ENHANCED THERMAL CREEP DEFORMATION UNDER CONDITIONS OF SECONDARY STRESS CYCLING IN THE ELASTIC REGIME

S. VAIDYANATHAN and R.G. SIM

General Electric Company, Fast Breeder Reactor Department, Sunnyvale, California 94086, U.S.A.

SUMMARY

Enhanced thermal creep straining can take place under conditions of secondary stress cycling even when the maximum stress is within the elastic region. In the present investigation, a simplified analysis procedure is proposed to estimate the extent of enhanced creep strains under such conditions. A particular instance where this effect can be important is the case of a fuel rod in an LMFBR where there are temperature variations in the cladding due to power cycling. A detailed analysis of the fuel rod cladding to determine the damage and thermal creep strains due to power cycling operation is expensive and time consuming. The proposed simplified analysis procedure is aimed at computing the decay of thermal stress as a function of time with the computed stress history being used to assess the thermal creep strains under power cycling conditions.

The secondary stresses resulting from through the wall temperature gradients in a thin tubular structure can be calculated on an elastic basis from solutions available in literature. Under thermal creep conditions these stresses relax at a rate determined by both the instantaneous level of the thermal stress as well as the level of primary stress induced by pressure loads at the time under consideration. Based on theoretical considerations, the decay of secondary stresses due to through the wall temperature gradients can be expressed by the equation:

$$\frac{dQ}{dt} = E(\dot{\varepsilon}_1 - \dot{\varepsilon}_0)$$

where (dQ/dt) is the decay rate of the secondary stress Q, E is the Young's Modulus and $\dot{\varepsilon}$ is the thermal creep strain rate. The suffixes 1, 0 denote that the strain rates are to be taken at stress level of $\sigma_H + Q/1.5$ and σ_H respectively where σ_H is the hoop stress due to the pressure load. That the actual decay of the secondary stress is closely approximated by the above formulation has been verified by comparison with more rigorous analysis. Therefore, it is possible to use the above simplified formulation to predict the level of stress as a function of time. This can then be used in turn to assess thermal creep damage and strains under conditions of temperature cycling.

The above procedure has been applied to investigate the thermal creep strain contributions for a fuel rod in an LMFBR under power cycling conditions. In this analysis it was assumed that the dominant stresses in the cladding were due to the fission gas pressure and thermal stresses due to through the wall temperature gradients. Although the simplified analysis procedure proposed here has been applied particularly for fuel rods, it would appear to have more general applications for situations involving stress relaxation in the presence of membrane stresses.

INTRODUCTION

Many components in a Liquid Metal Fast Breeder Reactor (LMFBR) are subject to time varying thermal gradients arising as a result of various duty cycles imposed upon it. A particular instance where this effect could be important is the case of a fuel rod in an LMFBR where there are temperature variations in the cladding due to daily power cycling. The determination of the accumulated inelastic strains under such conditions is often time consuming and expensive, and simplified methods of analysis are often preferred. The stresses that arise due to thermal gradients are classified as secondary stresses since they result from strain compatibility requirements. In contrast, the stresses resulting from equilibrium with externally applied forces, such as the fission gas pressure in a fuel rod, are termed as primary stresses. The estimation of inelastic strains under conditions of secondary stress cycling has received considerable attention in literature. In particular, it has been found that enhanced thermal creep straining could take place under conditions of secondary stress cycling. However, these methods are applicable only to situations where the secondary stress relaxation during one half of the cycle is insignificant. In this paper, a simplified procedure is postulated for monitoring the decay of secondary stresses under conditions of temperature cycling where relaxation could occur during both halves of a cycle. The method does not consider inelastic strains due to time independent plasticity and is therefore applicable only for secondary stress cycling in the elastic regime. The procedure has been applied to the estimation of thermal creep strains in an LMFBR fuel rod cladding under conditions of daily power cycling in the absence of fuel to cladding mechanical interaction.

THEORETICAL CONSIDERATIONS

In order to evolve a simplified analysis procedure for estimating the thermal creep strains under conditions of secondary stress cycling, it is first necessary to find a suitable approximation for the relaxation rate of secondary stresses. Except for simple cases such as a bar kept under constant initial strain, the rate of change of secondary stresses cannot be described in an exact fashion without involving the complete solution to the problem. Instead, it is necessary to find a suitable approximation for the decay and this can be best accomplished by considering the creep strain rates in the structure under two sets of conditions: one, where only the primary stresses act and second, where both the primary and secondary stresses are present. The difference in the strain rates between these two conditions contributes to the relaxation process and to a first approximation can be equated to the relaxation strain.

