

High-Temperature Life Assessment of Exhaust Components and the Procedure for Accelerated Durability and Reliability Testing

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ABSTRACT

Fatigue, creep, oxidation, or their combinations have long been recognized as the principal failure mechanisms in many high-temperature applications such as exhaust manifolds and thermal regeneration units used in commercial vehicle aftertreatment systems. Depending on the specific materials, loading, and temperature levels, the role of each damage mechanism may change significantly, ranging from independent development to competing and combined creep-fatigue, fatigue-oxidation, creep-fatigue-oxidation. Several multiple failure mechanisms based material damage models have been developed, and products to resist these failure mechanisms have been designed and produced. However, one of the key challenges posed to design engineers is to find a way to accelerate the durability and reliability tests of auto exhaust in component and system levels and to validate the product design within development cycle to satisfy customer and market's requirements. In this paper, a dimensional analysis based life assessment model and the corresponding accelerated testing procedure for exhaust components are developed. Their capabilities are demonstrated with a two-stress level pure fatigue test and the V-shape specimen test for creep-fatigue-oxidation durability evaluation. The modeling approach and the accelerated testing procedure developed in this paper can be used for commercial vehicle aftertreatment development as well as for other high-temperature applications.

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1. INTRODUCTION

Stricter regulations on the reduction of vehicle exhaust emissions have driven increasingly higher temperatures in the manifolds, thermal regeneration unit (RGU), and many other exhaust components and systems [1,2,3]. Understanding the durability and reliability of exhaust components and systems at high operating temperatures is essential for durable, reliable and cost-effective exhaust designs. Creep-fatigue-oxidation damage of materials becomes important when operating temperatures exceeding about a third of the material melting temperature and it is the dominant failure mechanism of exhaust systems failure [1]. To prevent premature failure, life assessment of these exhaust components and systems is essential and the basic understanding of the creep-fatigue-oxidation failure

phenomena at elevated temperature is vital to advanced exhaust designs.

First, fatigue is essentially a cycle-dependent failure mechanism while creep and oxidation are time-dependent phenomena. The modeling approaches for each of these individual failure mechanisms are well developed because of the research efforts done over the last decades. By contrast, modeling of fatigue-oxidation and creep-fatigue is more difficult because of possible failure mechanisms interaction. For example, alloys form oxide scales, which thicken with time at high-temperature environments, and these scales generally protect the metal against further oxidation and corrosive attack under isothermal and stress free conditions. However, under combined high temperature and cyclic loading conditions, the damage process becomes very complex because of the nature of multiple failure mechanisms and their interaction. Accelerated damage



process, which is strongly material dependent, is often observed because of oxide break-up mechanism [4,5,6] or “dynamic embrittlement” mechanism [7-8]; Creep-fatigue interaction can cause complex failure mechanisms interaction and life reduction as well. A variety of predictive creep-fatigue models have been proposed over the last decades and eventually resulting in several industry guidelines on creep-fatigue interaction diagram for several widely used engineering materials [9]. The combined creep-fatigue-oxidation is a more complex phenomenon and it is more difficult in both modeling and testing. Generally, the damage and life assessment methods can be conceptually divided into two approaches: crack growth approach and total life approach. In design processes of auto components and systems, the total life approach (total fatigue cycles to failure or total creep/oxidation time to failure) is more preferable than the crack growth approach, which is more used in maintenance and repair of damaged components and systems. However, total life based creep-fatigue-oxidation data usually show much wider scatter band than the crack growth data because of the uncertainties introduced by both crack initiation and growth, therefore, probabilistic analysis must be brought into the modeling and subsequent engineering designs.

Second, product designers and manufacturers are under continuous and increasing pressure to reduce the “time to market” of new products while assuring high levels of durability and reliability of these products. Accelerated testing is often used to achieve these objectives [10, 11]. The accelerated testing procedures for products with single failure mechanisms have been studied for several decades and essentially well established even though some fundamental issues are still unsolved [10, 11]. By contrast, much is needed to do for the accelerated testing procedure for products with multiple failure mechanisms and to validate designs within development cycle to satisfy customer and market's requirements. In fact, few methods are available for these important but difficult multiple failure mechanisms problems [11], and the accelerated testing and data analysis procedure with mechanisms interaction is essentially unavailable. Accelerated testing and data analysis can be further complicated by many other factors such as nonlinear behavior, heteroscedastic data characteristics, censored data, unknown distribution attributes.

