STP-NU-013

IMPROVEMENT OF ASME NH

FOR GRADE 91 NEGLIGIBLE CREEP AND CREEP-FATIGUE

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FOREWORD

This document is the result of work resulting from Cooperative Agreement DE-FC07-05ID14712 between the US Department of Energy (DOE) and ASME Standards Technology, LLC (ASME ST-LLC) for the Generation IV (Gen IV) Reactor Materials Project. The objective of the project is to provide technical information necessary to update and expand appropriate ASME materials, construction, and design codes for application in future Gen IV nuclear reactor systems that operate at elevated temperatures. The scope of work is divided into specific areas that are tied to the Generation IV Reactors Integrated Materials Technology Program Plan.

ASME ST-LLC has introduced the results of the project into the ASME volunteer standards committees developing new code rules for Generation IV nuclear reactors. The project deliverables are expected to become vital references for the committees and serve as important technical bases for new rules. These new rules will be developed under ASME's voluntary consensus process, which requires balance of interest, openness, consensus, and due process. Through the course of the project ASME ST-LLC has involved key stakeholders from industry and government to help ensure that the technical direction of the research supports the anticipated codes and standards needs. This directed approach and early stakeholder involvement is expected to result in consensus building that will ultimately expedite the standards development process as well as commercialization of the technology.

ASME has been involved in nuclear codes and standards since 1956. The Society created Section III of the Boiler and Pressure Vessel Code, which addresses nuclear reactor technology, in 1963. ASME Standards promote safety, reliability, and component interchangeability in mechanical systems.

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ABSTRACT

This report provides recommendations for improvement of ASME NH for Grade 91 in the areas of negligible creep and creep-fatigue.

The report is separated into the following four parts.

Part I Improvement of ASME NH for Grade 91 (Negligible Creep)

Examines the current approaches available to define negligible creep and checks their applicability to Grade 91 steel. The work is based on material data available in France and the U.S.

Part II Improvement of ASME NH for Grade 91 (Creep-Fatigue)

Compares Subsection NH and RCC-MR creep-fatigue procedures. Comparisons are performed on cases defined on the basis of experimental test results available from Japan, France and the U.S. on Grade 91 steel. Particular attention was paid to the definition of safety factors and creep-fatigue damage envelope. Improvements to existing procedures are recommended.

Part III Proposed Test Program to Assess Negligible Creep Conditions of Modified 9Cr-1Mo

Part III is aimed at defining tests necessary to validate negligible creep conditions for Mod 9Cr-1 Mo material.

Part IV Proposed Test Program to Validate Creep-Fatigue Procedures for Modified 9Cr-1Mo

Part IV completes the work performed in Part II which, on the basis of creep-fatigue tests results available from Japan, Europe and the US, compared creep-fatigue procedures of ASME Subsection NH and RCC-MR Subsection RB.

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PART 1
IMPROVEMENT OF ASME NH FOR GRADE 91 (NEGLIGIBLE CREEP)

1 INTRODUCTION

In the frame of the AREVA HTR-VHTR design, it is recommended to operate the Reactor Pressure Vessel (RPV) in the negligible creep regime in order to avoid the implementation of a surveillance program covering the monitoring of the creep damage throughout the whole life of the reactor. Within the two options that are currently under consideration for the RPV material of ANTARES (AREVA New Technology based on Advanced gas cooled Reactor for Energy Supply), the high chromium-alloyed steel known as grade 91 in ASTM SA 336 standard has more creep properties documented and is also expected to allow more severe hot transients. The purpose of this report is to discuss the negligible creep conditions of this steel, also called Mod. 9Cr-1Mo. Mod. 9Cr-1Mo is a ferritic steel and not an austenitic stainless steel. Following ASME Boiler and Pressure Vessel (B&PV) Code, Section III for Class 1 nuclear components, additional rules of Subsection NH which take creep and creep/fatigue interaction effects into account should be used for applications above the limit of 371˚C (700˚F). Thus, the definition of the negligible creep conditions is of prime importance to enable the use of elevated core inlet temperatures during normal operating conditions (400˚C at least) and to accommodate transients of limited duration. In addition, the negligible creep criteria will need to take account of the 60 year design life of the reactor which corresponds to 4.2×10^5 hours of operation (based on 80% availability).

2 NEGLIGIBLE CREEP CRITERIA

Negligible creep criteria can be found in the ASME [1] and RCC-MR [2] codes and in recent Japanese development for Fast Reactor Structural Design Standard [3]. The different negligible creep criteria are summarized in Table 1.

Table 1 - Negligible Creep Criteria

	Code	Reference Stress	Negligible Creep Criterion
Time fraction	ASME (Subsection NH T-1324)	1.5 Sy	≤ 0.1
Creep strain	ASME (Subsection NH T-1324)	1.25 Sy	≤ 0.2 %
	RCC-MR*	σ0 ≈ 1.2 Sy	≤ □0 **
	Japanese design	1.5 Sm	≤ 0.03 %

* in the case of austenitic stainless steels only

** 0.01% to 0.03% depending on the materials

2.1 ASME

For class 1 nuclear components, applicable rules in the ASME Code are found in Section III, Subsection NB. These rules are applicable subject to the constraint that metal temperatures do not exceed the temperature limits of Section II, Part D, Table 2A. Those limits are summarized in Table 2.

Table 2 - Temperature Limits in ASME Code

Materials	T_{max} °F (°C)
Carbon steel and low alloy steel	700 (370)
Martensitic stainless steel	700 (370)
Austenitic stainless steel	800 (425)
Nickel-chromium-iron	800 (425)

Above those limits, it is recognized that creep effects are to be considered and Subsection NH provides applicable rules to cover the following failure modes:

- Ductile rupture from short term loading
- Creep rupture from long-term loading
- Creep-fatigue failure
- Ratcheting
- Buckling and creep buckling.

Appendix T of Subsection NH provides non-mandatory rules for strain, deformation and fatigue limits at elevated temperatures. A detailed creep-fatigue evaluation is not required if the limits of test No A-3 (T-1324) are satisfied:

$$\sum \frac{t_i}{t_{id}} \leq 0.1 \tag{1}$$

$$\sum \varepsilon_i \leq 0.2\% \tag{2}$$

where t_i and t_{id} are respectively the time duration at high temperature and the allowable time duration for a stress value of 1.5 times the yield stress S_y, and ε_i is the creep strain that would be expected for a stress level of 1.25 times the yield stress S_y during the time period t_i.

Equation (1) is aimed at providing prevention against creep-fatigue damage. The stress level of 1.5 S_y was evaluated on the basis of the average yield stress (1.25 S_y) multiplied by a strain hardening factor of 1.2. Such a factor is justified for austenitic stainless steels but should not be applicable for a material such as Mod. 9Cr-1Mo subject to cyclic softening.

Equation (2) is defined to avoid risks of ratcheting at elevated temperature.

When the limits of test No. A-3 are satisfied, the elastic fatigue rules of NB-3222.4 can be used but the cumulative fatigue usage fraction should not exceed 0.9 (taking into account the other provisions in T-1435).

Code Case N 201-5, applicable to Class CS components in elevated temperature service, uses similar criteria in Part A, Appendix XIX where these criteria define the threshold when Subsection NG rules can be used with an extension of time independent properties beyond values provided by Section II, Part D. In this case, the negligible creep criteria apply to both stress controlled and strain controlled loading.

Code Case N-201-5 approach is also based on time-temperature negligible creep curves with an approach similar to that used by the RCC-MR Code.

2.2 RCC-MR

The RCC-MR code rules are also based on maximum temperature limits below which creep effects can be neglected. Values are close to those of Table 2. In addition, Subsection RB 3216 defines the following negligible creep criterion:

$$\sum \frac{t_i}{T_i} \leq 1 \tag{3}$$

where t_i and T_i are, respectively, the time duration at high temperature and the maximum time during which the material may remain at temperature θ_i without creep becoming significant. The latter is obtained from the time-temperature negligible creep curves in RCC-MR Appendix A3.4.

For austenitic stainless steel 316L(N), the negligible creep curve was originally based on the following criteria:

- Reference stress σ_o calculated as the stress corresponding to a strain (elastic + plastic) of $(3S_m)/E$ (E Young's modulus) on the average monotonic stress-strain curve
- Time corresponding to a relaxation of the reference stress by 10 %.

In practice, the values of σ_o for 316L(N) are not very different from 1.2 times the 0.2% yield stress (or $1.33S_m$) in the temperature range of interest (450–650˚C).

An attempt was made to use the same methodology for 304 stainless steel. It was shown, however, that a relaxation criterion was not appropriate for such a material due to its creep strain law (leading to rapid shift from no relaxation to significant relaxation). The negligible creep curve for this material was subsequently defined on the basis of the creep strain calculated for 316L(N) at the boundary of negligible creep. The corresponding creep strain varied as a function of temperature but was in the order of 0.01%.

The negligible curve of 316L(N) was revised in the 1990s based on reference [9]. The new curve was derived following two different approaches developed in Germany and France (respectively):

- Method (a): Reference stress of 1.5 S_m and negligible creep curve based on the time required to relax this stress by 20%
- Method (b): Reference stress of 1.25 times the 0.2% yield stress and reference creep strain of 0.03%.

It was shown that those criteria would lead to almost identical curves for this material. The 0.03% creep strain criterion was defined on the basis of creep fatigue tests at 600˚C. This creep strain was assessed on the basis of the hold time required to reduce the number of cycles to failure by 10% compared to continuous fatigue. Tensile and creep strain properties of 316L(N) material were revised in the 2002 edition of the RCC-MR code but it has not been judged necessary to revise the negligible creep curve accordingly.

It should be mentioned that method (b), above, seems to be the basis for most of negligible creep curves in R5 rules developed in the UK [10].

As far as method (a) is concerned, limiting the relaxation of $1.5S_m$ by 20% ensures that the $3S_m$ criterion is not affected by more than 10%. The relaxation of $1.5S_m$ is also used in the ASME Code in the definition of $=1.5S_m+S_rH$ (see section NH T-1324).

2.3 Japanese Development of Structural Design Standard

Negligible creep curves are based on time and temperature which generate damageable creep strain in each material. Diagrams that represent the relationship between temperature and accumulated operation time are provided. The criterion is the same as in the case of RCC-MR [i.e. equation (3)].

If the accumulated operation times at temperature are short, non-creep design rules are adopted. At temperature θ_i, the duration t_i is related to a critical creep strain which is defined as 3×10^{-4} (0.03%). This critical creep strain is based on the creep strain occurring in the 2.25Cr-1Mo steel, which is a less creep-resistant material considered in Japan, under the following conditions:

- Load equal to S_0 (allowable stress limit for 2.25Cr-1Mo steel at 375˚C)
- 375˚C
- 5×10^5 hours

Constant stress level of 1.5 S_m is used to obtain the negligible creep curve corresponding to the critical creep strain of 3×10^{-4}. The technical basis for the choice of 1.5 S_m as the reference stress, in lieu of S_0 which was used to evaluate the critical creep strain in the case of 2.25Cr-1Mo steel, is not provided in [3].

3 APPLICATION TO MOD. 9CR-1MO STEEL

No negligible creep curve is proposed in the present edition (2002) of the RCC-MR Code for Mod. 9Cr-1Mo, and creep is said to be negligible for temperatures less than 375˚C, whatever the hold time. This approach is consistent with ASME limits on ferritic steels.

3.1 Reference Stress for Negligible Creep Curve Using a Criterion Based on Stress to Rupture

The S_y values for Mod. 9Cr-1Mo are significantly higher than those for austenitic stainless steels. According to Table 4, on the basis of ASME material data for Mod. 9Cr-1Mo as well as on the basis of RCC-MR material data, the time fraction criterion is not applicable to this material with $1.5S_y$ as the reference stress. This stress exceeds the minimum creep stress to rupture for the lowest time tabulated in the case of Mod. 9Cr-1Mo (in both ASME and RCC-MR Codes). Table 3 gives the estimated minimum values of yield and tensile strengths at different temperatures, the ratio of tensile strength to yield strength is equal or smaller than 1.5. As a consequence it is not possible to perform creep test at stress level as high as 1.5 S_y without approaching or exceeding the tensile strength for Mod. 9Cr-1Mo. The choice of the reference stress must be lower than $1.5S_y$. By taking into account that 1.5 S_y defined for austenitic stainless steels incorporated a strain hardening factor, it could be justified to use by analogy a cyclic softening factor which would be in the order of 1.2 for Mod. 9Cr-1Mo. As a result, a reference stress of about S_y could be justified. Figure 1, based on data from reference 11, confirms that S_y is a good estimate of the 0.2 yield stress of the cyclically softened material. A value lower than S_y could be also envisioned, subject to taking account of additional softening resulting from fatigue-relaxation. More testing would be required, however, at temperatures lower than 500˚C to justify such an effect.

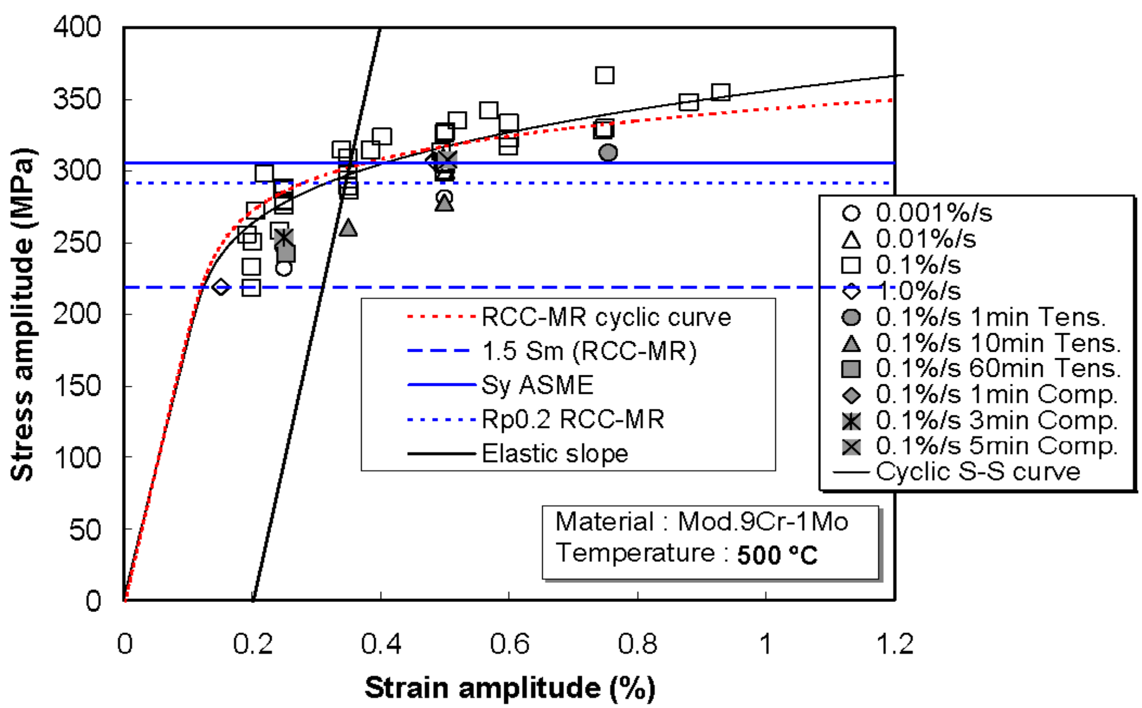


Figure 1 - Cyclic Stress Strain Behavior of Mod. 9Cr-1Mo at 500˚C

Table 5 gives the negligible creep times at different temperatures based on the time fraction criterion with S_y as a reference stress. A design life of 4.2×10^5 hours could be justified at 400˚C but only if the ASME creep stress to rupture curves are considered. The reliability of the ASME creep stress to rupture curves is discussed in section 4.2.3. Table 5 provides also, for sensitivity purposes, the negligible creep times for a reference stress of $1.5S_m$. It can be noted that, by considering such a more favorable reference stress, it could be possible to justify a temperature greater than 450˚C (again with ASME material properties).

It is finally to be pointed out that the above calculated negligible creep times assume that the stress is kept constant. Mod. 9Cr-1Mo is a material subject to significant relaxation even at low temperatures (below 500˚C) and a less conservative estimate could be given by considering this effect. Table 5 indicates that with a very conservative 1-hour hold time assumption, the negligible creep time at 400˚C would be increased by more than a factor of five. It seems therefore that stress relaxation could play a role in the definition of negligible creep conditions to provide additional margins.

Table 3 - Tensile Properties of Modified 9Cr-1Mo

Temperature ˚C		20	350	450	550
ASME	S_y or $R_{p0.2}$ (MPa)	413.7	371	336.5	266.3
	S_u or R_m (MPa)	586	561	494.2	376
	S_u/S_y	1.42	1.51	1.47	1.41
RCC-MR	S_y or $R_{p0.2}$ (MPa)	420	349	320	254
	S_u or R_m (MPa)	580	493	439	340
	S_u/S_y	1.38	1.41	1.37	1.34

Table 4 - Negligible Creep at 450˚C for Modified 9Cr-1Mo Steel Based on ASME Time Fraction Criterion

Reference Stress	Code Data	Reference Stress (MPa)	Expected Time to Rupture (hr)		Negligible Creep Time (hr)
			avg	min	
S_m	ASME	161	-	7.24×10^9	7.24×10^8
	RCC-MR	163	-	1.35×10^8	1.35×10^7
$1.5*S_m$	ASME	241.5		6.67×10^6	6.67×10^5
	RCC-MR	244.5	6.02×10^6	4.12×10^5	4.12×10^4
S_y	ASME	336	-	20,913	2091
	RCC-MR	320	81,311	6869	687
$1.25*S_y$	ASME	420		271	27.1
	RCC-MR	400	1640	142	14.2
$1.5*S_y$	ASME	504	< 1	< 1	<0.1
	RCC-MR	480	< 1	< 1	<0.1

Table 5 - Negligible Creep for Mod. 9Cr-1Mo Steel Based on ASME Time Fraction Criterion

		450˚C		425˚C		400˚C	
Reference Stress	Code Data	Reference Stress (MPa)	Negligible Creep Time (hr)	Reference Stress (MPa)	Negligible Creep Time (hr)	Reference Stress (MPa)	Negligible Creep Time (hr)
$1.5 S_m$	ASME	241.5	6.67×10^5	251.6	10^7	261.3	3.5×10^8
	RCC-MR	244.5	4.12×10^4	252.75	3.52×10^5	261	3.58×10^6
S_y	ASME	336	2,091	348	2.73×10^4 (*)	358	5.35×10^5 (*)
	RCC-MR	320	687	330	4.99×10^3	338	4.62×10^4

(*) Taking into account stress relaxation with RCC-MR creep strain law (and elastic follow-up factor of 1 which seems to be appropriate for Mod. 9Cr-1Mo) and assuming a series of cycles with 1-hold time, the negligible times would become:

- 2.95×10^6 hours at 400˚C
- $2.21\text{x}10^5$ hours at 425˚C

3.2 Negligible Creep Curve Based on RCC-MR Creep Strain Criterion

The application of the RCC-MR definition of negligible creep curve and associated reference stress to the case of Mod. 9Cr-1Mo was discussed in [4]. This document indicates that it would seem more physical to use the allowable stress S_m as a reference stress, which can be used with a reference creep strain of 0.01% as the negligible creep criterion. The resulting negligible creep curves would depend on different formulations of the creep strain laws.

3.2.1 RCC-MR Creep Strain Law

The creep strain law in the RCC-MR formulation is $\varepsilon_{creep} = C_1 t^{C_2} \sigma^{n_1}$. This formulation corresponds to a primary creep strain rate which decreases as the creep strain increases down to the minimum creep strain rate value.

3.2.2 ORNL Creep Strain Law

The creep stain is obtained in reference [5] by adding a primary creep strain, which depends on the power 1/3 of time, to the secondary creep strain corresponding to the minimum creep rate:

$$\varepsilon_{creep} = at^{1/3} + \dot{\varepsilon}_{\min} t \tag{4}$$

where a and $\dot{\varepsilon}_{\min}$ are dependent on stress and temperature.

The same equation for the creep strain was used to develop the isochronous stress-strain curves [8], and these curves are now included in ASME Section III, Subsection NH. It is noted that different fitted values were used for the minimum creep rate $\dot{\varepsilon}_{\min}$ and for the primary creep strain coefficient a. For a in particular, there were different correlations for temperatures below 538˚C (427–538˚C) and for temperatures from 538˚C to 649˚C. This shift in the primary creep strain law at 538˚C produces a discontinuity in the slope of the negligible creep curve shown in [4]. The creep strain law that was used to generate the ASME isochronous stress-strain curves does not seem to predict the creep strain adequately in the moderate temperature domain where negligible creep conditions are of practical interest.

3.2.3 Japanese Creep Strain Law of Reference [6]

In the Japanese work of [6], the creep strain is also obtained by adding the primary creep strain to the secondary creep strain that corresponds to the minimum creep strain rate. But the formulations of the primary creep strain and of the minimum creep strain rate are based on correlations with the rupture time. In the case of the minimum creep strain rate, this correlation is the well known Monkman-Grant relationship. In order to introduce the stress in the evaluation of the time to rupture and then in the determination of the creep strain, it is necessary to use the average curve from the stress versus time to rupture plot. The average stress to rupture is given in reference 6 by the following equation:

$$\left(\theta+273.15\right)\left[\log_{10}\left(t_r\right)+C\right]=B_0+B_1\log_{10}\sigma+B_2\left(\log_{10}\sigma\right)^2 \tag{5}$$

where t_r is in hours, θ in ˚C, C = 29.1146, B_0 =31808.82, B_1 = 3055.52 and B_2 = –5148.248. It appears that the stress should be in kg/mm^2.

3.2.4 Applicability of Negligible Creep Curve Based on RCC-MR Creep Strain Criterion

The negligible creep times determined from different creep strain laws are given in Table 6. The 0.01% creep strain criterion, even with a moderate reference stress equal to S_m, yields severe limits on negligible creep time. A service duration of 4.2×10^5 hours at 400˚C will be allowed only if the ORNL material data are assessed as being the most representative, or adequately conservative, properties for Modified 9Cr-1Mo in the corresponding temperature range. Table 6 provides also negligible creep times when 1.25 times the 0.2% yield stress is used together with a creep strain of 0.03% [as for 316L(N) material]. It is confirmed that the negligible creep times are very low even at 400˚C and the use of the 0.2% yield stress as a reference stress (minimum instead of average yield stress) is not expected to provide more comfortable margins. This creep strain criterion of 0.03% together with a reference stress in the order of the 0.2% yield stress seems to be therefore fully inappropriate for Mod. 9Cr-1 Mo.

Table 6 - Negligible Creep for Modified 9Cr-1Mo Steel Based on RCC-MR Creep Strain Criterion

		S_m and 0.01%		$1.25S_y$ and 0.03%	
Temperature	Creep Strain Law	Reference Stress	Negligible Creep Time (hr)	Reference Stress	Negligible Creep Time (hr)
400°C	RCC-MR	174 MPa (*)	2.82×10^4	422 MPa (*)	<0.1
	ORNL (ref.5)		2.73×10^6		143 (**)
	Japanese (ref.6)		2.17×10^4		3.15
425°C	RCC-MR	168.5 MPa (*)	2.23×10^3	412 MPa (*)	<0.1
	ORNL (ref.5)		9.33×10^4		5.7
	Japanese (ref.6)		2.60×10^3		0.55
450°C	RCC-MR	163 MPa (*)	199	399 MPa (*)	<0.1
	ORNL (ref.5)		4148		0.33
	Japanese (ref.6)		389		0.12

(*) Based on the RCC-MR code

(**) The negligible time would become 9325 h with the 0.2% yield stress instead of 1.25 times the 0.2% yield stress

3.3 Negligible Creep Curve Based on RCC-MR Stress Relaxation Criteria

Table 7 provides, for the RCC-MR creep strain law, the negligible creep times for the stress relaxation criteria defined in section 2.2. It can be noted that the reference stress based on $3S_m/E$ is in the order of 1.12 times the 0.2% yield stress and the time to relax this stress by 10% is very small. The second criterion yields more reasonable relaxation times. Table 6 indicates that the RCC-MR creep strain law is the most conservative in terms of creep deformation and is therefore the one which gives more relaxation. A reevaluation of the RCC-MR creep strain law could provide more margins in terms of relaxation time.

Table 7 - Negligible Creep for Modified 9Cr-1Mo Steel Based on RCC-MR Stress Relaxation Criteria

	Relaxation of $\sigma_0=3S_m/E$ by 10 %		**Relaxation of 1.5 S_m by 20%**	
Temperature	Reference stress	Negligible creep time (hr)	Reference stress	Negligible creep time (hr)
400°C	380 MPa	0.32	261 MPa	13,501
425°C	370 MPa	<0.1	253 MPa	1244

3.4 Negligible Creep Curve Based on ASME 0.2 % Creep Strain Criterion

Table 8 gives the negligible creep times on the basis of the 1.25 S_y reference stress. The 3 different creep strain laws mentioned in Section 3.2 were used to derive the results in this table. This table indicates that even the least conservative creep strain law is not sufficient to justify a design lifetime of 4.2×10^5 hours of service at 400˚C. Such a design life could be justified with the ORNL creep strain law and with S_y as a reference stress instead of 1.25 S_y. As the present criterion is aimed at preventing ratcheting, the stress to be considered should be the yield stress of the elastic core and it could be justified to select the yield stress of the cyclically softened material (S_y according to Section 3.1) instead of the average (monotonic) yield stress. However, it would be necessary to check which among the different creep strain laws is the most reliable in the range of temperatures of interest.

Table 8 provides also, for sensitivity purposes, the negligible creep times for a reference stress of $1.5S_m$. Such a stress would yield a negligible creep time limit compatible with 4.2×10^5 hours of service at 400˚C for all 3 different creep strain laws, and at 425˚C for the ORNL and Japanese creep strain laws. At 450˚C, the ORNL and Japanese creep strain laws predict negligible creep time limits that exceed 10^4 hours, which seems to be compatible with the order of magnitude expected for cumulated duration of the hot transients in the case of VHTR projects. In the case of the RCC-MR creep strain law, a reassessment of the data and creep law model is necessary in order to conclude about the compatibility of negligible creep limits with service in the corresponding temperature range.

With the reference stress of 1.5 S_m and the ORNL creep strain law, the 0.2% creep strain at 450˚C corresponds to a very small secondary creep strain (6×10^{-6}) combined with a dominant primary creep strain (0.1994×10^{-2}).

Table 8 - Negligible Creep for Modified 9Cr-1Mo Steel Based on ASME Creep Strain Criterion

		1.5 S_m		1.25 S_y	
Temperature	Creep Strain Law	Reference Stress	Negligible Creep Time (hr)	Reference Stress	Negligible Creep Time (hr)
400˚C	RCC-MR	261 MPa	3.19×10^5	447.6	6.26
	ORNL (ref.5)		1.51×10^8		11,975 (*)
	Japanese (ref.6)		7.37×10^8		379
425˚C	RCC-MR	252.75 MPa	36,073	435.2	1.70
	ORNL (ref.5)		5.76×10^6		508
	Japanese (ref.6)		7.83×10^5		38
450˚C	RCC-MR	244.5 MPa	4522	420.6	0.53
	ORNL (ref.5)		2.84×10^5		30
	Japanese (ref.6)		44,376		5

(*) The negligible time would become 985,583 hours with S_y instead of 1.25 S_y stress

3.5 Negligible Creep Curve Based on Japanese Creep Strain Criterion

If 1.5 S_m is chosen as the reference stress, the 0.03% total creep strain criterion gives the negligible creep times shown in Table 9, using the 3 different creep strain laws. Table 9 indicates that the negligible creep time limits obtained with the above mentioned criterion is compatible with 4.2×10^5 hours of service at 400˚C only for the ORNL creep strain law. Concerning the order of magnitude of the duration of transients up to 425˚C, it is compatible with the present negligible creep only if the ORNL creep strain law is assessed as being the most representative, and adequately conservative, for Mod. 9Cr-1Mo in the corresponding temperature range.

The negligible creep time limits of Table 9 appear to be much more restrictive than the curve from [3], which is duplicated in Figure 2. It has been clarified that the creep strain law used in the [3] paper has been developed in a design study sponsored by Japanese utilities and is not exactly the same as that described in [6]. This new creep strain law has not been published and does not seem to be available. This result indicates that a slight difference in creep strain equations can lead to very large differences in negligible creep curves.

The limits of Table 9 are based on a total creep strain of 0.03% (3×10^{-4}). At this total creep strain level, the Japanese creep strain law of reference 6 predicts a very small secondary creep strain (8×10^{8}) and a dominant primary creep strain (3.0×10^{-4}) at 450˚C.

Table 9 - Negligible Creep for Modified. 9Cr-1Mo Steel Based on Japanese Creep Strain Criterion

Temperature	Creep Strain Law	Reference Stress (*)	Creep Strain	Negligible Creep Time (hr)
400˚C	RCC-MR	261 MPa	0.03 %	395
	ORNL [5]	261 MPa	0.03 %	5.12×10^{5}
	Japanese [6]	261 MPa	0.03 %	1.23×10^{5}
425˚C	RCC-MR	252.75 MPa	0.03 %	55.8
	ORNL [5]	252.75 MPa	0.03 %	19,566
	Japanese [6]	252.75 MPa	0.03 %	2016
450˚C	RCC-MR	244.5 MPa	0.03 %	8.6
	ORNL [5]	244.5 MPa	0.03 %	968
	Japanese [6]	244.5 MPa	0.03 %	45.9

(*) Reference stress of 1.5 S_m

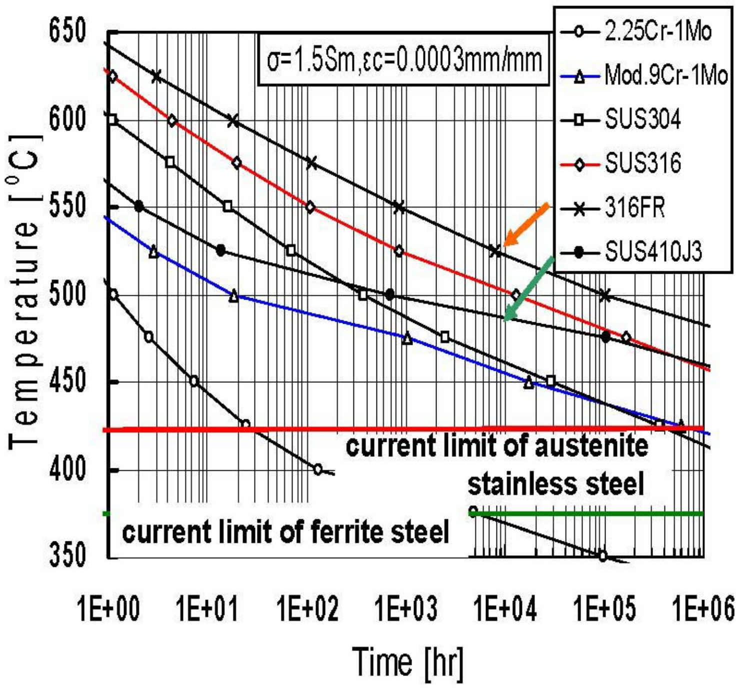


Figure 2 - Negligible Creep Curve from Ref. [3]

4 TENTATIVE IMPROVEMENT IN THE CORRELATION FOR MOD. 9CR-1MO AT MODERATE TEMPERATURES

4.1 Minimum Creep Rate

4.1.1 Correlation of Minimum Creep Rate and Time to Rupture

The so-called Monkman-Grant equation predicts that the product of the minimum creep rate and the time to rupture is constant in a wide range of creep conditions. In AREVA's database built on European, US and Japanese data (559 time-versus-stress-to-rupture data points), 417 minimum creep rate values are available. They are plotted in Figure 3 against the rupture time. The Japanese fit of the Monkman-Grant relationship ($\dot{\varepsilon}_{min}$ x t_r^r) as described in reference 6 results in a factor r = 1.0778 instead of 1, and a temperature dependent constant. As shown in Figure 3, which includes all the data from 450˚C to 650˚C, this temperature dependence is small between 450˚C and 650˚C. In addition, Figure 3 shows that the data at 450˚C are in the lower part of the scatter band and lower than predicted by the Japanese fit. This means that using this fit to connect the minimum creep rate to the time to rupture, and indirectly to the stress, overestimates the minimum creep rate at 450˚C.

4.1.2 Minimum Creep Rate Against Stress

For the relationship between minimum creep rate and stress, RCC-MR uses a Norton equation:

$$\dot{\varepsilon}_{\min} = C\sigma^n \qquad (6)$$

where parameters C and n are fitted against temperature.

In [5], ORNL proposes a more complicated relationship where the logarithm of minimum creep rate depends on both the stress and the logarithm of stress.

From the Japanese equations of [6], an indirect relationship between stress and minimum creep rate can be established by using the stress and time to rupture correlation and the Monkman-Grant equation to eliminate the time to rupture variable.

The different estimations of the minimum creep rate are compared to experimental data in Figure 4 to Figure 6 at 550˚C, 500˚C and 450˚C. At 550˚C, the ORNL equation underestimates the minimum creep rate. The Japanese fit is in better agreement with the experimental data. The RCC-MR Norton equation gives higher strain rate at low stresses but the slope of its stress dependence is not correct. This could be due to the use of a much smaller database than is available today. It is also noted that the estimated minimum creep rate is not used in the RCC-MR assessment of the total creep strain. In RCC-MR minimum creep rate is only used as an asymptotic value of the strain rate.

The same conclusion can be drawn from Figure 5. In addition, the experimental data suggest a shift in the slope of the stress dependence of minimum creep rate at low stress. At 500˚C this can be an artifact, due to the fact that low stress tests have been interrupted before rupture and before stationary secondary creep stage, leading to some overestimation of the so-called minimum creep rate.

Two observations support these conclusions:

- When minimum creep rate data are sorted out between interrupted tests and tests to rupture (Figure 5), the low stress-low creep rate domain includes interrupted tests only.
- The apparent transition observed at 500˚C between high and low stress domain is not found at 550˚C although the corresponding stresses are covered by the tests.

At 450˚C (Figure 6), the scarce experimental data are scattered in two groups. The Japanese fit corresponds to the high creep rate group whereas the ORNL equation fits the low creep rate group.

The gap between the two groups exceeds 50 MPa in stress or a factor 10 in creep rate. The RCC-MR minimum creep rates, estimated by extrapolation of the correlation obtained from test data with a large majority at higher temperatures, is more similar to the Japanese fit, but do not agree with the trend of the test results. The conclusions are:

- If minimum creep rate-stress relationship is needed at temperatures below 500˚C, a direct analysis of data at the considered temperature would be more appropriate than fitting the data over the entire temperature range.
- Since the estimation of minimum creep rate is questionable at 500˚C and below, analysis methods for creep strain data that do not use the concept of minimum creep rate would be preferred.

Nevertheless, in case of criteria based on total creep strain, the secondary or minimum creep rate has minor importance as the primary creep strain is dominant in the negligible creep regime at 450˚C, as found from the use of the ORNL and the Japanese creep strain laws.

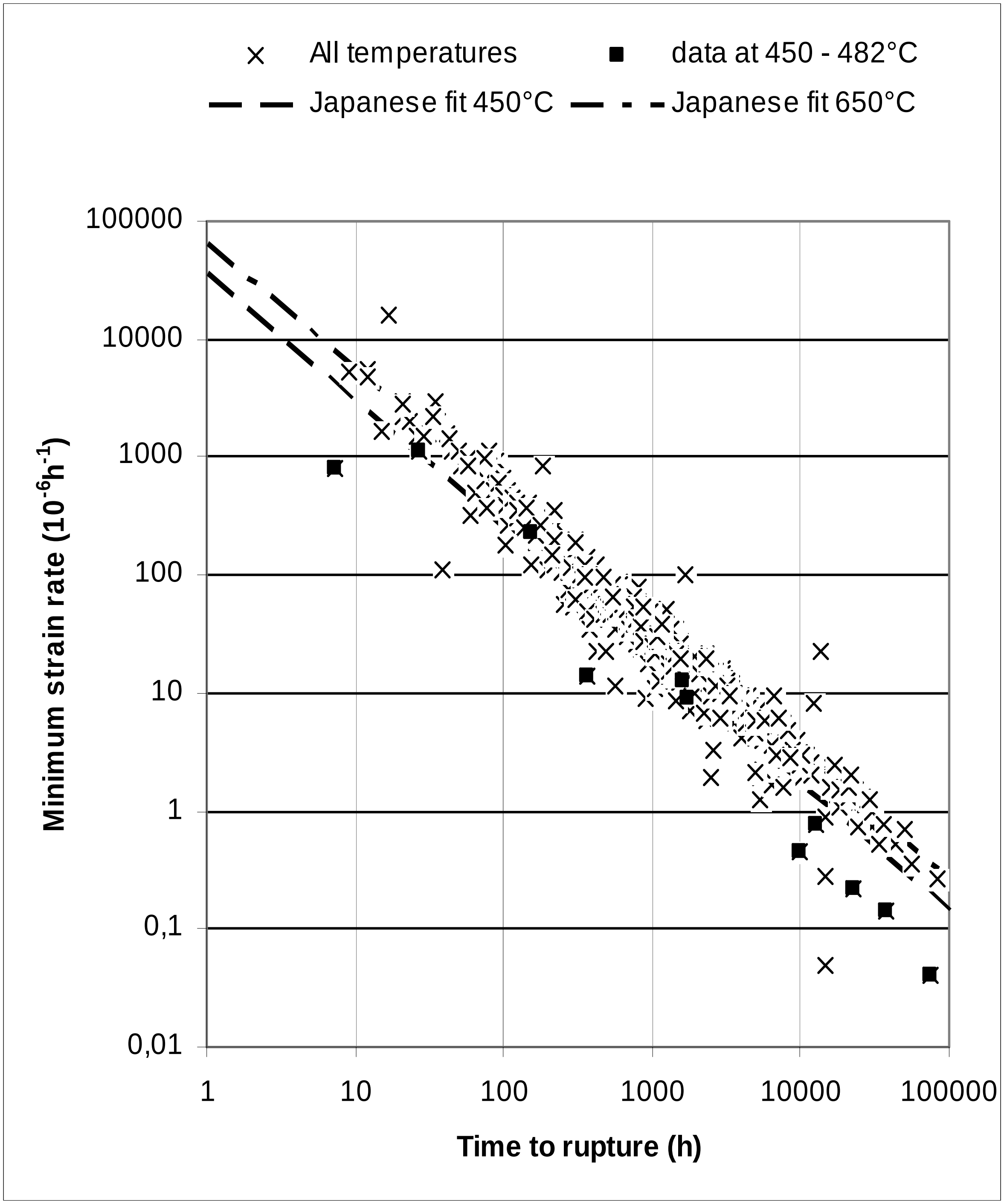


Figure 3 - Minimum Strain Rate and Time to Rupture Relationship

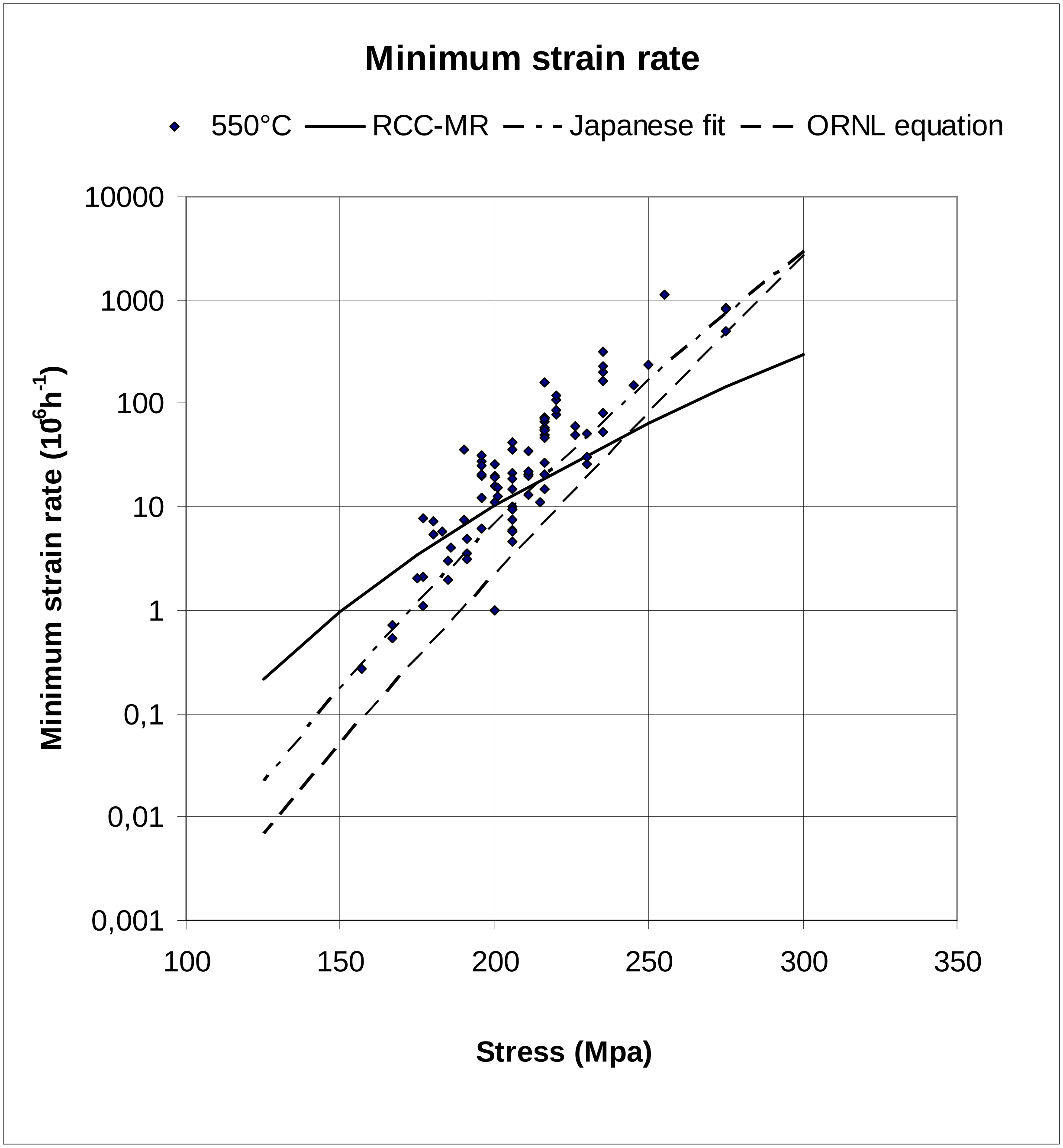


Figure 4 - Minimum Strain Rate versus Stress at 550˚C

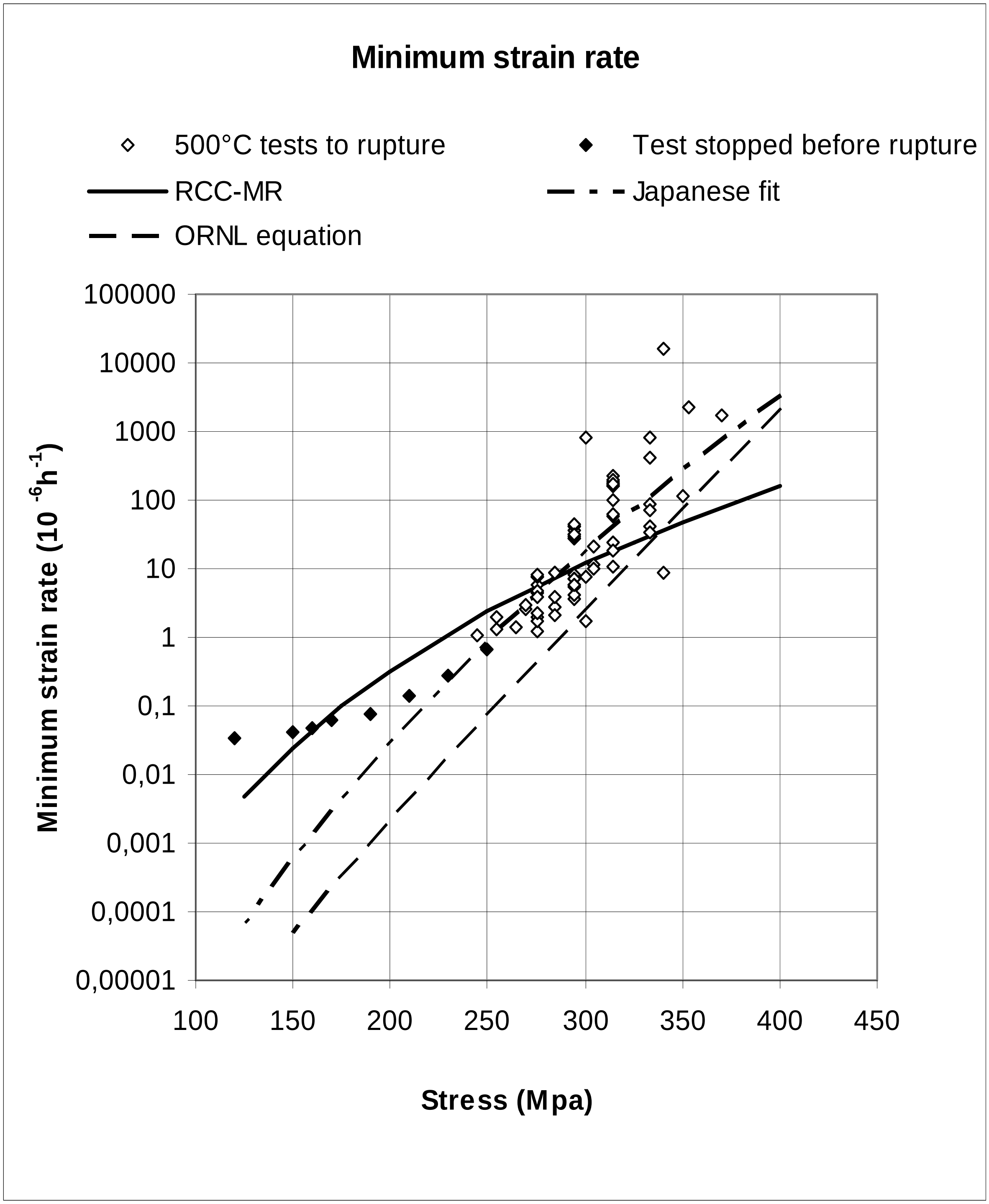


Figure 5 - Minimum Strain Rate versus Stress at 500˚C

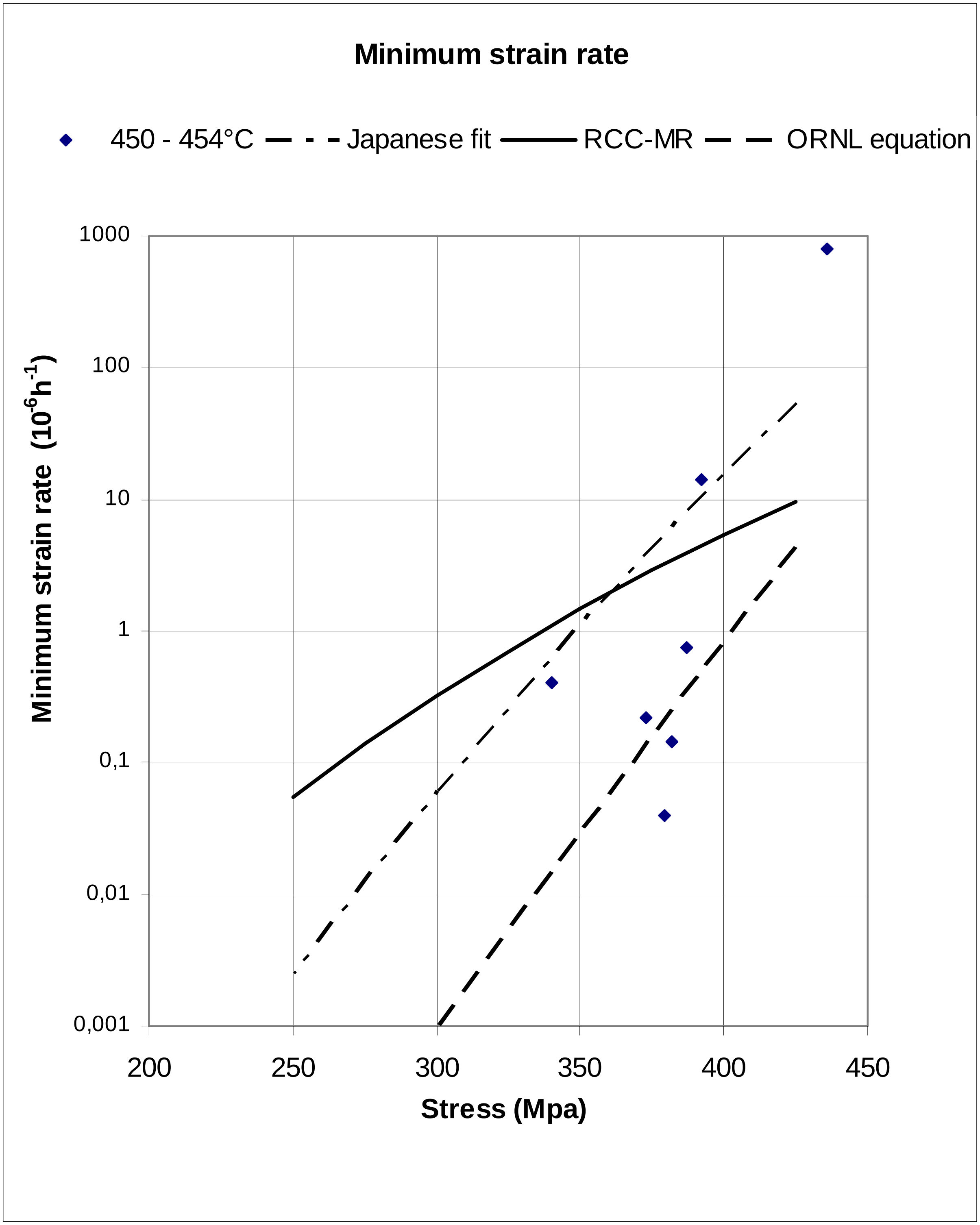


Figure 6 - Minimum Strain Rate versus Stress at 450˚C

4.2 Creep Strain Equations

4.2.1 Creep Strain Law in RCC-MR

In RCC-MR primary creep strain is given as a power function of time and stress:

