CREEP FATIGUE:
PAPER II
Creep Fatigue Life Prediction: Methods and Trends

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ABSTRACT

Creep-fatigue data of low allow steels were compiled from international sources, and trends in creep-fatigue behavior were identified in Paper I. This paper reviews the methods of creep-fatigue life prediction that were assessed from the compiled data. The methods reviewed in this paper are phenomenological in nature. However, an empirical method has also been discussed which offers very high reliability of the prediction capability. Various test requirements, from which material parameters are determined for each method, have been tabulated. No single method has been generalized as a best method of life prediction for all types of creep-fatigue test conditions.

KEY WORDS: creep-fatigue, life prediction, total strain range, plastic strain range, strain range, frequency, hold times, damage.

INTRODUCTION

The development of a reliable life prediction is very important, since it assists the designer in making a structural analysis and predicting lifetimes of engineering components operating at high temperatures. The key to such an analysis is material characterization. Many details of material characterization are classified and not available in the open literature. There is also a lack of publications which analyze creep-fatigue data with methods of life prediction. Only two publications describe creep-fatigue data and analysis of life prediction by widely used methods /1,2/. Two low alloy steels of the types 1Cr-Mo-V and 2.25Cr-Mo have been assessed to compare the prediction capability of methods with each other /1,2/. Hence these publications /1,2/ play key roles when discussing the applicability of the methods of life prediction.

Several review papers have been published on the subject; however, none examined the methods of life prediction using an international data bank and, besides, those reviews were oriented to specific data. Miller and Ellison /3/ used their data to assess methods of life prediction, while Lloyd and Wareing /4/ reviewed the methods in comparison with published data. Thus, methods of predicting creep-fatigue life, applied to different batches of a particular grade of steel, have been reviewed in this paper. Trends showing the ability of various methods to predict creep-fatigue life will be useful in indicating the applicability of those methods under a range of test conditions.

Several frameworks exist for the development of life prediction methods. These are as follows:

1. Constitutive equations (predict stresses on a microscopic scale),
2. Damage mechanics (stress or strain based),
3. Energy methods (use stress-strain in product form),
4. Fracture mechanics (stress-strain history ahead of crack),

5. Metallurgical approaches (monitoring micro-structural changes)
6. Phenomenological approaches (laboratory stress-strain history),
7. Empirical models
   a) extending best fit equation of a material to other materials,
   b) developing a model based on experience.

Only phenomenological approaches and Diercks' /5/ empirical method of life prediction have been discussed.

REVIEW OF LIFE PREDICTION METHODS

Linear Damage Summation

Life prediction under creep and fatigue conditions was proposed by Robinson /6/, and modified by Taira /7/. It is similar to the linear summation of cycle ratios, employed by Miner /8/ for fatigue analysis. Damage by separated time-dependent fraction (creep), together with the time-independent cycle fraction (fatigue) is added linearly. Culmination of damage to final failure occurs when the linear summation of fraction creep and fatigue damage reaches unity. These fractions are calculated from a waveform profile that contains, in addition to fatigue excursions, a steady hold time. The cycle fraction is a ratio of the number of cycles at a stress/strain level with cycles to failure (Nf) under the same loading conditions. The time fraction is a ratio of total time of hold with time to rupture (t_f) under the same loading conditions.

$$\Sigma n / N_f + \Sigma t / t_f = D = 1 \text{ at failure} \quad (1)$$

where n / N_f and t/t_f are the cycle and time fractions, respectively. This method was accepted by the ASME in the design of pressure vessels and piping under Code Case N 47 1597 /9/. When assessed with the creep-fatigue data of low alloy steels and other materials, shortcomings were observed. This method assumes the same time fractions for holds in both the tension and compression directions. Increasing hold times in very low strain ranges, in the peak tensile direction, produce a damage parameter (D) much less than unity. Environmental effects are unaccounted for, which, in the case of 2.25Cr-1Mo steel, reduces the creep-fatigue life /10/ when hold is applied in the compression direction.

Frequency Modified and Frequency Separation Approach

Coffin /11,12,14/ introduced time effects in the Manson-Coffin relationship [$\Delta\varepsilon_p = C_i(N_f)^{-\alpha}$] to account for the environmental and other time-dependent effects. With the frequency term included, the above relationship has the form [$\Delta\varepsilon_p = C_i(N_f.\ \nu^{k-1})^{-\alpha}$], where the symbols used are $\Delta\varepsilon_p$, plastic strain range, N_f, cyclic life, ν, frequency, and C_i, k and α are material parameters. Tests with creep-fatigue combinations were predicted from the constants of hysteresis loops generated by continuous fatigue or triangular waveforms. Under triangular (continuous fatigue) waveforms, strain rate and frequency are the same. Thus, by introducing a frequency term in the Basquin equation /13/, several expressions were presented for life prediction. However, there are disagreements among authors, and it has been stated that in this way hold times and unequal ramp rates in the tension and compression directions may not be predicted /15/.

$$A' (N_f)^{-\beta'} \nu^{K'} = \Delta\sigma \quad (2)$$

where K', β' and A' are material constants of the Basquin equation (2) and it can be obtained from regression analysis of the stress range versus cyclic life data on a log-log scale. The number of cycles to failure (N_f) was given as

$$N_f = (A' / \Delta\sigma_{sf})^{1/\beta'} [\nu_{t/2}]^{K'/\beta'} \quad (3)$$

where $\Delta\sigma_{sf}$ is the stress range with unequal ramp rates. It was assumed that the damage occurred only during the tensile part of the hysteresis loop. It was not a convincing assumption for compressive dwell sensitive materials where compression holds are more damaging.

