**DEVELOPMENTS IN HIGH-TEMPERATURE
DESIGN CRITERIA**

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ABSTRACT

In the last two years considerable effort has been devoted to the development of more sophisticated design methods and criteria for use in elevated-temperature design. Stimulus for this effort has been largely a result of the needs of the Liquid Metal Fast Breeder Reactor program (LMFBR). Preparation of rational design rules has been limited by a lack of knowledge of material behavior and a general lack of analytical methods capable of predicting stresses and deformations in a complex structure under time-dependent loading conditions. In this paper the time-dependent failure modes upon which relevant elevated-temperature design rules must be based are discussed, and criteria being developed to preclude failure in these modes are reviewed.

INTRODUCTION

The present nuclear codes were developed for reactor systems which operate at temperatures less than 600°F, where the usual materials used for the pressure boundary exhibit little or no time-dependent (creep) behavior. Simplified rules have been developed for treating the complex, nonlinear material behavior in the elasto-plastic range by employing limit load and shakedown concepts and pseudo-elastic analysis methods for the determination of the cyclic plastic strain quantities for fatigue analyses. The current code rules for subcreep conditions are aimed at preventing failure by (1) instantaneous bursting, (2) instability (buckling), (3) incremental collapse (ratcheting), and (4) failure by crack propagation due to plastic strain fatigue. Stress categories have been established to limit stresses to preclude failure by these modes. Primary stresses are limited to prevent instantaneous failure by either bursting or buckling, secondary stresses are limited to avoid incremental plastic collapse and also to validate the fatigue analysis on a pseudo-elastic basis, and peak stresses which are related to strain concentrations are limited to prevent fatigue crack initiation and propagation.

As was the case in developing relevant design rules for subcreep conditions, design rules for elevated temperature must be based on consideration of real failure modes. In designing for elevated temperature, creep distortion and stress-rupture phenomena must be considered. The creep phenomenon is time-dependent so that the time parameter, which can be ignored for lower temperature, becomes one of the essential factors.

The additional failure modes which must be considered at elevated temperature are: (1) creep and stress rupture, (2) creep buckling, incremental collapse (ratchet), and fatigue and creep fatigue interaction. Preparation of rational design rules to preclude failure in one of these modes has been limited by the general

lack of knowledge of material behavior and lack of analytical methods capable of predicting stresses and deformations in a complex structure under time-dependent loading conditions. Interim criteria for use within the current state-of-the-art of the pressure vessel and piping industry, which are more relevant to the modes of failure expected at high temperature, are proposed in the literature.^{(1-3)*} However, because of this state-of-the-art limitation, these criteria have been only partially based on time-dependent behavior considerations and have been appropriately criticized for this deficiency. Intensive efforts are under way to develop the more sophisticated analytical methods and the requisite material property data to establish and permit implementation of more rational elevated-temperature design criteria. As the requisite information is developed, there does exist the possibility that, for certain geometries and loading conditions, simplified methods (pseudo-elastic creep) may be developed similar to the pseudo-elastic-plastic methods contained in the existing codes.

It is still too soon to judge when current efforts will result in sufficient data and methods to prepare and apply criteria which can be labeled satisfactory. In line with this discussion, the time-dependent failure modes upon which such criteria must be based are reviewed in the following.

CREEP AND STRESS RUPTURE

Stress rupture and, at elevated temperature, creep rupture are among the failure mechanisms against which appropriate provisions must be made. Gross distortions and strain accumulations also may be of concern and held to some acceptable limit over the lifetime of the system. Creep deformations due to the equilibrium forces in the system may result in excessive strain accumulation at geometric or metallurgical discontinuities such as at weldments. This potential for strain accumulation, coupled with the relatively poor apparent ductility of weld

*Numbers in parentheses refer to similarly numbered references in bibliography at end of paper.

metal at elevated temperature, can result in premature failure. Provisions against these failure modes may be made by placing limits on primary stresses.

The term "primary" is used here in a broad sense. As discussed in the Introduction, the basic definition of primary stresses in Section III of the ASME Code is: those which can cause failure (bursting buckling) under a single application of load. Here it is used in the context of time-dependent failure, namely, whether failure can occur in the specified service life. Within this context, limits on primary stresses must provide for the following:

1. Maintain a minimum factor of safety against rupture (bursting) and buckling under design loading over the specified service life.
(Buckling is discussed further in the following section.)
2. Restrict the summation of creep damage from equilibrium forces over the design life below a safe value.
3. Limit the strain accumulation from equilibrium forces at geometric or metallurgical discontinuities. This limit should include consideration of enhanced strain accumulation due to stress gradients resulting from secondary and peak stresses, and the effect on local accumulation of strain resulting from redistribution of redundant forces in the system by creep and relaxation.