$$\varepsilon_{creep} = C_1 t^{C_2} \sigma^{n_1} \tag{7}$$

The corresponding equation for creep strain rate, which can be used with the strain hardening assumption in calculations, is given by:

$$\dot{\varepsilon}_{creep} = K \varepsilon^{\alpha} \sigma^{\beta} \tag{8}$$

The equivalence of these equations is assessed by analyzing the relationships between K, α and β on one hand and C_1, C_2 and n_1 on the other. This equation describes the decrease of primary creep rate down to the value of minimum creep rate which describes secondary creep stage and is given by the Norton equation.

The times to achieve creep strains of 0.1%, 0.2%, 0.5%, 1% and 2%, as extracted from different creep curves, were used to derive the isothermal values of C_1, C_2 and n_1 at 600˚C, 550˚C and 500˚C. The corresponding values of K, α and β were calculated and the dependence of the parameters k = log (K), α and β on temperature was assumed to be linear against T^{-1} with T in Kelvin.

The times to reach creep strains of 0.1%, 0.2%, 0.5% and 1% at 550˚C and 500˚C, using the RCC-MR creep strain laws, are compared to experimental data in Figure 7 to Figure 14.

The results are scattered, particularly for 0.1% creep strain and with one result at 500˚C out of the time domain of the other data. This could be due to the dependence of the 0.1% data on the identified starting point of creep deformation (total strain minus strain at loading).

At 550˚C, the 0.1% creep data are restricted to short term (less than 12 hours).

Concerning the fitted values of the parameters of the creep strain law, the time exponents are not very different from 1/3. Roughly speaking, the isothermal fits do not show better agreement with experimental data than temperature smoothed fits.

4.2.2 Comparison with other Creep Strain Laws

Both the ORNL creep strain equation and the equation resulting from the Japanese work consider a primary creep strain combined with a secondary creep strain accumulated in proportion to the minimum creep rate studied in the previous section.

In the ORNL equation, the time exponent of the primary creep strain is 1/3 and the primary creep strain is exponentially dependent on T^{-1} with T in Kelvin.

In the Japanese equation the primary creep strain formulation corresponds to the so-called θ projection formulation:

$$C_1\left(1-\exp\left(-r_1 t\right)\right)+C_2\left(1-\exp\left(-r_2 t\right)\right) \tag{9}$$

The parameters C_1, r_1, C_2 and r_2 are fitted to the time to rupture t_r for the corresponding stress and temperature conditions.

Figure 7 to Figure 14 show that the creep strain predictions based on the ORNL and Japanese equations are quite similar: The difference is less than 15 MPa in stress at 550˚C and 30 MPa at 500˚C. Figure 11 shows that at 500˚C some low strain data (0.1%) fall unreasonably outside of the scatter band (this is probably due to a problem of reset of zero creep strain data point after initial loading).

At lower temperatures (482˚C, 475˚C and 450˚C as shown in Figure 15 to Figure 26), the small number of data and the uncertainty of data for small creep strain have two drawbacks:

- It is not possible to classify the existing creep strain laws according to their ability to predict low temperature and low creep strain results.
- The isothermal fits of stress versus time for a given creep strain give curves that are in some cases far from the predictions based on existing creep strain laws. In some cases, this discrepancy is due to one test only (0.1% and 0.2% at 482˚C in Figure 15 and Figure 16; 0.1% and 0.2% at 475˚C in Figure 19 and Figure 20; 0.1% and 0.2% at 450˚C in Figure 23 and Figure 24).

4.2.3 New Fit Using RCC-MR Creep Strain Law

A larger database for Mod. 9Cr-1Mo has been assembled. It includes some new creep strain data at 450˚C, 475˚C, 482˚C and 500˚C (11 new creep curves at this temperature), as well as some data at 500˚C and 538˚C which were not used in the original development of the RCC-MR creep strain laws. Using the same analysis procedure as the original RCC-MR analysis, a new fit has been obtained and it leads to values of the different parameters (C_1, C_2, n_1, K, α and β at the new temperatures (538˚C, 482˚C, 475˚C and 450˚C). The data from a creep curve at 454˚C (850˚F) were grouped and analyzed with the 450˚C data. The stress and the time to arrive at a creep strain of 0.1%, 0.2%, 0.5% and 1% as predicted by the new isothermal fits at different temperatures are shown in Figure 7 to Figure 26. The isothermal fits show better agreement with the 0.5% and 1% data at 475˚C and 450˚C than the previous fit. But, as mentioned above, this conclusion is based on very few data in some instances. When the isothermal parameters K, α and β are fitted against temperature (in practice against T^{-1} with T in Kelvin) and when the parameters C_1, C_2 and n_1 are derived from the fitted values of K, α and β, the predicted stress versus time curves for a given creep strain are in better agreement than those based on previous creep strain laws. In addition, the comparison with experimental results is not so bad in spite of the scatter of some data. Thus, this creep strain law which is labeled as “new fit” in Figure 7 to Figure 26 is the more accurate creep strain law for moderate temperatures with respect to the existing creep strain data. The corresponding new set of parameters is given in Table 10.

The negligible creep times corresponding to a reference stress of S_m and a creep strain of 0.01% are revised in Table 11. The negligible creep time from the “new fit” is closer to the negligible creep time based on the ORNL creep strain law [5] than that from the original RCC-MR equation and the Japanese equation. The negligible creep time limit from the “new fit” is still shorter than that from the ORNL equation by a factor 1.8 at 425˚C and a factor 3 at 400˚C. But the new fit confirms that, with such a criterion, creep would be negligible at 400˚C for a service duration exceeding 4.2×10^5 hours. With 1.25 S_y as a reference stress and a creep strain of 0.03%, the negligible creep times calculated with the new fit are again shown to be too small.

The negligible creep time limits corresponding to a reference stress of 1.25 S_y and a reference creep strain of 0.2% (ASME criterion) are reported in Table 12. With this criterion, negligible creep time is intermediate between that given by the RCC-MR and the ORNL creep strain laws. Even with a reduced reference stress of S_y, the negligible creep time would be less than 160,000 hours at 400˚C. The table also updates the negligible creep times with $1.5S_m$ as a reference stress. The time limit from the new fit is this time closer to that based on the ORNL creep strain law. The new fit results in negligible creep conditions that exceed a service duration of 4.2×10^5 hours, even at 450˚C. With the ORNL creep strain law, negligible creep time limit at 450˚C is only 2.8×10^5 hours.

The negligible creep time limits corresponding to a reference stress of 1.5 S_m and a reference creep strain of 0.03% (the Japanese criterion) are reported in Table 13. There is agreement with negligible creep time limits at 450˚C based on the new fit and the Japanese equation. At 425 and 400˚C, the new fit gives shorter negligible creep time limits than those predicted from the Japanese equation. At 400˚C the new fit as well as the Japanese equation give negligible creep time limits that are shorter than the desired service duration of 4.2×10^5 hours.

Table 14 provides an update of negligible creep times based on stress relaxation criteria. It can be noted that the criterion based on the relaxation of 1.5S_m by 20% would yield a negligible creep time greater than the expected design life time at 400˚C. The value of 4.2×10^5 hours would be reached at 409˚C.

Table 10 - Set of Parameters of the New Fit Creep Strain Law

Temperature (˚C)	C_1	C_2	n_1
375	$3.6655\ 10^{-14}$	0.1548	4.5638
400	$6.2124\ 10^{-14}$	0.1688	4.5538
425	$1.1113\ 10^{-13}$	0.1842	4.5427
450	$2.1171\ 10^{-13}$	0.2014	4.5304
475	$4.3422\ 10^{-13}$	0.2206	4.5167
500	$9.7200\ 10^{-13}$	0.2421	4.5012
525	$2.4160\ 10^{-12}$	0.2665	4.4837
550	$6.8171\ 10^{-12}$	0.2945	4.4637
575	$2.2474\ 10^{-11}$	0.3266	4.4406
600	$8.9940\ 10^{-11}$	0.3641	4.4137

Table 11 - Application of Revised Material Data to Negligible Creep of Table 6

Negligible Creep Criteria	Temperature	RCC-MR 2002 Creep Strain Law	New Fit	ORNL Creep Strain Law
S_m and 0.01%	400˚C	28,200 hr	8.9×10^5 hr	2.73×10^6 hr
	425˚C	2230 hr	3.6×10^4 hr	6.32×10^4 hr
	450˚C	199 hr	1730 hr	4148 hr
1.25 S_y and 0.03%	400˚C	<0.1	<0.1	143
	425˚C	<0.1	<0.1	5.7
	450˚C	<0.1	<0.1	0.33

Table 12 - Application of Revised Material Data to Negligible Creep of Table 8

Negligible Creep Criteria	Temperature	RCC-MR 2002 Creep Strain Law	New Fit	ORNL Creep Strain Law
1.5 S_m and 0.2%	400˚C	3.19×10^5 hr	8×10^8 hr	7.37×10^8 hr
	425˚C	36,073 hr	1.9×10^7 hr	5.76×10^6 hr
	450˚C	4522 hr	5.5×10^5 hr	2.84×10^5 hr
1.25 S_y and 0.2%	400˚C	6.26 hr	384 hr (*)	11,975 hr
	425˚C	1.70 hr	29 hr	508 hr
	450˚C	0.53 hr	2.7 hr	30 hr

(*) The negligible time would become 158,753 hours with the S_y instead of 1.25 S_y stress

Table 13 - Application of Revised Material Data to Negligible Creep of Table 9

Negligible Creep Criteria	Temperature	RCC-MR 2002 Creep Strain Law	New Fit	Japanese Creep Strain Law
1.5 S_m and 0.03%	400˚C	395 hr	10,600 hr	1.23×10^5 hr
	425˚C	55.8 hr	637 hr	2016 hr
	450˚C	8.6 hr	44 hr	45.9 hr

Table 14 - Application of Revised Material Data to Negligible Creep of Table 7

Negligible Creep Criteria	Temperature	RCC-MR 2002 Creep Strain Law	New Fit
Relaxation of $\square_0=3S_m/E$ by 10 %	400˚C	0.32 hr	<0.1 hr
	425˚C	<0.1 hr	<0.1 hr
Relaxation of 1.5 S_m by 20%	400˚C	13,501 hr	1.67×10^6
	425˚C	1244 hr	5.30×10^4