The stress range for unequal ramp rate cycles was partitioned in terms of frequency or the total time in the tension or compression directions, as follows:

$$\Delta\sigma_f = A'' / 2\ \Delta\varepsilon_p^{n'}[(\nu_t / 2)^{K_1} + (\nu_c / 2)^{K_1}] \quad (4)$$

where A", n' and K_1 are coefficients of the second Basquin relation obtained from unequal ramp rates, below:

$$\Delta\sigma = A''\ \Delta\varepsilon_p^{n'}\nu^{K_1} \quad (5)$$

In order to address severely unbalanced loop shapes, a frequency separation technique was proposed. It uses coefficients obtained from balanced loops with equal tension and compression holds only. The approach was further modified /14/ to account for cyclic hold by separating the tensile and compressive strain rates. The constants in the above model were determined from the continuously cycled tests and also from some hold time tests and may be applied to any complex hysteresis loop for the life analysis.

$$N_f = [F/\Delta\varepsilon_p]^{1/\beta'}[\nu_t/2]^{1-K}[\nu_c/\nu_t]^d \quad (6)$$

In the above equation, ν, β' and K are constants that may be obtained from balanced loop data, and d is obtained from unbalanced loop data where tensile and compressive hold times are different.

This model has been criticized for under-predicting life. The damage produced by a tensile hold was considered the same irrespective of its occurrence in a hysteresis loop. Tensile mean stresses produced by a compressive hold cycle were not considered. With increasing peak tensile hold time this method predicts shorter life, but this may not be the case, as the opposite was found for SS 304 /15/, 2.25Cr-Mo /10/ and superalloys /16/, since 2.25Cr-Mo and some superalloys are compressive dwell sensitive materials.

Strain Range Partitioning Technique

Manson, Halford and Hirschberg /17/ developed a strain range partitioning technique. Plastic fatigue and inelastic creep strain components were separated in a hysteresis loop. Representation of these strain components in two directions involves PP, PC, CP and CC loops. Four base line relationships describe the combinations of strains with respect to cyclic life. The strain components, such as $\Delta\varepsilon_{pp}$, $\Delta\varepsilon_{pc}$, $\Delta\varepsilon_{cc}$ and $\Delta\varepsilon_{cp}$, represent the combination of strains where the subscripts denote "p" for plasticity and "c" for creep, and the first subscript is for tension followed by the second for compression, respectively. Representation of life was made in terms of the Manson-Coffin equation:

$$N_{ij} = A_{ij}\Delta\varepsilon_{ij}^{\theta jk} \quad (7)$$

where N_{ij}, A_{ij} and $\Delta\varepsilon_{ij}$ are cyclic life, material constant and strain range, respectively, θ being the slope of the strain range versus life line, ij referring to plasticity or creep.

Damage fractions, F_{ij} were added by an interacting damage rule:

$$F_{pp}/N_{pp} + F_{pc}/N_{pc} + F_{cc}/N_{cc} + F_{cp}/N_{cp}$$
$$F_{ij} = \Delta\varepsilon_{jk}/\Delta\varepsilon_{in} \quad (8)$$

$\Delta\varepsilon_{in}$ is the inelastic strain range

$$\Delta\varepsilon_{in} = \Delta\varepsilon_{pp} + \Delta\varepsilon_{pc} + \Delta\varepsilon_{cc} + \Delta\varepsilon_{cp} \quad (9)$$

This model was modified as a total strain version of SRP /18/. The mean stress, low strain range and long hold time situations were dealt with in this model. SRP has been criticized for the difficulty in partitioning the loop and the omission of an environmental term in the damage criterion. Bounds on life relationships can be plotted with four baseline strain combinations, such as PP, PC, CP and CC lines. This determines a particular combination as the most damaging. Strain components with cyclic life were observed parallel, and one of them is considered as representing the service condition in the design. For gas turbine blade materials, such as IN 100 and MAR M 200, these combinations coincide /19/ and help form the basis of the design. However, for other materials, such as stainless steels and some low alloy steels, 2.25Cr-Mo and 9Cr-1Mo, these lines intersect, and pose considerable difficulties in selecting a particular line appropriate to the service condition.

