An Analysis of the Effect of Environmental Damage on the Creep Fracture of Inconel Alloy X–750

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ABSTRACT

A mathematical model which allows for the analysis of the effect of environmental damage, caused by prior exposure of test samples to high temperature oxygen environment, on the creep life and ductility has been developed. This model developed specifically for nickel-base superalloys considers two forms of environmental damage one characterized by a profusely cavitated region and the other a free region of poor creep strength. The predictions of this model are discussed in detail and also compared with the experimental results of Pandey, Taplin, Ashby and Dyson (1986) on Inconel X-750 alloy.

KEYWORDS

Creep fracture;environmental effects;nickel base super alloys;oxidation-creep interaction

INTRODUCTION

The creep behaviour of nickel and nickel-base alloys are markedly affected by prior exposure to an oxygen environment (Bricknell and Woodford, 1982; Woodford and Bricknell, 1983). A comprehensive study of such an environmental damage, in an Inconel alloy X-750 in this particular case, has been carried out by Pandey and co-workers (Pandey, Dyson and Taplin, 1984; Pandey, Taplin, Ashby and Dyson, 1986). These investigators noted that the prior exposure of samples of Inconel alloy X-750 to oxygen (at $3x10^{-2}$ Pa pressure; upto 67 hrs. at 1150 °C) reduced the subsequent creep life and ductility by factors of around 10 and 15 respectively while the minimum creep rate increased by a factor of about 20. In addition, metallographic examination of prior exposed but uncrept samples of Inconel X-750 revealed the presence of two forms of damage -a profusely cavitated and a much smaller region devoid of γ' precipitates. The depth to which both the damage front extended into the sample from the surface increased with increasing time of prior exposure obeying parabolic kinetics (Pandey et al. 1986).

Pandey et al. 1986 have proposed a mathematical model which allows for the presence of the profusely cavitated region alone and which further assumes that this zone has no load-bearing capacity. On the basis of such a model, these authors have adequately explained the observed acceleration in the minimum creep rate as a function of duration of prior exposure to oxygen. However, the above model due to Pandey et al. has not been extended to predict the dramatic reduction in creep life and ductility resulting from the prior expo sure of the sample to oxygen. In addition, the effect of microstructural damage in the form of γ'-free region (caused by prior exposure) on the creep behaviour has not been modelled by the above investigators. The main objective of the present paper is to develop a mathematical model for intergranular creep fracture which allows for the interaction between the bulk cavitational damage created in the sample during creep testing with the cavitational and microstructural damage caused by exposure to high temperature oxygen environment prior to creep testing. The predictions of such a model with regard to the reduction in creep life and ductility as a function of duration of prior exposure are also compared with the data of Pandey et al. (1986).

THE FORMULATION OF THE MATHEMATICAL MODEL

The modelling of the effect of environmental damage on creep properties requires as the first step, the modelling of creep fracture in the undamaged test sample (i.e heat treated in bar form and then machined). Only then, the extent of deterioration in the creep properties as a function of prior exposure can be properly assessed.

Creep Fracture of Undamaged Samples: Following the approach pioneered by Dyson (1976) and refined by Cocks and Ashby (1982) the test sample undergoing the creep test can be divided into cavitated regions and uncavitated regions. These regions can be delineated on the basis of the definition that the grains adjoining any cavitated facet is considered cavitated. Let g be the volume fraction of the cavitated region. In that event, force equilibrium over the cavitated and uncavitated regions results in the following equation (Dyson, 1976; Cocks and Ashby, 1982).