Where significant creep effects are present in a system, it is clear that an inelastic analysis would be required to predict adequately the essential features of the structural behavior being evaluated. The current approach to provide for these effects is to limit the primary stresses associated with normal operation to an allowable stress value which is time-dependent (a function of the duration of loading), and to limit the rupture life fraction summation to an acceptable value.

The criteria in Reference 3 are typical of this approach. The general membrane stress intensity for any normal or upset condition, P_m , is limited to $\bar{S}_m$,

where $\bar{S}_m$ is determined for the time " $\bar{t}$ " which corresponds to the total duration of the loading during the entire design life of the component. The values of $\bar{S}_m$ are based on the same limits derived from multiples of yield and ultimate strength as contained in the current Section III of the ASME Code, but also include the following time-dependent criteria.

1. Two-thirds of the minimum stress to cause rupture in $\bar{t}$ hours. The term "minimum" refers to the stress level which will be below 95% of the experimental data 95% of the time. The stress to cause rupture is that initial stress in a conventional constant load uniaxial tensile stress-to-rupture test which is associated with rupture of the specimens at $\bar{t}$ hours.
2. Eighty percent of the minimum stress to cause initiation of tertiary creep at the end of $\bar{t}$ hours. The initiation of tertiary creep is defined as that point in a conventional constant load uniaxial tensile creep test where the observed strain departs from the linear secondary creep stage by 0.20% strain. The term "minimum" refers to the stress level which will be below 95% of the experimental data 95% of the time.
3. The minimum stress to produce 1.0% total effective equivalent strain at the end of $\bar{t}$ hours for a constant stress type of loading. This value corresponds to the stress at 1.0% strain from an isochronous stress-strain curve. The elastic, plastic, primary creep, secondary creep, and tertiary creep strains are accounted for in the isochronous formulation. The term "minimum" refers to the stress level which will be below 95% of the experimental data 95% of the time.

Fig. 1, reproduced from Reference 4, illustrates isochronous curves developed for 304-304H stainless steel. The data shown in Reference 4 represent average data, since sufficient data do not exist to determine adequately a statistical mini-

Fig. 1 — Isochronous Stress Strain Curves for 304-304H Steel - 1200°F

mum useful for design purposes. The minimum has been estimated to be about 80% of the average values.

Fig. 2 shows the allowable stress values for 304H material based on the criteria described above.

In addition to general membrane stress intensity meeting the $\bar{S}_m$ allowable, the life fraction sum associated with the general primary membrane stress intensity must satisfy the requirement:

$$\sum_i (t_i/t_{im}) \leq B$$

where t_i = the total duration of a specific loading (or loading combination) during the entire design life of the component. Note that $\sum_i (t_i)$ is equal to the specified component design life.

B = the life fraction usage factor, equal to 1.0.

t_{im} = the time derived using only the time-dependent factors for $\bar{S}_m$, as described above.

The limiting time-dependent factors are shown in a stress ($\bar{S}_m$) vs time (t_{im}) curve in Fig. 3 for 304H stainless steel.

CREEP BUCKLING

The investigation of creep buckling for components subjected to a constant compressive force entails the prediction of the time to the onset of instability (critical time). This critical time always will be finite regardless of how small the applied load, provided there are small initial imperfections in the component or provided that bending moments are present.

One way of evaluating creep buckling of components in high-temperature systems is to compute the loads acting on each component of the system based

Fig. 2 — $\bar{S}_m$ Values for 304H Stainless Steel

Fig. 3 — $\bar{S}_m$ vs Time Curve for 304 Stainless Steel

on elastic analysis. These loads then can be assumed to remain constant as creep occurs and the critical times calculated. A comparison of these critical times with the required design life of the component under the applied loading is the basis for a validation of the design. The designer should apply this approach with considerable caution, because the assumption that the load is invariant with time may be incorrect for several reasons. First, the distribution of forces in any system will depend on the creep stiffness characteristics of the components. For externally applied loads, creep deformations will result in a redistribution of the internal redundant forces with some increasing while others decrease. In this case, a creep buckling analysis based upon initial loading would be unconservative, and estimates of the redistribution of forces must be made. When loadings on the system result from thermal expansion (strain or displacement controlled loads), creep deformations will result in a general relaxation in internal forces. This relaxation of forces will not be uniform throughout the system and strains will tend to accumulate in the more flexible (in creep) members. A stability analysis based on constant initial forces will be conservative, but the estimates of critical times may be far more restrictive than required. Conservatism also results from the fact that the loads resulting from strain-controlled phenomena usually fall off very rapidly as the point of instability is reached. For example, as the buckling mode deformation of a column progresses, its end points move closer together. When this contraction equals the total expansion strain, no further deformation can occur. More realistic estimates of critical times require an analysis of the relaxation of internal forces and resulting deformations.