In this paper, a life assessment model based on dimensional analysis is developed, and an accelerated testing and data analysis procedure suitable for the life assessment of exhaust components is subsequently proposed. The life model and the accelerated testing procedure are demonstrated by analyzing durability life of exhaust components under pure fatigue and combined creep-fatigue-oxidation loading conditions. The probabilistic analysis of the uncertainties of testing parameters is also discussed. Life assessment and accelerated testing of exhaust systems consisting of many components are more complex and difficult and will be addressed separately. In order to protect the proprietary

information, the values of test data in the plots and tables of this paper are all expressed in nondimensional form.

2. DIMENSIONAL ANALYSIS ON THE LIFE CYCLE OF EXHAUST COMPONENTS

Many damage models, e.g. fatigue, creep, oxidation or their combinations, have been proposed, see [Appendix](#). With the gained information about the individual damage mechanisms and their interactions, finite element methods or other computer aided design approaches can be used to predict the life of exhaust components and systems, and this effort is still underway. If successful, the damage mechanisms based approach should be very general and extremely powerful, but it requires substantial and prolonged testing programs in order to be efficiently utilized. However, it is also clear that it is inconvenient to apply these complex models to accelerated testing and data analysis, therefore, a simplified engineering approach with predictive capability and a quick procedure could be very helpful for quick life assessment, accelerated testing, and engineering designs.

Sometimes, a simple engineering approach can be used to gain physical insight and to obtain simple formulae for quick engineering designs. Dimensional analysis [12, 13] is such a very useful tool for building mathematical and physical models. In fact, ignoring dimensional analysis techniques can lead to invalid models. In this section, with the gained detailed failure mechanisms described in the [Appendix](#), a dimensional analysis based engineering approach is developed and the life assessment process can be conducted without significant effort and it can also complement the detailed mechanistic analyses. The first step for developing a model for life assessment is to identify the relevant variables involved.

For various failure mechanisms, life cycle can be expressed as a function of failure mechanisms, which is a further function of testing parameters: $N = N[M_1(X_1, X_2, \dots), M_2(X_1, X_2, \dots), \dots]$ or $N = N(X_1, X_2, \dots)$. $M_1, M_2, \dots$ represent the possible failure mechanisms such as fatigue, creep etc., and $X_1, X_2, \dots$ stand for the independent testing or control parameters such as stress, temperature etc. Durability/reliability engineers are more interested in the relationship between the life cycle and the control parameters, but reluctant to think about the detailed failure mechanisms as long as the life is accurately assessed with an empirical formula.

In this section we make use of the Buckingham Π theorem [12, 13] to solve the creep-fatigue-oxidation life problem for exhaust at component level, that is, to determine the relationship between lifetime N and the control parameters such as stress range, temperature, hold time and specimen geometry. The Buckingham Π theorem allows us to represent any relationship between these variables $f(X_1, X_2, \dots, X_m) = 0$ in terms of a reduced set of dimensionless variables.

Table 1. Dimensional analysis of variables involved in the fatigue, creep, oxidation problem

	N	N_0	$\Delta\sigma$	$\Delta\sigma_0$	θ	$l_1, l_2, \dots$	Δt	p	γ
M	0	0	1	1	0	0	0	0	0
L	0	0	-1	-1	0	1	0	0	0
T_1	0	0	-2	-2	0	0	1	0	0
T_2	1	1	0	0	0	0	0	0	0
K	0	0	0	0	1	0	0	0	0

$$\Phi(\Pi_1, \Pi_2, \dots, \Pi_{m-k}) = 0 \quad (1)$$

where k is the total number of the independent units. Therefore, a dimensional relation for the m independent variables can be reduced to an equation with $m - k$ non-dimensional variables.

From experience accumulated over the years on creep-fatigue-oxidation phenomenon we know that several variables are involved in the damage problem: stress range $\Delta\sigma$, temperature θ , geometry information $l_1, l_2, \dots$, hold time Δt , probability of fatigue failure p and confidence level γ . The variables and their units are listed in [Table 1](#). Other possible relevant physical properties of materials may also be involved but are not listed in [Table 1](#). $\Delta\sigma_0$ and N_0 in [Table 1](#) are the reference or threshold values for $\Delta\sigma$ and N [14]. M , L , T_1 , T_2 , and K refer to the fundamental units of mass, length, time, cycle, and temperature, respectively. $l_1, l_2, \dots$ represent the geometrical features such as length, width, thickness and any other information related to length scale.

