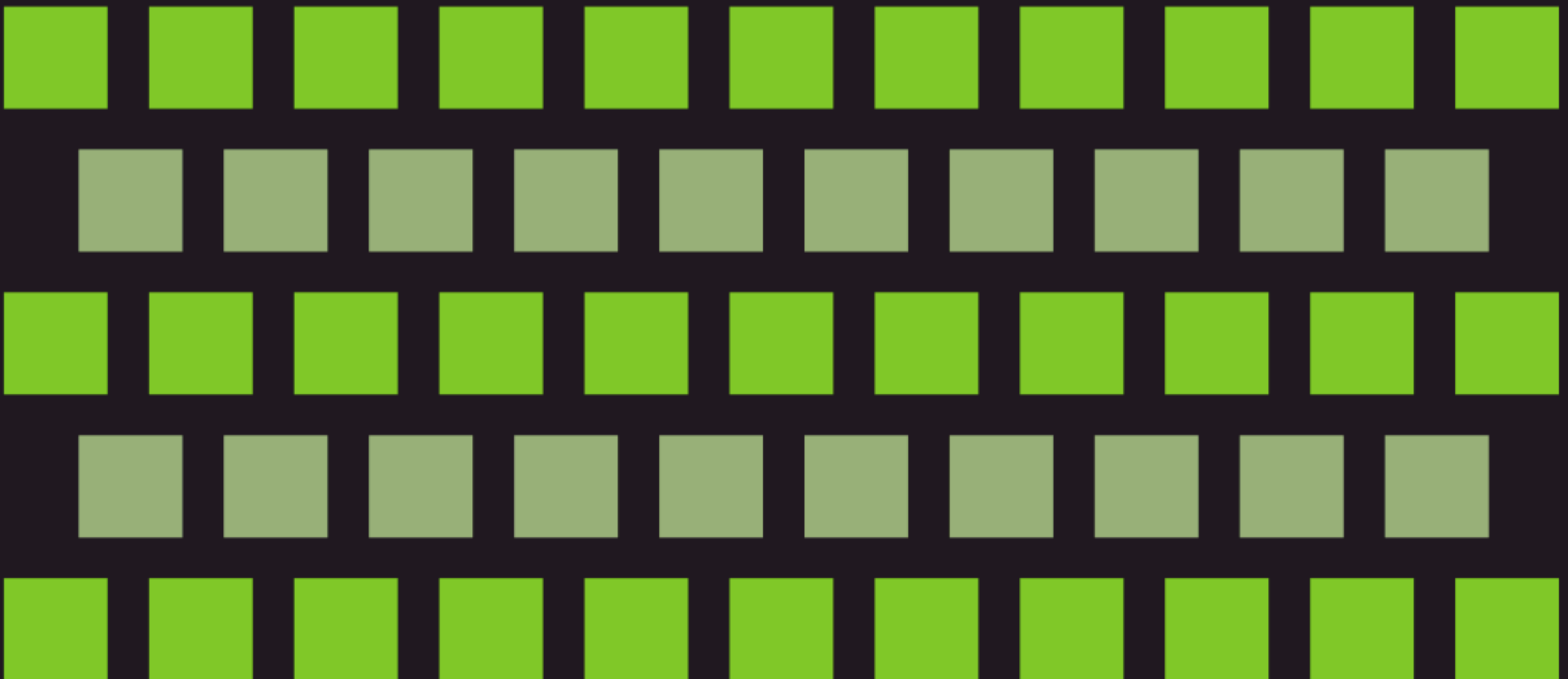


STP-PT-027

# EXTENDED LOW CHROME STEEL FATIGUE RULES



**STP-PT-027**

# **EXTEND LOW CHROME STEEL FATIGUE RULES**

*Prepared by:*

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Pressure Vessel Research Council



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## FOREWORD

This document was developed under a research and development project which resulted from ASME Pressure Technology Codes & Standards (PTCS) committee requests to identify, prioritize and address technology gaps in current or new PTCS Codes, Standards and Guidelines. This project is one of several included for ASME fiscal year 2008 sponsorship which are intended to establish and maintain the technical relevance of ASME codes & standards products. The specific project related to this document is project 07-04 (BPVC#2), entitled, “Extend Low Chrome Steel Fatigue Rules.”

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## ABSTRACT

In this report material models were examined for hardening/softening and creep behavior based on available material data sources. Creep and multi-axial effects will be considered. Analytical studies will be explored for typical components using these models. Based on the results, recommendations for an approach to develop fatigue design rules and suitable design factors will be made. Investigation should include consideration of 1-1/4, 2-1/4 and 9 to 12 Cr alloys.

A recommendation was made for developing a technical program for extending the current ASME Section VIII fatigue rules to higher temperatures to address fatigue design aspects for components operating at temperatures approaching the creep range. Vessels where this is commonplace occur in the refining industry; therefore, this development work is of high interest to the petrochemical industry.

## 1 INTRODUCTION

The impetus for this activity arises because the new ASME B&PV Code, Section VIII, Division 2 rules permit high strength materials of the type enumerated to be used to temperatures above 700°F and into their respective creep ranges. A life limiting failure mode is potentially the phenomenon of “creep-fatigue.” We shall define a “creep-fatigue” failure as one in which life is shorter than that expected due to either creep or fatigue acting on a structure independently. This occurs in those regimes of stress, strain-rate, time and temperature where the damage mechanisms due to creep and fatigue can be expected to damage the same microstructure and property characteristics. Creep-fatigue is of concern especially where there may be time-dependent straining and where varying stresses (loads, including start-up and shut down) are among the design conditions.

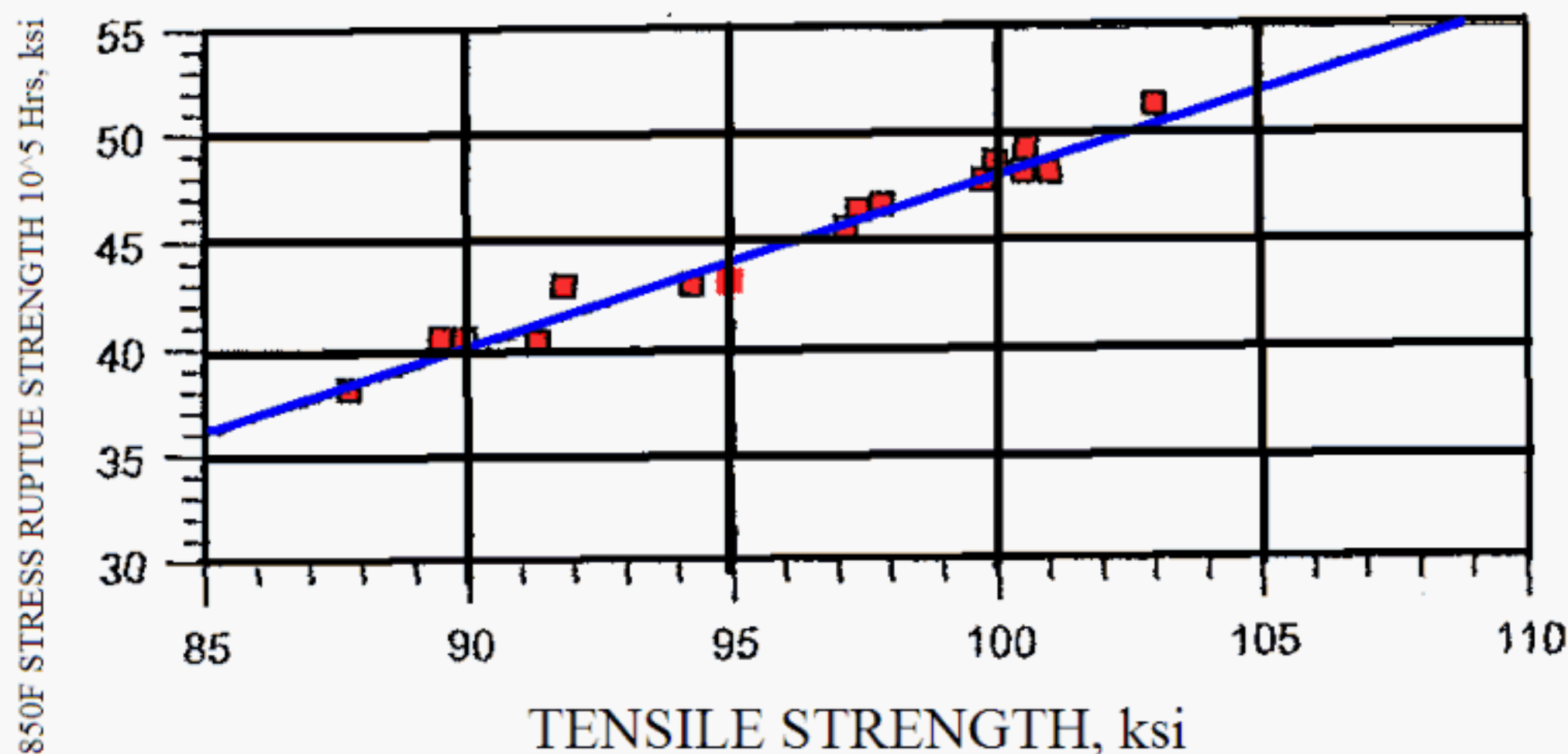
Comprehensive and correct creep-fatigue design rules are needed now for the aforementioned alloys because, under the new Section VIII, Division 2 rules, as the respective creep ranges of the materials are approached, in many cases the allowable stresses are significantly higher than those for which there is applicable service experience that would permit exempting design details from fatigue analysis based on documented “years of relevant experience.” The same must be said for any new alloys and applications for which there is literally no relevant service experience.

In summary then, the combination of new materials and applications for advanced energy systems with higher allowable stresses and increased design temperatures requires an understanding of creep-fatigue not now available, analytical models to explain and express damage accumulation and relevant test data in order that new, justifiable and correct rules may be developed.



## 2 MATERIALS

Relatively high strength alloys such as the very popular 2 ¼ Cr-1Mo-V (22V) and modified 9 Cr-1Mo-V-Cb-N (91) achieve their superior properties through accelerated cooling of these hardenable alloy steel compositions from high (normalizing) temperatures, transformation of the microstructure to martensite or bainite followed by tempering. For these materials, the specified minimum ambient temperature yield and tensile strengths are 60 and 85 ksi, respectively. Corresponding maximum respective yield and tensile strength values may range up to about 85 and 110 ksi. Typical values of strengths in finished pressure vessels are likely to be about 70 ksi yield and 92 ksi tensile. For the ranges of room temperature strengths usually expected, the time-dependent stress-rupture and creep properties increase directly as shown in Figure 1 for the 100,000 hour stress-rupture strength at 850°F for the 22V material.



**Figure 1 - The Effect of Tensile Strength on the  $10^5$  Hour Stress Rupture Strength at 850°F for 2 ¼ Cr-1Mo-V Alloy. [3]**

Elevated temperature straining of the alloys under consideration during creep exposure or cyclic stressing will lower the tensile strength and hardness, alter the optimal microstructure from that obtained by proper heat treatment and reduce the creep life. This behavior is well known and has been reported for decades in studies of 1Cr-1Mo-V turbine rotor steels and, more recently, in studies of the modified 9Cr-1Mo-V alloy used in many power piping and similar applications.

Figure 2 below contrasts strain softening behavior of a high strength Cr-Mo-V alloy with that of a strain hardening material such as a low tensile strength austenitic stainless steel or a conventional low tensile strength ferritic steel. Data on the latter types of materials are not useful in developing the approach to creep-fatigue design sought in this ASME project for the strain softening materials such as the accelerated cooled and enhanced 1-1/4, 2-1/4 and 9 to 12 Cr alloys.

