DI-10 Interrelation between Thermal and Elevated Temperature Fatigue Resistance of AISI 347 Stainless Steel

Noritada Katoh[1]

Abstract

In previous papers a series of experiments on thermal fatigue resistance of AISI 347 stainless steel had been investigated but the obtained results were very complicated. Then, a theoretical study was made further, and a thermal fatigue theory was developed, which agreed fairly well with almost all of the experiments. In the theory a quantity of effective fatigue damage per cycle was defined, considering many factors affecting thermal fatigue life. In the present paper this theory is extended to the case of elevated temperature fatigue and using the effective fatigue damage, the interrelation between both fatigue lives are studied. The obtained results show that thermal fatigue life is much shorter than elevated temperature fatigue life, but if local thermal strains around inclusions, which would be occured by the difference of thermal expansion coefficient between in and out of the inclusions, is considered, both fatigue lives agree fairly well.

Introduction

It is desirable to study the interrelation between thermal and elevated temperature fatigue resistance of materials for practical reasons as well as for a better understanding of thermal fatigue. On this subject some studies have been made (1,2,3,4), and much information of practical importance has been obtained, but it is still not enough to understand the basic characteristics of thermal fatigue. The purpose of this study is to present more information on these basic characteristics.

As generally believed, fatigue life of materials is affected by many factors such as stress, plastic strain, temperature and its duration. Therefore, fatigue life should be investigated by considering at the same time how such factors would damage the materials. If we want to obtain the real interrelation among fatigue resistances, fatigue lives should be compared on the basis of a quantity representing a cumulative fatigue damage per cycle, which would be defined under consideration of the effects of all these factors.

---

1 Mechanical Engineering Research Lab. of Hitachi Ltd., Kokubunji, Tokyo. Research Associate

In the previous paper a theory of thermal fatigue was developed, in which an effective fatigue damage per cycle was introduced in order to explain complicated phenomena of thermal fatigue. Using this effective fatigue damage, it was showed that this theory agreed fairly well with the experimental results obtained under various thermal fatigue conditions. This effective fatigue damage is applicable to the quantity for comparison in the present study. The results obtained in this study shows that number of cycles to failure of thermal fatigue is about 1/3.5 of that of elevated temperature fatigue for all values of the effective fatigue damage, but if local thermal strains, which would be occured around inclusions by thermal cycling, are considered, both fatigue lives agree fairly well.

Fatigue Theory

In the thermal fatigue theory developed in the previous paper, a model for a possible mechanism of fatigue is proposed. The model is as follows: supersaturated vacancies produced by cyclic plastic strain and atoms in solute, such as hydrogen and nitrogen, migrate randomly to nuclei of fatigue. When they meet there, stable voids would be formed, and the voids would continue to grow gradually as they absorb the vacancies and the solute atoms. At the voids, stress concentration is developed by their notch effect and the piling-up of dislocations. When the max. value of the concentrated stresses has attained the theoretical strength of the material, a fatigue crack would develop.

Following this model, the growing rate of the stable voids is assumed to be proportional to the product of the migration rates of vacancies and that of solute atoms, and fatigue damage ϕ is proportional to the magnitude of the void, then the fatigue damage $d\phi$ produced in time dt is given by Equ. (1).

$$d\phi = \kappa \cdot n_e \cdot n_i \cdot dt . \qquad \cdots\cdots\cdots (1)$$

Where κ is a proportional factor, n_e the rate of migration for vacancies and n_i the rate of migration for solute atoms.

In order to obtain the rate of migration for vacancies, three following assumptions are set forth. The first assumption is that the concentration of the supersaturated vacancies is proportional to plastic strain (5). The second, is that the vacancies imgrate randomly through sink sources to the nuclei without any interaction insofar as they do not come near enough to the nuclei. The third, is that the vacancies, which have entered within the domain of the interaction, migrate to the nuclei with an activation energy of Q_f. Under these assumptions the rate of migration for the vacancies is obtained in Equ. (2).

$$n_e = (Z_f/n_s)\, c_o\, \dot{\varepsilon}_p \exp(-Q_f/RT). \qquad \cdots\cdots\cdots\cdots (2)$$

Where Z_f is the number of lattice points contained in the domain of the interaction, n_s the atomic concentration of sink sources, c_o a proportional factor for the assumed relation between the produced vacancies and plastic strain, $\dot{\varepsilon}_p = d\varepsilon_p/dt$, R the gas constant and T the absolute temperature.