Damage Rate Approach

This is a strain-based approach, developed by Majumdar and Maiya /15,21,22/. The rate of fatigue or creep damage accumulation depends on plastic strain rate in high temperature fatigue. The total strain component has not been separated in time-dependent and time-independent parts in the damage criterion. Instead, microcrack growth from an initial length (a_o) to a final length (a_f) was assumed. In its simplest form, damage was thought to be microcracks, whose growth occurred differently under tensile and compressive stresses. Two scaling factors in tension (T) and compression (C) were introduced, as follows:

$da/dN = a[T][\varepsilon_p]^m[\dot{\varepsilon}_p]^k$ (in the presence of tensile stress) (10)

$da/dN = a[C][\varepsilon_p]^m[\dot{\varepsilon}_p]^k$ (in the presence of compressive stress)

where m and k are material parameters and remain constant over a given range of plastic strain rate $\dot{\varepsilon}_p$ and plastic strain range $\Delta\varepsilon_p$. T and C are scaling factors introduced to account for the differences that may occur in the crack growth rates under tension or compression holds. These equations were also extended to account for crack (fatigue) and cavity damage (creep), which grow independently. The cavity growth equation was expressed in terms of the following:

$$1/c\ da/dt = G[\varepsilon_p]^m[\dot{\varepsilon}_p]^{k'} \quad (11)$$

where c is cavity size, t test duration and G material constant.

These equations separate the cavity and crack growth; hence, fatigue and creep damage growth are independent. Mechanistically, environmental and test temperature effects are accounted for in the model to predict life. Some constants of the equation may be determined from completely reversed cycling data with several strain range and rate combinations. However, prediction of longer hold time cycles requires those specific tests to determine the constants. The equation may be integrated to calculate life for appropriate waveshapes.

Damage Function Method

The change in internal energy per unit volume of material in a time interval of (0,t) is a measure of damage,

$$U = \int \alpha_{ij}\,\sigma_{ij}\,\varepsilon_{ij}\,dt - \int h\,dt \quad (12)$$

where α_{ij} and h_{ij} are shape factors to correct a hysteresis loop and heat generated in plastic deformation, respectively. Morrow /23/ proposed hysteretic energy per cycle as a measure of fatigue damage. The damage relationship was given by Equation (13)

$$C = \Delta W\, N_f\, \nu \quad (13)$$

where ΔW is an energy term, C a material constant and ν frequency.

The lifetime prediction criterion with energy of a hysteresis loop is dependent on the tensile hysteretic energy which keeps the crack tip open /24/. thus, only tensile energy adds to the accumulation of damage. there is a limit of tensile energy above which only damage grows, and below which closure occurs. For a strain-controlled, low cycle fatigue test, it is common practice to consider the total tensile part of a hysteresis loop as causing damage, due mainly to difficulty in identification of the closure line /24/. Ostergren /24/ introduced the damage function, $\alpha\sigma_T\Delta\varepsilon_p$. The energy term was used in Coffin's frequency-modified equations as follows:

$$C = \alpha\sigma_T\Delta\varepsilon_p N_f^{\beta}\nu^{\beta(K-1)} \quad (14)$$

where C, β, k are material constants, and σ_T the maximum tensile stress term.

Damage Parameter Approach

Kachanov /25/ described creep rupture behavior in terms of a damage parameter (ω), which was related to the cavitated area fraction of grain boundaries. The damage parameter was unity at failure and zero for the virgin condition. to describe the damage parameter, the concept of material continuity (ψ) was introduced. This was assumed to be unity for the virgin condition and zero at failure. The change of continuity was expressed by Kachanov as:

$$d\psi/dt = -A(\sigma/\omega)^n \quad (15)$$

where (σ/ω) is an effective stress term and σ the nominal stress. The equation may be integrated between extremes of the virgin and failure conditions. The non-linear nature of creep-fatigue interactions can then be described by expressing the rate of damage accumulation as a function of effective stress, e.g.,

$$d\omega/dt = f[\sigma/(1-\omega)] \quad (16)$$

This concept was further exploited /26,27/ in the literature. Chrzanowski /28/ proposed an approach based on damage mechanics concepts. This is known as the damage parameter approach, which was developed on the basis of the following:

1. damage comprises time-dependent and time-independent parts,
2. damage by fatigue increases by virtue of stress increase, whereas creep increases with both positive and negative stress rates,
3. rates of fatigue and creep damage are zero with negative stresses.

Damage was considered to occur only under positive stress increments. The damage law was expressed by a non-linear equation, described by the rate of damage accumulation as a function of effective stress, e.g.,

$$d\omega/dt = [Co\{\sigma/(1-\omega)\}^{v_0} d\sigma/dt\, H(d\sigma) + C\{\sigma/(1-\omega)\}^{v}]H(\sigma) \quad (17)$$

The first and second terms represent fatigue and creep damage, respectively, and Co, C, v_0 and v are material constants, H (Heaviside function) being a function of tensile stress.