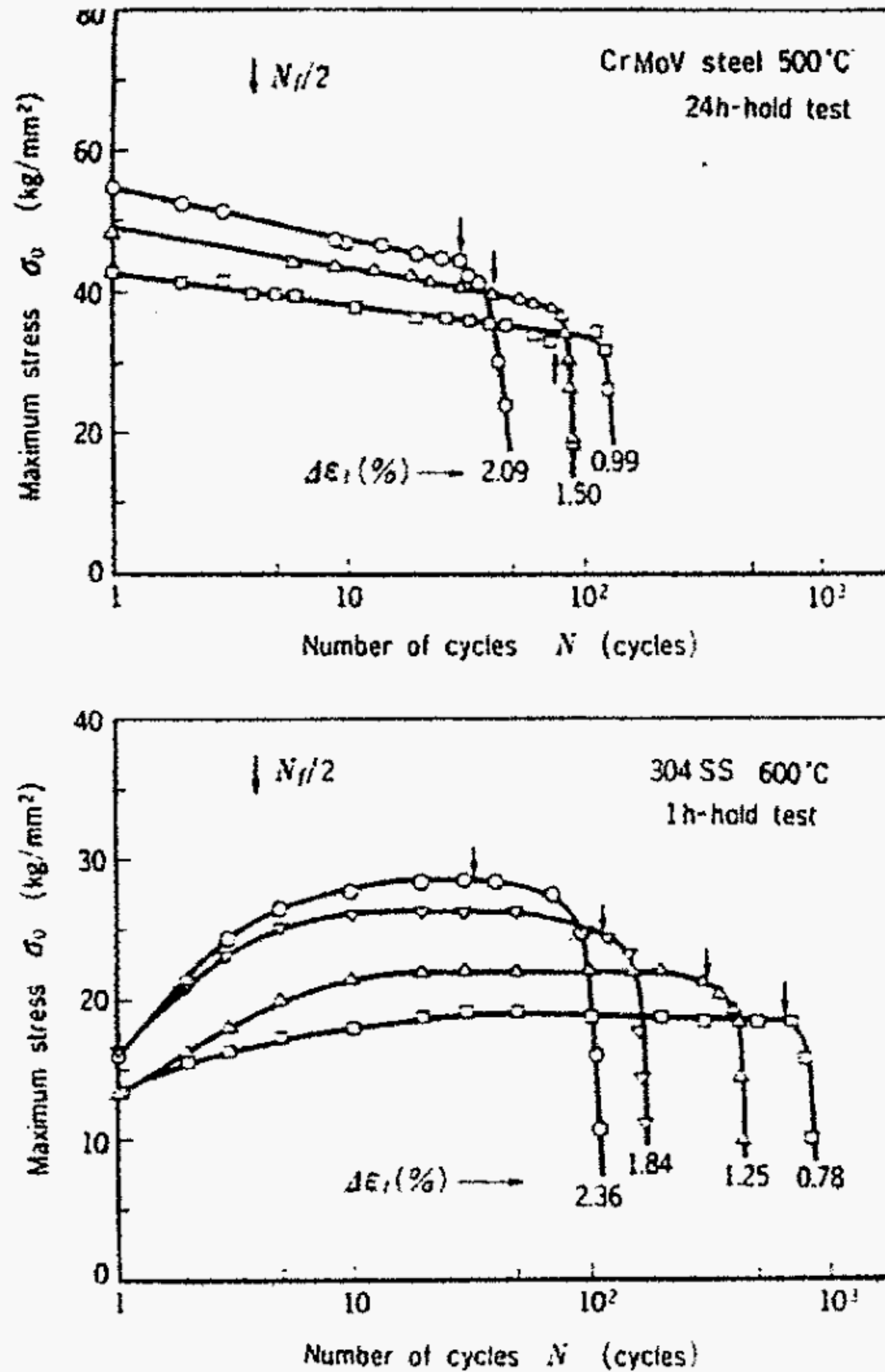


Figure 2 - Cyclic Softening and Hardening Behavior are Illustrated. High Strength Cr-Mo-V Alloys of Interest Here Display the Softening Behavior in the Upper Plot.

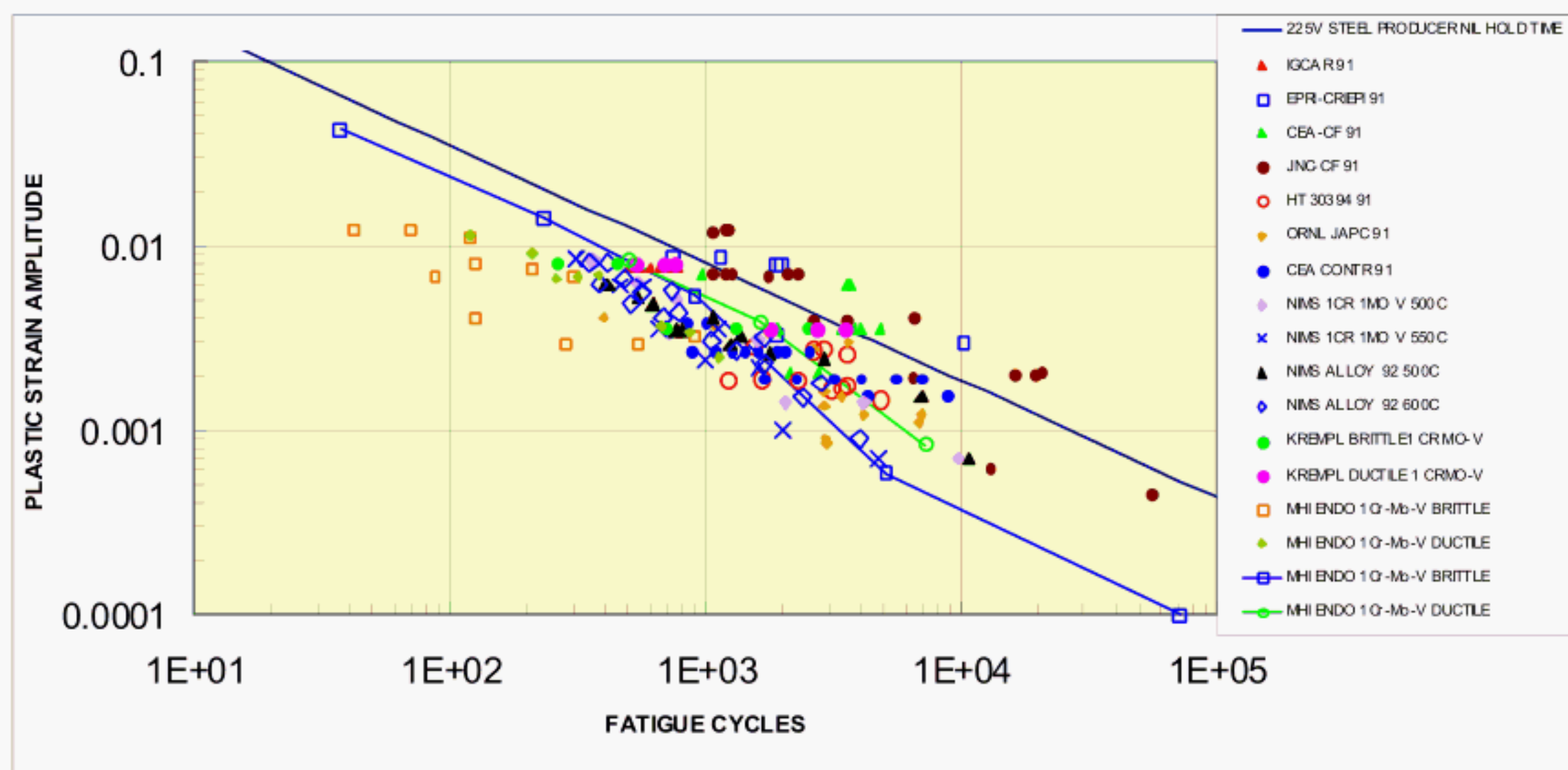


### 3 CREEP-FATIGUE DATA

Creep-fatigue data have been developed in tests utilizing many combinations of strain range, hold time, temperature and load measurement. For the most part, creep-fatigue tests are run with loads that cycle between compressive and tensile and with tensile hold periods ranging from seconds to times exceeding a few minutes, but rarely more than an hour. Plastic strain amplitudes typically do not exceed 1-2 percent and are usually only a fraction of 1 percent. The total number of cycles applied before failure or a specific load reduction is reached may extend into the thousands, but because of the high cyclic frequency, the total time of exposure may be only tens or, at most, a few hundreds of hours.

For the purpose of this study, creep-fatigue data on several of the strain softening alloys were gathered from many sources. The data from which the plastic strain range may be estimated are shown in Figure 3. Included in the plot are some data from tests that show the effects of tensile hold times. Most of the data are from relatively high frequency tests where the accumulated time at creep temperature is very short. It appears that longer hold time tests result in fewer numbers of cycles, i.e. there is a creep-fatigue interaction. A line provided by a producer of 2 ¼ Cr-1Mo-V alloy, shown in Figure 3, did not include significant hold time effects.

The most widely scattered points in the figure are for hold time tests of one brittle heat of an alloy for which the “no hold time” tests were also mainly outside the scatter band. It is not expected that pressure vessel alloys of interest in this project will behave in a creep brittle manner when tested in uniaxial tension.

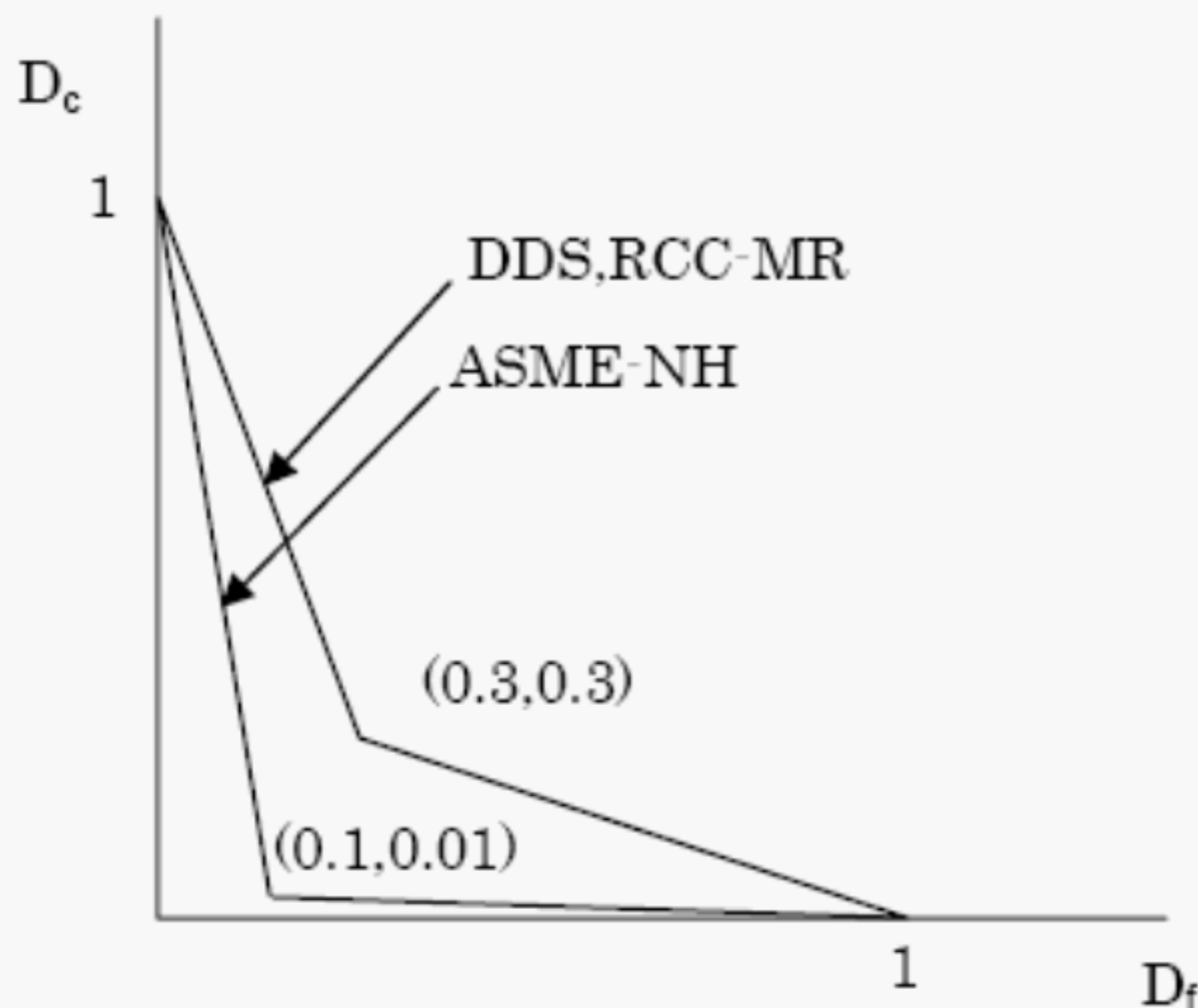


**Figure 3 - Assembled Creep-fatigue Data for Strain Softening Alloys Showing Similarity of Behavior.**

#### 4 CREEP-FATIGUE INTERACTION

Creep-fatigue test results are often plotted on an “interaction diagram” of the type shown in Figure 4 for the 91 alloy. The data supporting the ASME NH line for the 91 alloy lie close to the horizontal axis and suggest that short hold time fatigue loading severely negatively influenced creep life. However, such test data emphasize fatigue loading whereas high-temperature pressure vessel service would normally be expected to be creep dominated, i.e. representative data would be more closely aligned with the Y axis. Little data of that type is available because of the long test durations required and the corresponding increase in cost of data acquisition.