In any evaluation of creep buckling, it must be recognized that the effects of creep deformation may result in significant changes in the structural shape and thus the end-of-life load factor of safety against buckling may be lower than that for the initial shape. Analyses which account for creep deformation should be used where creep effects exist and the load factors of safety against immediate collapse should be maintained for the entire design life. Reference 3 recommends the factors of safety both on load and time, shown in Table 1.

TABLE 1 — MINIMUM INTENDED FACTORS OF SAFETY

<u>Type of Buckling</u>	<u>Theoretical Limit</u>	<u>Experimental Limit</u>
Elastic	3.0	2.5
Elastic-plastic	2.5	2.0
Elastic-plastic-creep	2.5 and (10)*	2.0 and (10)*

where the Load Factor of Safety = $\frac{\text{Load at Instability}}{\text{Design Load}}$

and the Time Factor of Safety = $\frac{\text{Time to Instability}}{\text{Design Load Duration}}$

INCREMENTAL COLLAPSE — RATCHETING

The rules for thermal stress ratchet in shells contained in Paragraph N-417 of Section III of the ASME Code are derived⁽⁵⁾ from the behavior of a one-dimensional element which exhibits an elastic-perfectly plastic response to load (stress). Fig. 4 illustrates the behavior regimes for this element with linear variation in temperature through the shell wall, where y is the maximum range of thermal stress computed on an elastic basis divided by the yield strength, S_y , and x is the maximum membrane stress due to pressure divided by the yield stress, S_y . In regime E, the stresses are always elastic and progressive distortion does not occur from cyclic application of the thermal stress. In regimes S_1 and S_2 , plastic flow occurs during the first thermal cycle, but during subsequent cycles the bar "shakes down" to a residual elastic state. In region S_1 , plastic flow takes place initially on both sides of the bar, while in region S_2 , plastic flow takes place on only one side of the bar. In stress regime P, plastic flow occurs during application of all thermal cycles but no net growth of the bar occurs. In other words,

*(10) refers to an additional factor of safety (based on time) of ten.

Fig. 4 — Stress Regimes for One-Dimensional Element

the bar undergoes captive plastic cycling. In regimes R_1 and R_2 , thermal ratcheting occurs with the bar extending in length with each application of the thermal cycle. Defining δ as

$$\delta = \frac{\epsilon_g}{\epsilon_y}$$

where ϵ_y is the elastic strain at yield, and ϵ_g is the growth strain per cycle, the incremental strain per cycle is given by:

$$\delta = 2(xy-1), \quad \text{in region } R_1$$

and

$$\delta = 2y \left\{ 1 - 2 \left[\frac{1}{y} (1-x) \right]^{1/2} \right\} \quad \text{in region } R_2$$

where x and y are as defined above. Again, in region R_1 , the plastic flow occurs on both sides of the bar upon application of the thermal cycle, while plastic flow occurs on one side in region R_2 . Contours of the strain increment per cycle are shown in Fig. 4.

Extensions of the one-dimensional bar analysis to include the effects of strain hardening (for an isotropically hardening material) and the effect of mean temperature on ratcheting exist in the literature. Paragraph N-417 of Section III of the ASME Code does not give any guidance as to the applicability of this approach in estimating the behavior of general shell structures. This model has been applied to the general case by the use of effective stress and strain relationships. The designer should bear in mind that this is an approximation, since the shakedown regimes only apply strictly to those cases where the principal stress ratios do not change during the application of the thermal (secondary) stress. This would be true for the spherical shell. Reference 7 indicates, how-

ever, that for most structures of interest, the shakedown regime would only be decreased by a small amount.

The introduction of creep behavior into the ratcheting problem complicates the strain prediction even for the simple one-dimensional element. Creep strain, as plastic strain, is irreversible, but is time-dependent. Creep strain will develop even for the membrane stress applied alone, causing the element to grow continuously. In addition to this, however, high stresses developed during the thermal (secondary) loading portion of the cycle will cause a short-term increase in stress pattern which will slowly dissipate due to an increase in the creep rate. This enhanced creep effect will cause the stresses to approach the original primary membrane stresses applied to the element. After a return to steady-state conditions after the transient, the reversed stresses may again cause plastic yielding and an increment in plastic flow, which again will dissipate with time toward the primary membrane stress level.