The rate of migration for the solute atoms is assumed from R. Maddin's experiments (6) to be given by Equ. (3).

$$n_i = c_i \alpha_i \left\{ 1 + a_o \varepsilon_{pa} \exp(W_i/T) \right\} \exp(-Q_i/RT). \qquad \cdots\cdots\cdots\cdots (3)$$

Where c_i is the atomic concentration of the solute atoms, Q_i an activation energy for migration, a_o and W_i constants representing the contributions of vacancies to the migration, ε_{pa} the plastic strain range and $\alpha_i = A_i Z_i \nu$, where A_i is an entropy term for diffusion, Z_i a constant determined by crystal system and ν the atomic vibrational frequency. The first term in the parentheses of Equ. (3) expresses the ordinary rate of diffusion, and the second, the accelerating effect by vacancies.

Using Equ. (1), (2) and (3), we find a fatigue damage per cycle,

$$\phi_o = (Z_f/n_s)\, c_o c_i \alpha_i \int_c \dot{\varepsilon}_p \left\{ 1 + a_o \varepsilon_{pa} \exp(W_i/T) \right\} \exp(-Q/RT)\, dt, \cdots\cdots (4)$$

where $Q = Q_i + Q_f$, and $\int_c \cdots\cdots\cdots dt$ represents integration for a cycle.

In order to find a relation between this fatigue damage and fatigue life, the following assumptions are given. The first assumption is that the fatigue damage would be accumulated linearly with the thermal cycling. The second, is that the stress concentration factor would be proportional to this cumulative fatigue damage. The third, is that when the max. value of these concentrated stresses has attained Orowan's theoretical strength, which is given by $\sigma_{th} = \sqrt{2\gamma E/a}$, fatigue fracture would occur. Following these assumptions, we obtain the relation on number of cycles to failure N such that:

$$(\sigma_a/\sqrt{E}) \int_c \dot{\varepsilon}_p \left\{ 1 + a_o \varepsilon_{pa} \exp(W_i/T) \right\} \exp(-Q/RT)\, dt\, N = K, \cdots\cdots (5)$$

where σ_a is stress range, E Young's modulus, K a material constant, given by $K = 2\sqrt{2\gamma/a}/\lambda \kappa Z_f c_o c_i \alpha_i$, where γ is surface energy, λ a proportional factor for the assumed relation between the stress concentration factor and the cumulative fatigue damage.

Defining an effective fatigue damage per cycle as Equ. (6),

$$\Phi_o = (\sigma_a/\sqrt{E})\int_c \dot{\varepsilon}_p\{1+a_o\varepsilon_{pa}\exp(W_i/T)\}\exp(-Q/RT)dt, \quad \cdots\cdots (6)$$

we can lead Equ. (5) to a more simple formula of Equ. (7).

$$\Phi_o N = K. \quad \cdots\cdots\cdots\cdots (7)$$

Equ. (7) means that when the effective fatigue damage has been accumulated to the critical value of K characteristic to the material, fatigue fracture would develop. Therefore, if we can calculate the effective fatigue damage for a given fatigue condition and at the same time the value K is known for a given material, fatigue life for such a case can be easily estimated by this theory.

Calculation of Effective Fatigue Damage

Accurate calculation of the effective fatigue damage, in general, is impossible. Therefore approximate calculation was made under some reasonable assumptions. One of the Assumptions for the case of thermal fatigue was that the fatigue damage produced in the heating half cycle would be independent to the fatigue damage produced in the cooling half cycle, and as the former was larger than the latter, the former would cause fatigue crack. Therefore the effective fatigue damage per cycle was not integrated for a whole cycle but for the heating half cycle. Other assumptions were that the heating rate was constant, and the elastic strain was negligible small comparing with the plastic strain, and the stress-strain relation was expressed by an equation of $\sigma = \sigma_y + k\varepsilon_p$, where σ was stress, σ_y yield stress, ε_p the plastic strain and k a proportional constant. Using these assumptions, the effective fatigue damage was calculated, and the values of K and three material constants involved in the formula were determined by using some experimental results. The finally obtained equation of the effective fatigue damage is shown in Equ. (8), and the value of K is 1.0×10^{-4}.