Here two different scales, i.e., T_1 , and T_2 , are used to measure time and cycle, respectively. This is analogous to Huntley's works of distinguishing the length vectors and distinguishing mass of quantity and mass of inertia [15]. Additionally, the conventional S-N curve and Paris law based fatigue crack growth, assumed to be material properties and do not contain the length information, is called "incomplete similarity" [16]. However, the length scale related geometry information must be included in dimensional analysis for life assessment of components and systems.

The dimensional analysis leads to equations with reduced variables. This means that any existing relation $f(N, N_0, \Delta\sigma, \Delta\sigma_0, \theta_{\max}, \Delta t, l_i, p, \gamma, \dots) = 0$ can be written in terms of dimensionless variables: $\Phi(N/N_0, \Delta\sigma/\Delta\sigma_0, [\theta_{\max}], [\Delta t], [l_i], p, \gamma) = 0$. The bracket "[]" represents dimensionless parameters with certain relationship, which can be determined when necessary loading, material, and geometry information are given. Though there are other alternatives, it seems convenient to choose N_0 and $\Delta\sigma_0$ as the normalizing variables, leading to the reduced set of variables [14]. The introduction of the reference parameters makes the models dimensionally correct and physically valid. However, unfortunately, not all models used in engineering community are always physically valid [14]. Such models include the mostly used fatigue life model: $\log(N) = A + B \log(\Delta\sigma)$ or

equivalently, $\Delta\sigma = C_W N^{-1/h}$ [14]. Creep related stress σ and the possible reference σ_0 can be also included for a complete analysis. However, it is not considered in this paper for simplicity. The creep effect is assumed to be implicitly reflected in the part related to dwell time Δt .

Since we are interested in N at certain p , γ , then, we have the following general form of life cycle formula

$$N = N_0 g(\Delta\sigma/\Delta\sigma_0, [\theta_{\max}], [\Delta t], [l_i], p, \gamma) \quad (2)$$

where $g()$ is a function to be determined.

Based on the experience, the following formula for creep-fatigue-oxidation damage caused life cycle is proposed.

$$N = CN_0 \left(\frac{\Delta\sigma}{\Delta\sigma_0} \right)^\eta (1 + \alpha\Delta t)^\mu l_i^{\omega_i} \exp\left(-\frac{Q}{R\theta_{\max}}\right) \quad (3)$$

The part related to stress range follows the conventional power-law form of fatigue S-N curves; The part related to temperature follows the Arrhenius relationship with Q and R are constants; $\theta_{\min}$, the maximum temperature within a cycle, is chosen here for convenience in the cycle problem. Other parameters such as $\theta_{\max}$, the minimum temperature within a cycle, can also be used; The part related to hold time satisfy the intuition that if $\Delta t = 0$ the pure fatigue formula is recovered. The unit of α is $1/T_1$ to make $\alpha\Delta t$ dimensionless. The term $(1 + \alpha\Delta t)$ generally reflects the additive characteristics of creep-fatigue in [Eq. \(A-8\)](#) and fatigue-oxidation in [Eq. \(A-9\)](#). With combined creep and oxidation, we assume that the power law is still followed for simplicity. Definitely the value of the exponent must be adjusted to obtain the best data correlation in data fitting.

Formula similar to [Eq. \(3\)](#) has been proposed by Tong et al. [17] in modeling creep-fatigue-oxidation crack growth:

$$\frac{da}{dN} = C_p (\Delta K)^m \exp\left(-\frac{Q}{RT}\right) (\Delta t)^{1/2-\xi} \quad \text{where } \Delta t = t_1 + t_2$$

the load ramp time plus the hold time and ξ is a fitting parameter. As indicated by Findley et al., [18] that the formula does not seem to work well for long dwell periods where the crack propagation is mostly due to time dependent effects that are caused by creep.