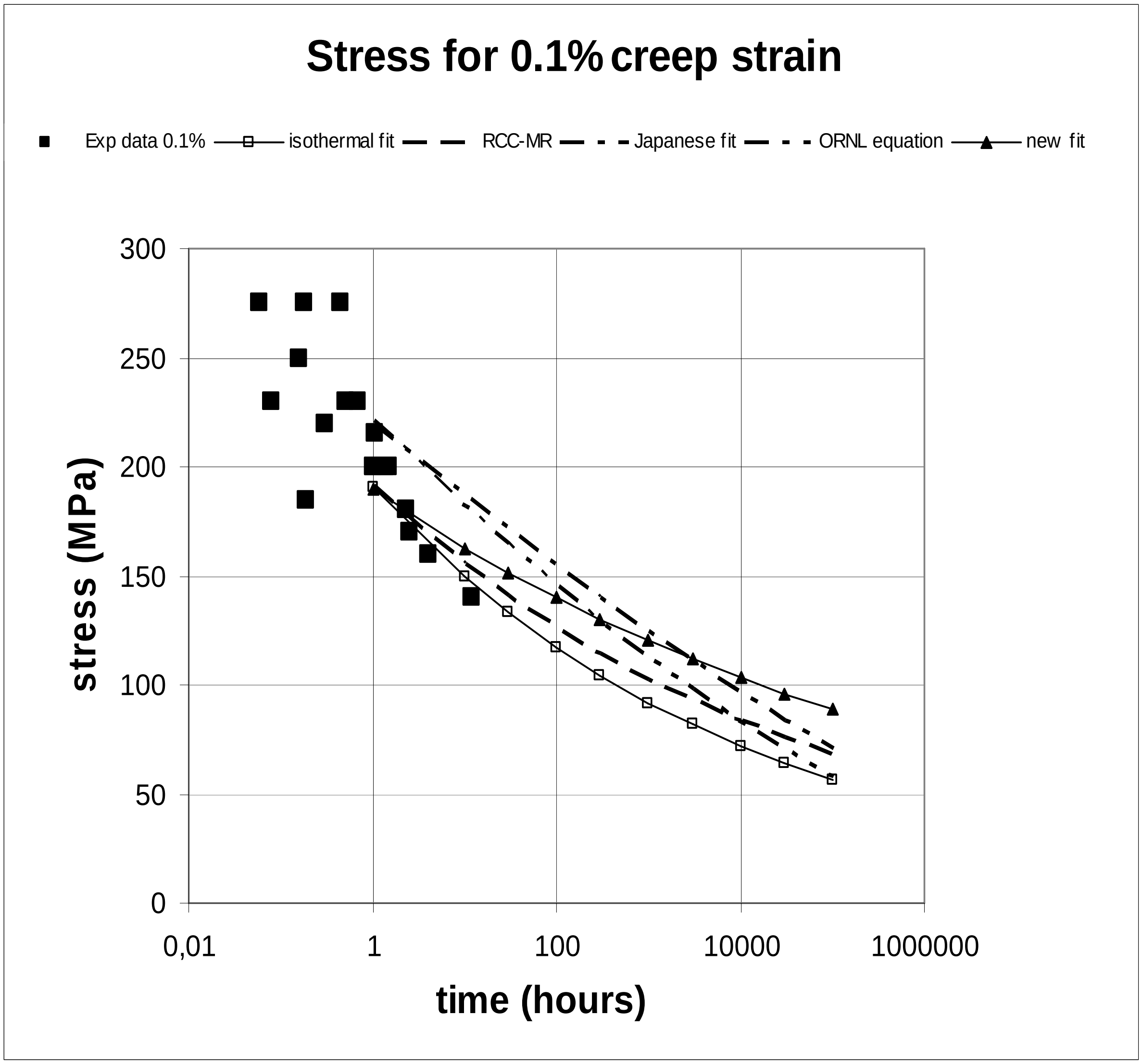


Figure 7 - Stress for 0.1% Creep Strain at 550˚C

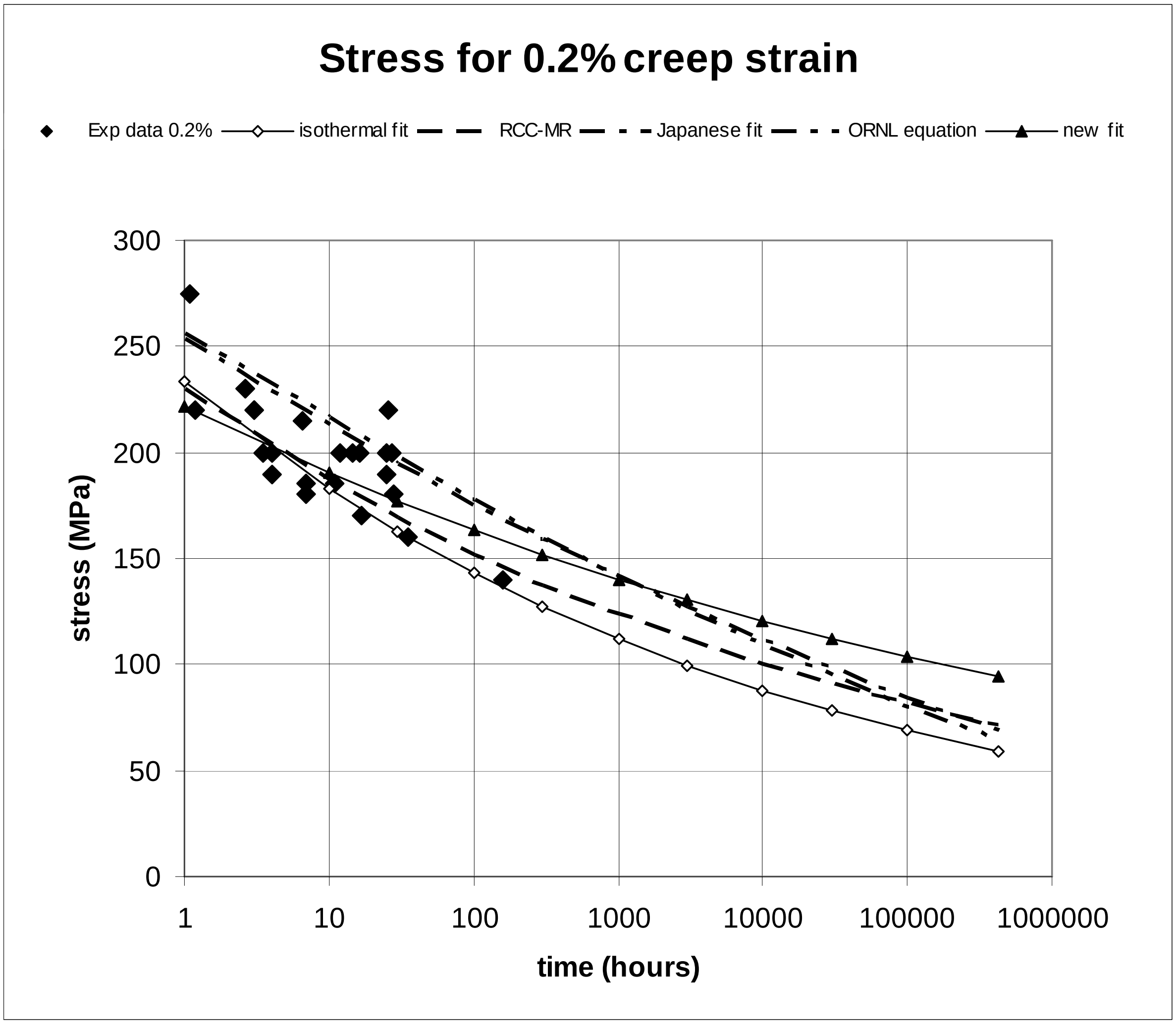


Figure 8 - Stress for 0.2% Creep Strain at 550˚C

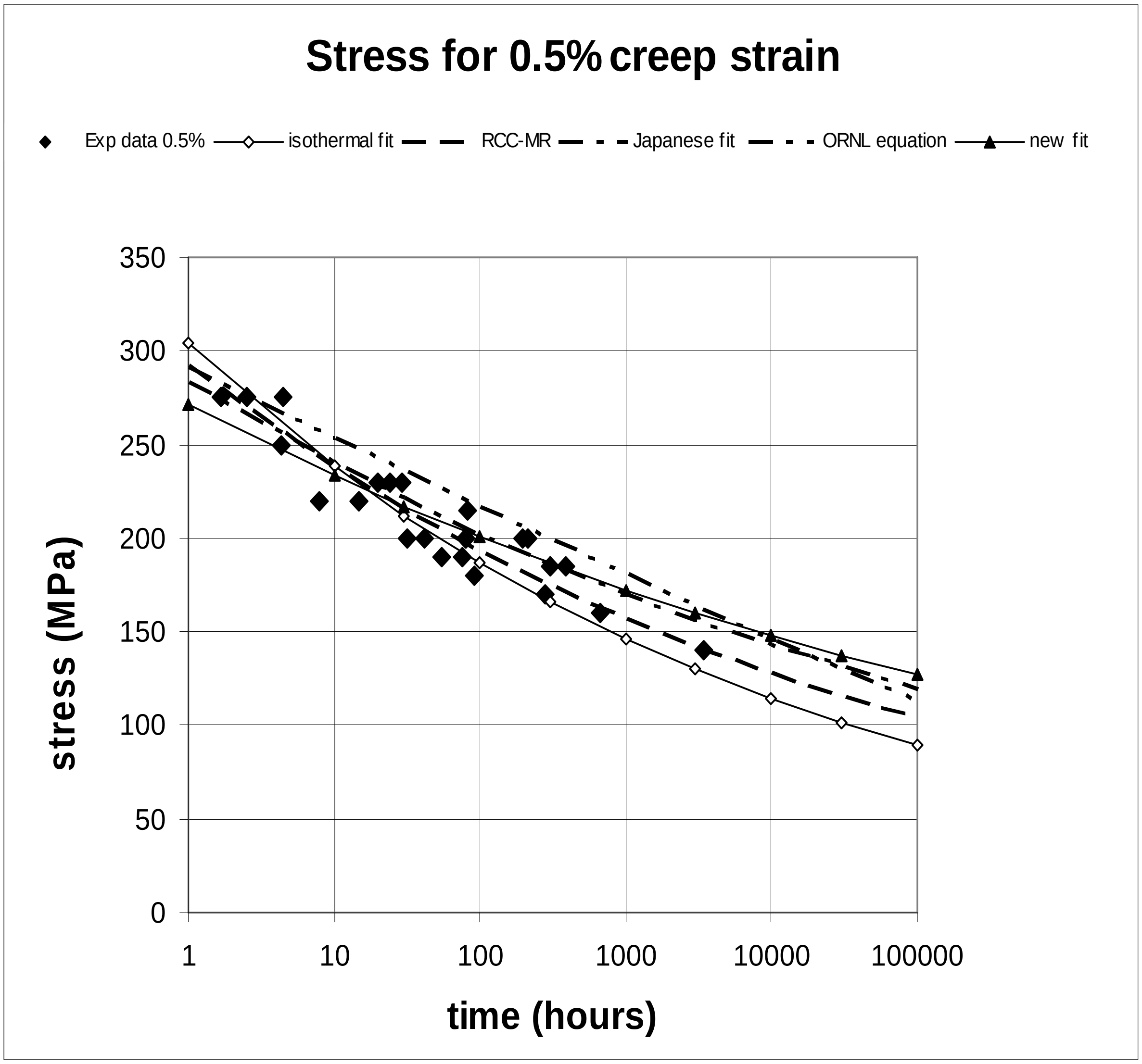


Figure 9 - Stress for 0.5% Creep Strain at 550˚C

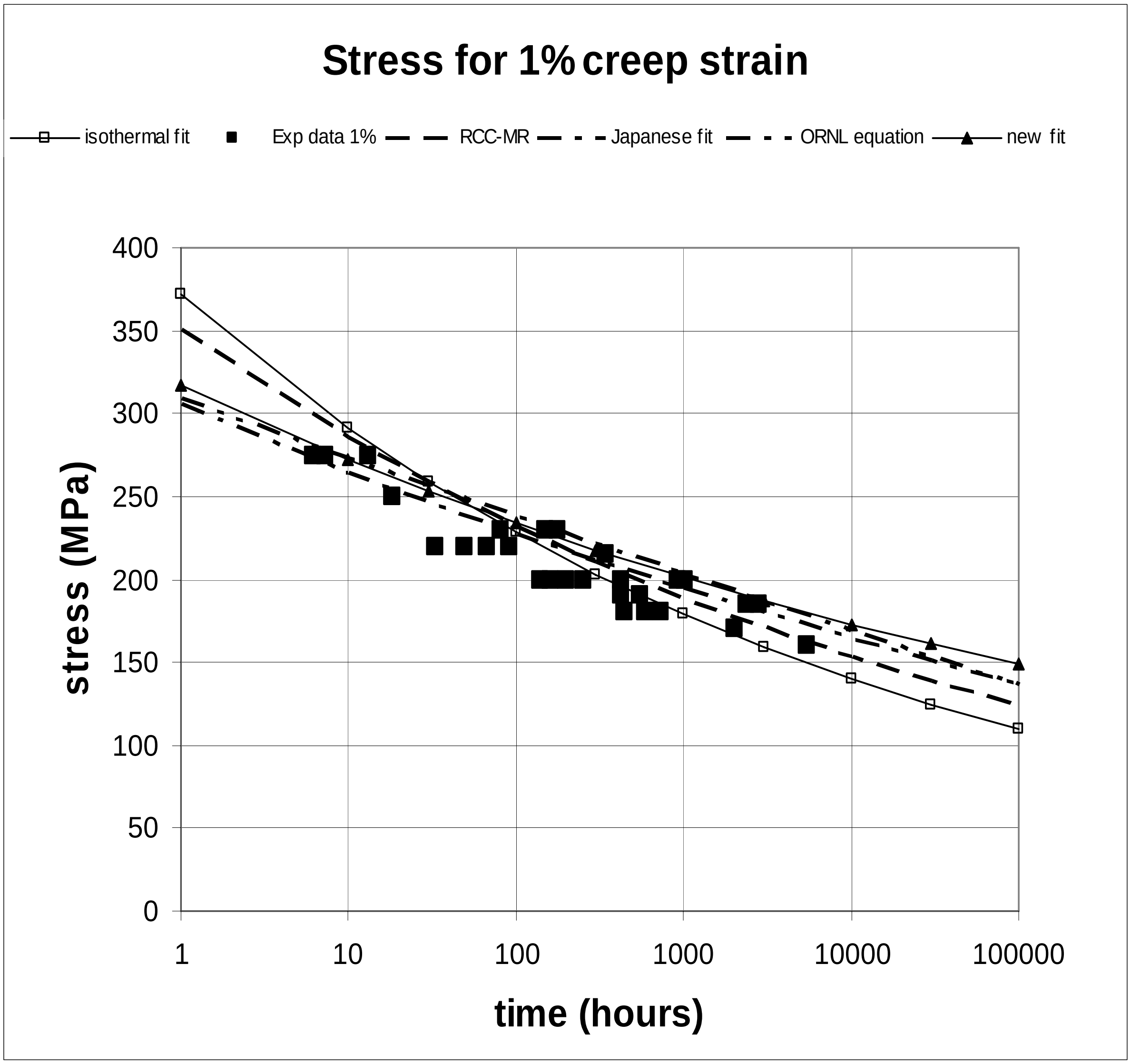


Figure 10 - Stress for- 1% Creep Strain at 550˚C

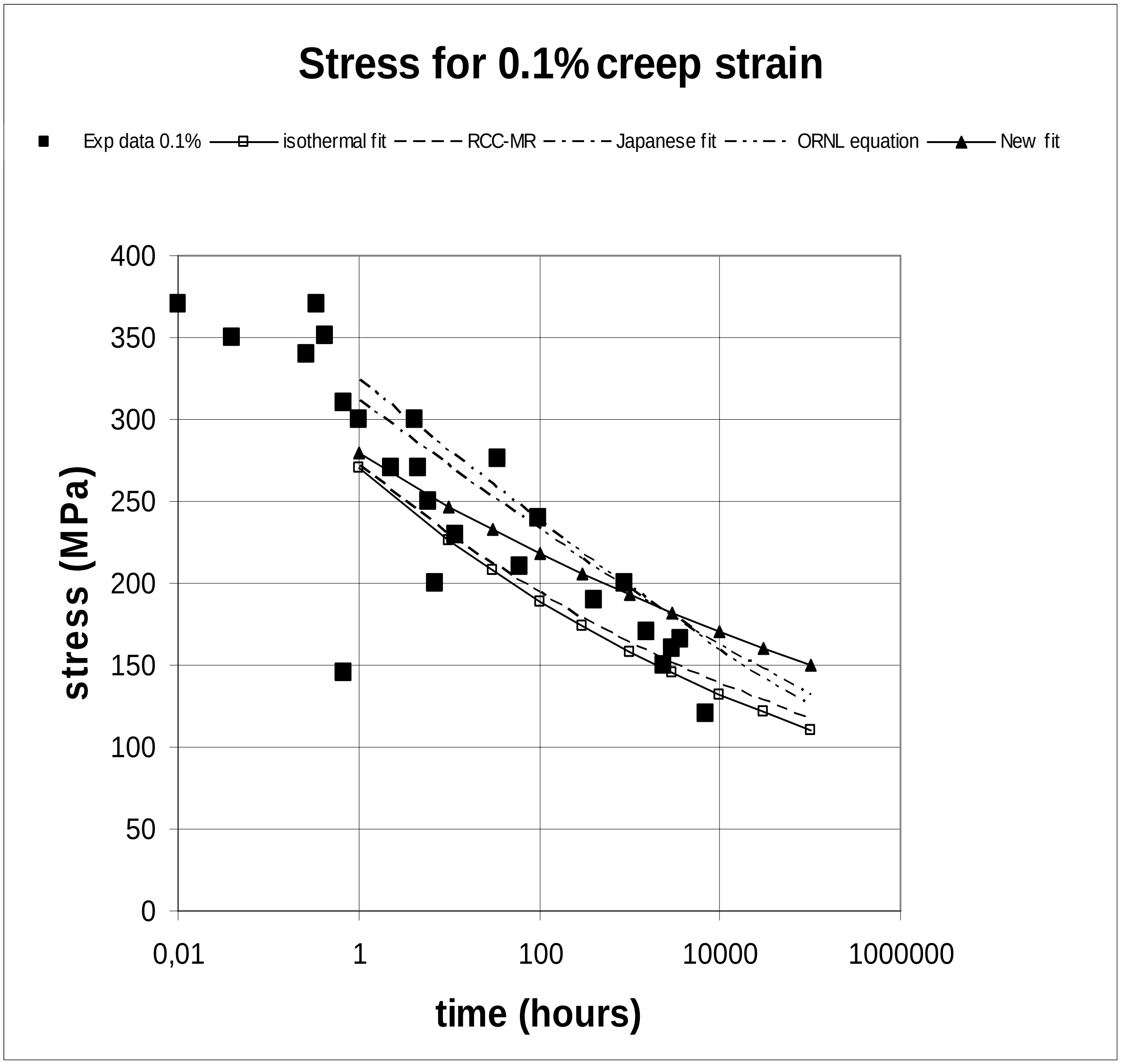


Figure 11 - Stress for 0.1% Creep Strain at 500˚C

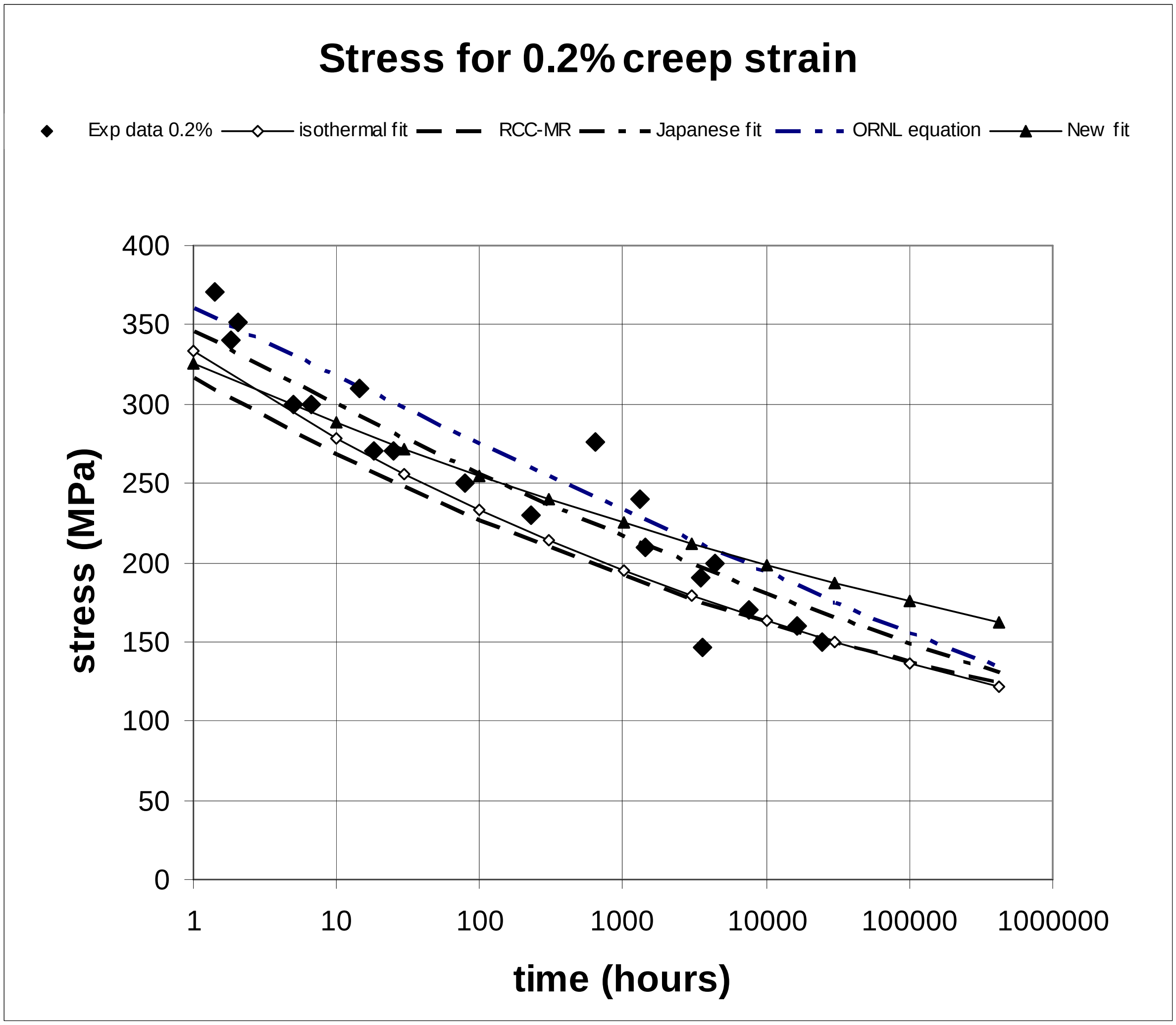


Figure 12 - Stress for 0.2% Creep Strain at 500˚C

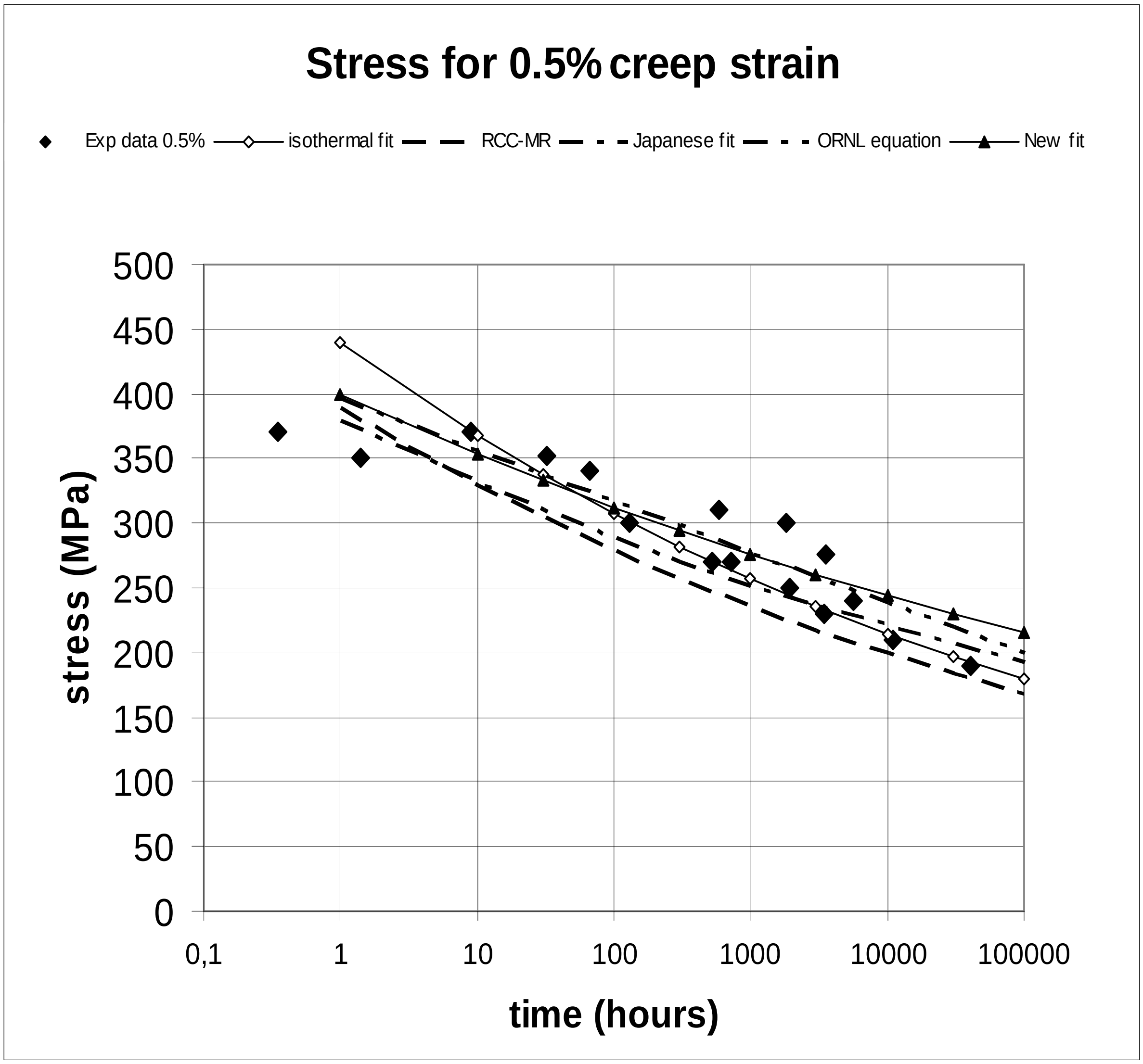


Figure 13 - Stress for 0.5% Creep Strain at 500˚C

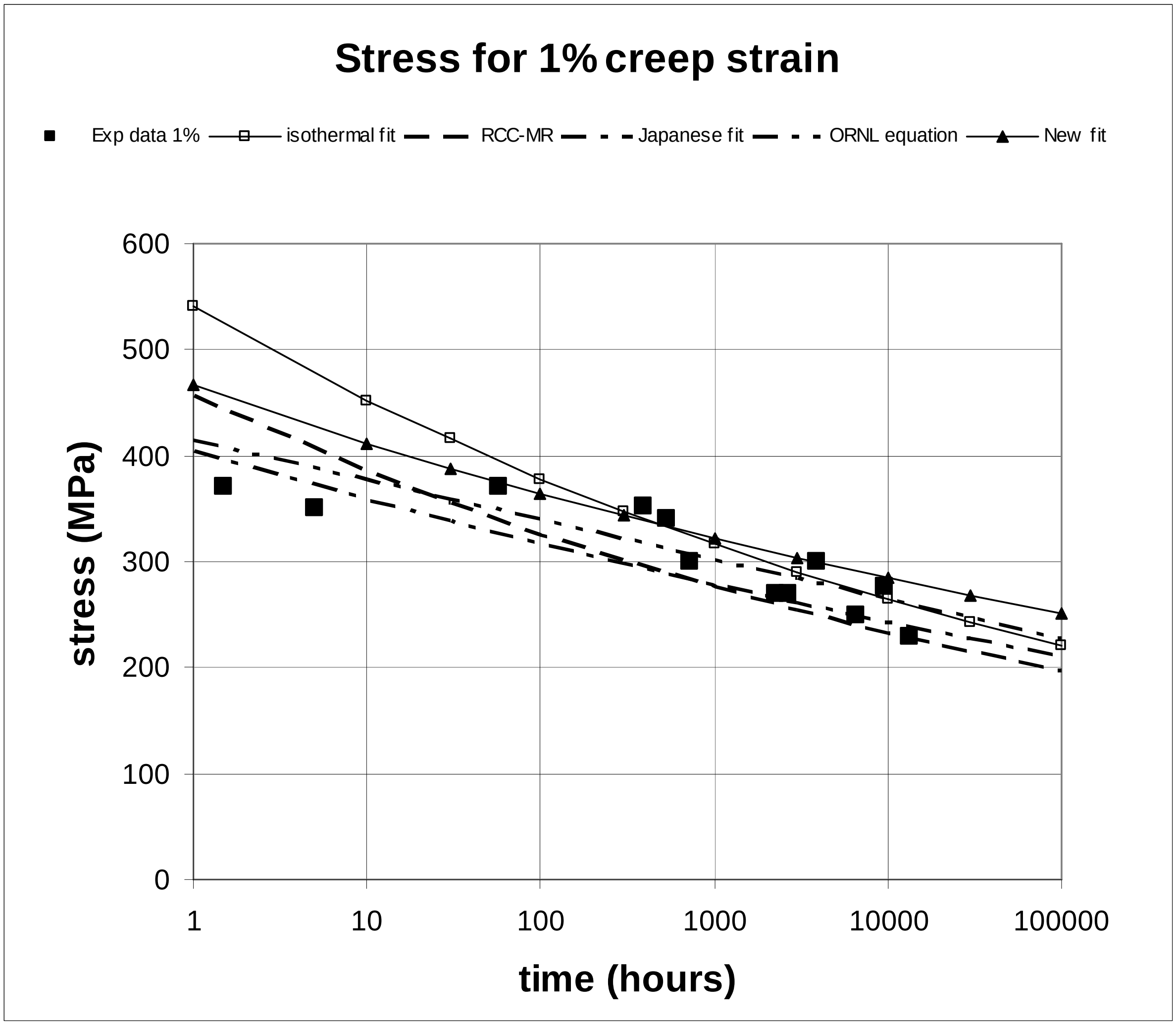


Figure 14 - Stress for 1% Creep Strain at 500˚C

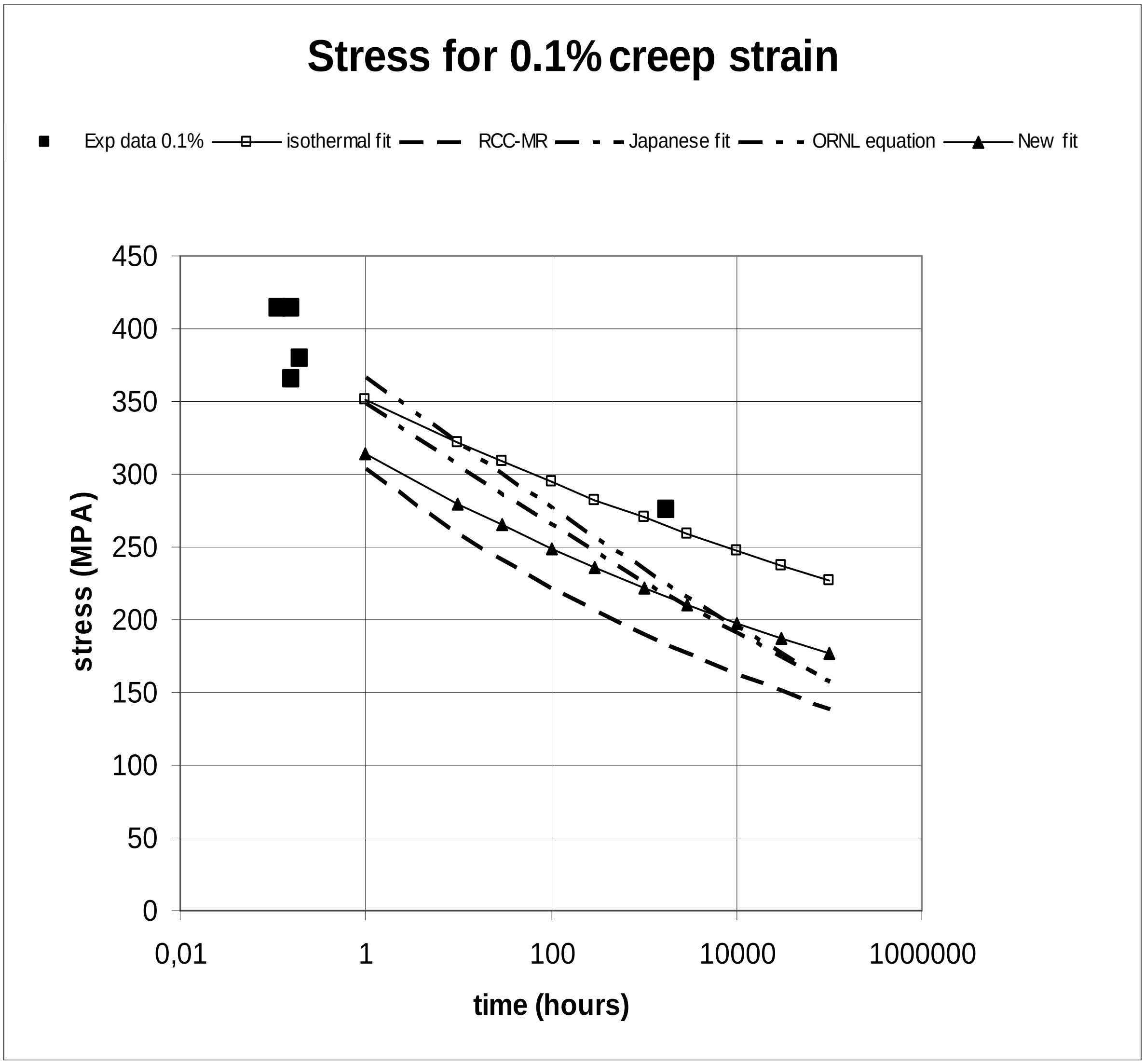


Figure 15 - Stress for 0.1% Creep Strain at 482˚C

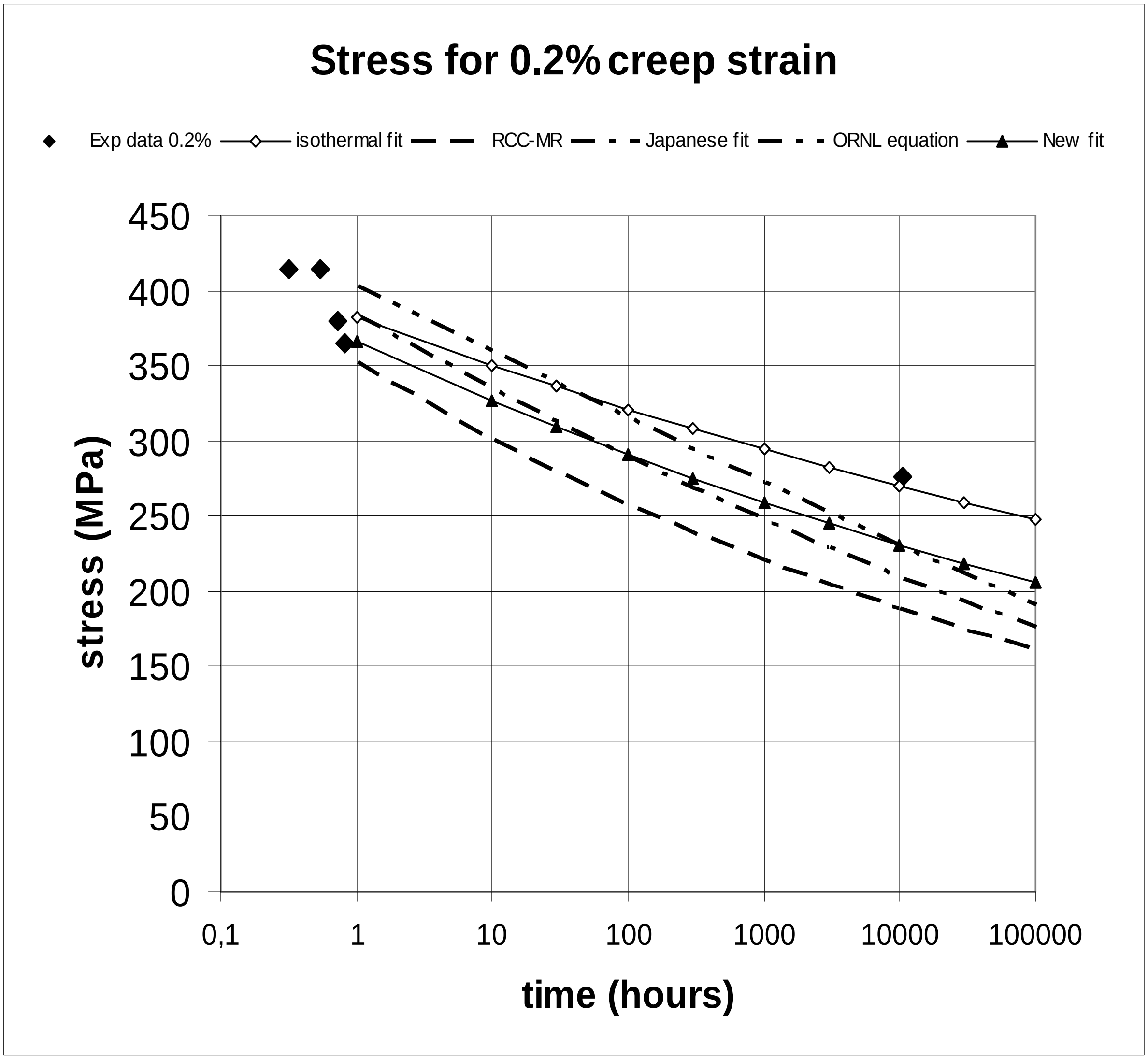


Figure 16 - Stress for 0.2% Creep Strain at 482˚C

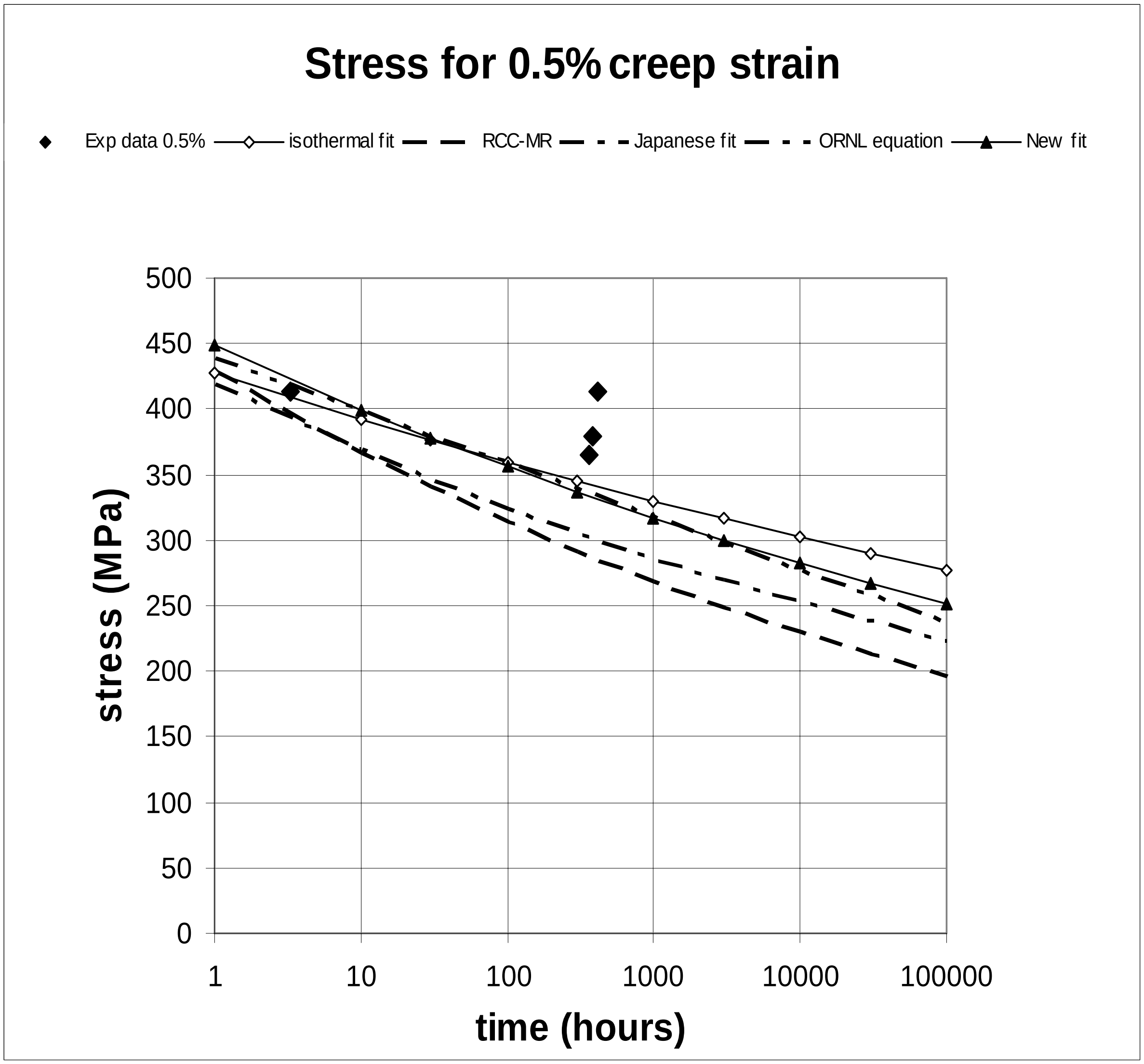


Figure 17 - Stress for 0.5% Creep Strain at 482˚C

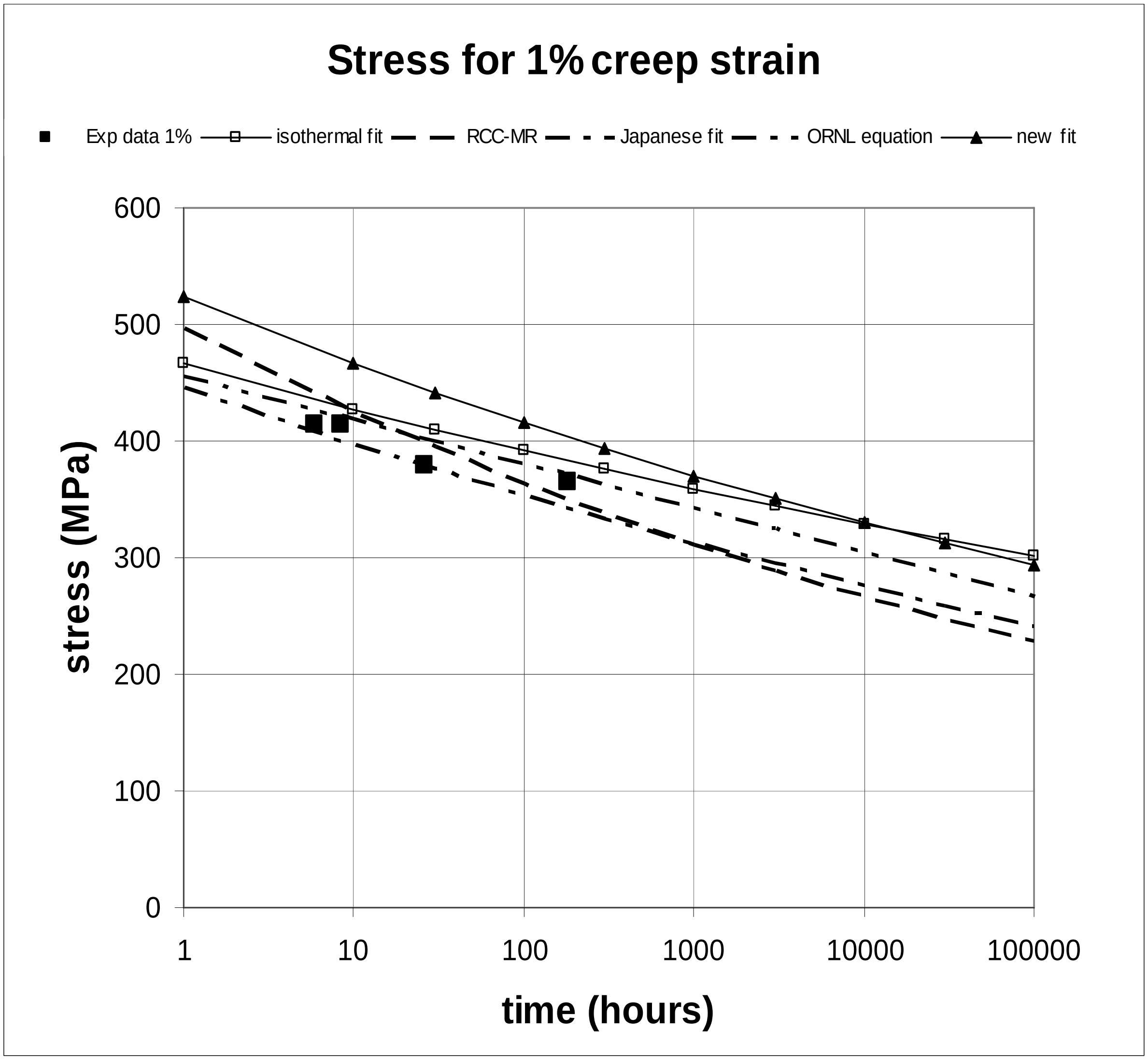


Figure 18 - Stress for 1% Creep Strain at 482˚C

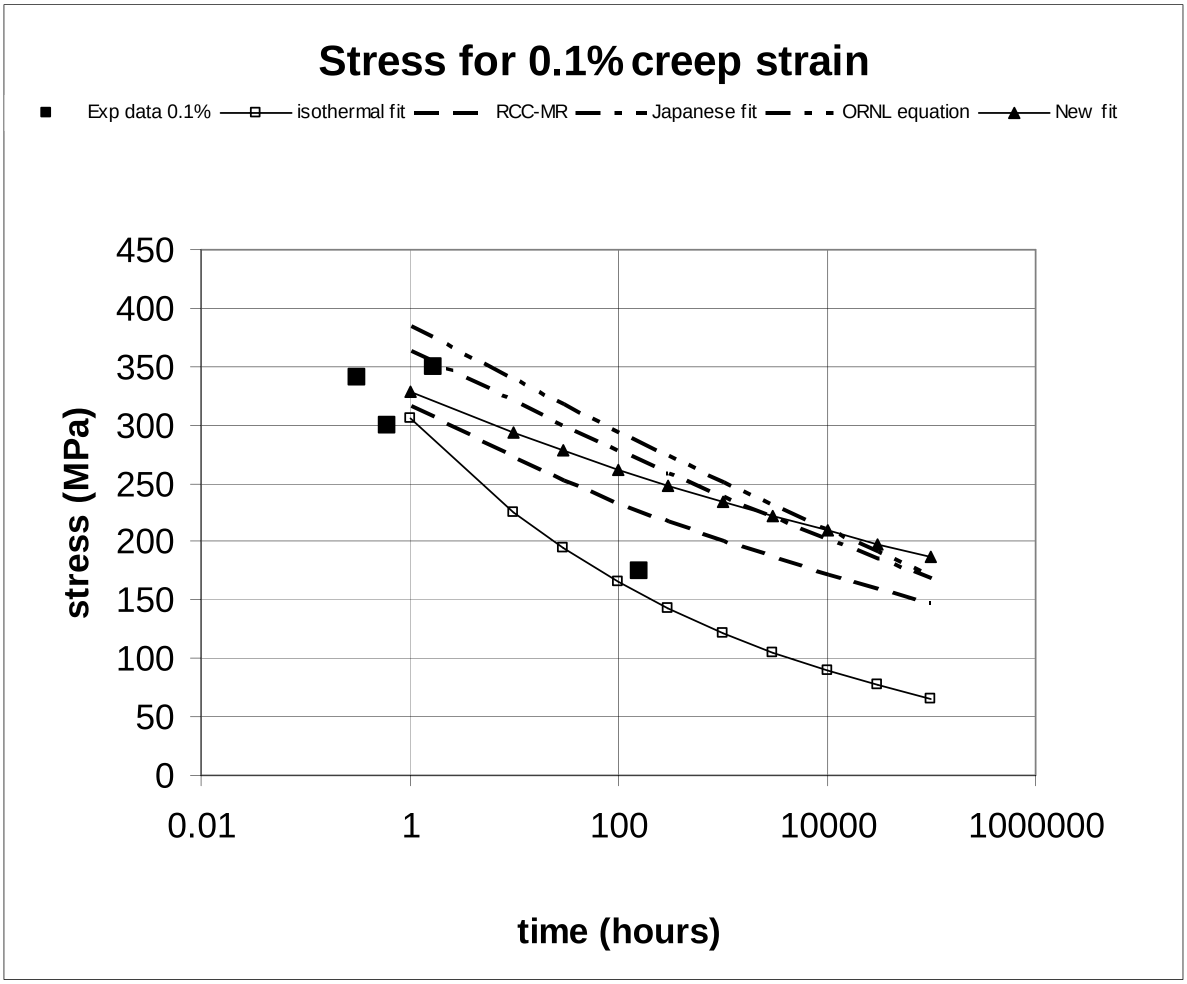


Figure 19 - Stress for 0.1% Creep Strain at 475˚C

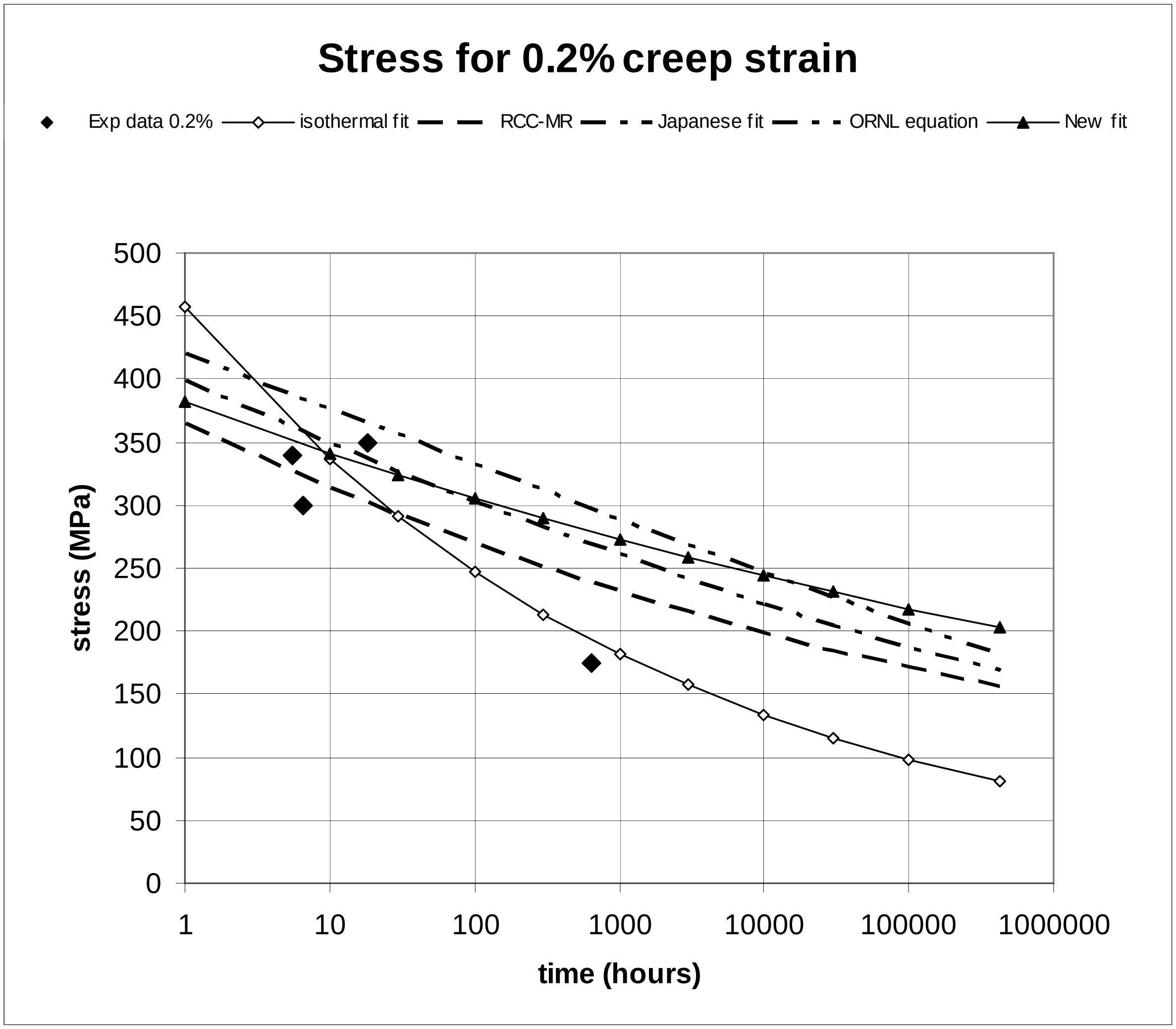


Figure 20 - Stress for 0.2% Creep Strain at 475˚C

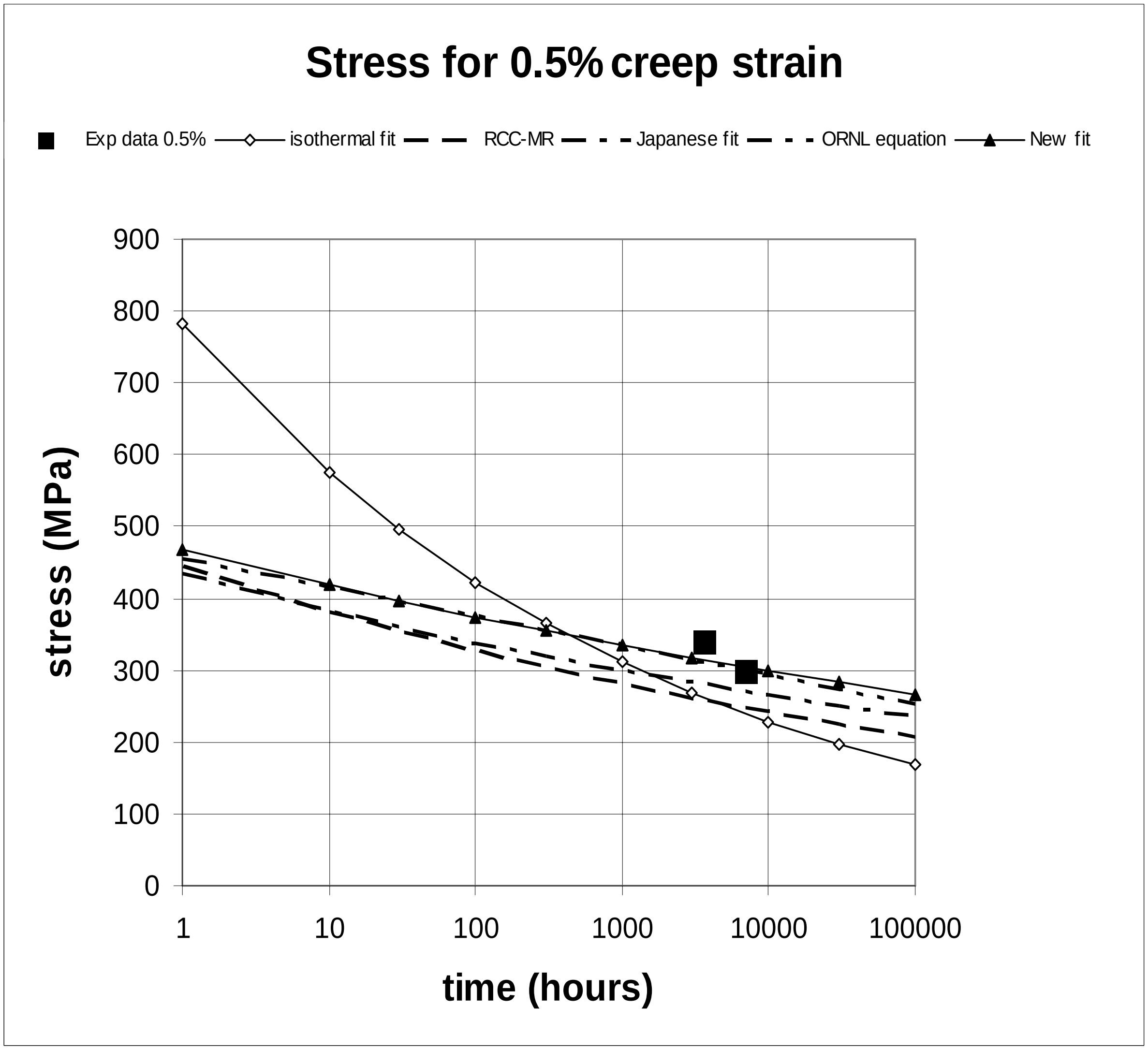


Figure 21 - Stress for 0.5% Creep Strain at 475˚C

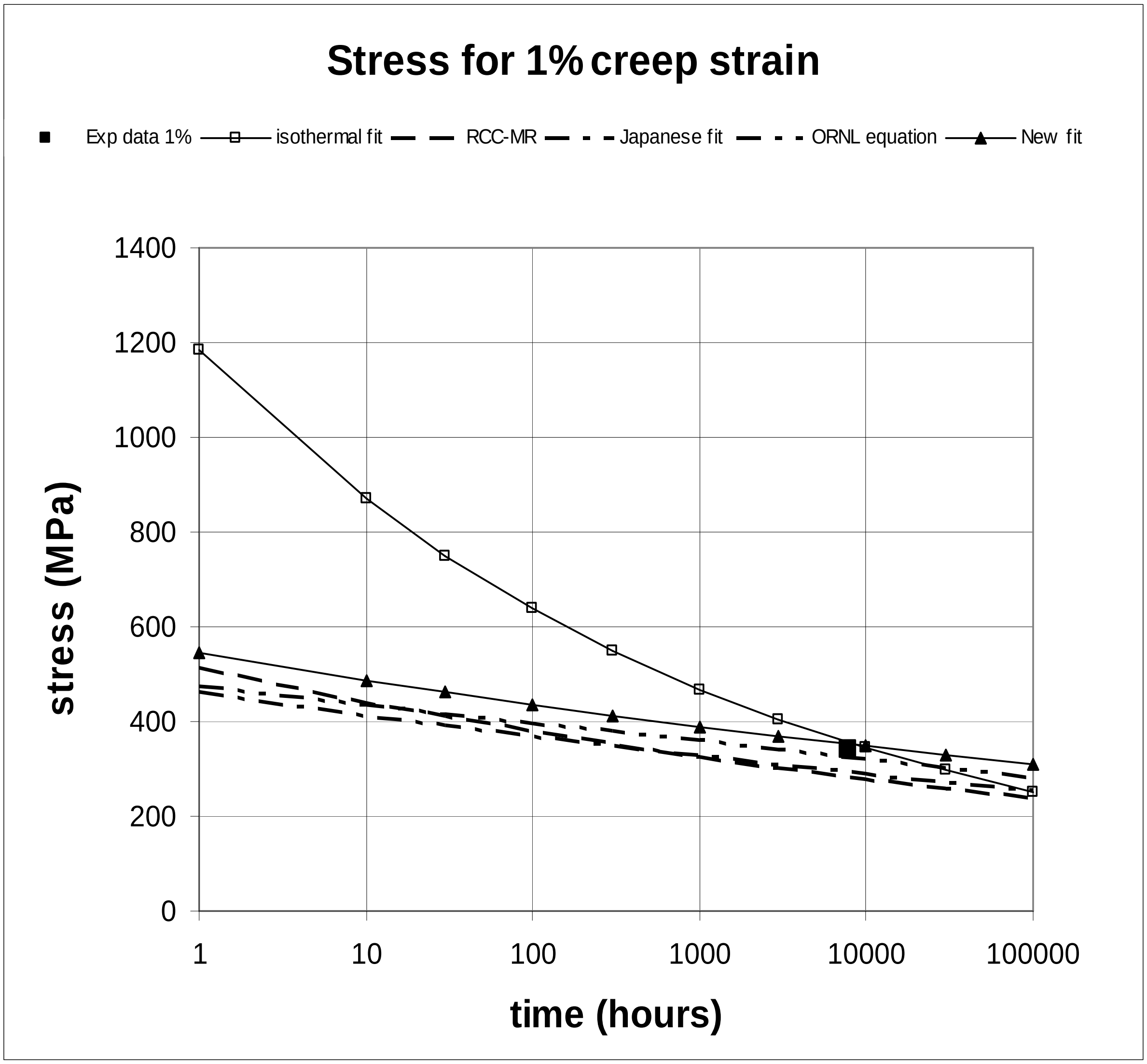


Figure 22 - Stress for 1% Creep Strain at 475˚C

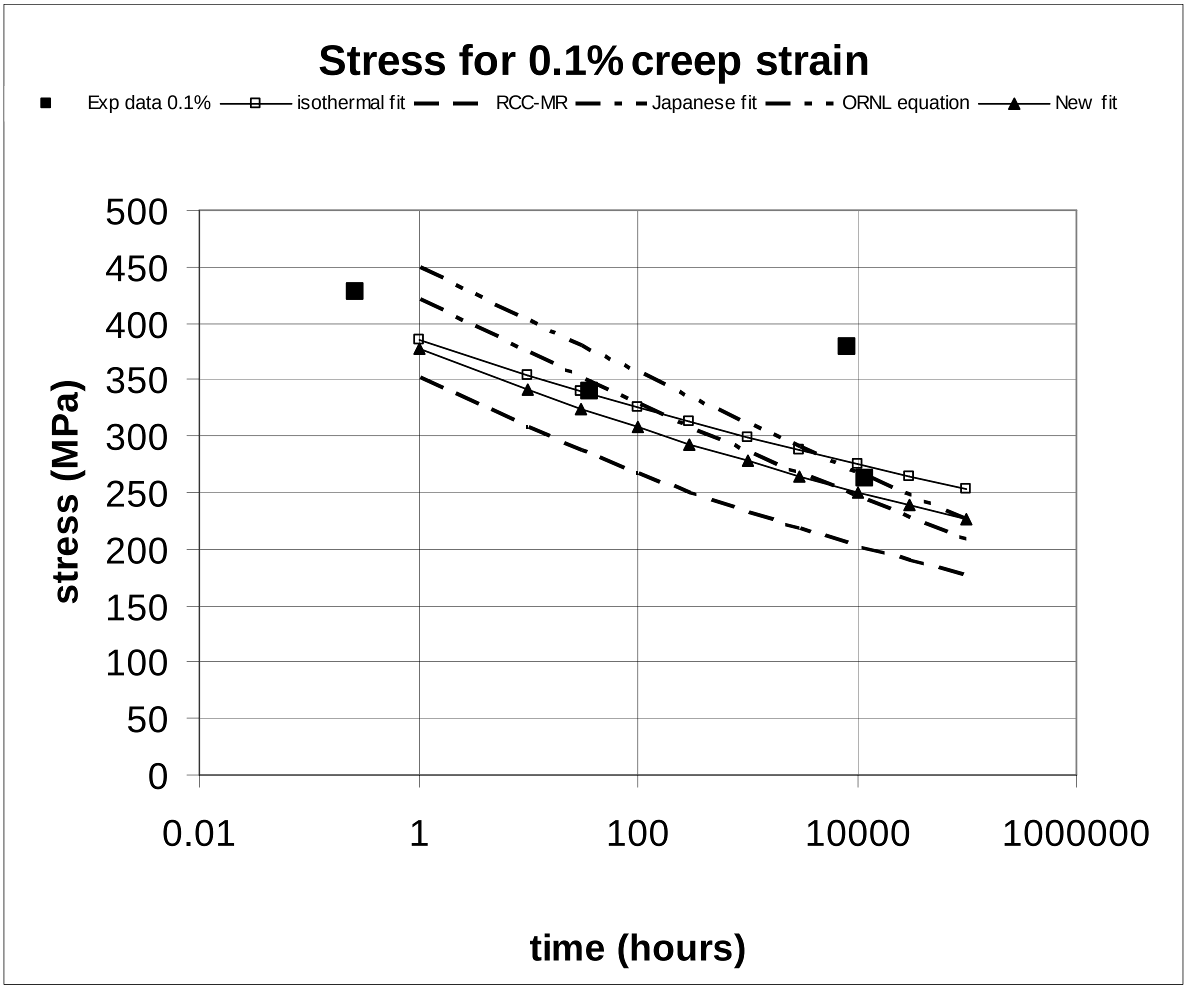


Figure 23 - Stress for 0.1% Creep Strain at 450˚C

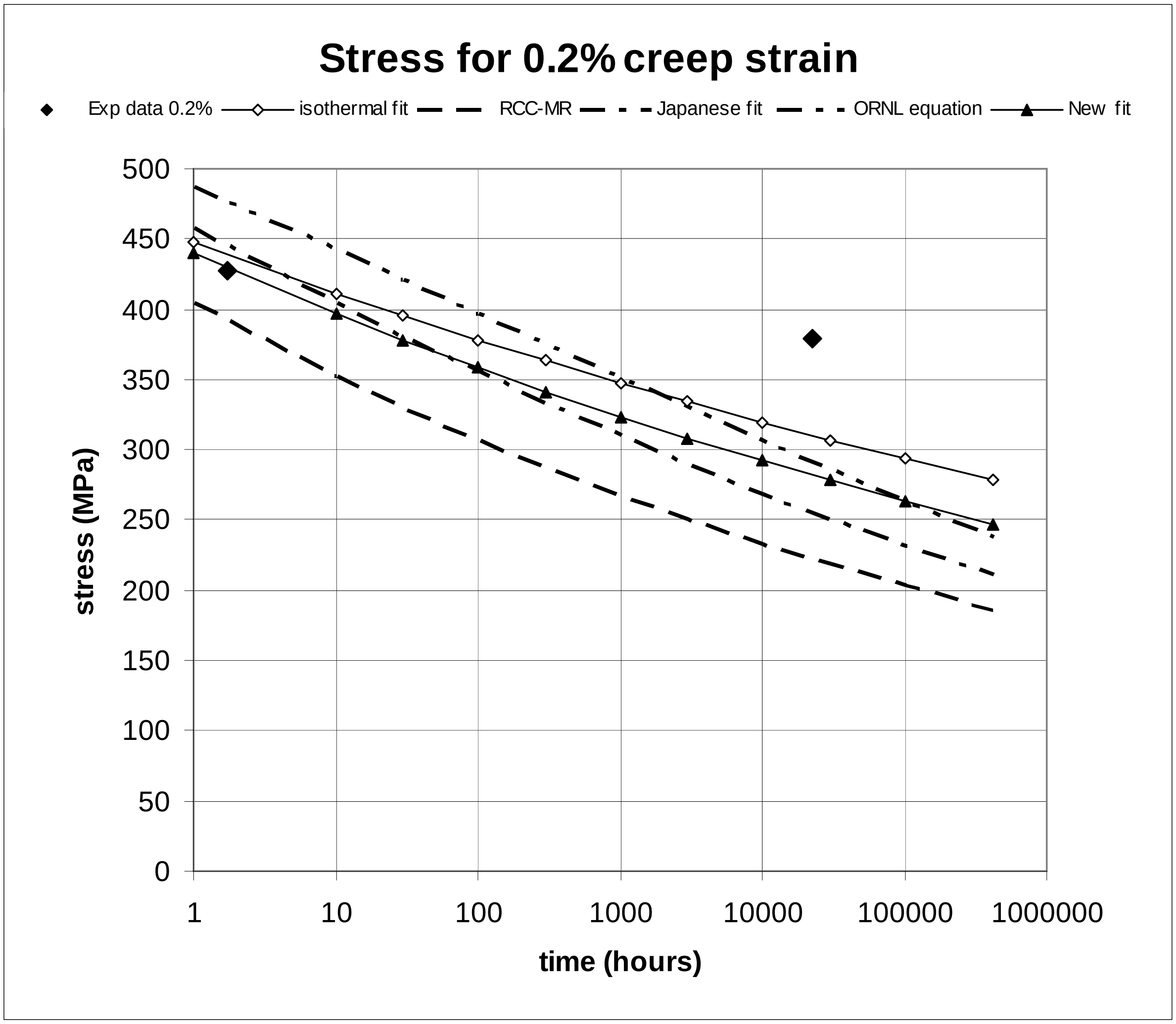


Figure 24 - Stress for 0.2% Creep Strain at 450˚C

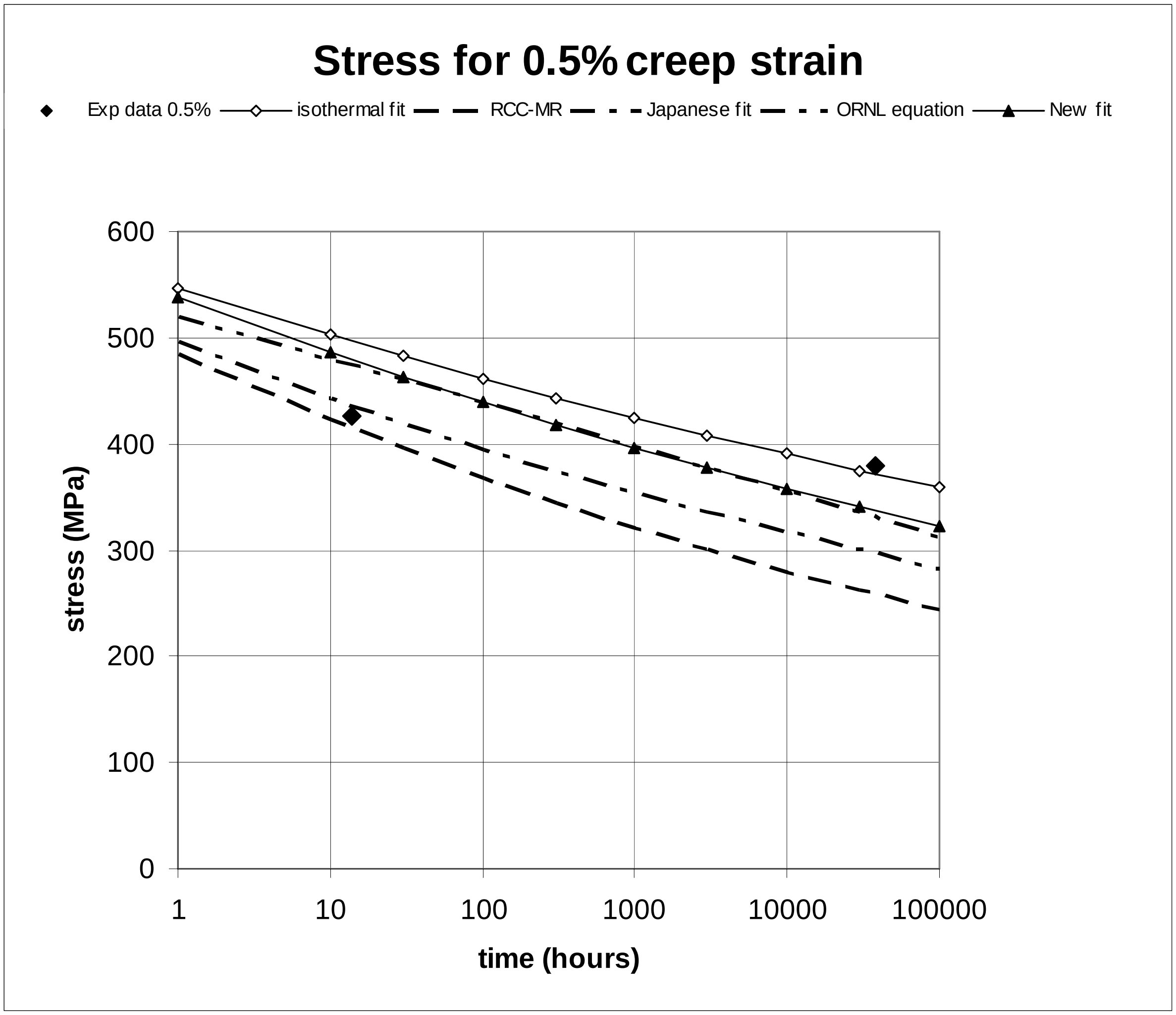


Figure 25 - Stress for 0.5% Creep Strain at 450˚C

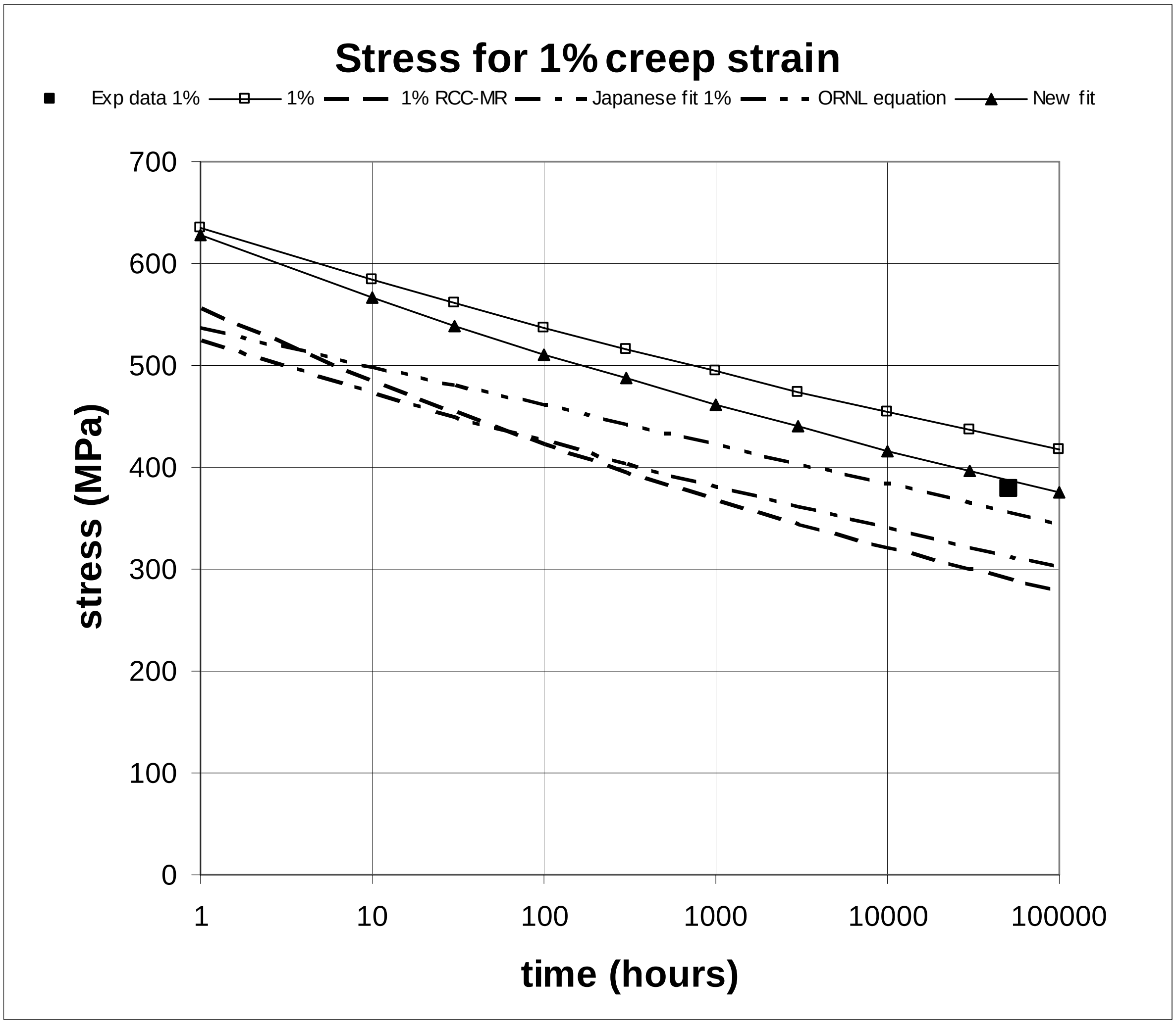


Figure 26 - Stress for 1% Creep Strain at 450˚C

4.3 Creep Stress to Rupture

4.3.1 RCC-MR Stress to Rupture Data for Modified 9Cr-1Mo

Average stress to rupture data are tabulated in RCC-MR 2002 against temperature in the range 425–675˚C and time up to 3×10^5 hours. These average values were obtained by an analysis performed in 1995 on a database consisting of 427 data. The correlation model used to fit the data by multiple regression was:

$$\log_{10}(t) = -C_0 + \frac{1}{T}\left[a_0 + a_1\log_{10}\sigma + a_2(\log_{10}\sigma)^2 + a_3(\log_{10}\sigma)^3\right] \tag{10}$$

T being the temperature in Kelvin.