A logical choice for the description of deformation rates in the absence of secondary stresses is the reference stress for the structure under the given system of primary loads. The concept of a reference stress has been investigated in detail by Mackenzie and Sim [1,2,3]. The results of these studies indicate that there is a single reference stress for a structure undergoing creep such that the deformation of the structure is approximately proportional to the deformation associated with the reference stress. In general, the reference stress for a structure is such that the rate of energy dissipation assuming this stress acts uniformly over the entire body is nearly equal to the stationary state energy dissipation rate. For a rectangular beam under pure bending the reference stress is $\sigma_0/1.5$ where σ_0 is the elastically calculated fiber stress. For a thin tube under internal pressure p_i and with internal and external radii a and b, the reference stress is given by $p_i/\ln/(b/a)$. The steady creep deformations of the tube or the beam can be predicted using the appropriate reference stress and the constitutive relation for creep. When secondary stresses are also present, it is once again desirable to approximate the deformation under such conditions using a suitable representative stress level. For a tube under internal pressure and subject to through the wall temperature gradients, it is proposed that the deformation rate can be represented using a modified reference stress level of $[p_i/\ln(b/a)] + Q/1.5$ where Q is the stress above and below the mean primary hoop stress level that results from the temperature gradients. Here the additional term Q/1.5 has been derived based on analogy with the beam under pure bending which has similar stress distribution. More exact computations were not found necessary as only an approximate representation for the deformation rate is being sought for conditions when secondary stresses are present.

With the aid of these two representative values for the deformation rates (first, the reference stress due to the primary loads alone and second, that modified due to the secondary stress) a rate relation could be developed for the relaxation process. If $\dot{\epsilon}_1$ and $\dot{\epsilon}_0$ denote the strain rates at any given time due to the modified and original reference stress levels respectively, the differential strain in an incremental time dt given by $dt(\dot{\epsilon}_1 - \dot{\epsilon}_0)$ will be responsible for driving the relaxation process. The differential strain could be equated to the elastic relaxation strain dQ/E where dQ is the change in secondary stress level in time dt and E is the Young's Modulus for the material. The rate equation can thus be expressed as:

$$\frac{dQ}{dt} = E(\dot{\epsilon}_1 - \dot{\epsilon}_0) \qquad (1)$$

In the case of a thin tubular structure subject to internal pressure the original reference stress $p_i/\ln(b/a)$ is very nearly equal to the mean hoop stress σ_H so that the two strain rates $\dot{\epsilon}_1$ and $\dot{\epsilon}_0$ can be taken at stress levels of $\sigma_H + Q/1.5$ and σ_H respectively.

In order to verify the postulated relation for the decay of secondary stresses, the results of applying eq. 1 were compared with the results obtained from more rigorous analysis. Several cases were analyzed using the method of successive elastic solutions to solve the creep problem associated with an internally pressurized thin tube subject to through the wall temperature gradients. Details of this method have been described by Mendelsen [4]. Tubes of inside radius 1.0" and wall thickness .12 inches were subject to internal pressure of 1000 psi. Several different temperature gradients, assumed to vary linearly across the wall thickness, were considered. In order to facilitate comparison with the approximate decay rate equation, time steps were taken in the exact solution procedure in the following manner. A time increment Δt was taken at each step given by:

$$\Delta t = \delta S_m / E(\dot{\epsilon}_1 - \dot{\epsilon}_0) \qquad (2)$$

where δ is a constant denoting the expected fractional decay in the maximum stress S_m. As could be easily seen this relation corresponds to the rate relation given by eq. 1 written in incremental form. The change in the maximum stress S_m is derived primarily from the change in the secondary stress level since the redistribution effects of the primary stress level in a thin tube are negligible. Therefore, if eq. 1 were valid, there would be a fractional decrease of δ in the maximum stress value with each time step. Thus when the maximum stress S_m is plotted on a log scale against the time step number, a straight line should result which has a decay rate of δ with each time step.

Figure 1 shows the results of this study where the maximum hoop stress computed from the rigorous analysis is plotted as a function of the time step number. Several cases were studied, with varying temperature gradients, and at different creep strain rates. The material properties were taken as those corresponding to solution annealed type 304 stainless steel for a mean tube temperature of 1100°F. An examination of the results given in Figure 1 shows that the proposed relation for the relaxation rate of secondary stress is satisfactory over a considerable range of thermal gradients and creep rates. In particular, it should be noted that when the creep rate was taken to be 1000 times the nominal rate, the time steps taken in the solution procedure decreased by approximately three orders of magnitude compared with the nominal creep rate case, while the decay rate with each time step number remained the same, as would be predicted. In summary, a rate relation has been proposed which takes into account the creep relation, the level of primary, secondary stress values and the time increment to calculate the decrease in secondary stress level. Results using this relation compare favorably with those arrived at using a more rigorous solution.

