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AN ANISOTROPIC CREEP DAMAGE MODEL FOR MULTIAXIAL CYCLIC LOADING HISTORIES

Kwang-Il Ho*

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The purpose of this study is the development of an anisotropic creep damage theory within the continuum damage mechanics, applicable to creep-dominated cyclic loading histories. A damage distribution is expressd in rate form as a symmetric tensor of rank necessary to match physically measured damage. A theoretical model which expresses general anisotropic creep damage phenomena with power law cavity growth is proposed. The coupling of damage with a bounding surface cyclic viscoplasticity theory is also accomplished. Comparison with experimental results are made for weakly anisotropically damaging materals, type 304 stainless steel at 593℃. Good correlation of rupture time, secondary creep, and tertiary creep has been obtained for proportional and nonproportional, isothermal, constant isochronous nominal stress loading histories. A modification of the isochronous stress (the set of stress state which have a same rupture time) for compressive hydrostatic stress state has been offered.

Key Words: Creep, Continuum Damage, Anisotropic, Nonproportational

NOMENCLATURE

$\underline{n}$: Unit vector normal to the unit sphere
w : Scalar damage distribution
$\underline{\sigma}$: Cauchy stress tensor
$\underline{s}$: Deviatoric stress tensor
$\underline{\phi}$: Damage effect tensor
$\underline{\alpha}$: Deviatoric backstress tensor
$\bar{\sigma}$: Effective Cauchy stress
$\bar{\alpha}$: Effective backstress
$\underline{\varepsilon}$: Strain rate tensor
$\underline{n}\otimes\underline{n}$: Outer product between two vectors

1. INTRODUCTION

When external forces are applied to a structures, the resulting strains are comprised of elastic, plastic, and time-dependent components when thermal strains are subtracted. At high temperatures (approximmately greater than 40% of the melting point), creep is an important phenomenon.

In early investigations of creep, researchers assumed that the strain rate was related only to the state of stress. In using this approach, however, it was very difficult to describe the behavior of damaged materials and the evolution of grain boundary damage which induces creep rupture. Hence, in a new analytical approach, damage parameters were used to describe the creep behavior of materials with significant levels of damage(e.g. tertiary creep stage).

By now, the theoretical application to isotropic creep damage has been well developed. This approach usually involves only a scalar damage parameter. However, in practical applications, damage phenomena are anisotropic and must be investigated experimentally. Physically, creep damage has a directional distribution which must be accounted for.

In case of materials which obey a maximum tensile principal stress rupture criterion, e.g. copper, the lifetime of a specimen would be increased by rotating the applied principal stresses. The reason is that the damage growth rate on one set of planes would decrease after the rotation and damage growth commence on a fresh set of planes perpendicular to the new maximum principal stress direction. Theoretically, if a torque reversal is made close to the steady load lifetime for combined axial-torsional loading, and if no interaction takes place between the damage planes, then a lifetime of twice steady load lifetime should be achieved. Hence, the damage accumulation is clearly anisotropic in such a case.

In case of materials which obey a maximum effective stress rupture criterion, e.g. aluminum alloys, the damage rates should not depend upon the degree of proportionality of the loading. So the lifetime for a steady load test and reversed torsion test should be identical. Hence, the damage accumulation is essentially isotropic.

The purpose of this study is the development of an anisotropic creep damage theory and the establishment of appropriate evolution equations of damage rate and strain rate. In this study, various damage theories are reviewed. A theoretical model which expresses general anisotropic creep damage phenomena associated with power law governed cavity growth is proposed. The coupling of damage with cyclic viscoplastic deformation is also accomplished. Comparisons with experimental results are made for weakly anisotropically damaging materials.

2. SUGGESTION OF THEORETICAL MODEL

2.1 Creep Damage Growth Law

The approach taken by Murakami and Ohno(Murakami, 1983, Murakami and Ohno, 1980) results in a second rank tensor damage distribution regardless of the nonproportionality of the applied loading history. Some

*R & D Center, Korea Power Engineering Company, Seoul 135-090, Korea

experiments(Hayhurst and Leckie, 1985, Oyata, 1982, etc.), however, revealed that the creep damage distribution for some materials is highly dependent on principal stress orientation. Leckie and Onat(1980, 1983) suggest stronger conditions, based on damaged material symmetry arguments, which require that a vectorial distribution of damage be even with respect to $\underline{n}$, i.e.

$$\omega(\underline{n})=\omega(-\underline{n}) \tag{1}$$

where $\underline{n}$ is a unit vector normal to the unit sphere and $\omega(\underline{n})$ is the distribution of damage.