This model analyzes non-linear, creep-fatigue situations with the inclusion of a total damage term, ω, in both the creep and fatigue terms of equation (17). The above equation may be integrated for a known stress-time history to predict life.

Assessment Procedure R 5

This code was developed by Nuclear Electric Inc. as "an assessment procedure for the high temperature response of structures" /29/. The cyclic endurances of a component subjected to an arbitrary cycle, where a dwell of any length may be present, can be described by assessment procedure R 5. It assumes that the endurance can be expressed in terms of the fatigue and creep components of damage, which can be summed up linearly to produce a damage term representative of service cycle. The fatigue damage is assumed to be proportional to the inverse of the continuous fatigue endurance (N_o) corresponding to the initiation of a crack of depth (a_o). This is determined for a total strain range, calculated as the difference between the extreme strain values in the hysteresis loops. If N_l is the number of cycles to failure in a continuously cycled laboratory specimen and at the time of failure the corresponding crack depth is a_l, then the required number of cycles (N_o) to initiate and grow a crack to a depth a_o can be expressed in terms of the following:

$$N_o = MN_l + (1-M)N_i \quad (18)$$

where N_i is the number of cycles undergone in initiating a defect of depth a_i = 20 µm, irrespective of the section thickness, given in terms of N_l by:

$$N_i = \exp(1.306 \ln N_l - 3.308) \quad (19)$$

This expression is valid for $50{,}000 > N_l > 15$ cycles.

The creep damage per cycle D_c is evaluated using the ductility exhaustion method by integrating over the dwell time t_h:

$$D_c = \int_0^{t_h} \dot{\varepsilon}/\varepsilon_f(\dot{\varepsilon})dt \quad (20)$$

where $\dot{\varepsilon}$ is the instantaneous strain rate during the dwell and $\varepsilon_f(\dot{\varepsilon})$ the corresponding creep ductility. The total damage per cycle is simply expressed as

$$D_t = 1/N_o + D_c \tag{21}$$

The creep-fatigue endurance N_o^* is given by:

$$N_o^* = 1/D_t \tag{22}$$

Equation (20) is simplified if the ductility ε_f is pessimistically assumed to be independent of $\dot{\varepsilon}$ and equal to the lower shelf ductility ε_l. The equation then becomes:

$$D_c = Z\Delta\sigma'/E\varepsilon_L \tag{23}$$

where $\Delta\sigma'$ is the stress relaxation in time t_h and E is Young's modulus.

Similarly, if the dwell occurs in the compressive part of the cycle, then Equation (23) is reduced to

$$D_c = Z\Delta\sigma'/E\varepsilon_u \tag{24}$$

The arbitrary value of Z was estimated as follows:

- Calculate 0/0 endurance (N_o) from N_l (Equation 18).
- Endurance including the effects of hold times was given by

$$N_o^* = (1/N_o + Z\,D_c)^{-1} \tag{25}$$

where D_c is calculated from Z=1. For higher values of Z, life prediction was found to be too pessimistic.

The above technique has been assessed with very limited creep-fatigue data, using tensile hold times from 3 min. to 16 hours, and sparingly with compressive holds.

Empirical Models

Empirical models have been developed, based upon past experience of manufacturers, for life prediction. In most cases these are not available in the open literature. One example of this type of approach extrapolates the baseline SRP behavior to 'several years of hold' (up to 100 years) /30/. However, only the Diercks equation /5/, which has been extended in the creep-fatigue life prediction of low alloy steels /31,32/, has been discussed in this paper.

Diercks Empirical Model

Diercks and Raske /5/ compiled a bank of creep-fatigue data of SS 304 and obtained a multivariate best fit equation, in terms of test variables, for creep-fatigue life extrapolation. Diercks' multivariate best fit equation uses the following parameters:

strain range parameter S = ($\Delta\varepsilon_t/100$),
strain rate parameter R = ($\log\dot{\varepsilon}$),
temperature parameter T = ($T_c/100$), and,
hold time parameter H = $\log(1+t_h)$, in a multi variate form, as below,

$$(\log N_f)^{-1/2} = 1.20551064 + 0.66002143{*}S + 0.18040042\ S{*}S - 0.00814329{*}S^4 + 0.00025308\ R{*}S^4 + 0.00021832TS^4 - 0.00054660\ RT^2 - 0.005567RH^2 - 0.00293919HR^2 + 0.0119714H{*}T - 0.00051639H^2T^2 \tag{26}$$

where T_c is the test temperature of SS 304 and t_h the time of hold. Equation (26) was used to extrapolate the creep-fatigue life of SS 304, recommended by ASME Code 1749, to design fatigue diagrams /31,32/.