Specifically, pressure vessel service can be better simulated by tests to demonstrate how a relatively small number of cyclic loads would shorten creep life (i.e test results would plot close to the Y axis). The severe effect of fatigue damage on creep life indicated by the NH lines in Figure 4 might be attributed in large part to a high level of strain softening that occurs with these alloys in hundreds or thousands of strain cycles which reduce the tensile strength and thereby degrade the creep properties. Data reported from creep-fatigue tests of the type shown in Figure 3 seldom, if ever, include post mortem information on the material properties or microstructural changes due to cycling. However, acceleration of softening behavior associated with creep straining and the life reduction that goes along with it are well known from interrupted stress-rupture tests of the subject alloys. What is needed is modeling and quantification of the effects.



**Figure 4 - Creep-fatigue Interaction Diagram of Type Used by International Codes for the Strain Softening Alloy 91.**

## 5 A MODEL FOR CREEP-FATIGUE IN PRESSURE VESSEL APPLICATIONS

We can start to develop a model for creep-fatigue interaction beginning with any expression for creep strain rate that may be modified to describe the increase of strain and strain rate with time (creep damage) and, eventually, with cycling. It is immaterial what model is chosen for the strain computation as long as it includes an explicit term for strain rate that we can propose will be accelerated by cycling or other damage related to fatigue. It is also important that the function can be integrated to obtain stress rupture life.

Then, we can start with strain rate,  $\dot{\epsilon}$ , defined as follows:

$$\dot{\epsilon} = d\epsilon / dT \quad (1)$$

For usual applications in which primary creep due to steady loading at allowable stresses is small, we can choose to only express tertiary creep behavior as follows:

$$\dot{\epsilon} = \dot{\epsilon}_0 e^{\Omega \epsilon_c} \quad (2)$$

where:

$\epsilon_c$  = accumulated creep strain

$\Omega$  = a coefficient characterizing the rate strain rate increases with creep strain absent fatigue

$\dot{\epsilon}_0$  = initial strain rate for the period of evaluation

Note that in the model here the notion of “secondary” creep is not applicable. The secondary behavior observed in laboratory testing may be viewed as the sum of decreasing primary and increasing tertiary components which together add up to an approximately constant “minimum.”

For the strain-softening, fatigue-damaging materials of interest we may describe the acceleration of creep rate due to cycling with the following relation

$$\dot{\epsilon} = \dot{\epsilon}_0 e^{\Omega \epsilon_c} \cdot e^{\beta \epsilon_p N' T} \quad (3)$$

where:

$\beta$  = creep strength reduction factor / unit strain / cycle

$\epsilon_p$  = Plastic strain per cycle

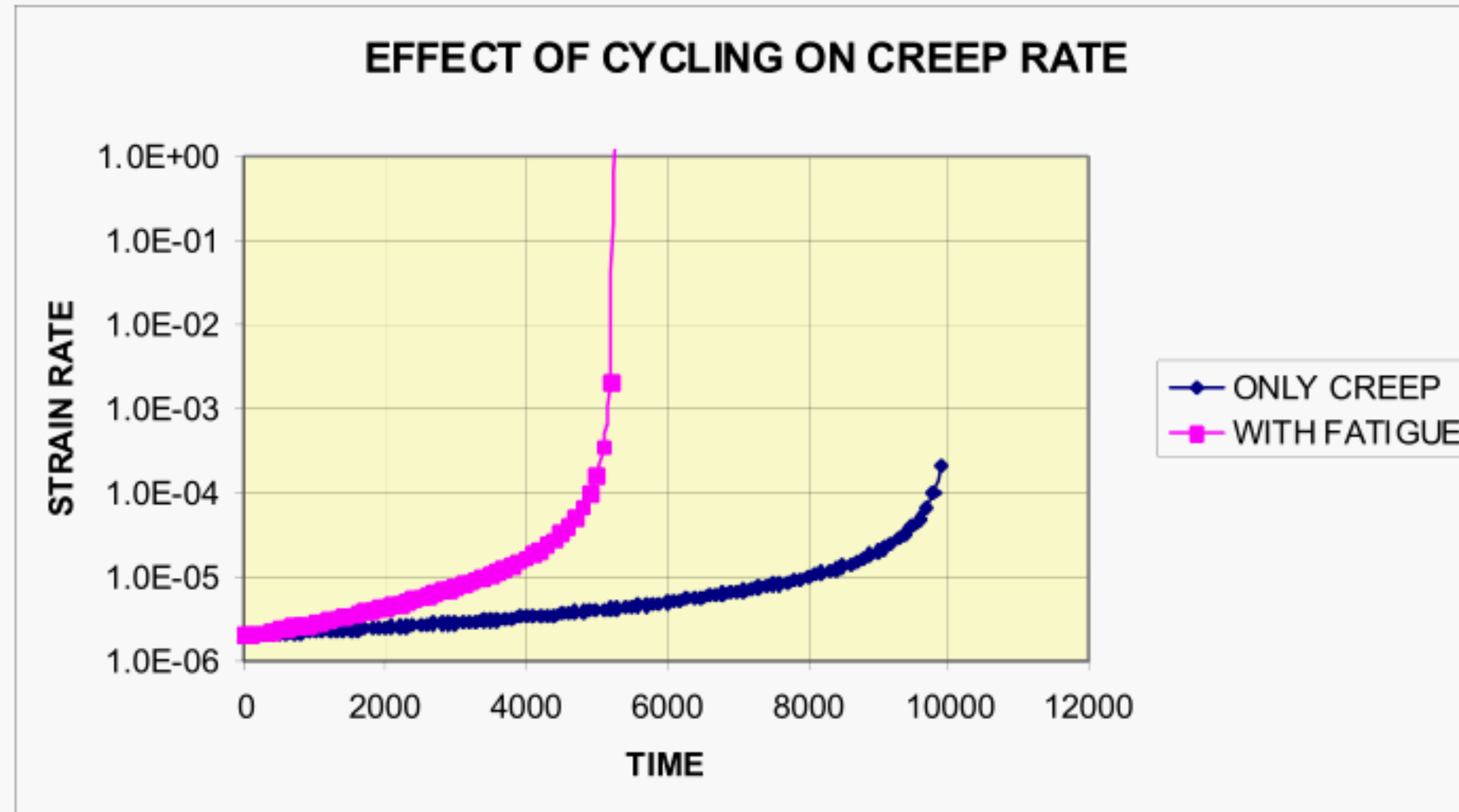
$N'$  = cycle frequency or 1/hold time

$T$  = elapsed time

It should be apparent that  $N'T$  indicates the number of cycles accumulated in the time  $T$  which can be a variable of integration to obtain total life.

The evolution of creep rate described in the equation above may be shown as in Figure 5 below where the shortening effect on creep life is also seen.





**Figure 5 - Cyclic Straining in a Creep-fatigue Test Will Accelerate the Creep Strain Rate and Thereby Shorten Creep Life.**

The reduction of stress rupture life is obtained by integrating the above relation.

$$(1 - e^{-\varepsilon_f \Omega}) / \dot{\varepsilon}_0 \Omega = (e^{\beta \varepsilon_p N' T_f} - 1) / \beta \varepsilon_p N' \quad (4)$$

where:

$T_f$  = creep life with fatigue

$\varepsilon_f$  = fracture strain

The life absent fatigue,  $T_r$ , is estimated as follows:

$$(1 - e^{-\varepsilon_f \Omega}) / \dot{\varepsilon}_0 \Omega = T_r \quad (5)$$

Or simply

$$T_r = (e^{\beta \varepsilon_p N' T_f} - 1) / \beta \varepsilon_p N' \quad (6)$$

Rearranging terms leads to

$$e^{\beta \varepsilon_p N' T_f} - 1 = \beta \varepsilon_p N' T_r \quad (7)$$

And, finally, life with fatigue is

$$T_f = \ln(\beta \varepsilon_p N' T_r + 1) / \beta \varepsilon_p N' \quad (8)$$

It is useful to recognize that the above relation between life with and without fatigue can take several forms. For example, starting with the relation derived above and multiplying and dividing the indicated terms on the right side by  $T_f$  gives

$$T_f = \ln(\beta \varepsilon_p N' T_f T_r / T_f + 1) / \beta \varepsilon_p N' (T_f / T_f) \quad (9)$$

setting:

$$\beta \varepsilon_p N' T_f = \alpha \quad (10)$$

then:



$$T_f = \ln(\alpha T_r / T_f + 1) / (\alpha / T_f) \quad (11)$$

where we see then simply

$$\alpha = \ln(\alpha T_r / T_f + 1) \quad (12)$$

then:

$$e^\alpha = \alpha T_r / T_f + 1 \quad (13)$$

$$e^\alpha - 1 = \alpha T_r / T_f \quad (14)$$

finally:

$$T_f = \alpha T_r / (e^\alpha - 1) \quad (15)$$

re-expanding:

$$T_f = \beta \varepsilon_p N T_r / (e^{\beta \varepsilon_p N} - 1) \quad (16)$$

or, for a set of variable amplitude cycles applied throughout life:

$$T_f = \beta \sum \varepsilon_{p,i} T_r / (e^{\beta \sum \varepsilon_{p,i}} - 1) \quad (17)$$

where:

$\sum \varepsilon_{p,i}$  = sum of the plastic strains

The above are convenient, powerful, compact and simple relations showing how, under fatigue conditions, creep-fatigue life at a location is related to hold time (cycling rate), plastic strain range, the stress rupture life absent fatigue and a material constant,  $\beta$ , and how to deal with variable amplitude conditions. The first three of these terms can be determined or specified by design activity.

The material constant,  $\beta$ , can reasonably be inferred by examining the performance of the alloy (or similar materials) in fatigue tests of sufficient hold-time duration that most of the associated creep-fatigue process damaging microstructural interactions can occur.

Another very useful feature of the equations is that the designer may calculate life absent fatigue and then apply a histogram of strain cycling to calculate in a simple step the corresponding life with fatigue. Such life should be computed taking into account multiaxial creep effects. Finally, by benchmarking  $\beta$  against actual test results it will take into some account any other damage interaction mechanisms in addition to softening that may occur in the subject alloy at the temperature of interest.

## 6 CREEP-FATIGUE DAMAGE AND EVALUATING $\beta$

Defining:

$D_c$  = relative creep life that has been shortened by fatigue =  $T_f/T_r$

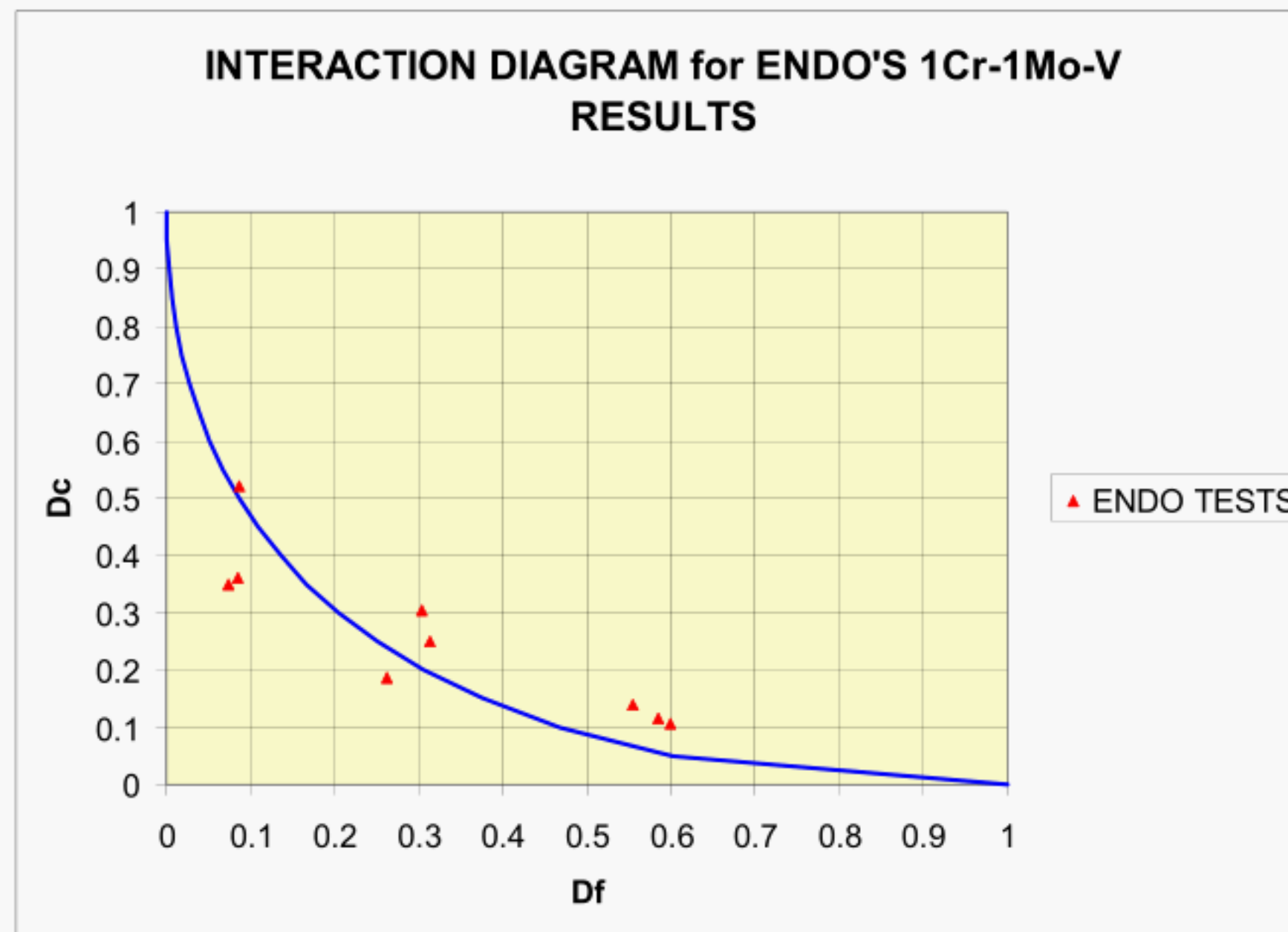
And similarly:

$D_f$  = relative fatigue life =  $N/N_f$

Then:

$$D_c = \ln(\beta \epsilon_p N' T_r + 1) / \beta \epsilon_p N' T_r \quad (18)$$

Most of the fatigue test data on pressure vessel steels in Figure 3 did not include the desired effects of hold times. However, alloys of the 1Cr-1Mo-V type have long been studied by electrical equipment manufacturers for turbine rotor applications and by MPC (in creep-fatigue interspersed tests). For example,  $D_c$  values for Endo's experimental creep-fatigue test results are shown in Figure 3 for hold times of 1, 6 and 24 hours and several plastic strain ranges. They were fit to the above equation to solve for  $\beta$  numerically using the corresponding creep rupture times. The  $\beta$  values obtained were in the range of 2.0 and the life results were not very sensitive in that range. Calculation of the corresponding values of the reduction in fatigue strength,  $D_f$ , in the hold time tests showed a strong creep-fatigue interaction depicted in Figure 6.



**Figure 6 - Interaction Diagram Indicates Strong Creep- Fatigue Interaction for Endo's Data Shown in Figure 3.**

From Figure 6 above it is apparent that

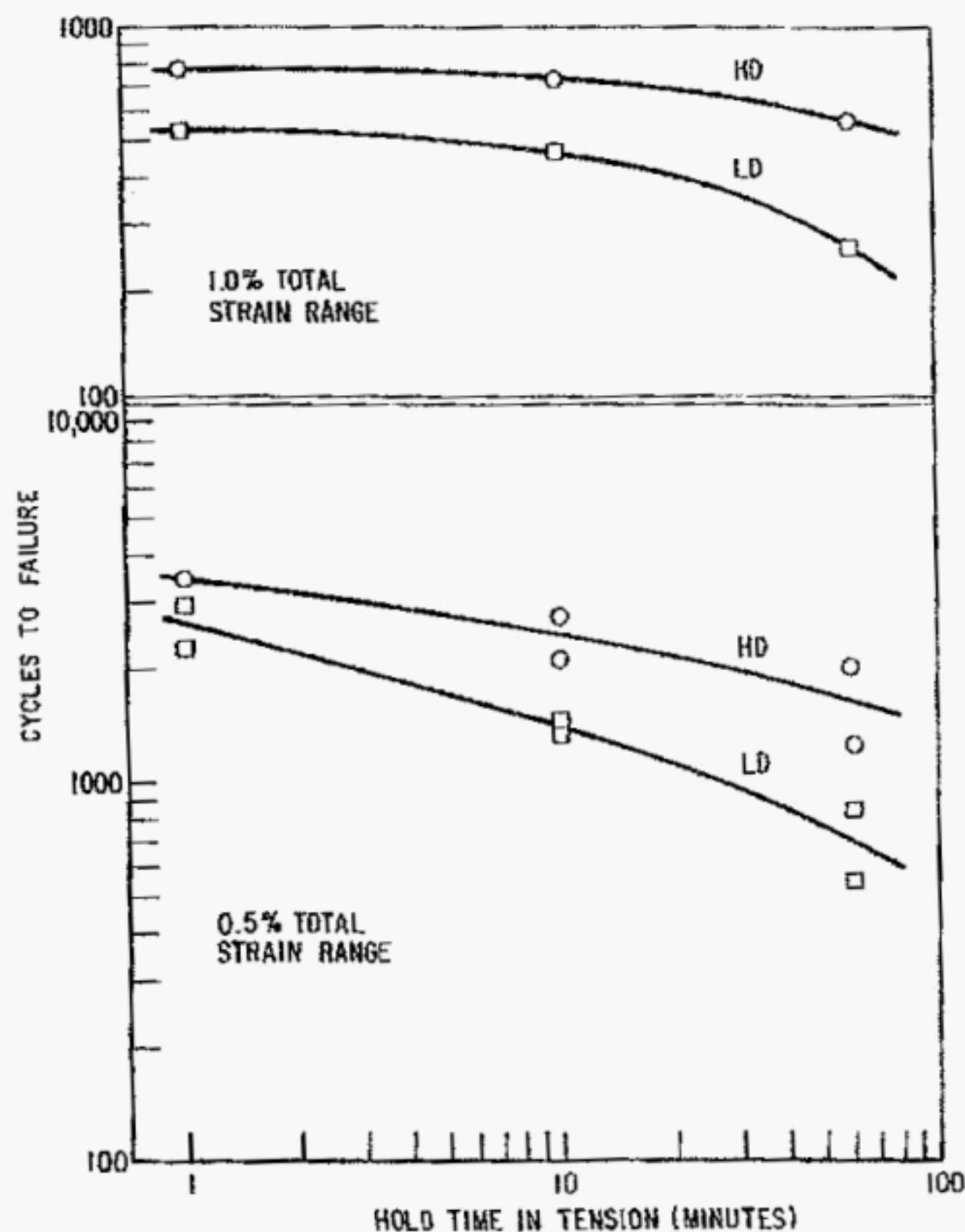
$$D_c + D_f \neq 1 \quad (19)$$

Instead the data indicates a strong interaction of creep and fatigue damage. It appears that the relation

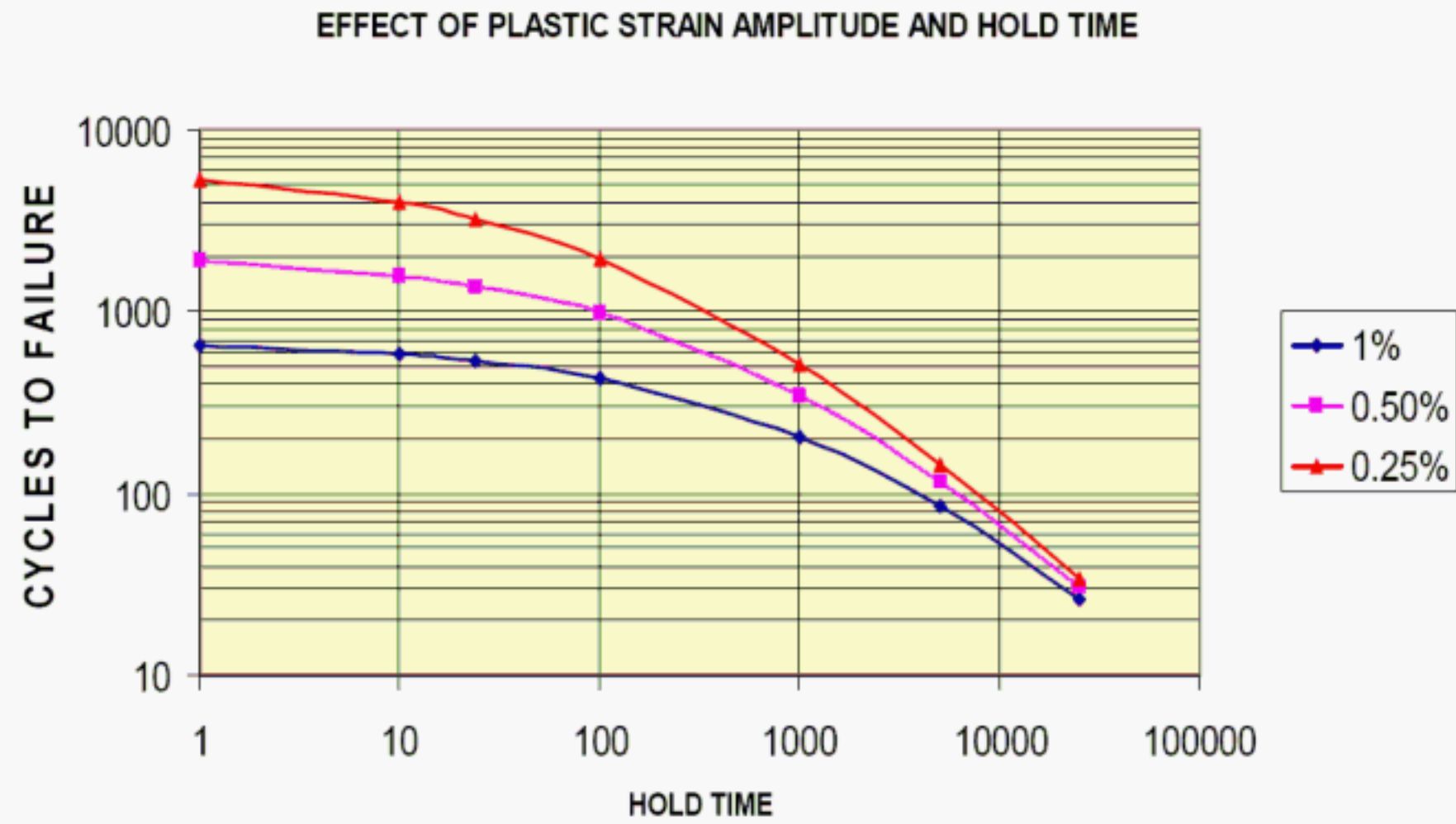
$$\sqrt{D_c} + \sqrt{D_f} = 1 \quad (20)$$

is a better fit for the data and this relation is plotted as the curve shown in Figure 6. However, no physical significance is ascribed to the square root relation at this time and other fractional exponents may be used to describe negative effects of interaction.

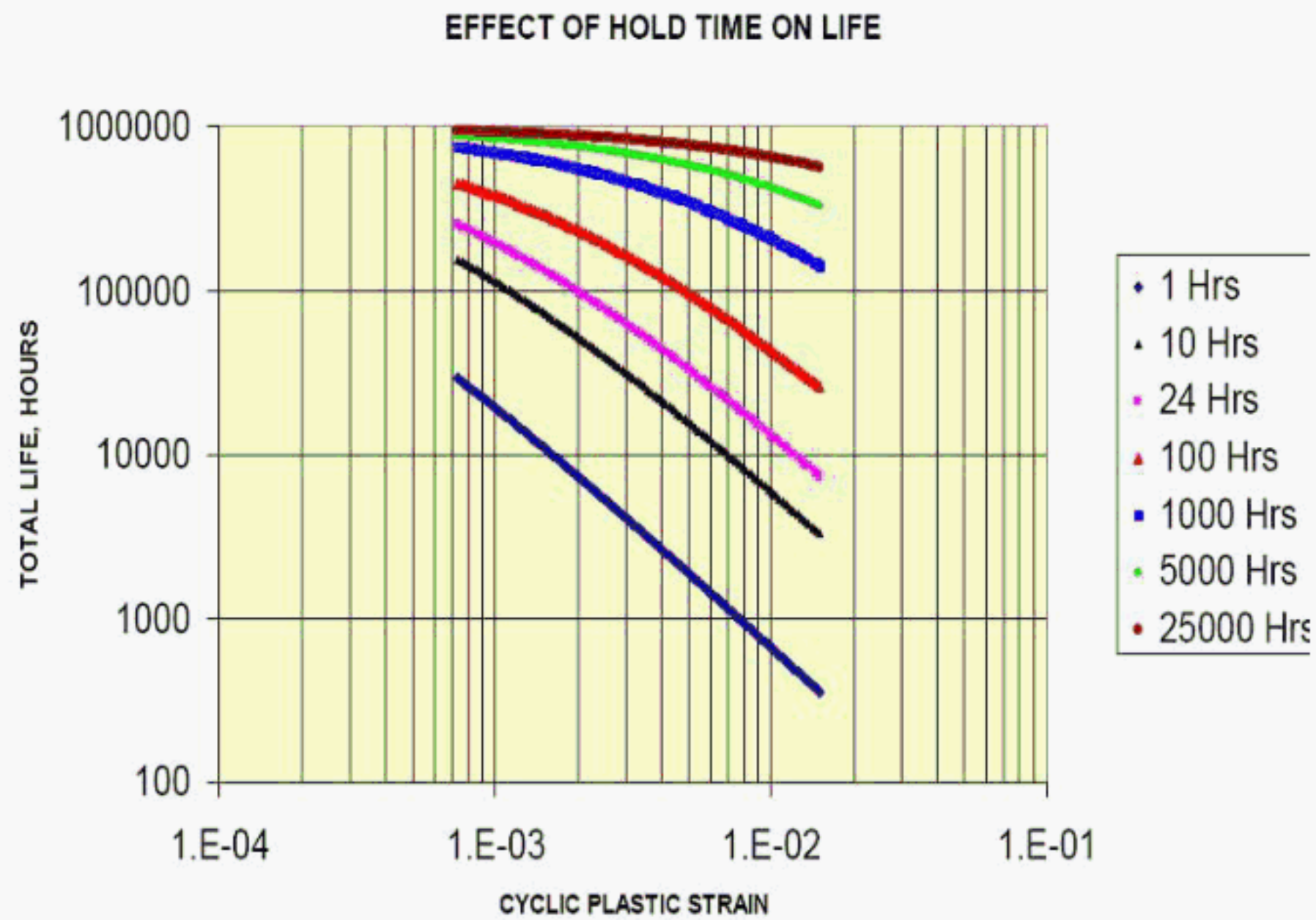
The effect of hold time was also studied by Krempl for GE and MPC. Figure 7 below presents some of his work on heats of high and low ductility (HD and LD respectively). It can be seen that in a fatigue test of an individual heat, increasing the creep hold time diminishes the number of cycles to failure, but, on reflection, it should be realized, increases life in hours. The answer to the question, "What is the effect of hold time on life?" is, "It increases life measured in hours, not cycles." In a pressure vessel, longer average hold times clearly mean there can be fewer cycles in any given period. This inverse relation is demonstrated in Figure 8 and Figure 9 below. Increasing hold time for the examples illustrated results in fewer cycles and therefore less fatigue damage and longer life.