This equation can be transformed to the Larson-Miller formulation as:

$$P = \left(\log_{10}(t) + C_0\right)T \tag{11}$$

where the Larson-Miller parameter P is defined as

$$P = a_0 + a_1\log_{10}\sigma + a_2(\log_{10}\sigma)^2 + a_3(\log_{10}\sigma)^3 \tag{12}$$

The constant value of C_0 used for the Larson-Miller parameter P is obtained by regression and can be different from the classical value of 20 (26.4 in the 1995 and 2002 analysis). The RCC-MR 2002 average values of the stress to rupture are given in Table 15.

4.3.2 Average Stress to Rupture of Modified 9Cr-1Mo Steel Using ORNL Data

In the [5] report, the average time to rupture is given as a function of stress as follows:

$$\log_{10} t_r = C_h - 0.0231\sigma - 2.385\log_{10}\sigma + \frac{31080}{T} \tag{13}$$

where t_r is in hours, σ in MPa, T in Kelvin and C_h = –23.737.

The average values of the stress to rupture derived from the equation in [5] are given in Table 16.

4.3.3 Average Stress to Rupture of Modified 9Cr-1Mo Steel Using Japanese Data

In the [6] paper, the average time to rupture is given as a function of stress as follows:

$$(\theta + 273.15)\left[\log_{10}(t_r) + C\right] = B_0 + B_1\log_{10}\sigma + B_2(\log_{10}\sigma)^2 \tag{14}$$

where t_r is in hours, θ in ˚C , C = 29.1146, B_0 =31,808.82, B_1 = 3055.52 and B_2 = –5148.248.

With these numerical values, it appears that the stress σ must be in kg/mm^2 to obtain correct order of magnitude for t_r.

The average stress to rupture derived from the equation in reference 6, assuming that the stress is in kg/mm^2, are given in Table 17.

4.3.4 Average Stress to Rupture of Modified 9Cr-1Mo Steel Using Minimum Commitment Method

In [7], an equation is given for the expected time to rupture as a function of temperature and stress as follows:

$$\log_{10}(t) = a_0 + a_1 \log_{10}\sigma + a_2\sigma + a_3\sigma^2 + a_4 T + a_5/T \qquad (15)$$

The coefficients a_0 to a_5 in this equation were obtained by the Minimum Commitment (MC) method in [7] as:

$a_0 = -0.73$

$a_1 = -5.15$

$a_2 = -0.0059$

$a_3 = -1.68\times10^{-5}$

$a_4 = -0.0089$

$a_5 = 21{,}058.5$

The average values of the stress to rupture from different methods are given in Table 18. The magnitudes of these values can be rank-ordered in the following manner:

At 500˚C: ORNL equation [5] > MC equation [11] > Japanese fit [12] > RCC-MR;

At 550˚C: ORNL equation [5] > Japanese fit [12] > MC equation [11] > RCC-MR.

Figure 27 and Figure 28 compare the different average curves of the stress to rupture to the experimental data at 550˚C and 500˚C, respectively. At 550˚C, the ORNL equation appears to overestimate the stress to rupture values for creep life exceeding 10,000 hours. At 500˚C, both ORNL [5] and MC [7] equations overestimate the stress to rupture values for creep life exceeding 10,000 hours. The agreement between RCC-MR (ed. 2002) and the Japanese fit of reference 6 is good at 500 and 550˚C.

The average values of the stress to rupture at 450˚C are compared to the experimental results in Figure 29. Greater differences are observed at this temperature. The different fits overpredict the stress to rupture at short times. At long term, the underestimation is smaller when Minimum Commitment (MC) equation or ORNL equation are used, but none of the available equations is appropriate for long term extrapolation in the 425–500˚C temperature range.

4.3.5 Average Stress to Rupture of Modified 9Cr-1Mo Steel at Moderate Temperatures

For use in the negligible creep curve criteria in the temperature range of interest (425–500˚C), the RCC-MR data were considered as lacking of confidence. As the database of 427 data comprised only 4 results at temperature less than 500˚C (and precisely at 482˚C) and only 25 data at 500˚C, stress to rupture at 450˚C and 425˚C were the result of large extrapolation. It was therefore decided to complete the database as far as possible in the temperature range 425–500˚C and to perform a new analysis for comparison. The number of data gathered in Europe, Japan and the U.S. is 559 with 23 data at 450˚C and 484˚C and 105 data at 500˚C.

In spite of the important increase of moderate temperature data, the average stress to rupture evaluated as indicated in paragraph 4.3.1 are not changed significantly (a maximum change of only 2 MPa in Table 15). The new determination of average stress to rupture confirms the value of 26.4 for the constant C_0 of the Larson-Miller parameter P. In Figure 27 to Figure 29, the RCC-MR curves and the revised curves cannot be distinguished graphically.

Table 20 provides the new set of parameters identified on the extended database.

No tests to failure were performed at temperatures lower than 450˚C. At 450˚C, failures were observed for a small range of stresses, between 450 and 360 MPa, which are not far from the tensile strength. This range of stress is between 102.5% and 82% of the minimum value of the tensile strength. There is a possibility of improving the isothermal fit of the 450˚C data but it requires more stress to rupture data at this temperature in the same stress range.

4.3.6 Application of Stress to Rupture at Moderate Temperatures to Negligible Creep

The need for, and the difficulty in, increasing the stress to rupture database at moderate temperatures (450–500˚C) with more results from a larger number of representative steel forgings are commented upon in paragraph 4.3.5. In addition, the stress to rupture values used in the design of nuclear components are minimum values, in contrast to the design of non-nuclear pressure vessel and piping where average values as proposed in [7] are used. This implies that negligible creep with a stress to rupture criterion should be based on minimum values.

The RCC-MR minimum values were evaluated using the standard deviation of the Larson-Miller parameter:

$$\upsilon = \left[\Sigma_{n\ data} \left(P_{estimated} - P_{\exp eriments} \right)^2 \big/ n \right]^{1/2} \tag{16}$$

The curve of minimum values of the stress to rupture corresponds to the minimum values of the Larson-Miller parameter $P_{min} = P_{estimated} - 1.96\ \nu$

RCC-MR minimum values and minimum values obtained with the revised RCC-MR average stress to rupture are given in Table 19. The values from the revised RCC-MR analysis are higher than the RCC-MR values for all temperature-time conditions. This is due to the small reduction in the standard deviation which results from the increase in the number of data (ν= 0.394 for the 1995 analysis and ν = 0.324 for the revised analysis using 559 data).

Similarly, minimum values of the stress to rupture can be derived for other formulations of the creep to rupture, such as the Minimum Commitment (MC) or ORNL equations, using the standard deviation of $\log_{10}$t. Such evaluation nonetheless requires the knowledge of the whole database used to derive average values or new derivation of average and minimum values from an agreed upon database.

An equation is given in reference 6 for the Japanese minimum values of the stress to rupture. The equation is the same as for the average values except that the time is affected by a factor of 10. The corresponding minimum values of the stress to rupture are given in Table 19. There is close agreement between the revised minimum values and the Japanese minimum values at 450˚C. The agreement is better than in the case of RCC-MR values. As the temperature is increased, the revised analysis gives long term minimum values that are more restrictive (lower) than those from the Japanese data.

Figure 27 to Figure 29 also compare experimental results with the ASME design creep stress to rupture taken from Subsection NH. At 500˚C and 550˚C, the ASME curves are almost a lower bound of experimental results. At 450˚C, the design curve provides significant margin compared to long term test results (beyond 10^4 hours).

Table 21 shows the impact of the revised minimum creep stress to rupture values using the ASME time fraction criterion with S_y as a reference stress. It can be seen that the design life of 4.2×10^5 hours could not be justified at 400˚C with the revised data. Further test results would be required to improve the creep stress to rupture at low temperatures (below 500˚C). In the meantime, it can be considered that the ASME curves can be reliably used at 450˚C and below.

Table 15 - RCC-MR Average Stress to Rupture for Times from 10,000 hours to 300,000 h

	Predicted Time	10,000 hr	30,000 hr	100,000 hr	200,000 hr	300,000 hr
450˚C	RCC-MR 2002	362	340	316	302	295
	Revised analysis	360	338	314	301	293
500˚C	RCC-MR 2002	268	247	225	213	206
	Revised analysis	266	246	224	211	205
550˚C	RCC-MR 2002	187	168	149	138	132
	Revised analysis	186	168	148	138	132
600˚C	RCC-MR 2002	119	103	87	78	73
	Revised analysis	119	103	87	78	74
650˚C	RCC-MR 2002	66	54	42	37	34
	Revised analysis	66	54	43	37	34

Table 16 - Average Stress to Rupture for Times from 10,000 hours to 300,000 hours Derived from [5]

	10,000 hr	30,000 hr	100,000 hr	200,000 hr	300,000 hr
450˚C	392	374	354	342	335
500˚C	286	268	249	238	232
550˚C	197	180	163	152	147
600˚C	124	109	94	85	80
650˚C	68	56	44	38	34

Table 17 - Average Stress to Rupture for Times from 10,000 hours to 300,000 hours Derived from [6]

	10,000 hr	30,000 hr	100,000 hr	200,000 hr	300,000 hr
450˚C	363	341	319	306	298
500˚C	267	248	228	217	211
550˚C	188	172	155	146	140
600˚C	125	111	96	88	83
650˚C	73	61	47	40	35

Table 18 - Comparison of Average Stress to Rupture for Times from 10,000 hours to 300,000 hours

	Predicted Time	10,000 hr	30,000 hr	100,000 hr	200,000 hr	300,000 hr
450˚C	RCC-MR 2002	362	340	316	302	295
	ORNL	392	374	354	342	335
	Japanese data	363	341	319	306	298
	MC [7]	380	361	339	326	319
500˚C	RCC-MR 2002	268	247	225	213	206
	ORNL	286	268	249	238	232
	Japanese data	267	248	228	217	211
	MC [7]	283	262	240	227	219
550˚C	RCC-MR 2002	187	168	149	138	132
	ORNL	197	180	163	152	147
	Japanese data	188	172	155	146	140
	MC [7]	193	173	152	141	134
600˚C	RCC-MR 2002	119	103	87	78	73
	ORNL	124	109	94	85	80
	Japanese data	125	111	96	88	83
	MC [7]	119	103	87	78	74
650˚C	RCC-MR 2002	66	54	42	37	34
	ORNL	68	56	44	38	34
	Japanese data	73	61	47	40	35
	MC [7]	67	56	46	41	38

Table 19 - Minimum Stress to Rupture for Times from 10,000 hours to 300,000 hours

	Predicted Time	10,000 hr	30,000 hr	100,000 hr	200,000 hr	300,000 hr
450˚C	RCC-MR 2002	313	292	270	257	250
	Revised analysis	319	299	276	264	257
	Japanese data	319	298	278	266	258
500˚C	RCC-MR 2002	225	206	186	175	168
	Revised analysis	231	212	192	181	174
	Japanese data	228	211	193	183	177
550˚C	RCC-MR 2002	151	134	116	107	101
	Revised analysis	156	140	122	112	107
	Japanese data	155	140	125	116	112
600˚C	RCC-MR 2002	90	76	63	56	51
	Revised analysis	95	81	67	60	55
	Japanese data	96	83	70	63	59
650˚C	RCC-MR 2002	46	36	28	24	22
	Revised analysis	49	40	31	26	24
	Japanese data	47	35			

Table 20 - Parameters of the Revised Creep Stress to Rupture

	New parameters
C_0	26.6395
a_0	43.20497
a_1	−18.7012
a_2	9.922035
a_3	−2.27989

Table 21 - Application of Revised Material Data to Negligible Creep of Table 5

Temperature	Code Data	S_y Reference Stress (MPa)	Negligible Creep Time (hr)
400˚C	ASME	358	5.35×10^5
	Revised analysis		2.36×10^4 (*)

(*) Taking into account stress relaxation with the revised creep strain law of Section 4.2.3 (and elastic follow-up factor of 1) and assuming a series of cycles with 1-hold time, the negligible times would become 1.08×10^5 hours.

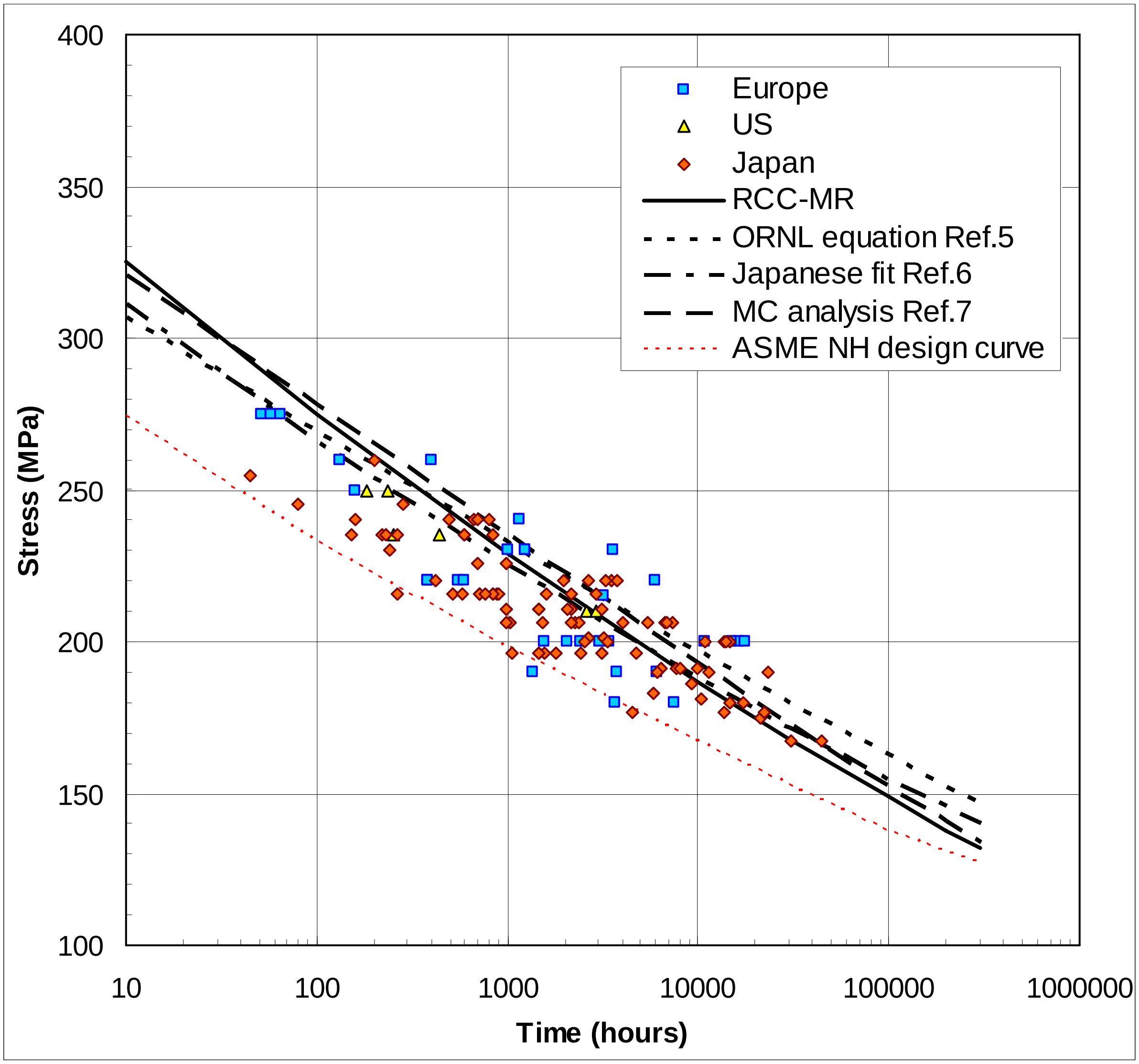


Figure 27 - Average Stress to Rupture and Experimental Data at 550˚C

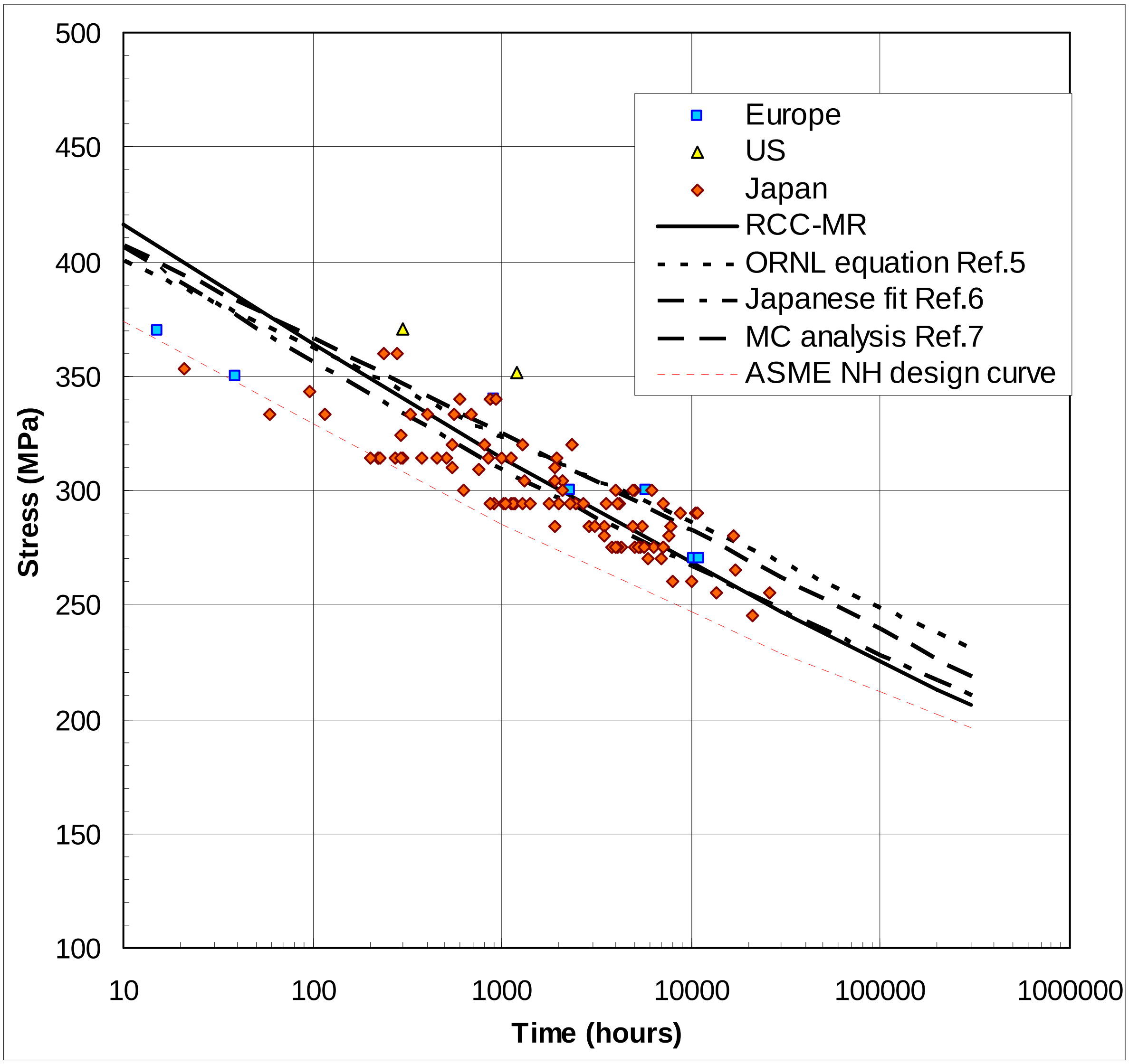


Figure 28 - Average Stress to Rupture and Experimental Data at 500˚C

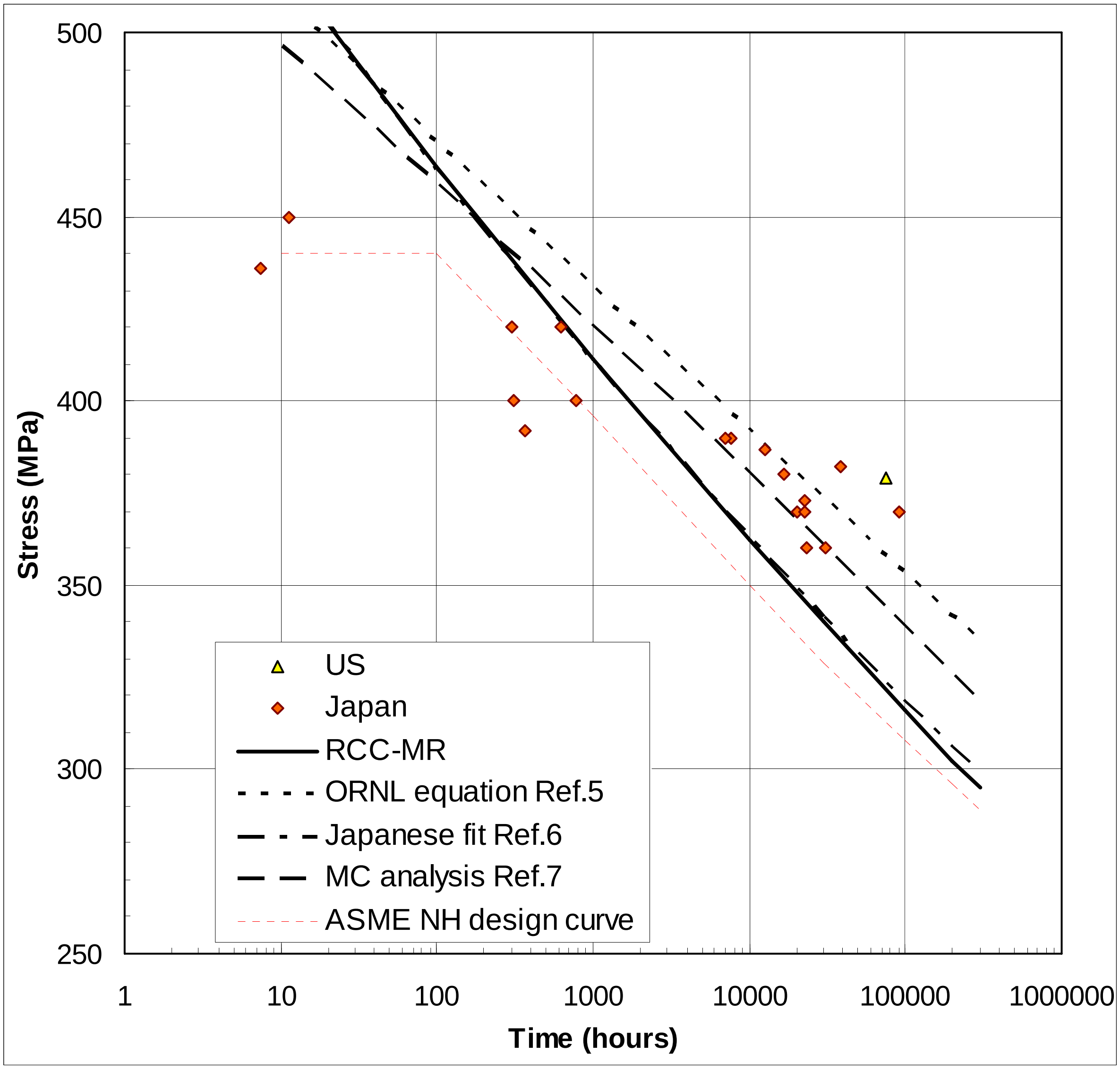


Figure 29 - Average Stress to Rupture and Experimental Data at 450˚C

5 DISCUSSION

On the basis of the previous analysis, it can be concluded that five criteria could be envisioned for Mod. 9Cr-1Mo:

- Time fraction criterion with S_y as a reference stress
- Time fraction criterion with S_y as a reference stress taking into account the stress relaxation
- Relaxation of 1.5 S_m by 20%
- Strain criterion with 1.25 S_y as reference stress and 0.2% creep strain
- Strain criterion with S_y as reference stress and 0.2% creep strain

Figure 30 gives the corresponding negligible creep curve curves and Table 22 gives the associated tabulated values. The creep stress to rupture properties used for the time fraction criterion are those from the ASME code. The creep strain law used for the other criteria is the revised law from section 4.2.3. Table 33 shows that the criterion with 1.25 S_y as reference stress and 0.2% creep strain is too conservative and would not be consistent with the present negligible creep temperature of 700˚C (371˚F) of the ASME code. The criterion with S_y as a reference stress and 0.2% creep strain is more conservative than the criterion based on the relaxation of 1.5S_m by 20%. The curves would be almost superimposed if a stress of 0.93 S_y would be used instead of S_y. The curve based on the relaxation of 1.5 S_m is also very close to that given by S_m as a reference stress and 0.01% creep strain (see Table 11).

An issue to be considered is to what extent the corresponding negligible curves would prevent creep fatigue interaction. Figure 31 plots fatigue and creep fatigue test results (from [12] and [13]) corresponding to a strain range of 0.5% at 500˚C and 550˚C. Fatigue tests are available at different strain rates and Figure 31 plots them artificially at the time which corresponds to the duration of the tests. It can be noticed, however, that the strain rate effect does not seem to be significant at those temperatures. At 500˚C, the dependence of the reduction of fatigue life as a function of the hold time is not obvious. Further tests would be required to confirm this trend. The effect of hold time seems to appear at 550˚C but it is worth mentioning that all of the points in the lower bound correspond to hold time in compression for which the life reduction is more an effect of environment (i.e., air) than an effect of hold time. In any case, all experimental points lie above the fatigue design limits defined by RCC-MR or ASME code for a strain range of 0.5%. Figure 31 also shows the limits of negligible creep for the three first criteria of Table 22. It can be noted that some experimental creep fatigue test results lie below the curve which corresponds to the time fraction criterion with stress relaxation effect. It means that the duration of those tests is below the negligible creep time given by this criterion and, if we consider that those tests have reduced life's due to the effect of hold time, it means that this criterion is not conservative enough and it would be prudent not to retain it.

Finally, it should be mentioned that all those criteria would not necessarily be used with the same temperature definition. The time fraction criterion is aimed at providing prevention against creep-fatigue and the temperature to be considered in this case should be the maximum peak temperature. The other criteria are aimed at providing prevention against ratcheting and the maximum average through wall temperature should be more appropriate.

Table 22 - Negligible Creep Times for Modified 9Cr-1Mo

Temperature (˚C) Criterion	375	400	425	450	475	500	550
Time fraction with S_y (w/o relaxation)		5.35×10^5	27,300	2091	232	32	1.47
Time fraction with S_y w/ relaxation)		2.77×10^6	194,042	28,301	5842	1817	541
Relax 1.5 S_m by 20%		1.67×10^6	53,600	2150	135	9.7	0.11
1.25 S_y / 0.2%	7028	384	29	2.7	0.35		
S_y / 0.2%	5.06×10^6	1.58×10^5	7108	428	33	3.3	

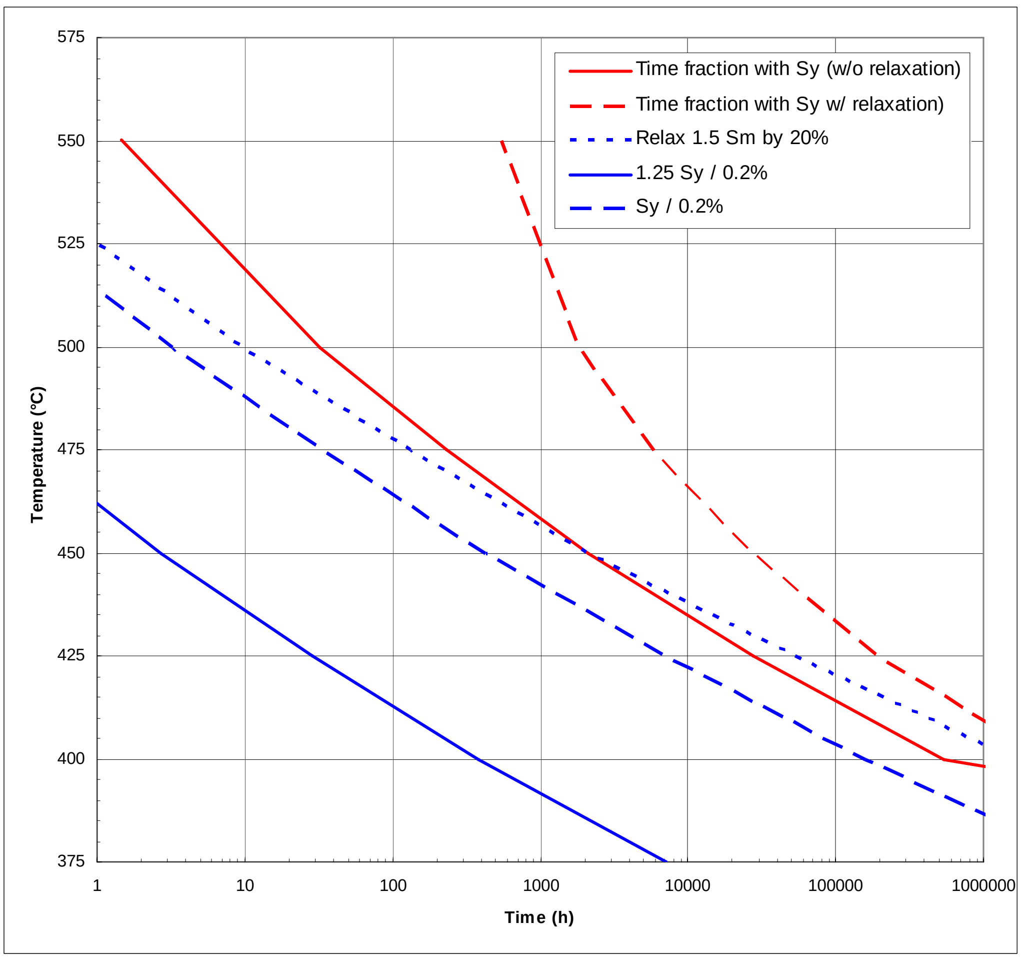
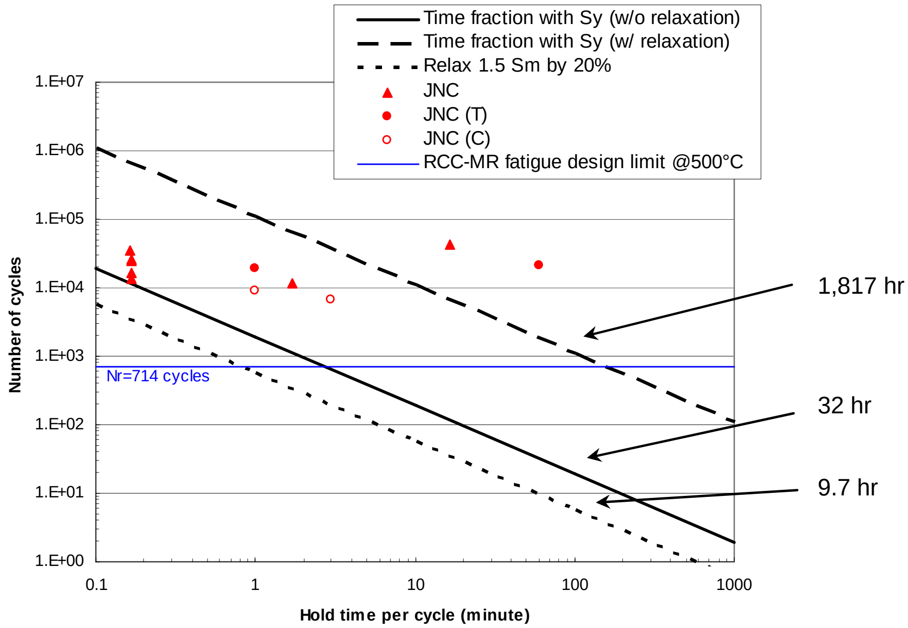
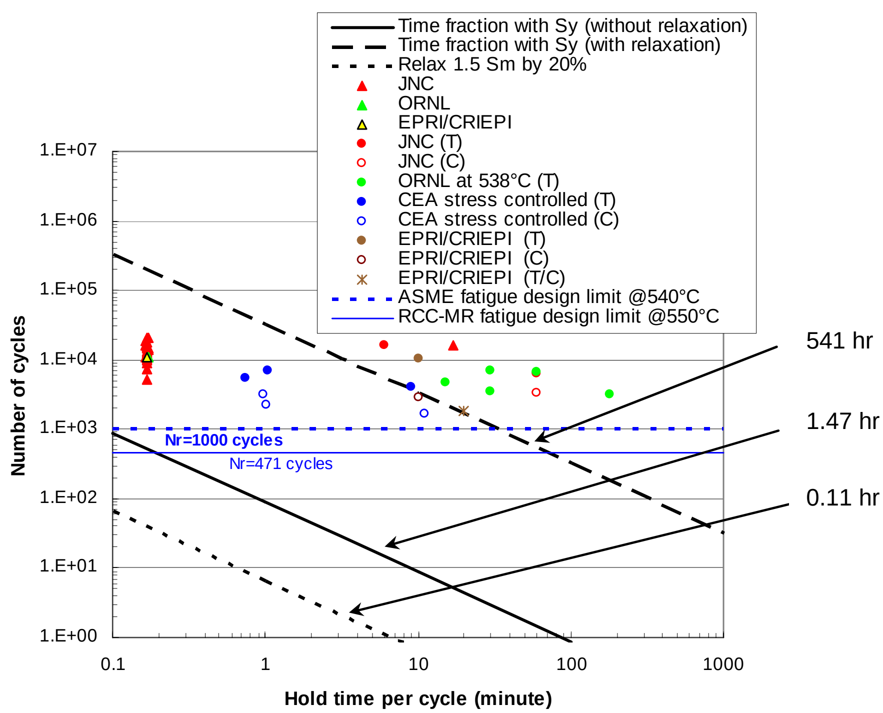


Figure 30 - Negligible Creep Curve for Modified 9Cr-1Mo



(a) 500°C – $\Delta\varepsilon$ = 0.5%



(b) 550°C – $\Delta\varepsilon$ = 0.5%

Figure 31 - Interaction between Negligible Creep and Creep-Fatigue

6 CONCLUSIONS

The conclusions are as follows:

- Negligible creep criteria developed for austenitic stainless steels are not directly applicable to Mod. 9Cr-1Mo and the definition of negligible creep conditions is very dependent on the creep properties taken into account (either creep strain laws or creep stress to rupture).
- A revised creep strain law with a formulation similar to that of RCC-MR code was developed in order to provide more reliable results in the low temperature range (below 500˚C).
- Creep stress to rupture results have been re-analyzed to evaluate if improved design data could be obtained at lower temperatures. The number of available stress to rupture data at temperatures lower than 500˚C is small and the data cover a limited range of stress (for example, 450–360MPa at 450˚C). It was shown that different equations for average values of stress to rupture derived from different databases do not describe the time dependence of stress to rupture at 450˚C correctly. The ASME minimum curves, however, seem to provide a conservative estimate of the creep stress to rupture at 450˚C and below for long term time durations (beyond 10^4 hours).
- From the different criteria investigated, three criteria seem to be applicable to Mod. 9Cr-1Mo:
  - Time fraction criterion with S_y as a reference stress
  - Strain criterion with S_y as reference stress and 0.2% creep strain
  - Relaxation of 1.5 S_m by 20%
- The first two criteria are those from the ASME code but are modified to take account of cyclic softening.
- The time fraction and 0.2% creep strain criteria would allow respectively up to $5.35 x 10^5$ hours and 1.58 $x10^5$ at 400˚C. The criterion based on stress relaxation would provide more favorable negligible creep conditions below 450˚C.
- RCC-MR and Japanese approaches which rely on creep strain criteria in the order of 0.01 to 0.03% are shown to be not applicable to Mod. 9Cr-1Mo. Reference stresses would have to be reduced to about the allowable stress S_m to achieve negligible creep conditions similar to what is given by the three other criteria.
- For further confirmation of the negligible creep limits, more creep strain data at 475˚C, 450˚C and, if possible, 425˚C will be needed. Further tests should also be performed to improve creep stress to rupture curves below 500˚C. The proposed test program is detailed in the companion document of [14].

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PART 2
IMPROVEMENT OF ASME NH FOR GRADE 91 (CREEP-FATIGUE)

1 INTRODUCTION

This report has been prepared in the context of Task 3 of the ASME/DOE Gen IV material project.

It has been identified that creep-fatigue evaluation procedures presently available in ASME (1) and RCC-MR (2) have been mainly developed for austenitic stainless steels and may not be suitable for cyclic softening materials such as Mod 9Cr-1 Mo steel (grade 91).

The aim of this document, starting from experimental test results, is to perform a review of the procedures and, if necessary, provide recommendations for their improvements.

2 MOD 9CR-1MO STEEL

Modified 9Cr-1Mo ferritic steel is considered for application in:

- High Temperature Gas-cooled Reactors (HTGR) components for a normal operating temperature of up to about 500˚C: pressure vessel, cross vessel, core support structures
- Steam generator components of Liquid Metal Fast Breeder Reactors (LMFBR)
- Structural materials at elevated temperatures in fossil fired power plants.

The advantages of this steel are mainly its low thermal expansion coefficient and high thermal conductivity. It has also a higher resistance to stress corrosion cracking in water-steam environment than austenitic steels and should have a better behavior in He environment than conventional PWR ferritic steel (particularly at high temperature).

It has a good weldability and microstructural stability over very long periods of exposure to high temperatures.

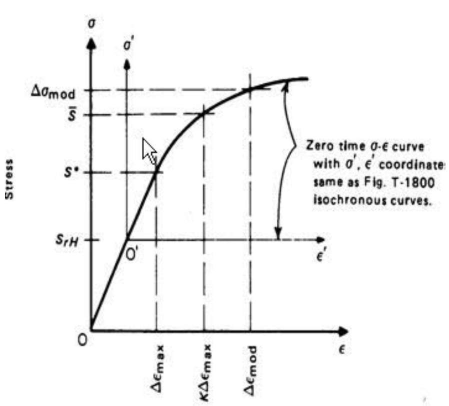
Alloying additions of niobium and vanadium enhance the creep strength of this steel compared to the standard 9Cr-1Mo steel.