$$S_{ucav}\ (1-g) + S_{cav}\ g = S \qquad (1)$$

In equation 1, S is the applied stress S_{ucav} is the stress in the uncavitated region and S_{cav} is the stress in the cavitated region. Next, compatibility of creep deformation among the cavitated and uncavitated regions require,

$$\dot{e}_s = \dot{e}_{ucav} = \dot{e}_{cav} \qquad (2)$$

where, $\dot{e}_s$ is the overall sample strain rate and $\dot{e}_{cav}$ and $\dot{e}_{ucav}$ represent the local strain rates in the cavitated and uncavitated regions respectively. The creep behaviour of nickel-base superalloys like Inconel alloy X-750 strengthened by γ'precipitates is best represented by an eqn. of the form (Burt, Dennison and Wilshire, 1979; Evans and Harrison, 1979; Stevens and Flewitt, 1981; Ajaja, Howson, S.Purushothaman and Tien, 1980).

$$\dot{e} = A.\ [S - S_{po}]^4 \qquad (3)$$

where, $\dot{e}$ is the minimum creep rate, S_{po} is the back stress arising from the interaction between the dislocations and precipitates (Brown and Ham, 1971; Shewfelt and Brown, 1977) and A is a constant for a given material and test temperature. If it is now assumed that the grain boundary cavities grow retaining an equilibrium shape and further that the creep deformability of the adjoinig crystals can be neglected, the following expression for the local strain rate in the cavitated region due to cavitation alone ($\dot{e}_{cav}$) derived by Rice (1981) can be utilised.

$$\dot{e}_{cav} = M.\ S_{cav} \qquad (4)$$

$$M = 4\ D_b\delta_b\ \Omega\ /kT\ b^2 LQ \qquad (5)$$

where, $D_b\delta_b$ is the grain boundary diffusivity, Ω is the atomic volume T is the temperature, k = Boltzman's constant, b = half intercavity spacing on the grain boundary, L is the grain size and Q is a function of a/b (Rice, 1981) where a is the cavity radius on the boundary plane. Substitution of eqn.3 (with S_{ucav} and S_{cav} substituting for S in the uncavitated and cavitated regions respectively), eqns 4 and 1 in eqn. 2 gives,

$$A(RS-S_{po})^4 = A\left(S\ \frac{1-R(1-g)}{g}\ -S_{po}\right)^4 + MS\ \left(\frac{1-R(1-g)}{g}\right) \qquad (6)$$

where $R = S_{ucav}/S$. In eqn.6, the left hand side represents the creep rate in the uncavitated

region while the first and the second terms in the right hand side of eqn 6, represent the creep rate due to deformation and cavitation respectively in the cavitated region. Eqn.6 should be solved numerically to obtain the value R. It is to be noted that the value of R always lies in the range 1 (unconstrained) to 1/1-g (fully constrained) and further that the value of R keeps changing with time as the cavity growth proceeds (since M in eqn 6 keeps changing). Once R is known, the various creep parameters can be determined as follows (Sundararajan, 1985).

$$e_s(t) = A\ (R(t).S - S_{po})^4 \qquad (7)$$

$$t_f = \int_{a_o}^{a_f} da/V(t) \qquad (8)$$

$$V(t) = da/dt = (1.66 D_b\delta_b\Omega R(t)S)/(kTa^2(t)Q(t)) \qquad (9)$$

$$e_f = \int_{a_o}^{a_f} \dot{e}_s(t)\ .\ (da/V(t)) \qquad (10)$$

In the above equations, $\dot{e}_s$, t_f and e_f represent the overall sample strain rate, time to fracture and strain to fracture respectively, V represents the cavity growth rate and a_o and a_f correspond to the cavity radius (on the boundary plane) at the time of nucleation and cavity coalescence respectively.

The Effect of Environmental Damage:- In this section, the mathematical model developed in the last section (Section 2) will be extended to include the effects of environmental damage. Consider a test sample which has been exposed to air at high temperatures prior to creep testing. As illustrated in Fig.1, this sample consists of two regions (I and II). Region I is the unoxidised and region II is oxidised (environmentally damaged) annular region. As noted earlier, the damage in region II is characterised by either extensive cavitation as assumed by Pandey etal (Fig.1a) or by γ' free regions having poor creep strength (Fig.1b). In reality, the above two damage mechanisms should be considered together to get the overall effect of environmental damage on t_f and e_f. However, the mathematical modelling of such a combined damage phenomena is quite complicated and will be attempted in a future publication. For the present paper, the two forms of environmental damage (extensive cavitation and γ' dissolution) will be considered individually (case 1 and 2 respectively) and their effects will be incorporated in the model described earlier.