Kitagawa *et al.* /32/ extended the above equation to creep-fatigue life prediction of low alloy steels. They assessed the creep-fatigue data on 2.25Cr-1Mo and 9Cr-1Mo steels. However, their proposed modification for generalization of the Diercks equation requires the following:

1. A cycle ratio (α) is the ratio of the fatigue life of SS 304 and that of a low alloy steel, shown schematically in Fig. 1 (α = fatigue life of SS 304/fatigue life of the material being investigated), under the same strain range, temperature (°C) and strain rate.
2. A temperature parameter (T) compares iso-stress creep rupture lives of low alloy steel with that of SS 304. This was defined as the temperature at which the investigated material has the same rupture life

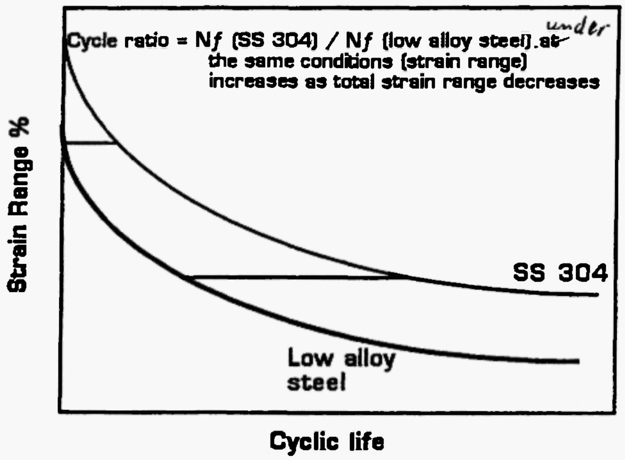

Fig. 1: Schematic determination of cycle ratio.

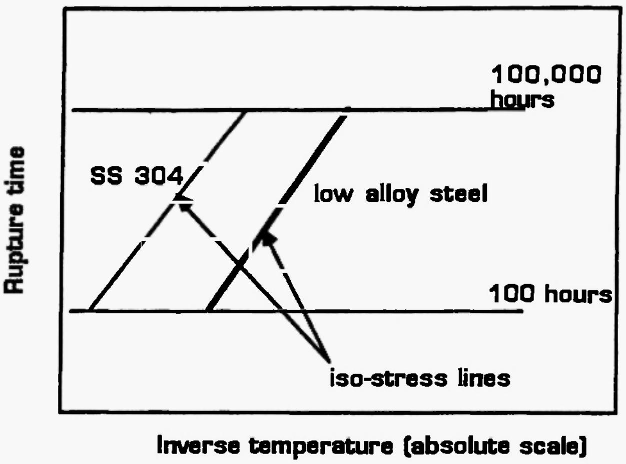

Fig. 2: Schematic determination of temperature correction factor.

under the same stress as that of SS 304 alloy. The low alloy steels, 2.25Cr-1Mo and 9Cr-1Mo, always appear lower than the corresponding temperature for SS 304. This difference was about 100 and 50°C for a 1Cr-Mo-V, 2.25Cr-1Mo and 9Cr-1Mo alloy, as shown schematically in Fig. 2 /32/.

3. Substituting the number of cycles to failure (N_f) and T in the Diercks equation with a-N_f and (T_t + T_a)/100, where T_t is the test temperature for low alloy steel and T_a is the temperature difference in iso-stress creep rupture lives, respectively, in °C.

Kitagawa *et al.* /32/ successfully extended the above modifications to the creep-fatigue life prediction of 2.25Cr-1Mo and 9Cr-1Mo steels. The range of this equation has been shown /32/ from pure fatigue to creep.

Requirements of Prediction Methods

Application of the phenomenological methods of life prediction to creep-fatigue data requires the following test information, where the constants are determined from best fit equations.

1. Creep rupture properties at the same temperature relate stress-time (two constants for each linear behavior).
2. Stress relaxation with respect to hold time.
3. Total strain versus life data (four constants).
4. Elastic and plastic components versus life and respective constants (four in this case).
5. Stress range versus plastic strain. Slope (n) and the intercept (K) or fatigue strength constant, applicable above or below a plastic strain range.
6. Tests with balanced and unbalanced hold times and ramp sequences.
7. Apart from these, several constants may be needed to apply a method, e.g., SRP needs eight such constants, inelastic strain components versus life data.
8. Frequency versus life data.

Table 1 summarizes requirements of individual life prediction methods. The published literature contains very little information on how to determine these constants. Since the description of test variables, tension / compression stresses, hysteretic behavior in X-Y plots (σ–ε and time) and loop stabilization histories are not quantitatively known, application of methods of life prediction to the collected data /33/ is not possible. Also, constants determined for a method from a particular low alloy steel grade cannot be extended to predict the creep-fatigue behavior of other batches of the same or different grades. Methods of life prediction and the conditions under which they apply to low alloy steels are discussed below.