Following the development of Leckie and Onat(1980, 1983), we define the damage distribution $\omega(\underline{n})$ in terms of a set $\underline{\Gamma}$ of even rank, irreducible tensors obtained from the distribution of damage on the unit sphere. Effective stress $\underline{S}$ may be expressed as a second rank tensor function of Cauchy stress $\underline{\sigma}$ and a fourth rank operator $\underline{M}(\underline{\Gamma})$, i.e.

$$\underline{S}=\underline{S}[\underline{M}(\underline{\Gamma}),\ \underline{\sigma}] \tag{2}$$

In this work, we make the simplifying assumption that the principal axes of the Cauchy stress and effective stress coincide, which may be true for proportional loading even up to large cavity volume fractions. However, for nonproportional loading such an assumption suggests a limitation to relatively small caity volume fractions to ensure that the rotation of the effective stress with respect to the Cauchy stress is suitably small. With this assumption we may express $\underline{M}$ in the principal stress coordinate frame as

$$M_{ijkl}(\underline{\Gamma})=\phi_{ij}(\underline{\Gamma})\,\delta_{ik}\delta_{jl}\,(\text{no sum on } i \text{ and } j) \tag{3}$$

$$\underline{\phi}=\Omega^{(j)}\ \underline{n}^{(j)}\otimes\underline{n}^{(j)} \tag{4}$$

where $\underline{\phi}$ is the damage effect tensor with principal components $\Omega^{(j)}$ and eigenvectors $\underline{n}^{(j)}$ collinear with those of $\underline{\sigma}$. $\underline{\phi}$ is viewed as an approximation of the intensification effect of grain boundary damage on the current principal stresses. We define the growth rate of ω as a function of $\underline{\phi}$ and $\underline{\sigma}$ in the following simple way:

$$\begin{aligned}\dot{\omega}(\underline{n}) &= \xi(\sigma^*)\,[\eta\chi^{(1)}\{\Omega^{(1)}\}^{\lambda(\sigma*)} \\ &\quad+(1-\eta)\textstyle\sum\chi^{(j)}\{\Omega^{(j)}\}^{\lambda(\sigma*)} \\ &\quad\times\{\underline{n}\cdot\underline{n}^{(j)}\otimes\underline{n}^{(j)}\cdot\underline{n}\}\{\underline{n}\cdot\underline{n}^{(j)}\}^{2P}] \\ &= \dot{\omega}(\underline{n})_{\text{isotropic}}+\dot{\omega}(\underline{n})_{\text{anisotropic}}\end{aligned} \tag{5}$$

anisotropic damage rate tensor $\dot{\underline{\Gamma}}$ of rank $2(P+1)$, i.e.

$$\begin{aligned}\dot{\underline{\Gamma}} &= \xi(\sigma^*)\,[\eta\chi^{(1)}\{\Omega^{(1)}\}^{\lambda(\sigma*)}\bigotimes_{i=1}^{p+1}\underline{I} \\ &\quad+(1-\eta)\sum_1^3\chi^{(j)}\{\Omega^{(j)\lambda(\sigma*)}\bigotimes_{i=1}^{2(p+1)}\{\underline{n}^{(j)}\}]\end{aligned} \tag{6}$$

where ξ and λ are functions of the isochronous stress σ^*, $\underline{n}^{(j)}$ is the unit vector in the jth principal stress direction, P is an integer of the order of the anisotropic damage distribution, outer product operator was multiplied by $(p+1)$ and $2(p+1)$ times respectively, and η is the fraction of damage rate in the $\underline{n}^{(1)}$ direction which is isotropic. Factor $\chi^{(j)}$ excludes contribution of compressive principal stresses to the damage rate, i.e. $\chi^{(j)}=0$.

$$\chi^{(j)}=\langle\underline{n}^{(j)}\cdot\frac{\sigma}{|\sigma_j|}\cdot\underline{n}^{(j)}\rangle \tag{7}$$

where σ_j are the ordered principal stresses with $\sigma_1>\sigma_2>\sigma_3$, and the $<>$ is Macauley bracket ($\langle Y\rangle=Y$ is $Y>0$, $\langle Y\rangle=0$ otherwise).

Among several proposed $\Omega^{(j)}$, one possibility is as follow:

$$\Omega^{(j)}=[\frac{1}{(1-\omega(\underline{n}^{(j)})^2)}+\frac{2}{(1-\eta\ \omega(\underline{n}^{(j)}))^2}]^{1/2} \tag{8}$$

Essentially, this equation assumes that stress intensification effect associated with each of the damage values $\omega(\underline{n}^{(j)})$ is represented by a symmetric term but dependent on η. The scalar multiplier $\Omega^{(j)}$ directly affects evolution of the ω distribution, as seen in Eq.(5). It should be noted that for isotropic hardening, the damage rate equation assumes the classical Kachanov-Rabotnov form. It is necessary to intorduce a specific definition for the isochronous stress discussed by Trampczynski and Hayhurst(1980) and Lemaitre and Chaboche (1982, 1975). Our suggested isochronous stress is

$$\sigma^*=(\frac{3}{2})S_1[\frac{2}{3}\ \frac{\sigma_e}{S_1}]^a\exp[b\{1+f(J_1)\langle-J_1/|J_1|\rangle\}(\frac{J_1}{S_s}-1) \tag{9}$$

$$S_1=\sigma_1-(1/3)\,\sigma_{kk},\quad \sigma_e=[(3/2)\underline{s}:\underline{s}]^{1/2} \qquad (10),\ (11)$$

$$\underline{s}=\underline{\sigma}-(1/3)\,\sigma_{kk}\underline{I},\quad S_s=[\sigma_1{}^2+\sigma_2{}^2+\sigma_3{}^2]^{1/2},\quad J_1=\sigma_{kk} \qquad (12,\ 13,\ 14)$$

It should be noted this from of the isochronous stress is a slightly altered form of that proposed by Huddleston (1984). This form of the isochronous stress has been rather thoroughly supported by a variety of biaxial creep experiments on tubular specimens of type 304 stainles steel at 593℃ at ORNL(Oak Ridge National Laboratory), including axial tension, equi-biaxial tension, internal pressure, torsion, axial tension and torsion, and axial compression and torsion.