Case 1:- First consider the case where region II corresponds to the severely cavitated region (see Fig.1a). Let, W_I, be the depth up to which severe cavitation has occurred (Fig.1a). Then, f_I defined as the area fraction of the sample that is profusely cavitated, is obtained as (Sundararajan, 1985 and 1988).

$$f_I = (2W_I/R_c)(1-(W_I/2R_c)) \qquad (11)$$

where R_c is the radius of creep sample. The value of f_I, given by eqn.11 lies in the range 0 (no damage) to 1 (sample fully damaged). Please note that f_I is a function of prior exposure time since W_I inreases with time. Following the procedure adopted by Pandey etal. (except for the fact that equation 3 has been used for creep rate) and with the imposition of force equilibrium and compatibility of deformation among regions I and II, the following expressions for stress in the unoxidised region I (S_{unox}) and the oxidised (damaged) region II (S_{ox}) are obtained.

$$S_{unox} = S/(1-f_I g) \qquad (12)$$

$$S_{ox} = S - S_{unox}\ (1-f_I)/f_I \qquad (13)$$

where f_I is given by eqn.11. Now, an assumption is made that the fracture of the whole creep specimen is determined solely by the fracture of the unoxidised region (region I). This assumption is reasonable, since the oxidised region (region II) is heavily cavitated and thus should fracture much earlier. On the basis of this assumption, the t_f, e_f and $\dot{e}_s$ can be now computed using equations 7,8 and 10 but with the understanding that S is replaced by S_{unox}(equation 12) and R is redefined as S_{ucav}/S_{unox}.

Case 2:-This case corresponds to a situation wherein the damage caused by the γ' dissolution of upto a depth W_2 (see Fig.1b) on prior exposure to oxygen is analysed with regard to its effect on creep fracture. Let f_2 defined by equation 14, given below, represent the area fraction of the sample that is free of γ'.

$$f_2 = (2W_2/R_c)(1-(W_2/2R_c)) \tag{14}$$

As in the case of f_1, the value of f_2 lies in the range 0 to 1 and increases with increasing time of prior exposure. In region I (unoxidised), the creep rate ($\dot{e}_{unox}$) is given as,

$$\dot{e}_{unox} = A\ \{S_{unox} - S_{po}\}^4 \tag{15}$$

Region II is free of γ' which contributes to the back stress .Thus, $S_{po} = 0$ in region II and one obtains,

$$\dot{e}_{ox} = A.\ S_{ox}^n \tag{16}$$

Compatability of deformation between regions I and II demands that $\dot{e}_{unox} = \dot{e}_{ox} = \dot{e}_s$ (e_s = overall sample strain rate). The above condition in conjunction with the requirement of force equilibrium results in the following expression for the stress in regions I (S_{unox}) and II (S_{ox}) respectively (Sundararajan, 1988).

$$S_{unox} = S + f_2 S_{po} \tag{17}$$

$$S_{ox} = S - S_{unox}(1-f_2/f_2) \tag{18}$$

Next, t_f, e_f and $\dot{e}_s$ can be computed using equations 7,8 and 10 with the modification that S is replaced with S_{unox} (equation 17) and R is redefined as S_{ucav}/S_{unox}.