Laboratory tests for creep-fatigue are conducted under total strain control. Waveforms of creep-fatigue cycles are complex in nature, due mainly to the stress

Table 1
Constants of the phenomenological approaches

Method of life prediction	Life prediction equation	No. of constants (n)	Details of the tests
Linear life fraction	$1 = \Sigma n / N_f + \Sigma t / T_r$	- strain-life data (4) - creep-rupture (2 to 4)	0/0 tests (ε_t-N_f); creep rupture °C; stress relaxation
Frequency modified approach	$N_f = [F/\Delta\varepsilon_p]^{1/\beta'}$ $[\nu_t/2^{1-k}[\nu_c/\nu_t]^d$	- strain-life data (4) - frequency vs. life (2) - stress-strain (2)	0/0 tests. Some hold times; frequency-life.
Strain range partitioning	$N_{ij} = A_{ij}\Delta\varepsilon_{ij}^{\theta jk}$ ij represent PP, PC, CP and CC loops	four inelastic strain vs. life relations (2 x 4)	Tests producing complex loops PP, PC, Cp and CC.
Damage rate approach (no creep damage)	$da/dN = a\,[T]\,[\varepsilon_p]^m\,[\dot{\varepsilon}_p]^k$ $da/dN = a\,[C]\,[\varepsilon_p]^m\,[\dot{\varepsilon}_p]^k$	scaling factors (2); strain-life (4); strain rate-life (2) assuming a crack size	0/0 tests, metallographic evidence; hold time tests.
With creep	$1/c\; da/dt = G[\varepsilon_p]^m\,[\dot{\varepsilon}_p]^k$	scaling factor in creep cavity size (1) strain-life and rate (6)	metallographic evidence; creep data; test duration
Damage function method	$C = \sigma_T/\Delta\varepsilon_p\, N_f^{\beta}\nu^{\beta}\,(K\text{-}1)$	strain-life (4); frequency-life (2); stress-strain (2); shape correcton factor	0/0 data. Frequency data; stress-life data; hold time data
Damage parameter approach	$d\,\omega/dt = [Co\,\{\sigma/(1-\omega)\}^{\nu}0\; d\sigma/dt\,H\,(d\sigma) + C\{\sigma/(1-\omega)^{\nu}]\,H\,(\sigma)$	material constants (3) fatigue-damage (2) creep-damage (2)	stress versus damage in creep and fatigue.

changes that take place with time. The maximum stress at the beginning of a cycle may be much higher compared to that at the end because of stress relaxation. The relationship between the creep rupture time and applied stress is often bilinear on a log-log plot. Ellison and Walton /34/ compiled constant-load creep rupture behavior at 565°C from the tests conducted at Bristol and Belfast Universities on 1Cr-Mo-V steel. They observed a bilinear trend below or above 280 MPa. The ratio of two slopes and intercepts (stress value at unit rupture time) are 2.15 and 2×10^{20}, respectively, between stresses above and below 280 MPa. A slight difference in stress would result in large variations in the extrapolated rupture life values. Also, creep rupture properties change considerably with a slight temperature increase. The iso-stress (15Kgf/mm^2) creep rupture properties in the case of low alloy steels vary from 50 to 10^5 hours in a temperature range of approximately 485 to 590°C for 2.25Cr-Mo and 9Cr-1Mo steels /32/. Hence the collected data on various steel grades under several test conditions in a temperature range from 483 to 600°C, cannot be assessed with the constants determined from one set of data.

DISCUSSION ON THE APPLICABILITY OF THE METHODS

The applicability of life prediction methods to the data compiled in Paper I /33/ has been reviewed. Only two methods, DSA and SRP, have been used extensively by most workers. Hence, the applicability of life prediction methods as assessed by various workers is addressed. From this analysis, trends in creep-fatigue life prediction methods will be identified.

Linear Damage Summation

This method was assessed for most batches of the collected data. It requires static creep data for time fraction analysis. Under constant tensile strain hold, steady state creep strain rate is computed and integrated for all cycles. During a dwell, stress relaxation is modelled by $\Sigma t/t_f$, where t is hold time and t_f rupture time at the same load levels. It has been pointed out /35/ for 1Cr-Mo-V, Batch 4, at 565°C, that the magnitude of relaxed stresses is considerable even in the first cycle. It accounted for 43% of the peak stress for a 0.5-hour hold. However, with the following cycles, it decreased. At 50% life, the relaxed stresses were 33% of the peak stress. In such situations, exact knowledge of the creep rupture behavior is extremely important. Therefore, assessing the predictability of this method with the constants determined from a steel grade, under a particular condition, cannot be extended to the creep-fatigue data compiled in Paper I. Instead, only the data sets included in this review have been analyzed by this method.

1Cr-Mo-V, 1.25Cr-Mo and 2.25Cr-Mo have been assessed with this method. Comparison of the prediction capability of batches and their conditions will indicate a trend when this is the best method to apply. Table 2 tabulates the percentage of test data points within a factor of ± x 2 of observed life.