Obviously, the integrated damage distribution $\omega(\underline{n})$ will depend on whether the loading history is proportional or nonproportional. Rotation of the principal stress eigenvectors will in general result in multiple peaks in the damage distribution with respect to a fixed material coordinate system.

Another key element of the damage formulation is the rupture criterion. If a stress level-dependent rupture criteria is adopted, than the damage at rupture is not constant and the time fraction at any given damage level depends on the isochronous stress for proportional loading. A stress level-dependent rupture criterion is more difficult to implement since experimental investigation of the damage distribution at different stress levels is quite involved. The logical approximation to the physically more precise stress level-dependent rupture criterion is the first approach, i.e. the assumption of a constant damage at failure. Therefore, from a practical viewpoint, the use of a constant damage at rupture is likely to be sufficient, especially in view of the inherent scatter in creep rupture tests. Hence, the rupture criterion $\omega_{\max}=\max\ \omega(\underline{n})=1$ is selected in this work.

3. RATE-DEPENDENT BOUDING-SURFACE FORMULATION ; TYPE 304 STAINLESS STEEL AT 593℃

3.1 Theoretical Equation

To describe rate-dependent, high-temperature creep-plasticity response for histories which involve both creep and cyclic plasticity, it is necessary to introduce a theory capable of modeling a wide range of behavior. For multiaxial cyclic plasticity, it has previously been demonstrated that a bounding surface approach(Dafalias, 1981, McDeowell, 1985, etc.) provides very good correlation of nonproportional deformation behavior. Since such behavior is of concern to the current investigation, a new strain-hardening model based on a rate-dependent bounding surface with a Mroz translation rule for backstress is adopted. Key features of this theory include isotropic hardening reflected through growth of the bounding surface rather than a scalar parameter in the flow rule, and rate-dependence of the backstress evolution even at high strain rates. In McDowell study (1985), the backstress was defined as the kinematic hardening parameter in case of general nonproportional loading. These features contrast with conventional unified creep plasticity models(Chan, 1984, Hart, 1976, etc.). Rate-dependence is reflected primarily through bounding surface dependence on overstress, strain-hardening is reflected through growth of the bounding surface, and smooth yielding response is obtained through use of the Mroz distance vector in the backstress hardening rate coefficient. Briefly, the damage-coupled bounding surface model can be stated in multiaxial form as

$$\underline{\dot{\varepsilon}}^n = (3/2) K < \bar{\sigma} D - K_o >^n \exp(Z < \bar{\sigma} D - K_o >^{n+1}) (\underline{s} - \underline{\alpha}) \sqrt{\sigma} \tag{15}$$

$$\bar{\sigma} = [(3/2)(\underline{s} - \underline{\alpha}) : (\underline{s} - \underline{\alpha})]^{1/2}, \quad \bar{\alpha} = [(3/2) \underline{\alpha} : \underline{\alpha}]^{1/2} \qquad (16),\ (17)$$

Here $\underline{s}$ and backstress $\underline{\alpha}$ are deviatoric stress, and we have defined K_o=constant. The inelastic strain $\underline{\varepsilon}^n$ includes both conventional creep and plastic strain as in other unified theories. The effective overstress and backstress are denoted as $\bar{\sigma}$ and $\bar{\alpha}$, respectively, The exponential term was proposed by Nouailhas(1987) for description of high strain rate events. Note that the effect of damage is reflected by a multiplicative factor D. Hence, we define the factor D as

$$D = 1 + C + \Psi^m \tag{18}$$

where Ψ is defined as the average value of $\omega(\underline{n})$ along the unit sphere area and C and m are material constants.

The competition between hardening and static thermal recovery terms in the backstress rate equation is introduced in this bounding surface formulation in the following way:

$$\underline{\dot{\sigma}} D = H(\bar{\alpha} D,\ \delta) \| \underline{\dot{\varepsilon}} \| \underline{\nu} - R(\bar{\alpha} D) \underline{\sigma} D \tag{19}$$

$$H(\bar{\sigma} D,\ \delta) = \beta_o + \beta_1 \exp(-\beta_4 < 1 - \beta_3 \left(\frac{\delta}{R^*}\right) >^{\beta_5}) + \beta_2 \exp(-\beta_6 \bar{\alpha}^* D) \tag{20}$$

$$\delta = (3/2)^{1/2} \| (2/3)^{1/2} R^* \underline{N} - \underline{s} \|, \quad \underline{N} = (\underline{s} - \underline{\alpha} / \| \underline{s} - \underline{\alpha} \| \qquad (21),\ (22)$$

$$\bar{\alpha}^* = (R^* - \delta - \bar{\sigma}) \tag{23}$$

$$R(\bar{\alpha} D) = \beta_7 \exp(-\beta_8 \bar{\alpha} D)(\bar{\alpha} D)^{\beta_9} \tag{24}$$