$$\Phi_o = (\sigma_a\varepsilon_{pa}/\sqrt{E_{T_1}})\{(\alpha/\varepsilon_{pa})\int_{T_1'}^{T_1}\exp(-Q/RT)dT + a_o\varepsilon_{pa}\}. \quad \cdots\cdots (8)$$

Where T_1 is the upper limit temperature of thermal cycling, E_{T1} Young's modulus in T_1, $T_1' = T_1 - \varepsilon_{pa}/\alpha$, α the thermal expansion coefficient, $Q = 19$ Kcal/mol and $a_o = 2.3\times10^{-3}$.

The relation Φ_o vs. N obtained for thermal fatigue is shown in Fig. 1. We note that this theory agrees fairly well with the experimental results and, also, with L.F. Coffin Jr.'s data, except for some of the results obtained for the prestrained specimens.

This theory is fundamentally not limited to thermal fatigue. Therefore, it can be easily extended to elevated temperature fatigue with similar assumptions. From this extended theory, the effective

fatigue damage is calculated for elevated temperature fatigue, using the same values of material constants as in the case of thermal fatigue. The result is shown in Equ. (9).

$$\Phi_o = (\sigma_a\varepsilon_{pa}/\sqrt{E_T})\{\exp(-Q/RT) + a_o\varepsilon_{pa}\}. \quad \cdots\cdots\cdots\cdots (9)$$

Experimental Result and Discussion

At first fatigue life of thermal and elevated temperature fatigue are compared on the basis of total strain range, plastic strain range and stress range. Specimens used in these experiments are made with the same charge rod of AISI 347 stainless steel and also heat-treated under the same condition (full annealing). The results are shown in Fig. 2, Fig. 3 and Fig. 4, respectively. Fig. 2 and Fig. 3 show that thermal fatigue life is much shorter than elevated temperature fatigue life, but Fig. 4 shows that thermal fatigue life of 600°C ⇄ 100°C is larger than elevated temperature fatigue life at 300°C, and that lives of thermal fatigue of 600°C ⇄ 100°C and 600°C ⇄ 300°C are larger than that of elevated temperature fatigue at their upper limit temperature of 600°C. This fact means that the interrelation between thermal and elevated temperature fatigue should be compared on the basis of such a quantity as the effective fatigue damage, which is theoretically introduced in the previous chapter under the consideration of all factors affecting the fatigue life. Comparison on the basis of the effective fatigue damage is shown in **Fig. 5**. This shows that the theory is also agrees fairly well with L.F. Coffin Jr's data and all of the elevated temperature fatigue experiments, except for the value of the material constant K. The value of K for this case is 3.5×10^{-4}, but the value previously obtained for thermal fatigue is 1.0×10^{-4}. This disagreement means that thermal fatigue life is about 1/3.5 times as long as elevated temperature fatigue life, and this fact means that in thermal fatigue the effective fatigue damage of the same value will embrittle or weaken the material much more rapidly than in elevated temperature fatigue.

To ascertain the embrittlement phenomena in thermal fatigue, microscopic observation was made both on virgin specimens and those tested in thermal and elevated temperature fatigue experiments. But any indication for embrittlement such as grain growth or precipitation of carbides, which would be stimulated by thermal cycling, was not observed.

The material, however, would have been embrittled or weakened by thermal cycling, even if such indication could not be found microscopically. To ascertain this, an experiment was conducted. In this experiment some numbers of thermal cycling had been applied to both virgin and partly fatigued specimens without any constraint of thermal expansion, and using these specimens, it was studied as to how such cycling would affect their thermal fatigue resistance. From this study it was then known that such thermal cycling had a strengthening effect rather than weakening. This fact shows that the material has not been weakened so rapidly as the theoretical result suggests, in other words, this theory must be studied further to clarify the origin of this disagreement.

After these studies, it was concluded that this disagreement would come from the assumption of the theory that the material would not contain any inclusion. Engineering materials, however, contain many inclusions and when thermal cycling is applied to them, thermal strain concentration would occur around them. In thermal fatigue there exsist not only the macroscopic thermal strain caused by the external constraint, but also the microscopic thermal strain caused by the difference of thermal expansion coefficient between in and out of the inclusions. In elevated temperature fatigue, however, there exist only the macroscopic mechanical strain. Therefore, when total strain range in thermal fatigue is macroscopically same as that in elevated temperature fatigue, fatigue life of the former would become shorter than that of the latter.

