LIFE MODELING OF THERMAL BARRIER COATINGS FOR AIRCRAFT GAS TURBINE ENGINES

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ABSTRACT

Thermal barrier coating life models developed under the NASA Lewis Research Center's Hot Section Technology (HOST) program are summarized. An initial laboratory model and three design-capable models are discussed. Current understanding of coating failure mechanisms are also summarized.

INTRODUCTION

Thermal barrier coatings are being developed for protecting air-cooled turbine blades and vanes in air-craft gas turbine engines. The current state-of-the-art coating system consists of about 0.25 mm of a zirconia-yttria ceramic over 0.13 mm of an MCrAlY alloy bond coat. Both layers are applied by plasma spraying. The benefits arise from the insulation provided by the ceramic layer. This insulation allows higher gas temperatures, lower component temperatures, reduced cooling air requirements, moderation of thermal transients, and/or a decrease in the severity of hot spots. This yields improvements in performance, efficiency, and component durability. Future engine designs are expected to rely heavily on thermal barrier coatings. Thus life models are required to assess the risks associated with any given design and to insure that these coatings can be exploited fully. Further details may be found in Miller (1987), DeMasi et al., (1988), Strangman et al., (1987), and Hillery (1987).

The NASA thermal barrier coating life model development program consisted initially of an in-house pro-gram designed to improve understanding and to develop a model suitable for treating laboratory life data (Miller, 1987). This work was then extended via three contracts under the HOST program to the development of design-capable models (DeMasi et al., 1988; Strangman et al., 1987; Hillery et al., 1987). These contracts were devised determine thermomechanical properties, to analyze coating stresses and strains, and to develop life models. Phase I of each contract has now been suc-cessfully completed, and the results will be summarized in this paper.

COATING FAILURE MECHANISMS

A basic understanding of coating failure mechanisms is a prerequisite to the development of life prediction models. Failure mechanisms in gas turbine engines and in laboratory simulations have been discussed in detail elsewhere (e.g., Miller, 1987; DeMasi et al., 1988; Strangman et al., 1987, Hillery et al., 1987). There is now general agreement that these coatings fail pri-marily as a result of stresses induced by the thermal expansion mismatch between the ceramic and metallic layers, and that these stresses are greatly influenced by time-at-temperature processes such as oxidation and possibly sintering. The stress state in the ceramic layer which leads to crack propagation and eventual spalling is one of biaxial compression in the plane of the coating and radial tension. These stresses are fur-ther complicated by the wavy and irregular interface between the ceramic and metallic layers. In fact, HOST-sponsored calculations indicate that the radial stresses above a wavy interface may actually alternate between regions of compression and tension as illus-trated in Fig. 1 (Chang et al., 1987). Figure 2 illustrates that the behavior of plasma sprayed zirconia-based thermal barrier coatings differs signifi-cantly from the behavior of conventional ceramics. This behavior, which is believed to result from the splat structure, includes very low thermal conductivity and very high compressive strain tolerance. In-plane ten-sile strain tolerance of the coating system is also very high because such loading may lead to segmentation cracking in the ceramic with no degradation to the attachment strength. Plasma sprayed zirconia-yttria also exhibits creep-like behavior, presumably as a result of sliding at the splat boundaries, and fatigue-like behavior, presumably as a result of slow crack growth. Experimental evidence of slow crack growth (or microcrack link up), creep, and fatigue are presented in DeMasi et al., (1988).

INITIAL LABORATORY MODEL DEVELOPMENT

A preliminary life prediction model has been described (Miller, 1987; Miller, 1984; Miller et al., 1984). This model assumed that the complex state of

stress and strain imposed on the coating system by the thermal loads could be expressed in terms of a single parameter. This parameter was labelled ε_r -- which was taken to be the radial component of the thermal expansion mismatch strain. Next it was assumed that the time-at-temperature effects could be treated in terms of oxidation alone and that oxidation could be characterized by the weight gain at the conclusion of each cycle w_N. Then, weight gain and strain were related using either of two alternate approaches. In the first case, depicted in Fig. 3(a), an oxidized coating is assumed to behave as if an effective strain ε_e is increasing. At zero weight gain this effective strain equals the radial strain ε_r. At a critical weight gain w_c -- defined as the weight gain required to fail the coating in a single cycle -- the effective strain equals a failure strain ε_f. This leads to the expression

$$\varepsilon_e = \varepsilon_f - \varepsilon_r \left(\frac{w_N}{w_c}\right)^m + \varepsilon_r \tag{1}$$

where the exponent m has been added to allow the curve in Fig. 3(a) to be nonlinear. The alternate assumption (Miller, 1987) is to assume that the failure strain degrades from an initial value ε_{f0} to a final value equal to ε_r. This case, illustrated in Fig. 3(b) leads to the expression

$$\frac{\varepsilon_f}{\varepsilon_r} = \left(1 - \frac{\varepsilon_{f0}}{\varepsilon_r}\right)\left(\frac{w_N}{w_c}\right)^m + \frac{\varepsilon_{f0}}{\varepsilon_r} \tag{2}$$