The radius of the bounding surface, R^*, evolves with accumulated plastic strain(creep hardening) and responds through the effective overstress to changes in inelastic strain rate, i.e.

$$R^* = \beta_{10}\ \rho [1 + \beta_{12} \cdot \bar{\sigma}^{\beta_{13}}] \tag{25}$$

$$\dot{\rho} = \beta_{11} (\rho_{mf} - \rho)(2/3)^{1/2} \| \underline{\dot{\varepsilon}}^n \| \tag{26}$$

The directional index, $\underline{\nu}$, for the backstress hardening rate is a rate-dependent Mroz form(Mroz, 1976). In this formulation K, K_0, Z, n, c, m, β_1, $\cdots\beta_{13}$, and ρ_{mf} are isothermal material constants. Non-isothermal generalization can be achieved primarily by invoking temperature dependence of the backstress recovery term(Murakami and Ohno, 1980) and some of the constants, although this is not necessary in the current isothermal work.

3.2 Determination of Material Constants

All material constants were determined from uniaxial creep tests found in the literature and by examination of ruptured uniaxial specimens. Constants a and b in isochronous stress level were given by Huddleston (1984) for type 304 stainless steel at 593℃ as a=1.086, b=0.289.

Interrupted creep tests are generally necessary to assess exponent λ in Eqs. (5, 6) at a given isochronous stress level; λ can also be determined in an approximate way by periodically unloading from the creep curve (Chaboche, 1982, Lemaitre and Chaboche, 1975) or by matching the integrated damage-coupled creep Eqs.(15~26) with observed onset of tertiary response assuming $m=1$. The value $m=1$ arises from the Murakami study (1983). Stress exponent k is easily identified as the slope of the log(t_R) vs. log(σ) curve obtained from uniaxial tests. issotropic damage fraction η is identified as the ratio of the transverse damage to the longitudinal damage in a uniaxial tests, and is identified by quantitative metallographic techniques. As the isochronous stress level of this study, cavity growth is governed by matrix power law creep. Hence, we define

$$\xi(\sigma^*) = B[\sigma^*]^k \tag{27}$$

Once k is known, coefficient B can be found at the isochronous stress level associated with λ by integrating and matching rupture times from uniaxial tests with the assumed rupture criterion $\omega_{max}=1$. For an isotropic stress of 176.1 MPa at 593℃, the constants are $b=2.71\times10\text{sec}^{-1}$ and $\lambda=4.8$. Units of stress and damage rate are MPa and sec^{-1}, respectively. Constants independent of isochronous stress include k=8.5551 and η=0.61. Quantitative evaluation of the grain boundary damage distribution for these biaxial experiments revealed that the damage distribution is suitably described by a second rank tensor (McDowell and Ho, 1986) which sets $P=0$;η was also estimated by quantifying grain boundary metallographs. Exponent λ was estimated by matching the tertiary response of uniaxial tests with the integrated damage-coupled creep equations.

Strain rate sensitivity data of Steichen (1976) for annealed type 304 stainless steel was used to determine constants β_{10}, β_{12}, and β_{13}. Constant K_o selected as representative of initial yielding of the annealed material; Constants K and n were selected on the basis of the relatively high ratio between backstress magnitude and deviatoric stress magnitude observed in nonproportional biaxial creep experiments (Oyata, Delobelle and Meret, 1982) on a somewhat similar material. Constats $\beta_0,\cdots\cdots\beta_6$, ρ_{mf}, and β_{11} were determined from data obtained by Corum (1974) on the same ORNL reference heat of annealed material at 593℃. Constants β_7, β_8, and β_9 were determined after the hardening terms were established by fitting the secondary creep rate versus stress relationship established from numerous uniaxial tests at ORNL at 593℃. The damage coefficient C was obtained with the assumption $m=1$ by fitting the tertiary portion of the creep response. In summary, the material constats at 593℃ for the damage-coupled creep-plasticity model are:

$K=5\times10^{-48}$, $K_0=13.8$, $n=30$, $\beta_0=1104$, $\beta_1=6.9\times10^6$
$\beta_2=1044$, $\beta_3=1,18$, $\beta_4=23.16$, $\beta_5=1.25$, $\beta_6=0.0196$
$\beta_7=1.55\times10^{-19}$, $\beta_8=-0.0207$, $\beta_9=5.088$, $\beta_{10}=0.0037$
$\beta_{11}=7$, $\beta_{12}=0.225$, $\beta_{13}=1.91$, $\rho_{mf}=517$, $\rho_O=145$
$C=0.32$, $m=1$, $Z=0$,

where the units of stress are in MPa and time in sec.