It can be seen that applicability of this method depends on material conditions and test temperature. With a decrease in temperature the prediction capability improved for both 1Cr-Mo-V and 2.25Cr-Mo steels in a range from 600 to 485°C. At 485°C it predicts 100% of test data points within a factor of ± x2, for both N&T and Q&T conditions, only for the tensile dwell cycles. For the annealed condition, prediction capability is very poor and this method is not applicable. With an increase in temperature to 600°C, 2.25Cr-Mo, Batches 3 and 7 in N&T condition, prediction trend is quite poor. Hence, test parameters such as temperature and material condition play a role in the prediction of life by the DSA method.

Table 2
Prediction capability of damage summation approach

Material	Batch No.	Heat treatment	% data within ± x 2	Temp. °C
1Cr-Mo-V	1	N&T	69	540
	1	N&T	100	485
	4	N&T	57	565
	4	N&T	43	565
2.25Cr-Mo	1	Annealed	29	540
	1	N&T	82	540
	1	Q&T	100	485
	3	N&T	70	600
	5	N&T	0	600

Frequency Modified Approach

This method underestimates damage severity for constant tensile load and is conservative for longer tensile hold tests, as observed by Priest and Ellison /1/. The healing effects of unbalanced cycles, with a 2-min. compressive hold on a 16-hour tensile dwell, were unaccounted for by this model /1/. Melton /35/ observed excellent agreement between experimental and predicted results for 1Cr-Mo-V, Batch 3, within an error factor of ± x1.5 for a hot-rolled bar material. The constants β' (K-1) = -0.076, which in the case of Batch 4 was -0.46. This method has not been thoroughly explored in the published literature for other batches of low alloy steel, since it requires the constants determined from unequal ramp rates, balanced and unbalanced tests. Table 3 describes the prediction capability of the method.

Strain Range Partitioning Technique

Constant strain hold in tension or compression are used to generate CP and PC components, respectively. A base line relationship has the following form /17/:

$$N_{CP\ or\ PC} = F_{CP\ or\ PC} / (1/n_{obsrv.} - F_{pp}/N_{pp}) \qquad (27)$$

Table 3
Prediction capability of frequency modified approach

Material	Batch No.	Heat treatment	% data within ± x 2	Temp. °C
1Cr-Mo-V	3	N&T	100	550
	4	"	66	565

Two steel grades of the 1Cr-Mo-V and 2.25Cr-Mo types have been assessed in the literature. Trends in the prediction capability for tensile dwell tests were reasonable, although not good for too short or long dwell cycles for Batch 4 /1/. This finding is supported by Melton /36/, for Batch 3 at 550°C. However, there are conflicting opinions about SRP prediction capability for 1CrMoV alloy /1/, type 304 SS /37/, IN-738 LC /38/, whereas it was shown as a reliable technique for Type 304, 316 SS, 2.25Cr-Mo /39/, In 100 /40/, 1Cr-Mo-V rotor steel /41/ and many others /42/. Bicego *et al.* /41/ investigated the predicted versus actual data of forged 1Cr-Mo-V at various strain rates and temperatures on specimens from different sampling positions from a rotor forging. In some cases life shortening effects were observed with strain rates of $3x10^{-6}$/sec, which was over-predicted by SRP, resulting in conservative prediction. Moderate life reductions with high temperature were observed with total strain ranges above 0.8%.

Halford *et al.* /39/ modified the SRP method with a ductility normalized approach. Priest and Ellison /1/ accounted for damage process and rates of deformation, associated with different damage mechanisms during hold times. Domains of dominant mechanisms were then defined to sub-partition the SRP loop. A good prediction with the ductility normalized SRP approach was reported /1/, which improved the prediction capability from 85% to 100% of test data points as tabulated in Table 4.

From Table 4, it is quite evident that SRP applies best to annealed and N&T conditions of 2.25Cr-Mo steel (e.g., Batches 1, 3 and 4 of 1Cr-Mo-V in N&T and 1 in annealed and 3 and 5 of 2.25Cr-Mo steels in N&T condition). The trend in the prediction capability remains unaltered; it rather improves with an increase in test temperature for several batches of 2.25Cr-Mo steel. However, Lloyd and Wareing /4/ concluded from the data of Day and Thomas /38/ and Halford *et al.* /39/ on SS 316 that, with an increase in temperature from 600-700 and 650 to 750°C, trends in the predicted life were outside the factor of 2 band. The prediction capability of SRP was found questionable only for the Q&T condition of 2.25Cr-Mo steel in Table 4, which needs to be verified with additional data.

Damage Rate Approach

Plumbridge *et al.* /43/ observed fatigue and creep damage in the case of Batch 4 (1Cr-Mo-V) at 565°C, independent of each other. Therefore, the damage rate equation was applicable, since it accounts for growth of fatigue damage in terms of cracks and creep by cavities. A very good prediction of Batch 4, 1Cr-Mo-V, and Batch 3 of 2.25Cr-Mo steels /1,2/ was observed (Table 5).

