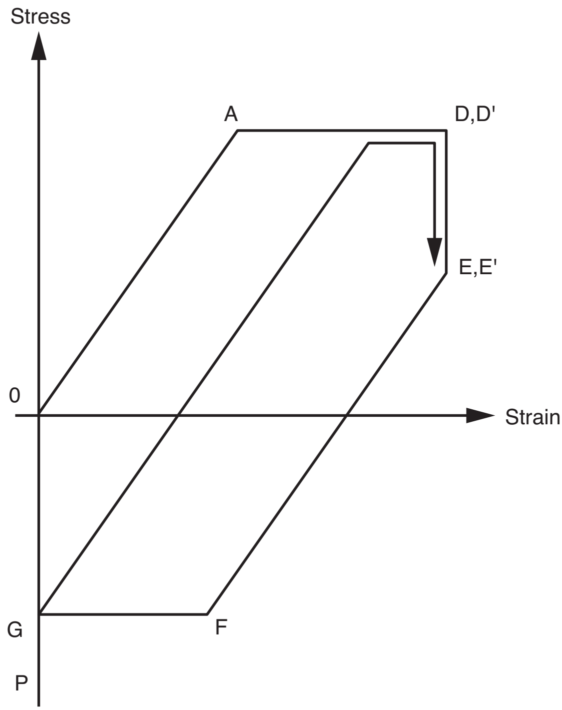

Figure 1.26 Cyclic strain control with creep and without shakedown.

strength. In effect, the creep damage for each cyclic hold time will be reinitiated at the yield strength, thus greatly increasing the creep damage, as compared to the previous case in which the stress at the start of the cycle is the same as the relaxed stress at the end of the previous hold time. In the case of the higher strain range, a hysteresis loop is established with creep strain and yielding in each cycle. One of the significant differences between the two cases is that, for shakedown, the creep damage is only that associated with monotonic stress relaxation throughout the life of the component. In the alternate case, where shakedown is not achieved, the creep damage is accumulated at a significantly higher stress level.

As noted above, another use for the term "shakedown" is to denote freedom from ratcheting – i.e., progressive incremental deformation. In the preceding example, the loading was considered to be a fully reversed strain-controlled cycle. However, in normal design practice there are both primary loads and secondary loads which, in combination, can cause ratcheting. (Purely displacement-controlled thermal stresses can also result in ratcheting, but these cases are usually associated with complete through-the-wall yielding.)

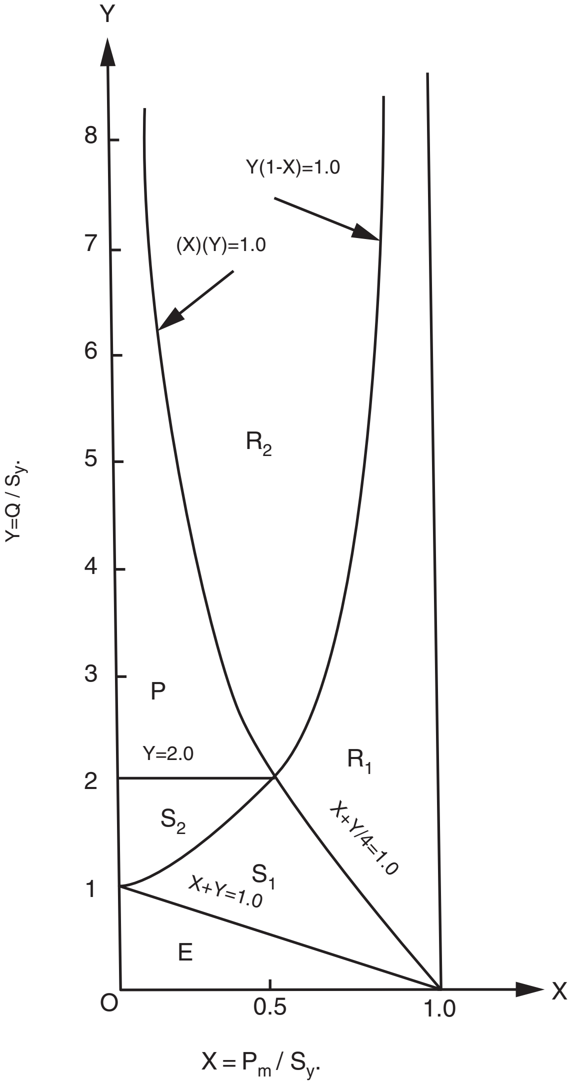

Figure 1.27 Bree diagram Bree (1967).