3 CREEP-FATIGUE PROCEDURES IN THE NUCLEAR CODES

A short comparative description of ASME and RCC-MR creep fatigue procedures is given in Table 23 and Table 24.

Sections 3.1 and 3.2 explain how these procedures are applied to the analysis of experimental test results on Mod 9Cr-1Mo. Section 3.3 provides a description of the Japanese DDS creep-fatigue design rules.

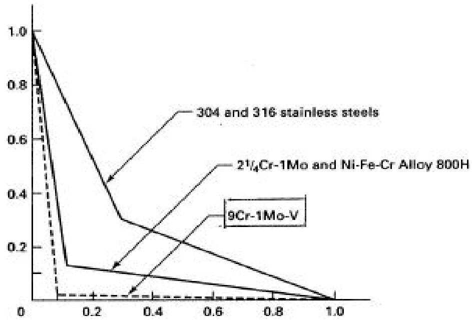
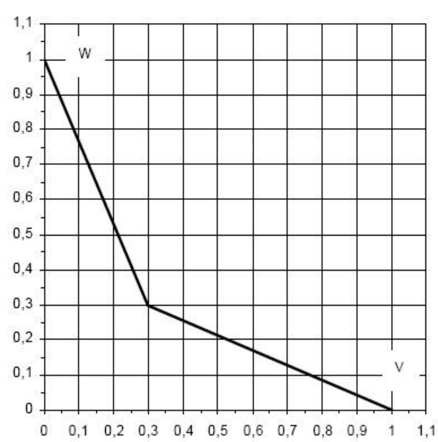
Table 23 - Creep-Fatigue – Calculation of Equivalent Strain Range

<table>
<tr><th>ASME, Section III, Subsection NH – Appendix T</th><th>RCC-MR</th><th>Comments</th></tr>
<tr><td>Strain range determination: T-1432
$\varepsilon_t = K_\nu \Delta\varepsilon_{\text{mod}} + K\Delta\varepsilon_c$</td><td>Equivalent strain range : RB 3261.123, RB 3262.123
$\overline{\Delta\varepsilon} = \overline{\Delta\varepsilon_{el+pl}} + \overline{\Delta\varepsilon_c}$
$\overline{\Delta\varepsilon_{el+pl}} = \overline{\Delta\varepsilon_1} + \overline{\Delta\varepsilon_2} + \overline{\Delta\varepsilon_3} + \overline{\Delta\varepsilon_4}$</td><td></td></tr>
<tr><td>$\Delta\varepsilon_{\text{mod}}$: modified max equivalent strain range
3 methods for calculating $\Delta\varepsilon_{\text{mod}}$:
1) T-1432 (c)
$\Delta\varepsilon_{\text{mod}} = \frac{S^*}{\bar{S}} K^2 \Delta\varepsilon_{\max}$
K: stress concentration factor
S* and $\bar{S}$ read on the composite stress strain curve in fig. T-1432-1 (shown below)
$\Delta\varepsilon_{\max}$: elastically calculated strain range not including stress concentration effects.


2) T-1432 (d)</td><td>$\overline{\Delta\varepsilon_1} = \frac{2}{3}(1+\nu)\left(\frac{\overline{\Delta\sigma_{tot}}}{E}\right)$
E based on $\theta_{\max}$ (maximum temperature).
$\overline{\Delta\varepsilon_2}$ from plastic strain increase due to primary stress range
$\Delta\left[\overline{P_m + 0.67(P_L + P_b - P_m)}\right]$
$\overline{\Delta\varepsilon_3}$ is plastic strain increase derived from formulation by Neuber. Where $\overline{\Delta\varepsilon_2}$ is negligible $\overline{\Delta\varepsilon_1} + \overline{\Delta\varepsilon_3} = K_\varepsilon . \Delta\varepsilon_3$
$\overline{\Delta\varepsilon_4}$ is plastic strain due to triaxiality from $\overline{\Delta\varepsilon_4} = \overline{\Delta\varepsilon_1}(K_\nu - 1)$</td><td>RCC-MR calculates an additional factor to account for plastic strain increase due to primary stress range
In RCC-MR triaxiality is considered as a separate quantity whereas in ASME adjustment is made to Poisson ratio</td></tr>
</table>

ASME, Section III, Subsection NH – Appendix T	RCC-MR	Comments
$\Delta\varepsilon_{mod} = \frac{S^*}{\Delta\sigma_{mod}} K^2 \Delta\varepsilon_{max}$ The unknowns $\Delta\varepsilon_{mod}$ and $\Delta\sigma_{mod}$ are calculated by an iterative method. 3) T-1432 (d) The most conservative method is the following $\Delta\varepsilon_{mod} = K_e K \Delta\varepsilon_{max}$ - if $K\Delta\varepsilon_{max} \leq 3\overline{S}_m/E$ => K_e=1 - if $K\Delta\varepsilon_{max} > 3\overline{S}_m/E$ => K_e= $K\Delta\varepsilon_{max}E/3\overline{S}_m$ $K_v = 1 + f(K'_v - 1)$ f: factor allowing for multiaxiality (fig. T-1432-2) K'_v: plastic Poisson ratio adjustment factor determined by entering fig. T-1432-3 at the ratio of $Ke.K.\Delta\varepsilon_{max}E/3\overline{S_m}$		
$\Delta\varepsilon_c$: creep strain increment due to load controlled stresses. $\Delta\varepsilon_c$ is calculated from isochronous stress-strain curves: ($1.25\sigma_c$; Tmax; t_j) $\sigma_c=Z.S_y$, effective creep stress (core stress) defined in T-1332	$\overline{\Delta\varepsilon_c}$:calculated creep strain allowing for stress relaxation of creep stress σ_k for period of hold time, using elastic follow-up factor Cr σ_k is from "sum" of mean $\overline{P}$ plus $K_S\overline{\Delta\sigma^*}$ "sum" means a simple addition or a combination using reduced cyclic curves. $\overline{\Delta\sigma}^*$ is calculated by entering the cyclic curve at $\overline{\Delta\varepsilon}_{el+pl}$ K_s is a symmetrization factor depending on $\overline{\Delta\sigma}^*$ σ_k is calculated taking account of uniaxial stress relaxation and elastic follow-up factor Cr (Cr=3 is recommended). Relaxation applies only to the term $K_S\overline{\Delta\sigma}^*$ of σ_k. σ_k is also the stress used for creep damage evaluation (see Table 3-2)	ASME considers a creep strain increment due to load controlled stresses during hold time. RCC-MR considers a creep strain increment due to relaxation of calculated stress σ_k during hold time, and σ_k is an elastic assessment of the residual stress during hold time.

Table 24 - Creep-Fatigue Damage

ASME, Section III, Subsection NH – Appendix T	RCC-MR	Comments
Fatigue damage D_f : $$Df = \sum_i \frac{n_i}{Nd_i}$$ n_i is number of strain cycles at θ max Nd_i is allowable number of cycles from design fatigue curve at θ max and strain range ε_{ti}	Fatigue usage $V(\overline{\Delta\varepsilon})$: $$V(\overline{\Delta\varepsilon}) = \sum \frac{n}{N_f}(\overline{\Delta\varepsilon}) \leq 1$$ n is number of strain cycles at θ max N_f is allowable number of cycles from design fatigue curve at θ max and strain range $\overline{\Delta\varepsilon}$	ASME NH and RCC-MR use Miner's rule
Creep damage Dc: (T-1433) $$Dc = \Sigma\left(\frac{\Delta t}{T_d}\right)_j$$ $(T_d)_j$ is calculated from Sj/0.67 At beginning of hold time, stress Sj is calculated by entering the hot tensile of the isochronous stress-strain curves at hold time temperature T_{HT} S_j is calculated taking account of relaxation by -an adjusted uniaxial stress relaxation analysis or -using isochronous stress-strain curves as in figure T-1433-1 (a) Relaxation applies to all of the stress but not less than the lower bound S_{LB} S_{LB}= 1.25σ_c where σ_c=effective creep stress (core stress) defined in T-1332.	Creep damage W (RB 3262.124) $$W = \int_0^r \frac{dt}{T_r(\sigma_k/0.9)}$$ where : σ_k from "sum" of mean $\overline{P}$ plus $K_S\overline{\Delta\sigma^*}$ is the same as in Table 3-1	ASME does not take into account any symmetrization effect.
Creep Fatigue damage :Df+Dc < D Using a Creep Fatigue interaction diagram  	Creep Fatigue damage :V +W < D using a Creep Fatigue interaction diagram  	RCC-MR and ASME use the same linear rule, but different creep/fatigue interaction diagrams: Bi-linear damage lines with intersection (0.3, 0.3) in the case of RCC-MR and (0.1, 0.01) in the case of ASME.

3.1 ASME Procedure

The ASME procedure is provided in nonmandatory Appendix T of Section III, Subsection NH (T-1400).

The procedure consists of first calculating the fatigue and the creep damages separately:

- Fatigue damage:

$$\sum_j \left(\frac{n}{N_d} \right)_j \tag{17}$$

- Creep damage:

$$\sum_k \left(\frac{\Delta t}{T_d} \right)_k \tag{18}$$

where:

n: number of applied repetitions of cycle type j

N_d: allowable number of cycles for cycle type j associated to the total strain range $(\varepsilon_t)_j$

Δt: time interval

T_d: allowable time associated to the stress S_k

In the context of the present document, the fatigue damage will be calculated by entering the fatigue curve with ε_t equal to the actual applied strain range. The best fit evaluation will be performed by replacing the design curve by the best fit curve. The latter has been obtained by shifting the design curve by the usual factors (factor 2 on the strain range and 20 on the number of cycles to failure). The corresponding curves are given in Figure 1. It is noted that in the present version of the ASME code, a single fatigue curve is provided at 1000˚F (540˚C).

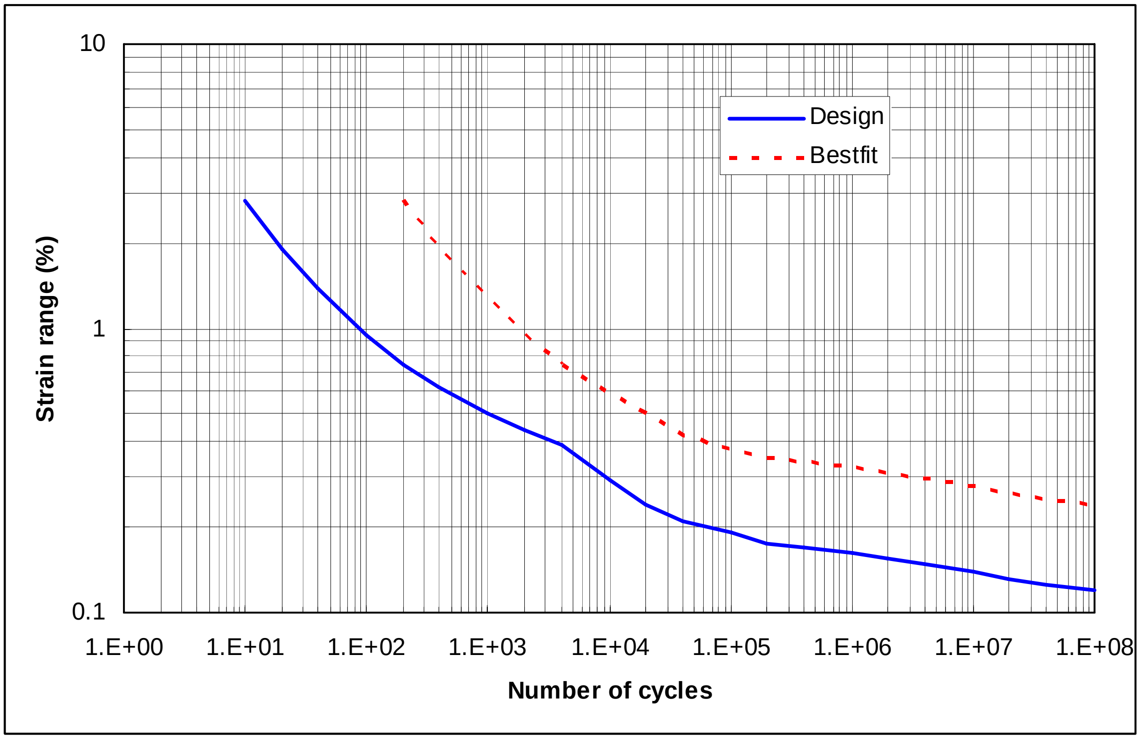


Figure 32 - ASME Fatigue Curves at 540˚C

For the calculation of the creep damage, the stress during the hold time will be calculated by entering the isochronous stress-strain curves with the actual applied strain range. The relaxation of the stress will be accounted for by entering the isochronous stress-strain curves and determining the stress levels at varying times [following figure T-1433-1(a) of (1). In this particular case S_{LB}= 0 (see Table 33)].

It should be noted that it is permitted according to NH T-1433 to calculate the stress relaxation on the basis of an analytical estimate of the uniaxial stress relaxation adjusted with correction factors to account for the retarding effects of multiaxiality and elastic follow-up.

The creep damage will be calculated on the basis of the stress profile during the hold time and using either the design or average creep stress to rupture curves. For the latter, the ASME code does not provide the corresponding curves and the RCC-MR data will be used instead. As far as safety factors are concerned, the design evaluation will be performed after dividing the calculated stress by a factor K'=0.67 and this factor will be set to 1 for best fit evaluation.

The allowable number of cycles will be calculated on the basis of the creep-fatigue damage envelope from figure T-1420-2 of (1) with focal point at (0.1, 0.01).

3.2 RCC-MR Procedure

The RCC-MR procedure is described in Subsection RB 3262.12.

Here again, the procedure is based on separate calculations of fatigue and creep damages.

The fatigue damage will be calculated with the same procedure as that used for the ASME analysis, except that RCC-MR curves will be used.

For the calculation of the creep damage, the stress at the beginning of the hold time is calculated by entering the cyclic curve at a strain range equal to the actual applied strain range and by correcting the associated stress range ($\Delta\sigma^*$) by the symmetrization factor (K_s). The symmetrization factor will depend on the strain range and the temperature. The relaxation of the stress during hold time is calculated by means of the equation of RB 3262.123c. The elastic follow up and triaxiality coefficient C_r will be set to 1 for best fit evaluations (which is more appropriate for uniaxial tests) and to 3 for design evaluations. The design evaluation will be performed after dividing the calculated stress by a factor 0.9 and this factor will be set to 1 for best fit evaluation. Here again, the creep damage will be evaluated using either the design or average creep stress to rupture.

The allowable number of cycles will be calculated on the basis of the creep-fatigue damage envelope with focal point at (0.3, 0.3).

3.3 DDS Procedure

The "Elevated temperature structural design guide for the demonstration fast breeder reactor" (DDS) has been developed in Japan.

Here again, the procedure is based on separate calculations of fatigue and creep damages.

The fatigue damage is calculated using an approach similar to that used for the ASME or RCC-MR analysis.

Creep damage $D_C = D_{CN} + D_{CR}$

$$D_{CN} = 2 \cdot \frac{\Sigma ti}{Td\left(Sg\right)} \tag{19}$$

is the creep damage due to steady stress Sg, and D_{CR} the damage by relaxation of stresses.

$$D_{CR} = D_0^* + S\left(n_k D_k^*\right) \tag{20}$$

D_0* is the damage due to relaxation of residual stress at installation from starting stress 1.5 S_m and D* the damage by relaxation of service stresses.

$$D^* = 2\int_0^{ti} \frac{dt}{Td(\sigma)} - \frac{2ti}{Td(Sg)} \tag{21}$$

The second term of D* is the damage due to steady stress which has already been accounted for in D_{CN}. In the case of strain controlled creep-fatigue tests, Sg = 0.

For design evaluations, the allowable time Td is calculated by entering the design creep stress to rupture at the stress level σ without any safety coefficient, contrary to ASME and RCC-MR, but a factor 2 is applied to the calculated damage.

For the calculation of the creep damage D* due to relaxation, the stress at the beginning of hold time S_i is calculated by entering the cyclic or monotonic stress-strain curve (depending on the material).

If the cyclic stress-strain curve is used (316FR), S_i is calculated by entering the curve for the total strain range and applying a factor equal to 0.5, i.e., S_i=0.5.$\Delta\sigma_R(\Delta\varepsilon)$. A symmetrization factor equal to 0.5 is therefore systematically applied.

If the monotonous (i.e., isochronous) stress-strain curve is used (Mod 9Cr-1Mo), Si is calculated by entering the curve for half the total strain range, i.e., S_i= S(0.5$\Delta\varepsilon$). It is another method to take into account symmetrization.

As in RCC-MR, the allowable number of cycles is calculated on the basis of the creep-fatigue damage envelope with focal point at (0.3, 0.3).

3.4 Comparison Between ASME and RCC-MR Design Data for Creep Fatigue

3.4.1 Fatigue Design Curves

Figure 33 compares ASME and RCC-MR fatigue curves. In both cases, the design curves were derived from best fit curves with the usual factors (factor 2 on the strain range and 20 on the number of cycles to failure). For RCC-MR data, the best fit curves were established as temperature dependent, by multiple regression from the fatigue life data base available in Europe. For ASME data, only those at 538–540˚C were treated to obtain a best fit curve. It can be noted that for strain ranges below 1%, the RCC-MR curves are always more restrictive than those from ASME. The difference, however, is not so significant except beyond 105 cycles for which the ASME curve will predict significantly larger number of cycles to failure. This difference can be important on evaluation of fatigue damage for strain range smaller than 0.2%. The fatigue tests with hold time, which will be discussed later in the document, were performed with higher strain range to keep test durations to reasonable limits.

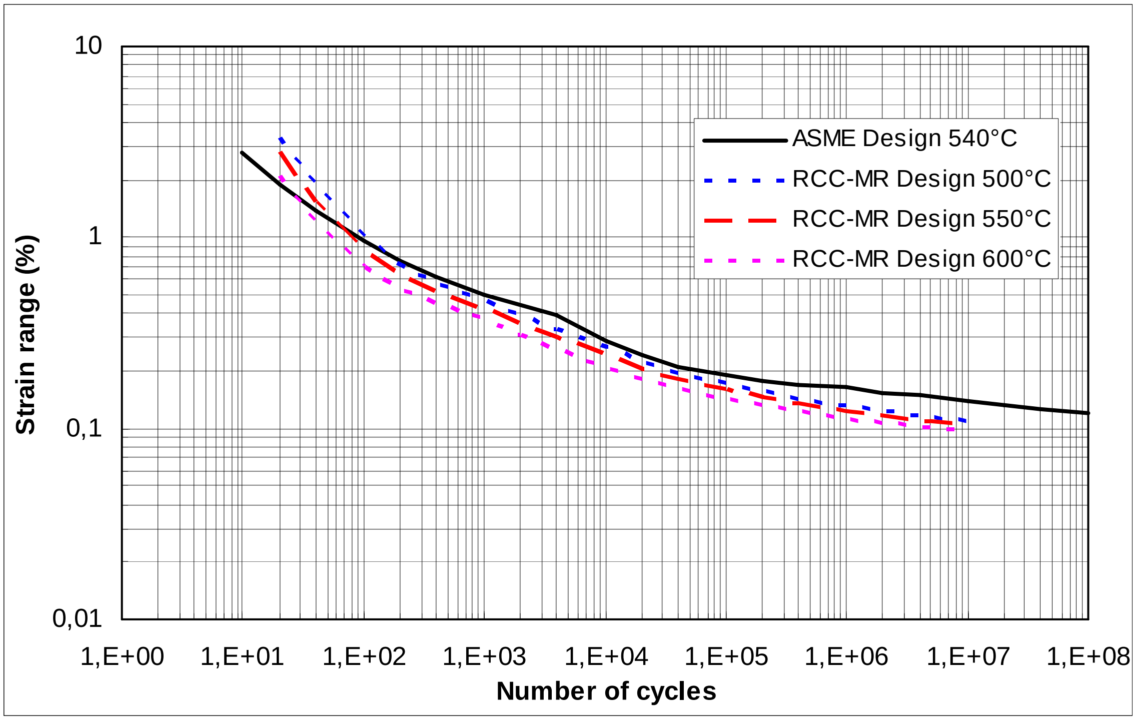


Figure 33 - Comparison of ASME and RCC-MR Fatigue Design Curves

3.4.2 Stress to Rupture Data

Figure 34 compares the RCC-MR average creep stress to rupture and one of the ORNL model [4]. RCC-MR average stress to rupture curves were established from a data base including results of creep tests performed in Europe in the frame of nuclear projects, and, in addition, the ORNL results available from report in [17]. In the case of European Fast Reactor project (EFR) creep tests were performed on a thick (300 mm) rolled and forged sheet. The equations used for the best fit curves are also different. In the case of RCC-MR, the Larson-Miller parameter is a polynomial function of the logarithm of stress, and all parameters, including the Larson-Miller constant, are adjusted by regression. In the equation used by ORNL, the logarithm of time to rupture depends both on a factor in stress and a factor in logarithm of stress. In spite of these differences, the best fit curves are very close together, at least at 500˚C, 550˚C and 600˚C where the difference is smaller than 25 MPa. Of course, more data on more representative materials covering longer times, will result in better assessment of the stress to rupture best fit curves.

Figure 35 compares the design creep stress to rupture (expected minimum values) given in both codes. RCC-MR expected minimum values were derived from best fit curves using the standard deviation of the Larson-Miller parameter. It can be noted that the RCC-MR curves are systematically more conservative than the ASME curves for times exceeding 1000 hours. This remark suggests that an assessment of ASME design data for creep should be made to ensure that the curves are adequately conservative.

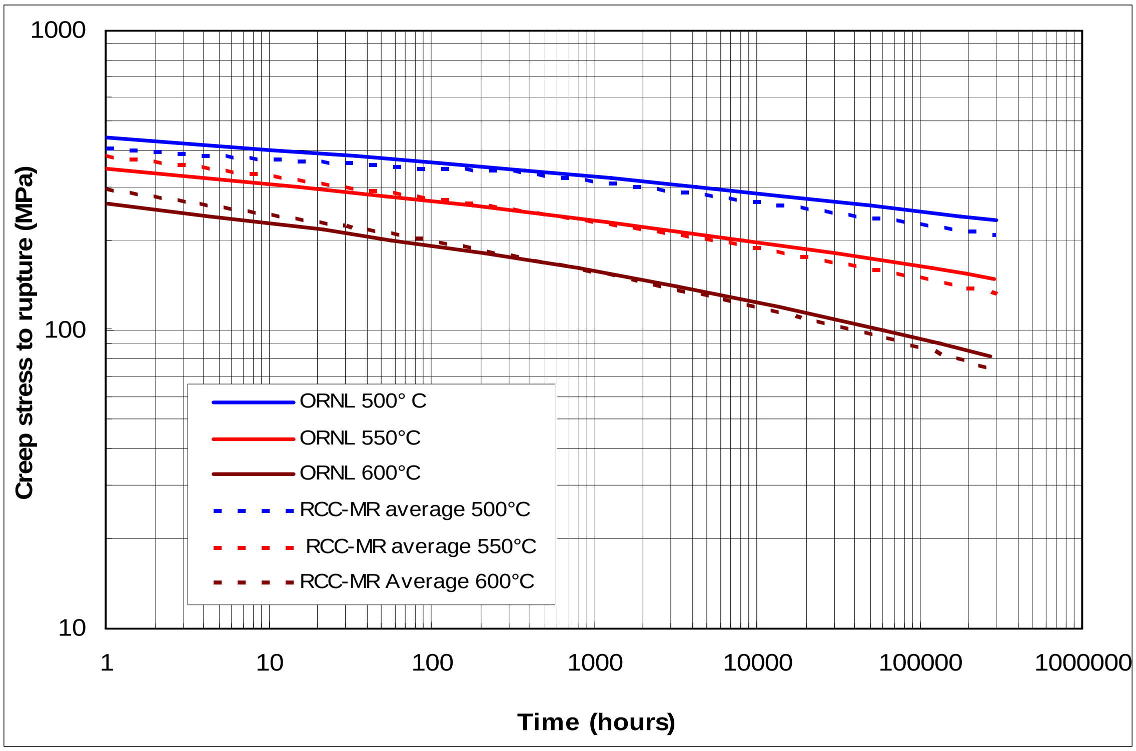


Figure 34 - Comparison of RCC-MR Average and ORNL Stress to Rupture

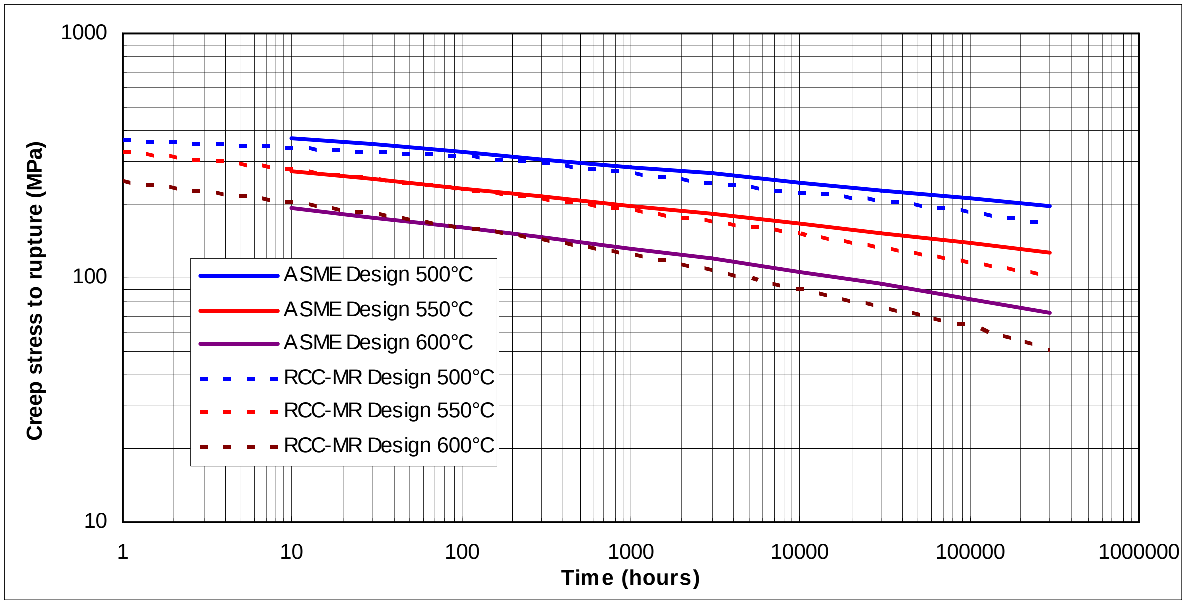


Figure 35 - Comparison of ASME and RCC-MR Stress to Rupture

3.4.3 Creep and Relaxation Behavior

To evaluate the creep damage during a fatigue relaxation cycle, it is necessary to know the stress variation during hold time. The evaluation of stresses at the beginning of hold times are compared in paragraph 6.3 below. This evaluation is performed either on the basis of the hot tensile isochronous stress-strain curve in the ASME case or with the cyclic stress-strain curve in the RCC-MR case.

During hold times, stresses decrease from the initial stress level. When it is decided to take this relaxation effect into account (included in the present RCC-MR case, and can possibly be included in a modified ASME case), it is necessary to calculate the corresponding behavior. This calculation uses a creep strain law in relation with a strain hardening, or a time hardening, hypothesis. The available creep strain laws are compared in [24] from the point of view of expected creep strains at moderate temperatures (≤500˚C). The creep strain laws to predict the relaxation behavior should be a further point of comparison in the frame of prevention of creep-fatigue.

3.5 Comparison between ASME and RCC-MR Procedures

For fatigue damage, the approaches will be the same and the difference will come only from material properties.

For creep damage evaluation, ASME and RCC-MR have different approaches for calculating the stress at the beginning and during the hold period. The RCC-MR takes account of cyclic hardening or softening effects (softening in the case of Modified 9Cr-1Mo) by means of the cyclic stress-strain curve and the benefit of symmetrization effects which are significant for this material. The ASME code neglects these effects and instead relies on an approach based on the isochronous stress-strain curves.

Safety factors on the stress are also significantly different, with a factor of 1.5 in the ASME procedure (K'=0.67) compared with a factor of 1.1 in the RCC-MR code (K=0.9).

Finally, Figure 36 compares the creep-fatigue damage envelopes. It can be noted that the ASME Code is significantly more conservative than the RCC-MR code. It is to be noted, however, that a creep-fatigue damage envelope is dependent upon the definition of the creep and fatigue damages.

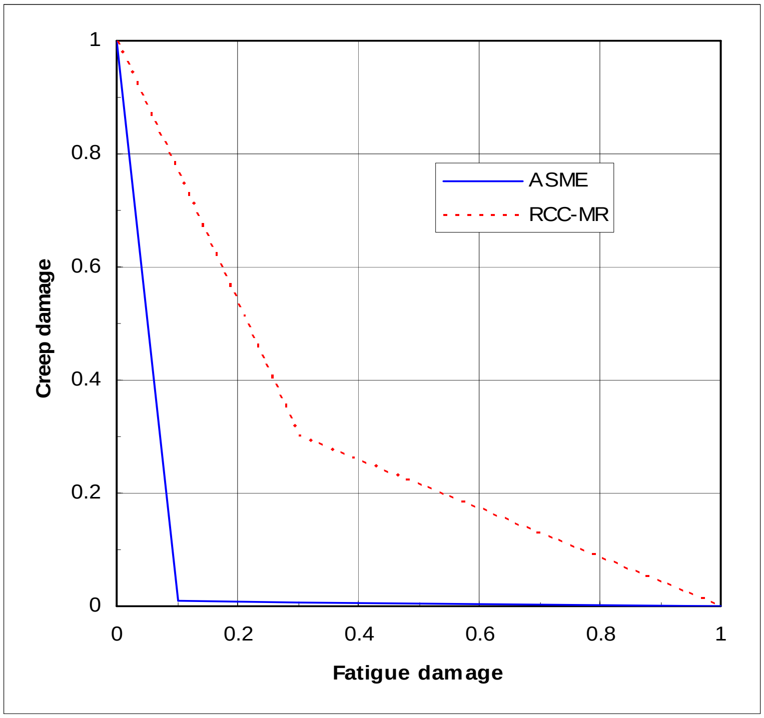


Figure 36 - Comparison of ASME and RCC-MR Creep Fatigue Damage Envelopes

4 PRESENTATION OF THE CREEP-FATIGUE TESTS AVAILABLE ON MOD 9CR-1 MO

The following sources of information have been used for the comparison of the procedures:

- JAPC-USDOE joint Study from [3], [4], [5], [7]
- JNC Study from [8]
- CEA Studies from [9], [10], [11]
- EPRI / CRIEPI joint studies from [12]
- IGCAR studies from [13]
- The University of Connecticut [14].

A total of 103 creep-fatigue tests have been analyzed.

Figure 37 gives a plot of creep fatigue test results of JAPC-USDOE joint study (total strain range vs. number of cycles to failure). All the tests presented are strain controlled. For all tests, axial stress is in tension during hold time. Tests were performed in air and in high vacuum (2 tests) on unaged and aged materials. Table 25 and Table 26 reproduce the tables of test results given in [3] to [7]. These tables also give values of creep damage as provided by these references. Creep damage was assessed on the basis of a representative relaxation curve for a hold period near mid-life. The creep strain law was adjusted using value of stress σ at times t of 0, $0.1t_h$ and t_h , where t_h is the hold time.

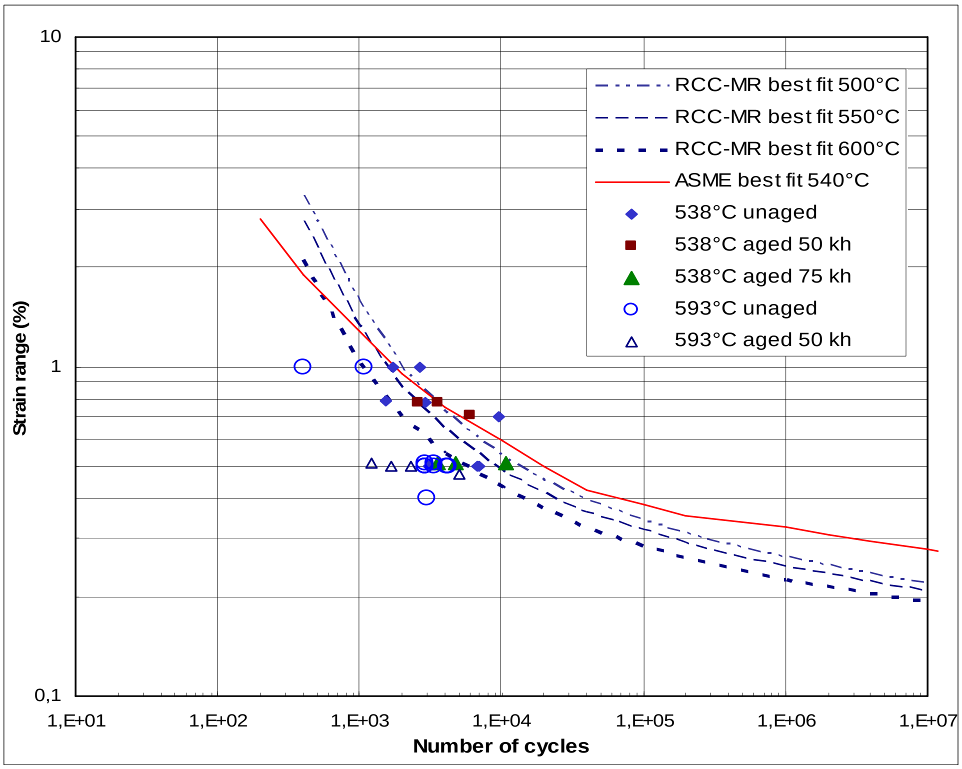


Figure 37 - JAPC-USDOE Joint Study – Fatigue and Creep Fatigue Test Results

Table 25 - ORNL- JAPC- USDOE Joint Study - Creep-Fatigue Tests of Modified 9Cr-1 Mo Steel

Test No.	Heat Treatment	Test Condition	Tempe-rature (˚C)	Total Strain Range (%)	Tensile Hold Time (h)	Compressive Mean Stress at Mid-Life (MPa)	Elastic Strain Range at Mid-Life (%)	Plastic Strain Range at Mid-Life (%)	Cycles to Failure Nr	Creep Damage Dc
1	NT	Air	482	1.00	0.01	17.0	0.41	0.59	3599	4.30E-02
2	NT	Air	593	0.51	1.00	81.0	0.33	0.18	2926	1.33E+00
3	NT	Air	538	1.00	1.00	28.5	0.33	0.67	1734	1.80E-02
4	NT	Air	538	1.00	1.00	26.0	0.44	0.55	2654	2.00E-04
5	NT	Air	593	1.00	1.00	25.0	0.27	0.73	1081	6.80E-02
6	NT	Air	593	1.00	1.00	5.5	0.82	0.18	400	3.00E-03
7	NT	High vacuum	593	0.50	0.50	35.5	0.26	0.24	4150	6.40E-01
8	NT	High vacuum	593	0.50	1.00	38.5	0.23	0.27	2900	1.20E-02
9	NT	Air	593	0.50	0.50	33.5	0.23	0.28	4202	9.90E-03
10	NT	Air	593	0.50	0.50	29.0	0.20	0.30	3360	1.60E-02
11	NT	Air	593	0.50	1.00	25.4	0.18	0.32	2882	5.00E-03
12	NT	Air	538	0.50	0.50	35.0	0.26	0.24	6975	2.70E-02
13	NT	Air	538	0.50	1.00	47.0	0.28	0.22	6787	4.80E-03
14	NT	Air	593	0.40	2.00	27.0	0.23	0.17	2958	9.50E-03

Note: Tests performed on 5 different heats

Table 26 - ORNL- JAPC- USDOE Joint Study - Creep-Fatigue Tests of Modified 9Cr-1 Mo Steel Heat 30394

Test No.	Heat Treatment	Test Condition	Tempe-rature (˚C)	Total Strain Range (%)	Tensile Hold Time (h)	Stress Range at Mid-Life (MPa)	Compressive Mean Stress (MPa)	Elastic Strain Range at Mid-Life (%)	Plastic Strain Range at Mid-Life (%)	Cycles to Failure Nr	Creep Damage Dc
1	NT aged 50kh	Air	593	0.47	0.00	331	0.0	0.20	0.27	5061	…
2a	NT aged 50kh	Air	593	0.50	0.50	310	3.6	0.12	0.38	1652	9.10E-05
2b	NT aged 50kh	Air	593	0.51	0.50	309	9.0	0.14	0.37	1221	3.80E-05
3	NT aged 50kh	Air	593	0.50	1.00	364	17.8	0.13	0.37	2303	2.10E-03
4	NT aged 75kh	Air	538	0.51	0.00	499	0.0	0.33	0.18	10,700	…
5	NT aged 75kh	Air	538	0.51	0.25	438	33.0	0.21	0.29	4791	9.50E-05
6	NT aged 75kh	Air	538	0.51	0.50	373	10.8	0.16	0.35	3535	9.50E-06
7	NT	Air	538	0.50	3.00	323	19.3	0.18	0.33	3113	3.30E-06
13	NT	Air	593	0.51	2.00	405	42.0	0.17	0.34	3352	3.30E-03
14	NT aged 50kh	Air	538	0.71	0.00	438	3.5	0.33	0.38	6060	…
15	NT aged 50kh	Air	538	0.78	0.25	482	23.2	0.26	0.52	3537	3.50E-05
16	NT aged 50kh	Air	538	0.78	0.50	444	16.2	0.24	0.54	2590	2.00E-04
17	NT	Air	538	0.70	0.00	549	1.7	0.43	0.27	9676	…
18	NT	Air	538	0.76	0.25	464	21.4	0.21	0.55	2894	4.40E-05
19	NT	Air	538	0.79	0.50	626	30.0	0.23	0.56	1530	6.80E-03

The JNC tests are strain controlled tests. Figure 39 gives a plot of the JNC test results. Tests have been performed in tension and in compression, and all in the air environment. Tests results are presented in Table 27.

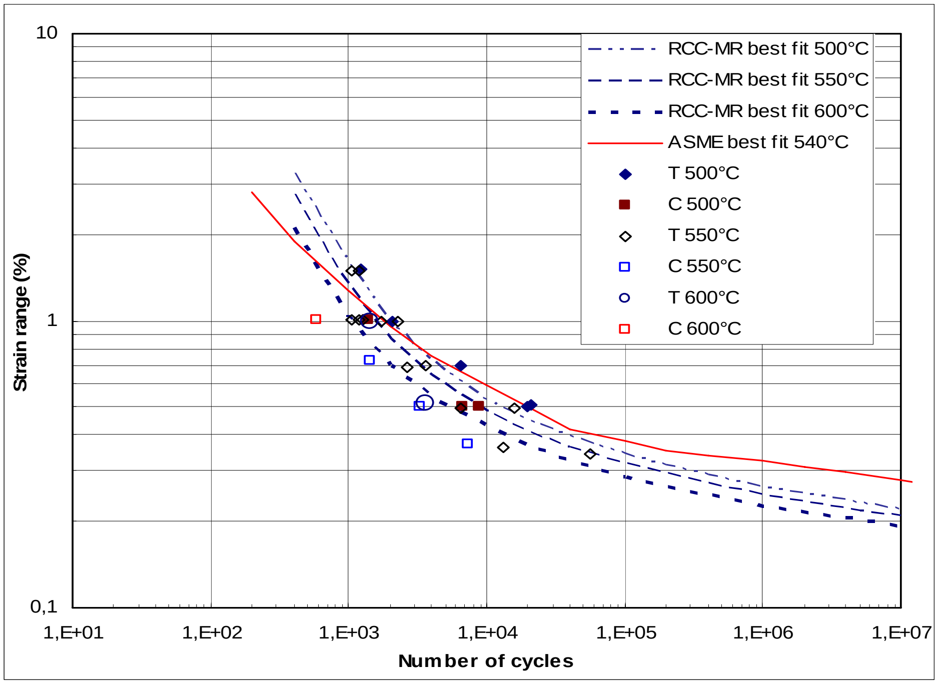


Figure 38 - JNC Study – Fatigue and Creep Fatigue Test Results

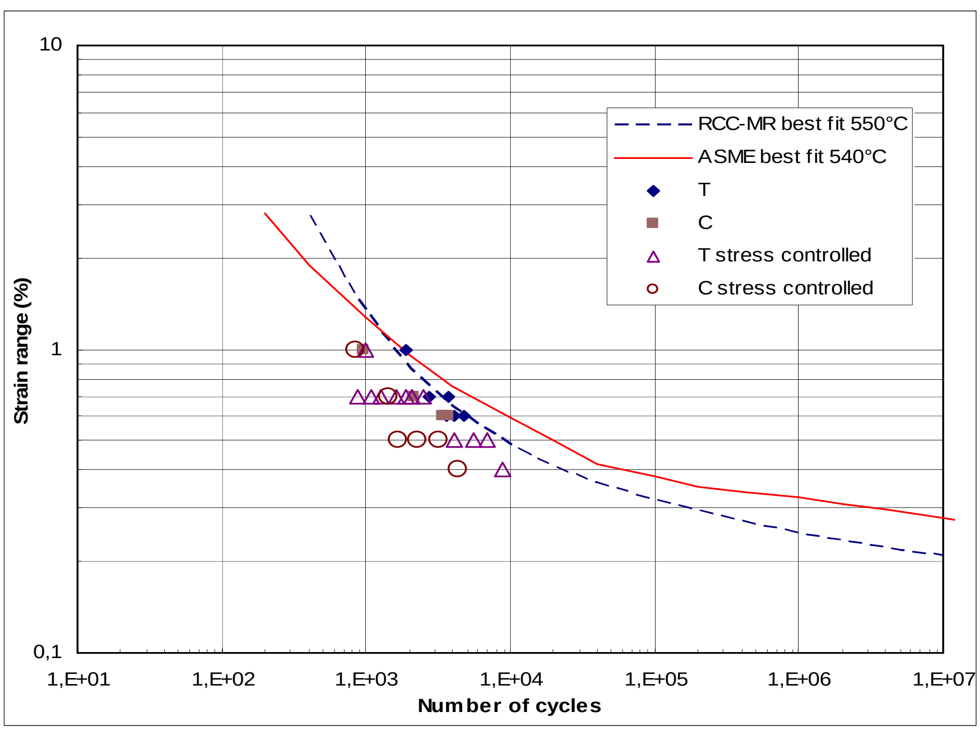


Figure 39 - CEA Studies – Fatigue and Creep Fatigue Test Results

Table 27 - JNC – Testing Result Data of Creep Fatigue Test (Mod 9Cr-1Mo)

Test No.	Heat Treatment	Test Condition	Temperature (˚C)	Total Strain Range (%)	Tensile Hold Time (h)	Compressive Hold Time (h)	Strain Rate (%/s)	Cycles to Failure Nr
9CR85	NT + SR	Air	500	0.5	0.017		0.1	19,468
9CR86	NT + SR	Air	500	0.5		0.017	0.1	8958
9CR92	NT + SR	Air	500	0.5		0.05	0.1	6717
HTH6G3	NT + SR	Air	500	0.507	1		0.1	20,686
9CR83	NT + SR	Air	500	0.7	0.167		0.1	6485
9CR58	NT + SR	Air	500	1	0.167		0.1	2070
9CR91	NT + SR	Air	500	1.01		0.083	0.1	1404
9CR82	NT + SR	Air	500	1.51	0.017		0.1	1232
HTH6E6	NT + SR	Air	550	0.345	0.1		0.001	56,097
HTL9A5	NT + SR	Air	550	0.361	1		0.002	13,012
HTL9A2	NT + SR	Air	550	0.373		1	0.002	7347
HTL9A3	NT + SR	Air	550	0.724		10	0.002	1428
HTH6F5	NT + SR	Air	550	0.494	1		0.1	6453
HTH6D3	NT + SR	Air	550	0.498	0.1		0.1	16,093
HTH6E0	NT + SR	Air	550	0.505		1	0.1	3293
HTH6D8	NT + SR	Air	550	0.692	1		0.1	2623
HTH6C8	NT + SR	Air	550	0.693	0.1		0.1	3568
HTH6D1	NT + SR	Air	550	0.991	0.1		0.1	1749
HTH6D7	NT + SR	Air	550	1.003	1		0.1	1266
HFF961	NT + SR	Air	550	1.5	0.333		0.1	1184
HFF956	NT + SR	Air	550	1.49	0.167		0.1	1065
HFF962	NT + SR	Air	550	1.001	0.167		0.1	1067
HFF964	NT + SR	Air	550	1.001	0.167		0.1	1197
HFF955	NT + SR	Air	550	0.998	0.333		0.1	2290
HTH6F6	NT + SR	Air	600	0.518	1		0.1	3630
HFF965	NT + SR	Air	600	1.001		0.333	0.1	589
HFF963	NT + SR	Air	600	0.996	0.333		0.1	1452

The CEA tests consist of strain hold and stress hold tests. Figure 5 gives a plot of CEA test results. All tests have been performed at 550˚C and in air environment. Hold times were in tension and in compression. Strain hold test results are presented in Table 28 and stress hold test results are presented in Table 29.