The application of the rate equation to estimate the thermal creep strain contribution for a tubular structure under internal pressure subject to cyclic temperature gradients follows directly. In order to estimate the creep strains, it is necessary to monitor the maximum stress S_m in the structure as a function of time and obtain the value of the secondary stress Q at the end of the cycle time under consideration. With the end of cycle value for Q being known, the thermal stress gradient due to the next cycle can then be imposed upon this stress state to obtain the value of secondary stress at the beginning of next cycle. The procedure can then be repeated for succeeding cycles.

The simplified analysis procedure thus consists of the following steps:

(i) Evaluate based on elastic analysis, the mean hoop stress σ_H and the secondary stress Q, due to the applied internal pressure and thermal gradient respectively.

(ii) Choose a time increment Δt given by eq. 2 where the fractional decay rate δ is assigned a specified value, generally of the order of 0.05. The strain rates $\dot{\epsilon}_1$ and $\dot{\epsilon}_0$ are based on the stress values $\sigma_H + Q/1.5$ and σ_H respectively.

(iii) Compute the maximum strain accumulated during this time interval Δt corresponding to the maximum stress level of S_m using either isochronous stress-strain curves or the creep constitutive relation. Compute the new value of the secondary stress Q and maximum stress S_m at the end of this time increment using the fractional decay constant δ.

(iv) Repeat steps (ii) and (iii) until the end of the cycle time and add the creep strain increments during each time increment thus calculated.

In applying this procedure it is convenient to assume that no further relaxation takes place once the mean hoop stress σ_H differs by less than δS_m from the maximum stress. In addition, where the time step chosen is such that it is greater than the time remaining to the end of the current cycle, eq. 1 could be directly used to arrive at the change in secondary stress level for the remainder of the time until the end of the cycle. Once the computations have been made until the end of the current cycle, the secondary stress due to the next cycle could be superimposed on the secondary stress level Q at the end of the previous cycle. The procedure can then be repeated for the next cycle.

In applying the above procedure, it is necessary to take into account the type of hardening rule to be employed with the creep relation. When the time hardening formulation is employed, the strain rates $\dot{\epsilon}_1$ and $\dot{\epsilon}_0$ are calculated based on the total accumulated time. When strain hardening formulation is employed, the strain rates are determined from the accumulated strain levels corresponding to the points in the structure where the maximum secondary stress acts and where the value of the secondary stress is zero.

APPLICATIONS

The simplified analysis procedure developed here has been applied to the case of fuel rods in an LMFBR. It is assumed that the plant is subject to daily power cycling with 100% power for 16 hours and 60% of the power for 8 hours. The mean temperature of the fuel rod remains essentially constant while the temperature gradient across the wall thickness changes with the cyclic power operation in almost direct proportion. In the present analysis, the effect of irradiation creep in relaxing the thermal stresses has been neglected and possible fuel to cladding mechanical interaction effects have not been taken into account. Eight different cases were analyzed corresponding to the hot, peak power, average power and minimum power rods, at both the top of core and core centerline locations. The hot rod operates at a higher mean temperature and includes the temperature uncertainties in the peak power rod estimated on a semi-statistical basis. The fuel rod cladding considered was of 0.115" nominal radius and 0.014" wall thickness. The thermal creep constitutive relation and other pertinent data were taken from Reference [5] corresponding to that for 20% cold worked type 316 stainless steel. The internal fission gas pressure was assumed to vary linearly between the beginning of life and end of life conditions.