In order to estimate the effect of the microscopic thermal strain, the strain concentrations caused by inclusions must be calculated both for the case where the microscopic thermal strain exists (thermal fatigue), and for the case where it does not exist (elevated temperature fatigue). Accurate calculation of these strains, however, is impossible. Therefore, only an approximate calculation is made in this study, too. For one of these approximations, we assumed that the strain concentration factor would be equal to the value for stress concentration factor calculated by elastic theory, even if the state of strain to be obtained would not be perfectly elastic, that is it would be elasto-plastic or perfectly plastic. Following this assumption, strain concentration was calculated for the most simple case. In this case, it was assumed that a prolate spheroidal inclusion was contained in an infinite elastic body, and that they were perfectly cohesive to each other on the entire surface of the inclusion. An elastic solution for such a case has been presented by R.H. Edwards (7). The state of strain to be calculated is, of course, three dimensional. From previous researches it has been known that the fatigue effect of the three dimensional strain can be expressed by its equivalent strain given by Mises-Henckey's condition. Then, using the solution, the maximum equivalent stresses both of thermal and mechanical fatigue are calculated for the case when their total strain ranges are same macroscopically. Fig. 6 shows the calculated result, assuming that the thermal expansion coefficients of the infinite body and the inclusion are 18×10^{-6} and 6×10^{-6} deg^{-1}., respectively, and that they

have the same Poisson's ratio of 0.3. In Fig. 6 H is the measure of relative rigidity of the inclusion and the surrounding medium and is given by the Equ. $H=(E'/E)-1$, where E and E' are Young's moduli for the outer and the inner region, respectively. σ_e and σ_{me} are the maximum equivalent stresses calculated by neglecting the microscopic thermal stresses and by considering these thermal stresses. Therefore, σ_e and σ_{me} would be the maximum equivalent stresses for mechanical and for thermal fatigue, respectively. Parameter s is the ratio of the minor to the major axis of the spheroidal inclusion. This shows that the effect of inclusions would be pronouned, when the inclusions were more rigid than the surrounding material. Alumina type inclusions and chromium or niobium carbides included in the material belong to such rigid inclusions.

Alumina type inclusions are very hard, but other information on their physical properties still have not been obtained. Their chemical composition, however, is known to be 95% alumina. Therefore, it was assumed that they would have the same physical properties as that of 95% alumina ceramics. In other hand physical properties of chromium and niobium carbide are known to be almost same as the assumed values for alumina type inclusions. Then, alumina type inclusion could be seen to be the inclusion which would have the most damaging effect on thermal fatigue resistance.

In order to estimate the thermal fatigue life of AISI 347 stainless steel, from its mechanical fatigue life, the strain concentration caused by alumina type inclusion must be calculated. To do this, their physical properties and the shape of the inclusion are assumed as shown in Table 1. Using these values the relative rigidity is calculated to range from 0.30 to 0.84, and the the ratio σ_{me}/σ_e is calculated to range from 1.24 to 1.44. The result means that the maximum equivalent strain occuring in thermal fatigue would be from 1.24 to 1.44 times as large as that in elevated temperature fatigue, when their total strain ranges are same each other macroscopically.

To estimate thermal fatigue life from elevated temperature fatigue data the relations between the total strain range and the effective fatigue damage are obtained for three elevated temperature fatigue tests. They are linear as shown in Fig. 7. Using each relation, we can easily estimate the effective fatigue damage for the case when the total strain range is multiplied by ξ . Using this effective fatigue damage, we can estimate elevated temperature fatigue life for this case from the relation between fatigue life and the effective fatigue damage (Fig. 5). As mentioned above, the real total strain range in thermal fatigue should be consider to be form 1.24 to 1.44 times as large as the total strain range in elevated temperature fatigue. Therefore if we estimate elevated temperature fatigue lives for $\xi = 1.24$ and $\xi = 1.44$, thermal fatigue life would range between the two estimated fatigue lives. The result estimated by this method is shown in Fig. 8. This shows that there exists a fairly good correlation between the estimated thermal fatigue life and those experimentally obtained.