The extent to which the secondary and residual stresses relax to the primary membrane stress level depends upon the cycle time of the thermal loading as well as upon the specific creep properties of the materials of interest. To trace this behavior through each loading cycle, in general, requires a detailed numerical analysis combining both plastic and creep effects.

However, using the one-dimensional element, some estimates of the additional ratcheting behavior caused by the creep effects can be attempted by making the conservative assumption that the maximum creep relaxation of the secondary stress occurs during the loading (transient) cycle, and the stresses reach their limiting value before application of a reversed unloading cycle. With this assumption, it is found that plastic ratcheting must occur even during the shakedown regime of Fig. 4, that is, whenever

$$x + y > 1. \quad (1)$$

This obviously must follow from the assumptions since, with stress relaxation during each part of the loading cycle, a shakedown state can not be achieved since the stress at the beginning of each half cycle is about the same.

Approximate estimates of the plastic ratcheting which will occur for this situation have been given⁽⁸⁾ and lead to the following relations:

$$\begin{aligned}
 \delta &= (1-x) + y \left(x - \frac{1}{y} \right), & \text{in Regimes } S_1 \text{ and } P \\
 \delta &= (1-x) + y \left\{ 1 - 2 \left[\frac{1}{y} (1-x) \right]^{1/2} \right\}, & \text{in Regime } S_2 \\
 \delta &= (1-x) + 2y \left(x - \frac{1}{y} \right), & \text{in Regime } R_1 \\
 \delta &= (1-x) + 2y \left\{ 1 - 2 \left[\frac{1}{y} (1-x) \right]^{1/2} \right\}, & \text{in Regime } R_2.
 \end{aligned} \tag{2}$$

Thus plastic ratcheting now occurs in those regimes where shakedown and plastic cycling previously occurred, while in the original ratcheting regimes the plastic strain increment per cycle is increased. The ratchet strain per cycle as specified by Eqs. 2 has been plotted for various loading regimes and is shown in Fig. 5.

In addition to the influence of creep on plastic ratcheting, the enhanced creep strain yields an increase of the total creep strain per cycle. Estimates of the total strain increment per cycle depend upon the specific data for a problem (material properties, time cycle data). Assuming a secondary creep law of the form

$$\dot{\epsilon}^C = A (\sigma/\sigma_0)^n \tag{3}$$

numerical evaluation of the total creep and plastic strain increments per cycle were made in Reference 8, for the one-dimensional bar element for various cycle

Fig. 5 — Ratchet Strain per Cycle

times of interest. The results of these calculations are shown in Figs. 6 through 11 for a material which exhibits a creep exponent, n , equal to 6. The dimensionless parameter, τ_1 , is defined by

$$\tau_1 = EA \sigma_0^{n-1} t_1 \quad (4)$$

where t_1 is the time for the loading duration of the cycle. For this particular creep exponent, the total ratchet strain per cycle increases with the thermal stress (y), applied membrane stress (x), and loading time (τ_1), and for any given values of stress (x, y) tends to an upper limit as τ_1 increases. This upper limit corresponds to complete relaxation of the secondary stresses to the applied primary membrane stress during the loading phase of the cycle.

A simplified analysis for more complex structural elements is unavailable and leaves two choices to the designer, namely: (1) use of the one-dimensional element data modified by considering effective stress and strain concepts, or (2) undertaking a complete numerical analysis.

To treat the detailed complex loading histories for nonlinear materials, various numerical tools (computer programs) are available to study particular structural problems. Appendix A, Part II, of Reference 3 contains a description of the state-of-the-art computer programs for inelastic structural analysis. These programs generally fall within two categories of analysis: (1) finite difference methods, and (2) finite element methods. In comparing these two methods in a general way, the finite difference methods are usually simpler and require shorter computational time than the finite element methods, but are generally restricted to simple geometries. Finite element methods, on the other hand, are usually longer running but can treat a rather arbitrarily shaped component with ease. Limited experience using these programs indicates that computation time can be saved, without sacrificing accuracy, in problems with long periods between application of high transient loads, by performing the analysis in a step-wise

Fig. 6 — Increment of Strain per Cycle

Fig. 7 — Increment of Strain per Cycle

Fig. 8 — Increment of Strain per Cycle

Fig. 9 — Increment of Strain per Cycle

Fig. 10 — Increment of Strain per Cycle

Fig. 11 — Increment of Strain per Cycle

fashion. During the periods when high transients are applied to the system, a relatively short-term elastic-plastic-creep analysis can be used to trace the stress and strain history in the material. Between these periods, an elastic-creep analysis can be performed using as starting conditions the results obtained from the previous detailed elastic-plastic creep analysis.