RESULTS AND DISCUSSION

Material Constants and Variables: The first aspect to be considered is the choice of the appropriate values for the material constants and the variables appearing in the various equations given earlier. Since the purpose of this paper is to compare the predictions of our numerical model with regard to t_f and e_f with the corresponding experimental data obtained on inconel alloys X-750 by Pandey etal (1984, 1986) the values of various material constants pertinent to Inconel alloy X-750, given in Table 1, have been used. All the creep experiments of Pandey etal on preexposed Inconel alloy X-750 test samples were conducted at a constant temperature (T=973K), constant initial stress (S=400MPa) and with test samples of diameter 5 mm (R_c= 2.5 mm). Thus, these variables have been held constant at these values for the purpose of the numerical model employed here. The initial cavity size on nucleation (a_o) has been fixed at the lower limit resulting from the use of the stability criterion (i.e $a_o = 2\ R_s/S$) while the cavity size at fracture (d_f) has been made equal to half cavity spacing (b). Pandey, Taplin, Ashby and Dyson (1986) noted that the grainsize (L), the depth of heavily cavitated zone (W_1) and also the depth of the γ' -free region (W_2) increased with increasing duration of preexposure at 1150° C, as shown in Table 2. The values of f_1 and f_2 calculated using the values of W_1 and W_2 listed in Table.2 (and equations 11 and 14) are also indicated in Table 2. For a given preexposure time, the corresponding set of values of L, f_1 (for case 1) and f_2 (case 2) given in Table 2 were used in the numerical computation. Finally, half cavity spacing (b) and fraction of cavitated regions (g) are the only two variables left whose values have not been fixed. In order to compare the model predictions with the experimental results of Pandey etal. the values of b and g were varied over a wide range and the best fit values of b and g were chosen.

Results of the Numerical Model: In this section the predictions of the model with regard to the effect of environmental damage on creep life and ductility will be presented and analysed.

Undamaged samples : The variation of time to fracture (t_f) and strain to fracture (e_f) as a function of both inter-cavity spacing (b) and fraction of cavitated grain boundaries (g) was computed using the numerical model given in section 2.1. For the experimental test conditions used by Pandey et al. (S = 400 MPa and T = 973K), the model predictions ($t_f = 8x10^5$ s; $e_f = 7.6\%$) can be made to fit the data of Pandey et al. (1984, 1986) on undamaged test sample ($t_f = 6\text{-}10x10^5$ s; $e_f = 7.6$ to 8.7%) if values of 15µm and 0.1 are assumed for b and g respectively. The model predicts t_f to decrease with increasing g while e_f increases with increasing g. Thus, the fact that both t_f and e_f can be predicted accurately using the same values of b and g implies that a reasonable stringent condition has been satisfied. The above values of b and g which give a good fit also appear reasonable and consistent with the optical micrograph of the undamaged Inconel alloy X-750 creep tested to fracture (fig.13; Pandey et al. [1984]).

Environemntally damaged samples : In this section the predictions of the model regarding the decrease in t_f and e_f with increasing damage (increasing f_1 , f_2) will be considered. To make the comparison between the model predictions and the experimental data easier, the t_f and e_f values of undamaged samples predicted correctly by the model for b=15µm and g=0.1 (designated as t_f^* and e_f^*) were used as the reference values. In fig.2, the variation of t_f/t_f^* and e_f/e_f^* as a function of the fractional area of the sample (f_1) that has been environmentally damaged in the form of profuse cavitation (case 1) is illustrated The full lines in the figure valid for different values of b represent the model predictions (sections 2.2; case 1) while the filled circles correspond to the experimental data of Pandey et al.(1986). A constant grain size of 120µm was assumed for the purpose of model calculations since the grain growth which occured with increasing duration of exposure prior to creep testing (Table 2) was confined only to the profusely cavitated region and not to the undamaged region.

The variation of t_f/t_f^* and e_f/e_f^* is a function of the area fraction (f_1) of γ' - free region (case 2) as predicted by the model (section 2) for different values of b(g=0.1) is indicated by full lines in fig.3. The filled circles represent the experimental data of Pandey et al. In this case the undamaged region (i.e region having γ') has a grain size of 120µm in the central portion which is free of environment-induced cavitation, while the outer portions exhibiting profuse cavitation but having γ' have a coarser grain size due to grain growth (see table 2). Thus, an average grain size, lying in the range 120µm (f_2=0) to 400µm (f_2 = 0.13), was used for the model calculations.