Table 28 - CEA – Results of Creep Fatigue Tests at 550˚C

Test No.	Heat Treatment	Test Condition	Tempe-rature (˚C)	Total Strain Range (%)	T / C	Hold Time (h)	Δσ/2 (MPa) at Mid-Life	Max Traction Stress at Mid-Life (MPa)	Mean Stress at Mid-Life (MPa)	Cycles to Failure Nr	Ref.
1 (1241)	NT	Air	550	1	T	0.5	286.7	268.5	-18.2	1920	10
2 (1257)	NT	Air	550	0.7	T	0.167	278	259.8	-18.2	2751	10
3 (1260)	NT	Air	550	0.7	T	0.5	278.2	255.4	-22.8	3778	10
4	NT	Air	550	0.6	T	0.5				4046	9
5 (595)	NT	Air	550	0.6	T	0.5	257.2	233.9	…	4802	9
6 (613)	NT	Air	550	0.6	T	1.5	257	227.5		3560	9
7	NT	Air	550	1	C	2				964	9
8 (1294)	NT	Air	550	0.7	C	0.167	280.1	299.2	19.2	2121	10
9 (603)	NT	Air	550	0.6	C	0.5	251.4	277.5		3362	9
10 (663)	NT	Air	550	0.6	C	1.5	252.2	278.5		3677	9

Table 29 - CEA - Results of Stress Controlled Creep-Fatigue Tests at 550˚C

Test No.	Heat Treatment	Test Condition	Tempe-rature (˚C)	Total Strain Range (%)	T / C	Hold Time (h)	Δσ/2 at Mid-Life (MPa)	Max Traction Stress at Mid-Life (Mpa)	Experimental Creep Strain at Mid-Life (%)	Cycles to Failure Nr
1311	NT	Air	550	0.7	T	0.0083	276.9	241.5	0.18	2533
1310	NT	Air	550	0.7	T	0.0167	278.9	241.3	0.23	1900
1309	NT	Air	550	0.7	T	0.0500	275.7	230.6	0.31	2050
1307	NT	Air	550	0.7	T	0.1000	279.3	233.6	0.39	1094
1277	NT	Air	550	0.7	T	0.1667	277.9	230.3	0.44	1270
1305	NT	Air	550	0.7	T	0.5000	274.7	222.4	0.7	880
1221	NT	Air	550	1	T	0.0063	…	282.4	0.3	999
1332	NT	Air	550	1	C	0.0081	…	293.5	0.3	849
1224	NT	Air	550	0.7	T	0.0528	…	242.5	0.3	1600
1331	NT	Air	550	0.7	C	0.0667	…	252.6	0.3	1415
1325	NT	Air	550	0.5	T	0.1497	…	202.3	0.2	4032
1335	NT	Air	550	0.5	C	0.1858	…	225.7	0.2	1676
1265	NT	Air	550	0.5	T	0.0125	…	222.8	0.1	5550
1268	NT	Air	550	0.5	T	0.0175	…	218.6	0.1	6995
1333	NT	Air	550	0.5	C	0.0169	…	246.7	0.1	2260
1334	NT	Air	550	0.5	C	0.0161	…	243	0.1	3170
1318	NT	Air	550	0.4	T	0.0972	…	186.7	0.1	8836
1341	NT	Air	550	0.4	C	0.0908	…	206.1	0.1	4300

Two series of stress tests have been performed.

- In the first series, the total strain range was equal to 0.7%, the hold time was imposed when the maximum strain value is reached in tension and the time duration is imposed.
- In the second series, different values of the elastoplastic strain range were tested, the duration of the hold time is not fixed, but the value of the creep strain ε_c is imposed. The table gives hold times corresponding to mid-life.

The EPRI/CRIEPI tests were strain controlled tests. Hold times were tensile (T), compressive (C) and tensile-compressive (TC). Tests were performed in the air environment. Table 30 gives the detail of test results and Figure 40 gives a plot of total strain range versus number of cycles to failure for these test results.

Table 30 - EPRI/CRIEPI Joint Studies – Results of Axial Creep Fatigue Tests

Test No.	Heat Treat-ment	Test Con-dition	Tempe-rature (˚C)	Total Strain Range (%)	Tensile Hold Time (h)	Compres-sive Hold Time (h)	Stress Range at Mid-Life (MPa)	Max Stress at Mid-Life (MPa)	Min Stress at Mid-Life (MPa)	Plastic Strain Range (%)	Relaxed Stress at Mid-Life Tension (MPa)	Relaxed Stress at Mid-Life Compres-sion. (MPa)	Cycles to Failure Nr25
CF-1	NTA	Air	550	1	0.1667		571	268	–303	0.786	118		1968
CF-2	NTA	Air	550	1		0.1667	565	290	–275	0.745		137	1006
CF-3	NTA	Air	550	1	0.1667	0.1667	535	263	–272	0.864	118	128	1142
CF-4	NTA	Air	550	1	1		518	241	–277	0.788	113		1885
CF-5	NTA	Air	550	1		1	542	281	–261	0.745		125	956
CF-6	NTA	Air	550	1	1	1	523	257	–266	0.868	139	134	734
CF-7	NTA	Air	550	0.5	0.1667		484	216	–268	0.294	88		10,120
CF-8	NTA	Air	550	0.5		0.1667	490	265	–225	0.297		69	2822
CF-9	NTA	Air	550	0.5	0.1667	0.1667	490	241	–249	0.331	74	83	1871
Fatigue	NTA	Air	550	0.5	0	0	…	…	…	0.32	…	…	10,960
Fatigue	NTA	Air	550	1	0	0	…	…	…	0.786	…	…	3120

The IGCAR tests were strain controlled. Hold times were in tension and in compression. Tests were performed in the air environment. Figure 41 gives a plot of total strain range vs. number of cycles to failure and Table 31 gives the details of test results.

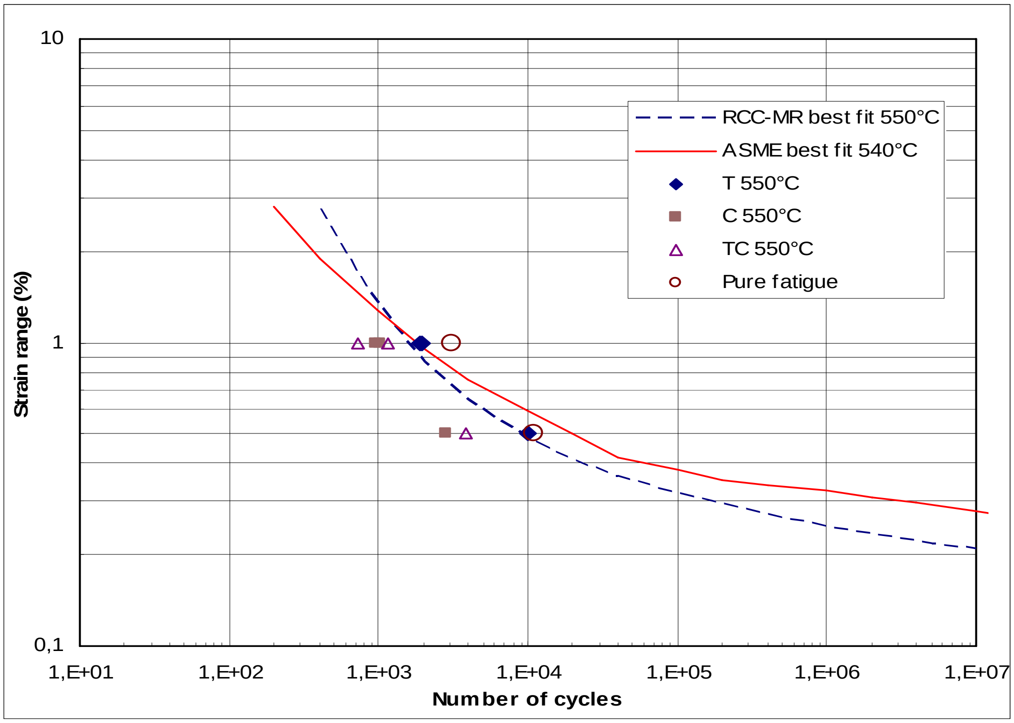


Figure 40 - EPRI/CRIEPI Joint Studies – Fatigue and Creep Fatigue Test Results

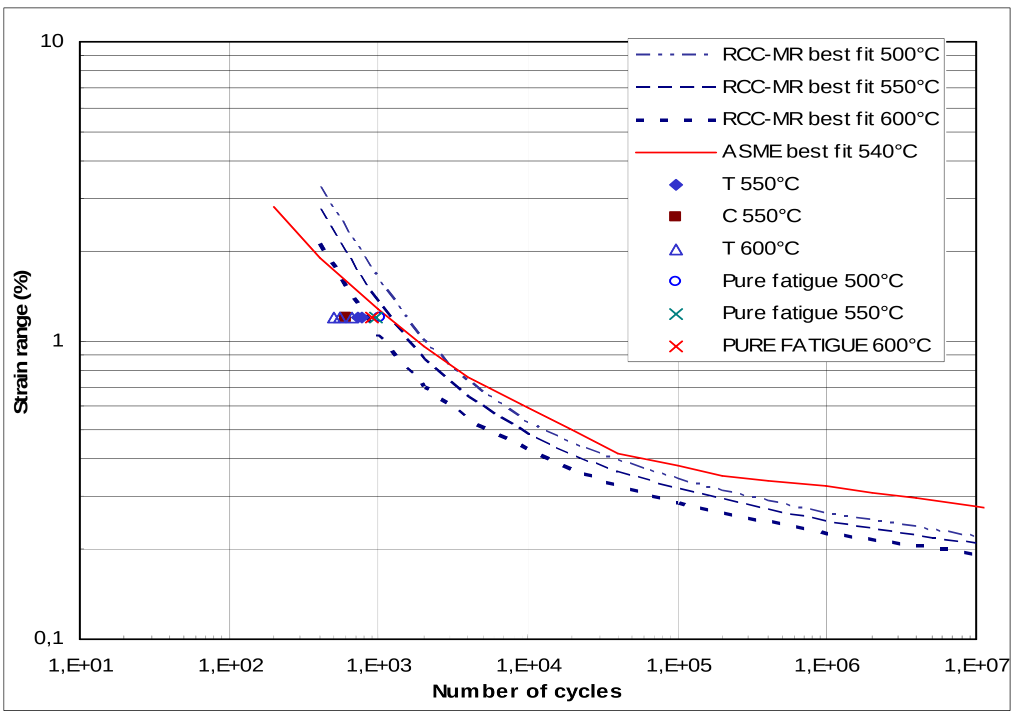


Figure 41 - IGCAR – Fatigue and Creep Fatigue Test Results

In [14], the University of Connecticut presents the research program carried out on the fatigue behavior of various chromium containing ferritic steels at elevated temperature (including Mod 9Cr-1Mo). This report contains, in particular, results of cyclic creep tests. In cyclic creep, the load is periodically removed and immediately reapplied. In the case of Mod. 9Cr-1Mo, the results are given in Table 32.

Table 31 - IGCAR – Results of Creep Fatigue Tests

Test No.	Heat Treatment	Test Condition	Tempe-rature (˚C)	Total Strain Range (%)	Tensile Hold Time (h)	Compressive Hold Time (h)	Δσ/2 at Mid-Life (MPa)	Δσ/2 Mmax at Mid-Life (MPa)	Δεp/2 at Mid-Life (Mpa)	Cycles to Failure Nr
1	NT	Air	550	1.2	0.016667	…	271	376	0.505	771
2	NT	Air	550	1.2	0.16667	…	212	386	0.48	736
3	NT	Air	550	1.2	…	0.016667	292	375	0.48	600
4	NT	Air	600	1.2	0.01667	…	180	331	0.4	657
5	NT	Air	600	1.2	0.16667	…	201	324	0.52	545
6	NT	Air	600	1.2	0.5	…	222	315	0.495	506
7	NT	Air	550	1.2	…	…	265	323	0.438	972
8	NT	Air	600	1.2	…	…	201	296	0.35	890

Table 32 - University of Connecticut – Results of Cyclic Creep Tests

Results of creep tests

Temperature	Creep Load (ksi)	Creep Load (Mpa)	UTS (%)	Creep Life (h)	εf (%)	Reduction of Area (%)
538	40	276	62%	1181	14	84
538	43	296	67%	610	21	83
538	45	310	70%	107	23	86

Results of cyclic creep tests

Temperature	Creep Load (ksi)	Creep Load (MPa)	Length of Cycles	Lifetime (h)	Change in Life	Cycles to Failure Nr
538	45	310	1h	92	down 14%	92
538	45	310	10 min.	181	up 14%	1086
538	45	310	1 min.	609	up 469%	36,540

5 DISTINCTIVE FEATURES OF CREEP FATIGUE OF MOD 9Cr-1 MO

5.1 Effect of Mean Stress

In the case of Modified 9Cr-1Mo, it is observed that hold times in compression produce a slight but detectable mean tensile stress and that tension hold times produce a mean compressive stress. The more damaging effect of compressive hold times was tentatively attributed to this observed difference [22]. It was more recently concluded in [18] that:

- A mean stress of 30 MPa, which is the maximum observed, is not large enough to explain the reduction in life due to compressive hold times,
- No correlation is found between the mean stress and the life reduction ratio of the different tests.

5.2 Effect of Air Environment

The formation of oxide layers on samples for mechanical tests at temperatures exceeding 350˚C is a current observation. The majority of fatigue and creep fatigue tests at elevated temperature were conducted in air and oxidation of the specimen surface took place during creep fatigue tests of different durations. Some features of creep fatigue behavior of Mod 9Cr-1Mo, which differ from the behavior of austenitic stainless steels, were tentatively attributed to the importance of oxidation as these features are not easily explained by creep contribution to initiation and propagation of cracks:

- The comparative damaging effects of tension and compression systematically indicate that compression is more damaging for fatigue life at 550˚C, [11] and [18]. This general feature is more pronounced at low strain amplitude.
- It is difficult to produce intergranular cavities and to promote intergranular crack growth. In most cases transgranular crack propagation with striations were observed.

Differences between the effects of compression and tension hold times were also found in the number of cracks, the branching of cracks and the morphology of the crack tips. Compression hold times produce a larger number of main cracks, a lot of secondary cracks and sharper crack tips. These differences are the origin of a more damaging effect of compression hold times, and in particular, the larger effective deformation at the sharper crack tip [18].

In order to understand the fundamentals of these differences, the behaviors of oxide layer in compression and in tension are under investigation in France in order to check the features of cracks in Modified 9Cr-1Mo. Correlation between cracks in the oxide layer and location of accelerated oxidation spots and possibly of crack initiation points in the base metal for 2.25 Cr-1Mo has been obtained in [21]. The purpose of the French study, where Modified 9Cr-1Mo is cycled in air with compressive hold times, is to determine if a similar environment effect as produced by compressive hold times exists. If such an environmental effect is supported by the data, it should not be taken into account in creep fatigue analysis. It is also clear that in the case of tensile hold times, the reduction of fatigue life should not be quantitatively related to creep damage if there is a dominant environment effect.

When environment effects on fatigue life at elevated temperatures are dominant, the dependence of fatigue on frequency and tensile and compressive strain rates is probably more appropriate to explain the data than creep damage evaluation.

5.3 Creep Fatigue Tests in Vacuum

Continuous fatigue tests and fatigue tests with hold times under vacuum at 593˚C are reported in [7]. The benefit of the vacuum environment in fatigue life is more marked in continuous fatigue tests. With tensile hold times of 1 hour or more, the increase in fatigue life under vacuum is small, if any. Nevertheless, the fatigue life is reduced by tensile hold times when compared to continuous fatigue results in vacuum and in air.

Other tests under vacuum at 600˚C are reported in [15] and [23]. A marked reduction in fatigue life is observed only for unsymmetrical cycles with tension hold time or with longer tension going time than compression going time. The frequency effect is small in the case of symmetrical cycles. Compressive hold time cycles show a slight life reduction from a symmetric continuous cycle, bringing a transgranular type of cracking in the specimen [19].

At 593˚C and 600˚C under vacuum, which eliminates or drastically reduces the oxidation effect, the observed reduction in fatigue life in Mod 9Cr-1Mo from creep-fatigue tests seems to be similar to that found in austenitic stainless steels. In addition, metallographic indications of creep cavities and intergranular path for cracks are observed in the case of fatigue life reducing cycles. At such temperatures, true creep fatigue interaction can probably be studied, after elimination of tests with compressive hold times in air. The analysis of this latter kind of tests should be performed in the frame of clarification of environment effects on fatigue.

At lower temperatures (550˚C and 500˚C) there is a lack of creep fatigue results under vacuum (or under protective environment) to direct the selection of relevant data for analysis of true creep fatigue interaction.

5.4 Cyclic Softening of Mod 9Cr-1Mo

In continuous fatigue, Mod 9Cr-1Mo shows cyclic softening, in contrast to austenitic stainless steels which exhibit cyclic hardening, as illustrated by Figure 42 which compares the cyclic curves ($\Delta\varepsilon/2$ – $\Delta\sigma/2$ for cycle corresponding to half life) at 550˚C and 600˚C to the monotonic tensile curves at the same temperatures. Generally speaking, the cyclic softening causes the destruction of dislocations microstructures which provide creep resistance of Mod 9Cr-1Mo. When hold times are introduced in the cycle (30 and 90 minutes) at 550˚C, the cyclic softening is accelerated in comparison with continuous fatigue cycles as indicated by measured stress amplitude at 20% and 40% of fatigue life. The cyclic stress amplitudes are similar with the hold time being tensile or compressive. Examples of stress amplitudes Salt data at 50% of fatigue life are given in Table 33. At 60% of fatigue life, the additional cyclic softening due to tensile hold time is smaller whereas large decrease in stress amplitude are produced by compressive hold times, indicating an effect of cracks on fatigue resistance of the specimens. In situations free from crack effect, the main softening effect on push-pull behavior occurs at the beginning of the cycling and the effect of tensile or compressive hold time is only moderate.

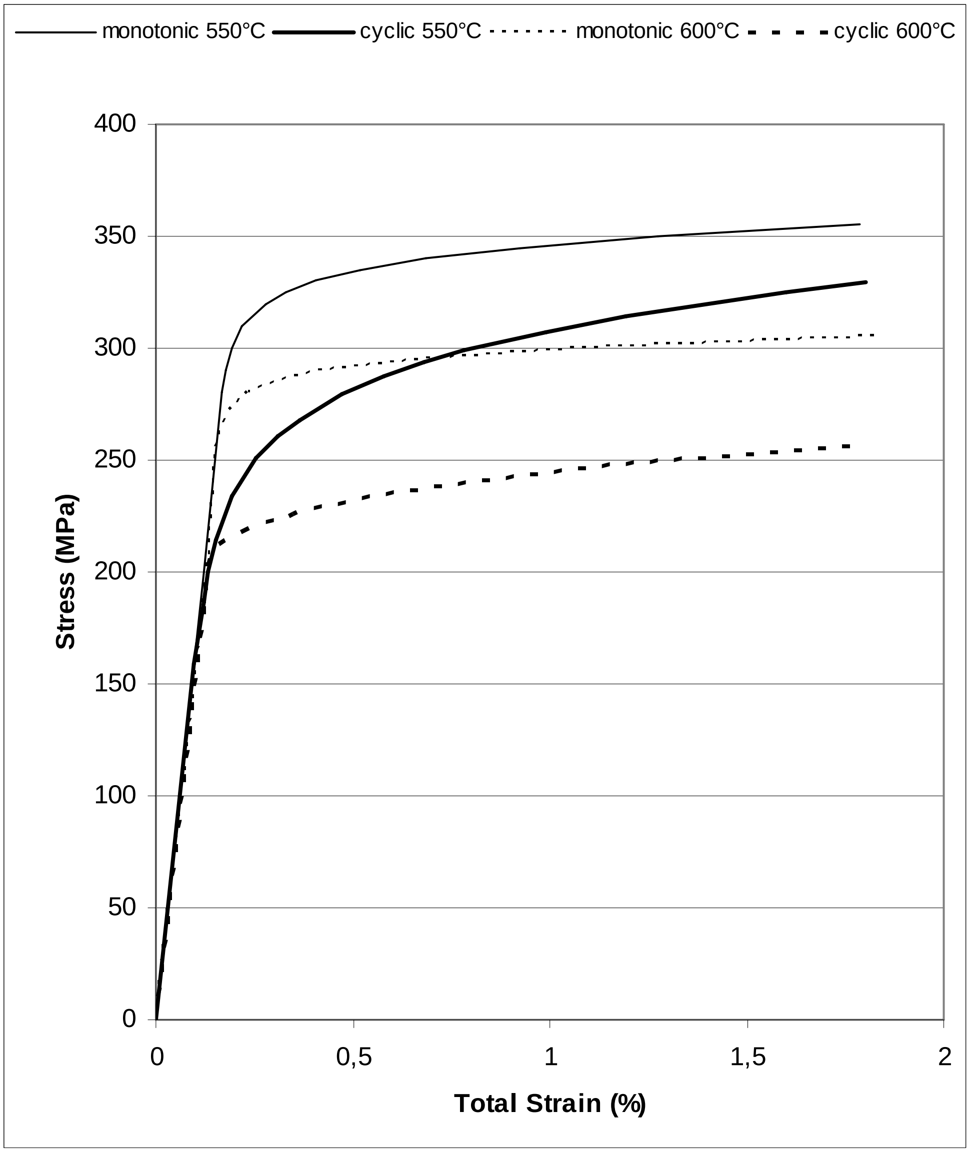


Figure 42 - Cyclic Softening at 550˚C and 600˚C

Table 33 - Stress Amplitudes at Mid Fatigue Life at 550˚C

Strain Range (%)	Hold time (minutes)		Stress Amplitude S_{alt} (MPa)
	Tensile	Compressive	
0.6	0	0	264 (average 3 tests)
0.6	30	0	257
0.6	0	30	251
0.6	90	0	257
0.6	0	90	252

5.5 Effects of Prior Aging

Long term aging (50,000 hours and 75,000 hours at 538˚C and 482˚C) was applied to Mod 9Cr-1Mo samples to evaluate its effect on impact toughness, material microstructures and tensile properties [4]. Complex and various changes in microstructures (carbides M23C6, Laves phases, NbC, VC, recovery and recrystallization zones adjacent to grain/subgrain boundary) result in degradation of impact transition temperature and increase in intergranular cracking tendency. The effect of aging on tensile properties is moderate at 482˚C with, perhaps, a tendency to hardening. Softening is detected after aging at 538˚C and 593˚C (50–60 MPa reduction in yield strength, and 80–90 MPa reduction in tensile strength). At 538˚C, the major part of the aging effect is obtained in 50,000 hours; tests after 75,000 hours do not show significant further evolution. The reduction in the tensile properties after aging is taken into account in ASME Subsection NH in accordance with NH-2160 and Table NH 3225-4 for Mod 9Cr-1Mo. Corresponding provisions are to be introduced in RCC-MR.

The sudden occurrence of cycles of fatigue or creep fatigue after long times at temperatures as high as 538˚C or 593˚C is not a very relevant situation for the design of HTRs. Nevertheless, it is interesting to investigate why such pre-aging affects continuous fatigue, and creep fatigue, life. In continuous fatigue at 538˚C, with 0.7% strain amplitude, the aging effect is not clearly detectable. For 0.5% straining, the results of aged materials is on the lower part of the scatter band of unaged material. After 50,000 hours of aging at 593˚C, continuous fatigue life is clearly lower than the lowest life for unaged material.

Some samples of materials aged at 538˚C and 593˚C were submitted to fatigue and creep fatigue tests, [5] and [7]. At 593˚C and 538˚C, the fatigue life with 0.5 hour tensile hold times is lower for material aged at the test temperature, [4] and [5]. But the effect is not confirmed at 593˚C by test with 1 hour hold time. Qualitatively, it was noted that aging reduces the stress amplitude for a given strain and consequently it decreases the elastic strain amplitude to the benefit of plastic strain amplitude; it is an additional cyclic softening effect.

The softening effect due to aging also reduces creep time to rupture [4]. The data on aged materials are not sufficient to quantify this aging effect in term of stress to rupture which can be used to re-evaluate the creep damage accumulated during creep fatigue tests with hold times. But it is of interest to note that:

- In the case of evaluation of test results any kind of softening can explain the underestimation of creep damages when the reference data are those of non-softened materials; this produces low representative points in the creep fatigue interaction diagram.
- In the case of creep fatigue design works, it is not necessary, in principle, to take account of the stress to rupture after aging if the creep fatigue interaction diagram is defined based on tests including this material condition. It should be ensured, however, that procedures used in design assessment and interpretation of experimental test results are consistent in terms of how aging is taken into account.

In contrast to design for normal service conditions, for which aging is already taken into account, in the long term creep stress to rupture, emergency and faulted situations may require the use of short term creep stress to rupture (presumably short term creep data of maximum 1000 hours). For such situations, smaller values due to reduction by aging (or softening in general) are to be considered as it is the case for tensile strength.

5.6 Effect of Cyclic Softening on Creep Damage in Creep Fatigue Tests

The effect of cyclic softening on creep resistance is a possible creep fatigue interaction effect which can explain the effect of tensile hold time on fatigue life when no clear metallographic indications of creep damage (creep cavities, intergranular creep damage) are detected. This is the case in Mod 9Cr-

1Mo at 550˚C, in contrast to clear metallographic observations of creep damage at 593˚C and 600˚C, [20]. In this situation (550˚C and below), the reduction in fatigue life cannot be correlated with creep damage. Instead of creep damage, a parameter describing cyclic softening could be considered in order to explain the scatter of fatigue life experimental data (cast to cast variation, heat treatment variation, slight change in loading in test starting, etc.).

5.7 Results of Cyclic Creep Tests

Cyclic creep tests or creep tests with periodic unloading are reported in [14]. In this type of test, there is an unloading but no applied plastic strain in compression and such tests are probably not relevant to creep fatigue interaction. In fact, the results summarized in Table 32 cannot be understood in the frame of creep fatigue damage analyses. The creep life is increased when the time between unloading is decreased from 1 hour to 1 minute, or equivalently, when the number of cycles is increased for a given test duration. The creep strain rate is also decreasing. The cycling (fatigue) appears to be beneficial to the creep life and to reduce the creep damage. The explanation must be found in the detail of mechanisms producing creep strain and this effect observed for one stress level in the case of Mod. 9Cr-1Mo is not a general rule. The effect of periodic unloading on oxidation behavior was not studied. Nevertheless, one conclusion which can be pointed out is that a reduction of creep life due to cyclic loading (or increase of creep strain rate) is not observed when cycles do not have a compression phase.

6 EVALUATION OF EXISTING PROCEDURES

6.1 Evaluation of ASME Procedure

The ASME creep-fatigue analysis is carried out as described in Section 3. Table 34 details, for one given case, the results obtained following the design and best fit approaches.

Table 34 - ASME Creep Fatigue Evaluation

Temperature (˚C)	550	
Hold time (hours)	1 (in tension)	
Total strain range (%)	0.361	
Number of cycles to failure Nr	13,012	
	Design	Best fit
Allowable number of cycles in pure fatigue	5080	159,124
Fatigue damage (per cycle)	1.97×10^{-4}	6.28×10^{-6}
S_j at the beginning of hold time (MPa / *ksi*)	320 / *46.5*	320 / *46.5*
S_r at the end of hold time(MPa / *ksi*)	242 / *35.1*	242 / *35.1*
S_j / K' (MPa / *ksi*)	478 / *69.4*	320 / *46.5*
Allowable time for S_j / K' (hours)	Evaluation not possible	12.3
Creep damage (per cycle)		4.31×10^{-3}
Calculated number of cycles to failure, Nc		228
Margin vs test (Nr / Nc)		57.1
Creep damage for Nr cycles		56.1
Fatigue damage for Nr cycles		0.08

Table 35 to Table 40 give the results obtained by using the ASME procedure for all the tests listed in section 4. These tables give the calculated number of cycles for the following cases: design evaluation, best fit evaluation and evaluation with best fit pure fatigue assumption. The corresponding margins defined as the ratio between the experimental and the calculated numbers of cycles are provided. The values of the creep and fatigue damages corresponding to the experimental number of cycles to failure are also provided (best fit evaluation only).

The following conclusions can be drawn:

- The ASME design evaluation procedure cannot be executed. The method to calculate the stress at the beginning of the hold time is too conservative and the use of a safety factor of 1.5 (K'=0.67) gives stresses in all cases that are higher than the creep stress to rupture for the lowest time provided.
- With a best fit evaluation, the results are very conservative when hold times are non zero, with the lower margins corresponding to the shorter hold times. For some tests carried out at 500˚C, the evaluation is again not possible because the calculated stresses at the beginning of the hold time are larger than the tabulated creep stress to rupture.

Table 35 - ORNL-JAPC-USDOE Joint Study – ASME Evaluation

Test No.	Temp (˚C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
3 Tab 1	538	1	T	1	1734	21	81.2			1775	1.0	71.48	0.98
4 Tab 1	538	1	T	1	2654	21	124.2			1775	1.5	109.4	1.50
12 Tab 1	538	0.5	T	0.5	6975	166	42.1			20,000	0.3	38.60	0.35
13 Tab 1	538	1	T	0.5	6787	114	59.5			20,000	0.3	56.19	0.34
4 tab 2	538	0	T	0.51	10,700	18,483	0.6			18,483	0.6	0.00	0.58
5 tab 2	538	0.25	T	0.51	4791	227	21.2			18,483	0.3	18.58	0.26
6 tab 2	538	0.5	T	0.51	3535	157	22.5	Evaluation not possible		18,483	0.2	20.64	0.19
7 tab 2	538	3	T	0.5	3113	62	50.2			20,000	0.2	48.70	0.16
14 tab 2	538	0	T	0.71	6060	4969	1.2			4969	1.2	0.00	1.22
15 tab 2	538	0.25	T	0.78	3537	73	48.6			3565	1.0	38.79	0.99
16 tab 2	538	0.5	T	0.78	2590	52	49.4			3565	0.7	42.21	0.73
17 tab 2	538	0	T	0.7	9676	5256	1.8			5256	1.8	0.00	1.84
18 tab 2	538	0.25	T	0.78	2894	78	37.3			3848	0.8	31.74	0.81
19 tab 2	538	0.5	T	0.79	1530	51	30.0			3435	0.4	25.62	0.45
2 tab 1	593	1	T	0.51	2926	44	66.7			18,483	0.2	65.10	0.16
5 tab 1	593	1	T	1	1081	12	92.4			1775	0.6	86.38	0.61
6 tab 1	593	1	T	1	400	12	34.2			1775	0.2	31.96	0.23
7 tab 1	593	0.5	T	0.5	4150	67	62.3			20,000	0.2	60.23	0.21
8 tab 1	593	1	T	0.5	2900	46	63.2			20,000	0.1	61.80	0.15
9 tab 1	593	0.5	T	0.5	4202	67	63.1			20,000	0.2	60.99	0.21
10 tab 1	593	0.5	T	0.5	3360	67	50.4	Evaluation not possible		20,000	0.2	48.77	0.17
11 tab 1	593	1	T	0.5	2882	46	62.8			20,000	0.1	61.42	0.14
14 tab 1	593	2	T	0.4	2958	53	55.4			62,525	0.0	54.89	0.05
1 tab 2	593	0	T	0.47	5061	25,578	0.2			25,578	0.2	0.00	0.20
2a tab 2	593	0.5	T	0.5	1652	67	24.8			20,000	0.1	23.98	0.08
2b tab 2	593	0.5	T	0.51	1221	64	19.2			18,483	0.1	18.52	0.07
3 tab 2	593	1	T	0.5	2303	46	50.2			20,000	0.1	49.08	0.12
13 tab 2	593	2	T	0.51	3352	31	109.7			18,483	0.2	107.9	0.18

13 Tab 2 : **unaged** *14 Tab 2* **: aged 50 kh** *4 Tab 2* : **aged 75 kh**

Table 36 - JNC – ASME Evaluation

Test No.	Temp (°C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
1	500	0.02	T	0.50	19,468	124.91	155.9			20,000	0.97	146.22	0.97
2	500	0.02	C	0.50	8958	124.91	71.7			20,000	0.45	67.28	0.45
3	500	0.05	C	0.50	6717	45.50	147.6			20,000	0.34	144.32	0.34
4	500	1.00	T	0.51	20,686	3	7580.2			18,922	1.09	7569.4	1.09
5	500	0.17	T	0.70	6485			Evaluation not possible		5256	1.23		1.23
6	500	0.17	T	1.00	2070					1775	1.17	Eval	1.17
7	500	0.83	C	1.01	1404	Evaluation not possible				1735	0.81	Not	0.81
8	500	0.02	T	1.51	1232					682	1.81	Possible	1.81
9	550	0.1	T	0.345	56,097	1028	54.6			298,786	0.2	52.69	0.19
10	550	1	T	0.361	13,012	228	56.9			159,124	0.1	56.14	0.08
11	550	1	C	0.373	7347	207	35.5			118,338	0.1	34.91	0.06
12	550	10	C	0.724	1428	12	123.7			4599	0.3	120.60	0.31
13	550	1	T	0.494	6453	94	69.0			20,983	0.3	65.96	0.31
14	550	0.1	T	0.498	16,093	317	50.7			20,321	0.8	42.89	0.79
15	550	1	C	0.505	3293	88	37.2			19,223	0.2	35.55	0.17
16	550	1	T	0.692	2623	41	63.3	Evaluation not possible		5501	0.5	58.53	0.48
17	550	0.1	T	0.693	3568	132	27.0			5469	0.7	20.52	0.65
18	550	0.1	T	0.991	1749	59	29.9			1813	1.0	20.34	0.96
10	550	1	T	1.003	1266	19	66.6			1763	0.7	59.52	0.72
20	550	0.333	T	1.5	1184	16	75.5			692	1.7	58.60	1.71
21	550	0.167	T	1.49	1065	22	48.7			703	1.5	33.67	1.51
22	550	0.167	T	1.001	1067	45	23.5			1771	0.6	17.50	0.60
23	550	0.167	T	1.001	1197	45	26.3			1771	0.7	19.63	0.68
24	550	0.333	T	0.998	2290	33	69.6			1784	1.3	56.89	1.28
25	600	1	T	0.518	3630	40	90.6			17,371	0.2	88.56	0.21
26	600	0.333	C	1.001	589	20	29.2	Evaluation not possible		1771	0.3	25.95	0.33
27	600	0.333	T	0.996	1452	20	71.5			1792	0.8	63.48	0.81

Note that at 500˚C, best fit evaluation may give a lower allowable number of cycles than at 538˚C for comparable strain range and hold time (Table 35). This anomaly does not occur in the RCC-MR procedure (see Table 42 and Table 43).

Table 37 - CEA Creep Fatigue Tests – ASME Evaluation

Test No.	Temp (˚C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
1	550	0.5	T	1	1920	27	71.4			1775	1.1	60.69	1.08
2	550	0.16667	T	0.7	2751	101	27.4			5256	0.5	22.17	0.52
3	550	0.5	T	0.7	3778	58	65.5			5256	0.7	58.38	0.72
4	550	0.5	T	0.6	4046	83	48.6			9674	0.4	44.42	0.42
5	550	0.5	T	0.6	4802	83	57.6			9674	0.5	52.72	0.50
6	550	1.5	T	0.6	3560	47	76.2	Evaluation not possible		9674	0.4	72.55	0.37
7	550	2	C	1	964	14	70.9			1775	0.5	65.53	0.54
8	550	0.16667	C	0.7	2121	101	21.1			5256	0.4	17.09	0.40
9	550	0.5	C	0.6	3362	83	40.4			9674	0.3	36.91	0.35
10	550	1.5	C	0.6	3677	47	78.7			9674	0.4	74.93	0.38

Table 38 - Stress Controlled Creep-Fatigue Tests – ASME Evaluation

Test No.	Temp (˚C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
1311	550	0.00833	T	0.7	2533	78	32.3			941	2.7	5.70	2.69
1310	550	0.01667	T	0.7	1900	54	34.9			714	2.7	8.56	2.66
1309	550	0.05	T	0.7	2050	27	75.5			425	4.8	27.70	4.83
1307	550	0.1	T	0.7	1094	17	64.4	Evaluation not possible		311	3.5	29.57	3.52
1277	550	0.16667	T	0.7	1270	12	109.1			242	5.2	57.21	5.24
1305	550	0.5	T	0.7	880	Eval. not possible				Eval. not possible		118.92	#N/A
1221	550	0.00625	T	1	999	50	20.2			552	1.8	2.23	1.81
1332	550	0.00805	C	1	849	45	19.0			508	1.7	2.44	1.67
1224	550	0.05278	T	0.7	1600	26	61.2			413	3.9	22.82	3.88
1331	550	0.06667	C	0.7	1415	22	63.2			372	3.8	25.50	3.81
1325	550	0.14972	T	0.5	4032	21	192.2			453	8.9	104.08	8.90
1335	550	0.18583	C	0.5	1676	18	95.7	Evaluation not possible		395	4.2	53.70	4.24
1265	550	0.01250	T	0.5	5550	122	45.6			1632	3.4	11.96	3.40
1268	550	0.01750	T	0.5	6995	100	70.1			1414	4.9	21.11	4.95
1333	550	0.01694	C	0.5	2260	102	22.2			1435	1.6	6.60	1.58
1334	550	0.01611	C	0.5	3170	105	30.2			1466	2.2	8.81	2.16
1318	550	0.09722	T	0.4	8836	51	172.4			1093	8.1	92.37	8.08
1341	550	0.09083	C	0.4	4300	54	79.5			1136	3.8	42.00	3.79

Strain range = elastic + plastic strain range. Calculations are made from the elastic + plastic + creep strain range

Table 39 EPRI/ CRIEPI Joint Studies – ASME Evaluation

Test No.	Temp (°C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
CF-1	550	0.17	T	1.00	1968	46	43.2	Evaluation not possible		1775	1.1	32.19	1.11
CF-2	550	0.17	C	1.00	1006	46	22.1			1775	0.6	16.46	0.57
CF-3	550	0.17	TC	1.00	1142	26	43.7			1775	0.6	37.36	0.64
CF-4	550	1.00	T	1.00	1885	19	98.5			1775	1.1	88.02	1.06
CF-5	550	1.00	C	1.00	956	19	50.0			1775	0.5	44.64	0.54
CF-6	550	1.00	TC	1.00	734	10	72.6			1775	0.4	68.54	0.41
CF-7	550	0.17	T	0.50	10,120	239	42.4			20,000	0.5	37.41	0.51
CF-8	550	0.17	C	0.50	2822	239	11.8			20,000	0.1	10.43	0.14
CF-9	550	0.17	TC	0.50	3871	127	30.5			20,000	0.2	28.62	0.19
	550	0.00		0.50	10,960	20,000	0.5			20,000	0.5	0.00	0.55
	550	0.00		1.00	3120	1775	1.8			1775	1.8	0.00	1.76

Table 40 IGCAR Study – ASME Evaluation

Temp (°C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
550	0.01667	T	1.2	771	78	9.9	Evaluation not possible		1162	0.7	3.29	0.66
550	0.16667	T	1.2	736	32	22.8			1162	0.6	16.53	0.63
550	0.01667	C	1.2	600	78	7.7			1162	0.5	2.56	0.52
600	0.01667	T	1.2	657	68	9.6			1162	0.6	4.03	0.57
600	0.16667	T	1.2	543	22	24.8			1162	0.5	20.15	0.47
600	0.50000	T	1.2	506	12	40.8			1162	0.4	36.50	0.44
500	0.00		1.2	1030	1162	0.9			1162	0.9	0.00	0.89
550	0.00		1.2	972	1162	0.8			1162	0.8	0.00	0.84
600	0.00		1.2	890	1162	0.8			1162	0.8	0.00	0.77

6.2 Evaluation of RCC-MR Procedure

The RCC-MR creep-fatigue analysis is carried out as described in section 3. Table 41 details, for the same case as that considered in Table 35, the results obtained following the design and best fit approaches.

Table 41 - RCC-MR Creep-Fatigue Evaluation

Temperature (˚C)	550	
Hold time (hours)	1 (in tension)	
Total strain range (%)	0.361	
Nr : Number of cycles to failure	13,012	
	Design	Best fit
Allowable number of cycles in pure fatigue	1868	40,790
Fatigue damage (per cycle)	5.35×10^{-4}	2.45×10^{-5}
σ_k at the beginning of hold time (MPa)	236	236
σ_r at the end of hold time(MPa)	148	148
σ_k / K (MPa)	262	236
Allowable time for σ_k / K (hours)	22	690
Creep damage (per cycle)	4.50×10^{-3}	2.82×10^{-5}
Calculated number of cycles to failure, Nc	174	11,715
Margin vs test (Nr / Nc)	74.8	1.1
Creep Damage for Nr cycles		0.366
Fatigue Damage for Nr cycles		0.319

Table 42 to give the results obtained by using the RCC-MR procedure for all the tests listed in section 4. Creep damages and fatigue damages for the experimental number of cycles to failure are also calculated in the case of the best fit analysis. These damage values are plotted on a diagram with the RCC-MR creep fatigue damage envelope (bilinear with 0.3, 0.3 intersection). Representative points located outside (above) the creep-fatigue envelope correspond to margins larger than 1. In this case the rule is conservative.

The following can be concluded.

- In the design approach:
    - The RCC-MR design approach leads to conservative results compared to experimental ones. The margin is ranging from 14 to136. The minimum values are obtained for a test at 500˚C with hold time in compression (JNC study, Table 43) and a test at 593˚C with hold time in tension on aged material (JAPC-ORNL joint study Table 42)
- In the best fit approach:
    - If we consider all the results, the margin is ranging from 0.3 to 4.6. The minimum value of 0.3 is obtained for a test at 593˚C on aged material in the JAPC-USDOE joint study.
    - If we consider results of tests in tension for unaged material, the minimum margin is 0.4, obtained for a test at 593˚C in the JAPC-USDOE joint study (test no. 6 from Table 25). With the exception of the latter test, which is perhaps not representative (the number of cycles to failure is only 400 while it is equal to 1081 in test no. 5 which has the same hold time and strain range), the smallest margin is 0.6 and the margin is greater than 1 for the majority of results analyzed: 48 results have a margin greater than 1 and 15 less than 1. The smallest margin of 0.6 is also reached in a test of JAPC-USDOE joint study (test no. 14 from Table 25). If we consider all other sources, the smallest margin is equal to 0.7 (Table 47). These considerations are illustrated in Figure 43, below.

- o If we consider the results of tests in compression, for which only the results for the unaged material are available, the smallest margin is 0.5 and the margin is lower than 1 for the majority of tests analyzed. Nineteen tests have a margin lower than 1 and four, a margin greater than 1.
- o If we consider the results of tests in tension for the aged material of the JAPC-USDOE joint study, the smallest margin is 0.3 and the largest margin is 1.5. On average, the margins are smaller than those for the unaged material.