Cracks in the ceramic layer may be assumed to grow according to a crack growth law of the form

$$\frac{dA}{dN} = A\varepsilon_e^b a^c \tag{3}$$

where da/dN is the incremental crack growth per cycle, A is a constant, b and c are exponents related to the subcritical crack growth exponent, and a is the crack length. The model resulting from expression 1 is

$$\sum_{N=1}^{N_f} \left[\left(1 - \frac{\varepsilon_r}{\varepsilon_f}\right)\left(\frac{w_N}{w_c}\right)^m + \frac{\varepsilon_r}{\varepsilon_f}\right]^b = 1 \tag{4}$$

and the alternative model resulting from expression 2 is

$$\sum_{N=1}^{N_f} \left[\left(1 - \frac{\varepsilon_{f0}}{\varepsilon_r}\right)\left(\frac{w_N}{w_c}\right)^m + \frac{\varepsilon_{f0}}{\varepsilon_r}\right]^{-b} = 1 \tag{5}$$

These models may also be derived from the familiar fatigue expression (Miller et al., 1984; Manson, 1966).

$$N_f = \left(\frac{\varepsilon_e}{\varepsilon_f}\right)^{-b} \tag{6}$$

and Miner's Law

$$\sum_{N=1}^{N_f} \left(\frac{1}{N_{fN}}\right) = 1 \tag{7}$$

where N_{fN} is the apparent number of cycles remaining after cycle N and weight gain w_N.

Figure 4 illustrates the fits obtained using expression 4 and applying it to life data collected at 1100 °C for three different cycle lengths. It should be mentioned that the set of parameters given in the figure are not unique. Numerous other sets provide equally good fits. For example raising the assumed value of b while lowering the strain ratio produces an equally good fit. Also, the life data can be fit equally well using expressions 4 or 5.

DESIGN-CAPABLE LIFE MODELING

While the above model represented a first step it was not in a form which would be of use to an engine designer. Therefore three contracts were instituted under the HOST program which were aimed at the development of design-capable models.

Pratt & Whitney Aircraft (DeMasi et al., 1988), along with subcontractor Southwest Research Institute, developed a fatigue-based coating life model which uses Miner's Law (expression 7) along with expression 6 rewritten as

$$N_f = \left(\frac{\Delta\varepsilon_i}{\Delta\varepsilon_f}\right)^{-b} \tag{8}$$

where $\Delta\varepsilon_i$ is the inelastic strain range defined by

$$\Delta\varepsilon_i = \Delta(\alpha \Delta T) + \Delta\varepsilon_h + \Delta\varepsilon_c - \frac{2\sigma_{ys}}{E} \tag{9}$$

The term $\Delta(\alpha \Delta T)$ in the above expression is the thermal expansion mismatch strain (which was expressed in terms of ε_r in the previous section), $\Delta\varepsilon_h$ is the strain resulting from the heating transient, $\Delta\varepsilon_c$ is the strain resulting from the cooling transient, and σ_{ys}/E is the elastic strain at yielding. The assumed relationship between oxidation and strain, analogous to expression 2, was

$$\Delta\varepsilon_f = \Delta\varepsilon_{f0}\left(\frac{1 - \delta}{\delta_c}\right)^c + \Delta\varepsilon_i\left(\frac{\delta}{\delta_c}\right)^d \tag{10}$$

where oxidation has been expressed in terms of the oxide layer thickness δ rather than the specific weight gain w. The inelastic strain range was calculated using finite element techniques which employed a time dependent inelastic model developed by Walker (1983). Figure 5 shows an example of the use of this model to calculate compressive and tensile strains which may be compared with experimental data. In Fig. 6 the ceramic stress-strain behavior is calculated for a single cycle. This figure displays the large amount of reversed inelastic strain produced by thermal cycling. Figure 7 shows a plot of observed versus calculated lives for a wide range of test conditions. As shown in the figure the model is accurate to plus or minus a factor of 3, which is considered adequate.