Bree (1968) evaluated potential ratcheting in cylinders under constant pressure and with a cyclic linear radial thermal gradient. He developed the relationships shown in Figure 1.27, which identify various regimes of behavior as a function of the relative magnitude of the elastically calculated thermal stress and pressure stress divided by the yield stress. Six areas of behavior were identified. Regions R_1 and R_2 resulted in ratcheting or incremental growth, even without creep. Region P resulted in plastic cycling in the absence of creep, and the regions S_1 and S_2 shook down to elastic action after one or two cycles – again in the absence of creep. When creep is considered only one region, E, resulted in structural behavior that could be considered as not being subject to incremental growth. Although useful, the simple approach of aiming to remain in the elastic region, E, can be too restrictive.

Based on a Bree-type model, O'Donnell and Porowski (1974) developed a less conservative approach to assess the strains accumulated under pressure (primary stresses) and cyclic thermal gradients (secondary stresses). This technique is a methodology for putting an upper bound on the strains that can accumulate due to ratcheting. The key feature of this technique is the identification of an elastic core in a component which is subjected to both primary and cyclic secondary loads. Once the magnitude of this elastic core has been established, the deformation of the component can be bounded by noting that the elastic core stress governs the net deformation of the section. Deformation in the ratcheting, R, regions of the Bree diagram can also be estimated by considering individual cyclic deformation. The resulting modifications to the basic Bree diagram are shown in Figure 1.28.

A much more comprehensive development of the Bree and O'Donnell/Porowski assessment of ratcheting is presented in Appendix D.

1.6.4 Fatigue and Creep-Fatigue

A very important consideration in elevated temperature design is the reduction in cyclic life due to the effects of creep. This is illustrated by Figure 1.29 showing the effects of hold time on the cyclic life of 304 stainless steel at 1100°F (595°C). The loading cycle constantly increases tensile strain, followed by a hold time at a fixed strain, and then a constantly decreasing strain back to the original starting point. This is referred to as a strain-controlled test, as compared to a load-controlled test in which the load is increased to a fixed level and then reversed. During the hold period at a fixed strain the specimen undergoes pure relaxation with no elastic follow-up. As can be seen from the figure, as the hold time increases the cycles to failure reduce. For a hold time of one hour, the reduction in life in this test is around a factor of 10, and it is not clear from the data if longer hold times will result in a further reduction in life. Fortunately, for most materials, as the hold time increases the stress relaxes and the rate damage accumulation slows until the effect essentially saturates. Such a saturation effect is shown in Figure 1.30, which is based on 304 stainless steel data at 1200°F (650°C). At this temperature, relaxation is fairly rapid and saturation occurs in the range of roughly one to 10 h, depending on the strain range.

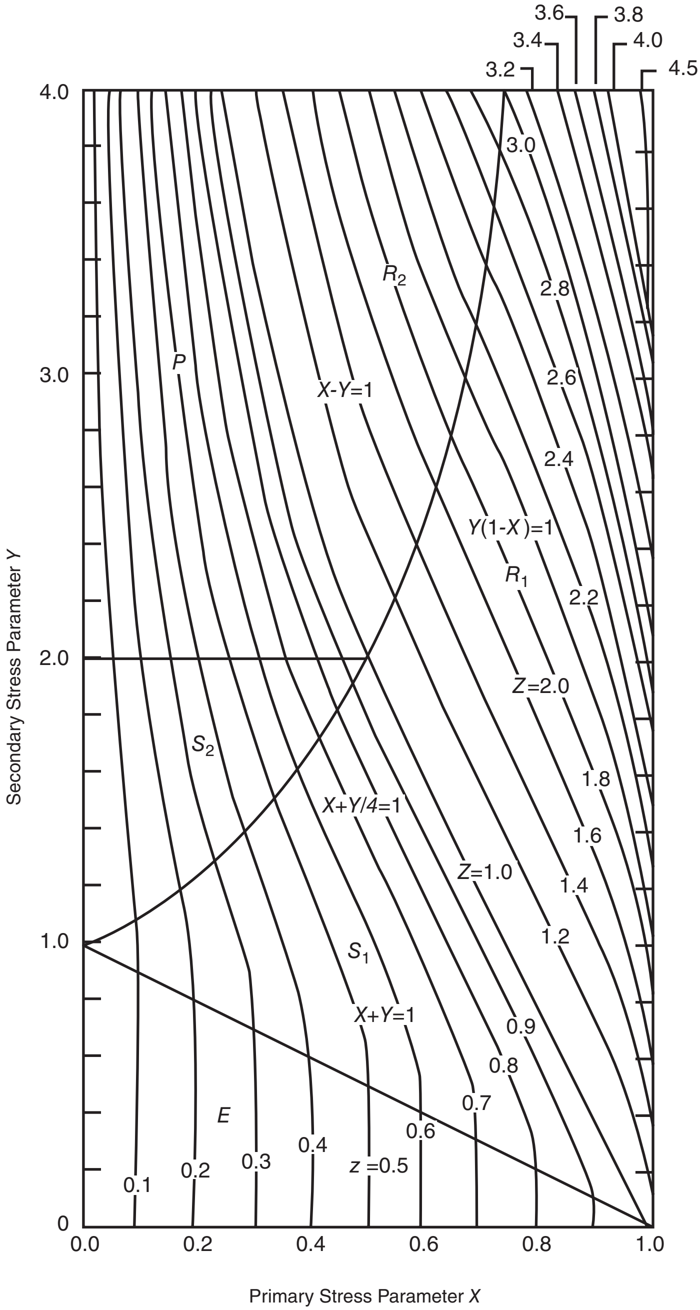

Figure 1.28 O'Donnell and Porowski's modified Bree diagram [ASME VIII-2].