[Eq. \(3\)](#) can be further simplified for a given specific problem with certain geometry, loading condition and

constraints. For example, there are two parameters need to be adjusted for the term $(1 + \alpha \Delta t)^\mu$, which is related to the hold time. However, it would be more convenient to fix one parameter to a certain value and leave another one to fit data. One possible way is to put $\mu \rightarrow \infty$. Then, we have, e.g. $N = C_D (1 + \alpha \Delta t)^\mu = C_D \exp(\lambda \Delta t)$ with $\lambda = \mu \alpha$ based on the

property $\exp(x) = \lim_{\mu \rightarrow \infty} \left(1 + \frac{x}{\mu}\right)^\mu$ and

$(1 + \alpha \Delta t)^\mu = \left(1 + \frac{\mu \alpha \Delta t}{\mu}\right)^\mu$. Mathematically, the condition $\alpha \rightarrow 0$ must be satisfied to make λ to be a physically meaningful value. C_D and λ can be easily obtained with the least squares method by solving Eq. (4), which is obtained by

minimizing $\Sigma = \sum_{i=1}^n [N^i - C_D \exp(\lambda \Delta t_i)]^2$ with $\partial \Sigma / \partial C_D = 0$ and $\partial \Sigma / \partial \lambda = 0$. Where n is the total number of data points.

$$\begin{cases} \sum_{i=1}^n N^i \exp(\lambda \Delta t_i) - C_D \sum_{i=1}^k \exp(2\lambda \Delta t_i) = 0 \\ \sum_{i=1}^n N^i \exp(\lambda \Delta t_i) \Delta t_i - C_D \sum_{i=1}^k \exp(2\lambda \Delta t_i) \Delta t_i = 0 \end{cases} \quad (4)$$

One of the most important features of Eq. (3) is that it can be linearized in a log-log plot, so that accelerated testing and data analysis can be easily conducted.

3. ACCELERATED TESTING PROCEDURE FOR LINEAR DATA

Durability/reliability design and related accelerated testing of advanced exhaust components and systems are essentially based on the observation of test data and accurate interpretation of these data. Therefore, testing plan and test allocation must be carefully determined in order to find a reasonable compromise between the accuracy and the cost of the tests. There are many test plans such as optimum test plan, traditional test plan and compromised test plan [11]. The major difference among these test plans is the number of test levels and the test points at each stress level. For example, the traditional test allocates test equally to individual test levels, whereas the optimum test plan requires more test points on the lower stress levels [11]. The accelerated testing procedure described below can be loosely used for all of these test plans.

Many life mathematical formulae, such as Eq.(3), can be directly converted into a linear manner with a proper transformation such as log-log transformation or a combination of linear functions. Generally, accelerated testing should be conducted for a curve, i.e. design curve, with certain reliability $(1 - p)\%$ and confidence level $(1 - \gamma)\%$ to ensure that a certain part of the fatigue or other durability

data falls above the minimum or lower bound value with a certain confidence interval [19, 20]. The common practice to define a design curve, such as a fatigue S-N curve, is to shift the mean curve by a distance to a direction based on the magnitude of the scatter. For a homoscedastic data assumption, the design curve Y_0 is generally expressed in Eq. (5) with Y as a function of X .

$$Y_0 = \bar{Y} - Ks \quad (5)$$

$\bar{Y}$ is the mean value of dependent variable at a specific stress level X . s is standard deviation of a data set and K is a factor related to confidence interval. Eq. (6) can be used to calculate the sample standard deviation.

$$s = \sqrt{\frac{1}{(n - \nu)} \sum_{i=1}^n [Y_i - \bar{Y}]^2} \quad (6)$$

where n is sample size and ν is the freedom constraint: $\nu = 1$ for one-stress level test and $\nu = 2$ for two-stress level or multiple stress level tests [19, 20].

The mean curve can usually be expressed in the following linear form.

$$\bar{Y} = \beta_0 + \beta_1 \bar{X} \quad (7)$$

where β_1 is the slope and β_0 the intercept. However, due to the scatter nature of test data, the true value of β_1 and β_0 cannot be obtained with limited test data, and only estimated value of them, i.e. b_1 and b_0 respectively for β_1 and β_0 , can be calculated with a proper data analysis method such as the least squares method.