The model developed by the Garrett Turbine Engine Company (Strangman et al., 1987) may be expressed as

$$\text{TBC LIFE} = \left[\frac{\text{HEATING CYCLE LENGTH FACTOR}}{(\text{OXIDATION LIFE})^{-1} + \frac{\text{ZIRCONIA DENSIFICATION}}{\text{PLUS OXIDATION LIFE}}^{-1}}\right]^{-1} + \left[\frac{\text{SALT FILM}}{\text{DAMAGE LIFE}}\right]^{-1} \tag{11}$$

Equation 11 is expressed schematically in Fig. 8 which shows that the model considers bond coat oxidation, zirconia toughness reduction, and damage due to molten salt deposits. The model is driven by the thermal analysis of the component of interest for its anticipated mission. The left side of the denominator in expression 11 as determined from test data calibrations is

$$\frac{(t^{0.25} + 0.181) \ \text{MTBREF}}{\{\exp[-0.015(T + 273) + C_1]\}^{-1} + (\exp[-0.041(+273) + C_2]\}^{-1}} \quad (12)$$

where MTBREF is a multitemperature burner rig experience factor which forces predictions and experiment into agreement. The right side of the denominator in expression 11 is calculated using a Garrett-developed model (Strangman, 1984; Strangman et al., 1987). In practice, the model is driven by thermal analysis of the component of interest. An example of the application of the thermal barrier coating life model to laboratory test data is shown in Fig. 9, and mission analysis predictions are shown in Fig. 10.

The approach used by the General Electric Company (Hillery et al., 1987) employed time-dependent, nonlinear finite element modeling of the stresses and strains present in the thermal barrier coating system, followed by the correlation of these stresses and strains with test lives. The life model developed using this approach may be expressed as

$$\Delta\varepsilon_{RZ} + 0.4 \ \Delta\varepsilon_R = 0.121 \ N_f^{-0.486} \quad (13)$$

where $\Delta\varepsilon_{RZ}$ is the shear strain range, $\Delta\varepsilon_R$ is the normal strain range, and N_f is the number of cycles to failure. The above model is the only one to consider failure induced by edges and hence is the only one to consider shear strain. Expression 13 is illustrated graphically in Fig. 11

CONCLUDING REMARKS

In conclusion, the materials and structural aspects of thermal barrier coatings have been successfully integrated under the NASA HOST program to produce models which may now or in the near future be used in design. Efforts on this program continue at Pratt & Whitney Aircraft where their model is being extended to the life prediction of physical vapor deposited thermal barrier coatings.

While the HOST program has been quite successful it should also be noted that many new and unanswered questions have been raised by this work. For example, the effects of creep and inelasticity in both the ceramic and bond coat layers are poorly understood. The role of

shearing stresses, including the role that shearing at an edge may play in reducing the fatigue exponent, is not well understood. The detailed mechanism by which oxidation controls coating system life is not well understood either. Also, it is not known whether the assumption of a smooth interface, commonly employed to simplify finite element analyses can lead to inaccurate or even misleading results. Other areas of uncertainty involve the importance of sintering at high temperatures and hot corrosion at relatively low temperatures.

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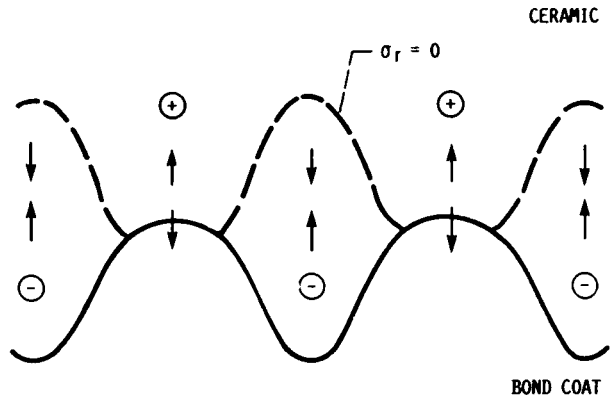
CERAMIC

$\sigma_r = 0$

$\oplus$ $\oplus$

$\ominus$ $\ominus$ $\ominus$

BOND COAT

FIGURE 1. - SCHEMATIC REPRESENTATION OF CALCULATED RADIAL
THERMAL EXPANSION MISMATCH STRESS ABOVE A WAVY INTER-
FACE.