**Figure 7 - Krempl's Study of the Effect of Hold Time on High and Low Ductility Materials.**



**Figure 8 - Predictions of Hold Time Effects on Cyclic Life for 3 Plastic Strain Amplitudes.**



**Figure 9 - Total Life Increases With Fewer Cycles, i.e., Longer Hold Time.**



## 7 THE DESIGN CURVE

The design curves offered here for 2 ¼ Cr-1Mo-V (22V) by way of example show in ASME fashion the total elastic “stress” amplitude calculated by multiplying the elastic plus plastic strains by the relevant modulus at the design temperature. The earlier derived expression for total life under creep-fatigue conditions repeated below provides the basis for calculating the design number of cycles.

$$T_f = \ln(\beta \epsilon_p N' T_r + 1) / \beta \epsilon_p N' \quad (21)$$

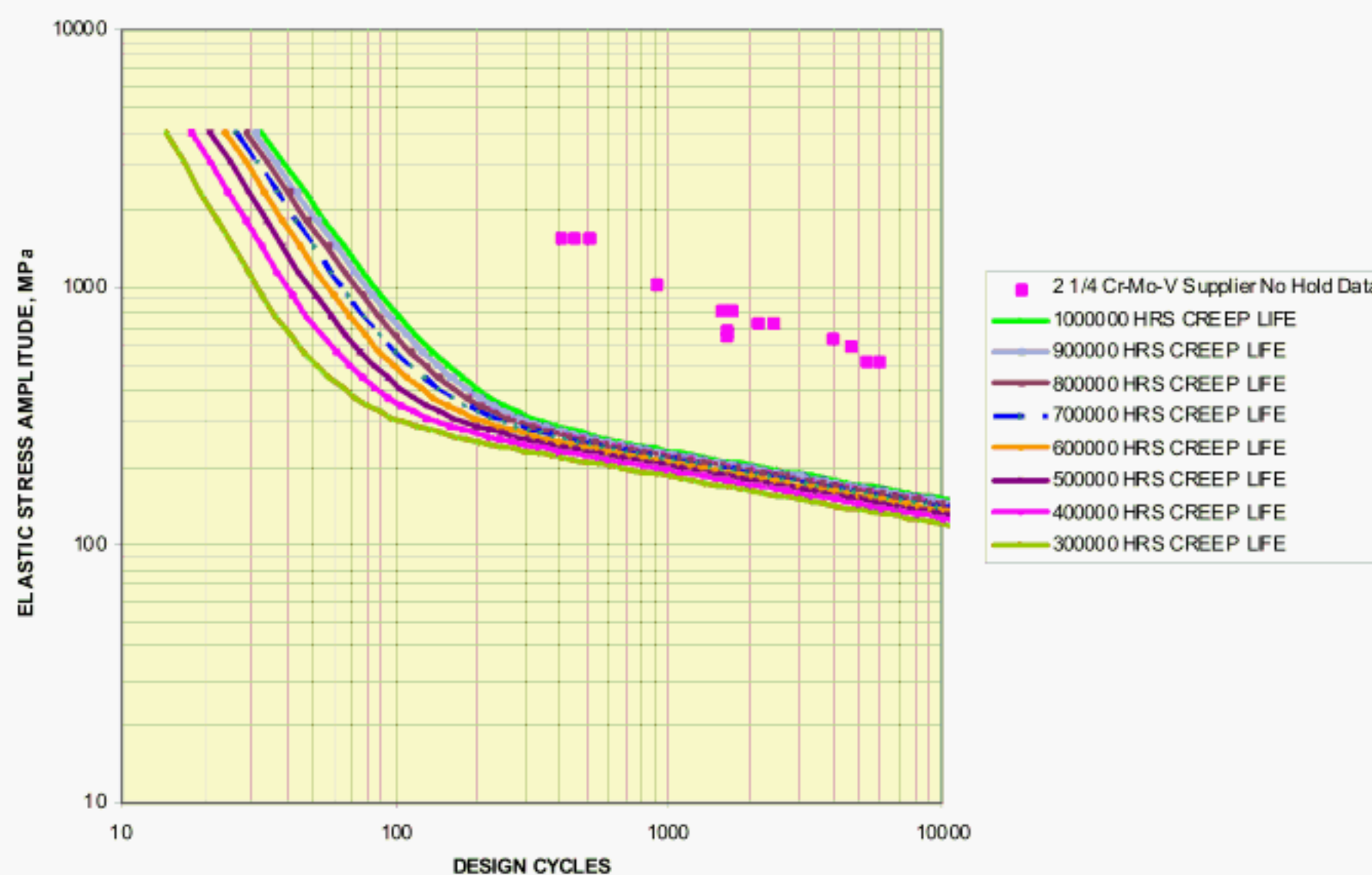
Rearranging gives for  $N_{\text{design}}$

$$N' T_f = \ln(\beta \epsilon_p N' T_r + 1) / \beta \epsilon_p = N_{\text{design}} \quad (22)$$

The curves presented in Figure 10 start with a hold time of 15,000 hours for the maximum plastic strain amplitude covered (2%). The hold time was reduced by the one third power of the plastic strain to permit more cycles at low strain amplitudes wherein less damage is done per cycle. The strain damage factor,  $\beta$ , was kept at the benchmarked value of 2 observed for the 1 Cr Mo V alloy as noted above.

For specified values of plastic strain amplitude, the number of design cycles is calculated using the above equation for several conservatively calculated rupture lives. The stress for each value of plastic strain was computed using a simple work hardening law based on minimum specified room temperature properties reduced to the values at 850°F. Work hardening was based on a plastic strain of  $1 \times 10^{-6}$  at 30 ksi (about 2/3 of yield) and 0.002 at 48 ksi, about the 0.2% offset stress. The strain hardening coefficient was then calculated to be 0.0618.

The design curves bear a striking offset relationship from the test line for 2 ¼ Cr-1Mo-V. This is remarkable since the “no hold time” test results were not used in the modeling. The appearance derives perhaps from the fact that hold time data on a similar Cr-Mo-V alloy was used in benchmarking the proposed model.



**Figure 10 - Fatigue Cycles Dependent on Creep Life With Comparison to No Hold Time Fatigue Tests.**

Creep-fatigue interaction for the design lines was examined using the baseline no hold time tests to estimate  $N_f$

where:

$$D_f = \text{fatigue damage} = N/N_f$$

and

$$D_c = \ln(\beta \epsilon_p N' T_r + 1) / \beta \epsilon_p N' T_r \quad (23)$$

Figure 11 presents the results calculated for a typical design line. Also shown for comparison are hold time test results from the literature. The design line points concentrated on the Y axis reveal allowance for a strong creep-fatigue interaction as demonstrated by the data available.

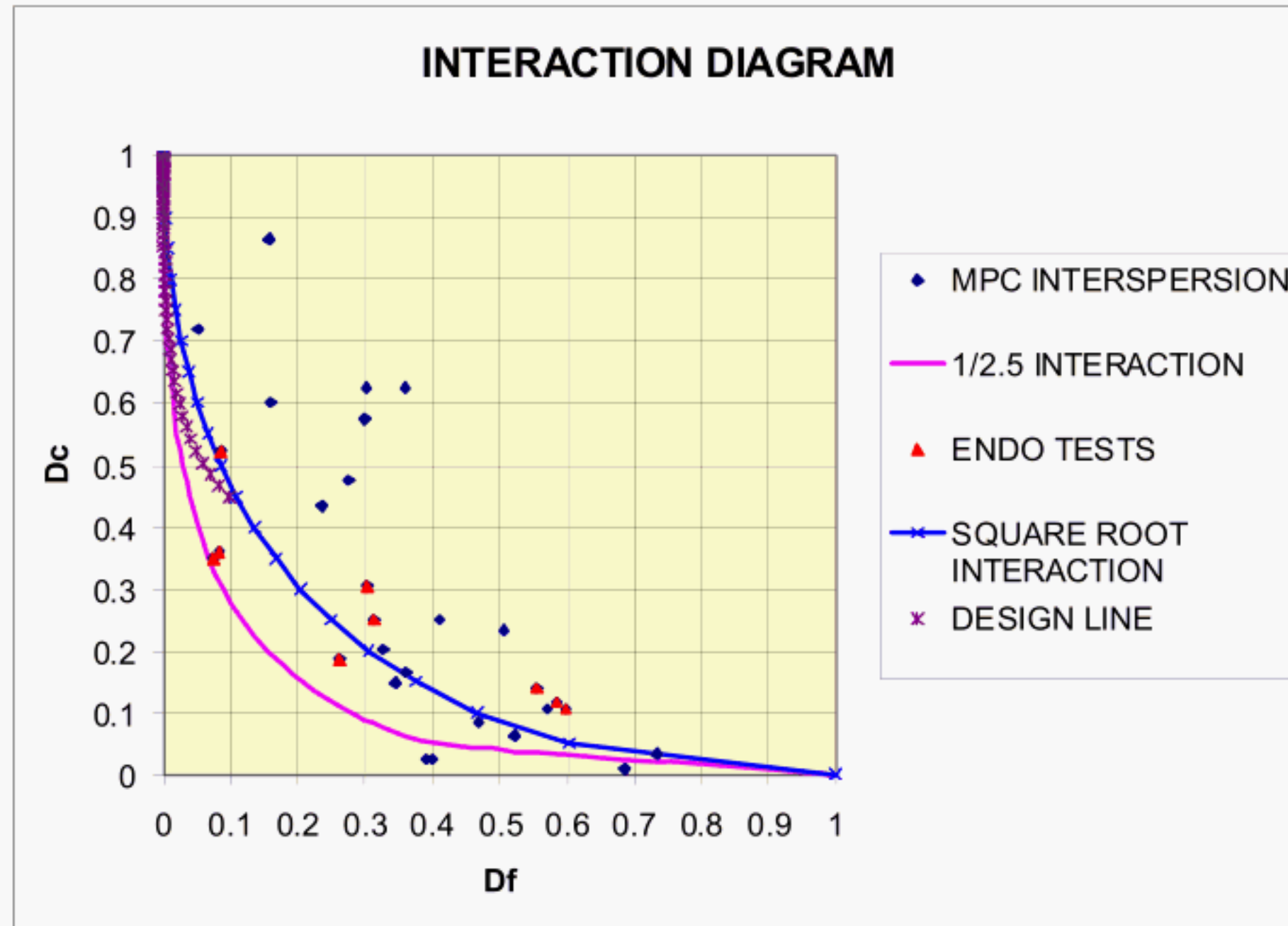
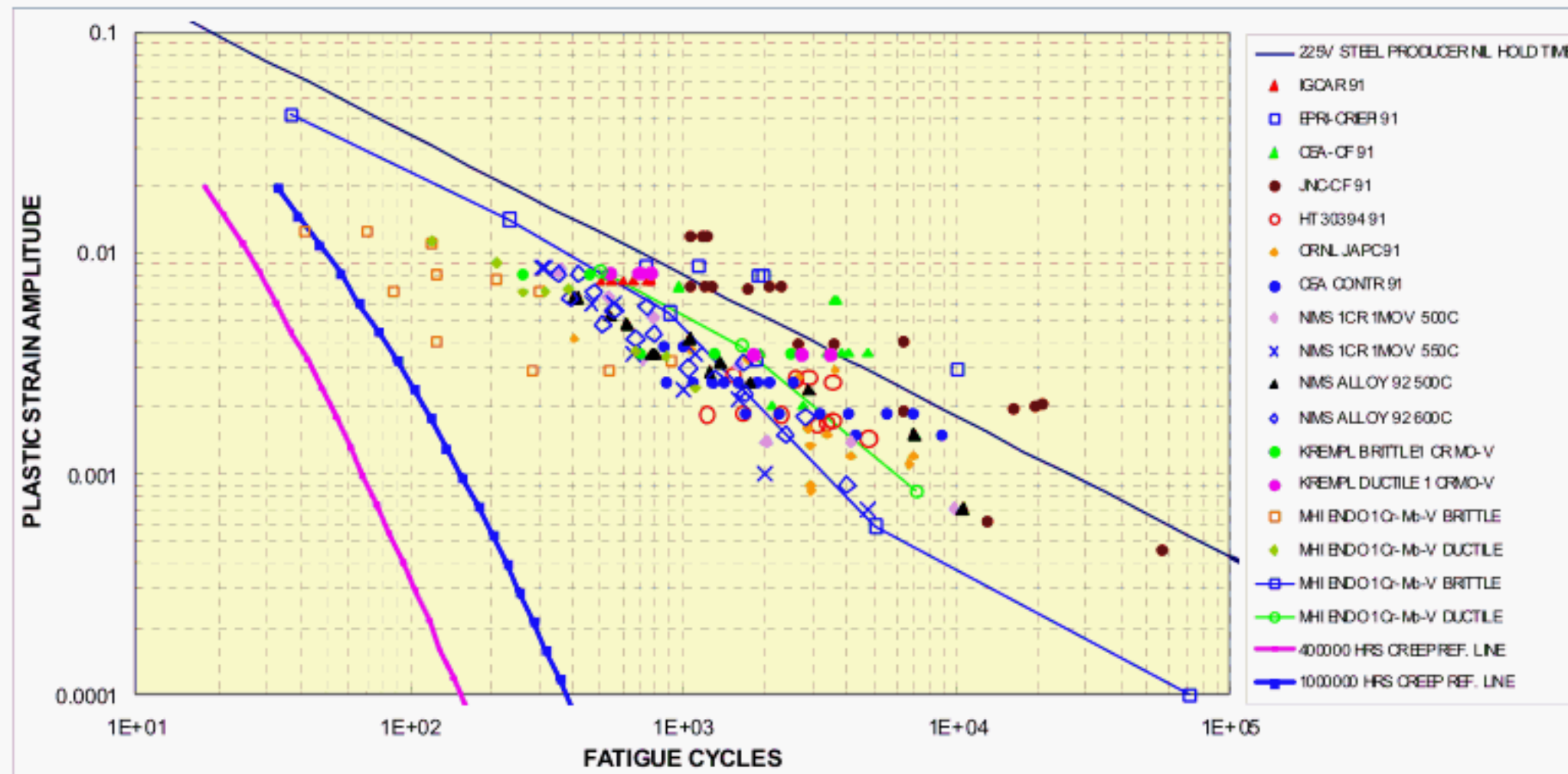