Conclusion

In this study the interrelation between thermal and elevated temperature fatigue resistance of AISI 347 stainless steel is studied theoretically, and the obtained results are as follows:

1. Fatigue resistance of materials should be studied under consideration of all factors affecting the fatigue life.
2. Thermal fatigue life of AISI 347 stainless steel is about 1/3.5 of elevated temperature fatigue life, if they are compared on the basis of the effective fatigue damage, calculated with the assumption that the material contain no inclusions.
3. When Microscopic thermal strains, which occur from the difference of the thermal expansion coefficient between Alumina type inclusion and the surrounding medium, is considered, a fairly good correlation can be found between thermal and elevated temperature fatigue resistance.

Acknowledgment

The author is thankful to Professor Dr. T. Utoguchi, Univ. of Tokyo, for his valuable suggestions and encouragement in conducting this study.

References

(1) Major,H., Trans. ASM, vol. 51, 1959.

(2) Swindeman,R. W. and Douglas,D. A., Trans. ASME Series D, vol. 81 No. 2, 1959.

(3) Taira,S. et al, Jour. J.S.T.M., vol. 10, No. 89, 1961.

(4) Kawamoto,M. et al, Journ. Soc. Mat. sci. Japan, vol. 12, No. 120, 1963.

(5) Cottrel,A. H., Vacancies and other Points Defects in Metals and Alloys, 1958, Inst. of Metals.

(6) Maddin,R., Vacancies and other Points Defects in Metals and Alloys, 1958, Inst. of Metals.

(7) Edwards,R. H., J. appl. Mech., 1951.

Table 1. Assumption for Calculation of Strain Concentration around an inclusion

shape of inclusion		
shape	prolate spheroid	
ratio of the minor to the major axis	1 ~ 1/5	
physical properties		
	thermal expansion coeff.	Young's modulus
inclusion	$5.7 \sim 7.6 \times 10^{-6}$ deg.$^{-1}$	$2.73 \sim 3.01 \times 10^6$ kg/cm^2
surrounding medium	2.0×10^{-6} deg.$^{-1}$	$2.1 \sim 1.64 \times 10^6$ kg/cm^2

