International Journal of Fatigue 59 (2014) 129–136

Contents lists available at ScienceDirect

International Journal of Fatigue

journal homepage: www.elsevier.com/locate/ijfatigue

Two lifetime estimation models for steam turbine components under thermomechanical creep–fatigue loading

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ARTICLE INFO

Article history:
Received 16 May 2013
Received in revised form 11 September 2013
Accepted 17 September 2013
Available online 24 September 2013

Keywords:
Thermomechanical fatigue (TMF)
Chromium steel
Lifetime estimation
Creep–fatigue
Damage accumulation

ABSTRACT

The flexibility of steam turbine components is currently a key issue in terms of the fluctuations in the power supply due to regenerative energy. Conventional steam power plants must run at varying utilization levels. Life estimation methods according to standards, e.g. ASME Code N47 and TR, assess the influences of creep and fatigue separately under the assumption of isothermal conditions at the maximum operating temperature. The influence of thermomechanical fatigue (TMF) loading still requires a significant number of experimental studies. Further, the interaction of creep and fatigue is not adequately taken into account. Thus, new lifetime estimation methods are required for the monitoring, re-engineering and new design of power plant components. In this paper, both a phenomenological and a constitutive crack initiation lifetime estimation model for steam turbine components are introduced. The effectiveness of each method is shown by recalculation of uniaxial as well as multiaxial service-type creep–fatigue experiments on high-chromium 10%Cr stainless rotor steel. Finally, the two models are compared with respect to different aspects, such as the type and number of necessary experiments to determine model parameters, the prerequisite for the application and the limitations of each model.

1. Introduction

In the foreseeable future, the coal-fired power supply will continue to play an irreplaceable role in the energy mix [1]. There exists a considerable energy saving potential by the existing power plants. The improvement of efficiency of coal-fired power plants is to be achieved by increasing the temperature and pressure of steam turbines. On the other hand, the increasing share of electricity produced by renewable energy will lead to more fluctuations in the power supply. Power plants are increasingly forced to run at varying utilization levels, which can shift the critical load to fatigue domain by superimposed creep on the heated surface of components. As a result, a very interesting point from an economic perspective will be the remaining life of these plants, as well as the optimization of start-up and shut-down processes in order to achieve maximum economic and ecological benefits. All these aspects require simple and effective methods for plant life analysis of high temperature steam turbine components.

Temperature transients, constant or variable pressure in pressurized systems and constant or variable speed of rotors produce a large variety of combined static and variable loading situations. Steam pressure and centrifugal forces at rotors lead to quasi-static creep damage. Load cycling due to start-up and shut-down causes fatigue damage, especially on the heated surface of turbine components. As a consequence, superimposed creep–fatigue can be considered to be the critical loading condition [2]. Traditionally, thermal fatigue cracking has been assessed using the results of isothermal tests conducted at (or close to) the peak operating temperature. For life estimation or assessment under creep–fatigue loading, the life fraction rule introduced by Robinson [3] and Taira [4] is used in standards, e.g. ASME Code N47 [5]. The rule is determined by the summation of fatigue damage as a cyclic fraction and creep damage as a time fraction up to a material dependent critical creep–fatigue value. Instead of time fraction, a strain fraction rule, the so called ductility-exhaustion method, is also widely applied [6]. A resulting problem is the determination of long duration rupture ductility properties [7]. Another well-known life assessment method is the strain range partitioning method [8]. The method distinguishes between damage due to time independent plastic and time dependent plastic (creep) deformation. In this context, the lifetime is estimated with the help of up to 4 S–N curves determined by experiments with 4 different cycle forms. Therefore, the application of the life fraction rule is relatively simple with respect to the preparation of experimental data, and the primary creep load due to steam pressure and centrifugal force can be superimposed easily [9]. Farragher et al. [10] presented a multiaxial,

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0142-1123/$ - see front matter © 2013 Elsevier Ltd. All rights reserved.
http://dx.doi.org/10.1016/j.ijfatigue.2013.09.007

critical-plane approach for life prediction. The lifetime model works as a post processing step of a transient heat transfer FEA and a sequential anisothermal viscoplastic FEA and was calibrated with a simplified representative TMF cycle of a P91 steam header. An extension of this methodology is required if loading histories with variable strain ranges are to be assessed.

In this study, two lifetime prediction models for high-temperature components under TMF loading in steam turbines are introduced. The first one is a phenomenological lifetime estimation model (*Model 1*) developed from "engineering" approaches as an extension of the generalized damage accumulation rule [4,11]. The second method (*Model 2*) is derived from a unified viscoplastic model [12,13] with incorporated isotropic damage. This approach is based on the concept of effective stress combined with the principle of generalized energy equivalence [14]. The model parameters of both methods were determined using uniaxial creep experiments and low cycle fatigue (LCF) experiments. As validation, a lifetime prediction for the conducted uniaxial as well as multiaxial service-type [9,15–17] experiments was carried out with both methods. Finally, the two methods are compared with respect to different aspects, such as the type and number of necessary experiments, the prerequisite for the application, advantages, disadvantages and limitations.