Figure 11 - Comparison of Design Line and Experiments.

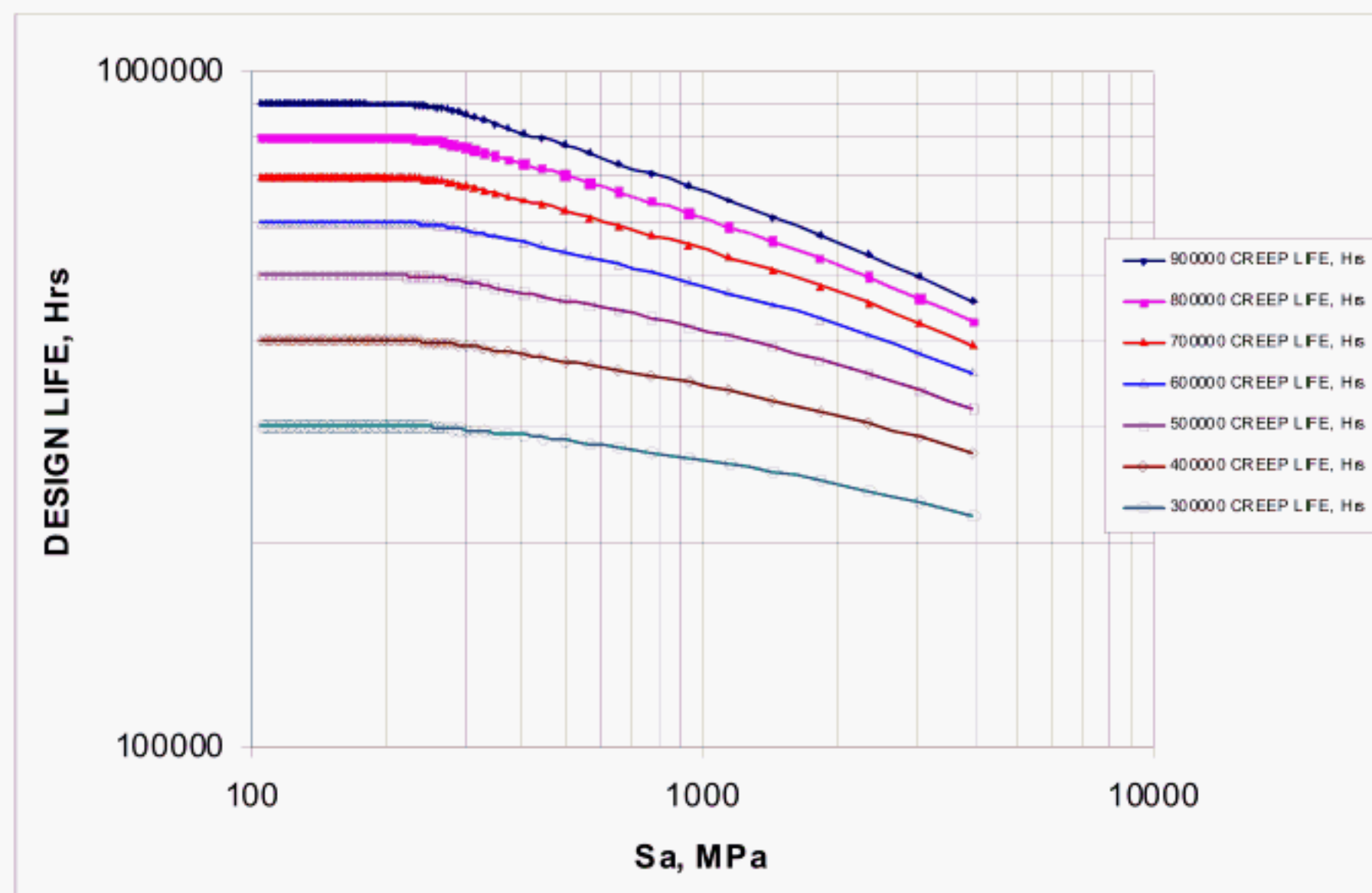


## 8 COMMENT ON MARGINS

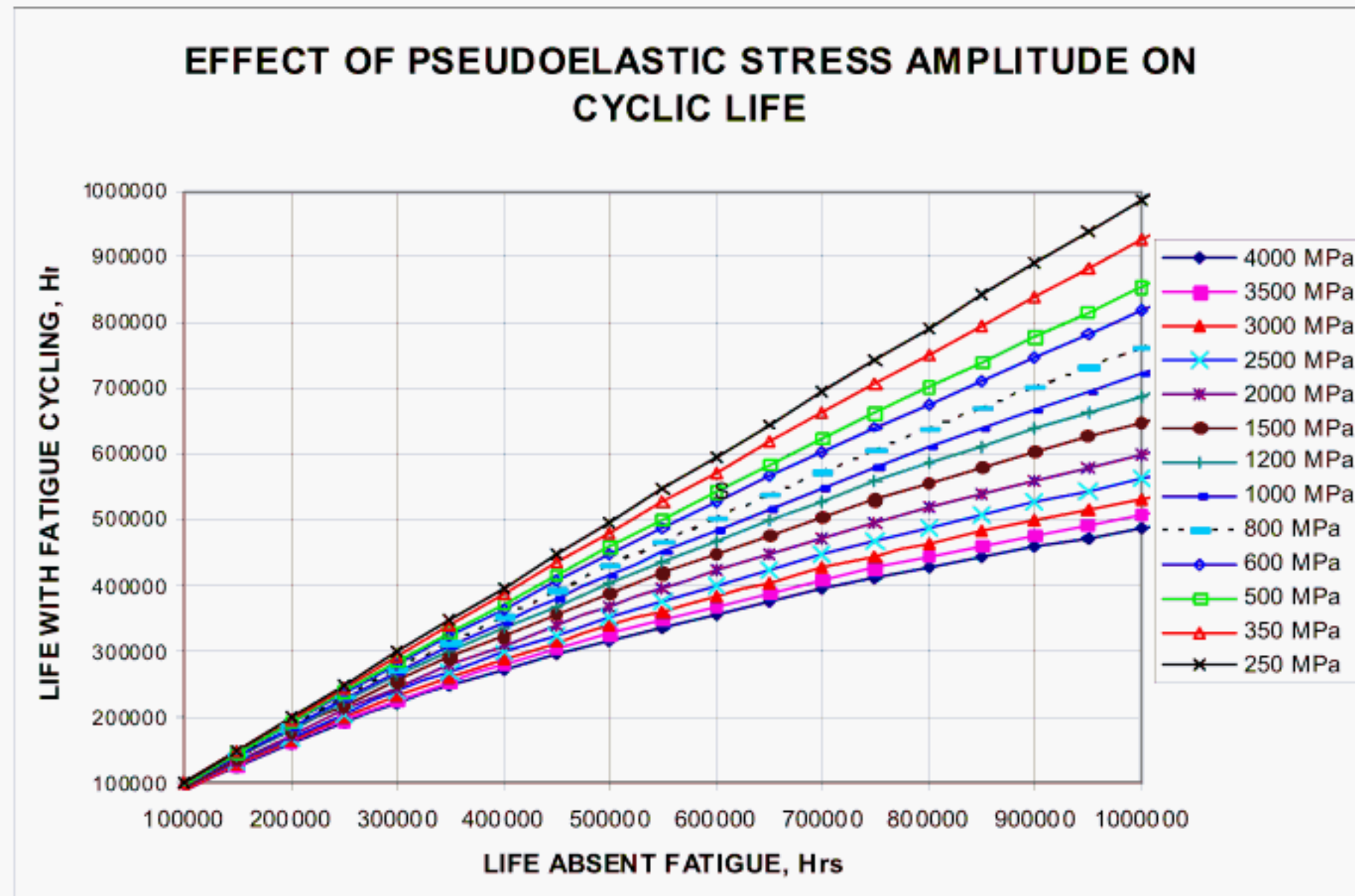
In practice the creep life calculation, which is an essential input to Figure 10, should be based on minimum material properties combined with a conservative multiaxial life calculation procedure applied to specified conservative design conditions. Figure 12 presents examples of the design lines as compared to the available reference creep-fatigue data. Figure 13 and Figure 14 indicate the relation between lives with and without fatigue cycling as functions of the pseudoelastically calculated stress amplitudes as used in the ASME procedure.



**Figure 12 - Hold Time Creep-fatigue Data as Compared to Design Lines Indexed to Stress Rupture Life Absent Fatigue. Only the Very High Strain Results on Brittle Material Approach the Design Curves.**



**Figure 13 - Reduction in Life Associated With Increase in Pseudoelastically Calculated Stress Amplitude.**



**Figure 14 - Comparison of Life With and Without Fatigue Cycling for Various Pseudoelastically Calculated Stresses.**

## 9 PROPOSAL FOR TEST PROGRAM

The equation below provides a basis for developing data to validate or improve the model offered herein. It can be used to estimate the failure times and the effects of the key variables. It is proposed that at least two materials, say 22V and 91, be used and that at least one material be tested at 2 levels of tensile strength and possibly at 2 temperatures.

$$T_f = \ln(\beta \varepsilon_p N' T_r + 1) / \beta \varepsilon_p N' \quad (24)$$

For planning the tests it is assumed that  $\beta$  is equal to 2 and the stress used is not too high (a short test) nor too low (an expensively long test). We therefore assume that the stress rupture life without fatigue  $T_r$  will be about 10,000 hours.

Table 1 and Table 2 below give predicted lives in cycles and hours for the above stipulations. The degree to which the trends in the test results differ from the predictions should allow fine tuning or modifying the model offered herein.