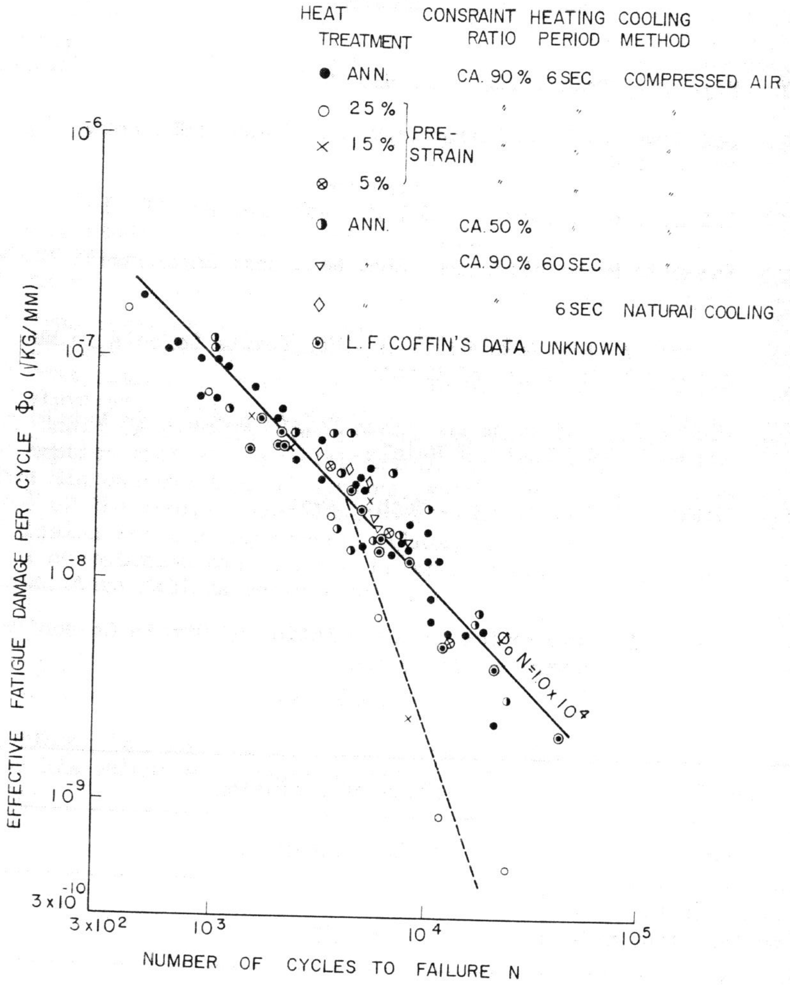

FIG. I EXPERIMENTAL RESULT ON THE RELATION ΦoVS. N FOR THERMAL FATIGUE

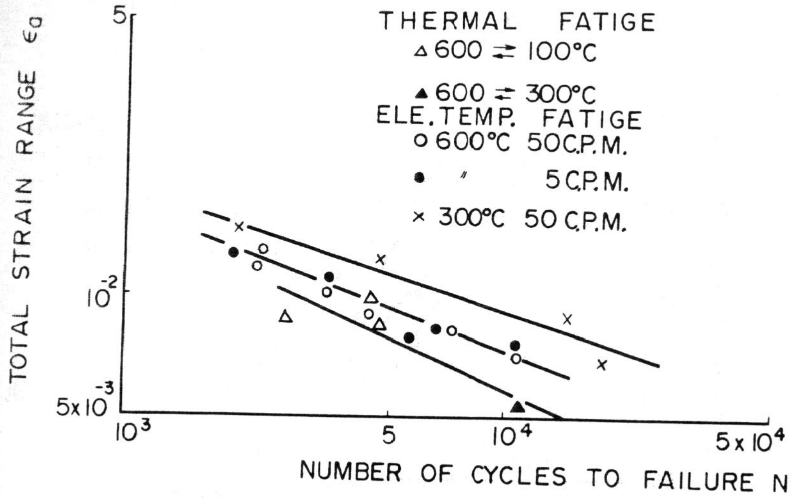

FIG. 2 COMPARISON OF FATIGE LIVES ON THE BASIS OF TOTAL STRAIN RANGE

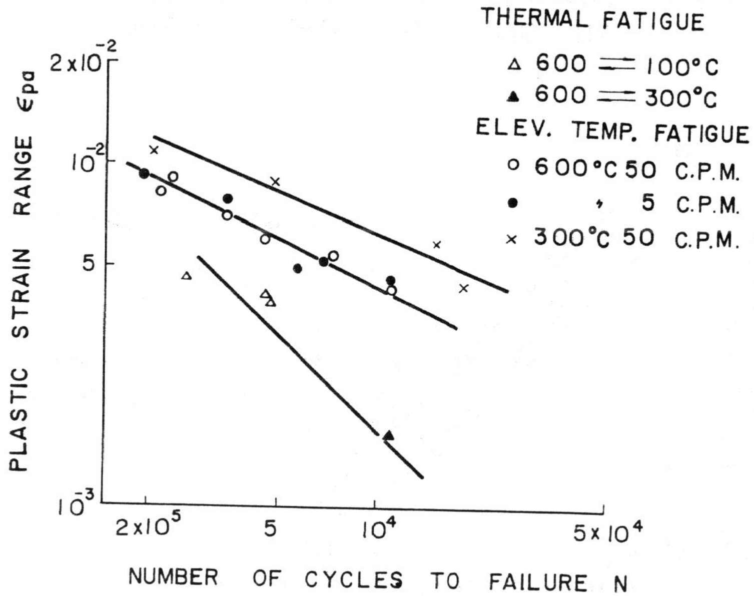

FIG. 3 COMPARISON OF FATIGUE LIVES ON THE BASIS OF PLASTIC STRAIN RANGE.