3.3 Discusson of Results

Model predictons and experimental data (McDowell and Ho, 1986) for dfferent biaxial creep experiments are shown in Figs. 1, 2, 3, 4. In these figures, inelatic axial and shear strain components which were defined in Eqs. 15~26 are plotted versus time in addition to the axial and shear stress history which are nominal values based on initial dimensions. The shear stress was applied at the bottom of each specimen in ether a clockwise or counter-clockwise sense viewng down the specimen longitudinal axis. It sould be noted that the isochronous stress for all specimens is constant at $\sigma^*=176.15$ MPa. Hence, differences in rupture time should be attributable to nonproportionality of loading, neglecting the possibility of reversal of plastic strain. Specimens GT-1 and GT-2 were subjected to identical loading histories apart from the sign of shear stress. The loading history of specimen GT-3 involved a reversal in sign of the shear stress at 456 hours and the loading history of specimen GT-6 involved a repeated reversal(cyclic loading).

It is observed that a rupture time of 998 hours is predicted for Specimen GT-1 and GT-2; the actual values are 892 hourse and 1173 hours, respectively. The inelastic behavior and rupture time is well predicted for proportionally loaded Specimen GT-1 and GT-2. It is noted that there is the excellent agreement of the onset of tertiary creep and magnitude of creep strain at rupture between theory and experiment.

Also note that the slope of the predicted creep rate is not infinite at rupture, which is confirmed by the experiments. The actual grain boundary microcrack damage at rupture is significantly less than for specimen GT-1 and GT-2, with a maximum damage of max $\omega(\underline{n})=\omega(\underline{n}^{(1)})=0.28$ in the maximum tensile principal stress direction. This result provides confirmation that the rupture event is a process of unstable linkage and propagation of grain boundary cracks after a critical level of grain boundary damage is reached. It is important to note that the rupture criterion used in this calculation, max $\omega(\underline{n})=1$, may be replaced by any other, more realistic criterion such as max $\omega(\underline{n})=0.25$, provided that the coupling constants C amd m in the creep strain rate equation are determined in conjunction with this criterion. It is concluded that the even rank damage tensor approach of this study offers an improvement on prior isotropic damage models by assigning magnitude and direction of creep damage for proportional loading.

The purpose of experiment GT-3 was to evaluate the capability of the tensor damage model to predict direction

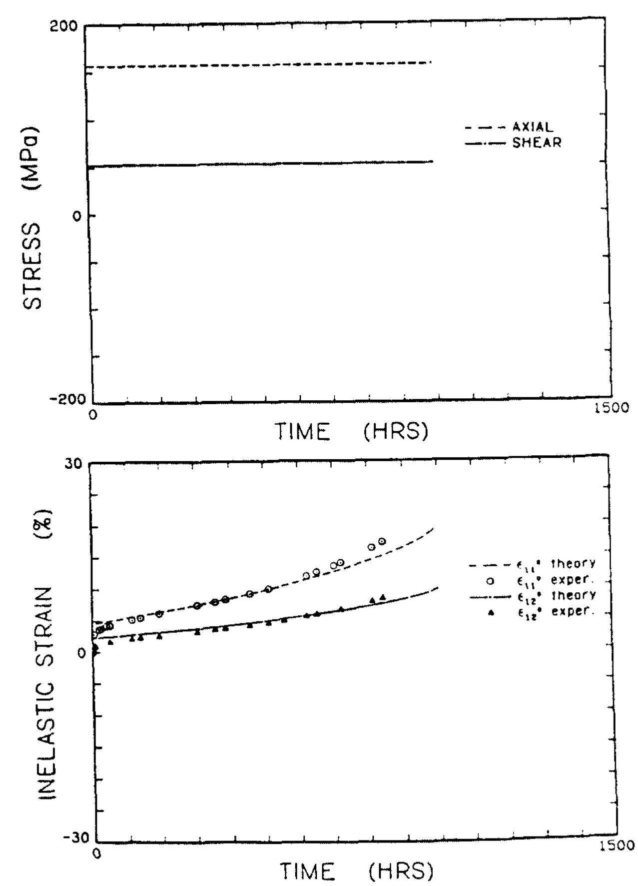

Fig. 1 Type 304 stainless steel at 593℃ : applied biaxial nominal stress history (top) and predicted versus experimental inelastic strains (bottom) for GT-1. Actual and predicted rupture times are 892 hr and 998 hr, respectively, Theoretical strains are plotted by using, Eqs. (15~26)