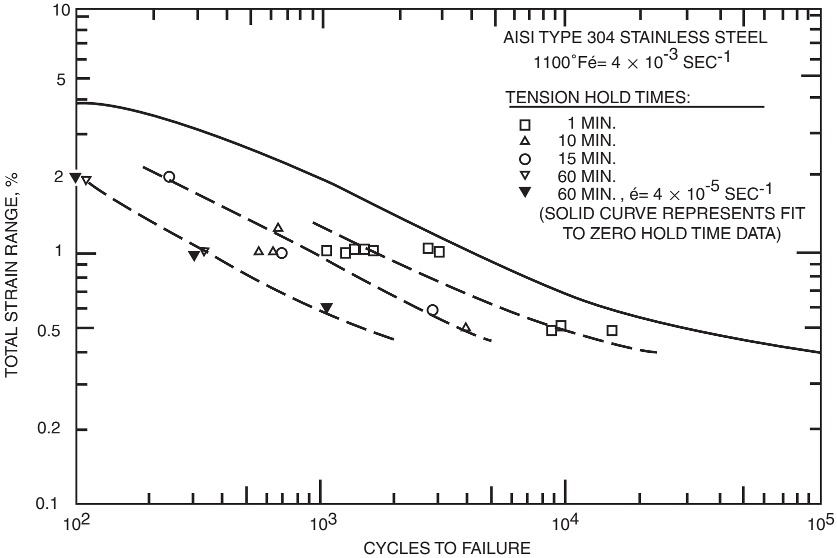

Figure 1.29 Effect of tension hold-time on the fatigue life of AISI Type 304 stainless steel at 1000°F (538°C) in air Weeks (1973).

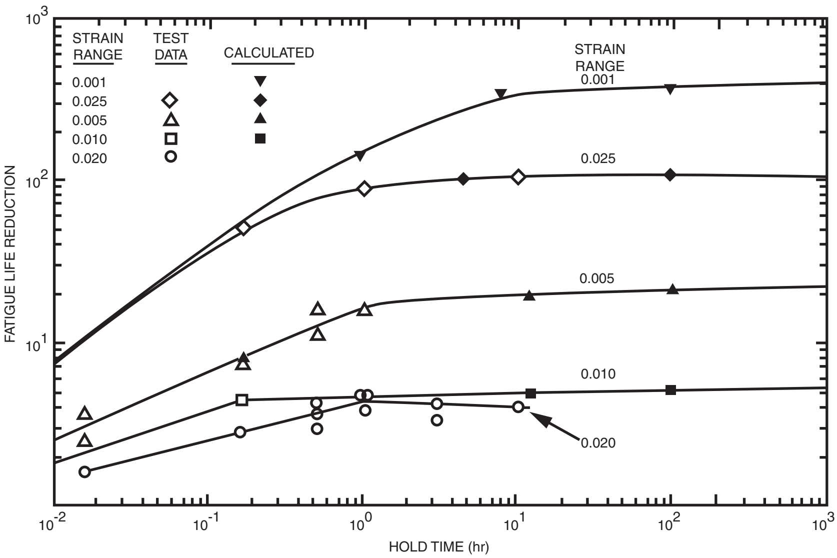

Figure 1.30 Hold-time effects on fatigue life reduction for 304 and 316 stainless steel ASME, December (1976).

Unfortunately, the actual loads encountered in design are rarely strain-controlled alone, because there are usually follow-up effects and non-relaxing stresses from primary loading conditions. The development of design methodologies to account for the effect of these varying loading mechanisms is one of the greatest challenges in elevated temperature design.

1.6.4.1 Linear Life Fraction – Time Fraction

A number of methods have been explored to correlate creep-fatigue test data and to evaluate cyclic life in design. The method chosen for III-5 is linear damage summation based on linear life fraction for creep damage and Miner's rule for fatigue damage

$$\sum(\Delta t / t_r) + \sum\left(n / N_f\right) \leq D \tag{1.14}$$

where

Δt = time at a given stress level
t_r = allowable time to rupture at that stress level
n = number of cycles of a given strain range
N_f = allowable number of cycles at that strain range
D = factor to account for the interaction of creep and fatigue damage.