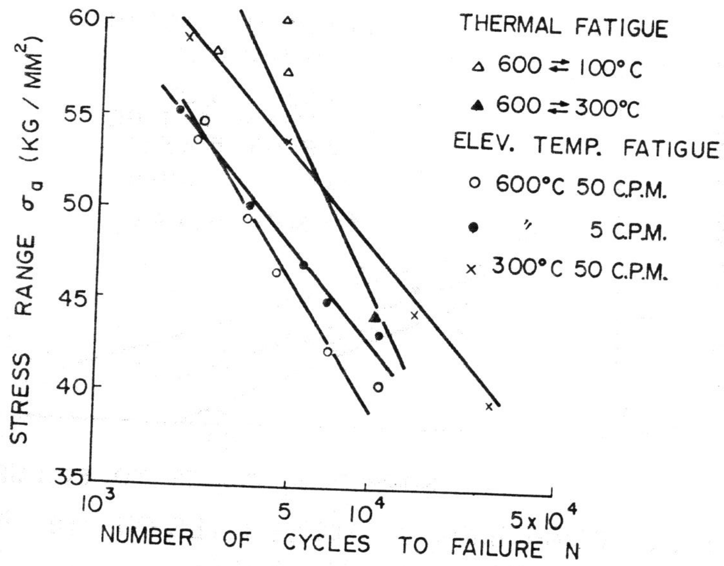

FIG. 4 COMPARISON OF FATIGUE LIVES ON THE BASIS OF STRESS RANGE

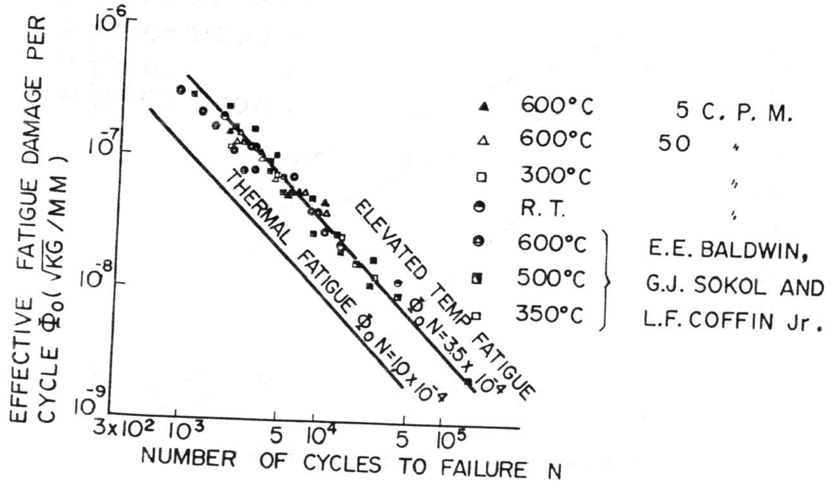

FIG. 5 EXPERIMENTAL RESULT ON THE RELATION Φ_0 VS. N FOR ELEVATED TEMPERATURE FATIGUE

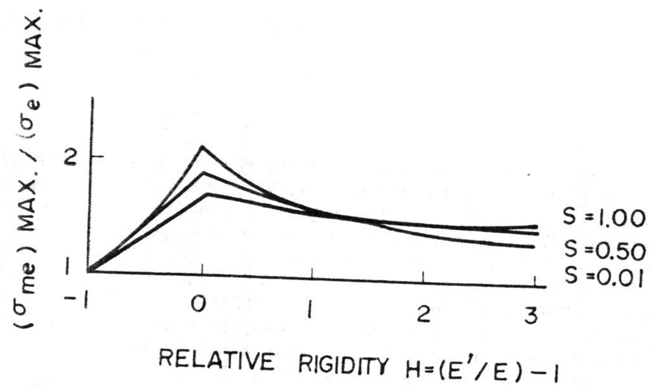

FIG. 6 RELATION BETWEEN RELATIVE RIGIDITY AND THE RATIO OF MAX. EQUIVALENT STRESSES CALCULATED BY ELASTIC THEORY

$\left(\alpha = 18 \times 10^{-6}\ \text{deg.}^{-1},\ \alpha' = 6 \times 10^{-6}\ \text{deg.}^{-1} \quad \nu = \nu' = 0.3 \right)$