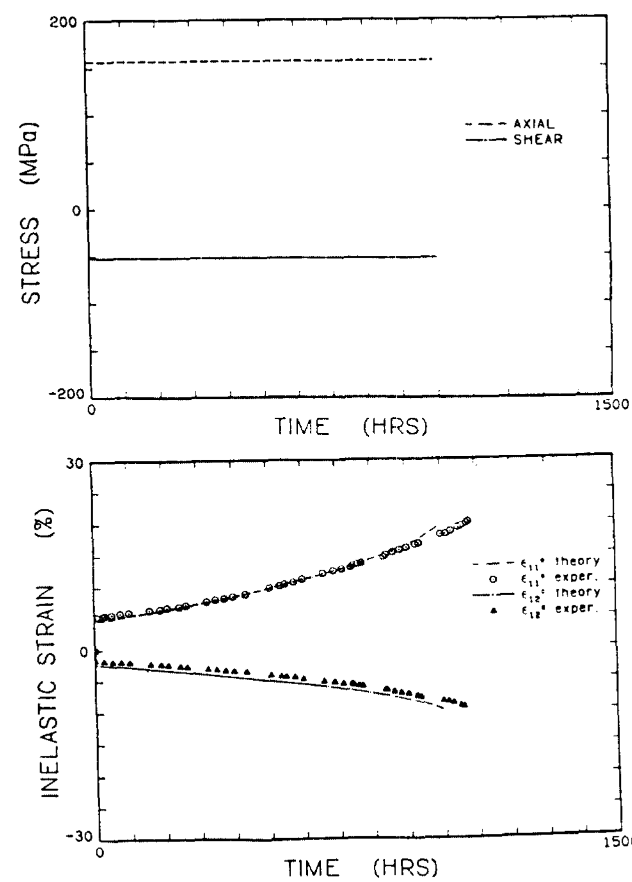

Fig. 2 Type 304 stainless steel at 593℃ : applied biaxial nominal stress history (top) and predicted versus experimental inelastic strains (bottom) for GT-2. Actual and predicted rupture times are 1173 hr and 998 hr, respectively, Theoretical strains are plotted by using Eqs.(15~26).

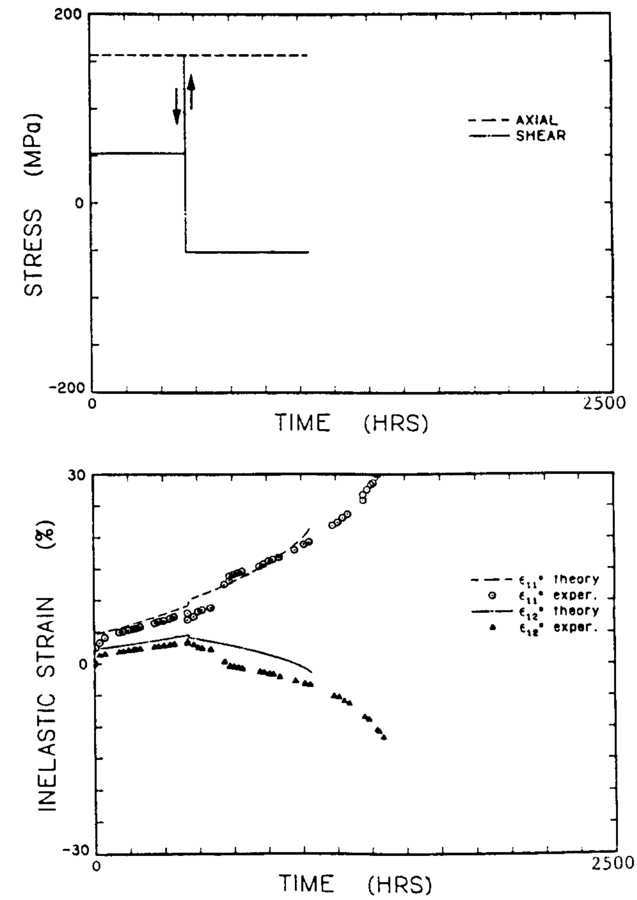

Fig. 3 Type 304 stainless steel at 593℃ : applied biaxial nominal stress history (top) and predicted versus experimental inelastic strains (bottom) for GT-3. Actual and predicted rupture times are 1398 hr and 1043 hr, respectively, theoretical strains are plotted by using Eqs.(15~26).

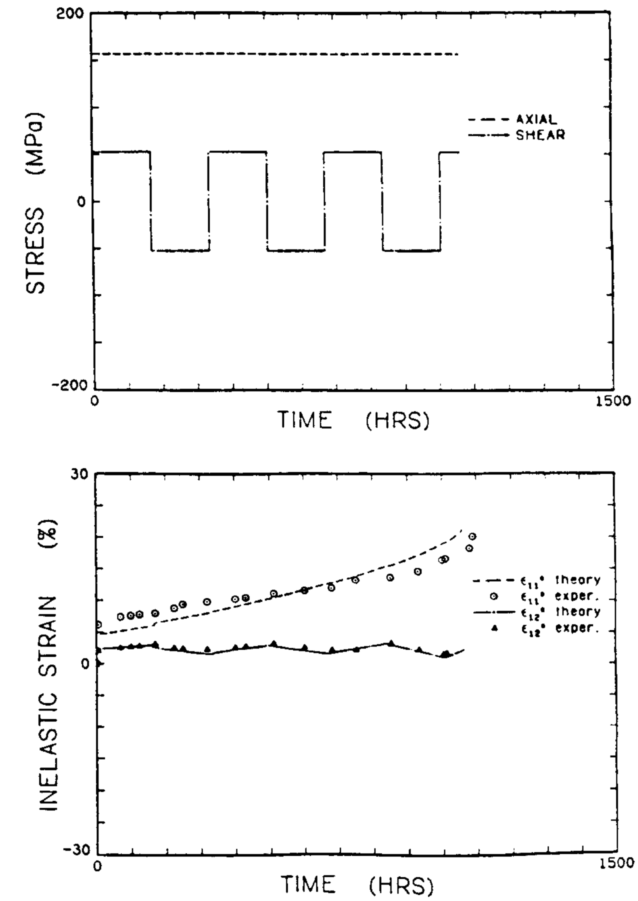

Fig. 4 Type 304 stainless steel at 593℃ : applied biaxial nominal stress history (top) and predicted versus experimental inelastic strains (bottom) for GT-6. Actual and predicted rupture times are 1088 hr and 1060 hr, respectively, theoretical strains are plotted by using Eqs.(15~26).