A detailed analysis of figs.2 and 3 indicates that for both case 1 and 2, a decrease in t_f with increasing extent of damage (f_1 or f_2) is predicted, the decrease being more dramatic for higher values of b. In contrast, while e_f is predicted to be insensitive to the extent of damage for case 1, it decreases with increasing damage (f_2) for case 2 the effect being more dramatic for higher values of b. This difference in behaviour is solely due to the fact that the grain size increases with increasing f_1 for case 2 unlike in case 1 wherein a constant grain size is assumed. The dotted lines in fig.3b, corresponding to model predictions for constant grain size, proves this beyond doubt. The observation that e_f increases slightly with increasing damage (figs.2b and 3b) can be explained on the basis that the effective stress (S_{unox}) in the undamaged region increases with increasing f which in turn causes the constraint to decrease with the attendant improvement in ductility (Sundararajan, 1988).

Comparison between the experimental data of Pandey et al. and the model predictions (figs.2 and 3) clearly indicates that the experimentally observed rapid drop in both t_f *and* e_f cannot be explained by the model if a constant value is assumed for b. Thus, a decrease in b with increasing damage (which is equivalent to increasing stress in the undamaged portion of the specimen) needs to be assumed if the model predictions have to agree with the data of Pandey etal. The above assumption is not unreasonable since it is well known that b decreases with increasing stress (equivalent to increasing damage in the present case) in many metals and alloys (Cane and Greenwood, 1975; Fleck, Taplin and Beevers, 1975; Needham and Gladman, 1980; Argon, Chen and Lau, 1980). Finally, an unambiguous conclusion regarding which form of damage (case 1 or 2) is more important is not possible since the assumption of either form of damage (with varying b) is capable of explaining the experimental data. At this juncture it should also be remembered that in reality, both forms of damage will coexist and interact with each other.

CONCLUSIONS

1. A mathematic model which allows for the interaction between the environmental damage in the form of profusely cavitated or γ' free regions and the conventional mechanical damage in the form of grain boundary cavitation has been developed.
2. The predictions of the model has been compared with the experimental data on an Inconel alloy X-750 obtained by Pandey etal. using test samples which were first exposed to high temperature oxygen environment and subsequently creep tested at 700 °C and a stress of 400 MPa.
3. The model predictions can be completely matched with the experimental data, irrespective of the form of damage (case 1 and 2) considered, only if the half cavity spacing (b) is aassumed to decrease with increasing time of prior exposure.

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Table.1 The Material Constants for Inconel X-750 Alloy

Parameter (symbol)	Value*	Units
Atomic volume Ω	1.79×10^{-29}	m^3
Boundry diffusivity D: $D_{ob}\delta_b$	3.5×10^{-15}	m^3/s
Q_b	115,000	J mol^{-1}
Constant A (eqn. 3)	3.0×10^{-40}	$Pa^{-4}s^{-1}$
Particle back stress S_{po}	305	MPa
Surface Energy R_s	1.5	J/m

*Values taken from Pandey et al. (1984)

Table. 2 The effect of prior exposure of Inconel X-750 at 1150 C on the grain size (L), area fraction of the sample heavily cavitated (case 1) and areafrac tion of the sample free of precipitates (case 2)

Time of exposure (hrs)	* Grainsize L(μm)	** W_1 (mm)	f_1 (eqn.11)	** W_2 (μm)	f_2 eqn.14
0	120	0	0	0	0
1	125	0.25	0.19	4	0.003
4	135	0.50	0.36	12	0.01
15	160	1.05	0.66	80	0.06
67	400	2.5	1.00	170	0.13

exposed at 1150 °C to a reduced air pressure of 3×10^{-2}Pa.
**-data taken from Pandey et al. (1986)
*- grain size in the profusely cavitated region.