**Table 1 - Creep-fatigue Test Matrix (hours)**

**Sample Creep-fatigue Test Matrix, Life in Hours**

Cycle Time		Plastic Strain Amplitude				
Days	Hours	0.015%	0.250%	0.5%	1.0%	2.0%
2	48	9700	6852	5405	3941	2680
4	96		8050	6852	5405	3941
7	168		8754	7846	6587	5116
14	336			8754	7846	6587
28	672				8754	7846

Beta=2, Tr=10,000 hours

**Table 2 - Creep-fatigue Test Matrix (cycles)**

**Sample Creep-fatigue Test Matrix, Life in Cycles**

Cycle Time		Plastic Strain Amplitude				
Days	Hours	0.015%	0.250%	0.5%	1.0%	2.0%
2	48	202	143	113	82	56
4	96		84	71	56	41
7	168		52	47	39	30
14	336			26	23	20
28	672				13	12

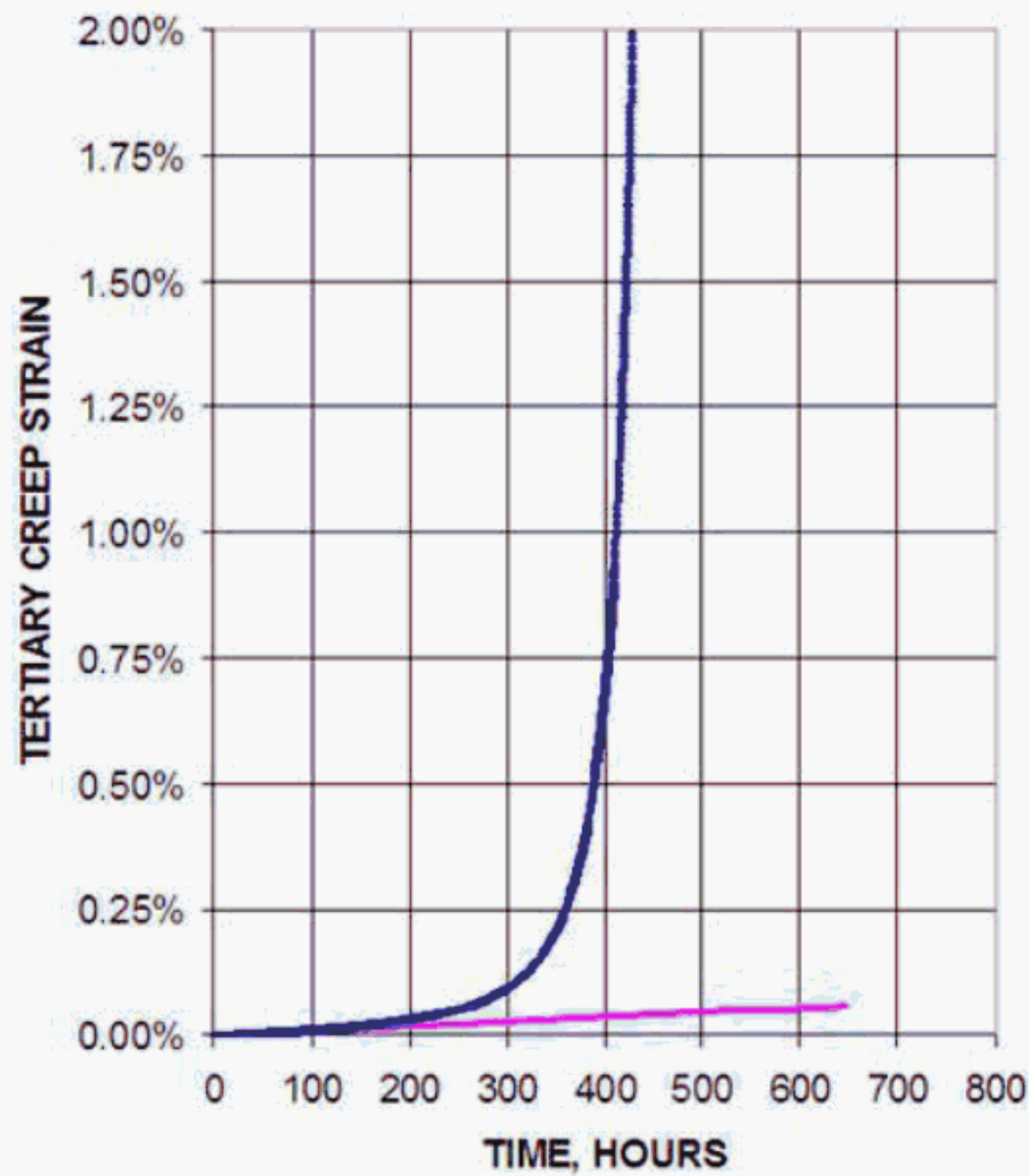
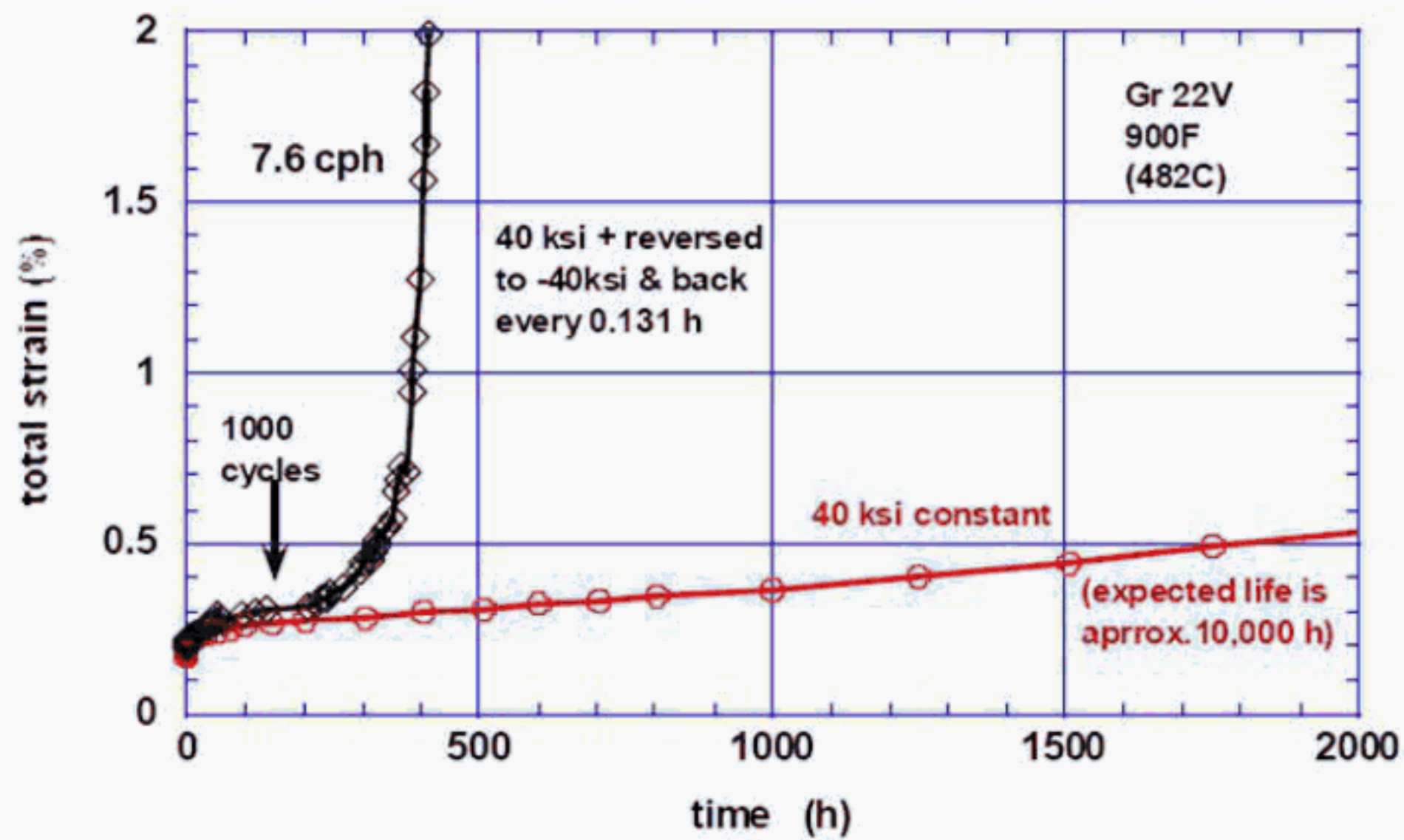
Beta=2, Tr=10,000 hours

It must be recognized that significant statistical variation (scatter of data) occurs in stress rupture tests, elevated temperature fatigue tests and materials properties. Therefore, sufficient testing must be conducted to establish trends and a few simple spot checks will not be sufficient to validate or improve the proposed model.

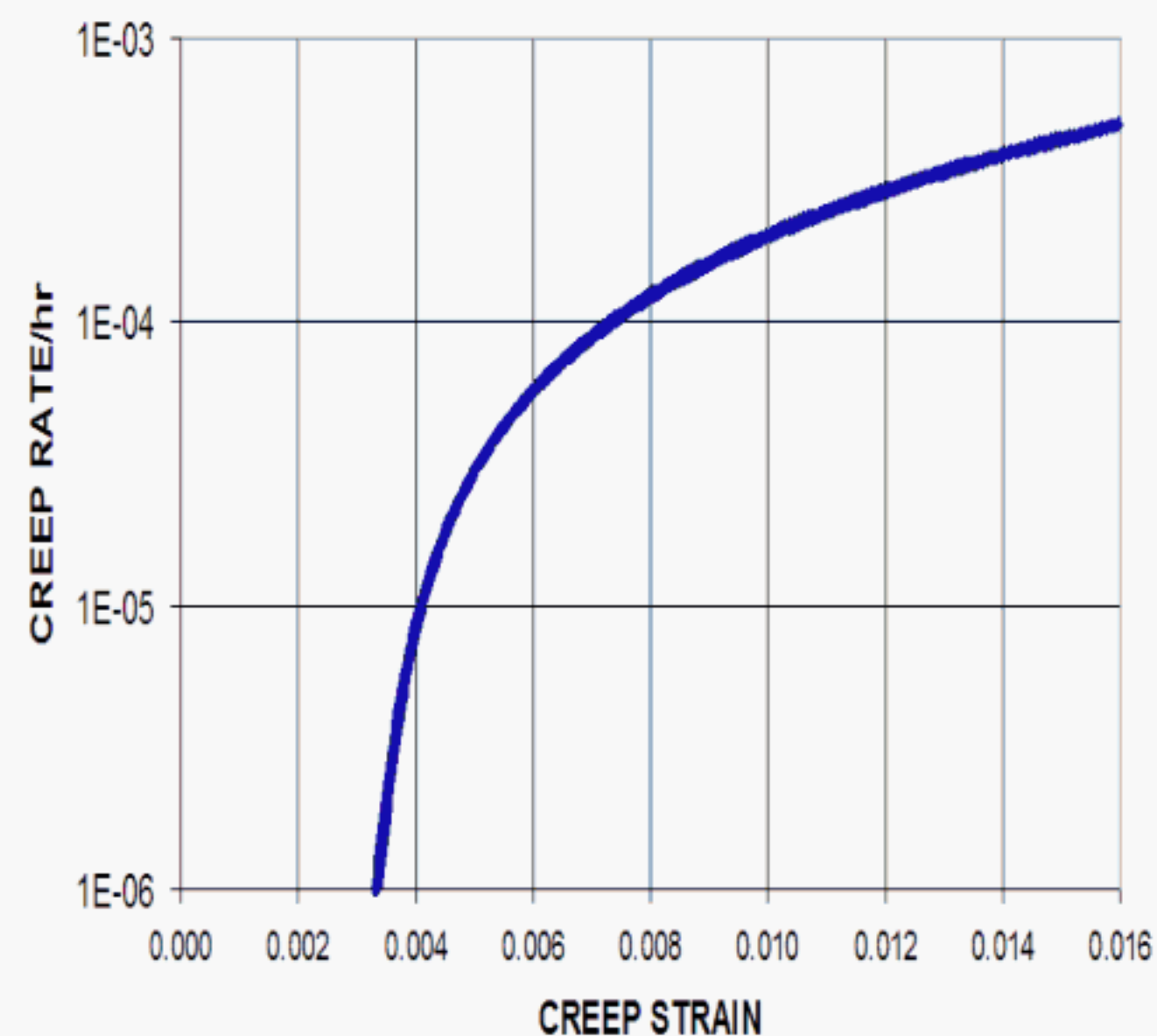
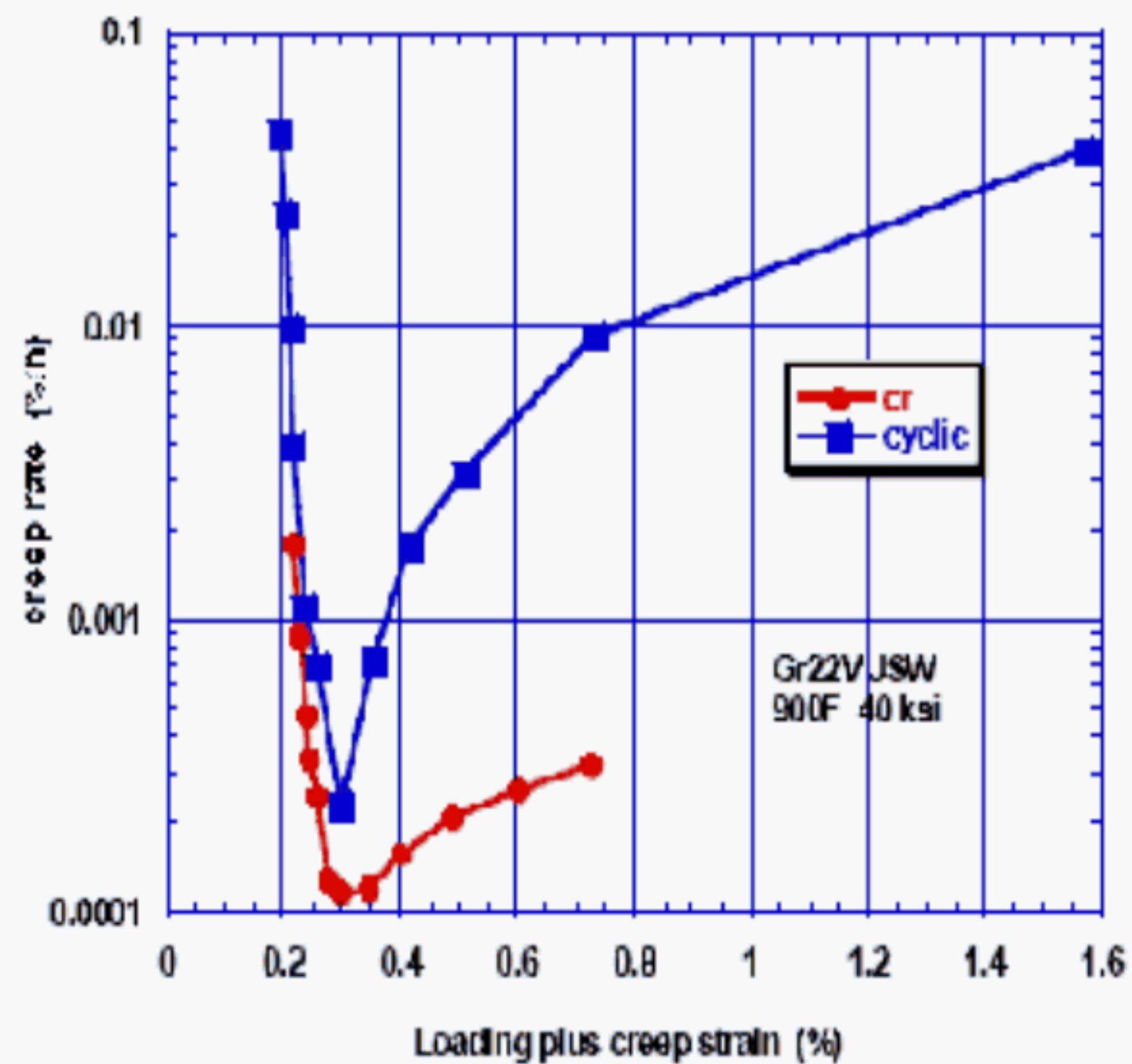
It should be noted that the results of a hold time tests on the 22V alloy at constant stress at ORNL disclosed excellent agreement with the model proposed herein when reexamined by this author. Tests were at 40 ksi and 900°F which, without fatigue cycling, was expected to provide a stress rupture life