- For the case of tests in tension, the margin, ranging from 0.6 to 1, does not necessarily reflect a strong creep-fatigue interaction. It can be attributed to lower than average fatigue and creep mechanical characteristics. This is apparently the case for the tests of (IGCAR study) where the margin is lower than 1 for continuous fatigue tests.
- According to the conclusions of Section 5, the lower margin for tests with hold time in compression seems to be attributable to the effect of environment.

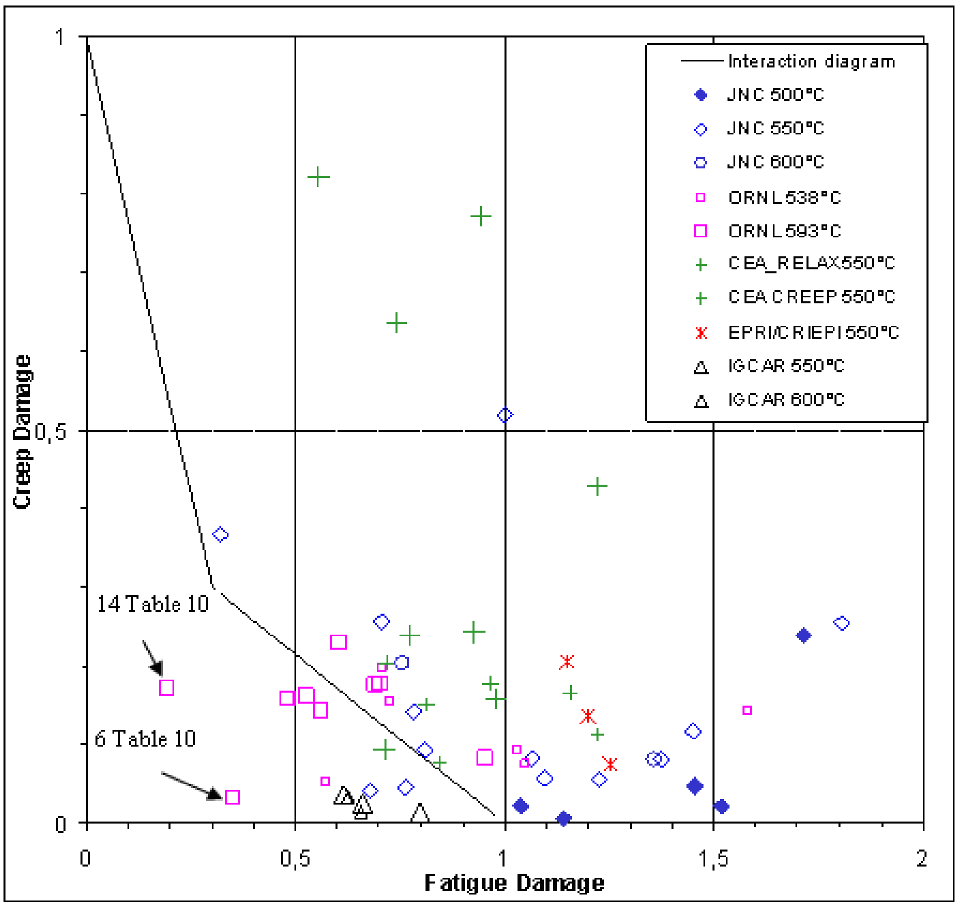
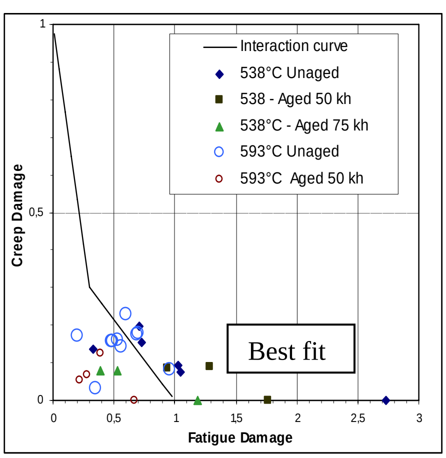


Figure 43 - Tests in Tension-Unaged-Creep-Fatigue Damage-Best Fit Approach

Table 42 - ORNL- JAPC- USDOE Joint Study – RCC-MR Evaluation

Test No.	Temp (˚C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
3 Tab 1	538	1.00	T	1	1734	1388	1.2	24	70.8	1682	1.0	9.33E-02	1.03
4 Tab 1	538	1.00	T	1	2654	1388	1.9	24	108.4	1682	1.6	1.43E-01	1.58
12 Tab 1	538	0.50	T	0.5	6975	6420	1.1	122	57.2	9602	0.7	1.54E-01	0.73
13 Tab 1	538	1.00	T	0.5	6787	5820	1.2	102	66.5	9602	0.7	1.97E-01	0.71
4 Tab 2	538	0.00	T	0.51	10,700	9034	1.2	487	22.0	9034	1.2	0	1.18
5 Tab 2	538	0.25	T	0.51	4791	6729	0.7	135	35.4	9034	0.5	7.79E-02	0.53
6 Tab 2	538	0.50	T	0.51	3535	6108	0.6	115	30.7	9034	0.4	8.03E-02	0.4
7 Tab 2	538	3.00	T	0.5	3113	4845	0.6	74	42.0	9602	0.3	1.36E-01	0.32
14 Tab 2	538	0.00	T	0.71	6060	3429	1.8	171	35.4	3429	1.8	0	1.77
15 Tab 2	538	0.25	T	0.78	3537	2363	1.5	53	66.1	2753	1.3	9.10E-02	1.28
16 Tab 2	538	0.50	T	0.78	2590	2267	1.1	43	60.5	2753	0.9	8.65E-02	0.94
17 Tab 2	538	0.00	T	0.7	9676	3544	2.7	177	54.6	3544	2.7	0	2.73
18 Tab 2	538	0.25	T	0.78	2894	2363	1.2	53	54.1	2753	1.1	7.44E-02	1.05
19 Tab 2	538	0.50	T	0.79	1530	2207	0.7	42	36.4	2673	0.6	5.18E-02	0.57
2 Tab 1	593	1.00	T	0.51	2926	3236	0.9	59	49.4	5565	0.5	1.62E-01	0.53
5 Tab 1	593	1.00	T	1	1081	943	1.1	20	53.1	1134	1.0	8.27E-02	0.95
6 Tab 1	593	1.00	T	1	400	943	0.4	20	19.7	1134	0.4	3.06E-02	0.35
7 Tab 1	593	0.50	T	0.5	4150	3768	1.1	71	58.1	5994	0.7	1.75E-01	0.69
8 Tab 1	593	1.00	T	0.5	2900	3399	0.9	62	46.7	5994	0.5	1.58E-01	0.48
9 Tab 1	593	0.50	T	0.5	4202	3768	1.1	71	58.8	5994	0.7	1.77E-01	0.70
10 Tab 1	593	0.50	T	0.5	3360	3768	0.9	71	47.0	5994	0.6	1.42E-01	0.56
11 Tab 1	593	1.00	T	0.5	2882	3399	0.8	62	46.4	5994	0.5	1.57E-01	0.48
14 Tab 1	593	2.00	T	0.4	2958	4971	0.6	89	33.3	15,061	0.2	1.71E-01	0.20
1 Tab 2	593	0.00	T	0.47	5061	7556	0.7	369	13.7	7557	0.7	0	0.67
2a Tab 2	593	0.50	T	0.5	1652	3768	0.4	71	23.1	5994	0.3	6.98E-02	0.28
2b Tab 2	593	0.50	T	0.51	1221	3576	0.3	68	17.9	5565	0.2	5.23E-02	0.22
3 Tab 2	593	1.00	T	0.5	2303	3399	0.7	62	37.1	5994	0.4	1.26E-01	0.38
13 Tab 2	593	2.00	T	0.51	3352	2955	1.1	53	63.6	5565	0.6	2.28E-01	0.60

13 Tab 2 : unaged *14 Tab 2* **: aged 50 kh** *4 Tab 2* **: aged 75 kh**



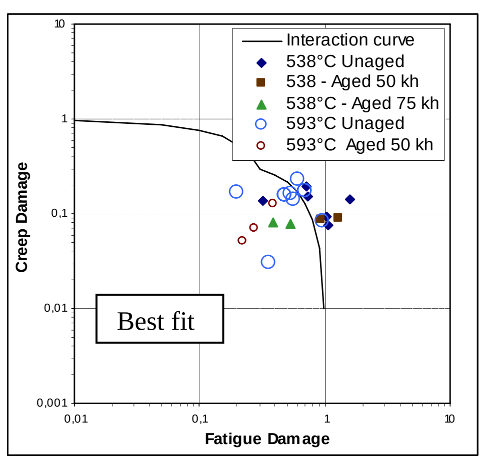
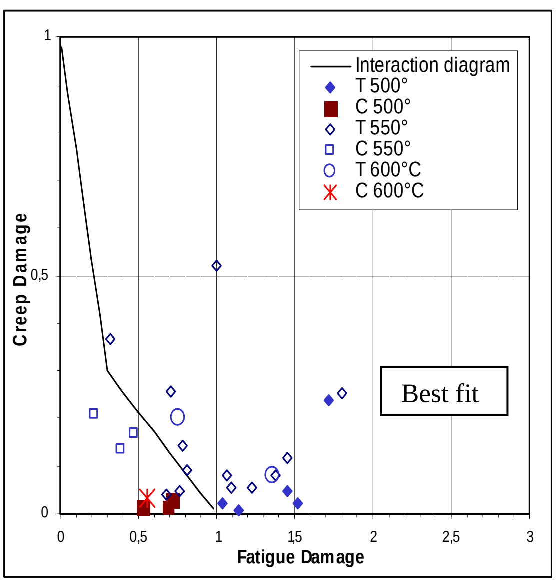


Table 43 - JNC – RCC-MR Evaluation

Temp (˚C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
500	0.017	T	0.5	19468	12,378	1.6	612	31.8	12800	1.5	2.22E-02	1.52E+00
500	0.017	C	0.5	8958	12378	0.7	612	14.6	12800	0.7	1.02E-02	7.00E-01
500	0.05	C	0.5	6717	11,969	0.6	476	14.1	12800	0.5	1.56E-02	5.25E-01
500	1	T	0.507	20686	9107	2.3	174	119.1	12,068	1.7	2.39E-01	1.71E+00
500	0.167	T	0.7	6485	4154	1.6	129	50.1	4464	1.5	4.64E-02	1.45E+00
500	0.167	T	1	2070	1902	1.1	57	36.2	1991	1.0	2.09E-02	1.04E+00
500	0.83	C	1.01	1404	1790	0.8	39	35.9	1962	0.7	2.94E-02	7.16E-01
500	0.017	T	1.51	1232	1068	1.2	37	33.5	1081	1.1	5.94E-03	1.14E+00
550	0.1	T	0.345	56097	25,404	2.2	491	114.3	56,203	1.0	5.19E-01	9.98E-01
550	1	T	0.361	13012	11,715	1.1	174	74.8	40,790	0.3	3.66E-01	3.19E-01
550	1	C	0.373	7347	10,458	0.7	166	44.3	34,073	0.2	2.09E-01	2.16E-01
550	10	C	0.724	1428	1652	0.9	23	63.1	3015	0.5	1.67E-01	4.74E-01
550	1	T	0.494	6453	4944	1.3	83	78.1	9138	0.7	2.57E-01	7.06E-01
550	0.1	T	0.498	16093	6711	2.4	141	113.7	8910	1.8	2.54E-01	1.81E+00
550	1	C	0.505	3293	4704	0.7	78	42.4	8529	0.4	1.35E-01	3.86E-01
550	1	T	0.692	2623	2353	1.1	39	66.9	3358	0.8	1.43E-01	7.81E-01
550	0.1	T	0.693	3568	2835	1.3	67	53.0	3346	1.1	8.24E-02	1.07E+00
550	0.1	T	0.991	1749	1424	1.2	37	47.2	1596	1.1	5.67E-02	1.10E+00
550	1	T	1.003	1266	1238	1.0	21	59.8	1566	0.8	9.18E-02	8.08E-01
550	0.333	T	1.5	1184	757	1.6	16	76.2	861	1.4	8.12E-02	1.37E+00
550	0.167	T	1.49	1065	785	1.4	19	56.1	868	1.2	5.61E-02	1.23E+00
550	0.167	T	1.001	1067	1377	0.8	32	33.0	1571	0.7	4.10E-02	6.79E-01
550	0.167	T	1.001	1197	1377	0.9	32	37.0	1571	0.8	4.60E-02	7.62E-01
550	0.333	T	0.998	2290	1330	1.7	26	88.3	1579	1.5	1.16E-01	1.45E+00
600	1	T	0.518	3630	2954	1.2	56	64.6	4811	0.8	2.03E-01	7.55E-01
600	0.333	C	1.001	589	929	0.6	25	23.4	1059	0.6	3.33E-02	5.56E-01
600	0.333	T	0.996	1452	937	1.5	25	57.3	1069	1.4	8.18E-02	1.36E+00



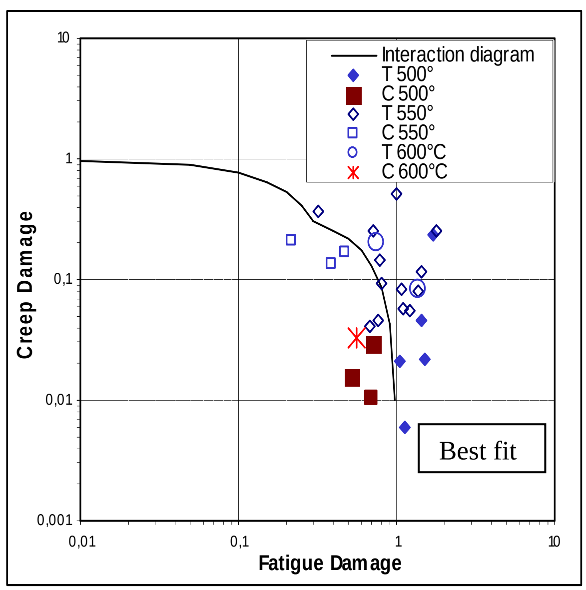


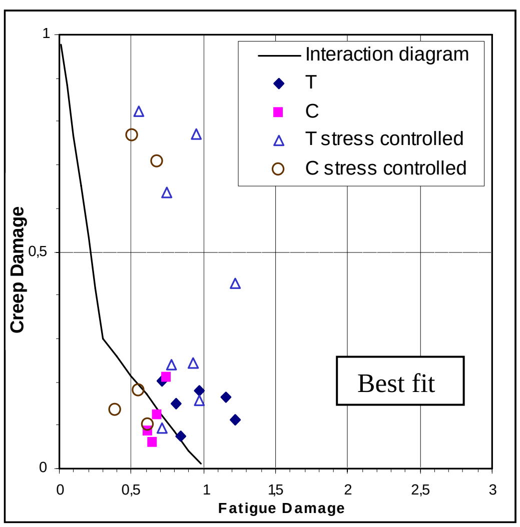
Table 44 - CEA – Creep Fatigue Tests - RCC-MR Evaluation

Test No.	Temp (°C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
1	550	0.5	T	1	1920	1296	1.5	23	82.5	1574	1.2	1.12E-01	1.22
2	550	0.16667	T	0.7	2751	2694	1.0	57	48.3	3267	0.8	7.67E-02	0.84
3	550	0.5	T	0.7	3778	2450	1.5	43	86.9	3267	1.2	1.65E-01	1.16
4	550	0.5	T	0.6	4046	3474	1.2	61	66.1	4969	0.8	1.50E-01	0.81
5	550	0.5	T	0.6	4802	3474	1.4	61	78.4	4969	1.0	1.78E-01	0.97
6	550	1.5	T	0.6	3560	2988	1.2	47	75.9	4969	0.7	2.04E-01	0.72
7	550	2	C	1	964	1186	0.8	19	50.1	1574	0.6	8.57E-02	0.61
8	550	0.16667	C	0.7	2121	2694	0.8	57	37.2	3267	0.6	5.91E-02	0.65
9	550	0.5	C	0.6	3362	3474	1.0	61	54.9	4969	0.7	1.25E-01	0.68
10	550	1.5	C	0.6	3677	2988	1.2	47	78.4	4969	0.7	2.10E-01	0.74

Table 45 - CEA - Stress Controlled Creep Fatigue Tests - RCC-MR Evaluation

Test No.	Temp (˚C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
1311	550	0.00833	T	0.7	2533	1881	1.3	80	31.8	2595	0.98	1.59E-01	0.98
1310	550	0.01667	T	0.7	1900	1427	1.3	56	34.0	2449	0.78	2.38E-01	0.78
1309	550	0.05	T	0.7	2050	747	2.7	29	69.7	2171	0.94	7.71E-01	0.94
1307	550	0.1	T	0.7	1094	518	2.1	21	53.2	1978	0.55	8.23E-01	0.55
1277	550	0.16667	T	0.7	1270	400	3.2	15	84.6	1869	0.68	1.59E+00	0.68
1305	550	0.5	T	0.7	880	192	4.6	6	135.6	1605	0.55	3.31E+00	0.55
1221	550	0.00625	T	1	999	1071	0.9	46	21.6	1396	0.72	9.32E-02	0.72
1332	550	0.00805	C	1	849	996	0.9	42	20.3	1382	0.61	1.02E-01	0.61
1224	550	0.05278	T	0.7	1600	720	2.2	29	55.7	2156	0.74	6.35E-01	0.74
1331	550	0.06667	C	0.7	1415	618	2.3	26	55.2	2089	0.68	7.09E-01	0.68
1325	550	0.14972	T	0.5	4032	886	4.5	32	124.2	3859	1.04	2.11E+00	1.04
1335	550	0.18583	C	0.5	1676	781	2.1	28	60.1	3698	0.45	1.09E+00	0.45
1265	550	0.0125	T	0.5	5550	3717	1.5	144	38.5	5986	0.93	2.43E-01	0.93
1268	550	0.0175	T	0.5	6995	3151	2.2	117	59.8	5729	1.22	4.28E-01	1.22
1333	550	0.01694	C	0.5	2260	3205	0.7	119	18.9	5754	0.39	1.34E-01	0.39
1334	550	0.01611	C	0.5	3170	3289	1.0	123	25.7	5793	0.55	1.79E-01	0.55
1318	550	0.09722	T	0.4	8836	2111	4.2	84	105.1	8264	1.07	1.69E+00	1.07
1341	550	0.09083	C	0.4	4300	2189	2.0	88	48.8	8394	0.51	7.69E-01	0.51

Strain range = elastic + plastic strain range. Calculations are made from the elastic + plastic + creep strain range



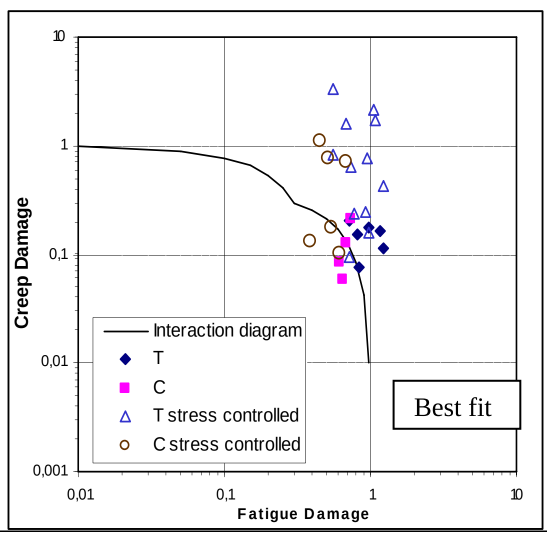
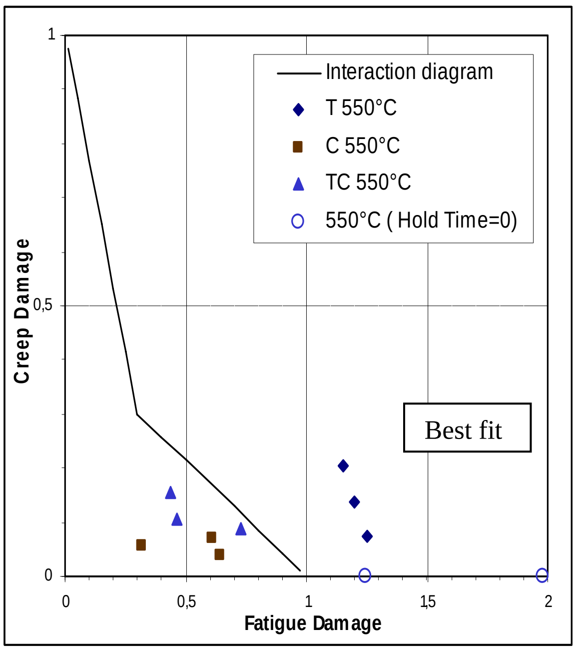


Table 46 - EPRI / CRIEPI Joint Studies – RCC-MR Evaluation

Test No.	Temp (˚C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
CF-1	550	0.16667	T	1	1968	1379	1.4	32	60.8	1574	1.3	7.55E-02	1.25
CF-2	550	0.16667	C	1	1006	1379	0.7	32	31.1	1574	0.6	3.86E-02	0.64
CF-3	550	0.16667	TC	1	1142	1228	0.9	22	51.6	1574	0.7	8.76E-02	0.73
CF-4	550	1.00000	T	1	1885	1243	1.5	21	88.5	1574	1.2	1.36E-01	1.20
CF-5	550	1.00000	C	1	956	1243	0.8	21	44.9	1574	0.6	6.91E-02	0.61
CF-6	550	1.00000	TC	1	734	1028	0.7	16	47.2	1574	0.5	1.06E-01	0.47
CF-7	550	0.16667	T	0.5	10,120	6216	1.6	124	81.5	8799	1.2	2.05E-01	1.15
CF-8	550	0.16667	C	0.5	2822	6216	0.5	124	22.7	8799	0.3	5.71E-02	0.32
CF-9	550	0.16667	TC	0.5	3871	4805	0.8	90	42.8	8799	0.4	1.57E-01	0.44
	550	0.00		0.5	10,960	8798	1.2	471	23.3	8799	1.2	0.00E+00	1.25
	550	0.00		1	3120	1574	2.0	79	39.6	1574	2.0	0.00E+00	1.98



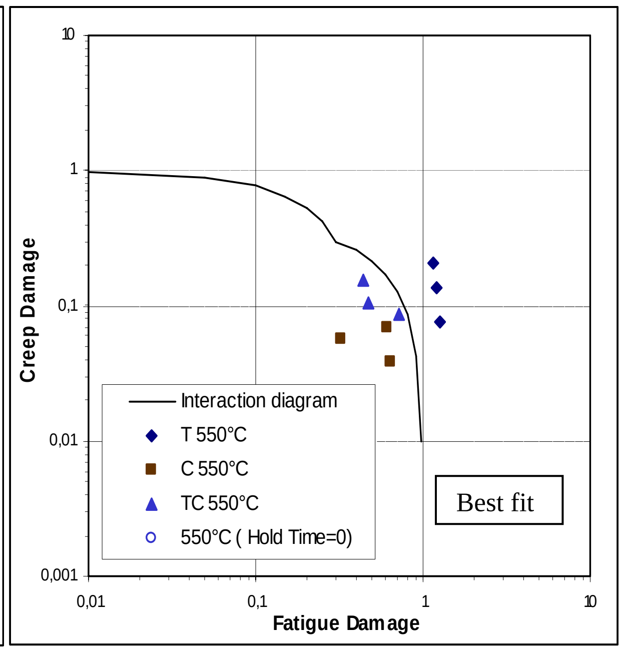
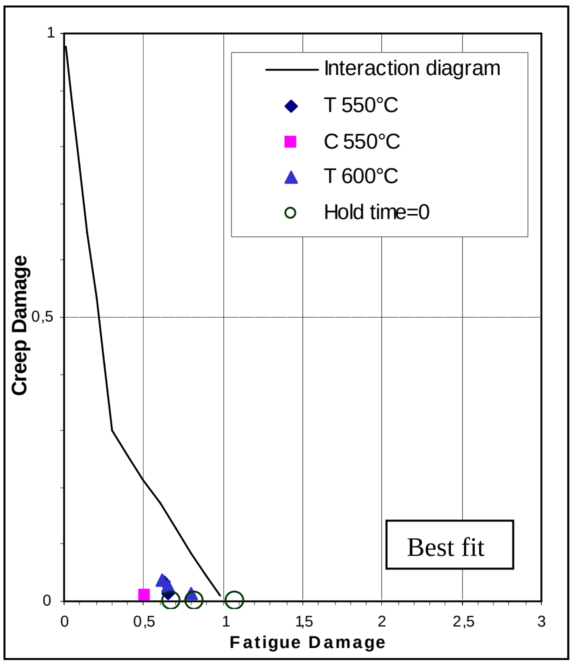
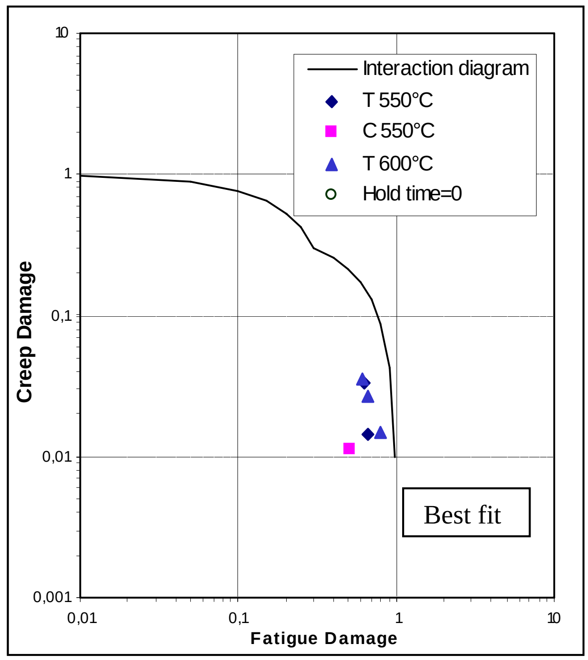


Table 47 - IGCAR Creep Fatigue Tests Results – RCC-MR Evaluation

Temp (˚C)	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal Design	Margin Design	Nfat Bestfit	Margin Fatigue	Creep D	Fatigue D
550	0.01667	T	1.2	771	1120	0.7	42	18.3	1178	0.7	1.44E-02	6.55E-01
550	0.16667	T	1.2	736	1047	0.7	25	29.3	1178	0.6	3.34E-02	6.25E-01
550	0.01667	C	1.2	600	1120	0.5	42	14.2	1178	0.5	1.12E-02	5.10E-01
600	0.01667	T	1.2	657	791	0.8	31	21.2	825	0.8	1.48E-02	7.97E-01
600	0.16667	T	1.2	543	754	0.7	23	23.6	825	0.7	2.66E-02	6.58E-01
600	0.5	T	1.2	506	727	0.7	19	27.0	825	0.6	3.55E-02	6.14E-01
500	0		1.2	1030	1519	0.7	76	13.6	1519	0.7	0.00E+00	6.78E-01
550	0		1.2	972	1178	0.8	59	16.5	1178	0.8	0.00E+00	8.25E-01
600	0		1.2	890	825	1.08	39	22.94	825	1.08	0.00E+00	1.08E+00





6.3 Comparison of Calculated Stresses at the Beginning of Hold Time

Figure 44 shows the calculated stress at the beginning of the hold time as a function of the elastic-plastic strain range and for a temperature of 550˚C. It can be seen that the stress calculated according to the ASME procedure is significantly greater than that calculated with the RCC-MR procedure. The difference is due to the use of the cyclic stress-strain curve which takes into account softening and symmetrization effects according to the RCC-MR procedure.

DDS initial stress (Si is calculated by entering the isochronous stress-strain curve at 0.5Δε) would also be significantly greater than that calculated using RCC-MR at large strains (>0.4%).

Figure 44 also shows the mid-life stresses measured experimentally at beginning of hold time. The data are taken from Table 28, Table 29 and Table 30.

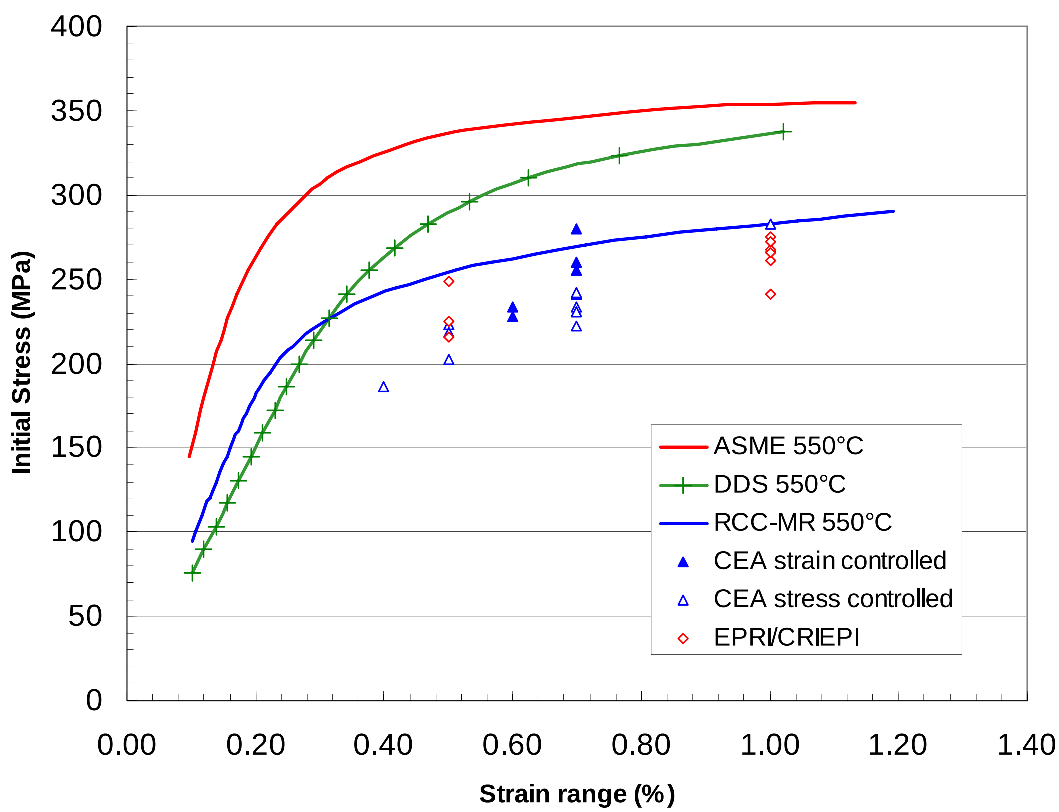


Figure 44 - Comparison of Stresses at the Beginning of Hold Time at 550˚C

At 593˚C and for a total strain range Δε = 0.5%, the stress values measured at beginning of hold times, as taken from [5], are 183 MPa and 195.8 MPa. The use of RCC-MR cyclic curve with a symmetrization factor Ks=0.5 gives a calculated stress value of 212 MPa.

It appears, therefore, that there is a reasonably good agreement between the initial stress calculated using the RCC-MR procedure and the measured values for unaged material.

For aged material, the hold time initial stresses calculated are lower because of reduced yield strength. No precise values appear in the collected data.

Figure 45 plots the ratio of the stresses calculated using the ASME procedure to those from the RCC-MR procedure. It can be seen that the ASME procedure could be improved by dividing the calculated stress by a factor to account for cyclic softening and symmetrization effects. For temperatures ranging between 400˚C and 600˚C a value around 1.25 would seem reasonable. This factor can also be dependent on the strain range and temperature. Further test results that provide values of the stresses at the beginning of the hold time would be useful to refine such a factor.

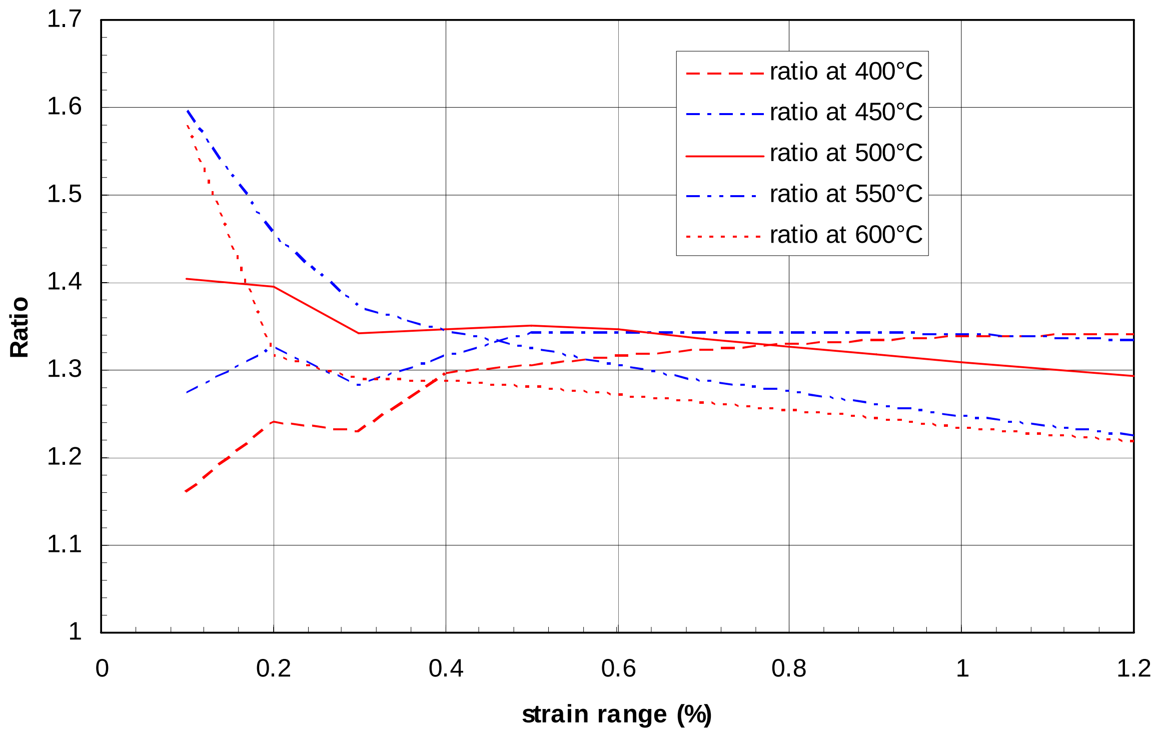


Figure 45 - Ratio between the Initial Stresses Calculated with ASME and RCC-MR

The implementation of such a corrective factor, Ks, can be done with only minor modifications of the ASME procedure. In the following we suppose that Ks is independent of temperature and strain range:

We introduce Table 48 to define the corrective factor Ks to account for cyclic softening and symmetrization for Mod 9Cr-1Mo.

Table 48 - T-1433-1

Material	Ks
Austenitic stainless steel	1
Ni-Fe-Cr (Alloy 800H)	1
2 1/4Cr-1Mo	1
9Cr-1Mo-V	1.25

T-1433 (a) Creep damage evaluation – general procedure

Step 4: the stress level corresponding to ε_t is named S'_j and S_j is calculated as S'_j/Ks

Step 5 [2]: the appropriate isochronous stress-strain curve should be entered at a stress level equal to Sj instead of the strain level ε_t in the current procedure.

6.4 Comparison of Relaxation Procedures

Figure 46 compares the relaxation curve calculated using the ASME and RCC-MR procedures. The initial stress level and temperature are those from Table 34. It can be noted that the ASME approach is rather conservative but the difference is reduced when an elastic follow up factor of 3 is taken into account in the RCC-MR procedure (as recommended for the design approach).

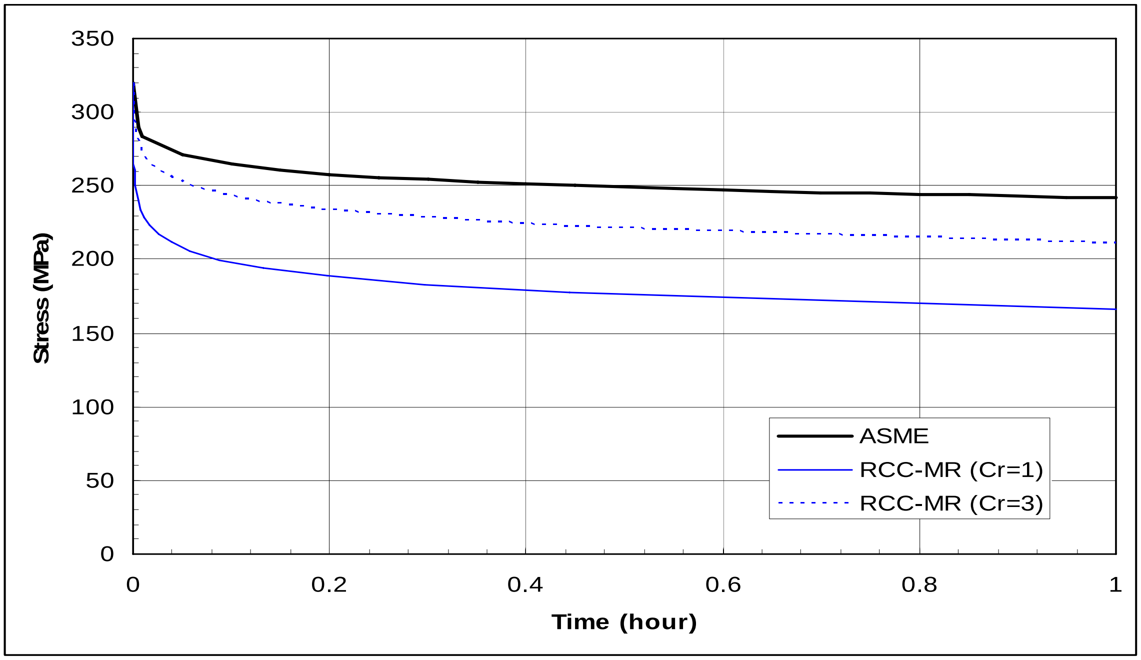


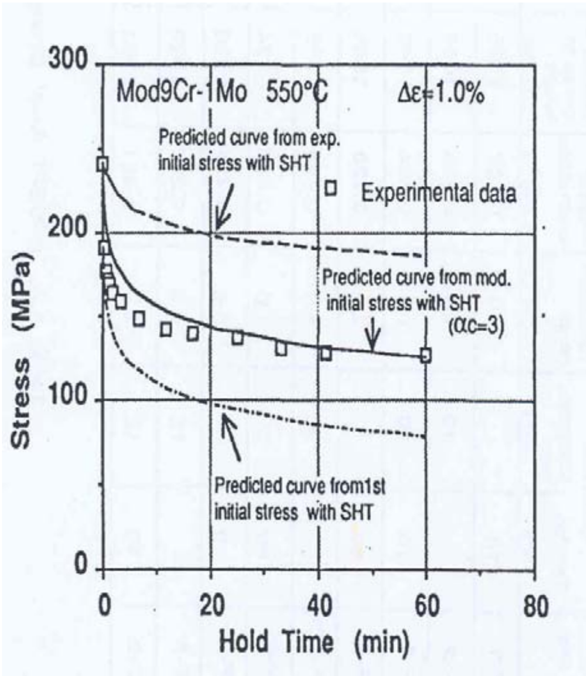
Figure 46 - Relaxation at 550˚C

References [5] and [12] show relaxation curves during hold times. Reference [12], in particular, compares experimental results and model predictions. Three methods were tried:

- Creep law coupled with strain hardening theory (SHT) with experimental initial stress. This method under predicts relaxation.
- Prediction produced by a method proposed by Aoto et al. for cyclic softening materials. Aoto et al. employ a creep strain equation and the SHT with initial stress taken from the first cycle to predict relaxation during the first cycle. This method over predicts the stress relaxation.
- Prediction proposed by Kawasaki which uses the initial stress from the first cycle multiplied by 0.85 to assess relaxation. This method is the one which is the closest to experiment.

These predictions are shown in Figure 47 which also shows the prediction obtained using the RCC-MR creep strain law coupled with SHT and mid-life experimental initial stress. Note that the elastic follow-up and triaxiality coefficient have been set to 1 for this evaluation (see section 3.2). The amount of stress relaxation is under predicted but the agreement seems reasonably good.

To conclude, assessment of relaxation using isochrounous stress-strain curves is a penalizing approach. The ASME procedure could be improved by providing, as in the RCC-MR code, a creep strain law which would allow relaxation analyses to be performed. The attention of the designer should be drawn on the difference between monotonic and cyclic relaxations and the ASME Code may need to be improved to provide recommendations on how to address elastic follow up effects.



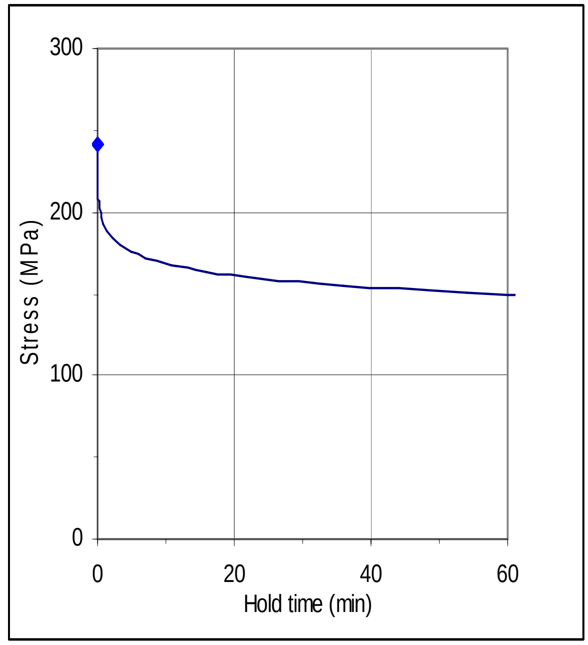


Figure 47 - Comparison of EPRI/CRIEPI and RCC-MR Relaxation Curves

6.5 Comparison of Safety Factors

Safety factors used in the ASME and RCC-MR fatigue curves are identical. The main difference in the two procedures is the use of a safety factor of 1.5 (K'=0.67) in the ASME creep damage evaluation versus a factor of 1.1 (K=0.9) in the RCC-MR procedure.

The value of K' used in the ASME procedure was a result from a decision taken in 1987 to increase the conservatism of the creep-fatigue evaluation procedure of Code Case N-47 (currently Subsection NH). The modification consisted of decreasing the factor K' from 0.9 to 0.67.

In [16], it is proposed to keep the value K'=0.67 for inelastic analyses but to restore the use of K'=0.9 for elastic analyses. Following is a summary of the technical basis of this proposal.

The 1987 decision was mainly taken due to the premature failure observed in Eddystone pipes. Thermal shock tests carried out at ORNL and in the UK also contributed to this decision.

For the Eddystone pipes, calculations carried out by ORNL and by AEA Technology in the UK (see ORNL results in Table 49) seemed to indicate that inelastic analyses may not be conservative. However, other analyses carried out by Philadelphia Electric Co. did not seem to confirm this trend but the information available was not sufficient to explain the differences. It was also impossible to confirm if a more consistent set of loads was taken into account by Philadelphia Electric Co. Elastic analysis remained in any case conservative by a factor of about 30 (with K'=0.9). It is also likely that other phenomena were involved in the cracking (massive intergranular precipitates) whose prevention should be performed by other means, for example, by the control of chemical composition.

Table 49 - Calculated Allowable Life for the Eddystone Pipes (With K'=0.9)

	Elastic Analysis	Inelastic Analysis	Component
Inside surface (compressive stresses)	7900 hr	330,000 hr	
Interior peak		266,000 hr	
Outside surface	4300 hr (margin of about 30 on life)	560,000 hr	130,500 hr (through wall crack)

For the thermal shock tests, inelastic analyses with a K' factor of 0.9 give best estimate predictions that are consistently too high (life overpredicted by a factor of up to 6.1). On the other side, Code allowable analyses are always conservative compared to the number of cycles to failure. Elastic results are only reported for Mod 9Cr-1Mo. In this case, the Code-allowable elastic life (with K'=0.9) is only 0.003 cycle, as compared with the actual number of cycles to failure of 2600.