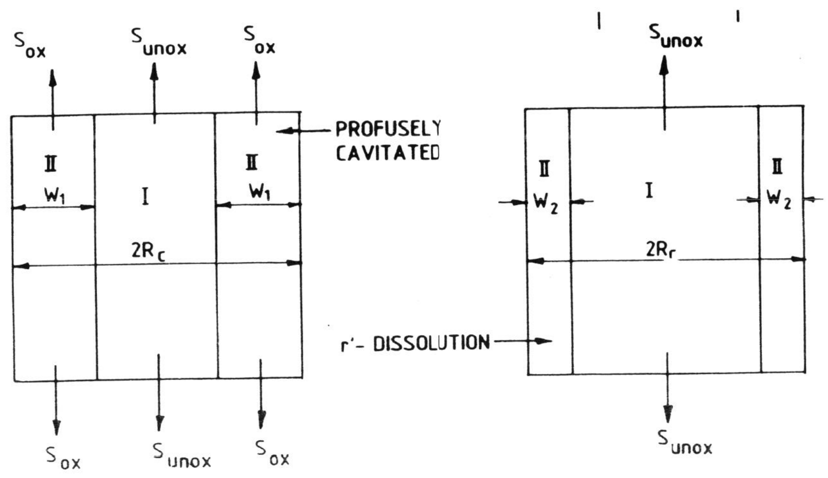

Fig.1:A schematic sketch of air exposed creep sample indicating the oxidised (region II) and unoxidised regions(region I).Case1 & case2 represent the damage in the form of profuse cavitation and γ' dissolution respectively.

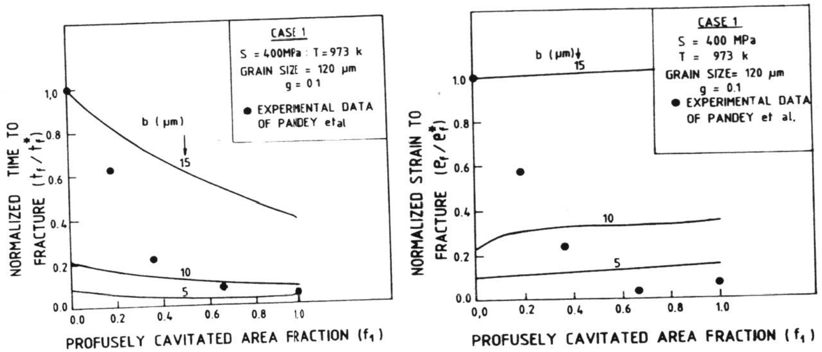

Fig.2:The variation of the normalised time to fracture (fig.2a) and the normalised strain to fracture(fig.2b) as a function of the profusely cavitated area fraction(f_1) as predicted by the model(full lines) compared with the experimental data(filled circles).

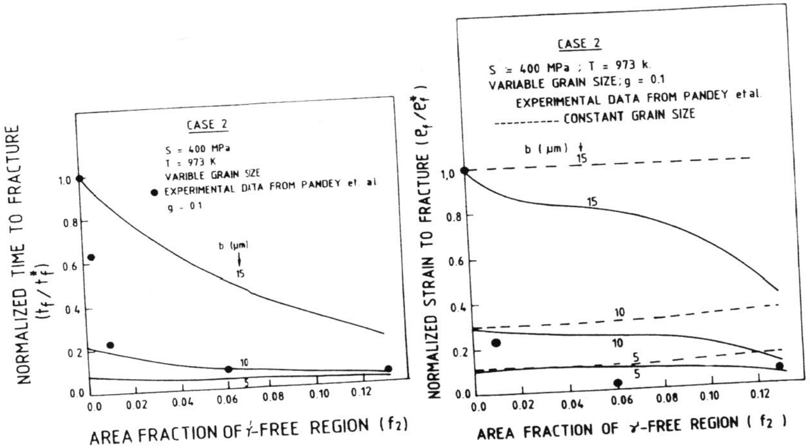

Fig.3:The variation of the nomalised time to fracture(fig.3a) and the normalised strain to fracture (fig.3b) as a function of the area fraction of γ' free region (f_2) as predicted by the model (full lines)compared with the experimental data (filled circles).