In practice, safety factors are applied to the given stress level to determine a conservative time to rupture and to the allowable number of cycles.

For a design evaluation using the above relationship, it is necessary to determine the stresses and strain as a function of time at critical points in the structure. This is conceptually straightforward, using an inelastic analysis that models stress and strain behavior as a function of time. However, as previously noted, the disadvantage of this approach is that it requires complex models of material behavior, which may only have been established for a quite limited number of materials. These models require substantial judgment in their selection and use, and the actual computation times and effort involved in interpreting the results can be significant.

Alternatively, one can use elastic analysis results in mechanistic models to bound the stress-strain history, without directly considering the effects of inelastic behavior. In this approach, adjustments are made to the elastic analysis results to compensate for the effects of inelastic behavior. The disadvantage of this approach is that the simpler methods tend to be overly conservative and the more complex methods can, themselves, be difficult to interpret and implement.

1.6.4.2 Ductility Exhaustion

Another frequently used approach to the assessment of creep damage during cyclic loading is ductility exhaustion. In its simplest form, the combined effect of creep and fatigue damage may be expressed as

$$\sum(\Delta\varepsilon_c / d_r) + \sum\left(n / N_f\right) \leq D \tag{1.15}$$

where

$\Delta\varepsilon_c$ = strain increment
d_c = creep ductility
n = number of cycles of a given strain range
N_f = allowable number of cycles at that strain range
D_{cf} = factor to account for the interaction of creep and fatigue damage.

Most of the above comments regarding the application of the time fraction approach also apply to the ductility exhaustion approach. Conceptually, it should be easier to calculate creep damage via ductility exhaustion, as compared to time fractions, because the time to rupture is quite sensitive to calculated stress; by contrast, creep ductility can be relatively constant, depending on the material. However, in practice there have been numerous modifications to the ductility exhaustion approach to take into account the variations of creep ductility and the significance of when strain accumulation occurs during the loading cycle. Experimentally, some studies show better correlation for some materials using ductility exhaustion, depending on selected modifications, but there has not been a clear indication of universal applicability. The ductility exhaustion approach tends to see greater use as a damage assessment tool in failure analyses than as a design tool.

1.7 Buckling and Instability

There are two types of buckling that need to be considered: elastic or elastic-plastic, that may occur instantaneously at any time in life; and, creep buckling, which may be caused by enhancement of initial imperfections with time resulting in geometric instability. The essential difference between elastic and elastic-plastic buckling and creep buckling is that the former occurs with increasing load independent of time, whereas creep buckling is time-dependent and may occur even when loads are constant. Elastic and elastic-plastic buckling depends only on the geometric configuration and short-time material response at the time of application. Creep buckling occurs at loads below the elastic and elastic-plastic buckling loads as a result of creep strain accumulation over time.

The sensitivity of creep buckling to initial imperfections is illustrated by the deformation-time relationships shown in Figure 1.31. Although typical of the behavior of axially compressed columns and externally pressurized cylinders, these curves are representative of most structures. In general, a structural component will deviate initially from a perfect geometrical structure by some small amount. Under a system of loads, below those that would cause elastic or

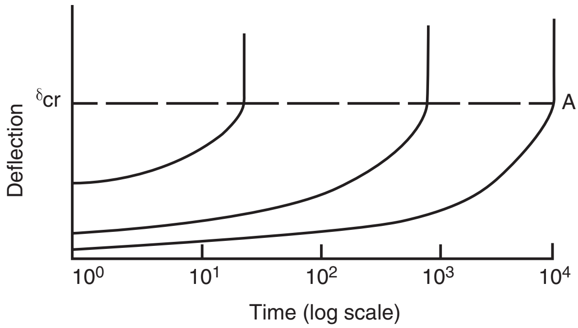

Figure 1.31 Creep buckling deflection-time characteristics of tubes with different ovalities *ASME, May (1976)*.

inelastic instability, the initial deflection is magnified over time due to creep. The deflection increases until the geometrical configuration becomes unstable, as shown by point A in Figure 1.31, and buckling occurs.

ASME III-5 provides figures that define temperature and time limits within which creep effects need not be considered when evaluating buckling and instability.

Chapter 9 gives a more comprehensive coverage of creep buckling.

Problems

1.1 The maximum effective strain in the longitudinal weld of a steam drum is limited to 0.5%. What is the effective operating stress in the cylinder, from Figure 1.11, that will result in an expected life of

a. 100,000 h?
b. 300,000 h?

1.2 What is the allowable stress of the material shown in Figures 1.13 and 1.14 at 1100°F, based on creep and rupture criteria?

1.3 A pressure vessel component operating at 850°F has an expected life of 300,000 h. What is the expected life if the temperature is inadvertently raised to 900°F while maintaining the same stress level?