in the neighborhood of 40,000 hours. Very short life was observed when a cyclic hold time test was performed. The rate of increase in strain rate per cycle accelerated as the number of cycles at constant stress increased due to strain softening. The model served to predict creep rate acceleration, strain accumulation, loss of life and softening as shown in the following figures. The test information was published in [1] and further disclosed in [2].

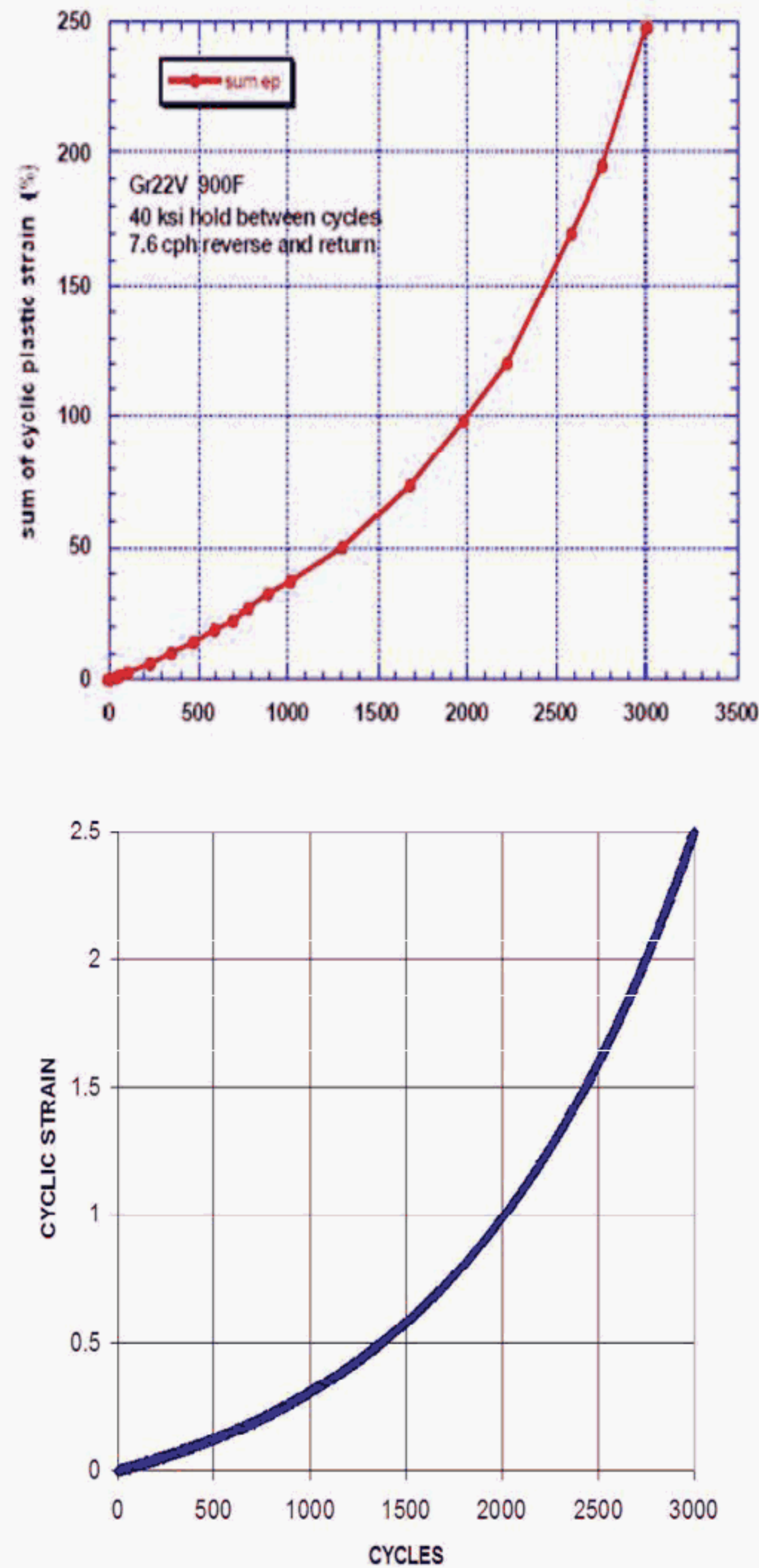


**Figure 15 - Comparison of Test Results (top) With Model Prediction (bottom) of Tertiary Creep Strain Accumulation With and Without Strain Cycling. With Strain Cycling, Tertiary Creep Strain Rises Rapidly as Compared to Constant Stress.**



**Figure 16 - Comparison of Test Results (top) with Model Prediction (bottom) of Creep Rate Acceleration With Tertiary Creep Strain Accumulation. Test Results With and Without Strain Cycling Disclose Cyclic Strain Softening (top). With Strain Cycling, Tertiary Creep Rate Rises Rapidly as Compared to Constant Stress as Predicted by Model (bottom).**





**Figure 17 - Comparison of Test Results (top) With Model Prediction (bottom) of Total Strain Accumulation With Strain Cycling Plus Steady Load Creep for Indicated Cycles. With Strain Cycling, Strain Rises Rapidly as Compared to Constant Load, see Figure 15.**

## REFERENCES

- [1] Response of Ferritic Steels to Nonsteady Loading at Elevated Temperatures in Research on Chrome-Moly Steels, MPC-21, ASME, 1984.
- [2] R. Klueh and R. Swindeman, Mechanical Properties of a Modified 2% Cr-1 Mo Steel for Pressure Vessel Applications, ORNL-5995, Dec. 1983.
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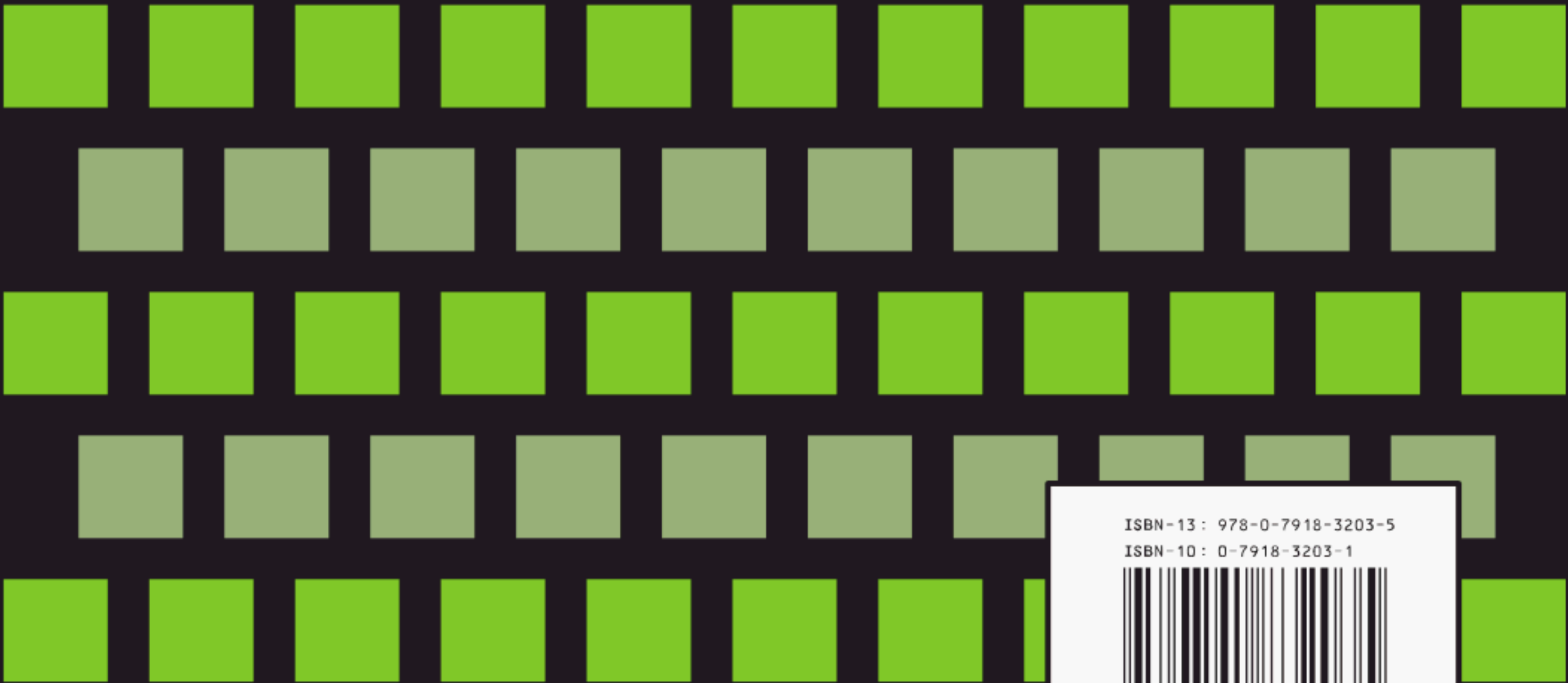
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