RULE OF MIXTURES

K

% POROSITY
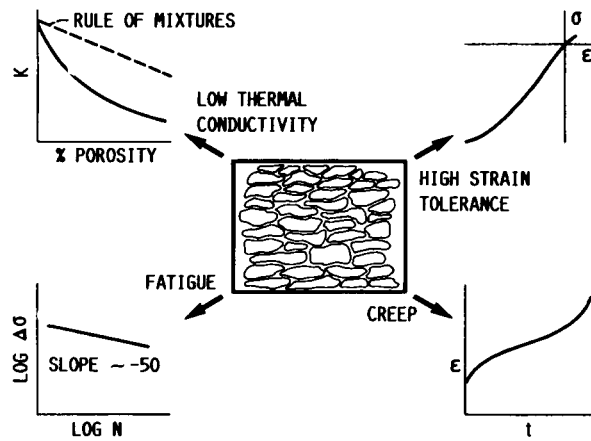
LOW THERMAL
CONDUCTIVITY

σ

ϵ

HIGH STRAIN
TOLERANCE

FATIGUE

LOG $\Delta\sigma$

SLOPE ~ -50

LOG N

CREEP

ϵ

t

FIGURE 2. - SCHEMATIC REPRESENTATION OF THERMAL MECHANICAL
PROPERTIES RESULTING FROM COATING SPLAT STRUCTURE.

(A) ASSUMED RELATIONSHIP BETWEEN EFFECTIVE STRAIN AND OXIDATIVE WEIGHT GAIN.

(B) ASSUMED ALTERNATE RELATIONSHIP BETWEEN FAILURE STRAIN AND OXIDATIVE WEIGHT GAIN.
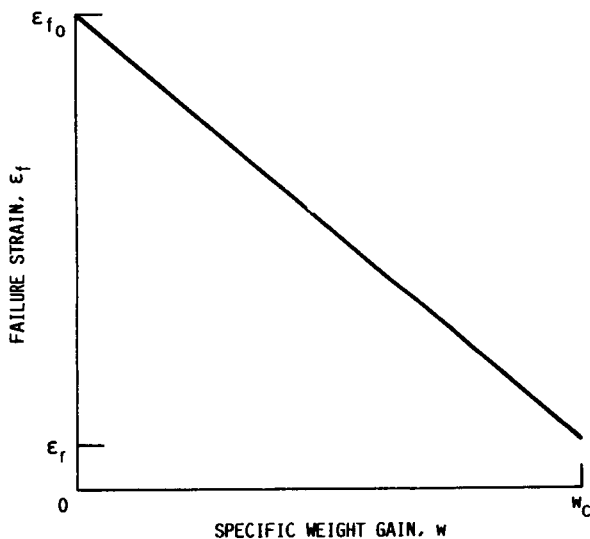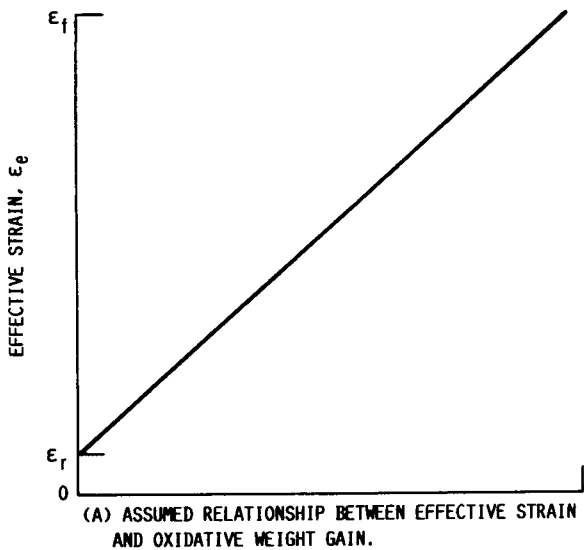
FIGURE 3. - ASSUMED WEIGHT GAIN/STRAIN RELATIONSHIPS.