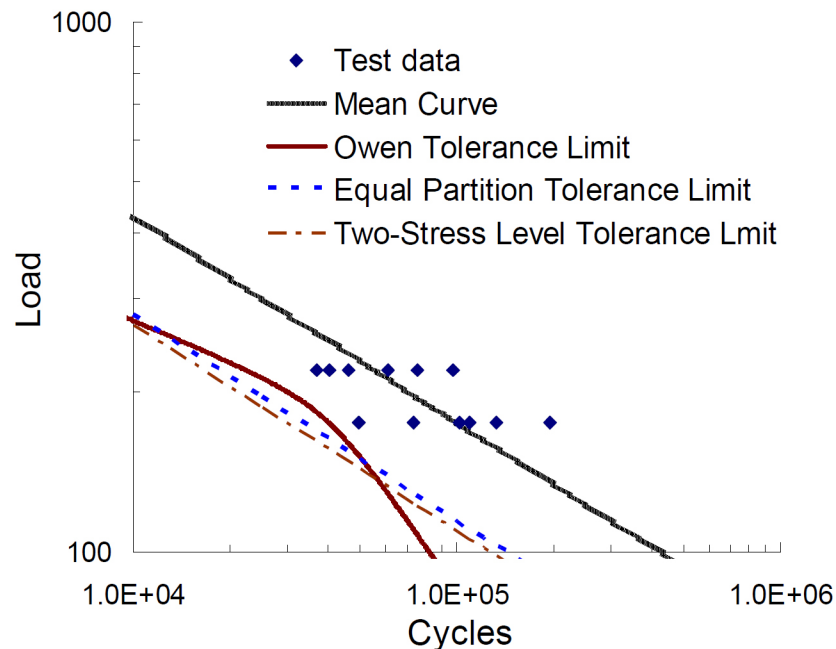
A solution of K in Eq. (5) can be solved with the following equation [19, 20]:

$$K = \frac{z_p(1 - \phi) + \{z_p^2(1 - \phi)^2 - [(1 - \phi)^2 - z_r^2 / [2(n - \nu)]](z_p^2 - A z_r^2)\}^{1/2}}{(1 - \phi)^2 - z_r^2 / [2(n - \nu)]} \quad (8)$$

where $\phi = 1/[4(n - \nu)]$. The expression of A for a general multiple stress level testing is provided in Owen formula [21]. For one stress level problem, the Owen tolerance limit is reduced to the one-dimensional tolerance limit with $A = 1/n$. z_p and z_r can also be determined by using an approximation formulae for a given reliability and confidence [19, 20]. Table 2 lists the calculated K value for the sample size n . The chosen values of the parameters were: $p = 0.10$ and $p = 0.05$; $\gamma = 0.50, 0.25, 0.10$ and 0.05 ; $n = 3$ to 10 by 1 . In Table 2, the reliability and confidence level are expressed in terms of automotive industry convention as $R(1-p)C(1 - \gamma)$. For example, for $n = 10$ and $p0.05r0.25$, $K = 2.0995$; For $n = 20$ and $p0.05r0.25$, $K = 1.9300$. Clearly, the value of factor K as

Table 2. Values of K for one-sided tolerance limit

N	R90C50	R90C75	R90C90	R90C95	R95C50	R95C75	R95C90	R95C95
3	1.4648	2.5396	5.7866	24.8836	1.8802	3.1857	7.2760	31.7304
4	1.3982	2.1381	3.5324	5.5945	1.7948	2.6793	4.4043	7.0061
5	1.3672	1.9604	2.8937	3.9269	1.7549	2.4577	3.5987	4.8869
6	1.3492	1.8567	2.5801	3.2792	1.7318	2.3295	3.2066	4.0703
7	1.3375	1.7874	2.3894	2.9261	1.7167	2.2444	2.9699	3.6284
8	1.3292	1.7372	2.2592	2.7003	1.7061	2.1831	2.8092	3.3474
9	1.3231	1.6988	2.1637	2.5417	1.6983	2.1364	2.6919	3.1509
10	1.3183	1.6683	2.0901	2.4231	1.6922	2.0995	2.6019	3.0047

**Fig. 1. Design S-N curves for two-stress level test with various design curve construction methods [20]**

shown in Table 1 is dependent on the confidence interval and the reliability value.

An equal partition principle based approach, was proposed for data with the homoscedastic characteristics [20]. With the equal partition principle $\nu = 2$ is used to calculate the standard deviation for test data with multiple stress levels, but $n / 2$ is used to calculate the factor K , Eq.(8). For test plan with two stress levels, the equal partition principle based method can be approximately reduced to the two-stress level methods for homoscedastic data [19]. Design S-N curves for a two-stress level data set with various design curve construction methods are plotted in Fig.1. Clearly, the Owen

tolerance limit method [21], which is widely used in industry, cannot be used to analyze the accelerated testing data because of its hyperbolic nature of the constructed curve, which could lead to too unrealistically conservative estimate when an extrapolation is made at service stress level. Both the two-stress level tolerance limit method and the equal partition tolerance limit method lead to design curves paralleling to the mean curve, therefore, suitable for accelerated testing for homoscedastic data. However, there are some differences between these two methods: the two-stress level method can be applied only to testing with two stress levels and it is more conservative [19] and will be used in this paper.