Table 4
Prediction capability of strain range partitioning technique (SRP)

Material	Batch	Heat treatment	% tests within ± 2	Temperature °C	Remarks
1Cr-Mo-V	1	N&T	75	540	
	1	N&T	100	485	
	3	N&T	100	550	
	4	N&T	85	565	SRP eq.
	4	N&T	100	Modified eq.	Ref. (1)
2.25Cr-Mo	1	Annealed	100	540	
	1	N&T	96	540	
	1	Q&T	58	485	worst case
	3	N&T	100	600	2 points
	5	N&T	100	600	

Table 5
Prediction capability of damage rate approach

Material	Batch No.	Heat treatment	% data within ± x 2	Remarks
1Cr-Mo-V	4	N&T	100	at 565°C
2.25Cr-Mo	3	N&T	100	at 600°C with two data points, 5 min. hold.

Table 6
Prediction capability of hysteresis energy approach

Material	Batch No.	Heat treatment	% data within ± 2	Remarks
1Cr-Mo-V	4	N&T	61	at 565°C (Ostergren's)
"	4	N&T	78	565°C (Priest *et al.*, 82) /1/
2.25Cr-Mo	3	N&T	100	at 600°C for 0/0 and two tests with 5 min. hold.

Hysteresis Energy Approach

The prediction capability of this method for 16 hours of tension-only hold tests was non-conservative for 1Cr-Mo-V steel, Batch 4 /1/. However, for compressive and balanced cycles, prediction was within the scatter band of ± x2. Since this method was originally proposed using the constants of continuous fatigue, prediction of hold time cycles is questionable. A further modification of Ostergren's equation was proposed /1/, using the hold time data to determine the constants. This improved the prediction capability, as tabulated in Table 6. The equation proposed in /1/ has the following form:

$$[N_f \nu^{K-1} (\nu_t/\nu)^r] \propto \Delta\varepsilon_p \, \sigma_t = C \qquad (28)$$

With the above modifications, very little creep-fatigue data have been assessed that need to be validated with additional data.

Damage Parameter Approach

A non-conservative prediction was observed for tension-only hold periods of 1Cr-Mo-V steel Batch 4. Though better predictions were observed for compression-only hold, this might have been due to the choice of stress rather than strain as a parameter, governing damage accumulation in Equation (16). Further work on this model and its extension to compressive dwell cases needs to be carried out. Applicability of this method with creep-fatigue data is shown in Table 7.

Table 7
Prediction capability of damage parameter approach

Material	Batch No.	Heat treatment	% data within ± 2	Remarks
1Cr-Mo-V	4	N&T	50	at 565°C
2.25Cr-Mo	3	N&T	100	at 600°C with 0/0 and two tests of 5 min. hold.

Assessment Procedure R 5

Two low alloy steels of the type 0.5Cr-Mo-V and 1Cr-Mo-V were reviewed in this paper. In the case of 0.5Cr-Mo-V steel, 75% of the test data points were predicted within a factor of ± x2. As the life range decreases, the trend in the prediction capability was found to improve. In the case of 1Cr-Mo-V steel, 56% of the test data points were predicted within a factor of ± x2. The same trend follows also in the case of 1Cr-Mo-V steel, i.e., the prediction capability improves only in lower life ranges (a few hundred cycles). As this method is very new and not yet widely assessed with a range of creep-fatigue data, more work needs to be undertaken to comment on the applicability of the method.

Diercks' Empirical Approach

Kitagawa *et al.* /32/ assessed this method with the creep-fatigue data of 2.25Cr-Mo and 9Cr-1Mo steels. A maximum of 10 min. hold times were applied, and

100% test data points within a factor of ± x 2 were observed. The prediction capability of this method appears promising mainly because it is a simple mathematical equation, predicts life and does not require any creep-fatigue tests as shown in Table 8.

CONCLUDING REMARKS

1. Life prediction, within phenomenological methods, requires a large number of material parameters from laboratory tests. A parametric relationship evolved from these results often lacks generalization to global creep-fatigue data. Hence most methods are dependent upon test parameters and material conditions.
2. The empirical methods of life prediction with scaling factors to simulate the creep-fatigue life for other materials was found promising in the literature.
3. There is a lack of publications that describe creep-fatigue data and assess them with methods of life prediction. No important material or test details are revealed in the literature.
4. Various material parameters determined from one type of test for one low alloy steel type cannot be extended to all the creep-fatigue conditions of the same or other steels.
5. Trends in the methods of life prediction exhibit test and material parameters, such as strain rate, temperature, material condition and heat treatment dependent behavior. With increasing temperature, the prediction capability of most methods deteriorates.
6. Several modifications of the existing life prediction methods are possible, specific to a data type. Such modifications should be examined with more data to demonstrate the applicability of the modified method of life prediction.

Table 8
Prediction capability of Diercks' empirical method

Material	Batch No.	Heat treatment	% data within ± 2	Remarks
2.25Cr-Mo	Data unknown classified	N&T	100	at 470°C with 10 min. hold.
9Cr-1Mo	Date unknown classified	N&T	100	at 600°C (unknown holds)

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