$b = 17.00$
$m = 1.00$
$w_c = 2.4$ MG/CM^2
$\varepsilon_r/\varepsilon_f = 0.38$
OPEN SYMBOLS DENOTE MEASURED
CLOSED SYMBOLS DENOTE MODELED
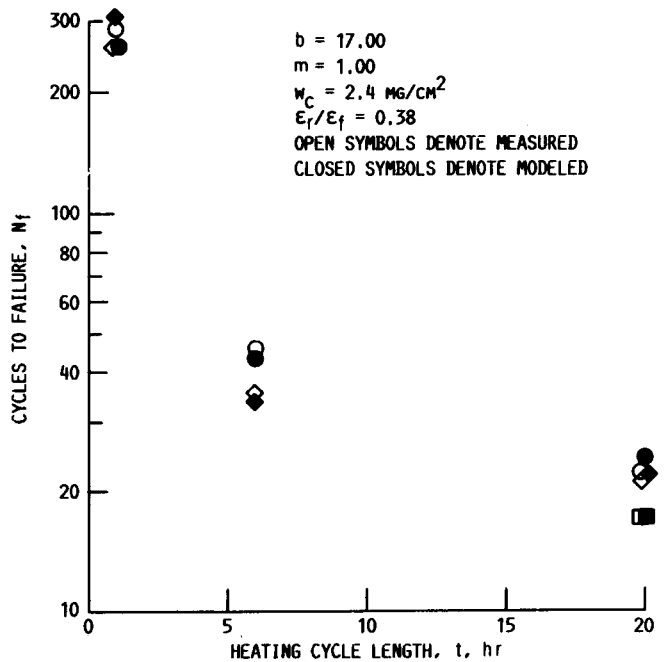
FIGURE 4. - COMPARISON OF CALCULATED AND MODELED LIFE AS A FUNCTION OF HEATING CYCLE DURATION ACCORDING TO NASA LABORATORY MODEL.
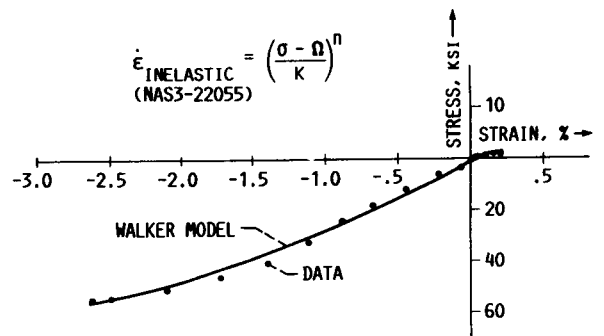
$$\dot{\varepsilon}_{INELASTIC} = \left(\frac{\sigma - \Omega}{K}\right)^n$$
(NAS3-22055)

WALKER MODEL

DATA

FIGURE 5. - CERAMIC BEHAVIOR MODELED WITH WALKER EQUATION ACCORDING TO THE PRATT & WHITNEY MODEL.

.113
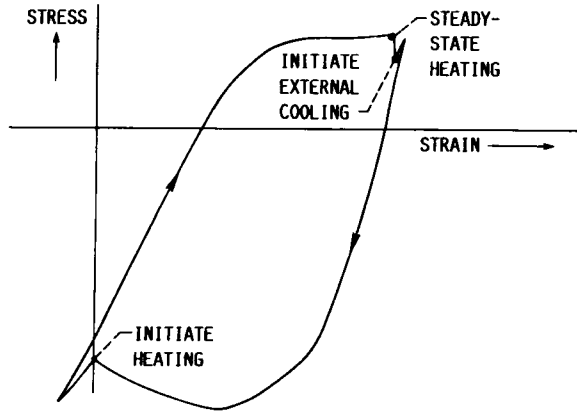
FIGURE 6. - SCHEMATIC OF STRAINS CALCULATED FOR A TYPICAL
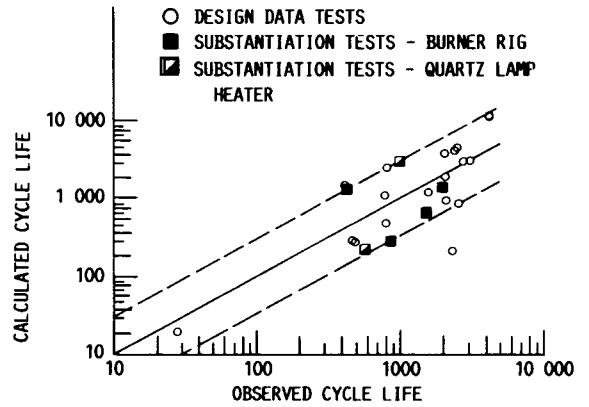CYCLE ACCORDING TO THE PRATT & WHITNEY MODEL.