Table 1 lists the mean operating temperatures, temperature gradients, and fission gas pressure for the different cases analyzed. The table also presents the results for the total effective thermal creep strain accumulated at the end of 15000 hours of operation. Both the accumulated maximum effective thermal creep strain under conditions of power cycling and that due to the internal fission gas pressure alone have been presented. It is seen that in most instances, particularly for the low power rods, the maximum effective thermal creep strain under power cycling conditions is approximately an order of magnitude higher than that arising from internal pressure alone. The difference in the thermal creep strains with and without power cycling is a function of the creep rate, and the range of secondary and primary stress levels. In general, the difference tends to be greater early in life as the contribution from the fission gas pressure loading is small and progressively becomes smaller as the secondary stress relaxes with time and the fission gas pressure loading increases. The additional strain due to the temperature cycles cannot be estimated based on a simple addition of the creep strain due to the fission gas pressure loading and an elastic strain corresponding to the maximum secondary stress range Q. For the hot rod and the peak power rod, the increased strain due to cycling is greater than that indicated by a relaxation strain of the magnitude Q/E. For the minimum power rod on the other hand the maximum effective creep strain is smaller than the relaxation strain Q/E as complete relaxation does not occur by the end of design lifetime. In all the cases studied, it is seen that enhanced thermal creep straining occurs as a result of the thermal stress contribution compared to the creep strain accumulated from the fission gas pressure loading alone. The results of the present study indicate, however, that the increased thermal creep straining due to power cycling which occurs in the absence of fuel to cladding mechanical interaction is not sufficiently high to cause loss of cladding integrity.

SUMMARY AND CONCLUSIONS

A simplified analysis procedure has been given for estimating the thermal creep strain contribution under conditions of secondary stress cycling in the elastic regime. The procedure is based on a postulated rate equation for the decay of secondary stresses which has been verified using more rigorous analysis. The application of this method to LMFBR fuel rods subject to daily power cycling operation appears to show that the increased thermal creep strains due to temperature cycling does not in itself promote loss of cladding integrity. The analysis neglects possible fuel to cladding mechanical interaction. Although the simplified analysis procedure proposed here has been applied particularly for fuel rods, it would appear to have more general applications for situations involving stress relaxation in the presence of membrane stress for thin axisymmetric shell type structures subject to through the wall temperature gradients.

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TABLE 1

Results of Thermal Creep Strain Estimates Under
Power Cycling Conditions for Various Categories Fuel Rods

Case	Mean Temperature °F	Temperature Drop Across Thickness for 100% Power °F	End of Life Pressure psi	Thermal Creep Strain due to Internal Pressure Only %	Maximum Thermal Creep Strain Under Daily Power Cycling %
Hot Rod - Top of Core Location	1279	65	931	0.73	1.17
Hot Rod - Core Center Location	1044	108	931	0.47×10^{-5}	0.3×10^{-4}
Peak Power Rod - Top of Core Location	1115	56	798	0.11×10^{-3}	0.30×10^{-3}
Peak Power Rod - Core Center Location	976	100	798	0.32×10^{-7}	0.24×10^{-6}
Average Power Rod - Top of Core Location	992	41	648	0.3×10^{-7}	0.76×10^{-7}
Average Power Rod - Core Center Location	904	74	648	0.8×10^{-10}	0.51×10^{-9}
Minimum Power Rod - Top of Core Location	919	35	445	0.35×10^{-10}	0.11×10^{-9}
Minimum Power Rod - Core Center Location	860	62	445	0.47×10^{-12}	0.44×10^{-11}

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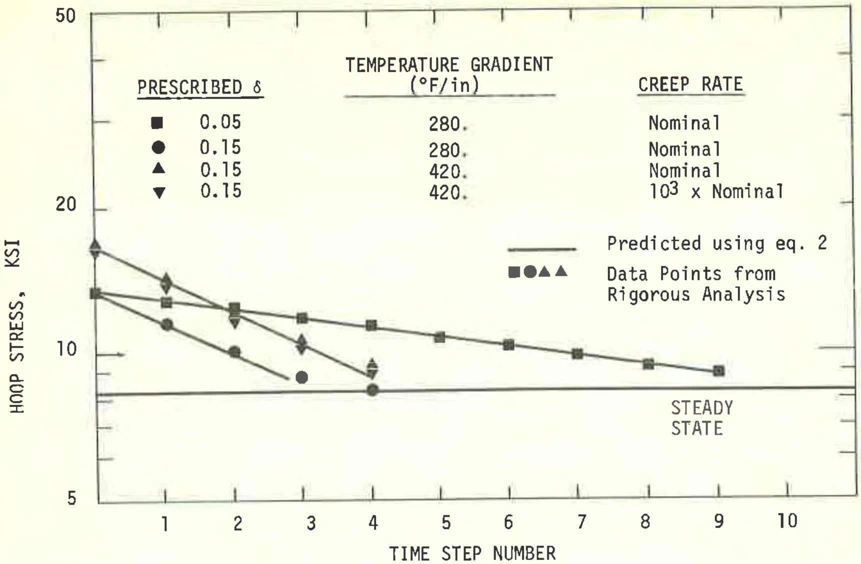

FIGURE 1 : Comparison of Predictions from Simplified Procedure with Results from Detailed Analysis