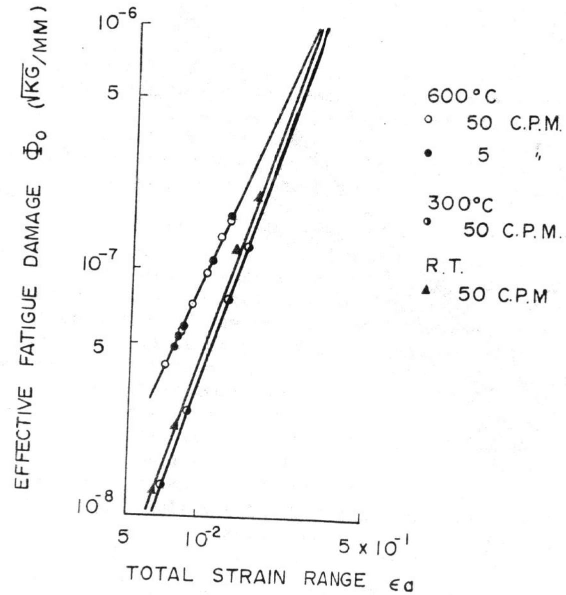

FIG. 7 RELATION ϵ_a VS Φ_0 FOR ELEVATED TEMPERATURE FATIGUE

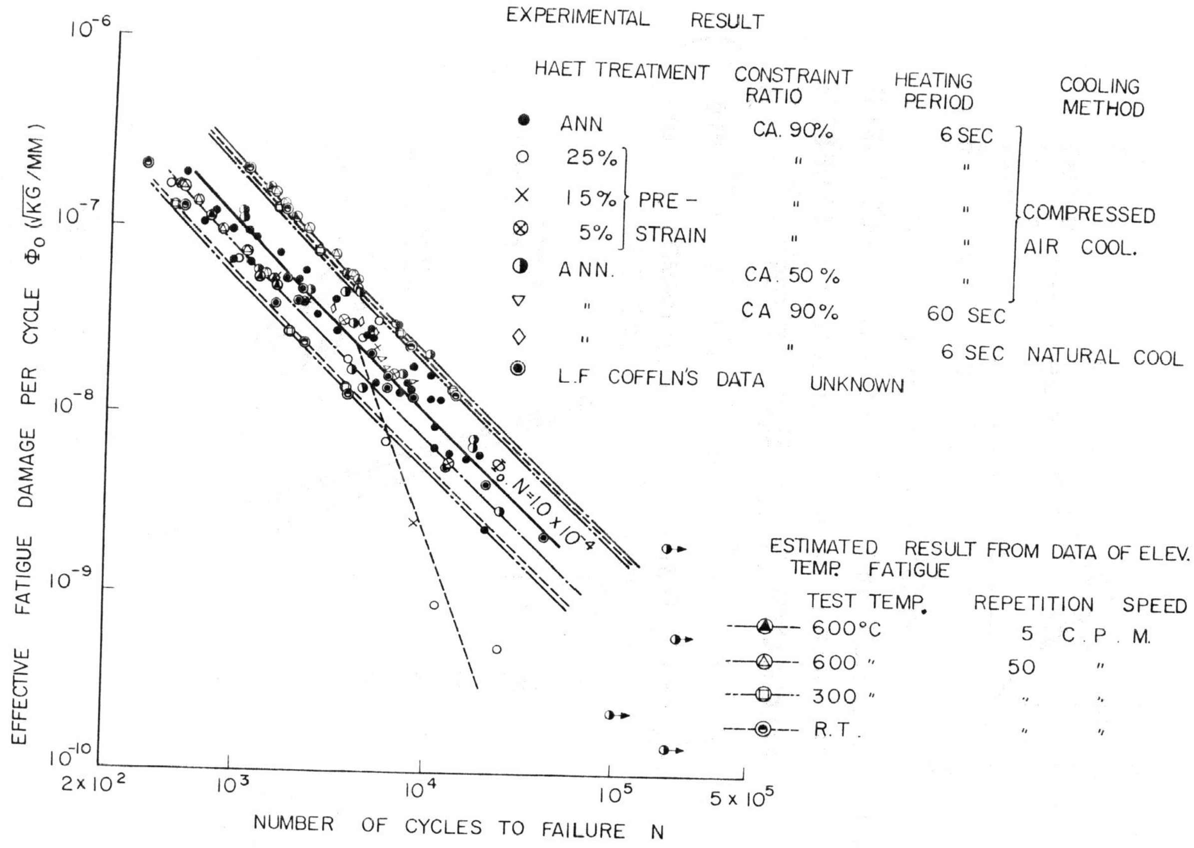

FIG.8 CORRELATION BETWEEN THE ESTIMATED AND THE EXPERIMENTAL THERMAL FATIGUE LIFE