and magnitude of accumulated creep damage in addition to rupture time for nonproportional loading histories. As shown in Fig.3, sepcimen GT-3 was subjected first to a counterclockwise shear stress and an axial stress at the same isochronous stress level as specimens GT-1 and GT-2 ; after 456 hours, however, the shear stress was reversed while maintaining the same isochronous stress level. A maximum principal stress rotation of 34° resulted from this shear stress reversal. It is noted first that the predicted rupture time of 1043 hours is significantly lower the observed 1398 hours. Also, the measured orientation of maximum damage is 13 from the predicted orientations. This result indicates that the actual rotation of the damage tensor was not as great as the predicted by the theory. Also, note that the magnitude of damage at rupture is less than in proportionally loaded specimen GT-1 and GT-2.

It is observed that a rupture time 1060 hours is predicted for Specimen GT-6 ; the actual value is 1088 hours. The correlation obtained for GT-6, a somewhat complex creep-dominated cyclic loading hostory, is quite good.

4. CONCLUSIONS

The present study has demonstrated that anisotropic creep damage can be successfully treated within the framework of contimuum damage mechanics, even for creep-dominated cyclic loading histories typical of nuclear components. A damage distribution with even symmetry has been introduced on the unit sphere ; this distribution evolves in rate form as a symmetric tensor of rank necessary to match physically measured damage distributions. The approach is motivated by the treatment of even rank tensor distributions forwarded by Leckie and Onat (1980, 1983), and contains as a subset the specific tensorial definitions of damage adopted in the anisotropic theories of Chaboche (1979, 1981) and Murakami and Ohno (1983, 1980).

A general form of coupling with damage has been suggested for an internal variable inelasticity framework and specific forms have frameworkigated for type 304 stainless steel at 593℃ with the assumption of small cavity volume fractions. A novel bounding surface cyclic viscoplasticity theory has been offered for multiaxial creep-plasticity deformations as one of the these specific forms. Good correlation of rupture time, secondary creep, and tertiary creep has been obtained for proportional and nonproportional, isothermal, constant isochronous nominal stress loading histories. A modification of the isochronous stress compressive hydrostatic stress states has been offered.

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REFERENCES

Chaboche, J.L., 1979, "Le Concept de Contrainte Effective Applique al'elasticite et a la Viscoplasticite en Presence d'un Endommagement Anisotrope," Coll. Euromech, 115, Grenoble.

Chaboche, J.L., 1981, "Continuous Damage Mechanics-A Tool to Describe Phenomena Before Crack Initiation," Nuclear Engr. and Design, Vol. 64, pp. 233~247.

Chaboche, J.L., 1984, "Anisotropic Creep Damage in the Framework of Continuum Damage Mechanics," Nucl. Engr. Des., 79, pp. 309~319.

Chaboche, J.L., 1986, "Continuum Damage Mechanics: Present State and Future Trends,"ONERA T.P. n1986-53, Seminaire Internatona sur l'approche Locale de la Rupture, Moret-sur-Loing,June 3~5.

Chan, K.S., Bodner, S.R., Walker, K.P. and Lindholm, U.S., 1984, "A Survey of Unified Cosititutive Theories," Proc. 2nd Symp. on Nonlinear Constitutve Relatons for High Temperatue Applications, NASA Lewis Research Center, June 13 ~15.

Corum, J.M., 1974, "Material Property Data for Elastic Plastic Creep Analysis of Stiffened Sham-Lag Panel," Appendix *C* to Report #ORNL-SUB-3754-1, By R.L.Egger, p. 222.

Dafalias, Y.F., 1981," The Concept and Applicaton of the Bounding Surface in Plasticity Theory," Physical Non-Linearities in Structural Analysis, Eds. J. Hult and J. Lemaitre, IUTAM Symposium,Senlis, France, Springer Verlag, pp. 56~63.

Dafalias, Y.F. and Popov, E. P., 1975, "A Model of Nonlinearly Hardening Materials for Complex Loading," Acta Mechanica, Vol.21, pp. 173~192.

Hart, E.W., 1976, "Constitutive Relations for Non-Elastic Deformations of Metals", J. of Eng. Mat. and Tech., Trans. ASME, Vol. 98.

Hayhurst, D.R., Leckie, F.A. and McDowell, 1985, "Damage Growth Under Nonproportional Loading," ASTM STP 853.

Huddleston, R.L., 1984, "An Improved Multiaxial Creep-Rupture Strength Criterion," ASME J.Pressure Vessels and Piping, paper 84-PVP-106.