2. Experiment

The TMF experiments applied in this paper were carried out under a "service-type" loading cycle according to [9,15–17] (Fig. 1a and b). It is noted that the concept of "service-type" loading cycles is rather similar to that of Holdsworth et al. [18] and was called therein "service-like" cycle or service cycle. A similar idea of "simplified cycle" and "realistic cycle" is presented in [10]. In any case, the terms "service-type" and "service-like" are too similar and should be renamed consistently in the future. The authors suggest the terms "simplified service cycle" and "realistic service cycle" [19]. Accordingly, the strain controlled loading cycle illustrated in Fig. 1 is referred to hereafter as a "simplified service cycle". It is characterized by a compressive strain hold phase 1 simulating start-up conditions, a zero strain hold phase 2 approximating temperature equi-balance during constant loading, a tensile strain hold phase 3 simulating shut-down conditions and an additional zero strain hold phase 4 which characterizes a zero loading condition [16]. The experiments were performed in accordance with the thermal strain compensation method of the current TMF Standard [20].

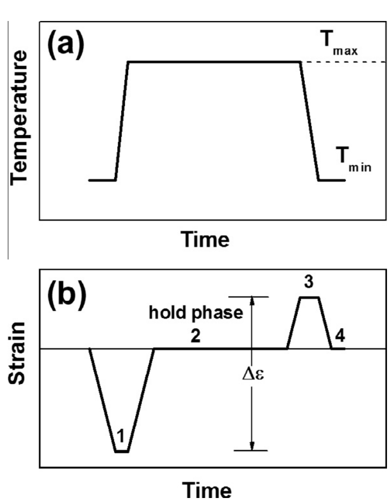

Fig. 1. Simplified service cycle: (a) temperature cycle and (b) strain cycle.

The tested material is a modern ferritic–martensitic stainless steel of type 10Cr–1Mo–1W–V–Nb (German grade X12CrMoWVNbN10-1-1). Uniaxial and biaxial loading conditions were simulated with cylindrical and cruciform specimens. The details of performed experiments were introduced in [16]. An overview of the testing parameters of the applied isothermal and TMF experiments are shown in Table 1.

3. Lifetime models

3.1. Phenomenological lifetime model

Model 1 proposed in this paper is a phenomenological lifetime model which adopts a simplified service cycle as input. It is based on the synthesis of stress–strain hysteresis loops and damage assessment under consideration of creep–fatigue interaction. The strain cycle begins with a strain of zero and reaches the maximum compressive strain first. This process is described by a cyclic stress–strain curve in accordance with the Ramberg–Osgood equation [21]:

$$\varepsilon = \varepsilon_{el} + \varepsilon_{pl} = \frac{\sigma}{E} + \left(\frac{\sigma}{K}\right)^{\frac{1}{n'}}, \quad (1)$$

where σ is the stress, ε_{el} is the elastic part, and ε_{pl} is the plastic part of the total strain ε. E is the temperature-dependent Young's modulus. The material parameters K and n' are temperature-dependent and determined with stress–strain loops of LCF (low cycle fatigue) experiments. To describe cyclic hardening and softening behavior of materials, eight sets of parameters K and n' are determined at 0%, 5%, 10%, 20%, 40%, 60%, 80%, and 100% of lifetime for 6 reference temperatures between 300 °C and 625 °C. For stress–strain relationships under intermediate temperatures, the parameters are interpolated.

The stress relaxation process $\sigma(t)$ in hold time 1 (Fig. 2, 1 to 1′) is described iteratively in j increments (Fig. 3). An effective stress concept is applied. The effective stress $\hat{\sigma}$ is defined as the difference between normal stress σ and internal stress $\tilde{\sigma}$:

$$\hat{\sigma}(t) = \sigma(t) - \tilde{\sigma}(t). \quad (2)$$

The internal stress $\tilde{\sigma}$ at the beginning and end of the four hold times are given in Table 2. Values of $\tilde{\sigma}$ at any other time points in a hold phase can be approximately determined by a linear interpolation.

For the first iteration, the creep strain increment $\Delta\hat{\varepsilon}_1$ during the time increment Δt_1 for effective stress $\hat{\sigma}_1$ is determined with a creep equation of the type Norton–Bailey [23], and the strain hardening rule is applied by varying stress (Fig. 3). The creep strain increment $\Delta\hat{\varepsilon}_1$ is calculated with the creep equation:

$$\hat{\varepsilon}_1 = \Delta\hat{\varepsilon}_1 = A\hat{\sigma}_1^n \Delta t_1^m \quad \text{with} \quad \hat{\sigma}_1 = \sigma_1 \quad (3)$$

in which the parameters A, n and m for the temperate T are temperature-dependent and determined with creep curves for the 6 reference temperatures. The total creep strain $\hat{\varepsilon}_1$ at the first iteration is the creep strain increment $\Delta\hat{\varepsilon}_1$.

The effective stress $\hat{\sigma}_1$ for the first iteration is the normal stress σ_1.

The effective stress $\hat{\sigma}_j$ and the normal stress σ_j for the increment j (j = 2 to J) are calculated as follows:

$$\hat{\sigma}_j = \sigma_j - \tilde{\sigma}_j \quad (4)$$

$$\sigma_j = \sigma_{j-1} - \hat{\varepsilon}_{j-1}/E \quad (5)$$

where E is the temperature-dependent Young's modulus. The time span $\bar{t}_j$ (Fig. 3) which the material needs to reach the total creep strain $\hat{\varepsilon}_{j-1}$ under the stress $\hat{\sigma}_j$ is to be determined as follows: