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Creep-Fatigue of Cu-Cr-Zr: A Review of the existing Fatigue, Creep and Creep-Fatigue Data Base and a Life Prediction Analysis using a Time based Damage Evaluation

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Deliverable 1

Creep-Fatigue of Cu-Cr-Zr: A Review of the existing Fatigue, Creep and Creep-Fatigue Data Base and a Life Prediction Analysis using a Time based Damage Evaluation

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Summary:

1. Introduction

Cu-Cr-Zr alloy is a primary candidate material for the heat sinks at the first wall of the ITER fusion reactor. Therefore extensive research was conducted in the past on the fatigue properties of Cu-Cr-Zr, under relevant ITER operating conditions. Unfortunately only few data exist on the creep-fatigue behaviour, mainly due to the very costly testing involved.
A collection of ITER data files, reports, reviews articles and papers on the fatigue and creep-fatigue of Cu-Cr-Zr has been delivered by EFDA and ITER, in order to verify the suitability of the linear damage accumulation rule for life predictions of creep-fatigue tests. The time based approach as proposed in the French RCC-MR design code [1, 2] will be used to check the possibility of reliable life predictions , based on existing fatigue and creep data. This method assumes that fatigue and creep damage are linearly additive and that a life prediction can be made based on the partition of pure creep and pure fatigue time.
The method requires a knowledge of the relevant fatigue life equations, the cyclic hardening curve and the creep rupture time. The method can be applied with full accuracy if the particular relaxation curve at the holding point is known, which of course requires the completion of at least a partial creep-fatigue test.
The analysis will be restricted to total strain controlled tests.

Ideally , the data should be collected from alloys close to the reference ITER Grade alloy, which has the following characteristics:

Chemical analysis: Cr:0.6-0.9%, Zr:0.07-0.15%, Si:0.003%, rest Cu.
Heat treatments: SAA, (solution annealed + aged) 30min at 975°C, water quenched, 2-4 hrs at 475°C, water quenched
Hardness: 130HV30 (DIN 50133)

For practical and necessary reasons, deviations from these conditions are possible and discussed for each case.

2. Fatigue data

It is necessary for further analysis to represent the data considered in the form of a fatigue life equation. The most useful equation is the Langer equation, because it is not connected to the physical mechanisms and contains therefore only dimensionless mathematical parameters:

$$\Delta\varepsilon_t = A + B \cdot N_f^C \qquad (1)$$

Where A, B and C are constants.

The Langer equation will give a poor fit, if the data points are not regularly distributed. To ensure a good fit, the following procedure is recommended [3]:

Rearranging equation (1) and solving it at two points $N_{f,1}$ and $N_{f,2}$ gives:

$$\Delta\varepsilon_{t,1} - A = B \cdot N_{f,1}^C$$
$$\Delta\varepsilon_{t,2} - A = B \cdot N_{f,2}^C \qquad (2)$$

which multiplied gives:

$$(\Delta\varepsilon_{t,1} - A)(\Delta\varepsilon_{t,2} - A) = B^2 \cdot (N_{f,1} \cdot N_{f,2})^C \qquad (3)$$

Now if another point $N_{f,3}$ is chosen as the geometric mean of $N_{f,1}$ and $N_{f,2}$, substitution in equation (3) and rearranging gives ($N_{f,3} = (N_{f,1} + N_{f,2})/2$):

$$A = \frac{\Delta\varepsilon_{t,3}^2 - \Delta\varepsilon_{t,1} \cdot \Delta\varepsilon_{t,2}}{2\Delta\varepsilon_{t,3} - \Delta\varepsilon_{t,1} - \Delta\varepsilon_{t,2}} \qquad (4)$$

The parameters of equation (4) are chosen such that the curve is forced to pass through the isolated points corresponding to low strain amplitudes, where fatigue experiments are difficult to conduct. With factor A fixed, the fitting of equation 1 is normally successful.

The first set of data provided is from a work of K.D.Leedy et al.[4] . For practical reasons this dataset is called DS1 (Fatigue data set one).The tested material is from Tréfimétaux and the material was given a P.A. (prime aged) heat treatment. The alloy was solution annealed for 1 hr at 950°C, water quenched , aged 0.5hr at 475°C and water quenched again. 18 tests have been completed at room temperature. The data are plotted and fitted to the Langer equation in figure 1, using the procedure described above.

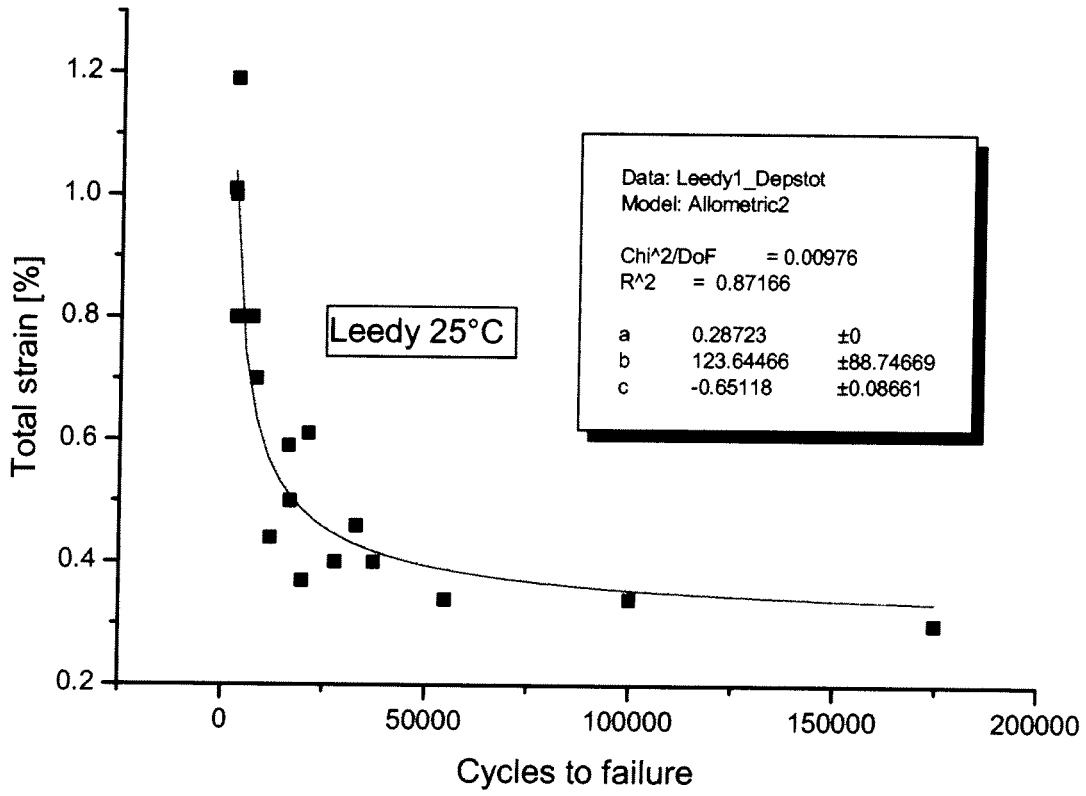

Figure 1: Fatigue data from K.D.Leedy et al [4].Cu-Cr-Zr tested at 25°C

The second set of data (DS2) is from a final report from A.F. Rowcliffe et al. [5] on copper alloys from the US Home Team and the data have been produced also by K.D. Leedy and J.F. Stubbins. The heat treatment and the material are the same as in the first dataset, but the specimens were cooled in vacuum instead of in air, after the ageing treatment. The tests were conducted into vacuum. The data are presented in Fig. 2.

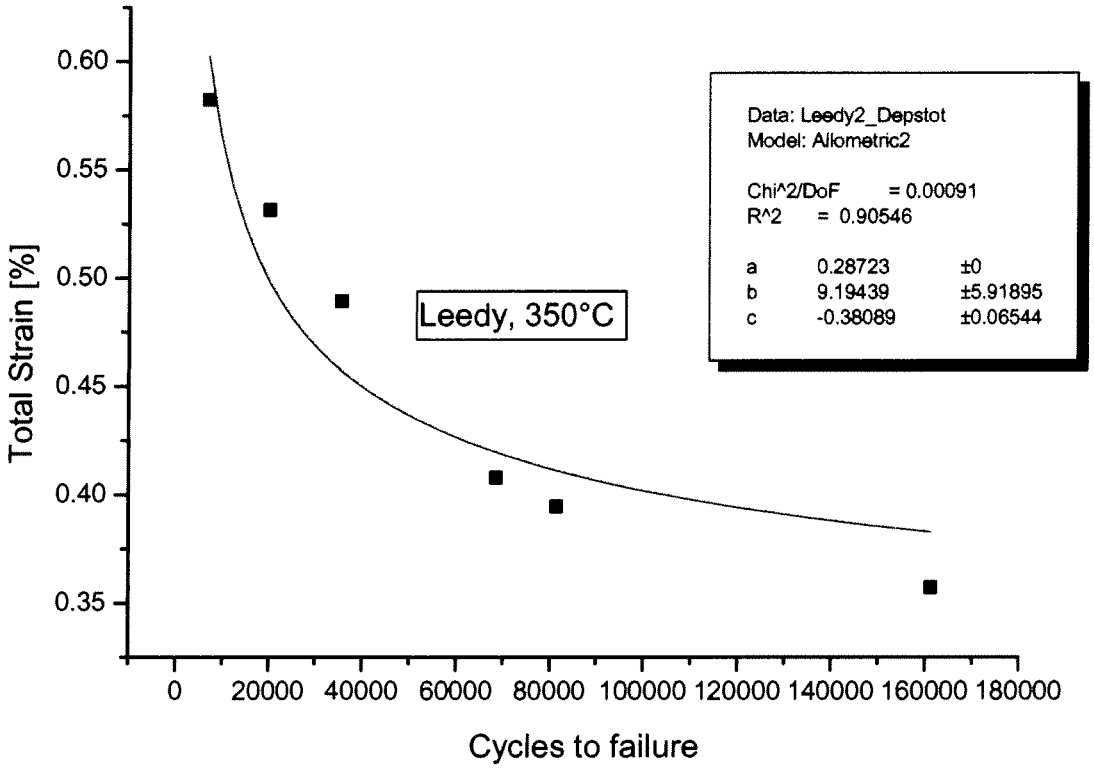

Figure 2: Fatigue data from K.D.Leedy et al. [5] obtained in Cu-Cr-Zr at 350°C

The third set of data DS3 is also extracted from [5]. The data are all produced in air, between 25 and 500°C. The material is an Elbrodur N produced by Kabelmetal. As compared with the Tréfimétaux, the alloy has a different chemical composition, different heat treatments and different tensile properties. The data are presented in Fig.3. We propose to reject the 500°C data because they are clearly to the left of the other data. Some strong temperature effects are probably present at 500°C. The data between 25 and 350°C do fit well with the preceding datasets when represented in a fatigue endurance diagram (see Fig.6). As reported in [5], part 9.4.2, Cu-Cr-Zr Tréf. and Cu-Cr-Zr Kabel have different stress response and fatigue performance. Therefore a direct comparison of both alloys may not be recommended . Nevertheless we retain the data DS3 without the 500°C data points.

The next set of data DS4 is from Bretherton et al. [6]. The material is very close in chemical analysis to the Tréfimétaux and received a similar SAA heat treatment with a longer aging time. 17 fatigue tests were conducted at 300°C. The data are presented in Fig.4.

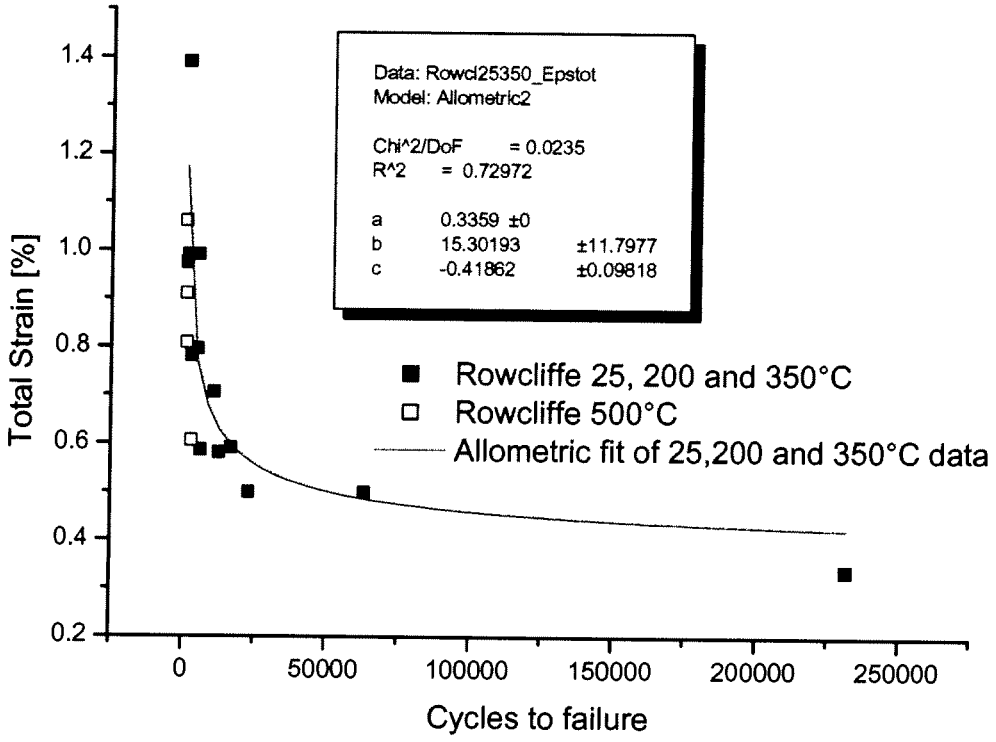

Figure 3: Fatigue data from Rowcliffe et al [5].Cu-Cr-Zr Kabelmetal/Elbrodur N.

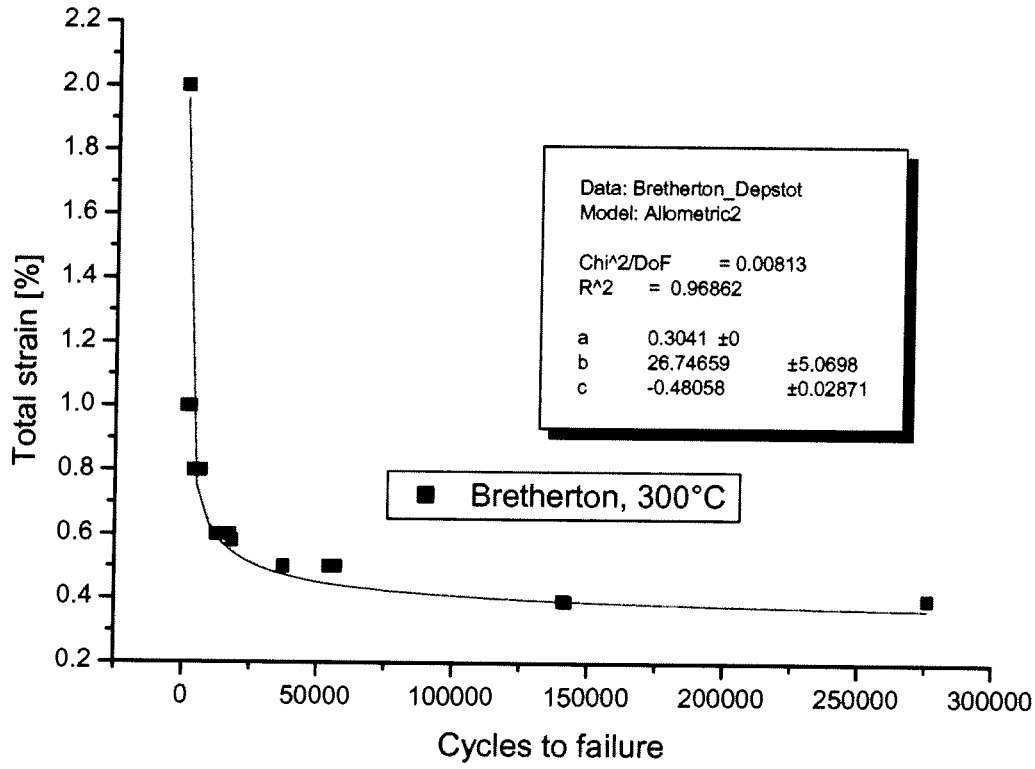

Figure 4: Fatigue data from Bretherton et al [5].Cu-Cr-Zr tested at 300°C

Meimei Li et al.[7, 8] have also conducted fatigue and creep fatigue tests in Cu-Cr-Zr at 25°C. Two alloys of Cu-Cr-Zr have been tested, a Tréfimétaux and an Outokumpu. Both alloy differ in chemical analysis and heat treatments. The Tréfimétaux was PA at 975°C for 30min, WQ, and then aged at 475°C for 30min and WQ. The Outokumpu was annealed at 960 °C for 3 hrs, WQ, then aged at 460°C for 3 hrs and WQ. The Tréfimétaux was tested with specimens having 3.1 mm diameter and the Outokumpu was tested with standard size specimens with a 6.35 mm gauge diameter. The results of both series is given in figure 5. Despite their differences, the data seem to fit well together and will be subsequently treated as a single data set DS5.
The next data set DS6 has been produced by Briottet et al.[9]. The tests were conducted at 20°C, into air. The first material is a Cu-Cr-Zr from Zollern which received an HIP cycle of 2hrs at 1040°C with subsequent quench and 2hrs at 460°C. The chemical composition is not accurately known and is given as 0.5-1.2%Cr and 0.03-0.3%Zr. The grain size is very large at 939 microns. The second material is an Elbrodur G, which received the same heat treatment. The chemical composition is 0.8%Cr and 0.146%Zr. The average grain size is 796 microns. The third material is a Osprey powder compacted by an HIP process as described above. The chemical analysis is 0.79%Cr, 0.09%Zr and 0.013%Fe.The average grain size (powder) is 26 microns. The results are presented in figure 5. The Zollern material does not follow well the trend of the other data. It is removed from data set 6.

The data sets DS1, 2 ,3, 4 ,5 and DS6 have been plotted in a single diagram with their respective fitting curves, in figure 6. Obviously the last data set DS5 fits well the other data points and therefore can be considered in further analysis.

Finally the remaining fatigue data are plotted in a common diagram and a fitting curve according to equation 1 is drawn. This is shown in figure 7. The Langer equation fitting best our accepted data points is:

$$\Delta\varepsilon_t = 0.2 + 17.721 \cdot N_f^{-0.4034} \qquad (5)$$

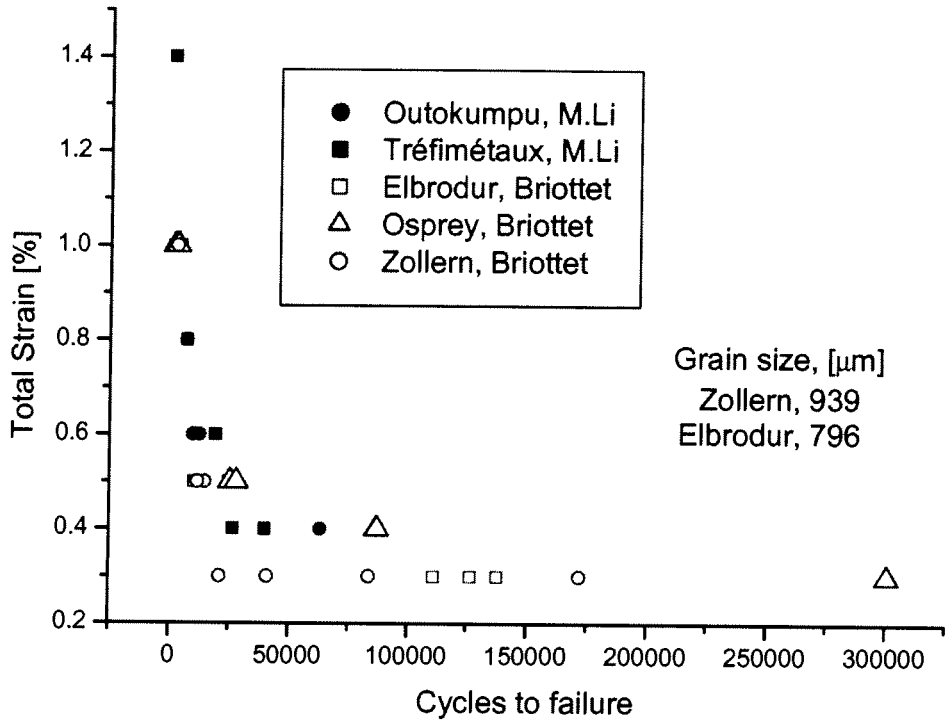

Figure 5: Fatigue data (DS5) from Memei Li et al[7, 8] and Briottet et al [9] (DS6) in Cu-Cr-Zr tested at room temperature.

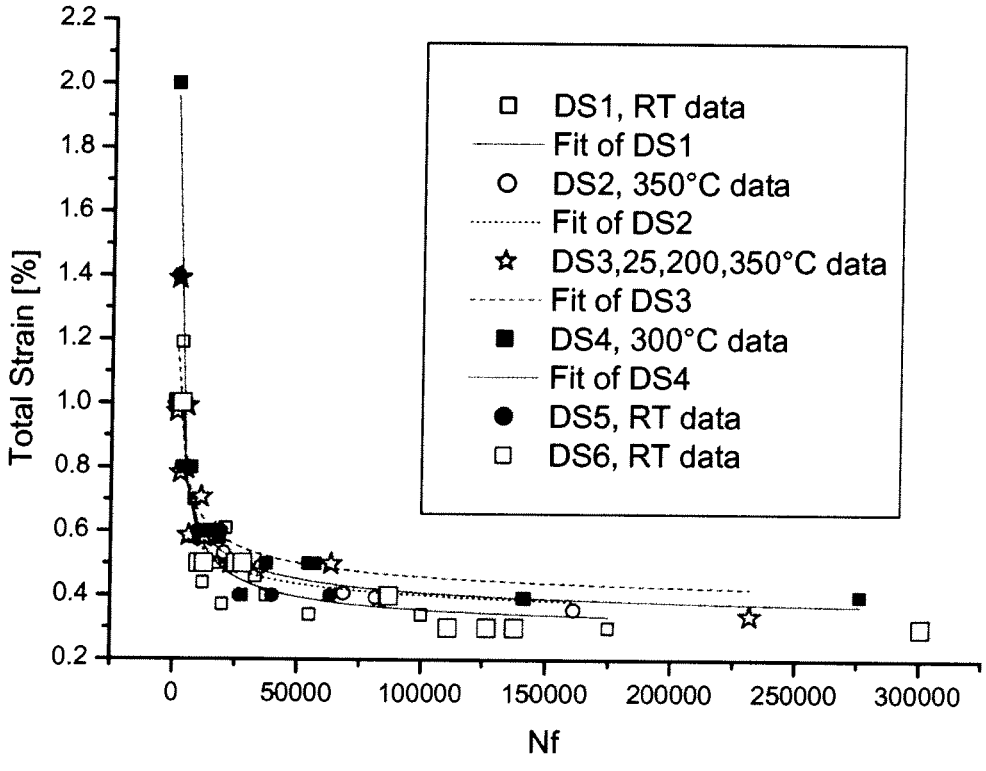

Figure 6: All fatigue data plotted together.

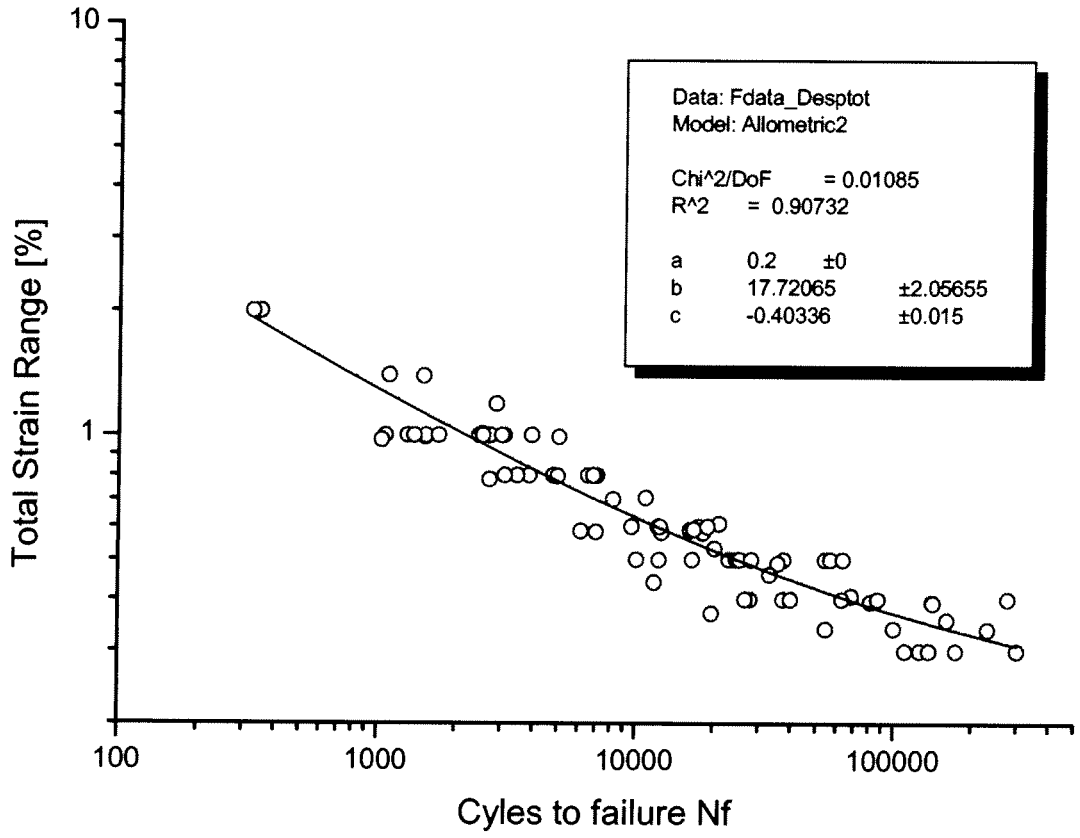

Figure 7: Fatigue life curve of Cu-Cr-Zr between RT and 350°C.

3. Creep data

To apply the linear damage accumulation rule for creep-fatigue interaction, a complete set of time to rupture data, covering the stress and temperature domain, are required. The collected creep data should come from materials matching with the greatest possible accuracy, the chemical analysis and heat treatments of the alloy of interest, as defined in the introduction. In practice, all authors present data from alloys with different chemical analyses and sometime completely different heat treatment procedures. On the other hand, some flexibility in mixing data from different sources is nevertheless allowed, realizing that:

Chemical analysis effects:

The primary alloying element for gaining a good creep resistance is zirconium. Alloying 0.11% is improving significantly the creep resistance at 200°C of a Cu-Cr alloy [10, 11]. It was also shown that alloying as little as 0.04% is enough to build up the creep resistance and that a further increase to 0.11% Zr has no significant effect. The ITER grade Cu-Cr-Zr has 0.07 to 0.15%Zr. Therefore, on the base of the work of Tautzenberger [11], we can argue that small differences in Zr will have no influence on the creep properties. The elements which will degrade the creep properties by weakening the boundaries are Bi, Se, Pb, S, Sb, Sn and H [10]. Normally these elements are present in the alloys as impurities. If present in large quantities, a deterioration of the creep properties can be anticipated.

Heat treatments effects:
The effect of little variations in the heat treatments is not clear. For sure, large variations will affect strongly the creep properties, as shown by Tautzenberger[11] for a comparison of a SAA and a cold worked Cu-Cr-Zr alloy.

Effect of temperature:
Creep is a temperature dependent process and the creep rates will increase rapidly with increasing temperature (see Fig.4 in [12]). A general expression for the creep rate of the secondary creep stage is:

$$\dot{\varepsilon} = A \cdot f(\sigma) \cdot e^{-\frac{Q}{RT}} \quad (6)$$

Some results however, seem to indicate that a steady creep rate is never reached and that the creep rate is decreasing with increasing time [12, 13]. Therefore a time dependent term should be added to equation (6).
It is assumed that both pre-exponential terms are independent of temperature, which seems to have been demonstrated experimentally for Cu-Cr-Zr having a similar chemical composition, see for instance the paper of Li et al [12].
This fact is allowing us to use the approach of Larson-Miller and to use a single parameter combining creep time and temperature and to relate it with creep stress. In this way, a larger number of data produced at different temperatures can be collected to form a creep master curve. A creep behaviour obeying a power law would not be suitable for a Larson-Miller approach.

Compatibility with previous data:
Creep data from materials from which the fatigue data had been rejected previously should be considered only with care.

Data collection:

For a time based creep-fatigue life prediction, the data of interest is the rupture time as a function of the applied stress, at the temperature of interest.

The first dataset called CDS1 (creep dataset 1) is from Tautzenberger [11].The creep specimen is cylindrical with a diameter of 3.2 mm. The alloy composition is similar to the ITER grade alloy but with a Zr content of 0.04%. The authors have shown that the behaviour of the alloy is similar to an alloy having 0.11%Zr. The heat treatment is SAA, 15 min at 980°C and 3 hours at 475°C. The data are shown in figure 8.

The second dataset called CDS2 is again from Bretherton et al [6]. The heat treatment is SAA, 20 min at 980°C and 3 hours at 475°C and very similar to the above one. The specimen used has a cylindrical geometry with a diameter of 6.3mm.The chemical analysis is Cu-0.84Cr-0.1Zr.

An isolated point from a commercial datasheet (Amax) measured at 300°C is added in figure 8. The alloy is a Cu-0.15Zr-0.8Cr-0.04Mg commercial alloy. Heat treatment is without more precision, 80% cold-worked and precipitation hardened.

The next dataset CDS3, 4 and 5 have been produced by Briottet et al.[9]. CDS3 was produced at 300°C, into vacuum. The material is a Cu-Cr-Zr from Zollern which received an HIP cycle of 2hrs at 1040°C with subsequent quench and 2hrs at 460°C. The chemical composition is not accurately known and is given as 0.5-1.2%Cr and 0.03-0.3%Zr. The grain size is very large at 939 microns. The data (4 pts) are presented in figure 9.
CDS4 was produced at 300°C, into vacuum. The material is an Elbrodur G which received the same heat treatment as CDS3. The chemical composition is 0.8%Cr and 0.146%Zr. The average grain size is 796 microns. The data (6 pts) are presented in figure 9.
CDS5 was also produced at 300°C. The material is a powder compacted by an HIP process as described above. The chemical analysis is 0.79%Cr, 0.09%Zr and 0.013%Fe.The average grain size (powder) is 26 microns. Five data points have been produced. The results are presented in figure 9.
As can be seen in figure 9, the dataset CDS3,4and 5 show quite a large dispersion. Especially the dataset CDS5 is clearly below CDS3 and 4.

CDS6, 7 and 8 are an old data collection already presented in the ITER MPH under document G74MA16/ITER-AK02-2202. The data were produced in Russia (V.M. Rosenberg and A.K. Nikolaev, "Zvetnye Metally", 1972-8) and from a private company, AMAX-Copper Inc, 1976, material MZC. MZC is a Cu-Cr-Zr alloy with small Mg additions. CDS6 covers the Russian alloy tested at 100°C, which received an SAA heat treatment +47% cold work (960°C,2.5hrs at 480°C). CDS7 is for the Russian and American materials tested at 300°C, SAA + 47% cold work as for CDS6. The dataset CDS8 covers again both materials and was produced at 350-400°C. The data are presented in figure 9. CDS6 at 100°C seems to fit well to the other data, whereas CDS7 and 8 show a poor correlation with the other datasets.
CDS9 contains two Cu-Cr-Zr data points from Taubenblatt et al reported (pp19-29) in a book by Ling et al[14] on copper alloys. The material is a MZC alloy containing Mg additions.CDS10 contains two other Cu-Cr-Zr data points produced by Harling et al.[15]. The material is MZC (0.02%Mg, 0.2%Zr, 0.6%Cr). The 100hr rupture strength has been determined and the four data points taken at 300, 400 and 450°C are presented in figure 9.

Finally dataset CDS11,12,13,14 contain creep data from a russian book by Nikolaev, Novikov and Rosenberg (Copper-Chrome Alloys, Moscow Metallurgy, 1983), for Cu-Cr-Zr tested at 100°C (CDS11), 200°C (CDS12), 300°C (CDS13) and 350°C (CDS14). In fact, the data were produced by Blaukloh et al[16]. The alloy chemical composition is 0.8%Cr and 0.11%Zr.

The data CDS11 to 14 have been plotted in figure 8, for a better visibility. At the exception of the 100°C data, they do not show a very good correlation with the other data.

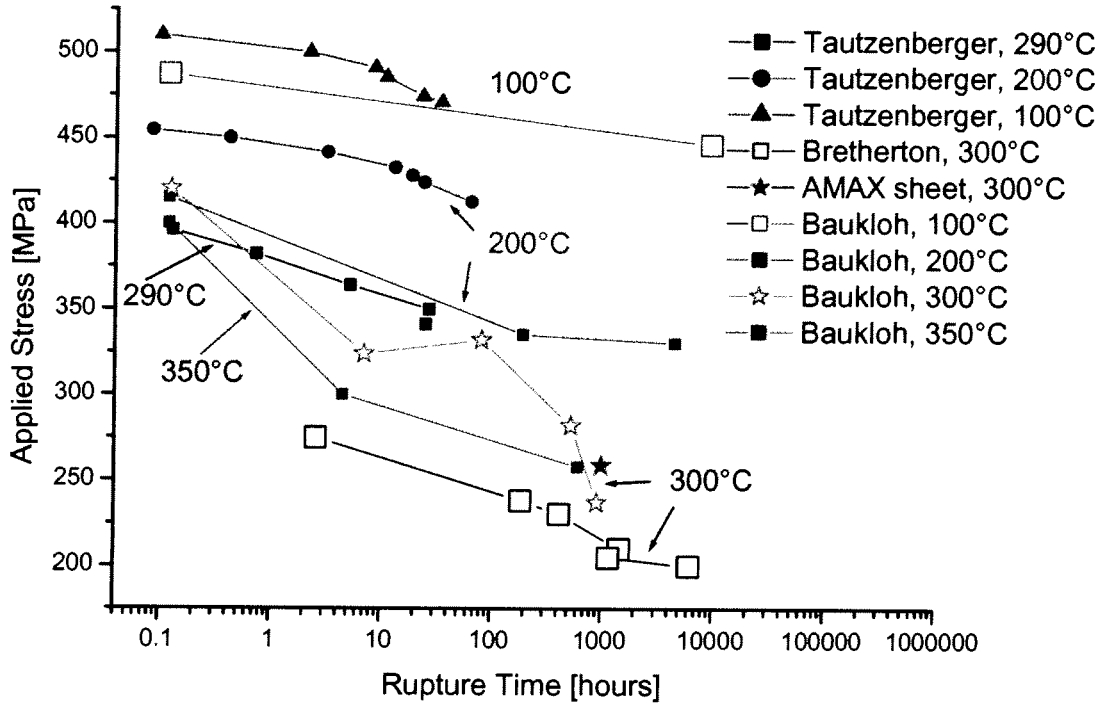

Figure 8: Creep rupture time curves of Cu-Cr-Zr for CDS1 and CDS2 and CDS11,12,13 and 14 between 100 and 350°C.

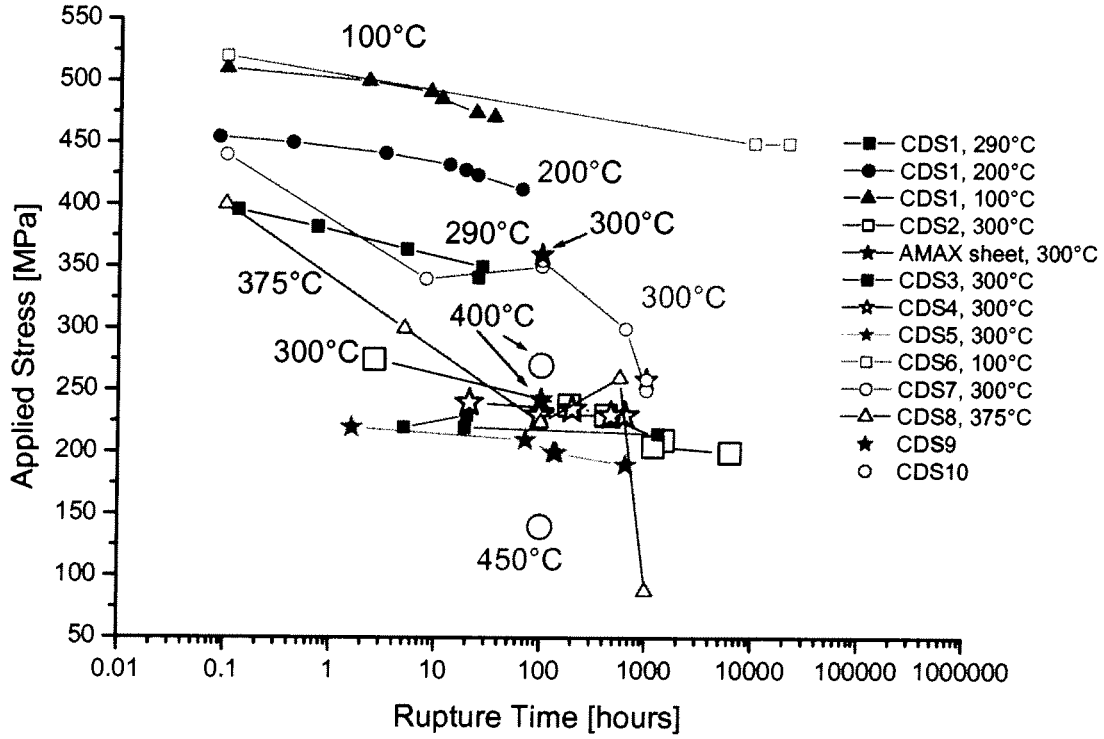

Figure 9: Creep rupture time curves of Cu-Cr-Zr for CDS1 to CDS10 between 100 and 450°C.

Larson-Miller approach:

The Larson-Miller method uses stress-rupture parameters to correlate stress-rupture data. It is not applicable if the creep equation is a power stress law but it works fine for a diffusion driven creep process like Coble creep. Assuming that most of the creep test is during stage II with a strain rate according to equation 6, it is reasonable to consider that the rupture time t_R will be inversely proportional to the second stage creep rate:

$$t_R = \frac{\Delta\varepsilon}{\dot{\varepsilon}} \qquad (7)$$

A re-arrangement of equation 6 and 7 yields the Larson-Miller equation [17]:

$$P = T(C + \log t_R) = a - b \cdot \log\sigma \qquad (8)$$

The constant C is normally around 20 for most metals. The constant C can be evaluated by looking for the best fit for equation (8) or by applying the procedure described by Conway [18]:
Re-arranging equation (8):

$$\log\sigma = \frac{T}{b}\left(\frac{a}{T} - C\right) - \frac{T}{b} \cdot \log t_R \qquad (9)$$

Applying equation 9 at two different temperatures having the same rupture time and choosing for simplicity t_R= 1 hour ($\log t_R$=0) yields the following result for the constant C:

$$C = \frac{b}{(T_1 - T_2)} \log\frac{\sigma_1}{\sigma_2} \qquad (10)$$

The data shown in Fig. 8 (less Blaukloh data) have been plotted according to equation 9. The results are shown in Fig. 10. For each data set a good linear fit is obtained, thus confirming that the data obey a relationship as per equation 8. Furthermore, the slope of each dataset steepens with increasing temperature, as it should be. The slope –T/b is indicated in the legend of figure 10.
Using only the data of Tautzenberger, the estimated C value between 290°C and 100°C is found to be equal to 20.52. Taking all data, an estimation for C yields a value of 30.79. This rather large discrepancy for C could be an indication of incompatibility between the data of Bretherton and Tautzenberger.

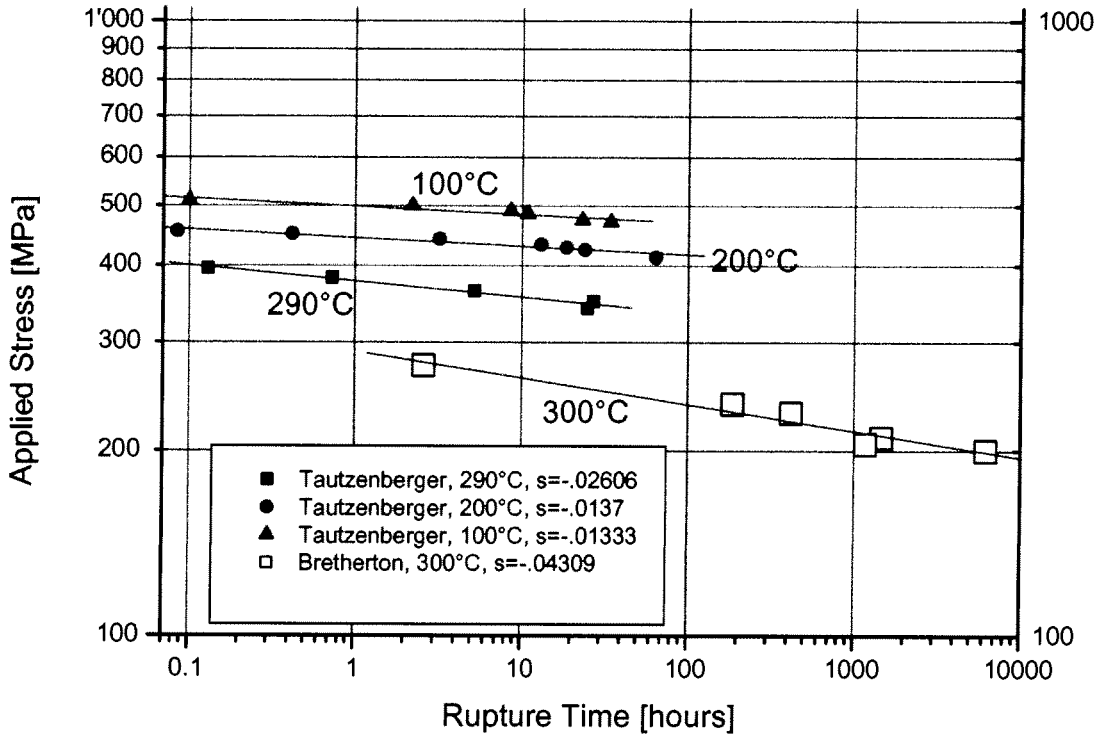

Figure 10: Log-Log creep curves of Cu-Cr-Zr for CDS1 and CDS2 between 100 and 300°C.

Finally a tentative master curve for CDS1+2 is proposed in figure 11, where the Larson-Miller parameter P (equ.8) has been plotted against the logarithm of the creep stress. C has been chosen as the mean value of the estimations done above.

A representation of the creep data of figure 8 according to equation 8 is possible using the following parameters, giving T in Kelvin and creep time in hours (see figure 11):

C=25
a=6.392194E+4
b=1.991096E+4

$$T(25+\log t_R) = 6.392\cdot 10^4 - 1.991\cdot 10^4 \log\sigma \qquad (11)$$

It is questionable whether equation 11 represents well all data. For sure more data should be available in order to be able to reject bad data and retain only data with a closer fit. The fit of the Bretherton data is also made in figure 11 and the equation reads:

$$T(25+\log t_R) = 4.707\cdot 10^4 - 1.330\cdot 10^4 \log\sigma \qquad (12)$$

The 72 available creep data points CDS1 to CDS14 are plotted in a log-log plot in figure 12. Some data sets taken at the same temperature are fitted to lines with different stress levels. These differences are reflecting the high sensibility of creep to the microstructural and chemical features of the material. Finally a tentative master curve for all data is proposed in

figure 13. The data dispersion band is too large and the mean curve cannot be called a master curve for the material.

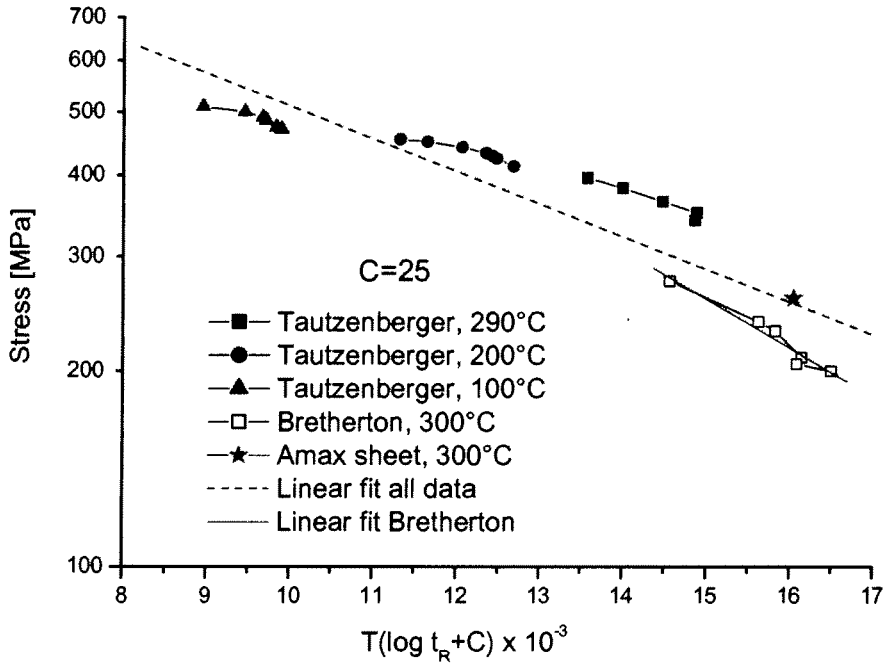

Figure 11: Tentative creep master curve for Cu-Cr-Zr between 100 and 300°C.

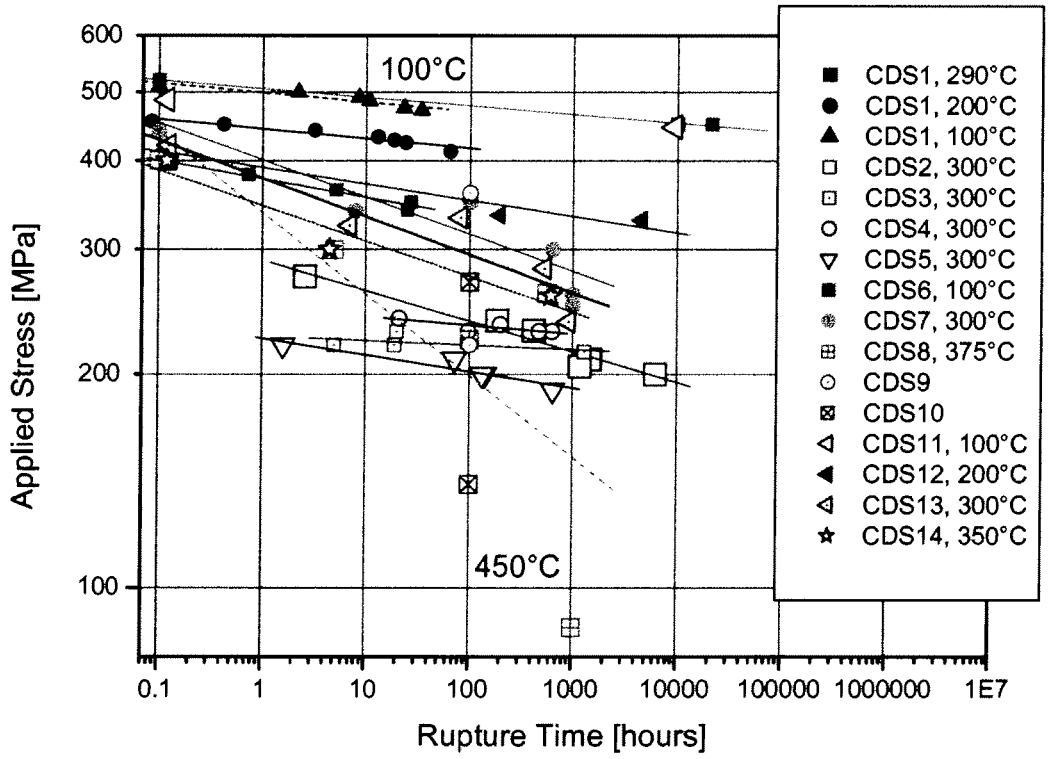

Figure 12: Log-log plot of all Cu-Cr-Zr time to rupture data between 100 and 450°C.

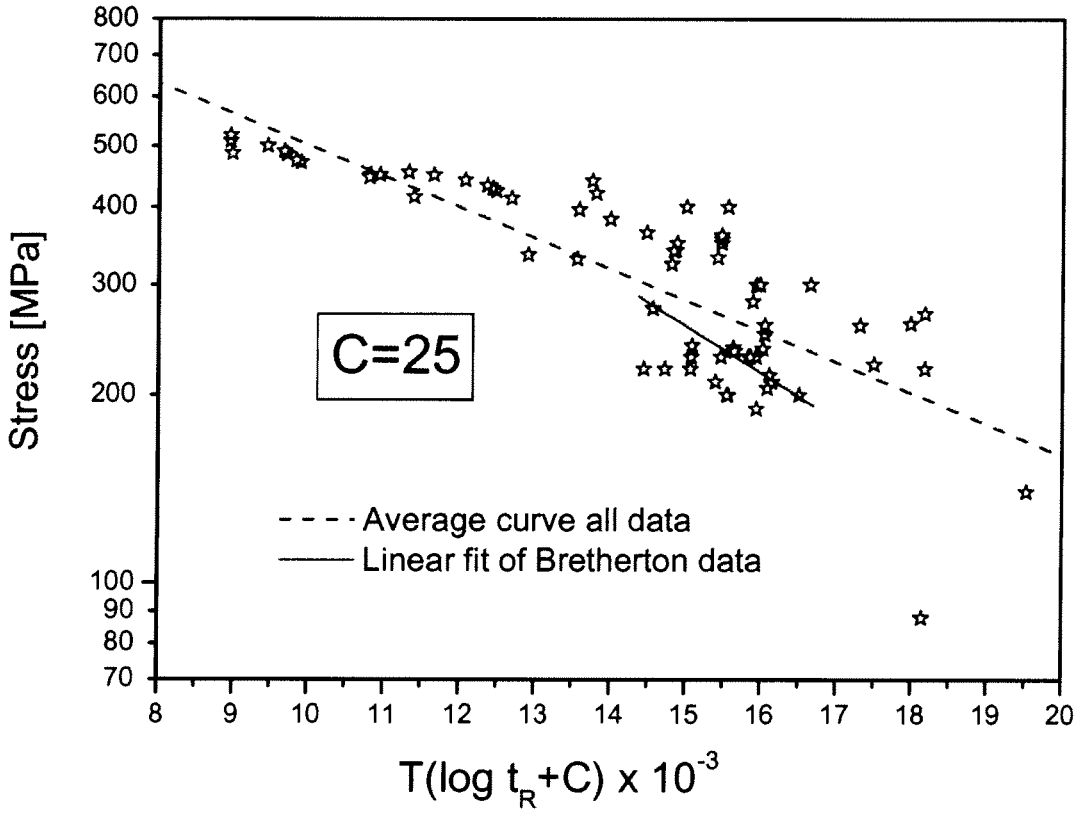

Figure 13: Tentative creep master curve for Cu-Cr-Zr between 100 and 450°C.

4. Creep-fatigue data

Creep-fatigue data are the most difficult to produce and apparently the subject has not been addressed frequently, therefore the published data are very rare. Bretherton et al [6] have performed a series of 15 creep-fatigue tests on Cu-Cr-Zr. The tests were conducted in vacuum at 300°C, applying a tensile dwell of 300 sec. The plot of those tests is shown in figure 14, together with the results obtained in monotonic fatigue testing. The introduction of the tensile dwell produces a strong reduction of the endurance, at low and high applied strain ranges. In order to perform creep-fatigue life predictions, two more information are essentials: the relation between total strain range and total stress range, at half life and the time shape of the stress relaxation during the tensile dwell. Both relations are temperature dependent and must be provided for each temperature of interest.

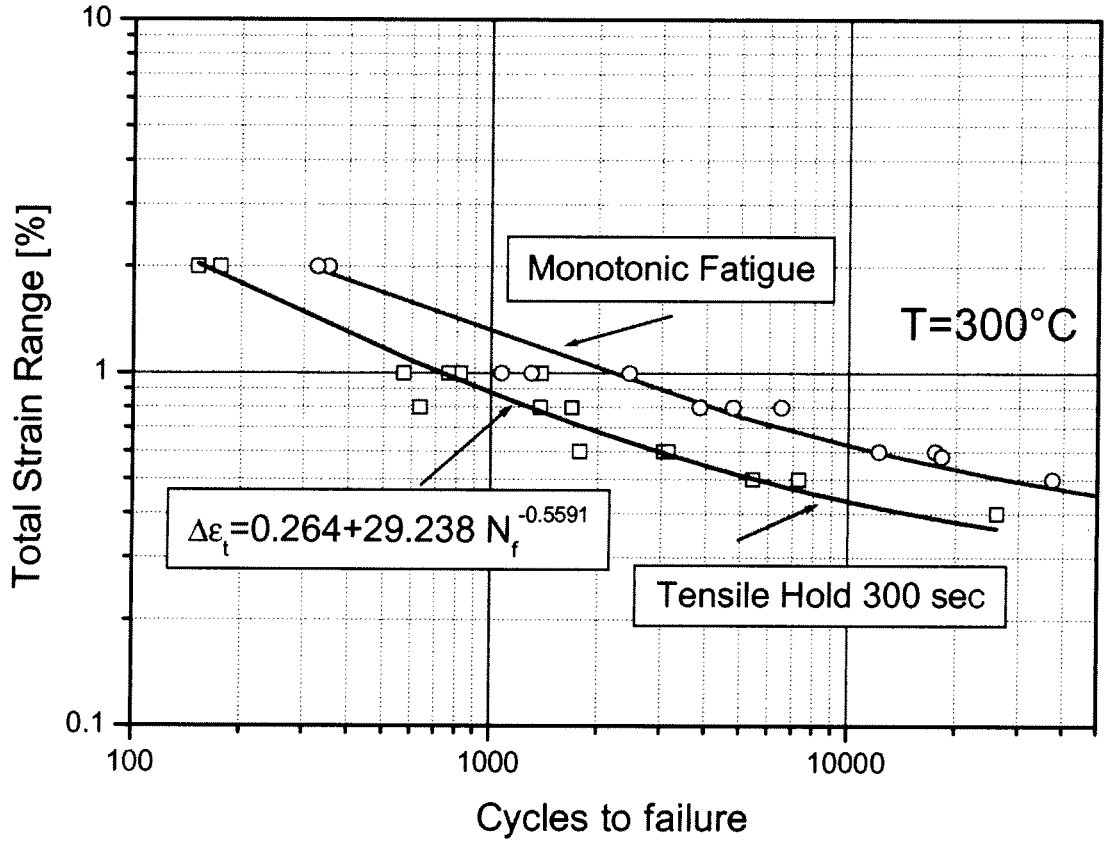

Figure 14: Langer plot of the Bretherton creep-fatigue data points.

4.1 Cyclic stress strain curve

The cyclic stress strain curve at each particular test temperature can be described by the Ramberg-Osgood equation:

$$\Delta\varepsilon_t = \frac{\Delta\sigma}{E} + \left(\frac{\Delta\sigma}{K}\right)^{\frac{1}{n}} \qquad (13)$$

Values for E, K and n were calculated for Cu-Cr-Zr at 300°C by Bretherton [6], at half life. E is a measured value of the modulus of elasticity, representing the cyclic behaviour:

E=120000 MPa
K=910.823
n=0.12257

Bretherton et al.[6] have also shown that there is only little difference between the fatigue and creep fatigue cyclic stress strain curves, thus indicating that at 300°C, the 300 sec dwell is not affecting the deformation behaviour significantly. Using equation 13 we can predict the maximum stress at dwell, from the given applied total strain. The solution must be found

numerically because the Ramberg-Osgood equation cannot be solved for stress. This is shown in figure 15 where the original curve has been fitted with an allometric equation similar to equation 1. The equation representing the Ramberg-Osgood data points is:

$$\Delta\sigma = 933.8 - 149.8 \cdot \Delta\varepsilon_t^{-0.238} \qquad (14)$$

As can be seen in figure 15, the real data are not well represented by equation 13. Therefore a new equation has been searched, fitting directly the data as they appear in the results table of Bretherton. The resulting curve is shown in figure 15:

$$\Delta\sigma = 644.9 - 1.74 \cdot \Delta\varepsilon_t^{-0.9298} \qquad (15)$$

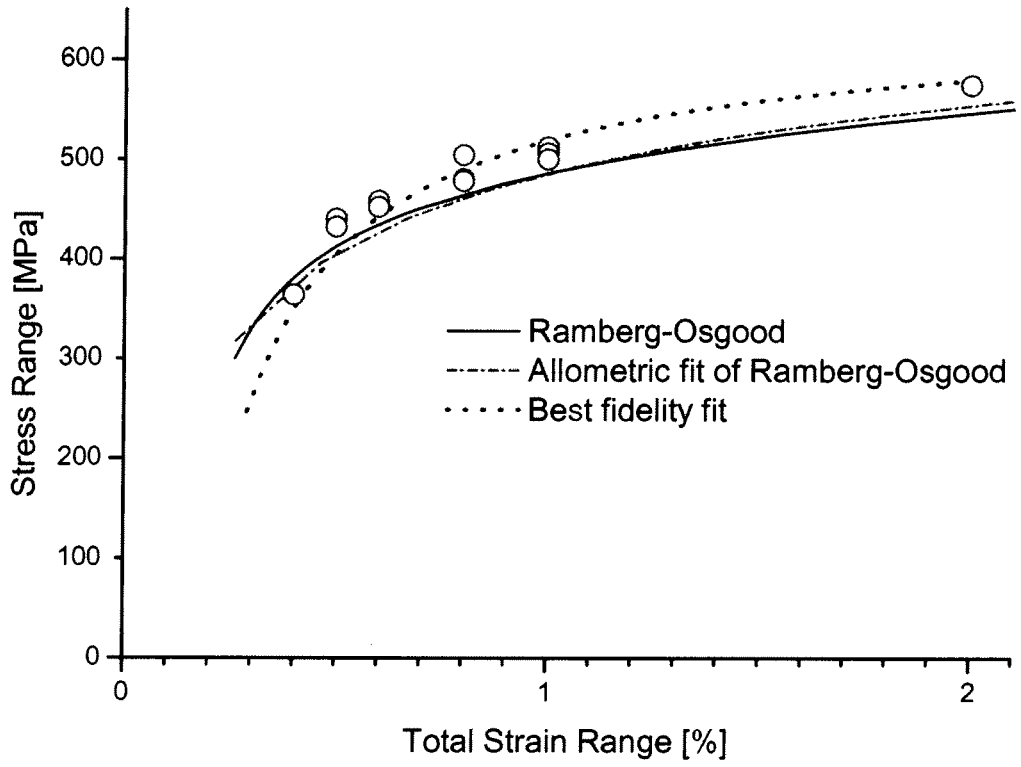

Figure 15: The Ramberg-Osgood cyclic stress-strain curve and its reciprocal fit curves

4.2 Stress relaxation during dwell

The stress as a function of time is best described in Cu-Cr-Zr by the Conway relation [6, 19]:

$$\sigma = \frac{a_0}{Exp\left(a_1 t^{a_2}\right)} \qquad (16)$$

a_0 has a value close to the stress at start of the relaxation and can be estimated from equation 13 to 15. For every condition, it is necessary to find the relaxations constants a_0, a_1 and a_2. Bretherton et al have determined the relaxation constants corresponding to their tests, at half life. In figure 16, the different relaxation fit are plotted, in order to compare them. When the plastic strain contribution is significant, at total strains greater than 0.4%, then the relaxations have a similar shape and do not depend much from the total strain. In figure 17, the relaxed stress at half life is plotted against the imposed strain. A simple polynomial equation fits the data points:

$$\Delta\sigma_{relax} = 3.74 + 46.7 \cdot \Delta\varepsilon_t - 13.1 \cdot \Delta\varepsilon_t^2 \qquad (17)$$

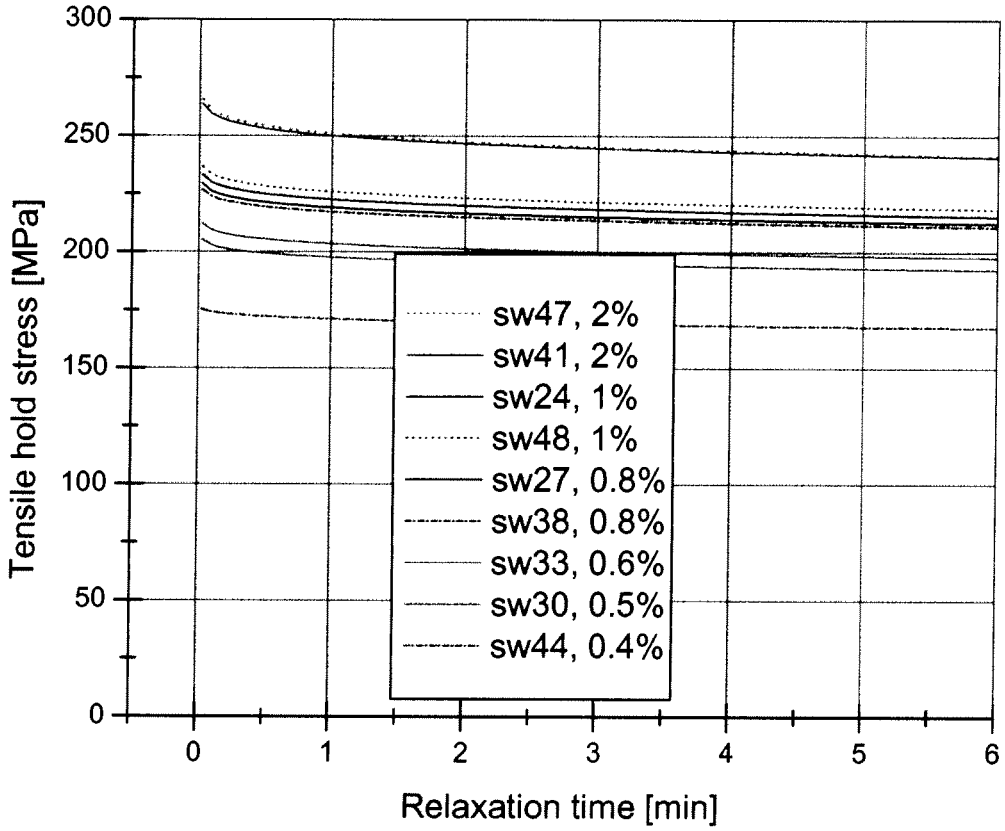

Figure 16: Relaxations at dwell for different imposed strain at 300°C. From Betherton [6]

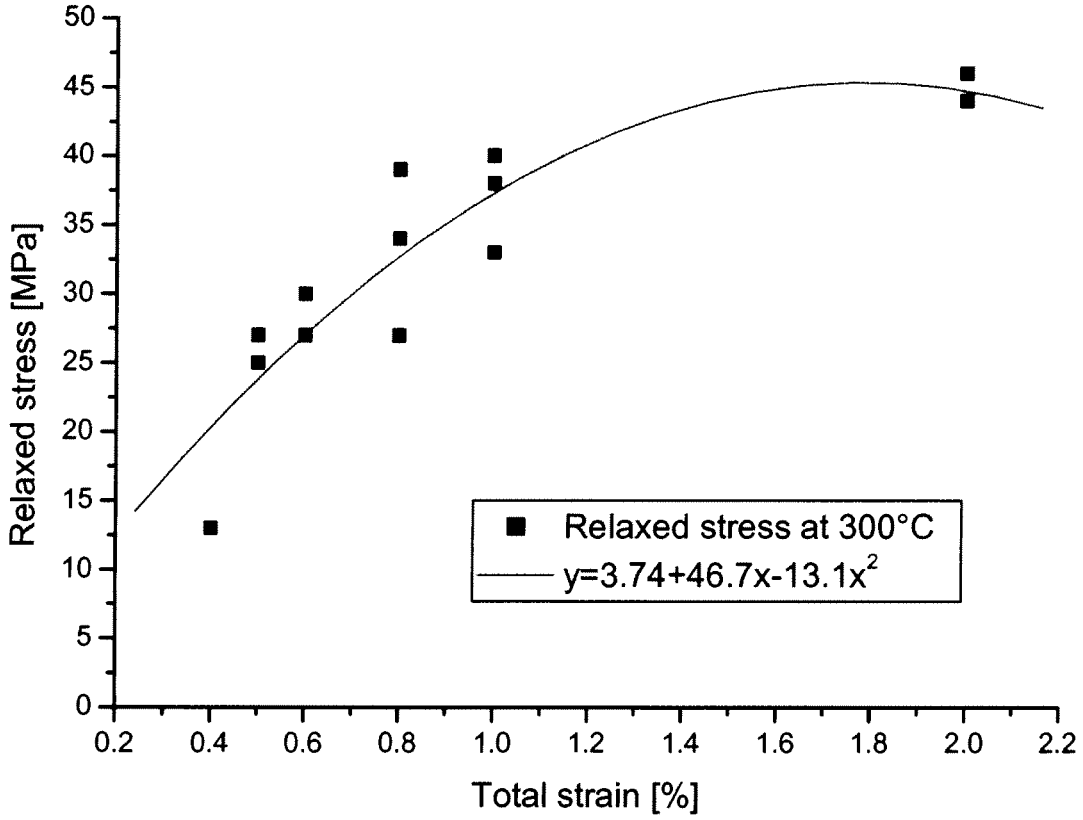

Figure 17: Maximum relaxed stress as a function of the total imposed strain in Cu-Cr-Zr at 300°C. Relaxation time=5min. From Betherton [6].

5. Creep-fatigue time based life predictions

Due to the lack of available creep-fatigue tests, we need to find a way to correlate pure fatigue with pure creep, as to obtain valuable predictions for the creep-fatigue condition. We will try to present a complete approach based on the time method as described in the RCC-MR rule code. Basically the method assumes that fatigue and creep damage are cumulative in a linear way, such that, for instance 30% creep and 70% fatigue damage will lead to failure. This assumption would imply that fatigue and creep mechanisms do not interact together. In fact, Bretherton et al [6] have shown that an interaction exists and that a prediction based on the linear damage accumulation rule is too optimistic.
Here we will try to use the results of Bretherton in order to find the conditions for rupture in the general case, when fatigue and creep are not linearly cumulative.

The general equation which describes the condition for failure reads [20]:

$$\sum_{j=1}^{P}\left(\frac{n}{N_D}\right)_j + \sum_{l=1}^{Q}\left(\frac{t}{T_D}\right)_l = D \qquad (18)$$

where:

n=number of cycles at loading condition j
N_d= number of design allowable cycles at condition j

t= time duration at condition 1
T_D= allowable time at condition 1
D=total creep-fatigue damage, a function of the creep and fatigue loading conditions as shown later in the damage diagram, the sum of creep and fatigue damage.

For the case of monotonic fatigue with holding times, the equation above can be simplified to:

$$n \cdot (d_f + d_c) = D \qquad (19)$$

where n is the number of allowable cycles to reach failure and d_f and d_c are the fatigue and creep damage per cycle.

$$d_f = \frac{1}{N_f} \qquad (20)$$

$$d_c = \sum_{k=1}^{m} \left(\frac{\Delta t_k}{T_{rk}(\sigma_k)} \right) \qquad (21)$$

N_f is the number of cycles to failure in monotonic fatigue
Δt_k is the creep time at stress σ_k during relaxation

According to equation 18, the fractional damage due to fatigue and the fractional damage due to creep are summed to give the total damage D at fracture. A value other than 1 for D would imply an interaction between fatigue and creep. For a better understanding of the concept, a simplified damage diagram is presented in figure 18. This diagram is called a bi-linear creep-fatigue envelope. It contains two symmetrical boundaries (1 and 2 in Fig.18), at which failure is supposed to occur.

For a particular test with a damage partition equal to $\alpha = d_c/d_f$, the intersection with the boundary line y=ax+b is at x=b/(α-a). The number of allowable cycles can be calculated as:

$$n = \frac{b}{d_f(\alpha - a)} \qquad (22)$$

and the corresponding value for D is from equation 19:

$$D = \frac{b(d_f + d_c)}{d_f(\alpha - a)} \qquad (23)$$

and knowing n from experiment, for the line (1) from figure 18, we can calculate the slope:

$$a = \alpha - \frac{b}{d_f \cdot n} \qquad (24)$$

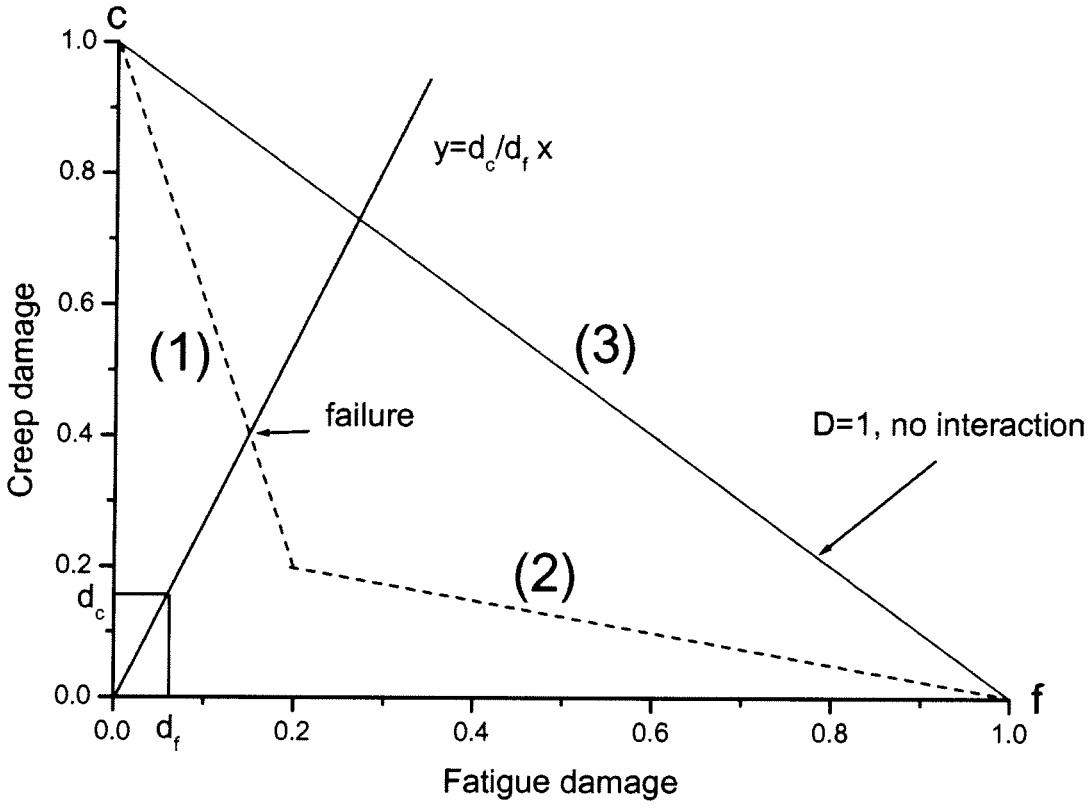

Figure 18: Creep-fatigue damage enveloppe. The boundary (3) applies for a linear damage accumulation without fatigue-creep interactions. Boundaries (1) and (2) are for the general case when fatigue is interacting with creep. d_c and d_f are the creep and fatigue damage per cycle.

5.1 Assessment of creep-fatigue life

The above equations should allow us to make an assessment of the creep-fatigue life, for the case of simple creep-fatigue tests. Starting with the master curve for fatigue plotted in figure 7 and represented by equation 5, we can predict the fatigue life. The Ramberg-Osgood relations equation 13 to 15 allow us to find numerically the associated stress amplitude at half life. The associated relaxation stress amplitude is found from equation 17. The Ramberg-Osgood parameters and equation 15 are valid for 300°C only and therefore only this temperature can be considered in our predictions.
The evaluation of equation 19 requires a knowledge of the relaxation curve. Since we do not have this information for all conditions, the following simplified procedure is proposed:

Figure 19 presents the relaxed stress as a function of time for the specimen SW41 from the work of Bretherton [6], for a test at 2% total strain. The stress decay is very rapid during the first seconds but then almost linear at times larger than 0.5 min. As shown in figure 16, the relaxations are quite similar in shape for all conditions. For simplicity, the evaluation of equation 21 is done for five different intervals of 1 min, as indicated in figure 19. The stress decay expressed in percent of total decay has been calculated and is given in table 1. Because of the observed similarity of the stress decay, table 1 is believed to give a general representation of the stress relaxations at 300°C.

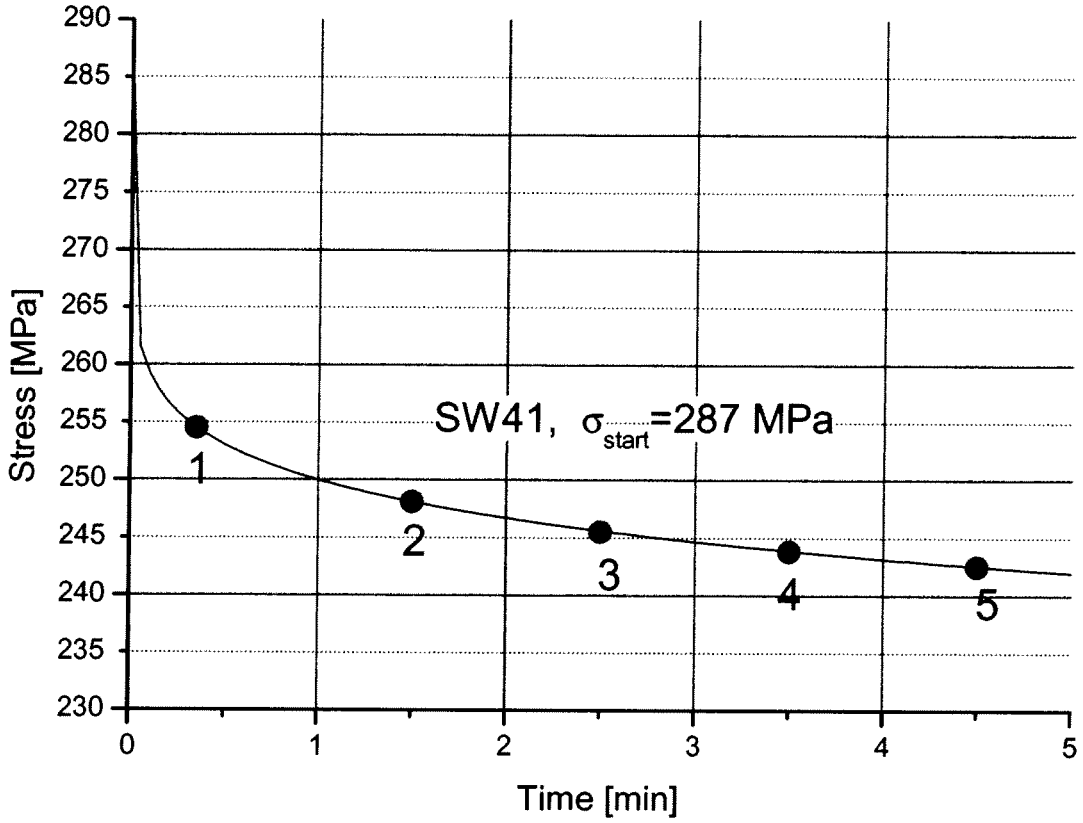

Figure 19: Stress relaxation in Cu-Cr-Zr at 300°C for a total applied strain of 2%. From Betherton [6].

Interval number	$\Delta\sigma_r/\Delta\sigma_{r,max}$
1	0.720
2	0.858
3	0.920
4	0.958
5	0.987

Table 1: Relax stress ratio for the first five intervals of 1 min in a typical relaxation in Cu-Cr-Zr at 300°C

5.2 Numerical results and discussion

Equation 19 has been evaluated using the only set of results available, for tension dwells of 300 sec at 300°C (equation 12), since only this time and temperature have been checked experimentally. The partial fatigue and creep damage have been calculated and the allowable total damage D was assessed according to the effective number of cycles to failure. It was found that a single line cannot represent the failure boundary as in figure18. The real

boundary is shown in figure 20. Since only 5min holding times have been performed, the boundary to the left of the diagram is unknown. In fact the values of the allowable total damage D are changing for all test conditions, indicating a very strong creep fatigue interaction. The interaction seems to be controlled by the total applied strain, as shown in figure 21. The interaction is extremely important at low applied strains and becomes absent at high imposed strains. On the same diagram, the total damage curve from the work of Li and Stubbins [7]has been also presented. The data represent the behaviour of two heats of Cu-Cr-Zr, supplied by Tréfimétaux and Otokumpu. The experiments were conducted at 25°C and tension and compression dwells of 10 sec were imposed to the specimen. Although a creep contribution to the deformation behaviour is claimed by the authors, a thermal creep damage cannot be attributed, since no creep experiments are available at such low temperature. It is questionable whether a failure in a classical creep test would occur at all. Therefore the creep damage in the sense of equation 21 is zero. In fact the reduction of life observed and indicated by D values lower than one is an effect of the interaction of the primary creep (relaxation) process with fatigue. The interaction seems to be very important in the case of Cu-Cr-Zr. More than that it appears clearly that the interaction depends strongly from the experimental parameters, the test temperature and the hold duration. The interaction is important at low strains and disappears at high strains. Unfortunately the shape of the boundary is unknown at high α values. In order to have the information , creep-fatigue tests should be conducted for longer hold times.
With the experimental information presented in this report, we are able to do reliable predictions only for the two conditions indicated in figure 21. To do a prediction we would simply determine the α ratio using the method presented in sect.5.1 and then search for the intersection of the line $y=\alpha x$ with the damage boundary of figure 20.

Recommendations for future work:

In order to do predictions on a broader base, a few experiments are needed to learn about the influence of temperature and tensile hold duration on the shape of the damage boundary.

A new experimental program should include the following points:

- Improve the Larson-Miller plot for the creep data
- Find out reliable fatigue cyclic curve equations for the temperatures of interest
- Make systematic experiments to measure the associated relaxation curves
- Conduct some creep fatigue experiments to find the corresponding damage boundary

The collected information should allow for a better knowledge of the damage envelopes and make predictions reliable.

Nevertheless, concerning the reality of fusion, we have to realize that predictions done on the base of the unirradiated situation will be wrong. In fact, creep and fatigue damage are cumulating, as the irradiation damage is accumulating and changing the material response continuously. Only *in situ* experiments will be applicable to understand the evolution of the damage and measure the effective interaction between creep and fatigue.

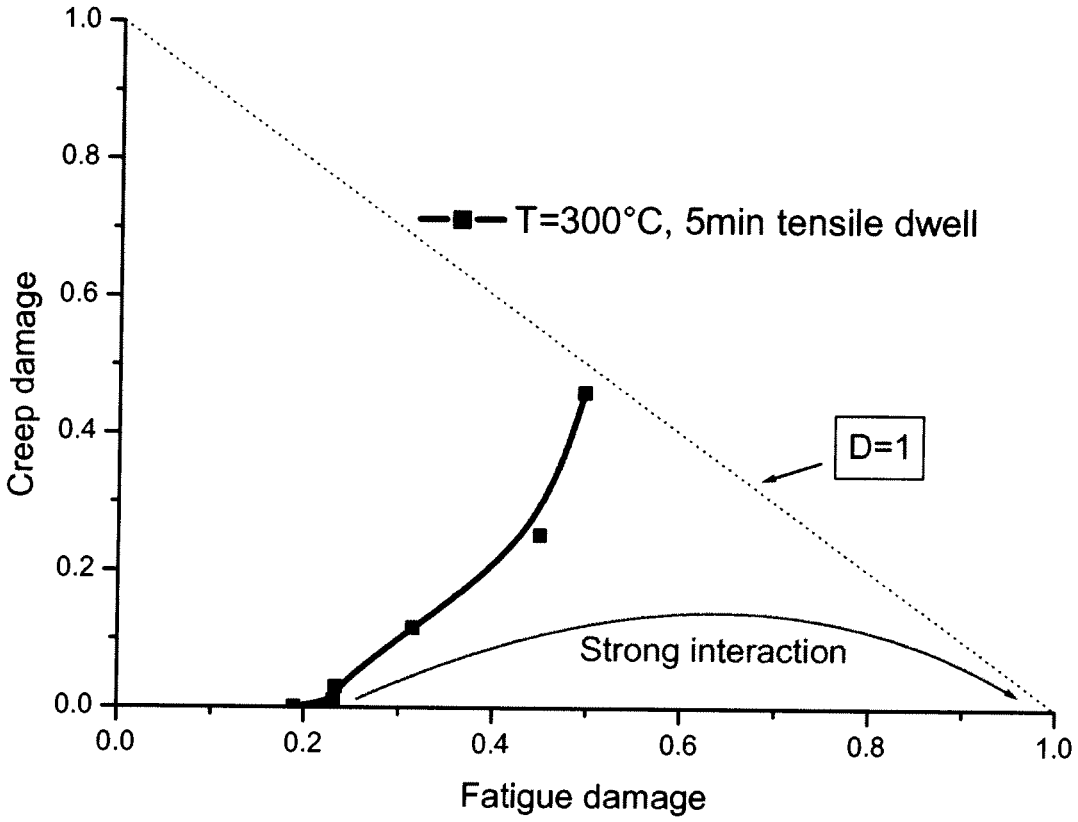

Figure 20: Damage envelope for Cu-Cr-Zr at 300°C under tension dwells of 5 min.

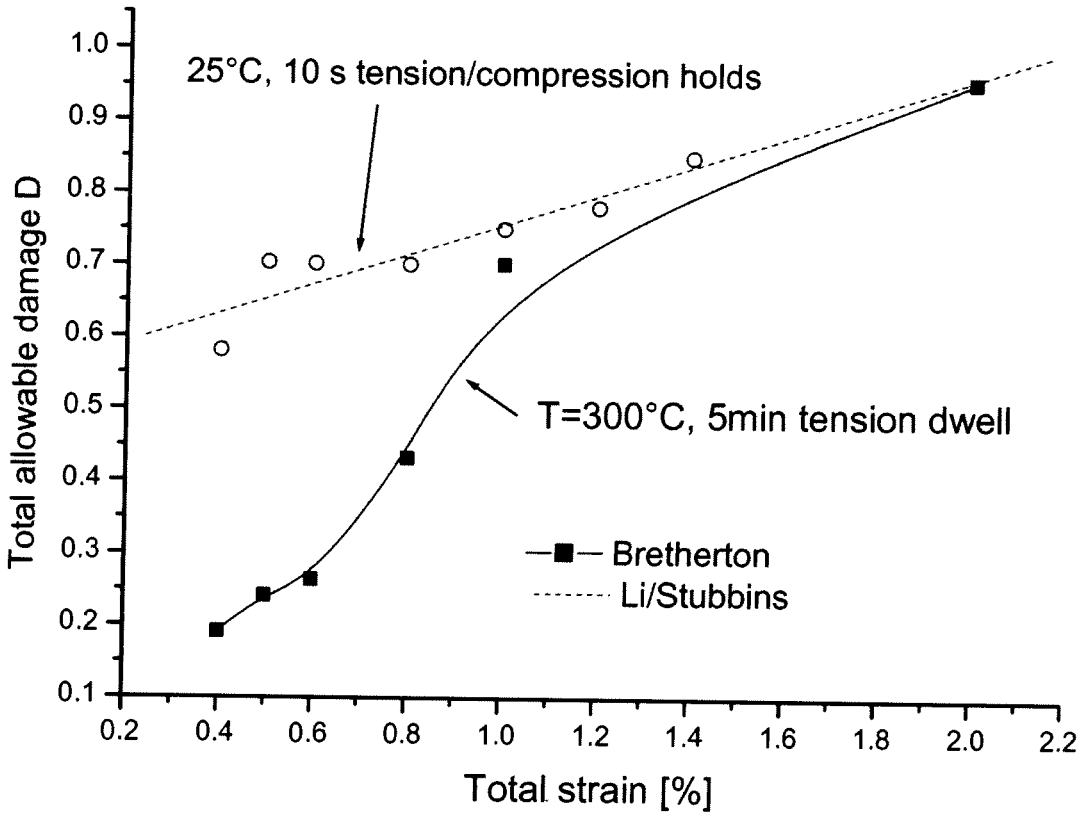

Figure 21: Total allowable damage as a function of the applied total strain.

6. Conclusions

The data available to EFDA on creep, fatigue and creep-fatigue have been reviewed and the applicability of a time based damage approach for the prediction of creep-fatigue life has been checked. The results of the study show that:

1. The fatigue data from experiments done at temperatures between RT and 350°C have been plotted in a single diagram. They show an acceptable dispersion and can be represented by a master equation.
2. The creep data were plotted into a Larson-Miller plot. The dispersion of the data is not acceptable. The Larson-Miller best fit equation does not represent well the creep properties. For predictions, only the concerned dataset can be used to assess creep damage.
3. The time based approach, as proposed in the French RCC-MR design code, which assumes that fatigue and creep damage are linearly additive, does not work for the case of Cu-Cr-Zr. In Cu-Cr-Zr, creep and fatigue are strongly interacting, specially at low strain levels. Predictions are only possible with the knowledge of an applicable fatigue-creep damage envelope.
4. *In situ* experiments are strongly recommended for reliable *in reactor* life predictions

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