Krieg, R.D., Swearengen, J.C. and Rohde, R.W., 1978, "A Physically-Based Internal Variable Model for Rate-Dependent Plasticity," Inelastic Behavior of Pressure Vessel and Piping Components (Eds. Chang and Krempl), PVP-PB-028, ASME, pp. 15~28.

Lagnebong, R., 1972 "A Modified Recovery-Creep Model and its Evaluation," Metal Science Journal, Vol 6, pp. 127 ~133.

Leckie, F.A. and Onat, E.T., 1980, "Tensorial Nature of Damage Measuring Internal Variables," Physical Non-Linearities in Structural Analysis, IUTAM, pp. 140~155(Eds. Hult and Lemaitre).

Leckie, F.A., 1983, "The Constitutive Equations for High Temperatures and Their Relationship to Design," Proc. Int. Conf. on Constitutive Laws for Engineering Materials, Eds. Desai and Gallagher, Univ. of Arizona, Tucson, p. 93.

Lemaitre, J. and Chaboche, J.L., 1975, "A Non-Linear Model of Creep Fatigue Damage Cumulation and Interaction," Mechanics of Visco-Plastic Media and Bodies, Ed. Jan Hult, Springer, Berlin, pp. 297~301.

Lemaitre, J. and Chaboche, J.L., 1978, "Aspect Phénoménologique de la Rupture par Endommagement," J. de Mecanique Appliquée, Vol.2, No.3, pp. 317~365.

McDowell, D.L. 1985, "A Two Surface Model for Transient Nonproportional Cyclic Plasticity: Part 1," ASME J. Applied Mechanics, Vol. 52, pp. 298~302.

McDowell, D.L., 1985, "A Two Surface Model for Transient Nonproportional Cyclic Plasticity: part 2," ASME J. Applied Mechanics, Vol, 52, pp. 303~308.

McDowell, D.L., Ho, K.I. and Stalley, J., "An Anisotropic, Damaged-Coupled Viscoplastic Model for Creep-Dominated Cyclic Loading," presented at Third Int. Symp. on Nonlinear Fracture Mech., Knoxville, TN, Nov. 1986.

Miller, A., 1976, "An Inelastic Constitutive Model for Monotoic, Cyclic, and Creep Deformation," ASME J. of Eng. Mat. and Tech., Vol. 98, pp. 97~113.

Mroz, Z., 1976, "An Attempt to Describe the Behavior of Metals Under Cyclic Loads using a More General Workhardening Model," Acta Mechanica, Vol. 7, pp. 199~212.

Murakami, S. 1983, "Notion of Continuum Damage Mechanics and its Application to Anisotropic Creep Damage Theory," ASME J. of Engineering Materials And Technology, Vol. 105, pp. 99~105.

Murakami, S., and Ohno, N., 1980, "A Continuum Theory of Creep and Creep Damage," Creep in Structures, IUTAM, pp. 422~444(Eds. ponter and Hayhurst)

Nouailhas, D., 1987, "A Viscoplastic Modelling Applied to Stainless Steel Behavior," in Constitutive Laws for Engr. Materials: Theory and Applications, Vol Ⅱ, (Eds. Desai, krempl), Kiousis and Kundu, Tucson, Arizona, USA, pp. 717 ~724.

Oyata, C., Delobelle, P. and Meret, A., 1982, "Constitutive Equations Study in Biaxial Stress Experiments," ASTM J. of Enger. Materials and Technology, Vol. 104, pp. 1~11.

Ponter, A.R.S. and Leckie, F.A., 1976, "Constitutive Relationships for the Time-Deformation of Metals," J. of Eng. Mat. and Tech., Trans. ASME, Vol. 98.

Pugh, C.E. and Robinson, D.N., 1987, "Some Trends in Constitutive Equation Model Development for High-Temperature Behavior Model Development for High-Temperature of Fast-Reactor Structural Alloys," Nuc. Engr. and Design, Vol. 48, pp. 269~276.

Steichen, J.M., 1976, "Tensile Properties of Thermally Exposed Type 304 Stainless Steel", ASEM J. of Engr. Mat. and Tech, pp. 357~360.

Trampczynski, W.A. and Hayhurst, D.R., 1980, "Creep Deformation and Rupture Under Non-Proportional Loading," Creep in Structures, IUTAM, pp. 388~405. (Eds. Ponter and Hayhurst).

Trampczynski, W.A. and Hayhurst, D.R., and Leckie, F.A., 1980, "Creep Rupture of Copper and Aluminum Alloy under Non-Proportional Loading," Univ. Leicester, Dep. of Engr., Report No. 80-8.

Tseng, N.T. and Lee, G.C, 1983, "Simple Plasticity Model of the Two-Surface Type," ASCE Journal of Engineering Mechanics, Vol. 109, No. 3, pp. 795~810.

Walker, K.P., 1981, "Research and Development Program for Nonlinear Structural Modeling with Advanced Time-Temperature Dependent Constitutive Relationships," NASA Report CR-165533, NASA Lewis Research Center.