In addition, tests performed in France are also reported and elastic analyses are compared to the actual life. It is concluded that the present ASME elastic procedure is overly conservative and that it would be justified to use a K' factor of 0.9 instead of 0.67.

It is therefore proposed to modify Subsection NH Appendix T by making a distinction between inelastic and elastic analyses and restoring a factor of 0.9 for the latter. The proposition consists of modifying ASME NH Table T-1411-1 by Table 50 as follows.

Table 50 - Proposal for K' Factor

Material	K'	
	Elastic Analysis	Inelastic Analysis
Austenitic stainless steel	0.9	0.67
Ni-Fe-Cr (Alloy 800H)	0.9	0.67
2 1/4Cr-1Mo	0.9	0.67
9Cr-1Mo-V	0.9	0.67

6.6 Creep-Fatigue Damage Envelope

As indicated in Section 3, the ASME creep fatigue damage envelope is much more conservative compared to the RCC-MR one.

In the case of Modified 9Cr-1Mo, the straight lines which represent the bilinear creep-fatigue interaction intersect at coordinates (0.1, 0.01) whereas they intersect at (0.1, 0.1) in the case of 2¼ Cr-1Mo and alloy 800H and at (0.3, 0.3) for austenitic stainless steel.

This low value of the creep damage at the intersection point was introduced to account for the very small values of creep damages on failed specimens (see Figure 37 and Figure 38). Among these tests, some are probably relative to effect of air environment on fatigue at elevated temperature. In this case, the degradation of fatigue life is not directly related to the creep damage; the tests to be considered in creep fatigue envelope assessment should be the tests in vacuum and the tests in air where metallographic indications of creep damage have been found. The latter is very difficult to identify in the data base.

The evaluation of creep damage following the rules of the ASME and RCC-MR codes are based on creep rupture properties of initially virgin material. In the evaluation of creep fatigue experimental results, there is an underestimation of the creep damage if the material has been softened as compared with virgin material which can occur after prior aging or after cyclic softening. Long term prior aging at high temperatures followed by creep fatigue at a lower temperature, however, is not considered as a realistic situation.

Concerning the cyclic softening correction of creep rupture properties, it should be applied in the creep fatigue envelope assessment if it is intended to take this correction into account in design assessment.

If we eliminate tests whose failure seems mainly due to environment effects, tests on aged materials for which the assessment of creep damage using unaged material properties is not realistic, the application of RCC-MR method with a (0.3, 0.3) focal point gives satisfactory results.

This practical observation about the RCC-MR method is not a justification of the validity of an interaction diagram for Mod 9Cr-1Mo. For that purpose, complementary analyses are needed: If tests at 600˚C or 593˚C seem sufficient to study true creep fatigue interaction, that is not the case at 500˚C or 550˚C (see Section 5.2). Longer hold time tests would be required to improve the understanding of creep-fatigue interaction at those temperatures.

In the meantime, it seems reasonable to keep the RCC-MR interaction diagram with focal point at (0.3, 0.3).

6.7 Example of Application of Proposed Modifications

In this section, the modifications of the ASME NH rules proposed in Sections 6.3 (calculated stresses at the beginning of hold times) and 6.5 (safety factors) are applied to one of the series of tests previously analyzed.

The tests chosen are those at 550˚C extracted from JNC list of tests of Table 27. They cover strain ranges from 0.345% up to 1.5% and hold times from 0.1 hour up to 1 hour.

Results are presented in:

- Best fit results

Best fit calculations were performed using a symmerization factor Ks=1.25. Table 6-17 shows the stresses at the beginning of hold times. It can be noticed that these stresses are close to those predicted using RCC-MR at the same temperature (see the initial stress vs. strain range curves of Figure 44). However, the creep damages are still larger and the allowable numbers of cycles are smaller than those found using RCC-MR. As in the case of best fit evaluation the same stress to rupture curves are used for both RCC-MR and ASME evaluations (see Section 3.1). The result seems to be mainly attributable to the underprediction of stress relaxation when it is assessed using the isochronous stress-strain curves.

Figure 48 shows the representative points of calculated creep-fatigue damages together with the interaction diagram with a (0.3, 0.3) intersection point.

Table 51 - Example of ASME Evaluation using Proposed Modifications

Test No.	Hold Time Duration (h)	Sign	Strain Range (%)	Cycles to Failure Nr	Ncal Bestfit	Margin Bestfit	Ncal esign	Margin Design	Nfat Bestfit	Margin Fatigue	Best Fit		Init Stress (MPa)
											Creep D	Fatigue D	
1	0.1	T	0.345	56,097	9427	6.0	242	232	298,786	0.2	4.09	0.19	254
2	1	T	0.361	13,012	2361	5.5	78	168	159,124	0.1	4.70	0.08	256
3	1	C	0.373	7347	2149	3.4	72	102	118,338	0.1	2.80	0.06	258
4	10	C	0.724	1428	187	7.6	Eval not possible		4599	0.3	4.58	0.31	279
5	1	T	0.494	6453	959	6.7	34	189	20,983	0.3	3.69	0.31	269
6	0.1	T	0.498	16,093	1616	10.0	67	240	20,321	0.8	2.12	0.79	269
7	1	C	0.505	3293	910	3.6	32	102	19,223	0.2	1.92	0.17	270
8	1	T	0.692	2623	388	6.8	Evaluation Not Possible		5501	0.5	2.04	0.48	278
9	0.1	T	0.693	3568	502	7.1			5469	0.7	0.65	0.65	278
10	0.1	T	0.991	1749	176	9.9			1813	1.0	0.38	0.96	283
11	1	T	1.003	1266	152	8.3			1763	0.7	1.20	0.72	283
12	0.333	T	1.5	1184	67	17.6			692	1.7	0.64	1.71	286
13	0.167	T	1.49	1065	69	15.4			703	1.5	0.37	1.51	286
14	0.167	T	1.001	1067	170	6.3			1771	0.6	0.33	0.60	283
15	0.167	T	1.001	1197	170	7.1			1771	0.7	0.37	0.68	283
16	0.333	T	0.998	2290	166	13.8			1784	1.3	1.10	1.28	283

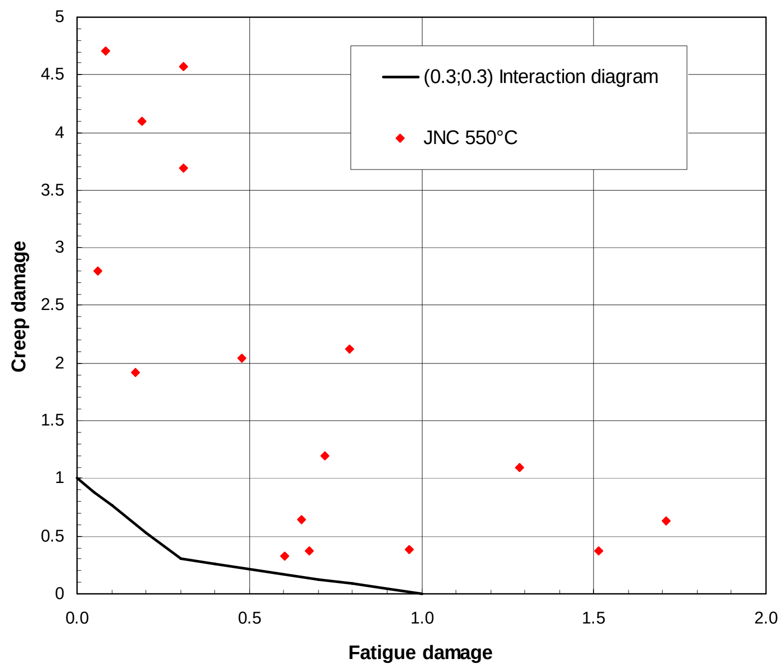


Figure 48 - Example of ASME Evaluation using Proposed Modifications Creep-Fatigue Damage – Tests in Tension

- Design results

Design calculations were performed using a symmetrization factor Ks=1.25, a safety factor K'=0.9 and design stress to rupture curves extrapolated to 1 hour. Stress values for 1 hour were extrapolated by multiplying the stresses values for t=10 hours by the ratio Sr(1h)/Sr(10h) using the ORNL model in (4).

Figure 49 below shows the 550˚C design stress to rupture curve so obtained.

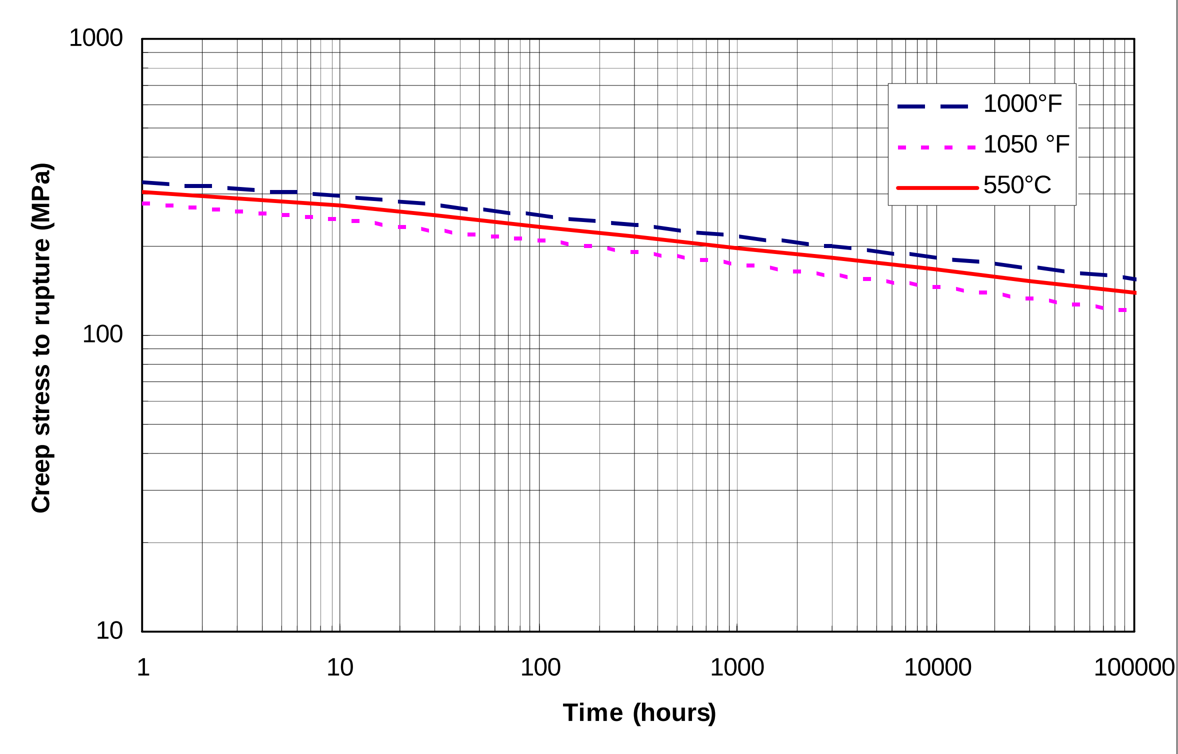


Figure 49 - Extrapolation of Design ASME Creep Stress to Rupture For T=1hr

Evaluation still could not be performed for strains larger than 0.7%. The reason is that the ASME extrapolated stress to rupture curve is lower than the RCC-MR design curve. For t=1 hour, the stress to rupture value is 328 MPa from RCC-MR and 306 MPa in the case of the ASME extrapolated value. The ASME evaluation is therefore not possible for stresses at beginning of hold times larger than 306×0.9= 275 MPa if the RCC-MR stress to rupture curves had been used, the ASME design evaluation would have been possible for all the cases in Table 51.

In conclusion, in the case of best fit evaluations, application of the proposed modifications allows the margins to be reduced by a factor greater than 3.5 but creep damage evaluations remain conservative because of the underprediction of stress relaxation when the isochronous stress-strain curves are used. In the case of design evaluations, evaluation of creep-fatigue damage becomes possible except for large strain ranges (when the allowable time Td is less than 10 hours).

6.8 Combination of Primary and Secondary Stresses

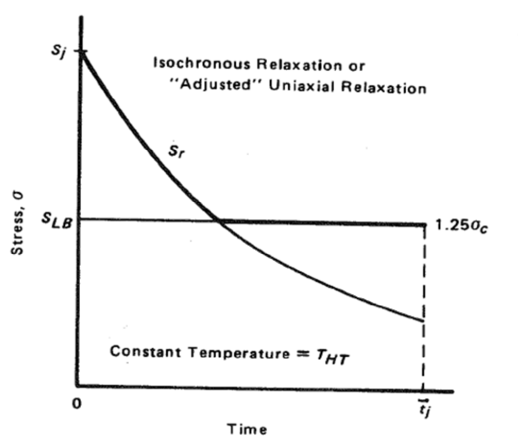
The test results considered thus far were based on fatigue relaxation tests without any primary stress and this effect was therefore not addressed in the previous comparison.

In the ASME procedure, the effect of primary stresses is considered by ensuring that the stress relaxation process does not proceed to a stress level less than S_{LB}=1.25 σ_c, where σ_c is the core stress intensity that exists during sustained normal operating conditions (see Figure 50).

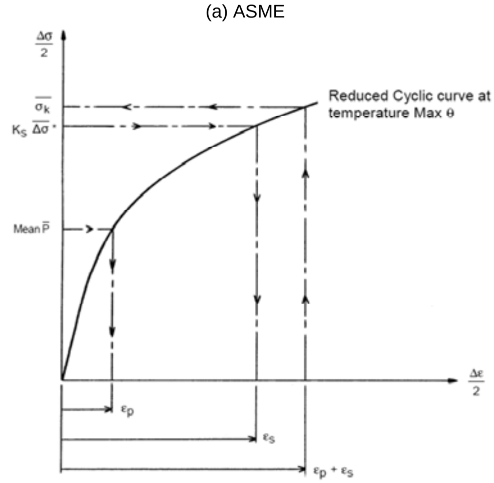
In the RCC-MR Code, the primary and secondary stresses are combined using the cyclic stress-strain curve and by adding the strains instead of the stresses (see Figure 50).

For the particular case of Mod 9 Cr 1 Mo, the RCC-MR approach could be very conservative. This is illustrated in Table 52 which gives an example of an application with a combination of primary and secondary stresses. In this example, it can be seen that the stress at the beginning of the hold time as calculated using the RCC-MR procedure is greater than that calculated by the ASME procedure, even if the latter does not take account of softening and symmetrization effects. In addition, the stress calculated using the RCC-MR procedure is greater than the elastic stress range, which is not physical. The allowable number of cycles calculated using the ASME rules is finally lower than that calculated with RCC-MR due to the use of a severe safety factor in the creep damage evaluation (K'=0.67) and to the conservative creep-fatigue damage envelope.

The analysis of specific tests that combine the primary and secondary stresses would be needed to confirm the conservatism of the RCC-MR approach and to propose a more realistic evaluation.



(a) ASME



(b) RCC-MR

Figure 50 - Comparison of Combination of Primary and Secondary Stresses

Table 52 - Comparison of Combination of Primary and Secondary Stresses

Elastic stress range	MPa	250	
Membrane+bending Primary stress	MPa	120	
Temperature during hold time	˚C	450	
Maximum temperature	˚C	450	
Minimum temperature	˚C	400	
Hold time per cycle	Hours	200	
		RCC-MR	ASME NH
Elastic+plastic strain range	%	0.122	0.142
Stress at the beginning of the hold time	MPa	271.4	250.2
Stress at the end of the hold time	MPa	268.1	249.8
K'		0.9	0.67
Corrected stress at the beginning of the hold time	MPa	302	373
Allowable time (without relaxation)	Hours	17971	3212
Creep damage / cycle		9.67×10^{-3}	6.10×10^{-2}
Creep strain increment	%	0.126	$1.70\ 10^{-5}$
Total strain range	%	0.248	0.142
Allowable number of cycles in fatigue	Cycles	16,092	7,711,380
Fatigue damage / cycle		6.20×10^{-5}	1.30×10^{-7}
Allowable number of cycles in creep-fatigue	Cycles	102	16

7 CONCLUSIONS

The ASME and RCC-MR creep-fatigue evaluation procedures have been analyzed on the basis of creep-fatigue test results.

The ASME procedure is very conservative. For the test conditions studied, the design approach is shown to be not executable. With the best fit approach, the life prediction is very conservative as compared with experimental results when the hold times are non zero. The following conclusions can be drawn.

- The values of the predicted stresses at beginning of hold times are far too high. Results could be improved by modifying the procedure for calculating the stress at the beginning of the hold time by taking into account the cyclic softening and symmetrization effects. This objective could be reached by applying a reduction factor to the stress calculated on the isochronous stress-strain curve.
- The prediction of stress relaxation using the isochronous stress-strain curves is too conservative (the stress relaxation is underpredicted as compared with the experimental results). Conservatism could be reduced by performing systematic cyclic stress relaxation analyses using a creep strain law as in the RCC-MR procedure. For that purpose ASME subsection NH should provide creep strain laws so that such analyses can be performed. The ASME Code may need also to be improved to provide recommendations on how to address elastic follow-up effects.
- The high safety factor used in the calculation of the creep damage (1/K'=1/0.67) is not justified. In the case of elastic analyses, at least, it would be more justified to use, like RCC-MR, a value of 0.9 instead of 0.67 for K'. A proposal for modifying ASME Subsection NH in such a way has been made. The proposal applies to all materials.
- The ASME NH creep-fatigue damage envelope is very conservative in the case of Mod 9Cr-1Mo [bi-linear damage lines with (0.1, 0.01) intersection]. On the basis of existing results, this diagram does not seem to be fully justified. For the analysis of true creep fatigue interaction, tests where environment plays a role (tests with hold time in compression) should be eliminated. At 593˚C or 600˚C, true creep fatigue interaction can probably be studied. But at lower temperatures, 550˚C or 500˚C, it seems that there is a lack of creep fatigue results under vacuum to select relevant data for the analysis of true creep fatigue interaction. Looking forward to such analyses, as RCC-MR procedure gives consistent results using bi-linear damage lines with (0.3, 0.3) intersection, it seems reasonable to use the same interaction diagram (which is already used in ASME NH in the case of austenitic stainless steels) for Mod 9Cr-1Mo in ASME NH.
- When the environment effects on fatigue life at elevated temperatures must be treated, as it is the case for cycling in air or non inert gas, the fatigue dependences on frequency, tensile and compressive strain rates instead of creep damage evaluation are probably more appropriate to improve the design rules.
- The RCC-MR procedure provides results that are consistent with the experimental tests and could be used to help improving the ASME procedure. However, when used for combined primary and secondary stresses, the RCC-MR procedure can be very conservative. The stress calculated at the beginning of the hold time by the RCC-MR procedure is greater than that calculated by using the ASME rules. Improvement of this part of the RCC-MR procedure, when supported by specific test results, is needed.

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PART 3
PROPOSED TEST PROGRAM TO ASSESS NEGLIGIBLE CREEP CONDITIONS OF MODIFIED 9CR-1MO

1 INTRODUCTION

In the frame of the AREVA HTR-VHTR design, it is recommended to operate the Reactor Pressure Vessel (RPV) in the negligible creep regime in order to avoid the implementation of a surveillance program covering the monitoring of the creep damage throughout the whole life of the reactor. Consequently it is of prime importance for design works to establish negligible creep conditions which can be fully assessed against data.

This report has been prepared in the context of Task 3 of the ASME/DOE Gen IV material project. It is aimed at defining tests necessary to validate negligible creep conditions for Mod 9Cr-1 Mo material.

The present situation after the study of reference 1 which takes into account an as large as possible amount of data at moderate temperature (< 500˚C) is the following:

- From the different criteria investigated, three criteria seem to be applicable to Mod. 9Cr-1Mo:
- Time fraction criterion with S_y as a reference stress (S_y: conventional 0.2% yield stress)
- Strain criterion with S_y as reference stress and 0.2% creep strain
- Relaxation of 1.5 S_m by 20% (S_m: time independent allowable stress)

The first two criteria are those from the ASME code but are modified to take account of cyclic softening.

- The time fraction and 0.2% creep strain criteria would allow respectively up to 5.35×10^5 hr and 1.58 $\times 10^5$ hours at 400˚C. The criterion based on stress relaxation would provide more favorable negligible creep conditions below 450˚C.

It was also concluded that, for further improvement of negligible creep limits, more creep strain data at 475˚C, 450˚C and, if possible, 425˚C will be useful. Further tests should also be performed to improve creep stress to rupture curves below 500˚C.

The first purpose of the proposed test program is to improve the knowledge of creep properties at moderate temperature (< 500˚C), including the possible effect of the post weld heat treatment (which is absolutely necessary for the welded joints of this grade), thermal aging and cyclic softening.

The second part of the program concerns the creep fatigue properties and is aimed at collecting further information to demonstrate that the negligible creep conditions are sufficient to prevent a significant reduction in fatigue life in creep fatigue conditions.

2 MATERIAL

Tests should be performed preferably on heavy section products corresponding to the future HTR–VHTR procurement specifications for the RPV. The following types of product forms will be relevant for HTR-VHTR projects:

- Forged parts with thickness of at least 200 mm
- Plates 140 mm thick or more

Depending on the scheduled procurement of prototypic parts and on the planning of test programs, more representative material can be made available when the proposed tests start.

The effect of the required post weld heat treatment on the creep properties in the temperature range of interest needs to be evaluated. The proper way to evaluate this effect is to compare test result in the as received and post weld heat treated conditions of the same product form (same cast, same manufacturing operations). The anticipated post weld heat treatment consists of 20 hours at 750˚C (the simulated PWHT conditions will have to be confirmed at the beginning of the test program).

The effect of cyclic softening on creep properties (creep strain law and creep stress to rupture) needs to be analyzed. The proposed cyclically softened conditions consist of subjecting specimens to continuous fatigue cycling with a strain range of 0.5%. Tests should be stopped when the stress-strain conditions are consistent with the cyclic curve at the temperature of the test. It could also be envisioned to add a hold time to those tests in order to achieve the cyclic softened conditions more rapidly.

3 CREEP TESTS

3.1 Improvement of Creep Strain Database

Creep tests at 450˚C and 475˚C are defined to obtain the targeted strains of 0.2, 0.5 and 1%. The proposed loads (Table 23) are chosen to be decreased to as low as possible levels in order to improve the extrapolation towards the reference stress levels defined in the negligible creep criterion, and yet be able to keep the test duration to a maximum of 15,000 hours. As usual, the program should be started with the higher loads in order to check the expected duration and, if necessary, to adjust the loads of longer term tests.

During the revision of creep strain law, it was noted a lack of data at 525˚C (2 short term tests only available) and at 538˚C, where very few creep strain results were recorded on a small number of heats and products forms. In order to improve the description of the temperature dependence of the creep strain, it will be useful to extend the data base at one of these temperatures. With the loads proposed in Table 24, the tests are expected to reach rupture in less than 15,000 hours. They have to be conducted to rupture with a record of the times corresponding to usual creep strain levels (0.1, 0.2, 0.5, 1 and 2%, at least). A record of the entire creep curve should be provided. In Table 24, creep tests at 500˚C performed for comparative characterization of creep properties of the new material are also expected to reach rupture in less than 15,000 hours.

3.2 Creep Stain Rate

At a temperature like 425˚C, past experience has shown that it is difficult to measure correctly the time for a given creep strain as the creep curves are very flat. The possible improvement of knowledge of the creep behavior at 425˚C concerns the creep strain rate. It is interesting to check that after a transient stage, the creep curves show zero or negligible strain rate. As there is no driving force for creep damage if the strain rate is zero, this can be a criterion for negligible creep conditions for the corresponding load and for lower loads. Table 23 defines two load levels for such tests at 425˚C. These tests are proposed with material in the as received condition, after post weld heat treatment (PWHT), and in an aged condition after PWHT (availability of material subjected to long term aging should be clarified). Tests of material in the last condition are aimed at checking whether or not aging reduces the resistance of the material to creep. It is anticipated that test conditions are: an aging temperature of 475˚C and a minimum duration of 10,000 hours (the aging conditions will have to be confirmed at the beginning of the test program).

3.3 Stress to Rupture at Moderate Temperature

At 450˚C and 475˚C, additional stress to rupture data will be valuable. Such tests are proposed in Table 23 with loads of 400 MPa and 350 MPa, at 450˚C and 475˚C, respectively. Additional tests with higher loads (425 MPa and 375 MPa, at 450˚C and 475˚C, respectively) are also proposed in case of non failure in 15,000 hours of tests under 400 MPa or 350 MPa.

3.4 Test Specimens and Number of Tests

It is recommended to perform all the creep tests with the same specimen size. If not, round robin tests should be carried out to demonstrate that results are independent of the specimen size. Tests listed in Table 23 and Table 24 should be performed for both product types listed in Section 2 (covering therefore two different heats). Depending on the results, it should be decided as to whether there is a need for duplicating some of the tests or adding another heat.

Table 53 - Proposal of Creep Tests on Mod 9Cr-1 Mo for Assessment of Negligible Creep Conditions

Temperature (˚C)	Stress (MPa)	Product Conditions	Comments
425	400	As received Simulated PWHT Simulated PWHT + aged	Evaluate the stationary creep rate (which should be hopefully zero). If aged material in as received condition, it might be necessary to foresee tests in three conditions for both stresses: as received, as received +aged, simulated PWHT.
	375	Simulated PWHT Simulated PWHT + aged	
450	425 (*)	As received Simulated PWHT	(*) Optional tests to failure to be performed if tests at 400 MPa stopped before failure
	400	As received	Test to failure or stopped at 15,000 h
		Simulated PWHT	
		Cyclically softened	
	375	Simulated PWHT	Target : 1 % creep strain
	350	As received	Target : 0.5 % creep strain
		Simulated PWHT	Target : 0.5 % creep strain
	325	Simulated PWHT	Target : 0.2 or 0.5 % creep strain
475	375 (*)	As received Simulated PWHT	(*) Optional tests to failure to be performed if tests at 350 MPa stopped before failure
	350	As received	Test to failure or stopped at 15,000 h
		Simulated PWHT	
		Cyclically softened	
	325	Simulated PWHT	Target : 1 or 2 % creep strain
	300	As received	Target : 0.5 or 1 % creep strain
		Simulated PWHT	Target : 0.5 or 1 % creep strain
	275	Simulated PWHT	Target : 0.2 or 0.5 % creep strain

Note:

(*) Expected creep strains should correspond to test durations < 15,000 h

Table 54 - Proposal of Creep Tests on Modified 9Cr 1 Mo for Extension of the Database

Temperature (˚C)	Stress (MPa)	Product Conditions	Comments
500	330	As received Simulated PWHT	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
	290	Simulated PWHT Cyclically softened	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
	260	As received Simulated PWHT	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
525	280	As received Simulated PWHT	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
	250	Simulated PWHT Cyclically softened	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
	220	As received Simulated PWHT	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
538	260	As received	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
		Simulated PWHT Cyclically softened	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
	230	Simulated PWHT	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
	200	As received	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains
		Simulated PWHT	Test to failure with records of times for 0.1, 0.2, 0.5, 1 and 2% creep strains

Note:

I. Tests should be performed either at 525˚C or 538˚C but not duplicated

II. A record of the entire creep curve should be provided

4 CREEP FATIGUE TESTS

The purpose of the proposed tests is to check that the negligible creep conditions are sufficient to prevent a significant reduction in fatigue life in creep fatigue conditions at 500˚C and 450˚C. The creep fatigue conditions can be preferred to a fatigue relaxation condition for the proposed tests in order to be more severe in terms of creep-fatigue life reduction. From data at 550˚C, it appears not necessary to perform the tests at small strain amplitudes: a range of 0.7% should be appropriate, which corresponds to an expected fatigue life of 5,600 and 4,700 cycles at 450˚C and 550˚C respectively. Increasing creep times shall be tested: 1 minute, 10 minutes and 1 hour. Longer creep times (5 hous) can also be tested but, in this case, it will be not mandatory to go to failure of the specimen.

The number of test results and the need for replicating tests on different heats will have to be addressed at the beginning of the test program. Tests should also cover the effect of post weld heat treatment.

5 CONCLUSIONS

The proposed test program includes two parts:

- The first part is devoted to increase the knowledge of creep behavior at moderate temperatures (425˚C–525˚C) in order to improve the evaluation of negligible creep conditions from different criteria.
- The second part is devoted to creep fatigue conditions with the purpose to evaluate the margin between negligible creep conditions at moderate temperatures (450˚C–500˚C) and conditions that produce a significant reduction in fatigue life.

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PART 4
PROPOSED TEST PROGRAM TO VALIDATE CREEP-FATIGUE PROCEDURES FOR MODIFIED 9CR-1MO

1 INTRODUCTION

This report has been prepared in the context of Task 3 of the ASME/DOE Gen IV material project. It completes the work performed in Reference [1] which, on the basis of creep-fatigue tests results available from Japan, Europe and the US, compared creep-fatigue procedures of ASME Subsection NH and RCCMR Subsection RB. The conclusions of reference 1 are as follows:

- It has been found that stresses at the beginning of hold times calculated according to the ASME procedure are far too high. The reason is that this procedure does not take into account cyclic softening and symmetrization effects. The proposal of Reference [1] consists of applying a reduction factor to the stress calculated on the basis of the isochronous stress-strain curves. The value of this reduction factor is based both on RCC-MR rules, in which these effects are taken into account, and on experimental results. As stresses at the beginning of hold times are available from experimental results, it does not seem that specific supplementary tests are needed to validate this point.

- In ASME NH, relaxation can be calculated based on isochronous stress-strain curves. It appears that this method is very conservative compared to experimental results. This conservatism can be reduced by performing relaxation analyses using a creep strain law in relation to a strain hardening or a time hardening hypothesis. The creep strain law used to construct the isochronous stress-strain curves could be used for that purpose. For design applications, the stress relaxation should be corrected to account for elastic follow-up effects.

The present ASME NH creep-fatigue damage envelope is very conservative in the case of Mod 9Cr-1Mo. ASME uses bilinear damage lines with (0.1, 0.01) as an intersection. On the basis of existing results, this diagram does not seem totally justified. For the analysis of true creep-fatigue interaction, tests where environment plays a role (tests in air environment and hold time in compression) should be eliminated. At 593˚C or 600˚C, true creep fatigue interaction can probably be studied, but at lower temperatures, 550˚C or 500˚C, representative environments and longer hold times must be considered.

The present report is aimed at defining tests to improve the understanding of Mod 9Cr-1Mo behavior and validate creep-fatigue procedures for this material. Further validation, outside the ranges of stress, strain and hold time accessible by experimental programs and representative of service conditions, should be based on visco-plastic constitutive equations whose development and validation is not addressed in the present test program.

2 MATERIAL

In the context of HTR–VHTR projects, Mod 9Cr-1Mo could be envisioned as a material for the Reactor Pressure Vessel (RPV) and for the RPV internals. Creep-fatigue tests should be performed preferably on products corresponding to the future procurement specifications for such projects. The following types of product forms will be relevant for HTR-VHTR projects:

- Forged parts with thickness of at least 200 mm
- Plates with thickness ranging from 30 to 140 mm (or more).

3 SUMMARY OF CREEP-FATIGUE TESTS RESULTS

Reference [1] provides a comprehensive collection of creep-fatigue test results. The following sections provide additional information based on an on-going test program in France.

3.1 Creep-Fatigue Tests in Air

Recent studies have confirmed that, at 550˚C, reduction in fatigue life of Mod 9Cr-1Mo due to hold times is not related to classical intergranular creep damage. The correct prediction of life of specimens tested with hold times both in tension [2] and in compression [3] is obtained when damage initiated by oxide layers is taken into account. Two types of oxide behavior have been observed, the more damaging one being related to the cracking of the oxide layer when the viscoplastic strain per cycle is large enough (mechanism 2). The more damaging effect of compressive hold times is confirmed and attributed to the tensile straining of oxide layer during the hold time when the base material is in compression. Mechanism 2 is more easily observed with compressive hold times than with tensile hold times.

3.2 Preliminary Results of Recent Creep-Fatigue Tests in Vacuum

The tests in vacuum performed during the above mentioned studies in France have confirmed the expected extension of pure continuous fatigue lives as compared to in air results. But surprisingly, no drastic improvement of fatigue life was obtained with tensile relaxation hold period. However, the more damaging effect of compressive hold times is suppressed. The explanation is as follows: under vacuum the above mentioned more damaging behavior of oxide layer (mechanism 2) is not acting but the other damage mechanism which is related to frequency effect (mechanism 1) is present as in air due to residual oxidation in imperfect vacuum when the cycle duration is increased.

Another conclusion is that tests in air produce, in all cases, pessimistic results when compared to tests in vacuum.

4 PURPOSE OF FUTURE TEST PROGRAM

4.1 Knowledge of Stress-Strain Behavior

A better knowledge of stress-strain behavior including cyclic softening, symmetrisation effect, cyclic relaxation and covering longer hold times is useful to validate modifications of the ASME creep-fatigue procedure. The improved knowledge of visco-plastic strain versus stress behavior is also necessary for an accurate evaluation of visco-plastic strains in cycles with long hold periods. As the creep-fatigue interaction is disturbed by oxidation, the reduction in the number of cycles to failure is more easily related to cyclic visco-plastic strain than to the classical creep damage based on stress to rupture (at least at 550˚C and at lower temperatures).

Tests should also help to support the validation of elastic follow-up factors to be used for assessing cyclic stress relaxation, as opposed to monotonic uniaxial relaxation.

4.2 Extension of the Relation of Number of Cycles to Failure versus Visco-Plastic Strain

In references [2]and [3], it is shown that a Manson Coffin best fit curve ($\Delta\varepsilon_p$ or $\Delta\varepsilon_{vp} = Nf^{\alpha}$) is applicable at 550˚C to continuous fatigue, fatigue relaxation and creep fatigue data. The data considered, however, were limited to 90 minute hold times for fatigue relaxation tests and 0.7% creep strain for creep fatigue tests. Extension of the Manson Coffin equation to longer times or larger visco-plastic strains should be one objective of the present program. It is expected that information on the temperature dependence of creep fatigue test results will be obtained from tests described in reference 4 and from tests at 500˚C or above as detailed in Section 5.

4.3 Characterization of Softened Material

There is a lack of knowledge on the mechanical properties of Mod 9Cr-1Mo in cyclically softened conditions. Tensile, short and medium term creep properties in this material condition can be of importance for design against accidental events. Creep properties of softened material might need to be considered in the revision of rules using creep damage evaluation. Finally, it is interesting to compare the properties of aged Mod 9Cr-1Mo to those of cyclically softened material. It may be possible to produce by aging microstructure and mechanical properties similar to those of the softened material. A higher aging temperature (600˚C– 650˚C) can be used to reach this condition after a reasonable time. It will be interesting to compare the creep-fatigue properties of such an aged material with the data obtained with the longest hold times.

4.4 Review of Creep-Fatigue Interaction Diagram

The margins arising from the creep-fatigue interaction diagram should be reviewed to cover the following points:

- A diagram assessed with 550˚C data and above could be too conservative for service at lower temperatures and, in particular, for service at a temperature corresponding to the negligible creep limit.
- A more realistic evaluation of creep damage taking into account creep properties in cyclically softened conditions could give a more satisfactory diagram.
- A diagram assessed with air data could be too conservative for other service environment and, in particular, sodium, which prevents oxidation.

4.5 Evaluation of Environmental Effect

As tests in vacuum appear not to be promising to provide a reference for environmental effect on creep fatigue, a program providing significant progress on this point is very difficult to define. Other means to suppress oxidation effect (such as using a coating) could be investigated.

The effect of environment on creep-fatigue life of specimens is difficult to check in an environment representative of service conditions (quality of water, steam, helium, nitrogen, sodium). As tests in environment should be dedicated to a particular part of a component in a chemically well defined medium, the corresponding conditions must be first identified in the context of a given project. Secondly, the selection of the most damaging environment may require screening tests; for instance, comparison of impure helium and air effects should be cross checked. Moreover, environmental effects, which appear to be significant on specimens, make questionable the transferability of data from specimens to mock ups and from mock ups to components. The behavior of thin walled components should not be far from that of specimens. In the case of larger parts, the moderate oxidation (mechanism 1) can be non significant whereas attention should be paid to early initiation of a few deeper cracks (mechanism 2).

5 PROPOSED TEST PROGRAM

5.1 Tests in Air at 500˚C or 525˚C

These tests will provide creep-fatigue data at a temperature lower than 550˚C. The choice between 500˚C and 525˚C is dependent on design applications. Two types of tests are proposed:

- Strain controlled tests as described in Table 55
- Tests under strain controlled conditions before hold times and stress controlled during hold time to achieve one given creep strain value (see Table 56).

The second type of tests has the advantage of accumulating more creep damage or more creep deformation and is easier to analyze due to the constant stress during hold times.

Table 55 - Fatigue-Relaxation Tests

Temperature (˚C)	500 or 525˚C
Total strain range $\Delta\varepsilon t$ (%)	0.5, 0.7, 1
Tensile relaxation hold period (minute)	10, 30, 60, 90, 120
Compressive relaxation hold period (minute)	10, 30, 60, 90, 120
Strain rate (%:s-1)	0.1 or 0.2

Table 56 - Creep-Fatigue Tests

Temperature (˚C)	500 or 525˚C
Strain range during the strain controlled part of the cycle (%)	0.5, 0.7, 1
Creep strain during stress controlled tensile hold time (%)	0.1, 0.3 (1)
Creep strain during stress controlled compressive hold time (%)	0.1, 0.3 (1)
Strain rate (%:s-1)	0.1 or 0.2
(1) At temperature lower than 550˚C, the expected creep strain per cycle must be adjusted to reasonable cycle durations.	

5.2 Long Term Tests in Air at 550˚C

It is suggested to perform the tests described in Table 57 and Table 58 which extend the data from references [2] and [3].

Table 57 - Fatigue-Relaxation Tests at 550˚C

Temperature (˚C)	550˚C
Total strain range $\Delta\varepsilon t$ (%)	0.4, 0.5, 0.7
Tensile relaxation period (minute)	90, 180
Compressive relaxation period (minute)	90, 180
Strain rate (%:s-1)	0.1 or 0.2

Table 58 - Creep-Fatigue Tests at 550˚C

Temperature (˚C)	550˚C	
Strain range during the strain controlled part of the cycle (%)	0.4, 0.5	0.7
Creep strain during stress controlled tensile hold time (%)	≥ 0.5 (1)	≥ 0.3 (1)
Creep strain during stress controlled compressive hold time (%)	≥ 0.5 (1)	≥ 0.3 (1)
Strain rate (%:s-1)	0.1 or 0.2	0.1 or 0.2

5.3 Tests on Softened Material

As indicated in Section 4.3, the priority in testing softened material is tensile, short and medium term creep tests at 550˚C. The quicker way to produce the greater softening effect is creep fatigue cycling (with stress controlled hold time as described in Section 5.1). A strain range during the strain controlled part of the cycle of 0.6 or 0.7% with a creep strain ε_{creep} of 0.5% during hold time will produce significant softening at mid lives (around 450 cycles) as detected by an increased creep strain rate analysis (ref. [2] and [3]). The volume of cyclically softened material, however, must be appropriate to provide tensile or creep specimens and further definition of the test program requires a concerted action with the laboratories in charge of the experiments.

5.4 Tests on Aged Material

The target is to reproduce the tensile strength and, if possible, the elongation obtained after cyclic softening in Section 5.3. A comparison of microstructure will also be performed. A first trial will be made by aging at 650˚C. In parallel aging at 600˚C will be started in order to replace aging at 650˚C if not satisfactory. Tests described in Section 5.2 and, if possible, in Section 5.1 will be replicated on aged material similar to cyclically softened one.

5.5 Tests in Reactor Environment

As pointed out in Section 4.5, tests in a reactor service environment require not only a representative medium but also mock-ups representative of the reactor component operations. A more precise definition of such tests requires more advanced design. As a first step, and with limitation to tests on specimens, it is proposed to replicate, in an agreed helium environment, the tests defined in Section 5.2 and, if possible, Section 5.1.

5.6 Tests on Post Weld Heat Treated Material

In order to prepare the analysis of creep fatigue tests on welded joints, some comparative creep fatigue tests shall be performed on post weld heat treated material. It is proposed as a first step to duplicate fatigue relaxation tests of Table 57

5.7 Creep Fatigue of Welded Joints

For welded joints, weld factors are used in the creep-fatigue damage evaluations. In the case of Mod 9Cr-1Mo, there are only preliminary proposals with stress to rupture weld factors decreasing from 1 to 0.76 for temperatures increasing from 425˚C to 650˚C. The first step to validate extension of creep fatigue rules to welded joints is the confirmation of stress to rupture factors by creep tests on representative welded joints. Then, some tests on cross weld specimens (pure fatigue + fatigue relaxation tests of Table 57) shall be performed to check if the modified creep damage evaluation (taking account of the presence of the weld) is enough or not to cover creep fatigue.

6 CONCLUSIONS

The work in reference [1] has shown the importance of the following points in relation with creep-fatigue of Mod 9Cr-1Mo:

- Cyclic softening and symmetrization effects
- Stress level at the beginning of hold time
- Treatment of relaxation based on isochronous stress-strain curves or creep strain law modified or not by an elastic follow-up factor
- Definition of the creep-fatigue interaction diagram.

The proposed program takes into account recent results which point out:

- The influence of oxidation in the effect of hold time on fatigue life at 550˚C
- The failure of tests in vacuum to provide pure creep fatigue data, free from oxidation effect.

The following aspects are treated by the proposed actions:

- Tests at 500˚C or 525˚C for comparison with data at 550˚C
- Extension of the data base at 550˚C with tests with longer hold times
- Characterization of cyclically softened material and comparison with thermally aged material
- Effect of reactor environment and in priority, tests in impure helium
- Tests after post weld heat treatment and comparison with data of as received material
- Screening tests on cross weld specimens.

REFERENCES

[1] Report 12-9045964-001, "Improvement of ASME NH for Grade 91 (Creep-Fatigue)," Part 2 of this report.

[2] B. Fournier, M. Sauzay, C. Caës, M. Noblecourt, A. Bougault, V. Rabeau and A. Pineau, "Creep Fatigue-Oxidation Interactions in a 9Cr-1Mo Martensitic Steel–Part I: Effect of Tensile Holding Period on the Fatigue Lifetime," to be published in International Journal of Fatigue.

[3] B. Fournier, M. Sauzay, C. Caës, M. Noblecourt, A. Bougault, V. Rabeau and A. Pineau, "Creep Fatigue-Oxidation Interactions in a 9Cr-1Mo Martensitic Steel–Part II: Effect of Compressive Holding Period on the Fatigue Lifetime," to be published in International Journal of Fatigue.

[4] Report 12-9047093-001, "Proposed Test Program to Assess Negligible Creep Conditions of Modified 9Cr-1Mo Grade," Example reference 2, Part 3 of this report.

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ABBREVIATIONS AND ACRONYMS

ANTARES	AREVA New Technology based on Advanced Gas Cooled Reactor for Energy Supply
ASME	American Society of Mechanical Engineers
ASME ST-LLC	ASME Standards Technology, LLC
ASTM	American Society for Testing and Materials
B&PV	ASME Boiler and Pressure Vessel
CRIEPI	Central Research Institute of Electric Power Industry
DOE	US Department of Energy
EPRI	Electric Power Research Institute
HTGR	High Temperature Gas-cooled Reactors
HTR	High Temperature Reactor
IGCAR	Indira Gandhi Centre for Atomic Research
JAPC	Japan Atomic Power Company
JNC	Japan Nuclear Cycle Development Institute
LMFBR	Liquid Metal Fast Breeder Reactors
ORNL	Oak Ridge National Lab
RPV	Reactor Pressure Vessel
VHTR	Very High Temperature Reactor
SHT	Strain Hardening Theory

S_y	Yield Stress
S_m	Time Independent Allowable Stress
t_i	Time Duration
t_{id}	Allowable Time Duration
E	Young's Modulus

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