CREEP FATIGUE

In real structures, the loading history is such that pure creep seldom occurs. In the real case both creep and relaxation may occur under cyclic application of loading. Recovery effects may also occur, and hence unloaded periods may influence the life of the structure. The evaluation of structures subjected to cyclic loading when creep occurs must, therefore, include consideration of these effects. The current code procedures are not applicable in the creep range, because the plastic strain would be improperly computed if creep deformations are neglected, and the effect of creep strain on life is significantly different than that of the slip-type of plastic flow.

Both creep and slip-type of plastic deformation are cumulative in nature, and the mechanisms can interact. In general, the interaction is detrimental to life but it can also be beneficial in some cases, where strain aging improves the creep strength.

Current approaches in treating the combined effects of creep and fatigue are based on the concept of life-fraction damage accumulation. In the life fraction method, the total damage is separated into creep and fatigue components, with the time period of constant or slow strain rate periods (hold times) contributing to the creep damage, while cycles at rapid strain rates cause fatigue damage. This separation permits fractional damage accumulation in an analogous method to that utilized in fatigue damage assessment at low temperatures and creep rupture life at elevated temperatures.

As an example of the formulation of this approach, Reference 9 proposed the following relationship for the evaluation of creep fatigue damage:

$$\sum^p \alpha \left(\frac{n}{N} \right)_i^u + \sum^q \beta \left(\frac{t}{t_r} \right)_j^\nu = \kappa_\ell \quad (5)$$

where n_i = number of cycles for stress σ_i

N_i = fatigue life for σ_i

t_j = time period under σ_j

t_r = creep rupture time for σ_j

q = total number of creep periods

p = total number of stress cycles

α, β, u, ν = parameters dependent on the creep fatigue interaction

κ_ℓ = correlation parameter for cumulative damage.

An alternate form of the creep fatigue life fraction summation (Reference 3) is the following relationship:

$$\sum_{i=1}^n \left(\frac{t_\sigma}{t_r} \right)_i + \sum_{j=1}^m \left(\frac{t_\epsilon}{t_r} \right)_j + \sum_{k=1}^p \left(\frac{n_k}{N_k} \right)_k \leq D \quad (6)$$

where t_σ = total duration of a load which is primary in nature and thus does not relax due to plastic or creep deformation.

t_r = allowable duration for the stress, σ , at temperature.

(t_ϵ/t_r) = creep-rupture usage factor associated with a relaxing load. It may be evaluated using the integral $\int (dt/t_r)$ determined for the time-dependent stress level.

(n_k/N_k) = fatigue usage factor similar to that defined for Equation 5 above.

D = life fraction summation limit.

The final form and degree of conservatism incorporated into the creep fatigue design procedure are still the subject of much discussion. Alternate ap-

proaches to the life fraction concept do exist, but are only conceptual in nature at this time. It does appear, therefore, that criteria which will be used in the near term will be based on the life fraction approach.

SUMMARY AND CONCLUSIONS

The preceding discussions have pertained to criteria (allowable stresses and deformation limits) to be applied to stresses and strains determined from a time-dependent evaluation of the expected service or operating conditions. The current pressure vessel codes have not been very specific in defining the design lifetime for which the allowable stresses are to be used. The writers of the codes point out that the public acceptance of a code depends upon the safety record associated with its use, and that a risk of failure does exist if too high a stress is permitted for short design lives, if the users neglect to take the equipment out of service at the end of its specified design life. Sections I and VIII of the ASME Code base the allowable stresses at elevated temperature on a minimum (secondary) creep rate of 10^{-7} in./in./hr with rupture data extrapolated to 10^5 hr. These allowable stresses apply to design pressure and temperature, which are usually significantly higher than the conditions at which the equipment is actually operated for a substantial portion of its service life.

Individuals writing code rules for designing at elevated temperature are giving consideration to retaining a separate set of allowable stresses for design conditions. These allowables for design conditions would be related to those in the current codes, and would provide a bridge between new and past practice such that the new design practices would not be completely divorced from previous practice for which considerable service experience does exist. This approach is, of course, a compromise; a thick-walled vessel with low-pressure stresses may have high thermal transient stresses and be less safe than a thinner vessel for a short service life. It is important that the designer of high-temperature equip-

ment be aware of these considerations since existing codes do not provide all that must be known, or should be considered, about the failure modes and material behavior at high temperature.

During the past two years, the design methods and criteria have been undergoing a steady evolution. It is still too soon to conclude what the final form of the design rules and methods for elevated temperature design will be when the rules are finally codified. It is hoped that this paper will have provided those interested in this subject an indication of the considerations and status of the development of high-temperature design criteria.

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