FIGURE 7. - COMPARISON OF CALCULATED AND
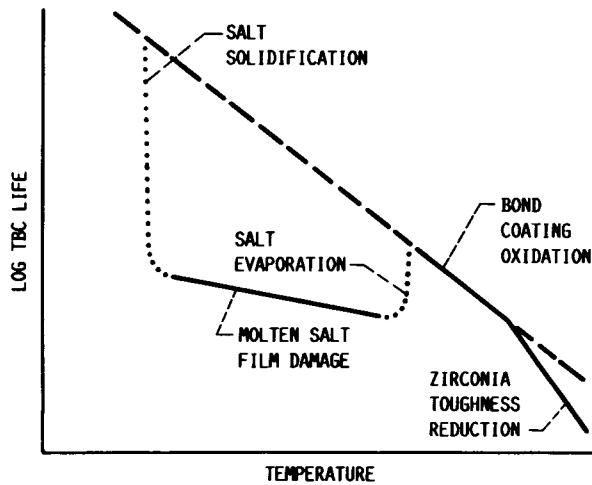MODELED LIVES ACCORDING TO THE PRATT &
WHITNEY MODEL.

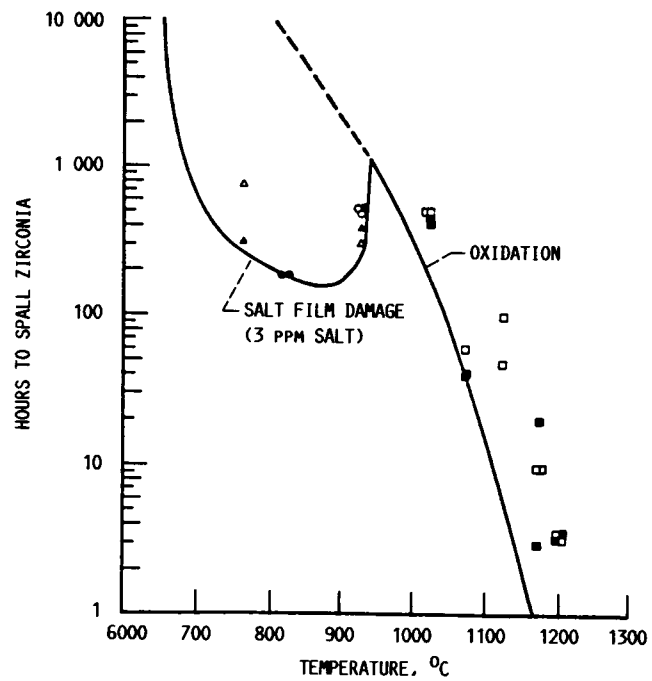FIGURE 8. - SCHEMATIC OF PROCESSES CONSIDERED IN THE
GARRETT MODEL.

FIGURE 9. - CONSERVATIVE FIT OF LABORATORY LIFE DATA
TO THE GARRETT MODEL.

TBC SYSTEM	BUSINESS JET, HR	MARITIME SURVEILLANCE, HR
PLASMA SPRAY		
CHROMALLOY	16 517	9 843
UNION CARBIDE	6 656	5 207
KLOCK	49 644	29 973
EB-PVD		
TEMESCAL	55 607	2 106

FIGURE 10. - MISSION ANALYSIS PREDICTIONS
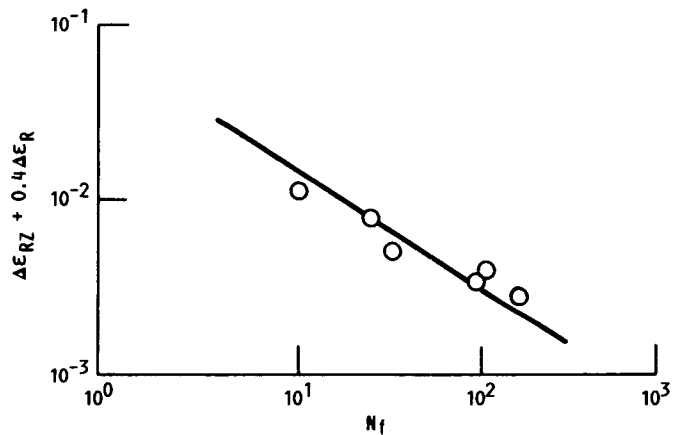BY THE GARRETT MODEL.

FIGURE 11. - CORRELATION BETWEEN CALCULATED STRAIN RELA-
TIONSHIP AND EXPERIMENTAL CYCLES TO FAILURE ACCORDING
TO THE GENERAL ELECTRIC MODEL.